FR-6011
VOLUME II
15 DECEMBER 1973

DESIGN STUDY OF RL10 DERIVATIVES

FINAL REPORT

VOLUME II

ENGINE DESIGN CHARACTERISTICS

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Under Contract NAS8-28989

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Printed in the United States of America

FOREWORD

This technical report presents the results of the Design Study of RL10 Derivatives for Space Tug propulsion. The study was conducted by the Pratt & Whitney Aircraft Division of United Aircraft Corporation for the National Aeronautics and Space Administration, George C. Marshall Space Flight Center under Contract NAS8-28989.

The results of this study are contained in the following four volumes, which are submitted in accordance with the data requirements of Contract NAS8-28989.

Volume I	Program Summary
Volume II	Engine Design Characteristics
Volume III, Part 1	Engine/Stage Interface Control Document
Volume III, Part 2	Operation and Flight Support Plan
Volume IV	Development Plans and Program Costs

This program was initiated in the middle of February 1973, with the technical effort being completed in seven months and the delivery of the final report on 15 December 1973. The study effort was conducted under the direction of the George C. Marshall Space Flight Center Science and Engineering organization with Mr. Frederick W. Braam as Contracting Officer's Representative. This effort was carried out by Pratt & Whitney Aircraft at their Florida Research and Development Center under the direction of Mr. J.P.B. Cuffe, Study Manager.

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SECTION A
INTRODUCTION

The Design Characteristics Analysis examined selected RL10 derived candidate engines for the cryogenic Space Tug to define detailed engine system performance, mechanical and operational characteristics. This task was subdivided into two major parts: Critical Elements Evaluation and Baseline Engine Design. A brief summary of the analysis undertaken during these two subtasks may be found in the following paragraphs.

1. CRITICAL ELEMENTS EVALUATION

During a previous study (Reference 1) various RL10 derived engines were investigated and critical engine features were identified which required additional analysis to confirm their feasibility. These features were analyzed as part of this current study using the baseline engines defined in the previous study as shown in figure A-1. A summary of these studies may be found in the following paragraphs.

1.1 Idle Mode Operation and Transient Analysis

Engines defined during the previous study (Reference 1) of RL10 engines for Space Tug use incorporated a tank head idle mode of operation for engine conditioning and propellant settling, and a pumped idle mode of operation for prepressurizing the vehicle's propellant tanks.

For maximum benefits to the vehicle in the pumped idle mode of operation, the engine must be capable of stably operating with NPSH levels available from vehicle acceleration head alone while delivering large amounts of prepressurization flow. These areas received additional analysis to confirm this capability.

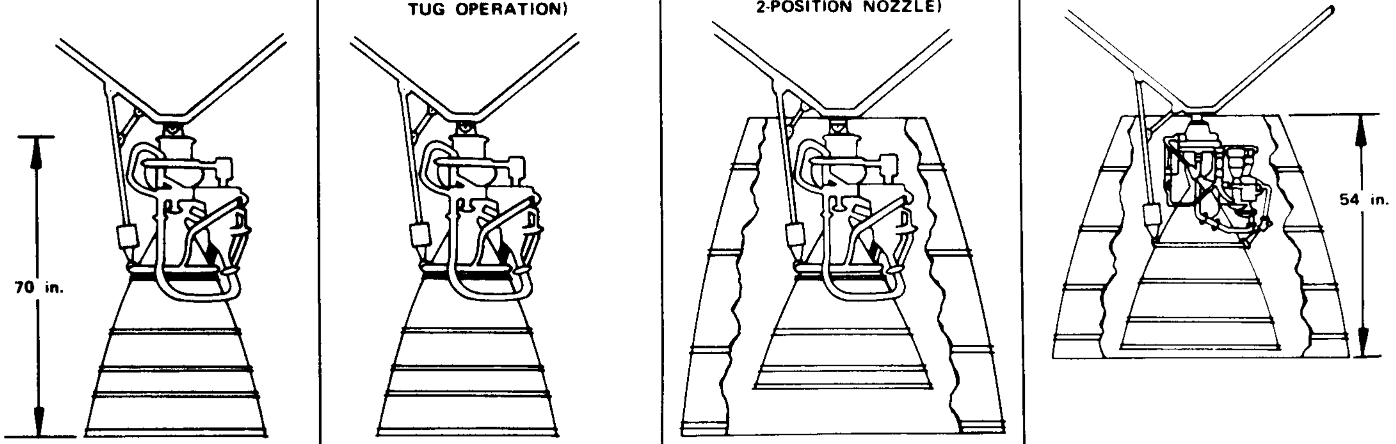
A dynamic simulation of the Category II engine was used to evaluate the dynamics of operation at tank head idle and the transition to pumped idle, and to full thrust. Analysis included an assessment of effects of propellant phase change during cooldown, control of propellants during cooldown, analysis of turbopump chilldown times, turbopump breakaway at low inlet pressures and mixture ratio, thrust and suction requirements during the acceleration.

1.2 GO_2 Heat Exchanger

An engine mounted GO_2 heat exchanger is needed to provide the gaseous oxygen bleed flow for tank pressurization and to gasify the engine oxygen prior to injection in the combustion chamber in the tank head idle mode operation. The previous engine study (Reference 1) determined the level of heat transfer necessary to provide gaseous oxygen for tank pressurization and tank head idle mode operation. During this study, these preliminary results were expanded to define the mechanical and operational characteristics of the required heat exchanger.

1.3 Two-Phase Pumping

Pump inducers of recent advanced design have demonstrated a capability to provide acceptable pressure rise when supplied with two-phase fluid. Using this ability to operate with two-phase flow at the engine inlet would completely eliminate the need for propellant tank pressurization. The mechanical and operational feasibility of providing new fuel and oxidizer inducers with two-phase pumping capability was established during the initial portion of this study.



	CATEGORY I (MINIMUM MODIFICATION)	CATEGORY II (MODIFIED FOR SPACE TUG OPERATION)	CATEGORY III (MODIFIED AND UPRATED WITH 2-POSITION NOZZLE)	CATEGORY IV ("CLEAN SHEET")
THRUST, lb	15,000	15,000	16,800	15,000
CHAMBER PRESSURE, psia	400	700	700	870
AREA RATIO	57	57	350	350
ISP, sec	438 AT 6.0 MR	440 AT 6.0 MR	466 AT 6.0 MR	467.4 AT 6.0 MR
OPERATION	FULL THRUST (LOW NPSH)	FULL THRUST (LOW NPSH) PUMPED IDLE	FULL THRUST (LOW NPSH) PUMPED IDLE	FULL THRUST (LOW NPSH) PUMPED IDLE
CONDITIONING	DUMP	TANK HEAD IDLE	TANK HEAD IDLE	TANK HEAD IDLE
WEIGHT, lb	292.5	315	431	400

Figure A-1. Baseline Engine Summary - Contract NAS8-28314 (Application of RL10 Engine for Space Tug Propulsion) FD 68809A

1.4 Two-Position Nozzle Concept

High performance versions of the RL10 have been identified which achieve increased specific impulse over that of the RL10A-3-3 by increasing nozzle expansion ratio. A fixed thrust chamber nozzle of a large area ratio design would significantly increase engine length thus reducing the length available in the Space Shuttle's payload bay for payload. A two position extendible nozzle was proposed to gain the increased performance while maintaining an acceptable stowed engine length. Since a two-position nozzle system has never been flight qualified for a rocket engine, there was some question concerning the feasibility of a lightweight configuration for use on the Space Tug. Two-position nozzle system concepts were therefore evaluated including a review of items such as actuation mechanism, sealing, and coolant flow requirements.

1.5 High Area Ratio Nozzle Performance

The Space Tug capability is sensitive to small changes in main engine specific impulse and it is therefore important for vehicle design that engine specific impulse be accurately estimated. There are abundant test and analytical nozzle data available for predicting performance levels for lower area ratio nozzle; however, for high area ratio nozzles, there are no full-scale nozzle data available. Specific impulse values were estimated during the previous study (Reference 1) by estimating the performance changes expected due to the nozzle differences from the RL10A-3-3 engine and applying these differences to RL10A-3-3 test data levels. The method of predicting specific impulse level was updated as part of this study using analytical computer programs such as the JANNAF ODK, TDK, and TBL programs, and the P&WA in-house Bell Nozzle Performance and Bell Nozzle Design Programs.

1.6 Category III Engine

The Category III engine identified in the previous study (Reference 1) was predicted to operate at a chamber pressure of 700 psia, which is 75% above the level of RL10A-3-3 engine. To achieve this higher chamber pressure condition, the turbomachinery and main chamber must be capable of operating at conditions more severe than that encountered by the RL10A-3-3 engine. A life evaluation of critical engine components for the Category III engine was therefore undertaken. Fatigue life and wear life of the critical components were identified and evaluated. The thrust chamber/nozzle design was evaluated in terms of allowable thermal cycles and allowable chamber pressure limits. Components subject to a high degree of wear such as the turbomachinery bearings, gears and face seals were evaluated in terms of allowable operating time in hours.

1.7 Category IV Engine

During the previous study, (Reference 1) analysis of the Category IV engine was limited to preliminary cycle evaluation. Since this is a clean sheet engine targeted to operate at approximately 900 psia chamber pressure, additional investigation was needed to assess component performance capabilities and mechanical design limits. The thrust chamber/nozzle design was reviewed in terms of cycle life and allowable chamber pressure limits. While the high speed turbopump performance levels were re-evaluated, analysis of various mechanical items associated with the increased speed and pressures such as rotor critical speeds, rotor thrust balance and bearing load/life analysis predominated. The critical cycle components were reevaluated and new cycle operating points generated using the updated characteristics.

2. BASELINE ENGINE DESIGN

Three specific baseline candidate engines were identified and evaluated to provide definition of hardware characteristics and the steady-state and transient operating characteristics for each of the selected engines. Cycle characteristics were generated for full thrust, pumped idle, and tank head idle operation using component characteristics defined during this portion of the study or during the Critical Element Evaluation.

Dynamic characteristics of the baseline engines were predicted for all transients, from pressurized idle to full thrust by using dynamic simulations. The analyses covered the initial ignition transient, the variations that occur during thermal and gravitational transients of tank head idle, and the transient to pumped idle and to full thrust and shutdown. Control schedules necessary to regulate propellant flowrates during tank head idle, pumped idle, full thrust, and transient operation were also defined.

Engine installation drawings were produced for each engine as well as propellant flow schematics and electrical circuit diagrams. Conceptual design drawings were made of any new major component showing materials and critical dimensions. Sufficient detail was presented in all drawings to allow the establishment of center of gravity, moment of inertia, and weight data.

Parametric studies were also conducted to determine the effects of changes in design point mixture ratio, thrust, and chamber pressure on engine specific impulse, weight, and life. The impact of environment, maintainability, interchangeability and safety constraints was also evaluated for each of the three candidate engines.

SECTION B
CRITICAL ELEMENTS EVALUATION

1. IDLE MODE OPERATION

1.1 Pumped Idle

1.1.1 Suction Requirements

For the Space Tug engine to eliminate the need for storing tank pressurants by providing bootstrap autogenous pressurization capability, it is necessary that both pumps operate at pumped idle with saturated conditions in the propellant tanks. Depending upon the level of inlet line pressure loss and the pressure gain provided by the engine produced "g" field, the propellant inlet conditions could be liquid or two phase. This study was conducted to predict the inlet conditions that would exist at both pump inlets during pumped idle and to define the pumping capability of the RL10 pumps used on the Category II engine defined during the previous study (Reference 1).

Suction characteristics of the RL10 pumps were determined by correlating RL10A-3-3 and RL10A-3-7 engine and pump rig test data generated at operating conditions ranging from full thrust to pumped idle. These correlations, documented in Section C, paragraph 3.1, were used to predict the operating capability of the Category II engine at pumped idle. The data shown in table B-1 indicates that operation with two-phase propellants at pumped idle operating conditions is feasible because the propellant Thermodynamic Suppression Head (TSH) values are predicted to be significantly greater than the required Net Positive Suction Head (NPSH).

Table B-1. Category II Engine Required NPSH at Pumped Idle

	Oxidizer	Fuel
Volumetric Flow	19.2 gpm	61.7 gpm
Rotational Speed	3513. rpm	8782. rpm
Temperature	163.8°R	38.9°R
Flow to Speed Ratio	0.0055	0.007
Suction Specific Speed	14400.	12400
Required NPSH	1.093 ft	9.86 ft
Available TSH	5.6 ft	96.3 ft

While RL10A-3-3 and RL10A-3-7 data demonstrate the capability to pump two-phase fluid at pumped idle conditions, these data are not sufficient to define the two-phase vapor volume fractions that the pumps were capable of pumping without cavitation. The pump vapor volume fraction capabilities were therefore determined analytically using the method described in Paragraph 4.1 of this section. This method defines vapor volume capabilities by the following equation:

$$\text{Vapor Fraction} = \left[1 - \frac{\Phi'_L}{B^*(A_1/A^*)}\right]$$

where:

Φ'_L = Liquid Flow Coefficient Within Blading
B^* = Blade Angle
A_1 = Effective Area At Pump Inlet
A^* = Effective Area Within Blading

Using this equation it was determined that the present RL10 oxidizer pump should be capable of pumping up to 67% vapor and the present RL10 hydrogen pump up to 74% vapor at the pumped idle operating conditions of 10% thrust.

The expected vapor volume fraction at the pump inlets during pumped idle operation was calculated using the predicted vehicle propellant supply system characteristics available at the time. The calculation considered pressure losses due to friction, propellant line area changes and bends, valves, pressure gains due to acceleration produced by the engine thrust, and the pump inlet velocity heads at pumped idle flowrates. Table B-2 summarizes the assumed geometry and the calculated pressure losses and gains. A positive NPSP of 0.12 psi was predicted available at the oxidizer pump inlet and a vapor volume less than 1% was predicted produced at the fuel pump inlet during pumped idle operation. The available inlet conditions when compared with the predicted capabilities defined for the pumps, indicate that the Category II engine should have no difficulty operating satisfactorily at pumped idle with the propellant inlet conditions available before tank pressurization.

Table B-2. Category II Engine Inlet Conditions Available at Pumped Idle

1st Burn Thrust = 1500 lb Vehicle Weight = 64,000 lb

Oxidizer	Fuel
Tank Head = 7.5 ft	Tank Head = 16.5 ft
Line Length = 3.5 ft	Line Length = 19.5 ft
Line Diameter = 2.5 in.	Line Diameter = 2.5 in.
ΔP Line Loss = -0.0031 psid	ΔP Line Loss = -0.216 psid
ΔP Acceleration = +0.1252 psid	ΔP Acceleration = +0.0204 psid
ΔP Velocity Head = - 0.0179 psid	ΔP Velocity Head = - 0.0061 psid
Propellant Inlet Conditions = positive NPSH of 0.1220 psi	Propellant Inlet Conditions = Vapor Fraction less than 1%

1.1.2 Tank Pressurization Capability

For a full thrust burn with the Category II engine defined during the previous study (Reference 1), both propellant tanks must be pressurized. The prepressurization can be provided by tapping GO_2 and GH_2 from the engine while it is operating at pumped idle (10% thrust) and returning those gases to the propellant tanks. Since the specific impulse level at pumped idle is lower than at full thrust it is advantageous to minimize the time spent in pumped idle pressurizing the tanks. The gas flowrates and temperatures, as well as the tank ullage volumes, determine the time required for pressurization of the tanks. Therefore, during the Critical Elements Evaluation, a study was made of the Category II engine's capability to supply large quantities of high temperature tank pressurization gases during pumped idle.

Liquid oxygen is gasified in a hydrogen/oxygen heat exchanger located in the turbine discharge line. The oxygen heat exchanger operating characteristics were based on the results of a heat exchanger design analysis conducted during the Critical Elements Evaluation and reported in Section B3. An evaluation of oxygen pressurant gas characteristics at pumped idle revealed that the engine can supply oxygen at satisfactory conditions up to 0.5 lb/sec. Beyond this flowrate, the pressurant would be supplied at a temperature below 240°R and the effectiveness of the pressurant gas would be reduced. The predicted oxygen pressurant temperature characteristics are presented in figure B-1 as a function of GO_2 flowrate. The figure also shows that for a fixed hydrogen pressurization gas flowrate, the GH_2 gas temperature is not significantly affected by GO_2 flowrate variations.

Hydrogen pressurant temperature characteristics were also evaluated. Pressurant temperature was found to vary little over the range of flowrate evaluated. This range of flowrate was selected to ensure that the hydrogen pressurant system did not control the required pressurization time. Hydrogen pressurant gas characteristics as a function of GH_2 flowrate are presented in figure B-2. Also shown is the effect of varying the GH_2 flow on the GO_2 pressurant gas temperature with a fixed GO_2 flowrate.

An evaluation of engine operating characteristics under bleed conditions showed that the engine cycle is not significantly affected by bleeding pressurization gas at pumped idle. As shown in figure B-3 the only parameter to be significantly affected is engine inlet mixture ratio. Thrust and specific impulse characteristics are essentially unaffected.

In summary, the Critical Elements Evaluation showed that high tank pressurization flowrates can be bled from the engine cycle at pumped idle without significantly affecting engine operation. However, the evaluation also showed that the GO_2 flowrate was limited by heat exchanger capability to approximately 0.5 lb/sec. Later during the baseline Engine Design effort the available GO_2 flowrate was increased by redesigning the heat exchanger and raising the pumped idle operating level.

1.1.3 Combustion and Fuel System Instability

A study was conducted during the Critical Elements Evaluation to define the stability characteristics of the Category II engine while operating at pumped idle (10% thrust). Both combustion (chugging) and fuel system boiling instabilities were addressed in the study.

Chugging instability (approximately 150 cps) occurs at low thrust levels when the pressure losses across the oxidizer injector are low and unable to prevent pressure fluctuations in the combustion chamber from feeding back through the injector. Chugging instability is a concern only at pumped idle. At tank head idle the GO_2 heat exchanger vaporizes the oxidizer flow upstream of the injector and this increases the pressure loss across the injector sufficiently to prevent chugging instability.

Fuel system instability (approximately 2 cps) is caused by boiling in the thrust chamber cooling jacket. This unstable boiling occurs at low thrust levels when coolant flowrates and velocities in the coolant tubes are low. During pumped operation a coupling between the pressure oscillations in the cooling jacket and the fuel pump takes place resulting in large-magnitude flow variations.

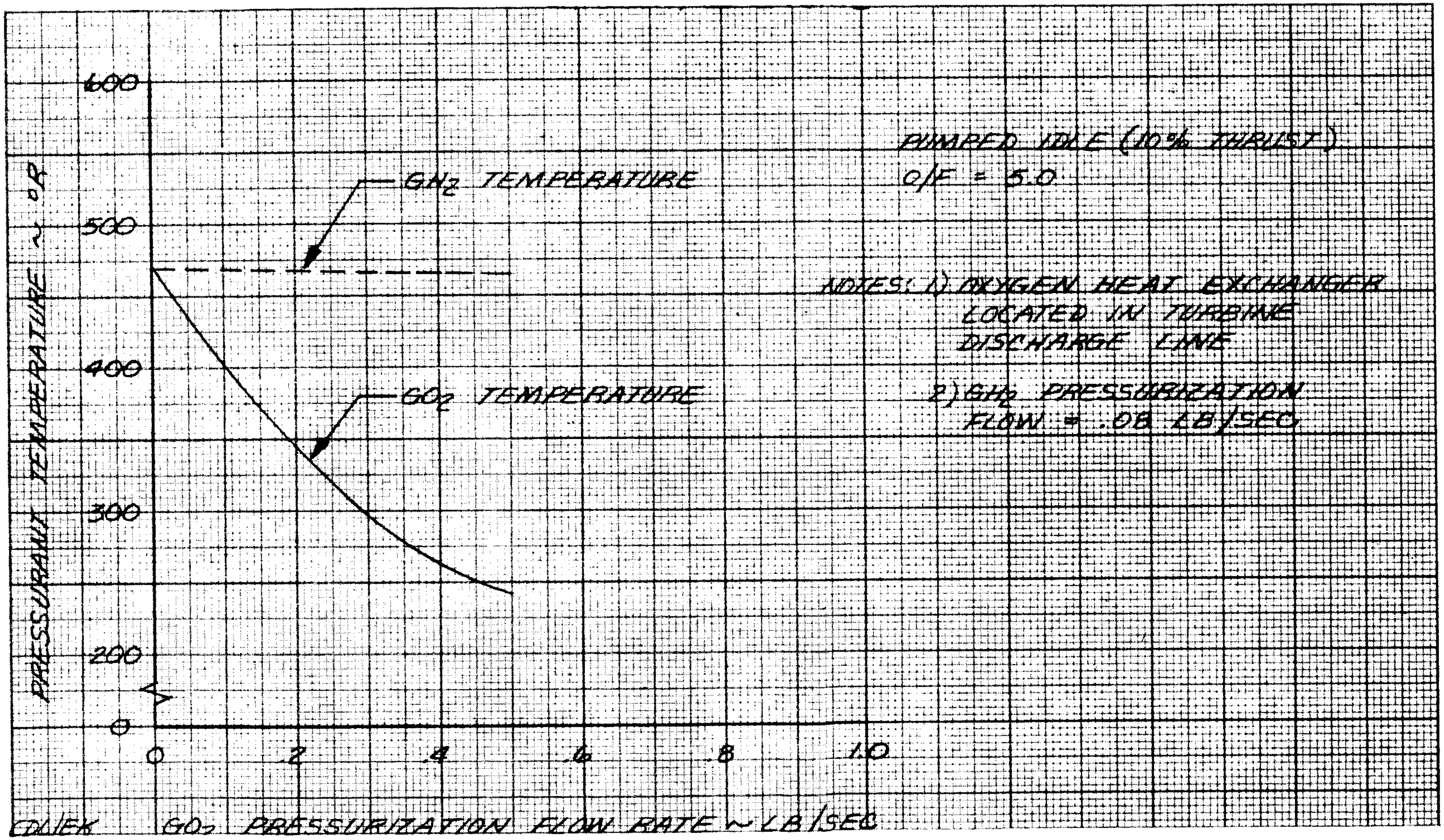


Figure B-1. Effect of Varying GO_2 Pressurization Flow on Pressurant Temperature Category II Engine DF 97443

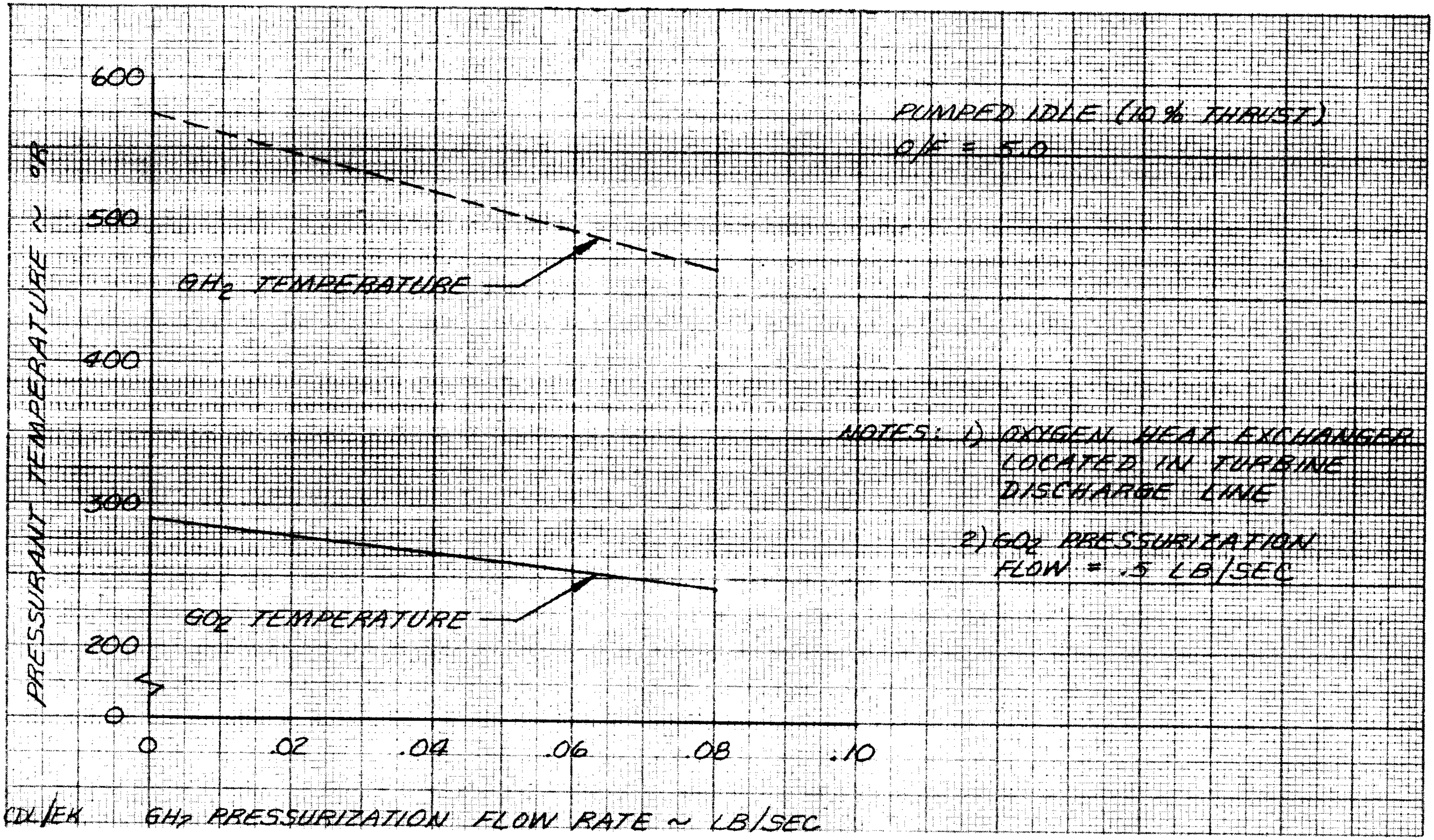


Figure B-2. Effect of Varying GH_2 Pressurization Flow on Pressurant Temperature Category II Engine DF 97442

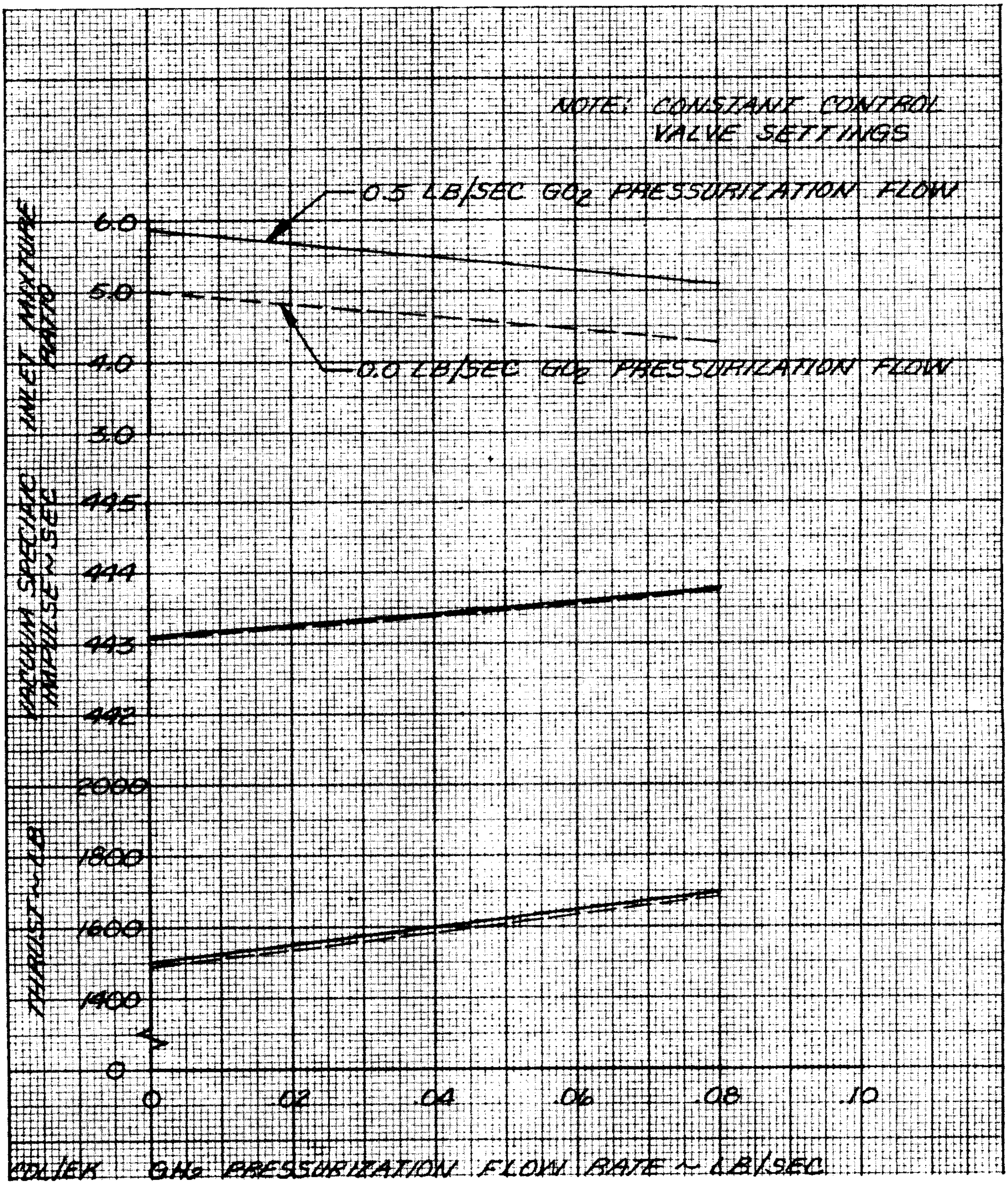


Figure B-3. Effect of Pressurization Flowrate on Pumped Idle Characteristics Category II Engine

DF 97444

Stability characteristics were defined by reviewing RL10A-3-7 engine test results. The occurrence of instability was determined to be a function of both chamber pressure level and mixture ratio. The regions of unstable engine operation predicted for the Category II engine are shown in figure B-4. Both fuel system and chugging instability could occur at the 10% pumped idle thrust level.

The test data showed that at the Category II engine pumped idle operating conditions, fuel side instability could cause chamber pressure variations of ±3 psi at a frequency of approximately 2 cps unless a cavitating venturi was added to the engine. RL10A-3-7 test data confirmed that a cavitating venturi installed between the turbopump and the main chamber cooling jacket could keep the boiling instability from feeding back into the fuel pump. Chamber pressure oscillations caused by coolant boiling were not apparent in any of the data with a cavitating venturi installed in the system.

The test data also showed that at pumped idle operating conditions chugging instability would be expected to cause chamber pressure oscillations of approximately +4.5 psi at a frequency of 150 cps. Because of the relatively high frequency of these oscillations, the turbopump is not affected by them. No detrimental effects to the engine as a result of the oscillations were observed during any of the tests. It was concluded that chugging instability of this magnitude, while undesirable, should not adversely affect engine operation.

Later in the Baseline Engine Design effort, the pumped idle thrust level was raised from 10 to 25 percent. As previously indicated in figure B-4 this level is outside of both instability regions eliminating chugging as a worry item and permitting the cavitating venturi to be removed.

1.2 Tank Head Idle

Tank Head Idle (THI) is a pressure fed mode of operation with nonrotating turbopumps. It is used to settle propellants and condition the turbopumps before accelerating the engine to higher thrust levels without incurring excessive performance penalties. It can also be used to provide low thrust levels for small ΔV changes.

In tank head idle the engine must be capable of starting and operating with the propellants supplied from an unsettled tank in a zero gravity environment. These propellants may be any combination of vapor, mixed phase or liquid propellants. Control of mixture ratio to prevent component overheating or engine flameout under these conditions could be difficult and because of the low flowrates involved stable engine operation is also an area of concern.

While a closed loop control system was used previously during the RL10A-3-7 testing to provide satisfactory control of the engine in tank head idle, such a control system is complex and expensive. To alleviate the need for such a control system and to also eliminate the possibility of chugging instability, a gaseous hydrogen/liquid oxygen heat exchanger concept was baselined in the previous study (Reference 1) for all engines with tank head idle. The GO_2 heat exchanger was to be designed to vaporize the oxygen downstream of the pump thus ensuring the delivery of gaseous oxygen to the injector under all tank head idle operating conditions. Limited analysis completed during the previous study indicated that the hydrogen used for thrust chamber cooling could supply the heat needed to gasify the oxygen.

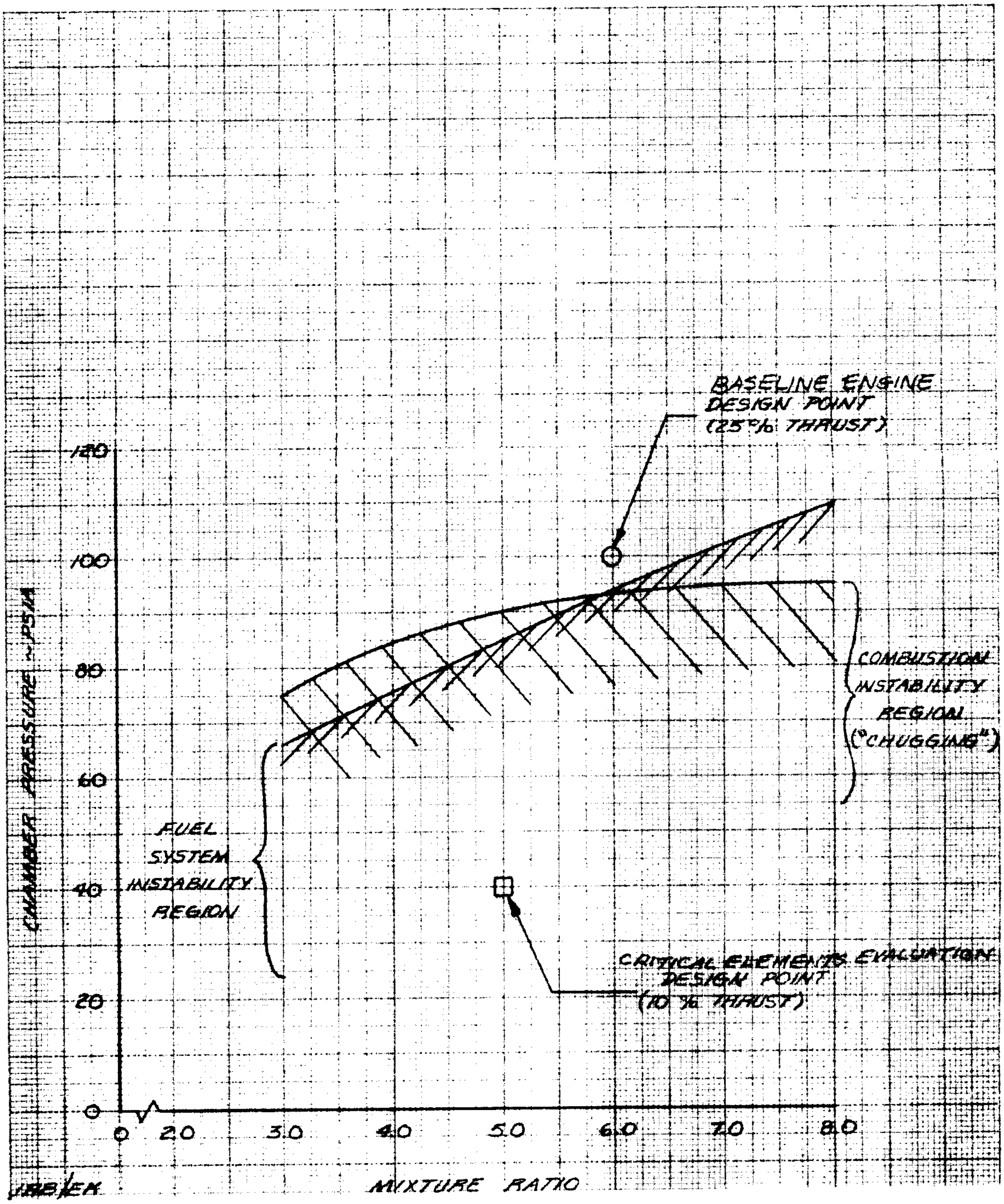


Figure B-4. Estimated Engine Stability Limits Derivative II

DF 96512

Studies were completed during the Critical Elements Evaluation to determine if the engine could in fact be conditioned and operated satisfactorily with a GO_2 heat exchanger in tank head idle with fixed control areas over a wide range of inlet pressures, temperatures, qualities, and initial pump housing temperatures. An examination was also made of the effects of operating the engine without the GO_2 heat exchanger.

The Category II engine configuration was used for the Critical Elements Evaluation of tank head idle since its characteristics were well known and the results should be generally applicable to all of the engines. For this study the GO_2 heat exchanger was located in the turbine discharge line and it derived its heat from the total fuel flow. Later during the Baseline Engine Design effort (Section C), the location was changed; however, the conclusions obtained from these Critical Elements Evaluation are valid.

1.2.1 Engine Operation During THI With and Without a GO_2 Heat Exchanger

The first step in the Critical Elements Evaluation was to confirm the need for a GO_2 heat exchanger for satisfactory THI operation. This was accomplished through the use of a computer simulation programmed to match an engine with and without a GO_2 heat exchanger. The control valve areas were set to provide an engine mixture ratio of 4.0 under equilibrium conditions at the end of THI. The simulated THI cooldown was made with 16 psia saturated liquid inlet conditions and 500°R initial pump housing temperatures.

The resultant predicted operating characteristics without a GO_2 heat exchanger were unacceptable. Chamber flameout problems were apparent early in the cooldown because of extremely low mixture ratios (i.e., less than 0.5). The low mixture ratios occurred because the small oxidizer control valve area required to hold chamber mixture ratio at 4.0 after the pumps are conditioned restricts the flow early in the transient when the oxidizer is gasified while flowing through the pump. Because starting oxidizer flowrates and mixture ratios would be much lower with the saturated vapor conditions that would exist before the propellants in the tanks are settled, it is likely that under these conditions the engine would not ignite.

The characteristics obtained with a GO_2 heat exchanger were acceptable. The mixture ratio variations predicted during this cooldown were considerably less since the GO_2 heat exchanger provided a self compensating control on engine mixture ratio. As mixture ratio decreased, thrust chamber heat and hydrogen temperature also decreased thereby lowering the oxygen temperature and increasing oxygen flow and mixture ratio. Mixture ratio and thrust characteristics for both of the cases are compared in figure B-5.

Engine cooldown and operating characteristics with and without the GO_2 heat exchanger are summarized in table B-3. Engine cooldown requirements were significantly affected by the GO_2 heat exchanger. An additional thirty-six seconds and 3.3 lb more propellants were required to condition the engine without the GO_2 heat exchanger. The oxidizer pump was primarily affected requiring a significantly longer cooldown time but less total propellants. The total fuel and oxidizer cooldown propellants increased, however, because the engine had to be operated an additional 38 seconds to cool the oxidizer pump after the fuel pump had been cooled.

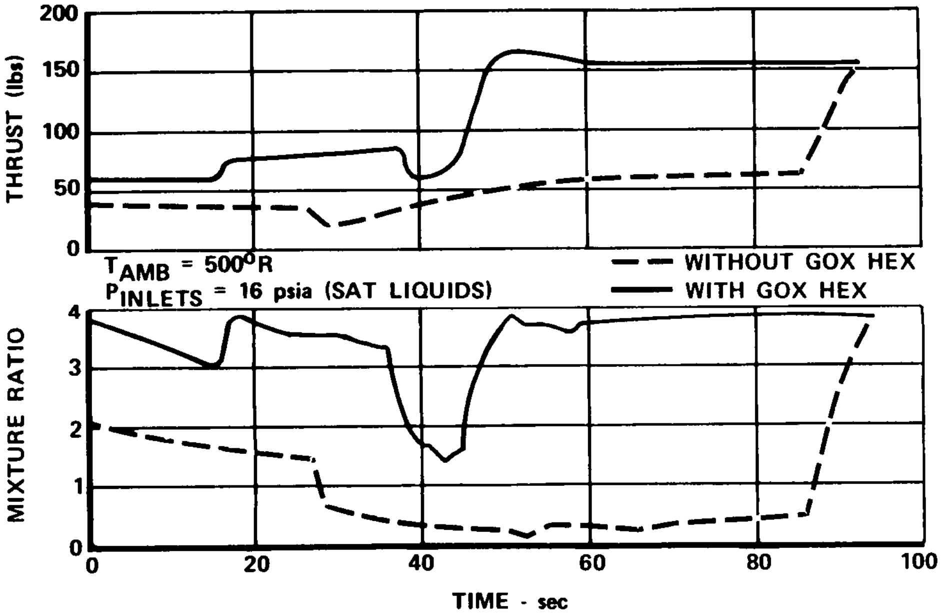


Figure B-5. Effect of GO_2 Heat Exchanger on Tank Head Idle Transient, Category II Engine

FD 71336

Table B-3. Effect of GO_2 Heat Exchanger on Tank Head Idle Operating Characteristics

	Without GO2 Hex	With GO_2 Hex
Mixture Ratio Range		
Minimum	0.2	1.5
Maximum	4.0	4.0
Cooldown Time, sec (see note 2)		
Oxidizer	84*	44
Fuel	46	48*
Propellants Consumed, lb (see note 3)		
Oxidizer	4.4	6.2
Fuel	7.2	2.1
Total	11.6	8.3
Engine Stability	Some low frequency combustion instability predicted.	Stable

NOTES:

1. Pump Inlet Pressures = 16 psia; Initial Pump Housing Temperatures = 500°R
2. Cooldown duration defined as the time period in THI required to cool the fuel pump 1st stage housing temperature to 50°R and the oxidizer pump housing temperature to 190°R
3. Cooldown consumption defined as the amount of propellants consumed by the engine to condition the pumps to the levels indicated in item 2 above.

*Cooldown time used to determine propellant consumption.

Operation without a GO_2 heat exchanger could result in some combustion instability due to the small oxidizer injector pressure drop predicted with liquid flow. Heat transfer takes place in the injector from the fuel to the oxidizer, however, and it is unlikely that the instability would be severe. Data from RL10A-3-7 tank head idle tests indicated that only moderate chugging instability would occur.

Chugging instability during THI should not occur on an engine with a GO_2 heat exchanger. With gaseous oxygen the flow through the injector is choked and with choked flow across the oxidizer injector, variations in chamber pressure cannot affect the oxidizer flowrate.

1.2.2 Engine Operation in THI With a GO_2 Heat Exchanger

It was concluded during the first part of this study that a GO_2 heat exchanger was essential for satisfactory operation of the engine without an active control system. During the second part of the study the effects of changes in inlet conditions (phase and pressure) and initial turbopump housing temperatures were evaluated. Particular emphasis was placed on determining the effects of these variables on cooldown time, propellant consumption, thrust and mixture ratios during THI.

The same mathematical simulation of the Category II engine used to evaluate the GO_2 heat exchanger was used for the evaluation of these variables. Control area settings were used for most of the cases that would provide an equilibrium mixture ratio of 4.0 with saturated liquid propellants.

The effects of variations in both pump inlet pressures and pump housing temperatures were determined with 16 psia and 20 psia saturated liquid inlet pressures at initial pump housing temperatures of both 350°R and 500°R. The changes in inlet pressures and pump housing temperatures caused only minor changes in the engine operating characteristics. This illustrates the capability of the GO_2 heat exchanger to limit variations in mixture ratio as environmental and/or inlet conditions change. The minimum mixture ratio of 1.5 indicated during these transients occurred with minimum inlet pressures and maximum initial pump housing temperatures whereas the maximum mixture ratio of 5.9 occurred at maximum inlet pressures and minimum initial pump housing temperatures. Mixture ratio excursions were similar in all cases as shown in figures B-6 and B-7. Operating characteristics are summarized in table B-4.

Increasing the inlet pressure from 16 to 20 psia resulted in a 5-7 sec decrease in cooldown time, a 1.7 lb increase in the consumed propellants, and an increase in THI steady-state thrust of 47 lb. Decreasing the initial pump housing temperature from 500°R to 350°F reduced the cooldown times 11 to 13 sec and decreased the total propellants consumed by 0.6 lb. In all of these cases, the cooldown time was set by the time required to condition the fuel pump. It was apparent from these simulations that inlet pressure and initial pump housing temperature changes do not significantly affect the cooldown characteristics of the engine.

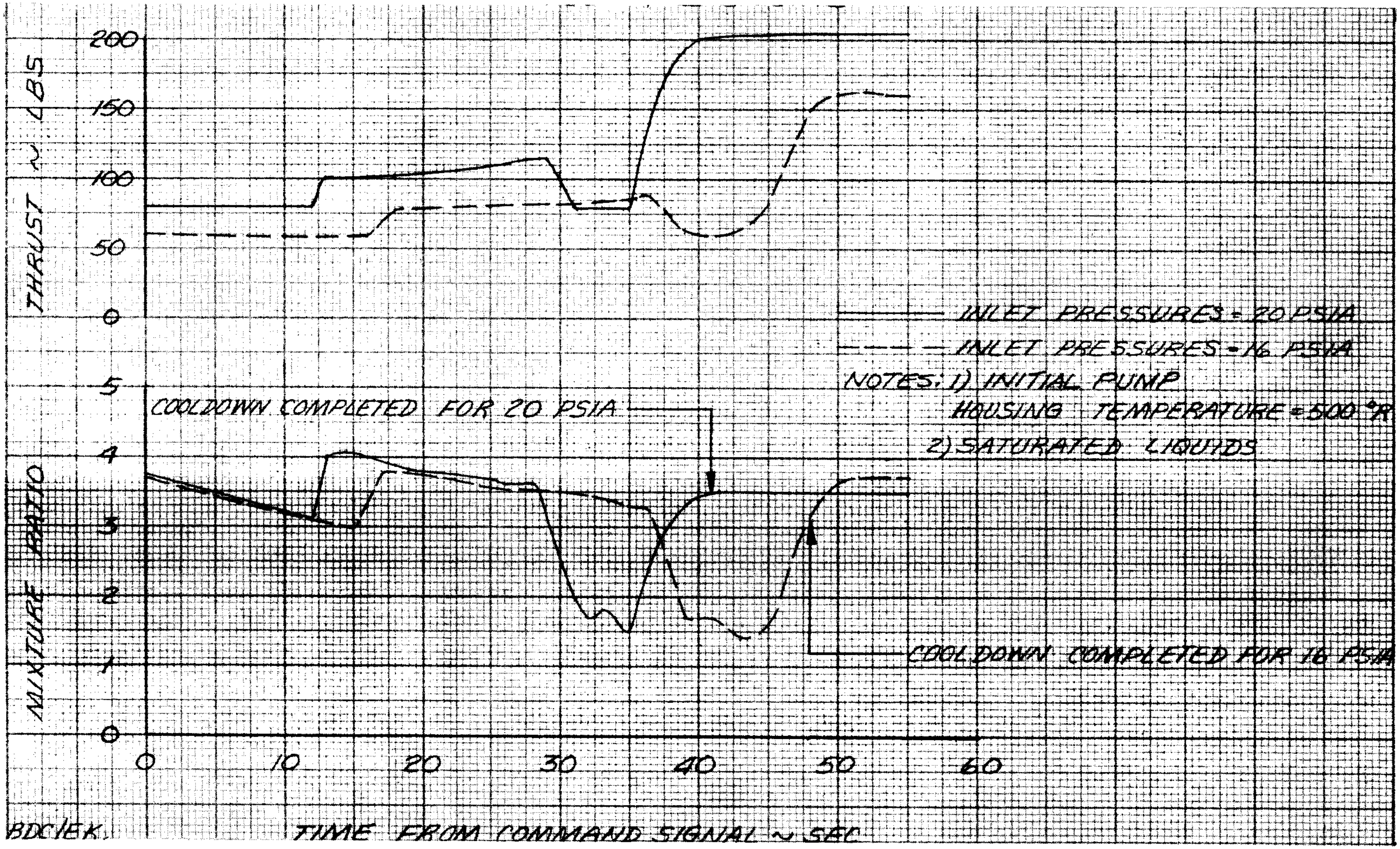


Figure B-6. Effects of Inlet Pressures on Tank Head Idle Transient Category II Engine

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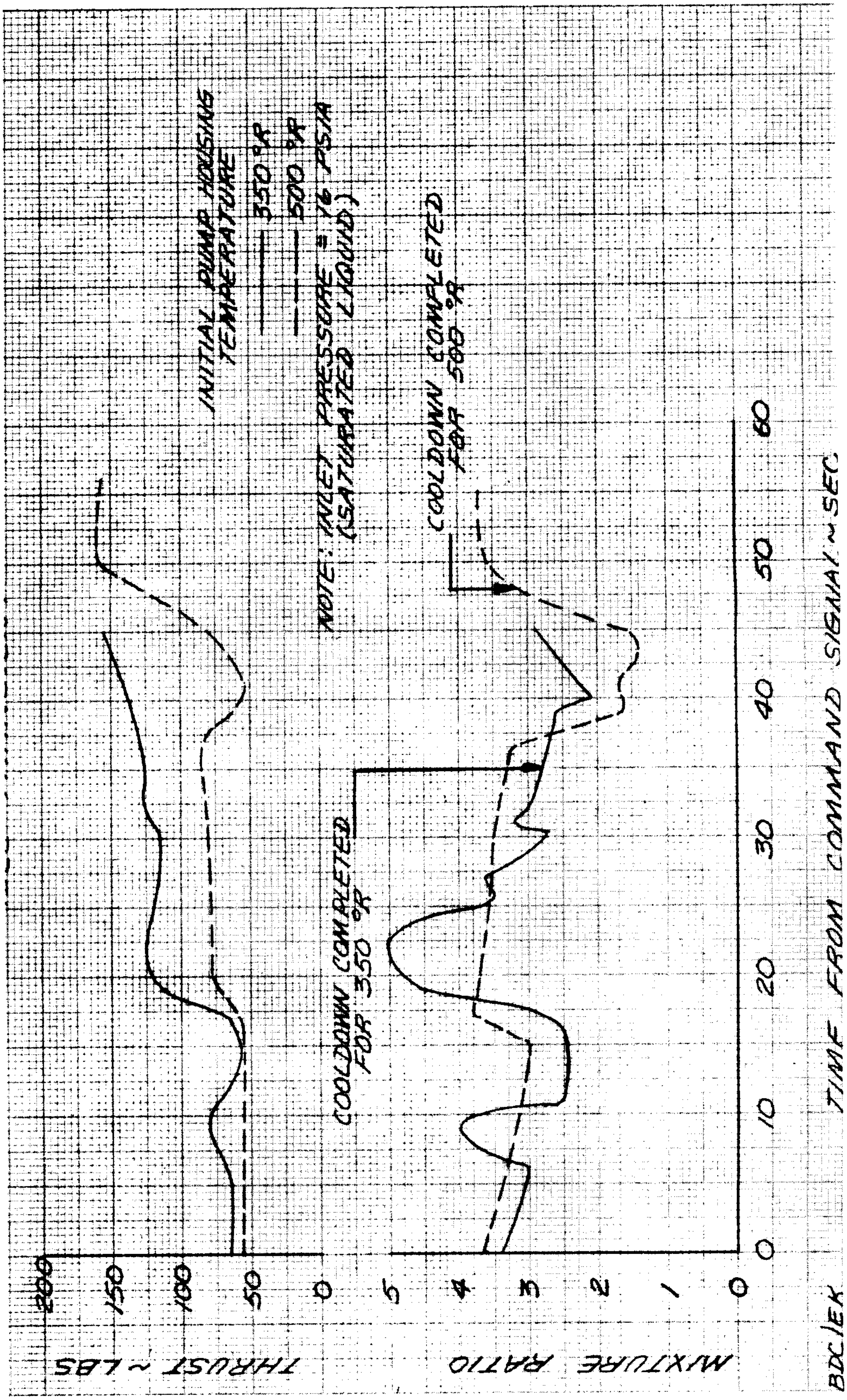

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Figure B-7. Effects of Initial Pump Housing Temperatures on Tank Head Idle Transient

Table B-4 summarizes the effect of inlet pressure and initial pump housing temperature changes on THI operating characteristics.

Table B-4. Effect of Inlet Pressures and Initial Pump Housing Temperatures on THI Operating Characteristics

Inlet Pressure, psia	16			20		
Inlet Conditions (Saturated)	Liquid/Liquid			Liquid/Liquid		
Thrust (Engine (Conditioned, lbs	142			189		
Mixture Ratio (Engine Conditioned)	4.0			4.0		
Specific Impulse (Engine (Conditioned), sec	391			400		
Cooldown Time, sec	Oxidizer	Fuel		Oxidizer	Fuel	
With Initial Housing Temperatures of 350°R	16	35		12	30	
With Initial Housing Temperatures of 500°R	44	48		34	41	
Propellants Consumed, lbs	Oxidizer	Fuel	Total	Oxidizer	Fuel	Total
With Initial Housing Temperatures of 350°R	6.0	1.7	7.7	7.4	2.0	9.4
With Initial Housing Temperatures of 500°R	6.2	2.1	8.3	7.6	2.4	10.0
Mixture Ratio Range During Cooldown, (min/max)						
With Initial Housing Temperatures of 350°R	2.1/5.0			2.0/5.9		
With Initial Housing Temperatures of 500°R	1.5/4.0			1.5/4.0		

NOTE:

Cooldown consumption is based on cooldown time required by fuel pump.

The effect of having saturated vapor at the pump inlets during tank head idle was also evaluated. It was realized that this was not a realistic case because the propellants should settle prior to pump cooldown and the engine flow would be liquid. However, useful information was obtained for the early part of the cooldown when the propellants would not normally be settled, and it also provided an upper limit for cooldown time. Inlet pressures of 16 psia and an initial housing temperature of 500°R were used and the control valve areas were held at the same settings that were used for the saturated liquid cases. The simulation was run for 170 sec without satisfactorily conditioning the pumps. Sufficient data were available, however, to extrapolate the housing temperatures and determine the required cooldown time.

Thrust and mixture ratio characteristics for this run are shown in figure B-8. Thrust levels during the first 60 sec of the transient ranged between 50 and 65 lb. These thrust levels should be acceptable for settling propellants in the tanks as vehicle studies have shown that only 20-25 lb is required. It was estimated that 215 sec would be required for a complete cooldown of the engine, and that mixture ratio would vary between 1.3 and 4.0 during this period. This long cooldown time was required by the oxidizer pump. The fuel pump was conditioned after 48 sec.

1.3 Conclusions

The following conclusions were reached as a result of the Critical Elements Evaluation of idle mode operation.

1.3.1 Pumped Idle (10% Thrust)

a. An RL10 Space Tug engine could successfully operate in pumped idle with saturated propellants in the vehicle tanks.

b. The engine can supply over 0.08 lb/sec of hydrogen pressurant and 0.5 lb/sec of oxygen pressurant at pumped idle without adversely affecting engine operation.

c. While the engine will encounter some (±4.5 psi at 150 cps) chugging instability at pumped idle, fuel system instability can be avoided by placing a cavitating venturi downstream of the fuel pump.

1.3.2 Tank Head Idle

a. Engine mixture ratio characteristics would be unacceptable with a fixed area control system and no GO_2 heat exchanger

b. With a GO_2 heat exchanger, mixture ratios should remain well within acceptable limits with fixed area controls even though inlet conditions (pressure, temperature and phase) and initial pump housing temperatures may vary over the range of anticipated Space Tug conditions

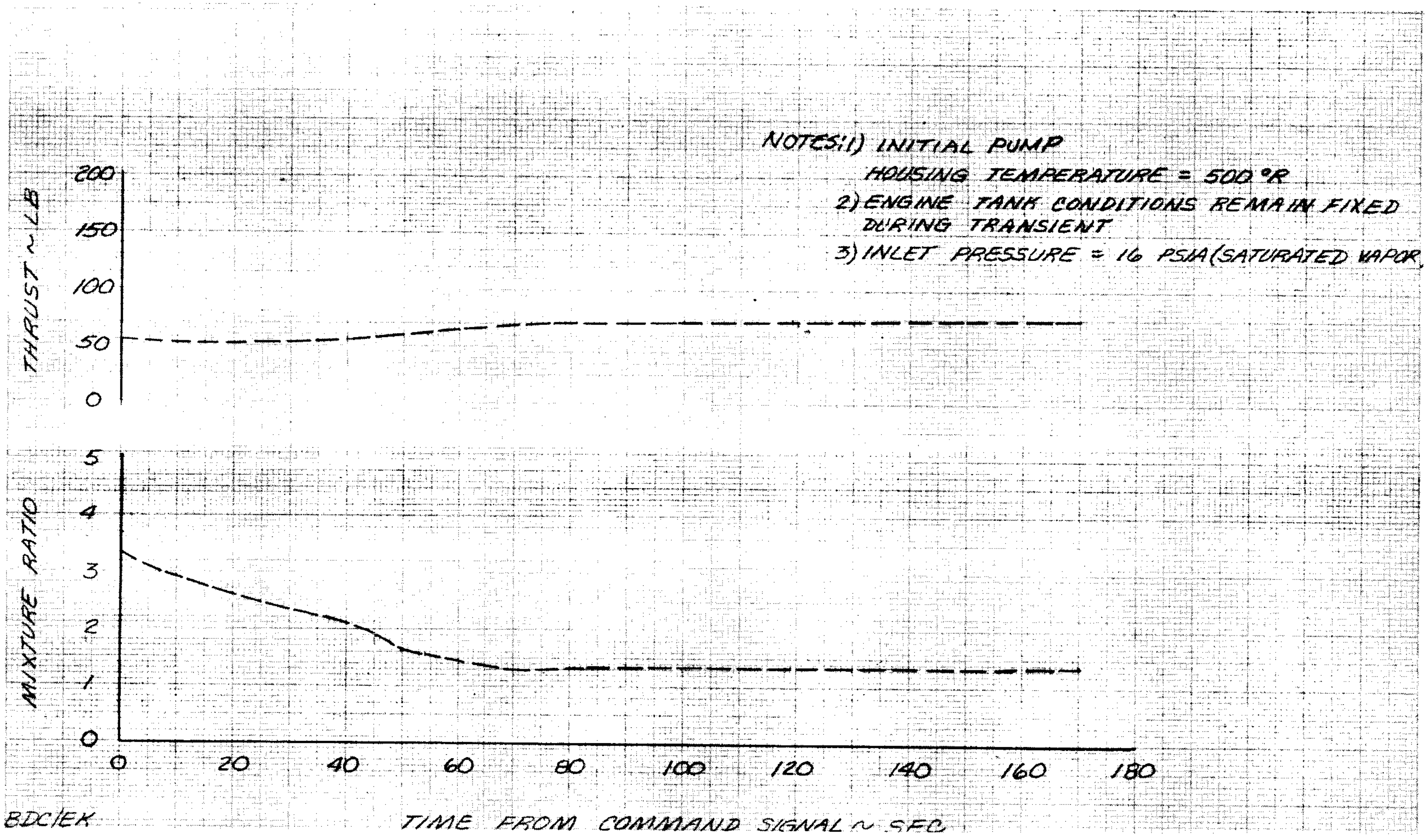

Figure B-8. Tank Head Idle Transient With Saturated Vapor at Engine Inlets Category II Engine

DF 97597

c. Sufficient thrust levels will exist early in the cooldown with saturated gas propellants to satisfactorily settle propellants in the tanks.

d. Depending on propellant conditions supplied to the engine the required cooldown times in tank head idle could vary between 0.5 min and 3.5 min. With the propellant conditions expected for the Space Tug the cooldown times should be nearer the minimum value.

2. TRANSIENT ANALYSIS

The acceleration transient is always a critical period of operation for any rocket engine and since a unique method of acceleration was recommended for the Category II engine during the previous study (Reference 1) additional analysis was needed in this area. The engine was to be accelerated to pumped idle with saturated liquid propellants in the tanks and to full thrust with minimum additional pressurization in the tanks. Because of pressure losses in the propellant feed system the acceleration to pumped idle may have to be accomplished with two-phase propellants at the engine pump inlets. Because this mode of operation was recommended based on limited transient analysis during the previous study, further studies were required.

During the Critical Elements Evaluation, studies of the engine transients from tank head idle to pumped idle and from pumped idle to full thrust were undertaken. The Category II engine was chosen for analysis because its operating characteristics were better defined and the conclusions reached for it should be applicable to other derivative engines. The transients were evaluated using computer simulations of the engine cycle and propellant feed system. Interactions between the engine and feed systems were considered and control requirements and transient operating characteristics defined.

2.1 Tank Head to Pumped Idle Transient

The transient from tank head to pumped idle was evaluated during the Critical Elements Evaluation with emphasis on turbopump breakaway torque characteristics, control system requirements and engine/vehicle suction characteristics. Turbopump breakaway torque was considered a critical item because Space Tug vehicle studies were trending toward low propellant tank pressure levels. Control requirements were studied in combination with suction characteristics to establish the level of control complexity needed to provide a cavitation-free acceleration to pumped idle without tank pressurization.

The predicted level of available turbine torque for the Category II Engine was determined using a dynamic transient computer model of the engine. The dynamic model was adjusted to match data from special RL10 nonrotating torque tests as shown in figure B-9. Breakaway torque requirement levels were established from accumulated measurements taken on RL10 production engines. Breakaway torque margin was established by comparing the predicted available starting torque levels with the demonstrated required levels of torque.

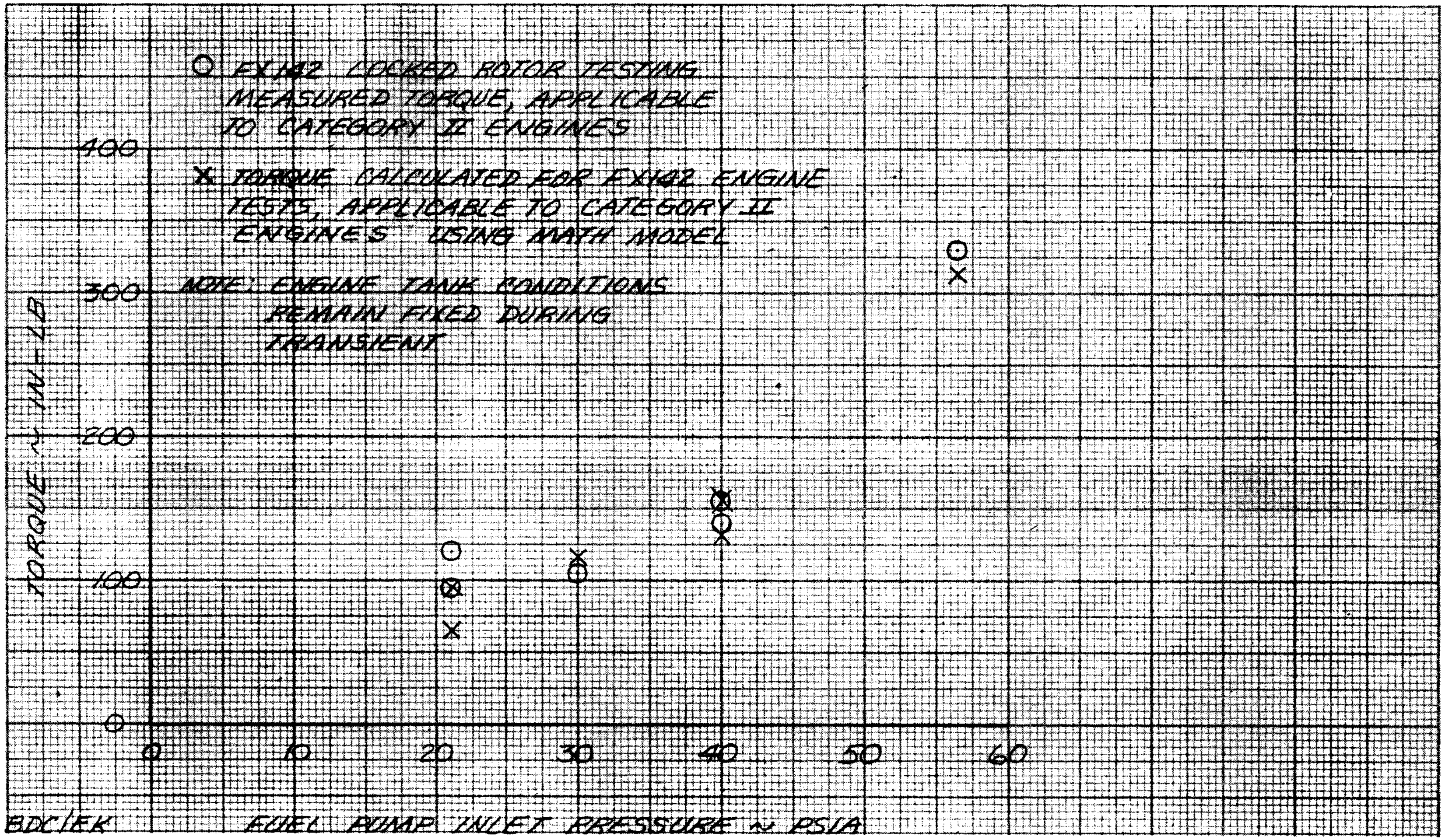

Figure B-9. Category II Engine Starting Torque Model Matches Test Data

DF 97601

Results of this comparison showed that the Category II engine predicted turbine torque was greater than the worst required level (nominal plus 3σ) at the anticipated fuel pump inlet pressures of 16 psia. The torque levels predicted over a large range of fuel pump inlet pressure are shown in figure B-10. Predicted breakaway torque requirement levels have also been superimposed to show how the available and required torques compare.

The dynamic transient computer simulation was also used to evaluate transient characteristics and establish required control functions between Tank Head Idle and pumped idle. Two criteria were considered necessary to achieve a successful tank head to pumped idle transient: (1) adequate mixture ratio control to prevent flameout or thrust chamber overheating and (2) an acceleration rate low enough to keep the two-phase propellant vapor volumes at the pump inlets from exceeding the pump's capabilities.

Successful transients were obtained with an open loop control system with all of the valve functions being accomplished either with solenoid supplied helium pressure or internal engine pressures. Thrust and speed reached rated levels in approximately 1.6 sec and, as shown in figure B-11, predicted mixture ratio remained between 2.5 and 6.0 (well within safe limits) during the entire transient. The predicted vapor volumes at the engine inlet during the acceleration from tank head to pumped were very close to zero for the entire transient. Predicted vapor volume capabilities were sizable as shown in figure B-12.

The two-phase propellant conditions expected at the pump inlets during this transient were determined using a simulation of the propellant suction lines combined with the engine transient characteristics. Entrance, valve, bend and friction pressure losses were considered along with the pressure losses resulting from flow acceleration and the pressure gains resulting from vehicle acceleration. Because of the rapid changes in fluid properties with two-phase flow, the suction line simulation was broken into 6 in. increments and the propellant conditions at the end of each increment used in the calculations for the succeeding increment. The assumed inlet line geometry is defined in figure B-12.

The two-phase pumping characteristics of the Category II engine pumps were estimated using the following equation along with flows and pump speeds from the engine transient simulation:

$$\alpha_c = \left[1 - \frac{\phi_L'}{\beta^* (A_1/A^*)} \right]$$

α_c = vapor fraction

ϕ_L' = liquid flow coefficient within the blading

β^* = blade angle

A_1 = effective area at pump inlet

A^* = effective area within blading

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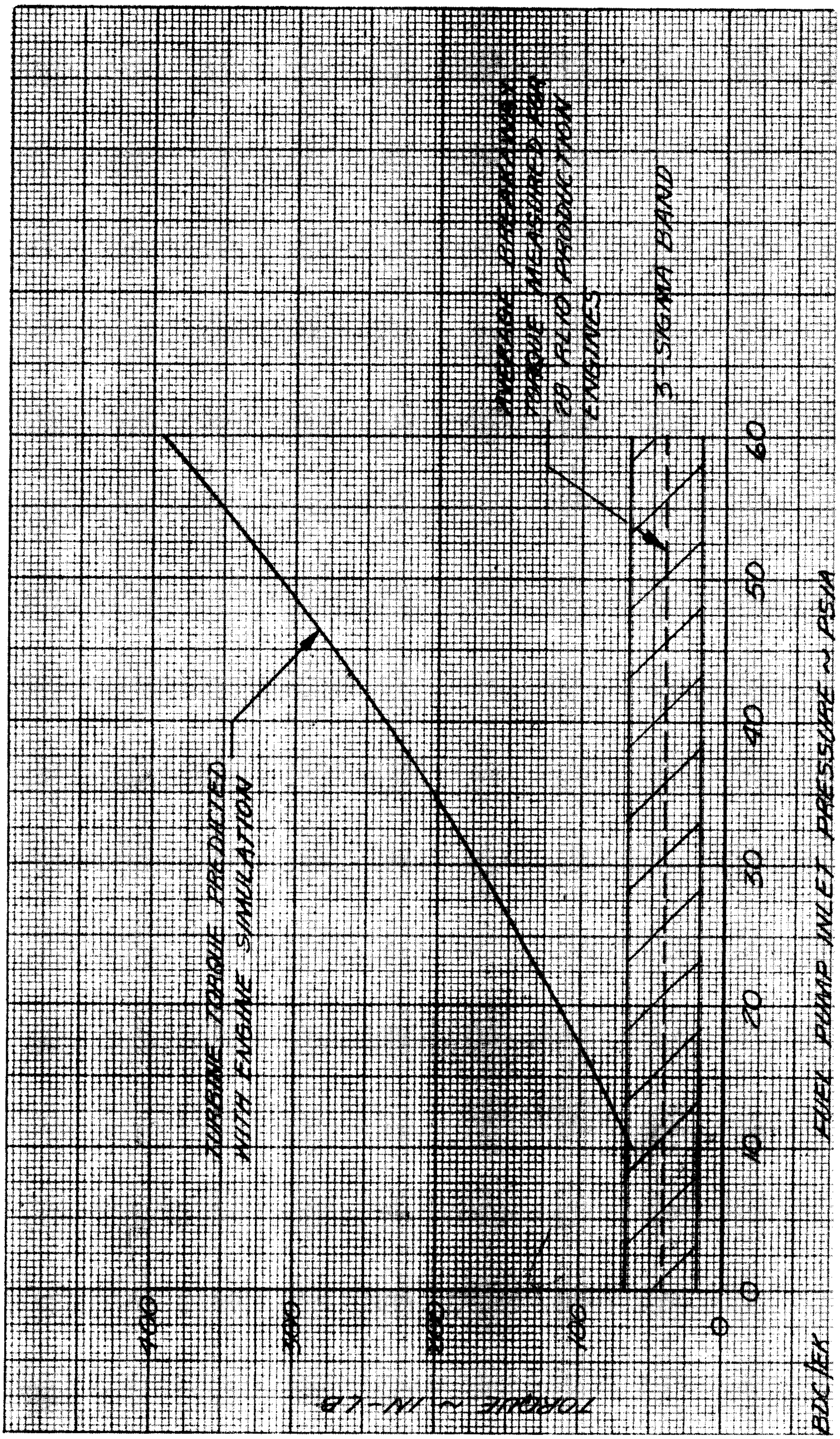


Figure B-10. Estimated Category II Engine Breakaway Torque Margin

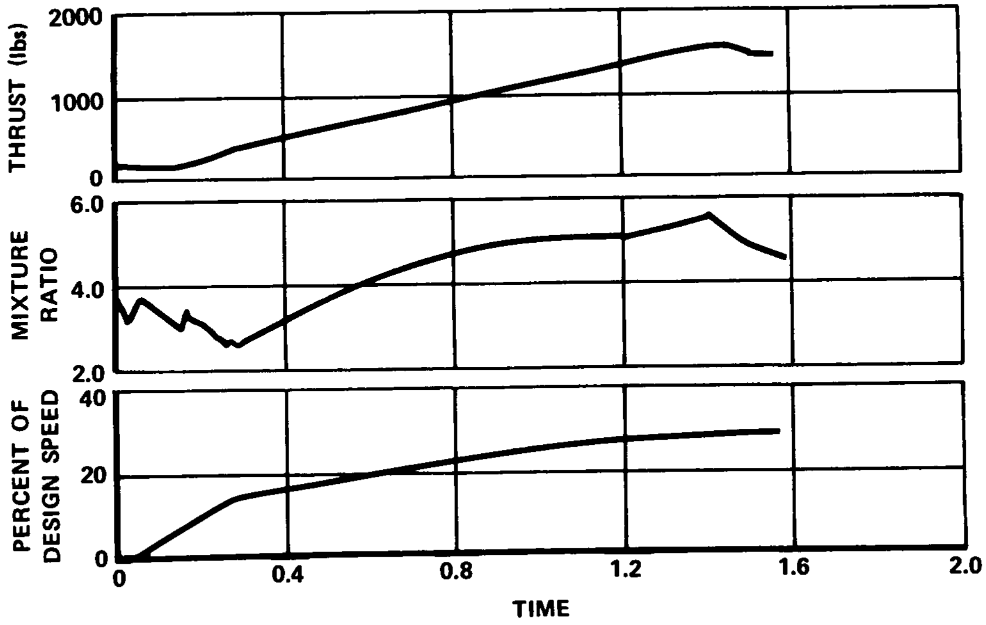


Figure B-11. Tank Head Idle to Pumped Idle Transient Category II Engine

FD 71362

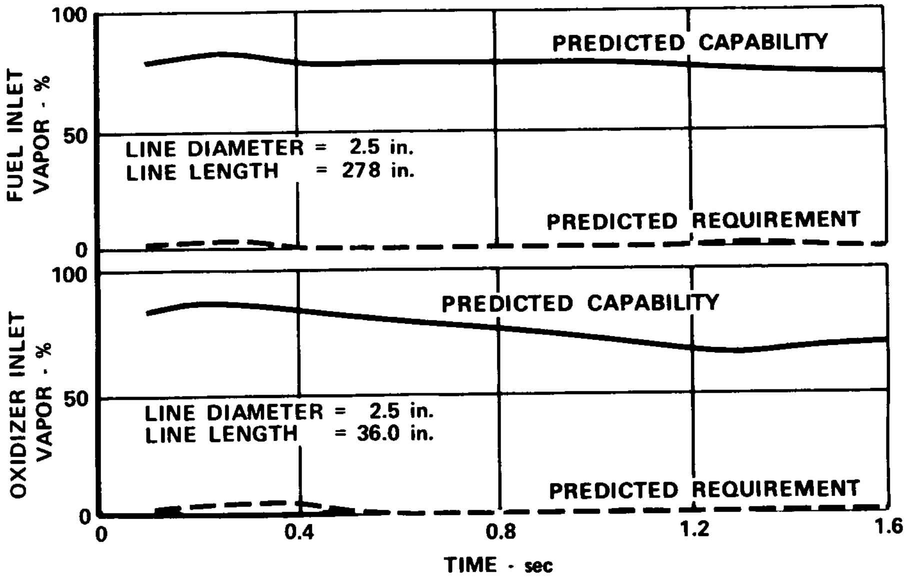


Figure B-12. Tank Head to Pumped Idle Transient Category II Engine

FD 71338A

2.2 Pumped Idle to Full Thrust Transient

The pumped idle to full thrust transient for the Category II engine was also evaluated during the Critical Elements Evaluation. The primary areas of concern during this analysis were control requirements and minimum tank NPSP levels required for a successful acceleration.

The characteristics for this transient were defined using the same dynamic transient computer model that was used to define the THI to PI transient characteristics. The suction line simulation was also the same one used in that analysis.

The requirements for this transient were similar to those for the THI to PI transient; i.e., mixture ratio should stay within safe operating limits and the acceleration should be gradual enough to minimize suction line pressure losses and keep the tank NPSP requirements to a minimum, preferably no higher than the levels required for steady state operation. To avoid design complexity it was also desirable that the transient be accomplished using an open loop control system whenever possible. The RL10A-3-3 engine closed loop thrust control could be used if necessary to control the transient overshoot.

The Category II engine pumped idle to full thrust transient was initially simulated with a completely open loop control system. All control functions were actuated either by solenoid supplied helium pressure or with engine internal pressure changes. During the transient, mixture ratio remained well within safe operating limits. However, thrust exceeded the full thrust level and the turbopump's overspeed near the peak caused undesirable propellant suction characteristics. The thrust, mixture ratio and turbopump speed characteristics obtained are shown in figure B-13. Also, the available and required pump NPSP levels determined for the transient are shown in figure B-14. Because of the overshoot in pump speed and flowrate that occurred during the overshoot period, the required NPSP levels were significantly higher during the start transient than during steady-state. This did not meet the desired criteria and it was apparent that some means of reducing overshoot would be required to reduce the transient NPSP requirements.

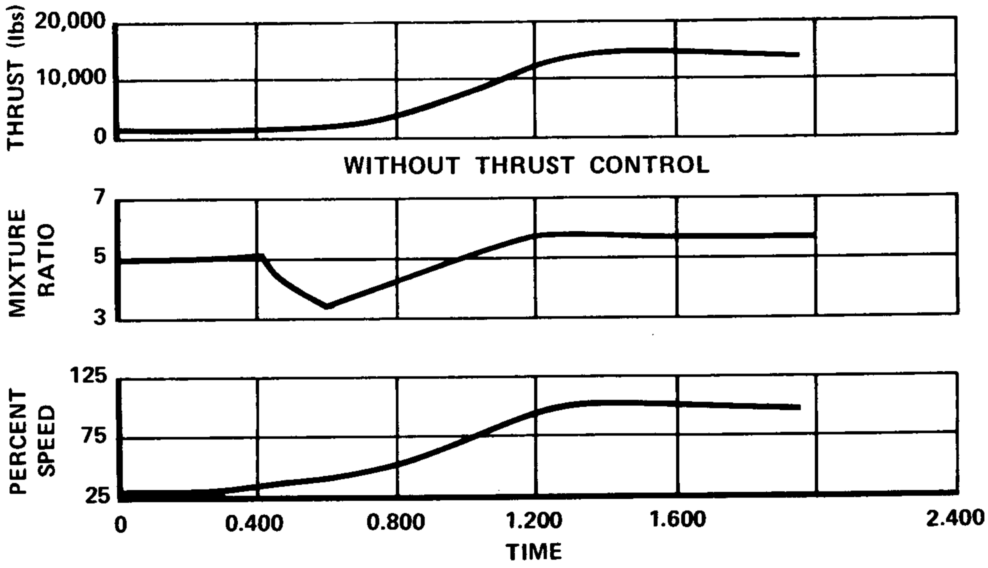


Figure B-13. Thrust, Mixture Ratio and Speed During Pumped Idle to Full Thrust Transient Category II Engine

FD 71319

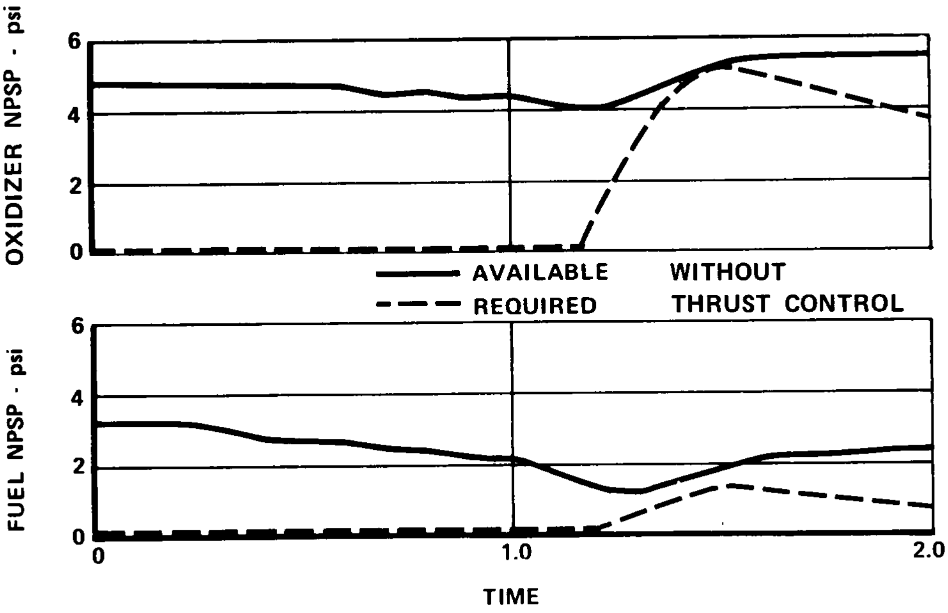


Figure B-14. Suction Characteristics During Pumped Idle to Full Thrust Transient Category II Engine FD 71318

The transient simulation was repeated with an active RL10 type thrust control. With the exception of the thrust control, all control functions were similar to those used on the first transient. This transient met all of the desired criteria. With the thrust control installed the overshoot was eliminated and the engine transient NPSP requirements were reduced to essentially the same levels that would be required for steady-state operation. It was concluded that this transient would be acceptable for the Category II engine. The thrust, mixture ratio and turbopump speed characteristics for this transient are shown in figure B-15. Also, the available and required pump NPSP levels are shown in figure B-16.

2.3 Conclusions

The following conclusions were reached as a result of the Critical Elements Evaluation of engine transients.

2.3.1 Tank Head Idle to Pumped Idle

a. The engine should provide adequate breakaway torque with the anticipated inlet pressure of 16 psia

b. Satisfactory transient characteristics to pumped idle can be obtained with a simple open loop control system.

c. The vapor volume pumping capabilities of the pumps are considerably greater than the predicted propellant vapor volumes during the transient.

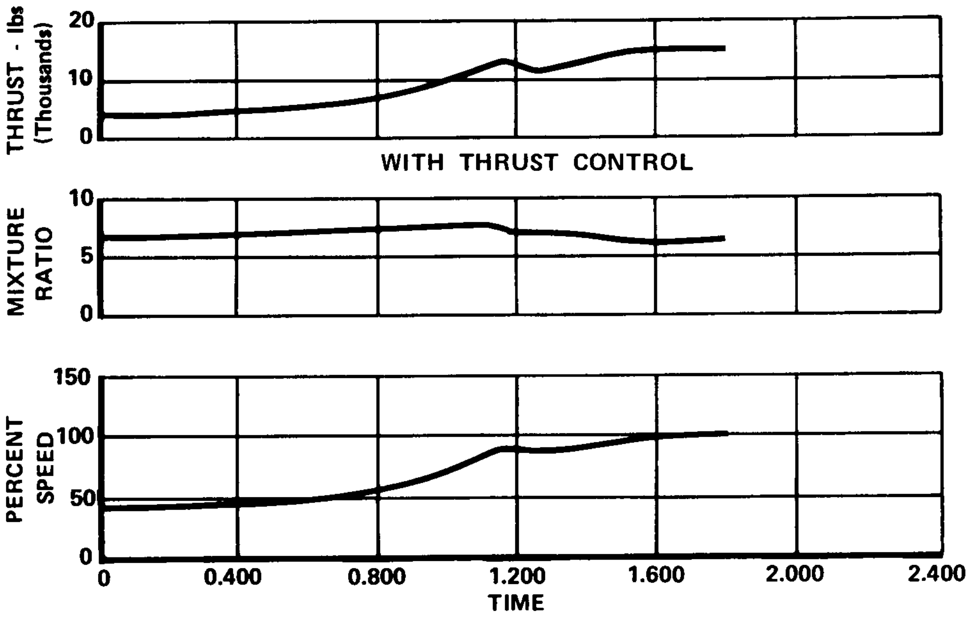


Figure B-15. Thrust, Mixture Ratio and Speed During Pumped Idle to Full Thrust Transient Category II Engine FD 72903

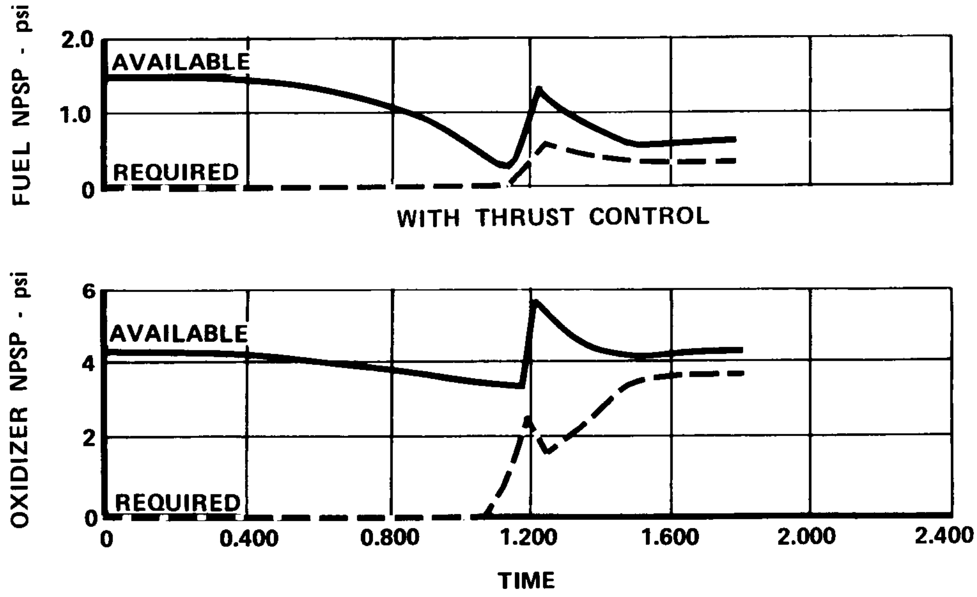


Figure B-16. Suction Characteristics During Pumped Idle to Full Thrust Transient Category II Engine FD 73103

2.3.2 Pumped Idle To Full Thrust

a. An acceptable transient can be obtained using a combination of open and closed loop controls

b. The engine can be accelerated with NPSP levels no higher than those required for steady-state operation if engine overshoot is kept to a minimum.

3. GASEOUS OXIDIZER HEAT EXCHANGER

The RL10 derivative engines may be required to supply gaseous oxygen flow for Space Tug vehicle tank pressurization during full thrust operation and for prepressurization of the tanks during pumped idle mode. Furthermore, tank head idle operation may be improved if the injected oxygen is gasified. Engine system analysis completed during the previous study (Reference 1) determined the level of heat transfer necessary to provide gaseous oxygen for both tank pressurization and tank head idle mode of operation, and established the use of hydrogen gas as the heat transfer medium. The heat transfer was accomplished in a heat exchanger located in the turbine exit line as shown in figure B-17.

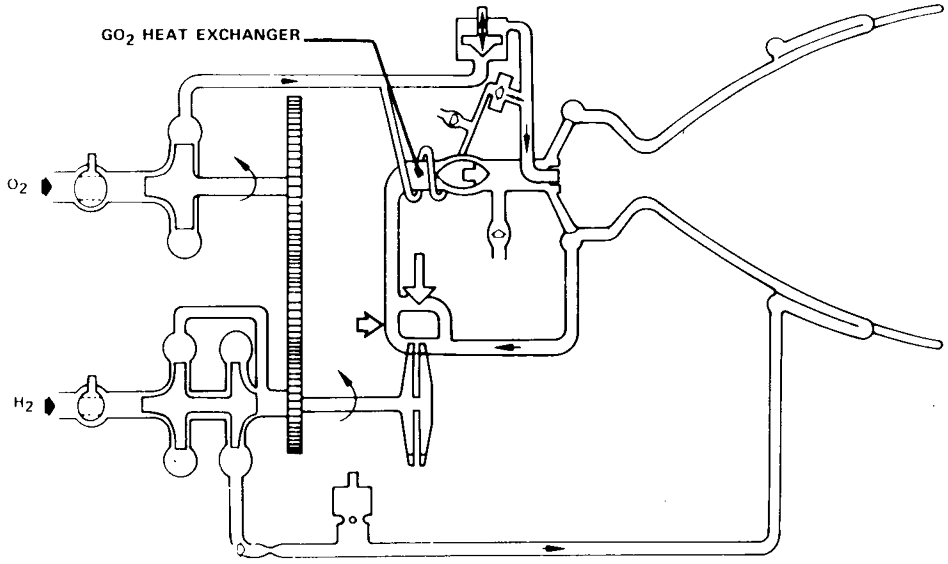


Figure B-17. GO_2 Heat Exchanger in Turbine Discharge FD 72586B

This Critical Element Evaluation was undertaken to verify the feasibility of an engine-mounted heat exchanger to provide the required gaseous oxygen for the various engine operating modes. The areas of concern included heat pickup, packaging, safety, weight, and fluid pressure losses.

The GO_2 heat exchanger study was performed on the Category II engine because, of the engine candidates, it possessed the lowest energy level of hydrogen for oxygen vaporization.

3.1 Heat Exchanger Requirements

The candidate heat exchanger configurations evaluated during this feasibility study were chosen to satisfy the heat pick up (ΔT) requirements during tank head idle mode operation and the maximum pressure loss (ΔP) requirement during full thrust operation. Tank head idle operation established the heat transfer requirement because the ratio of fuel flow to oxidizer flowrates is the lowest at this operating condition.

Full thrust operation established the pressure loss requirement because the adverse effect of turbine back pressure is most critical at this condition.

Heat exchanger operating requirements for Tank Head Idle (THI) and full thrust operation were defined as the following:

a. Fuel Inlet Conditions in THI

Fuel flowrate = 0.090 lb_m/sec
Fuel temperature = 700°R
Fuel pressure = 7.5 psia

b. Oxygen Inlet Conditions in THI

Oxidizer flowrate = 0.480 lb_m/sec
Oxidizer temperature = 168°R
Oxidizer pressure = 21.5 psia

c. Oxygen Exit Conditions in THI

Oxidizer temperature = 225°R
Oxidizer pressure = 16.2 psia

d. Fuel Conditions at Full Thrust

Fuel flowrate = 4.88 lb_m/sec
Fuel temperature 420°R
Fuel pressure = 490 psia
Fuel pressure loss approximately 18 psia

The basic heat exchanger design parameters listed in table B-5 were established based upon the required operating conditions listed above.

Table B-5. GO_2 Heat Exchanger Parameters For THI Operation

Parameter	Fuel	Oxidizer
Flowrate, lb_m/sec	0.090	0.48
Fluid ΔT, °R	155.	57.
Flow Area, in^2	4.5	1.0
Fluid ΔP, psia	0.50	4.5
Heat Transfer Area = 700 in^2		

The fuel and oxidizer flow areas were sized to meet the heat exchanger pressure loss requirements at full thrust and tank head idle respectively. The resulting fuel and oxidizer convective film coefficients defined the heat exchanger overall thermal resistance and the heat transfer area required to meet the heat pickup (fluid ΔT) requirements. These fuel and oxidizer flow areas, and the heat transfer area thus defined became the common denominator for the heat exchanger configuration studies that follow.

3.2 Concept Selection

Configuration studies were conducted to define the smallest practical heat exchanger that could meet the design requirements. The various heat exchanger configurations studied are illustrated in figure B-18. The configuration on the left is a simple concentric cylinder heat exchanger that required 120 in. of length to produce the needed 700 in^2 of heat transfer area. By placing a plug in the fuel flowpath, as illustrated by the center configuration in figure B-18, the heat transfer area per unit length was significantly increased, making possible the length reduction to 71 in. A further increase in heat transfer area was realized when the oxidizer was passed through tubes suspended in the fuel annulus, the tubes making ideal containers for the high pressure oxidizer and exposing large surface areas to the fuel for heat transfer. This configuration, also illustrated in figure B-18, was subsequently selected for further analytical and mechanical evaluation, as its 30-in. length was considered the shortest practical heat exchanger length consistent with the imposed design requirements.

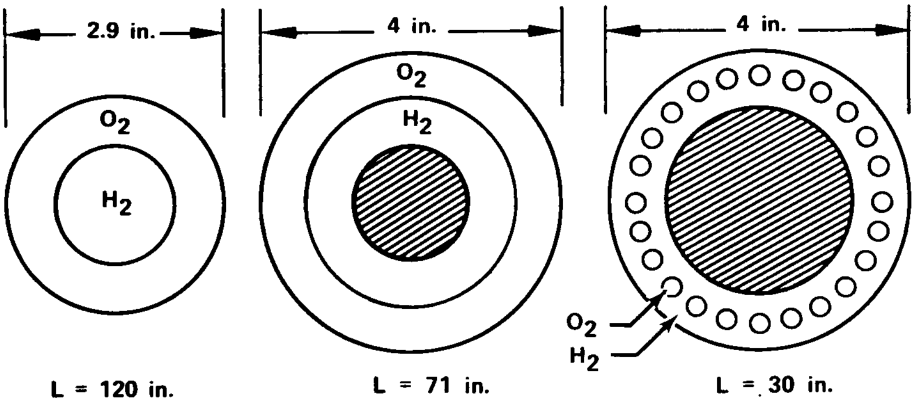


Figure B-18. GO_2 Heat Exchanger Design Study FD 71309

3.3 Heat Exchanger Performance

Performance predictions were made for the selected heat exchanger configuration to ensure that acceptable performance could be expected at all engine operating modes. The resulting heat exchanger operating characteristics were acceptable and are summarized in table B-6.

Table B-6. Heat Exchanger Performance Characteristics

Parameter	Tank Head Idle	Pumped Idle	Full Thrust
Oxidizer flowrate, lb_m/sec	0.48	1.00	0.50
Fuel flowrate, lb_m/sec	0.09	0.58	4.88
Fuel inlet temperature, °R	700.	570.	420.
Oxidizer discharge temperature, °R	225.	250.	290.
Fuel pressure drop, psia	0.5	4.0	24.0
Oxidizer pressure drop, psia	4.6	6.5	0.4

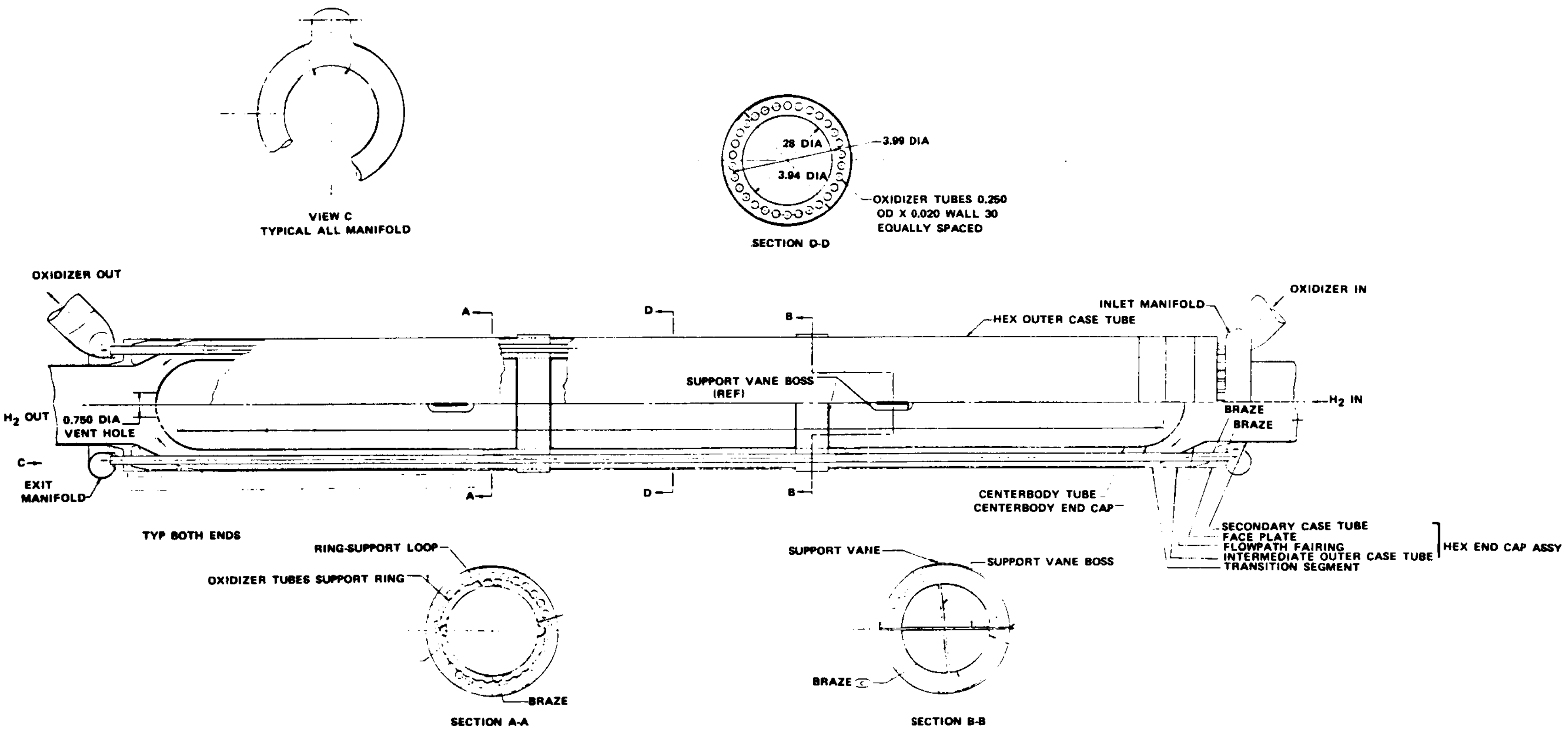
3.4 Mechanical Design Studies

Manufacture, structure and installation were studied to confirm the mechanical feasibility of the design. Figure B-19 depicts the oxidizer heat exchanger design analyzed for structural and manufacturing feasibility.

The fuel annulus is formed by concentric tubes - a center body tube and an outer case tube. The center body tube is supported inside the outer case tube by two sets of support vanes, each set consisting of two vanes mounted at approximately 90 degrees to one another and located at different axial positions. The vanes are positioned such that no interference occurs with the oxidizer tubes in the fuel annulus. The center body tube contains welded support vane bosses which encircle the vane and thus provide support when the two surfaces (vane and boss) are brazed together. The vanes are, in turn, welded to the outer case tube.

Thirty oxidizer tubes are equally spaced around the fuel annulus and are supported by the two faceplates and the two support rings. The support rings are convoluted sheet metal rings supported from the center body tube by four equally spaced support loops. The convolutions form "saddles" into which the oxidizer tubes are brazed. At the heat exchanger ends, the oxidizer tubes pass through and are brazed into faceplates. Inlet and exit manifolds are then brazed to the exposed tube ends. The brazed joints exposed to fuel and oxidizer on the inside are open to the surrounding environment on the outside, thus averting a mixing hazard in event of a leak. The outer case tube is joined to the fuel turbine discharge tube by an end cap assembly which is butt welded to the outer case tube.

The material selected for the heat exchanger is 347 SST. This material was selected for its compatibility with hydrogen and oxygen, high strength, and good brazing and welding characteristics. The nature of the heat exchanger assembly prohibits inspection and braze repair of the internal braze joints after the final furnace braze. This difficulty is alleviated by relaxation of the braze coverage specification allowable (80% coverage to 40% coverage) required on the internal brazes. More than sufficient braze area is provided to resist the light loading experienced in these areas. The remaining brazes must meet full specification requirements however, because of the sealing requirements at these joints. The weight and volume of the heat exchanger was determined to be 15.5 lb and 440 cu in.


VIEW C
TYPICAL ALL MANIFOLD
28 DIA
3.94 DIA
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OXIDIZER TUBES 0.250
OD X 0.020 WALL 30
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SECTION D-D
OXIDIZER OUT
A
D
B
INLET MANIFOLD
OXIDIZER IN
HEX OUTER CASE TUBE
SUPPORT VANE BOSS
(REF)
H_2 IN
BRAZE
BRAZE
H_2 OUT
0.750 DIA
VENT HOLE
C
EXIT
MANIFOLD
CENTERBODY TUBE
CENTERBODY END CAP
SECONDARY CASE TUBE
FACE PLATE
FLOWPATH FAIRING
INTERMEDIATE OUTER CASE TUBE
TRANSITION SEGMENT
HEX END CAP ASSY
TYP BOTH ENDS
RING-SUPPORT LOOP
OXIDIZER TUBES SUPPORT RING
BRAZE
SECTION A-A
SUPPORT VANE
SUPPORT VANE BOSS
BRAZE
SECTION B-B

Figure B-19. Straight Oxidizer Heat Exchanger

FD 75868A

3.5 Installation Study

The straight heat exchanger previously defined in figure B-19 could not be integrated into the engine overall packaging envelope without exceeding the engine minimum powerhead diameter due to its 30 in. length. Various plumbing schemes were studied in an attempt to locate it where it could not impact the overall powerhead diameter. It became apparent that the heat exchanger would need to be curved in a manner that could wrap it close to the combustion chamber. The resultant curved heat exchanger configuration, shown in figure B-20, was selected. No attempt was made to structurally analyze the curved 30-in. heat exchanger because the complexity of the analysis placed it beyond the scope of this preliminary design. It was determined, however, that the curved heat exchanger could be manufactured by using the basic assembly techniques defined for the straight unit.

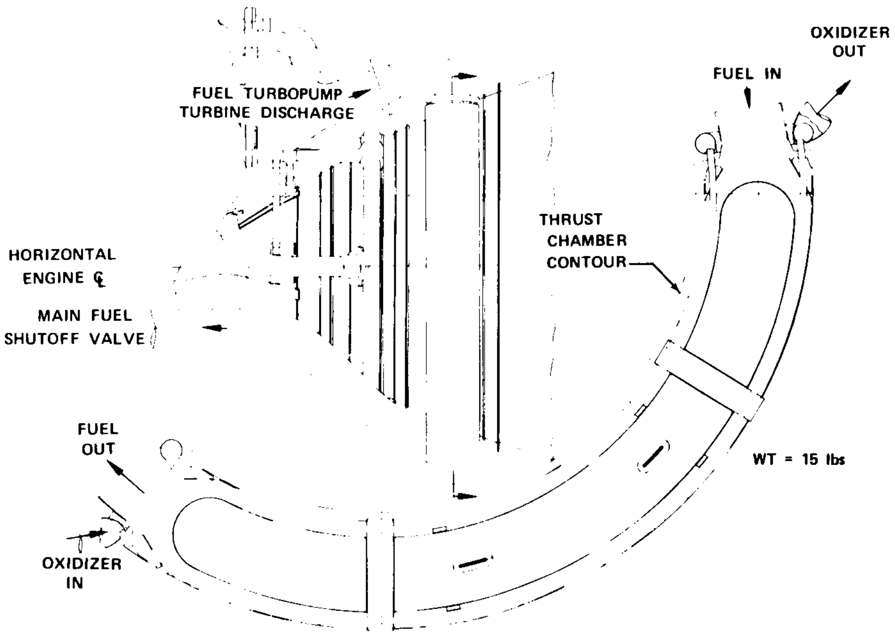


Figure B-20. GO$_2$ Heat Exchanger Concept Category II FD 71306

3.6 Conclusions

The feasibility of locating a GO$_2$ heat exchanger in the fuel turbine discharge line that would produce acceptable performance at all engine operating modes was confirmed. Furthermore a heat exchanger configuration of acceptable weight and length was defined. During the Baseline Engine Design effort, which followed this Critical Elements Evaluation, the GO$_2$ heat exchanger design configuration and location were changed to increase the amount of GO$_2$ available for tank pressurization.

4. TWO-PHASE PROPELLANT PUMPING

Significant advantages may be derived from the ability to pump two-phase propellants in rocket engine applications. Among the benefits that have been noted in past studies are the elimination of the need for tank pressurization gases and the operation at reduced ullage pressures. A design analysis of two-phase pumping capability for use in an RL10 derived engine for the Space Tug application was conducted to determine feasible cycle objectives, and to define a preliminary pump configuration and drive system capable of meeting those objectives.

The pumping of two-phase flow in turbopumps is not an entirely new concept. Nearly all recent low-NPSH level rocket engines have been designed to function with some combination of liquid and vapor within their pumping elements. Numerous tests have indicated that the ingestion of some amount of two-phase flow from the intake line is feasible as long as the total vapor content within the pumping elements is limited. On the other hand, excessive vapor content within the pumping elements can result in complete head breakdown even if little or no vapor exists within the intake line. Properly applied design techniques can avoid head breakdown.

4.1 Hydrodynamic Design Philosophy

The existence of a two-phase flow condition at the engine inlet requires special application of the basic hydrodynamic design parameters. To provide rated head rise with two-phase propellants at the pump inlet, the parameters which determine the amount of vapor generated within the inducer blading itself must be controlled, since the pump must accommodate the self-generated vapor in addition to that vapor ingested through the intake line. In other words, the quantity of vapor generated within the pump significantly influences its ability to handle the additional vapor content from the inlet line without head falloff.

Past studies have shown that an important parameter that indicates the degree of vapor formation within the pump, and the resultant potential pressure falloff is given by the cavitation number, thus:

$$k = \frac{H_{static} - H'_{vapor}}{W_1^2/2g} = \frac{\gamma - \phi^2}{\phi^2 + 1} \qquad \text{(Equation 1)}$$

where k = cavitation number

H_{static} = Average static head upstream of inducer blading, ft.
H'_{vapor} = Minimum cavity vapor head within inducer blading, ft.
W_1 = Inlet relative velocity of fluid, fps
ϕ = Inlet tip flow coefficient
U_{1t} = Inducer inlet tip speed, fps
$\gamma = \frac{(NPSH + TSH)2g}{U_{1t}}$
NPSH = Net positive suction head, ft.
TSH = Thermodynamic suppression head, ft.

The level of static pressure relative to saturation pressure that exists within the inducer blading can be shown to be a function of (1) the static head upstream of the blading, (2) the losses incurred in directing the flow through the blading due to skin friction and fluid incidence, and (3) the acceleration of the fluid due to blade, boundary layer, and vapor cavity blockages. It can be seen, therefore, that the level of cavitation number is an indication of the level of static pressure relative to the vapor pressure within the blade as a result of the above influences, and the resultant amount of vapor formation. The application of a given combination of NPSH, TSH, and tip speed between two geometrically similar inducers operating at a given flow coefficient implies the existence of similar vapor fractions within their blading, and thus similar performance for a given additional vapor fraction ingested at the inlet. This result allows the use of test data from previous designs in new applications, such as the Space Tug, on the basis of hydrodynamic similarity.

Typical demonstrated head falloff characteristics with varying cavitation numbers are shown in figure B-21 for several existing pump designs. The percent of head falloff in a pump for a given degree of cavitation within the inducer section, as reflected by the cavitation number, can be shown to be dependent upon the specific speed of the overall pump, and the type of pump geometry that is used. A given head falloff within the inducer itself, for example, becomes less discernible percentagewise in terms of the overall head falloff for a reduced specific speed design. A sizeable falloff in head rise within the inducer portion of the pump may reflect a minute loss in head rise of the overall inducer-impeller combination up to the point of complete head breakdown of the pump. The overall head falloff characteristic is dependent upon the suction capability of the blading following the inducer and the amount of head margin incorporated within the inducer section. All of the above factors determine whether the characteristic degradation in head is gradual with decreasing cavitation number, or pronounced with a sharp head falloff knee. These same results apply to a pump ingesting various degrees of two-phase flow through the inlet line, as will be shown later, and must be accounted for in the design.

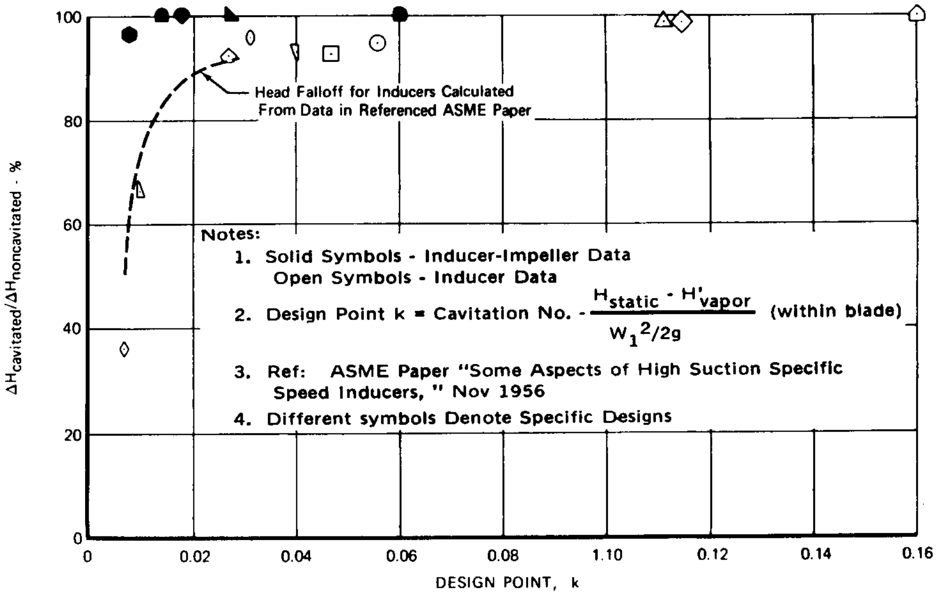


Figure B-21. Non-Two-Phase Head Falloff FD 61288 A

The maximum vapor/liquid capability of an inducer, for a given liquid flow requirement, can be shown to be basically a function of (1) the flow coefficient of the vapor/liquid mixture at which choking incidence occurs for a given inlet blade angle, (2) blade, boundary layer and vapor cavity blockages, which accelerate the flow into the blade throat, and (3) the value of critical relative area ratio through which the mixture passes at the point of choking between the inlet line and the blade throat.

In order to maintain an acceptable angle of attack of the two-phase mixture relative to the inlet blade angle, to avert choking of the mixture, it is necessary that the flow coefficient of the mixture be limited to the maximum design value for non-two-phase operation. That is,

$$\phi_{\text{mixture}} = \phi_{\text{liquid}} + \phi_{\text{vapor}} = \phi_{\text{design}}$$

At a given value of liquid flow coefficient, the maximum vapor ingestion attainable can then be estimated as follows:

$$\alpha = \text{Vapor fraction} = \frac{\text{Vv}}{\text{Vm}} \frac{(\text{Volume vapor})}{(\text{Volume of mixture})}$$

$$= \frac{\phi\text{v}}{\phi\text{m}}$$

$$= \frac{\phi\text{m} - \phi\text{L}}{\phi\text{m}} \qquad \text{(Equation 2)}$$

Denoting the critical flow coefficient of the mixture (ϕmc) at the maximum vapor fraction (α_c),

$$\alpha_c = \frac{\phi\text{mc} - \phi\text{L}}{\phi\text{mc}} \qquad \text{(Equation 3)}$$

The critical flow coefficient of the mixture can also be written,

$$\phi\text{mc} = \tan\beta_c' \approx \beta_c' = \beta^* - i_c = \beta^*\left(1 - \frac{i_c}{\beta^*}\right) \qquad \text{(Equation 4)}$$

where

β_c' = Inlet fluid angle of mixture

β^* = Blade angle

i_c = Angle of attack of mixture

Referring to figure B-22, the relationship between blade, boundary layer and vapor cavity blockages and the critical relative area ratio can be expressed as follows:

$$\frac{\tau_2}{\tau_1}\,\frac{A_1}{A_2} = \frac{\sin\beta_c'}{\sin\beta_2'} \approx \frac{\beta_c'}{\beta^*} = \frac{\beta^* - i_c}{\beta^*} = 1 - \frac{i_c}{\beta^*} \qquad \text{(Equation 5)}$$

Also

$$\tau_2 = \tau_1 (1-B^2)$$

where B = Total blockage of fluid

$$= B_{\text{blade}} + B_{\text{boundary layer}} + B_{\text{vapor cavity}}$$

Substitution of Equation 5 into Equation 4, and denoting $A_2 = A^*$ at the condition where choking occurs,

$$\alpha_c = \left[1 - \frac{\phi_L A^*}{\beta^*(1-B)A_1}\right] = \left[1 - \frac{\phi'_L}{\beta^*(A_1/A^*)}\right] \qquad \text{(Equation 6)}$$

where

ϕ_L = Liquid flow coefficient prior to blading (point 1)

ϕ'_L = Liquid flow coefficient within blading (point 2)

The value of $A1/A^*$ at which choking occurs can be shown to be a function of the inlet relative Mach number, the vapor volume fraction through the blading, and the losses incurred in the process. Typical values of $A1/A^*$ required for choking are on the order of 1.0.

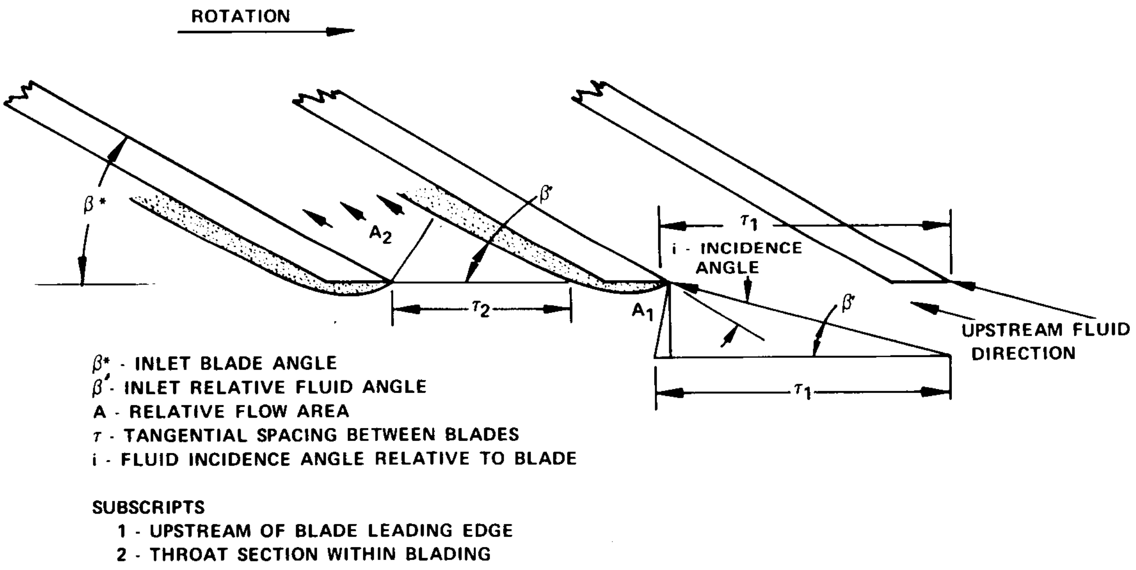

ROTATION
β*
A₂
β'
τ₁
i - INCIDENCE ANGLE
τ₂
A₁
β'
UPSTREAM FLUID DIRECTION
τ₁
β* - INLET BLADE ANGLE
β' - INLET RELATIVE FLUID ANGLE
A - RELATIVE FLOW AREA
τ - TANGENTIAL SPACING BETWEEN BLADES
i - FLUID INCIDENCE ANGLE RELATIVE TO BLADE
SUBSCRIPTS
1 - UPSTREAM OF BLADE LEADING EDGE
2 - THROAT SECTION WITHIN BLADING

Figure B-22. Inlet Incidence Angle Affects Flow Area

FD 75863

Examination of the derived equations for two-phase pumping capability indicates that increasing vapor fractions are theoretically attainable with decreasing liquid flow coefficients. Practical limits on the minimum liquid flow coefficient that may be used, however, do exist and must be evaluated in the design.

As the pump is subjected to increasing amounts of vapor, and decreasing amounts of liquid, a point is reached where the decreasing density of the mixture begins to result in a significant falloff in pressure rise from the pump. Proper design control, to avoid impaired performance as a result of the above, can be obtained by specifying a maximum liquid incidence to blade angle ratio (i/β_{it}^*) within demonstrated limits. Maximum values of approximately 0.7 are recommended in Reference 2 based upon available test data.

Attainment of the maximum potential vapor fraction capability, as given by the above relationships, can also be shown to be dependent upon the effective Mach number of the vapor/liquid mixture within the intake line. Mach numbers must be restricted within acceptable limits to assure that no choking exists prior to the inducer, which could prevent achieving the maximum potential capability. This factor is of particular importance since the acoustic velocity of the two-phase mixture can be appreciably lower than that of either the liquid or vapor phases alone.

Typical head-falloff characteristics from existing pump designs operating with two-phase flow are shown in figures B-23 through B-25. As previously noted, where the inducer is followed by a high head rise, low specific speed impeller, a sharp head falloff knee is exhibited at the point of maximum two-phase capability. On the other hand where the inducer is operating alone, a gradual head falloff is exhibited to the point of maximum capability.

In order to assess the accuracy of the derived vapor-fraction equations for the current Space Tug study, an attempt was made to predict the performance of the designs tested. The resultant comparison or predicted versus test values is given in figure B-26. These results indicated that reasonable levels of accuracy can be achieved with the two-phase flow equations derived above. These equations were subsequently applied to a study of two-phase pumping for the Space Tug application.

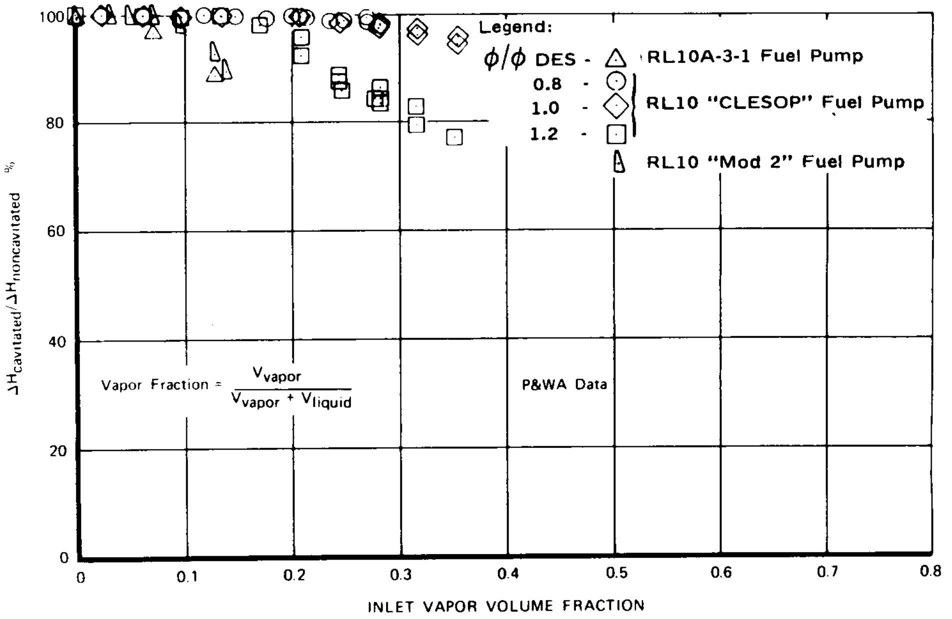


Figure B-23. Two-Phase Pumping Capability - LH_2 (Inducer-Impeller Data) FD 61289 B

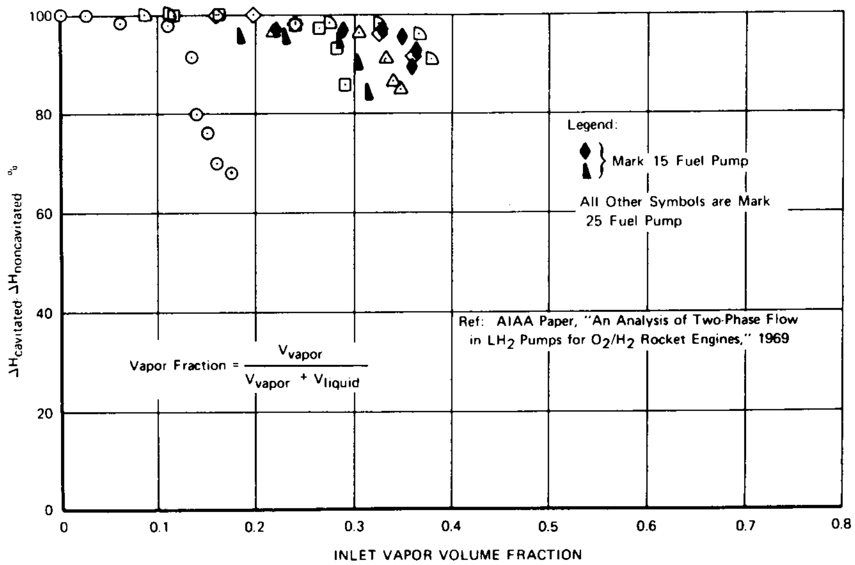


Figure B-24. Two-Phase Pumping Capability - LH_2 (Inducer-Impeller Data) FD 61290A

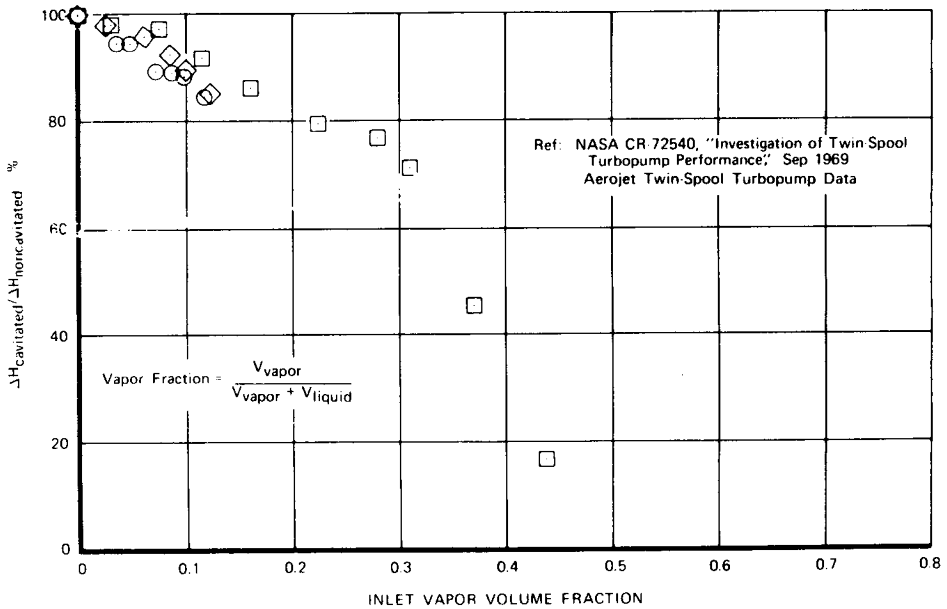


Figure B-25. Two-Phase Pumping Capability - LH_2 (Inducer Data) FD 61291A

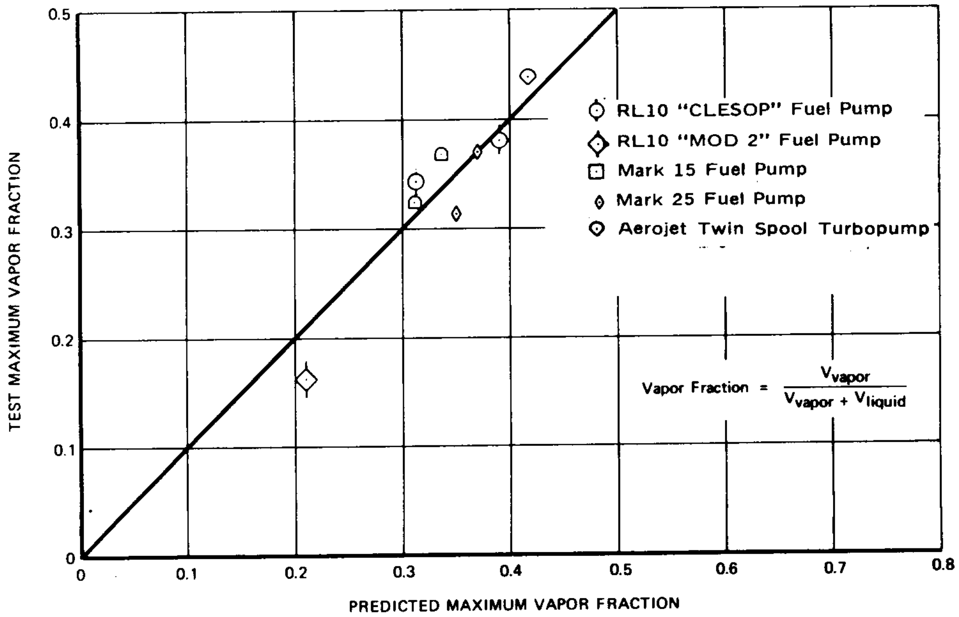


Figure B-26. Maximum Vapor Fraction Pumping Capability (Predicted vs Test) FD 61292A

4.2 Two-Phase Design for Category II Engine

A study was conducted to determine the feasibility of providing two-phase pumping capability using minimum modifications to existing RL10 hardware. The pertinent variables which were investigated in the study were (1) inducer diameter, (2) α - vapor fraction capability and requirement, (3) k - cavitation number, (4) inlet line Mach number, and (5) i/β_{1t}^* - incidence to blade angle ratio.

Results of the study of main fuel pump inducer configurations for the RL10 engine pumps, based on the derived two-phase flow equations, is given in figures B-27 and B-28. Inlet duct choking did not appear to be restrictive within the usable range of inducer diameters where predicted vapor pumping capability was greater than required (i.e., the level predicted to exist in the Space Tug inlet lines). Selection of the smallest inducer diameter that would yield a comfortable margin in cavitation number and calculated two-phase capability was considered desirable from three standpoints: (1) minimum size and weight of the inlet ducting and of the inducer itself, (2) minimized incidence/blade angle ratio at worst conditions (pumped idle) which could result in impaired performance, and (3) minimum degradation in efficiency and head coefficient of the overall pump system resulting from the use of large inducer diameters.

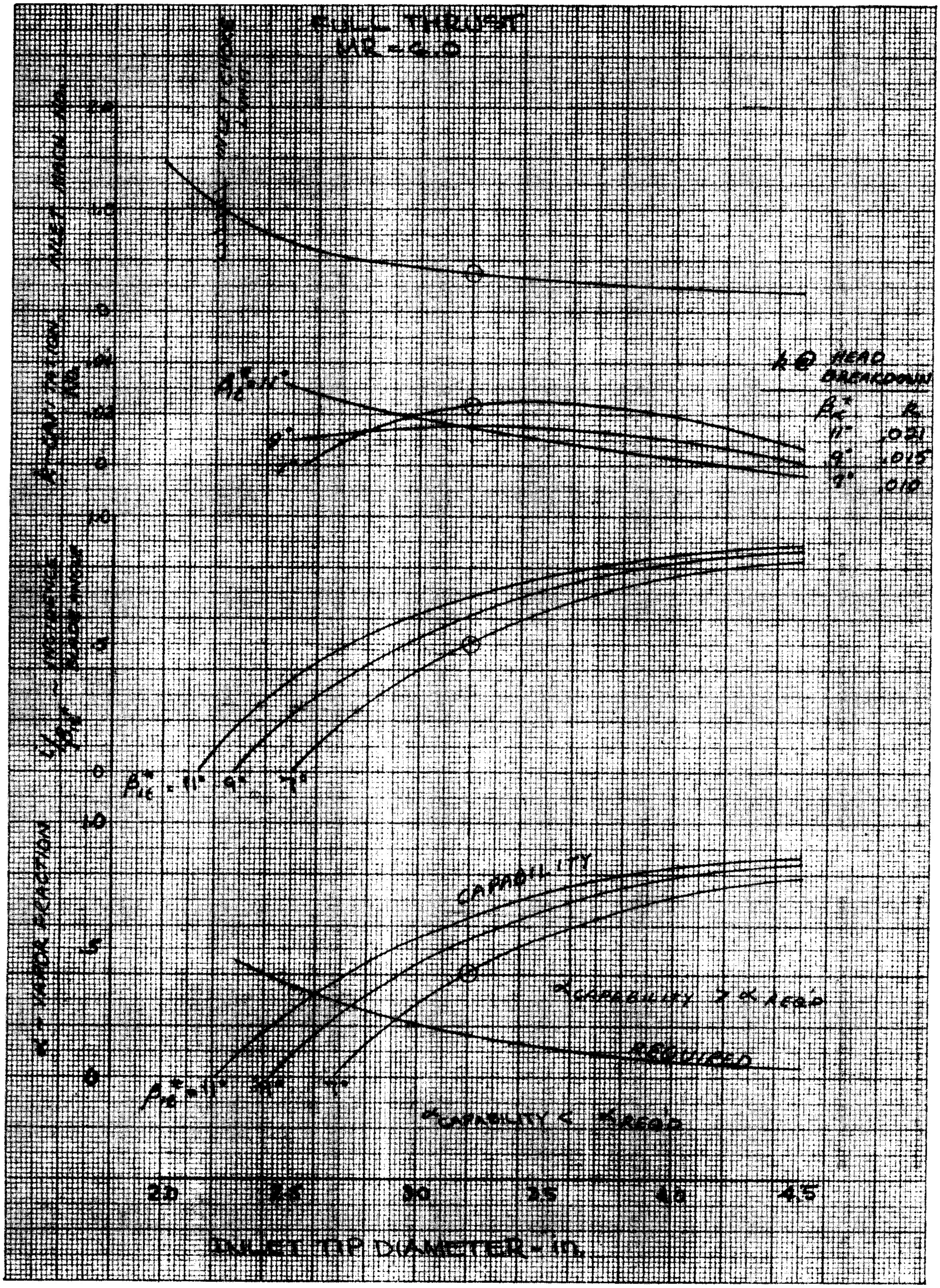


Figure B-27. Effect of Fuel Pump Inlet Diameter and Blade Angle on Suction Characteristics

DF 98264

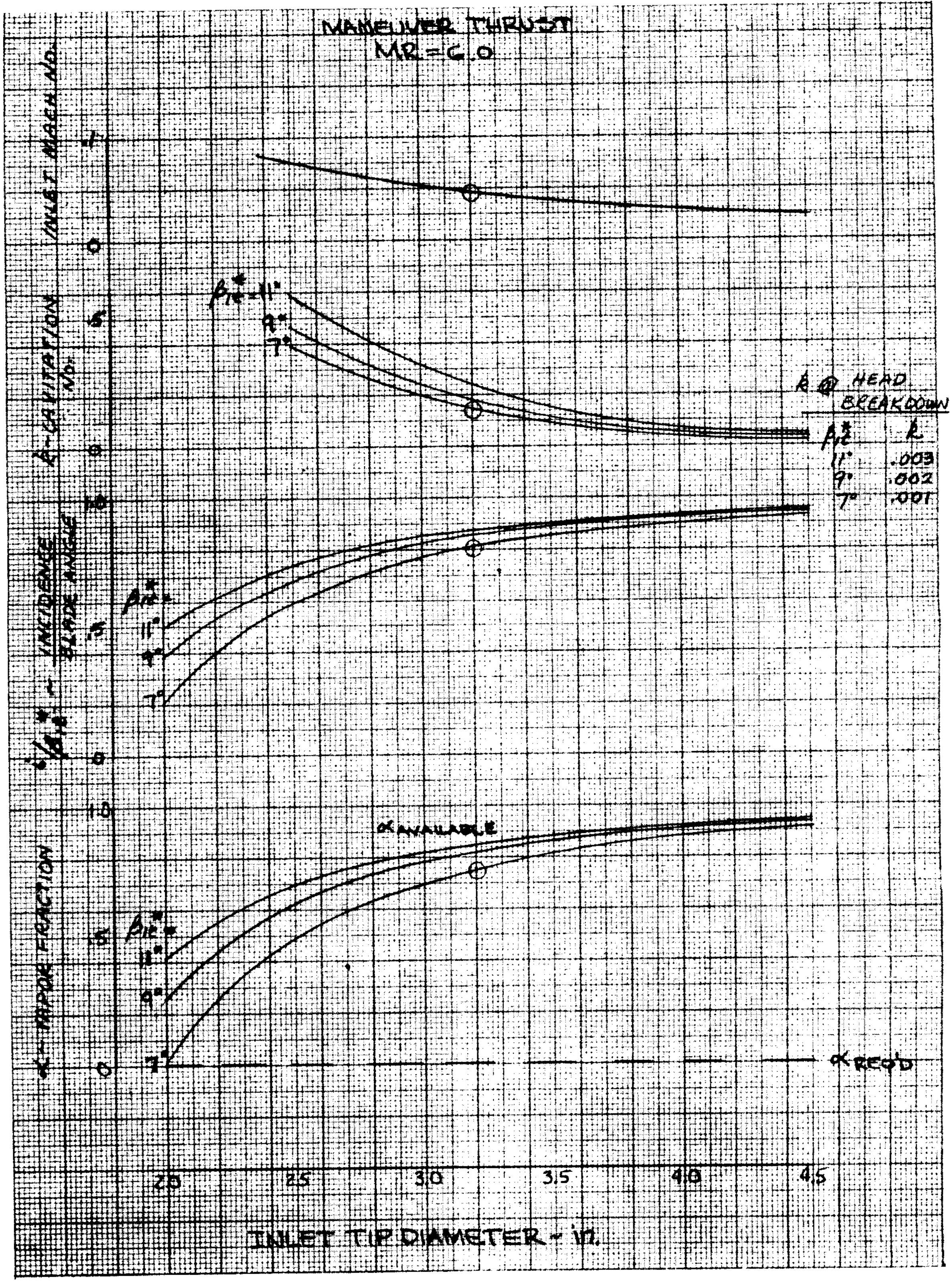


Figure B-28. Effect of Fuel Pump Inducer and Blade Angle Diameter on Suction Characteristics

DF 98265

As noted in the foregoing discussion, the cavitation number of a pump is an important parameter which determines the attainable two-phase pumping capability. The value of cavitation number at which complete head breakdown of a given pump occurs is dependent upon the maximum suction specific speed of the pump and the flow coefficient at which it occurs. Values of cavitation number at head breakdown conditions were defined as a function of inlet blade angle from available test data to provide an indication of the margin available for any given design in the parametric study. These values are shown in figures B-27 and B-28. The greatest margin in cavitation number above head breakdown conditions was provided by a low inlet tip blade angle, on the order of seven degrees for the fuel pump. P&WA experience with two-phase liquid hydrogen inducers on the RL10 "Clesop" and "Mod 2" designs, both of which incorporated 7-degree blade angles, indicated performance levels comparable to predicted values. An inlet diameter of approximately 3.2 in. (equal to that of the successful Mod 2 configuration) yielded approximately the highest cavitation number margin while providing a comfortable margin above required values in two-phase capability.

Liquid incidence to blade angle ratio for the above design is 0.5 at design point conditions depicted in figure B-27, and 0.75 at pumped idle operation shown in figure B-28. These values are considered acceptable in accordance with the demonstrated values of the RL10 Mod 2 turbopump, particularly in view of the nearly insignificant vapor requirements at idle operation.

A similar study was conducted for various oxidizer inducer configurations. The study was initially directed toward determining the feasibility of two-phase pumping with no low speed inducer. The study indicated that two-phase pumping at main oxidizer pump speeds was not feasible, because the required cavitation numbers were lower than previously demonstrated values at head breakdown conditions. An analysis of low speed inducers was therefore conducted with speed introduced as an additional variable.

Selection of an optimum speed was made primarily based upon the acceptability of cavitation number. A study of the head falloff characteristics of several inducer designs as a function of cavitation number indicated that a conservative value on the order of 0.06 would be desirable to avoid excessive vapor formation and attendant head falloff within the blade passages prior to two-phase ingestion. This cavitation number was found to occur at an inducer speed of approximately 3000 rpm, as presented in figures B-29 through B-32, and was selected for the design. Speeds greater than 3000 rpm indicated cavitation numbers lower than the minimum desirable value of 0.06. Selection of a design speed lower than 3000 rpm would have provided a conservative design but large in diameter.

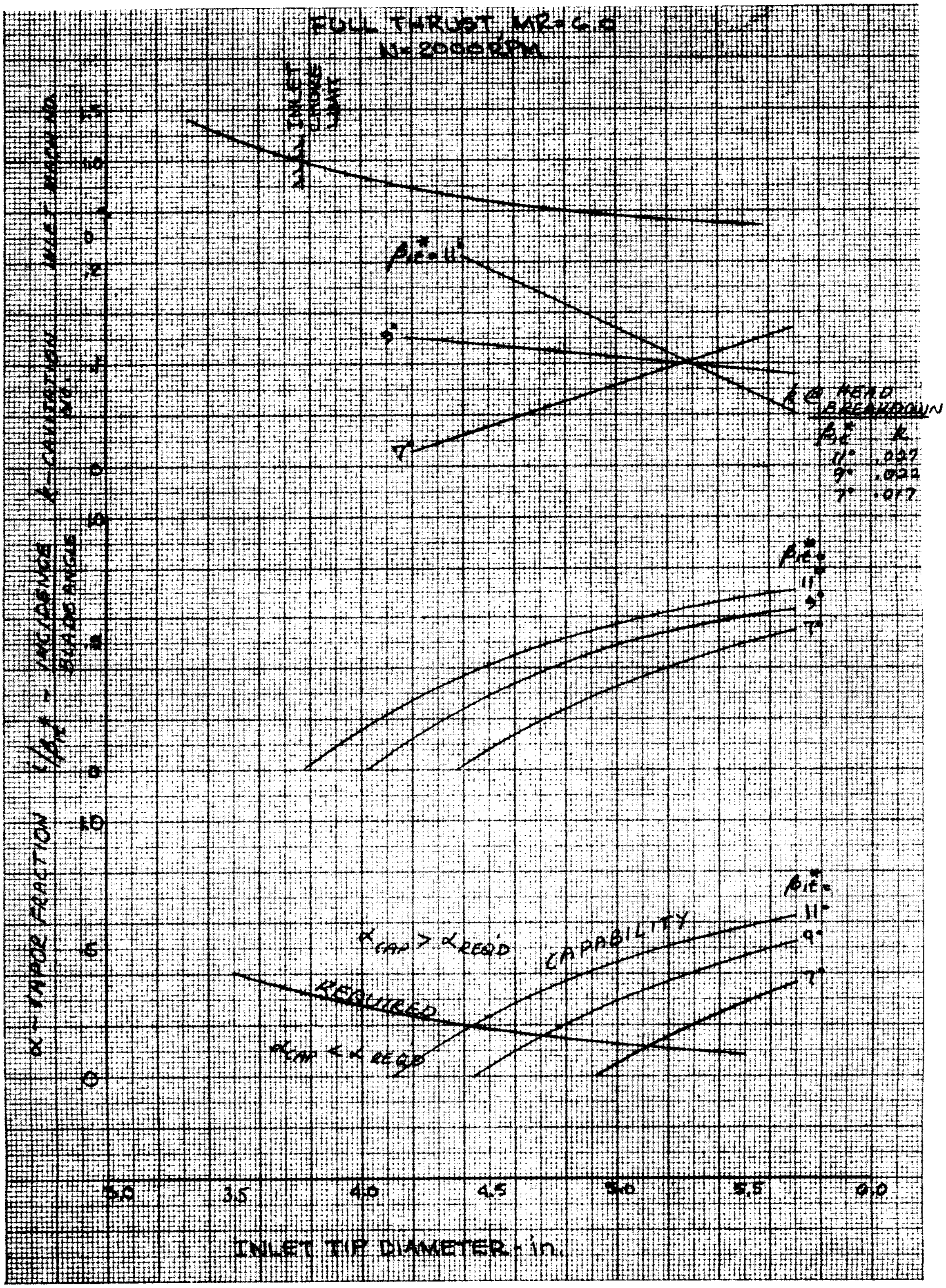


Figure B-29. Effect of Oxidizer Low Speed Inducer Diameter and Blade Angle on Suction Characteristics N = 2000 rpm

DF 98266

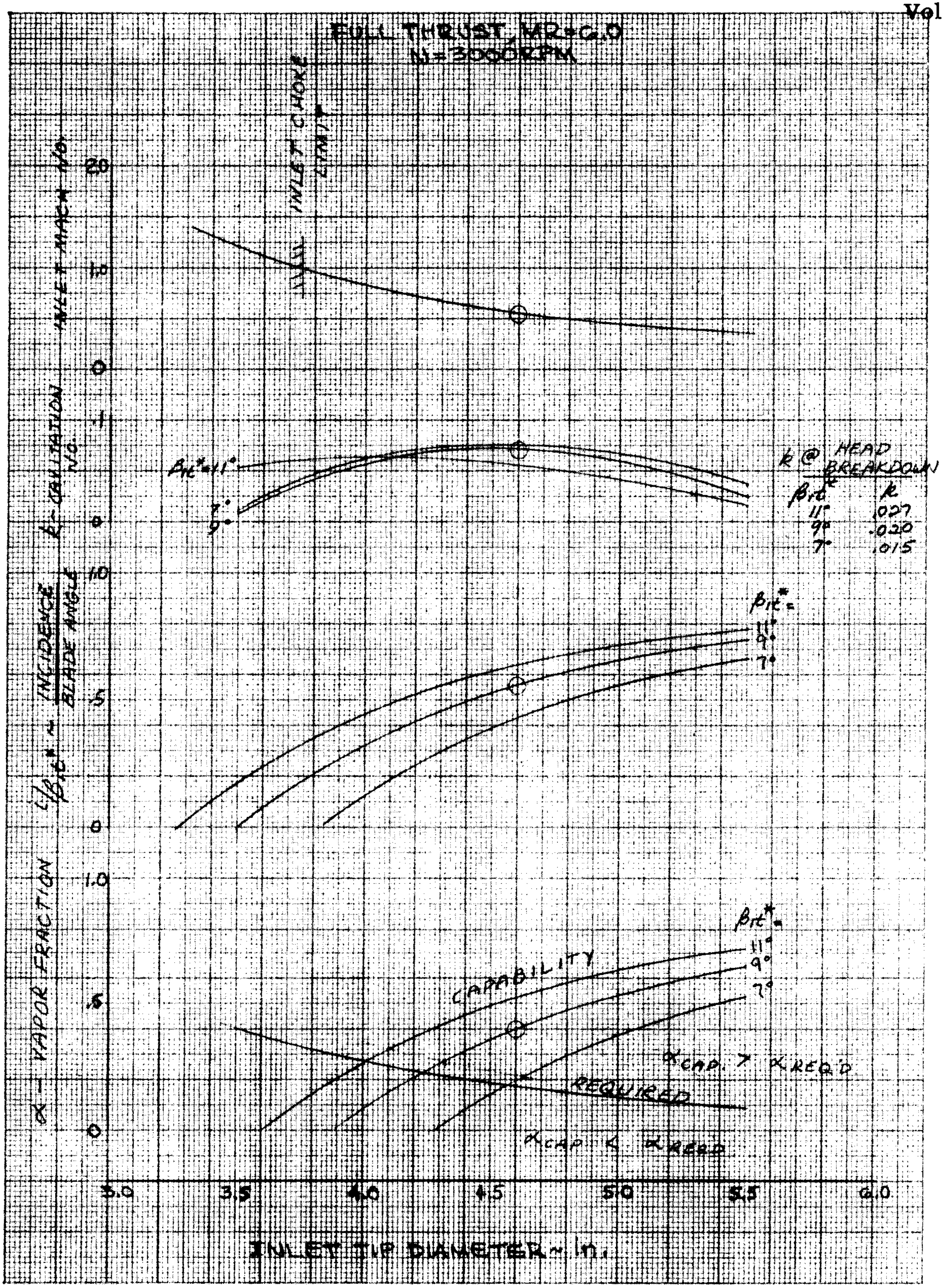


Figure B-30. Effect of Oxidizer Low Speed Inducer Diameter and Blade Angle on Suction Characteristics N = 3000 rpm

DF 98267

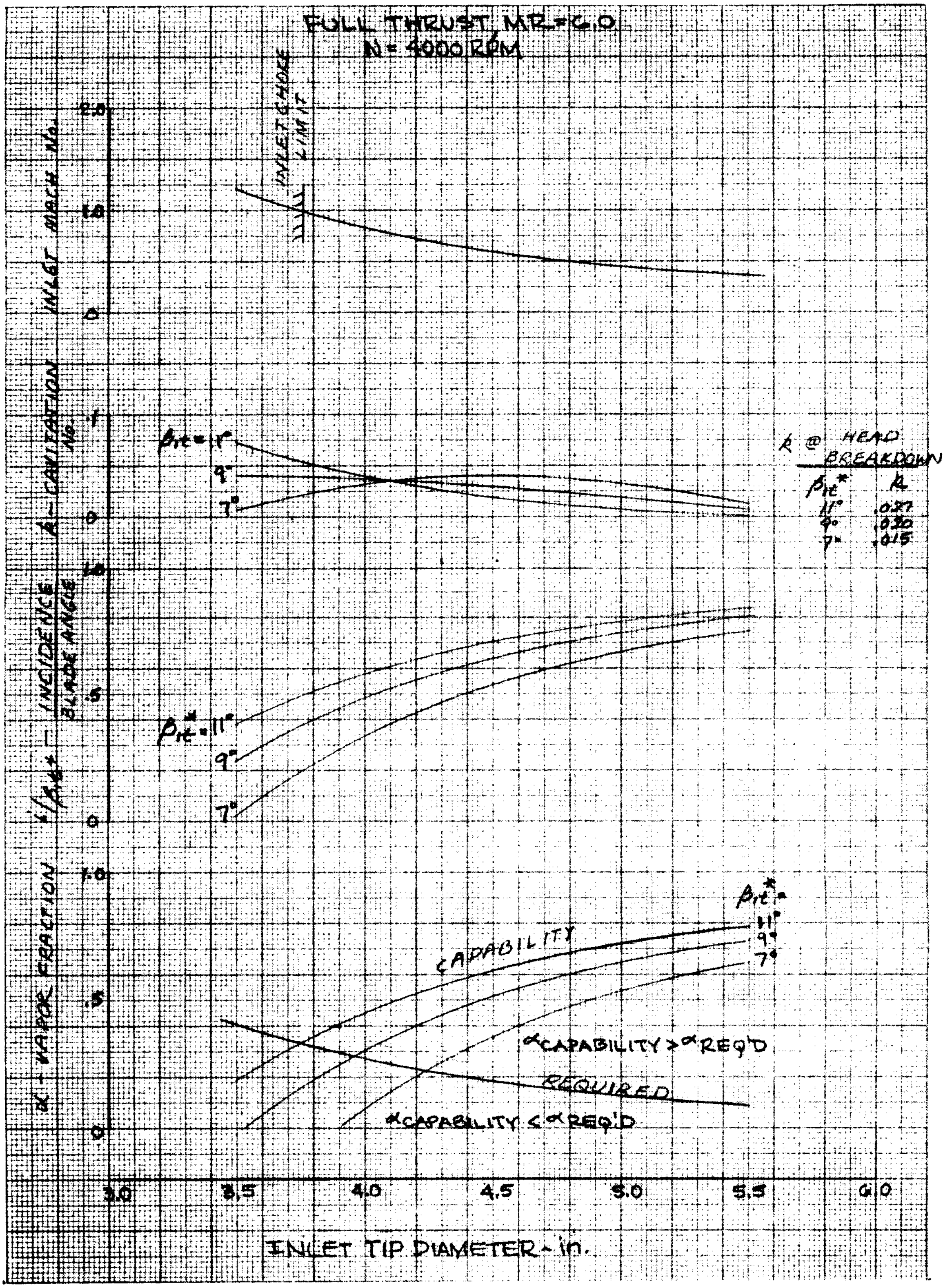


Figure B-31. Effect of Oxidizer Low Speed Inducer Diameter and Blade Angle on Suction Characteristics N = 4000 rpm

DF 98268

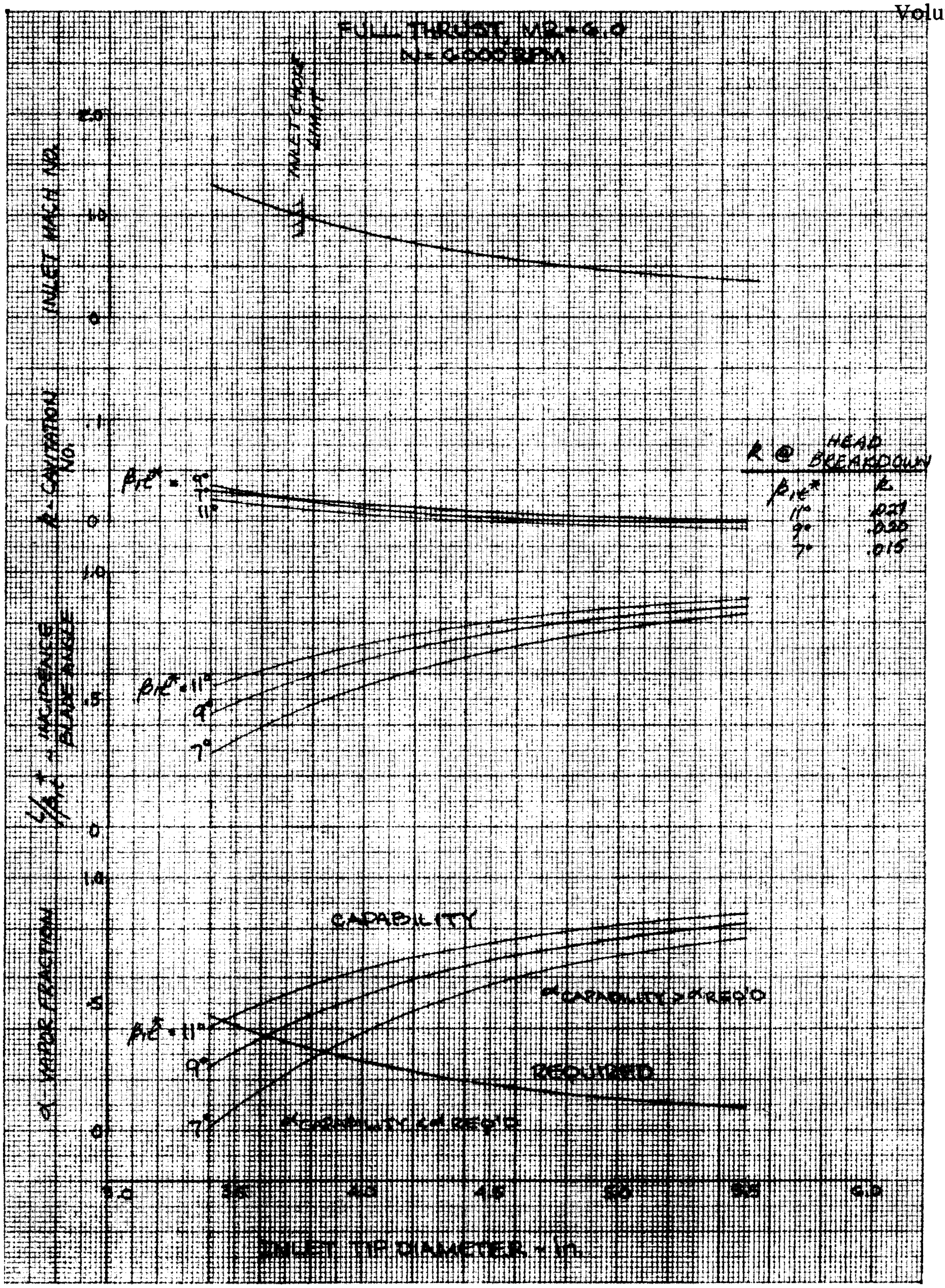


Figure B-32. Effect of Oxidizer Low Speed Inducer Diameter and Blade Angle on Suction Characteristics N = 6000 rpm DF 98269

As with the fuel pump study, a number of different designs appeared capable of meeting the required vapor fractions, which varied as a function of inducer diameter and matching line size, although certain factors again pointed toward a generally favorable design area. Choking within the inlet duct appeared not to be restrictive within the usable range of inducer diameters which satisfied the required vapor fractions. The largest margin in cavitation number was again provided by a low blade angle design. A 9-degree blade angle, while yielding a slightly lower margin in cavitation number, was considered more favorable for the oxidizer boost inducer as opposed to a 7-deg blade angle in order to provide reduced sensitivity to blockage effects. This factor is considered important for oxidizer pump designs, that appear to show greater sensitivity to flow blockages than hydrogen pump designs. Blade angles greater than 9 deg indicated substantially reduced cavitation number margin. An inducer diameter of approximately 4.6 in. yielded the peak cavitation number for the selected 9-deg blade angle, while providing a comfortable margin in two-phase pumping capability over the required values.

4.3 Low Speed Inducer Drive Selection

Gear, GH_2 turbine and electric motor drive systems were evaluated, considering the various advantages and disadvantages of each candidate system to the RL10 derivative engines. Table B-7 summarizes the advantages and disadvantages considered for each of the drive systems studied.

The gear drive was selected as the best system for use with the RL10 derivative engines. The gear drive maintains a fixed inducer/main pump speed relationship that satisfies all steady-state and transient suction requirements. The gear system can also be designed to operate with pitch line velocities and Hertz stresses no greater than those currently encountered on the RL10. The commonality of the gear environment should result in low development risk and cost and gear life should be at least as good as that of the other engine gears. In addition, the RL10A-3-3 oxidizer seal package design may be used directly thereby eliminating the need for additional seal development. Analytical studies using the transient computer deck indicated that the additional breakaway torque of a geared inducer would not be a problem on the RL10 derivative engines.

An engine mounted electric motor drive system could provide operational flexibility not available with the gear drive system such as higher propellant pressure for THI or cooldown. However the disadvantages such as complexity (due to the need for a control system, power source, and routing of power lines), higher cost (due to the need for developing the motors, and power source, and control system) and increased cooldown mass outweigh the good features of an engine mounted system.

The GH_2 turbine drive system provides no advantages over the gear drive system, and its disadvantages such as poor system efficiency, complexity, transient speed mismatch, and higher development cost indicate that it is not the optimum drive system for use with the RL10 Derivative engines.

4.4 Conclusions

The design analysis of two-phase pumping capability conducted during this study indicates that two-phase pumping is feasible for the RL10 engines derived for Space Tug use provided that the main fuel pump inducer is replaced with one of increased diameter and an oxidizer low speed inducer is added to the engine. A gear drive is the most desirable means of powering the oxidizer low speed inducer.

Table B-7. Comparison of Low Speed Inducer Drive Systems

	Gear Drive	GH_2 Turbine Drive	Electric Motor Drive
Advantages:	(1) Fixed Inducer/Main Pump Speed Relationship (2) Simplicity (3) No Control Required (4) Lowest Development Risk (5) High Drive Efficiency	(1) Packaging Flexibility (2) Long Life Capability (3) No Control Required	(1) Packaging Flexibility (2) Long Life Capability (3) No rotating Seal Packages (4) Does not Affect Engine Breakaway Torque (5) Can be Incorporated on Either Engine or Vehicle (6) Motors can Operate Before Engine Start to Provide Higher Inlet Pressure for THI and/or Cooldown (7) High Drive Efficiency
Disadvantages:	(1) Hydrogen/Oxygen Seal Package (2) Engine Mounted Only (3) No Flexibility in Operational Sequence (4) Life Limited Due to Gear Wear (5) Increased Engine Breakaway Torque	(1) Hydrogen/Oxygen Seal Package (2) High Axial Shaft Load (3) Requires High Pressure GH_2 Lines Across Interface if Vehicle Mounted (4) Inefficient System (5) Possible Breakaway Torque Problem (6) Overspeed Possible (7) Medium Development Risk	(1) Requires Speed Control (2) Increased Cooldown Mass (3) Highest Development Risk (4) Requires Engine Alternator and/or Vehicle Power Source and Power Lines

5. TWO-POSITION NOZZLE

The advanced RL10 engines achieve increased specific impulse over that of the RL10A-3-3 by increasing nozzle expansion ratio. Since the Space Tug and its payload must fit the restricted length of the Space Shuttle's payload bay, a two-position extendible nozzle was proposed to gain the increased performance while maintaining an acceptable stowed engine length. The two-position nozzle would be extended prior to the first firing and would be retracted prior to returning the Space Tug to the Space Shuttle payload bay. During the critical elements analysis of two-position nozzle systems, various mechanical concepts were evaluated and a performance evaluation was made comparing payload potential of fixed and two-position nozzle engines of equal length. The system components studied included nozzle seals, actuator brakes, drive motors, and actuation systems. The actuation systems considered were a cable and pulley system, and a jackscrew system shown in figures B-33 and B-34.

To be judged acceptable these two-position nozzle systems had to be:

a. Lightweight: impulse gain must offset increased weight to improve payload capability

b. Sturdy: strong enough to accept Space Shuttle input loads while retracted and Tug input loads while extended

c. Repeatable: provide accurate thrust alignment with each nozzle actuation

d. Compact: allow installation in the Space Shuttle payload bay

e. Sealed: provide positive thrust nozzle sealing between the fixed and translating nozzle to prevent hot gas leakage

f. Low powered: power for translating the nozzle must be within the Tug allowables

g. Reliable: Provide 300 cycles of operation without seizure or excessive increase in drag.

5.1 Mechanical Design Study

5.1.1 Actuation Systems

The ballscrew actuator system previously shown in figure B-33 guides the translating nozzle on three ballscrew actuators which are parallel to the engine centerline and are equally spaced about the nozzle circumference. Ballscrew translating nuts are attached to the moving thrust nozzle and thus pull the thrust nozzle as the ballscrews are rotated. Two redundant electric motors drive the three ballscrews through the sheathed flexible drive cable. Locking of movement in the forward or aft position is provided by a spline lock mounted on one of the ballscrew actuators.

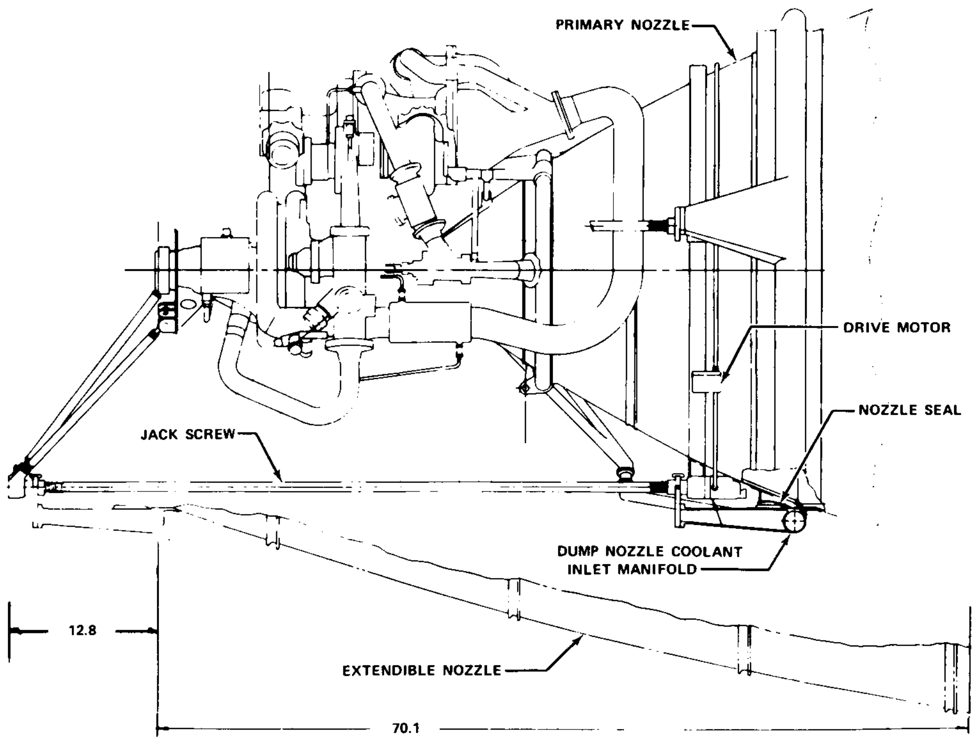


Figure B-33. Jackscrew Actuation System FD 71302A

The ballscrew actuator concept ensures positive linear motion during actuation because of the close tolerance mechanical interconnect between the thrust nozzle and the ballscrew. Synchronization of the jackscrews is provided by a continuous loop of sheathed flexible cable supplying power to each ballscrew. The system is designed to function satisfactorily should one cable and/or one motor fail. Because of the low windup (0.023) deg/in.-lb/in.) of the 0.330 in. diameter drive cable the maximum possible axial mismatch, i.e., nonsynchronization between any two screws is 0.014 in. This mismatch causes the nozzle to be canted 0 deg 2 sec from its nominal position, a value well within acceptable tolerances. Any increase in friction during the life of the system is compensated by providing over-capacity motors.

The pulley and cable system previously shown in figure B-34 guides the translating nozzle on 6 axial rails which are parallel to the engine centerline and are spaced at 60 deg intervals around the engine's circumference. Power to drive the thrust nozzle is applied to the aft pulley located on each rail. This pulley pulls a closed-loop cable connected to a similar pulley on the forward end of each rail. The thrust nozzle is attached to the cable on each rail and is thus translated as the pulley is rotated. Synchronization of the drive pulleys is provided by a sheathed flexible drive cable connecting the drive pulleys and driven by two redundant electric drive motors. An overcenter lock is integrated into each rail guide such that when the nozzle is in the extended position thrust loads are directly fed into the primary nozzle and not into the rails or pulley system.

The locks are unlocked as the cable system is reversed to move the thrust nozzle to the stowed position.

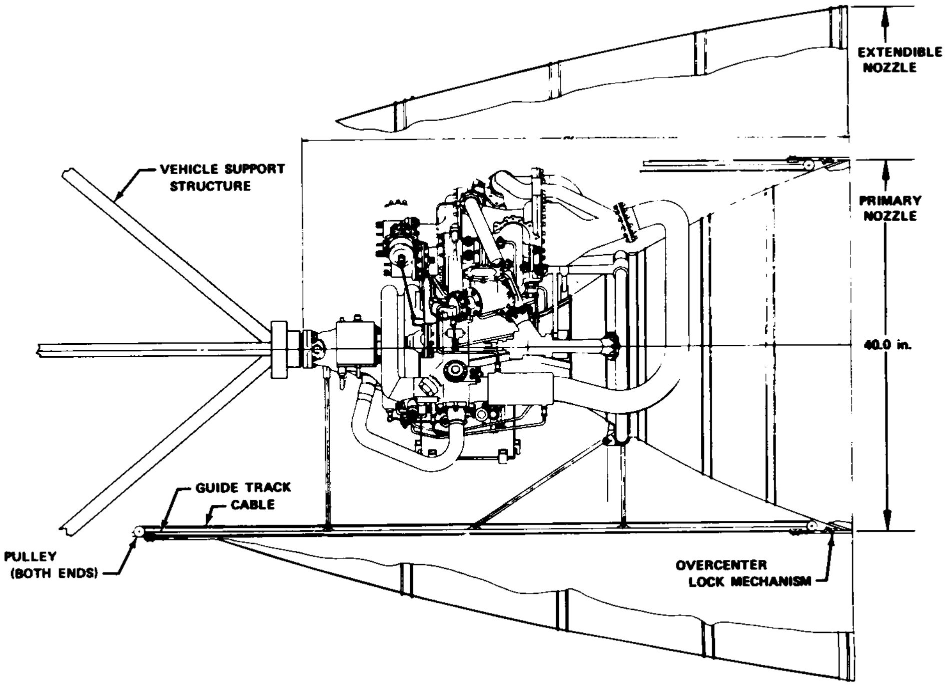


Figure B-34. Pulley and Cable Actuation System FD 67234B

While synchronization of the drive pulleys at the forward end of the guide rails is provided in a manner similar to that described for the jackscrew system, nozzle movement with the pulley and cable system is not as positive as that obtainable with the ballscrew system. Frictional effects of the thrust nozzle guides on the guide rails certainly vary from rail to rail and allow the nozzle to cock from side to side while it is translating. Final positioning is assured, however, by the use of a synchronization ring which drives all guides until each individual overcenter lock is sensed in the "home" position. This action requires the leading pulley and cable to slip allowing the lagging ones to catch up. The drive motors would drive until all overcenter locks are in the locked or extended nozzle position. The nozzle may cant from side to side during this translation but is aligned axially when it hits positioning stops in the stowed position.

In summary the ballscrew actuation system provides positive drive for the nozzle guides. Thrust nozzle alignment is inherent in the ballscrew actuators due to the closely toleranced parts associated with the screws, and their drive mechanisms. The pulley and cable actuation system can also be synchronized to maintain alignment by the use of a sync ring but the system provides a less positive drive mechanism than the ballscrew system.

5.1.2 Motion Brake

A thrust nozzle brake is necessary to prevent translation of the extendible thrust nozzle during firing or when in the retracted position for storage. The brake concepts selected for the pulley and cable system,, and the jackscrew systems are shown in figures B-35 and B-36, respectively.

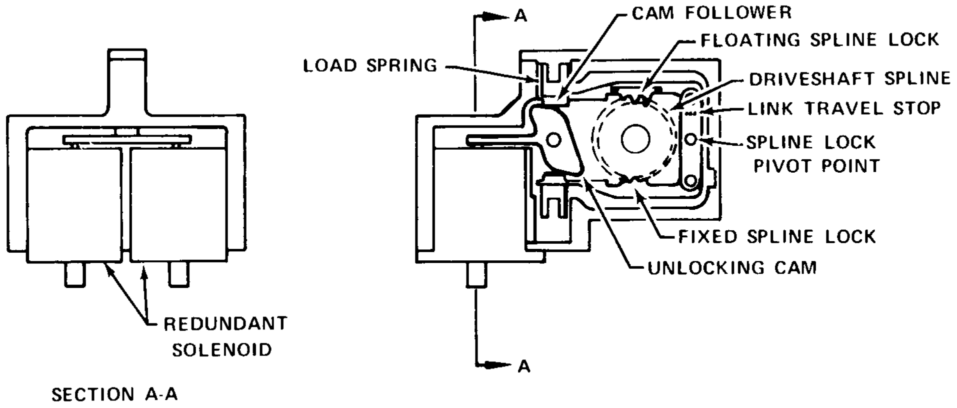
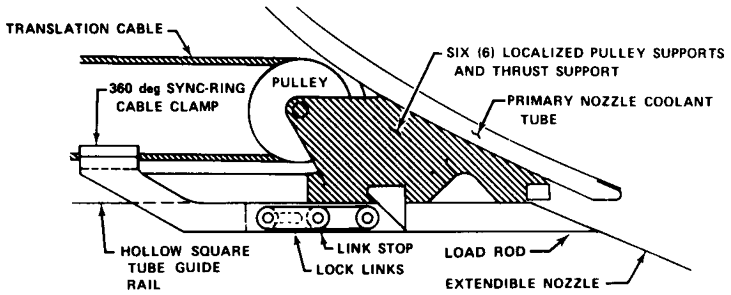
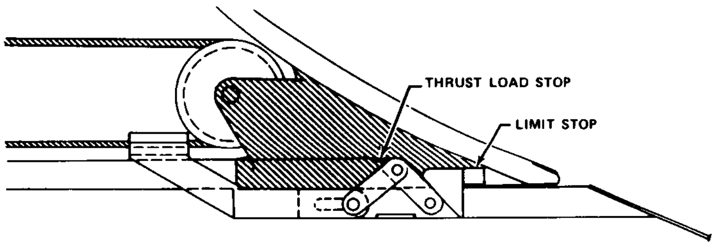

A
CAM FOLLOWER
FLOATING SPLINE LOCK
LOAD SPRING
DRIVESHAFT SPLINE
LINK TRAVEL STOP
SPLINE LOCK
PIVOT POINT
FIXED SPLINE LOCK
UNLOCKING CAM
REDUNDANT
SOLENOID
A
SECTION A-A

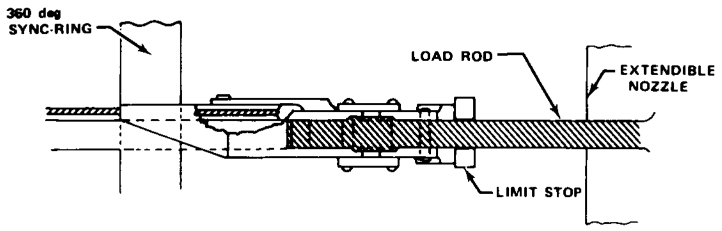
Figure B-35. Solenoid Actuated Brake FD 75864


TRANSLATION CABLE
360 deg SYNC-RING
CABLE CLAMP
PULLEY
SIX (6) LOCALIZED PULLEY SUPPORTS
AND THRUST SUPPORT
PRIMARY NOZZLE COOLANT
TUBE
HOLLOW SQUARE
TUBE GUIDE
RAIL
LINK STOP
LOCK LINKS
LOAD ROD
EXTENDIBLE NOZZLE

(a) NOZZLE IN TRANSLATING POSITION


THRUST LOAD STOP
LIMIT STOP

(b) NOZZLE IN LOCKED POSITION (SIDE VIEW)


360 deg
SYNC-RING
LOAD ROD
EXTENDIBLE
NOZZLE
LIMIT STOP

(c) NOZZLE IN LOCKED POSITION (TOP VIEW)

Figure B-36. Extendible Nozzle Overcenter Lock Mechanism FD 67232

The brake selected for the ballscrew actuator system is the spline lock, shown in figure B-35, located on one of the ballscrew actuators. A spline is added to the cable such that engaging the spline of the lock can stop travel. The engagement is ensured by dual helical springs that load the spline locks. Electric power provided to the drive motor energizes electric solenoids within the lock. Movement of the solenoids rotates the cam, and depresses the cam follower and springs within the lock mechanism, disengaging the spline lock and allowing drive shaft rotation. The fixed spline lock is always the first to engage and as it moves with the shaft pulls the floating spline into the locked position. Rotation torque is taken out by the spline lock pivot pin. The redundant solenoids are actuated during translation only, thus conserving electric power.

One brake is sufficient to lock the nozzle and the spline lock serves to prevent nozzle translation when in either the extended or retracted position.

View (a) of figure B-36 depicts the overcenter locks mechanism of the pulley and cable system in the axial or unlocked position while the nozzle is translating. This view is typical for any nozzle position along the rails. Mechanical stops keep the link from "breaking" to the outside during translation. As the lock links advance into the lock cutaway, the center roller guides into it until it stops on the aft surface of the cutaway. In this position, thrust loads from the translating nozzle are transferred through the aft link and into the thrust load stop which is part of the fixed nozzle, as shown in view (b) of figure B-36. The limit stop is adjustable so that alignment of the fixed and translating nozzle is assured along the flowpath. Thrust loads are not transmitted through the pulleys and/or cables, thus minimizing weight. Once the nozzle is actuated to move to the stowed position, the forward link pulls the center roller out of the corner and both links are repeatedly trapped in the axial plane. Locking is not provided in the stowed position.

The spline lock of the ballscrew actuator system provides positive locking in both the retracted and extended nozzle position and is less complex than the overcenter lock of the pulley and cable system, and was selected as the primary lock design approach.

5.1.3 Drive Motors

A two electric motor concept was selected to ensure reliability with each motor capable of providing the power necessary for translating the nozzle. Expected failures for electric motors during the engine life (10 hours) would be of electrical rather than mechanical nature and should not cause the system to mechanically seize. Shaft seizure caused by bearing failures normally occurs at 2,000 to 3,000 hours. Each motor, sized to provide two times the nominal torque of the system, will provide 25.0 in.-lb of torque at 240 rpm.

The electric drive was recommended for use in both actuation systems studied with neither system showing any particular advantage in this area.

5.1.4 Thrust Nozzle Seal

5.1.4.1 Finger Seal Concept

The extendible nozzle requires the use of a reliable seal between the fixed portion of the nozzle and the portion being translated. P&WA has developed a variety of hot gas seals for use on turbojets. The most applicable of these is the finger seal. The initial application of the finger seal concept for nozzle sealing is shown in figure B-37. Here the fingers are mounted to the fixed portion of the nozzle through a flange brazed to the tube assembly. Sealing is obtained by pressure loading the fingers against the translating nozzle. A finger guide is provided to ensure safe engagement with the seal during translation. A purge flow is provided from the nozzle coolant manifold to bathe the seal and its surroundings with cooled hydrogen. The normal exit for the purge flow is into the thrust nozzle main flow where it provides some cooling of the transition section. However, should a finger seal get deflected or bent, this purge flow would be directed to the leak protecting it from potential hot gas damage.

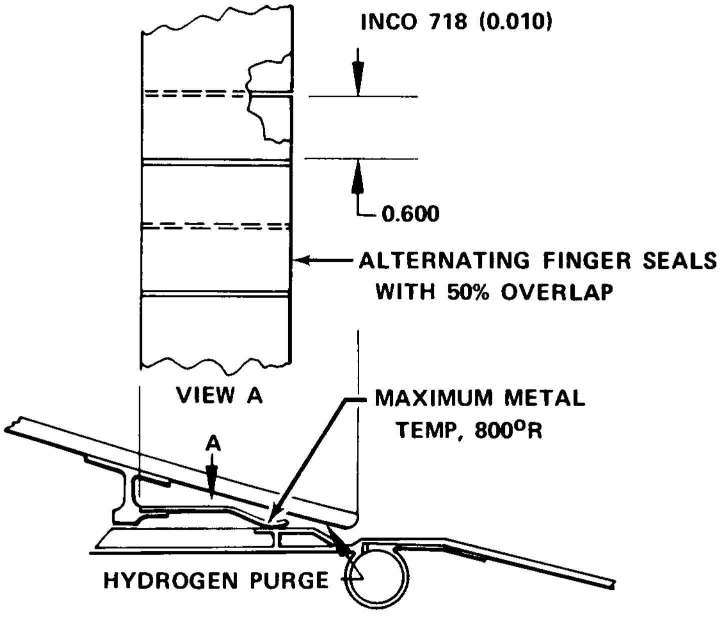

INCO 718 (0.010)
0.600
ALTERNATING FINGER SEALS WITH 50% OVERLAP
VIEW A
A
MAXIMUM METAL TEMP, 800°R
HYDROGEN PURGE

Figure B-37. Translating Nozzle Seal FD 71301

5.1.4.2 Double Finger Seal Concept

While the initial finger seal concept meets the sealing requirements, its failure mode to protect a damaged finger resulted in excessive overboard leakage. A second radial seal was therefore added which provided a more positive overboard seal and put the protective purge flow and transition section cooling flow in series, thus reducing the total purge flow requirements. This final configuration, shown in figure B-38, is called the double finger seal and is the selected configuration for all the derivative RL10 engines. The radial seal is of the diaphragm type and is pressure-loaded closed. Since it does not have fingers, leakage is held to a minimum. Purge flow introduced between the diaphragm seal and the finger seal pressurizes the cavity and lifts the fingers to pass flow over the nozzle transition section. Should a finger seal become damaged, flow is controlled to an acceptable cycle level by a choked orifice.

5.1.4.3 Bellows Seal Concept

An alternate bellows seal design is shown in figure B-39. It has all the operation safety features of the finger seal and in addition provides for large axial and radial displacements of the translating nozzle relative to the fixed nozzle. Although this design is considered feasible, P&WA has little experience with bellows of this diameter. The bellows seal characteristics may be more suited for use with the pulley and cable translation system due to the larger axial displacement capability.

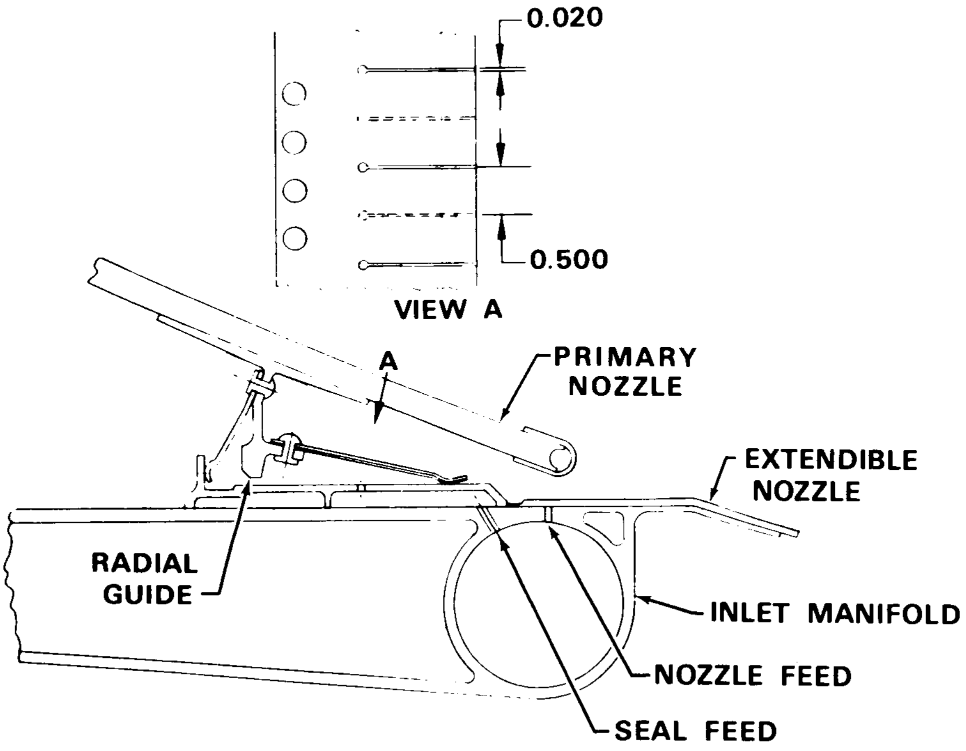

0.020
0.500
VIEW A
A
PRIMARY NOZZLE
EXTENDIBLE NOZZLE
RADIAL GUIDE
INLET MANIFOLD
NOZZLE FEED
SEAL FEED

Figure B-38. Double Finger Nozzle Seal Arrangement FD 72061A

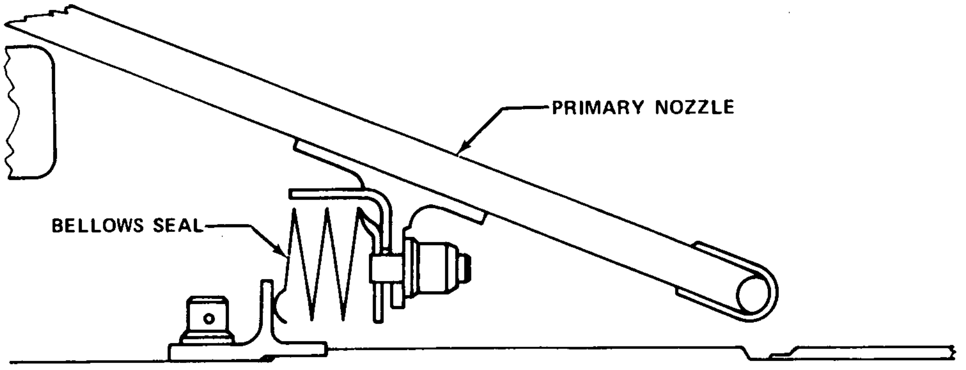

PRIMARY NOZZLE
BELLOWS SEAL

Figure B-39. Bellows Nozzle Seal Arrangement FD 75865

5.2 Weight Comparison

The weight summary shown in table B-8 depicts the subsystems of the two actuation systems investigated and respective weights. It can be seen that tracks, rollers, synchronization ring and supports of the pulley and cable system are heavier by almost 17 lb than their counterpart on the ballscrew system. Only the transmission and brake are heavier by a negligible amount on the ballscrew system.

Table B-8. Two-Position Nozzle System Weights

	Pulley and Cable	Jackscrews
Tracks, Rollers	21.3	12.7
Sync Ring	3.7	-
Motors and Cables	8.2	8.2
Transmission and Brake	5.0	5.9
Gussets	10.3	10.3
Supports	9.7	4.2
Total Weight, lb	58.2	41.3

5.3 Performance of Two-Position Nozzles

To evaluate two-position nozzle performance specific impulse, weight and length were established for two-position nozzles, and the values compared with similar values for fixed nozzles. Minimum length nozzle contours which have the highest performance for a given length were used. The nozzle contours and geometric characteristics were established using a two-dimensional method of characteristics nozzle design computer program. Specific impulse levels were determined using JANNAF methodology. Weights for the retracting mechanism were estimated from conceptual design layouts while nozzle weights were estimated from trade factors that were a function of surface areas. In all cases the retractable nozzle was assumed to be dump cooled while the primary fixed nozzles were of regeneratively cooled tubular construction.

Figure B-40 compares specific impulse and weight values for a typical engine configuration with both two-position and fixed nozzles installed. At comparable stowed lengths (nozzle retracted) the two-position nozzles show significant performance advantages over the fixed nozzles; however, as expected they weigh considerably more. When both effects are taken into account and they are compared on a payload basis the two-position nozzles still show significant overall payload gains. The payload for a Space Tug vehicle with a 70-in. long engine and a two-position nozzle is predicted to be 867 lb greater than for a comparable length engine with a fixed nozzle. A breakdown of the gains and losses for this case is shown in table B-9. A fixed nozzle engine with an installed length of approximately 118 in. would be required to obtain this overall payload gain.

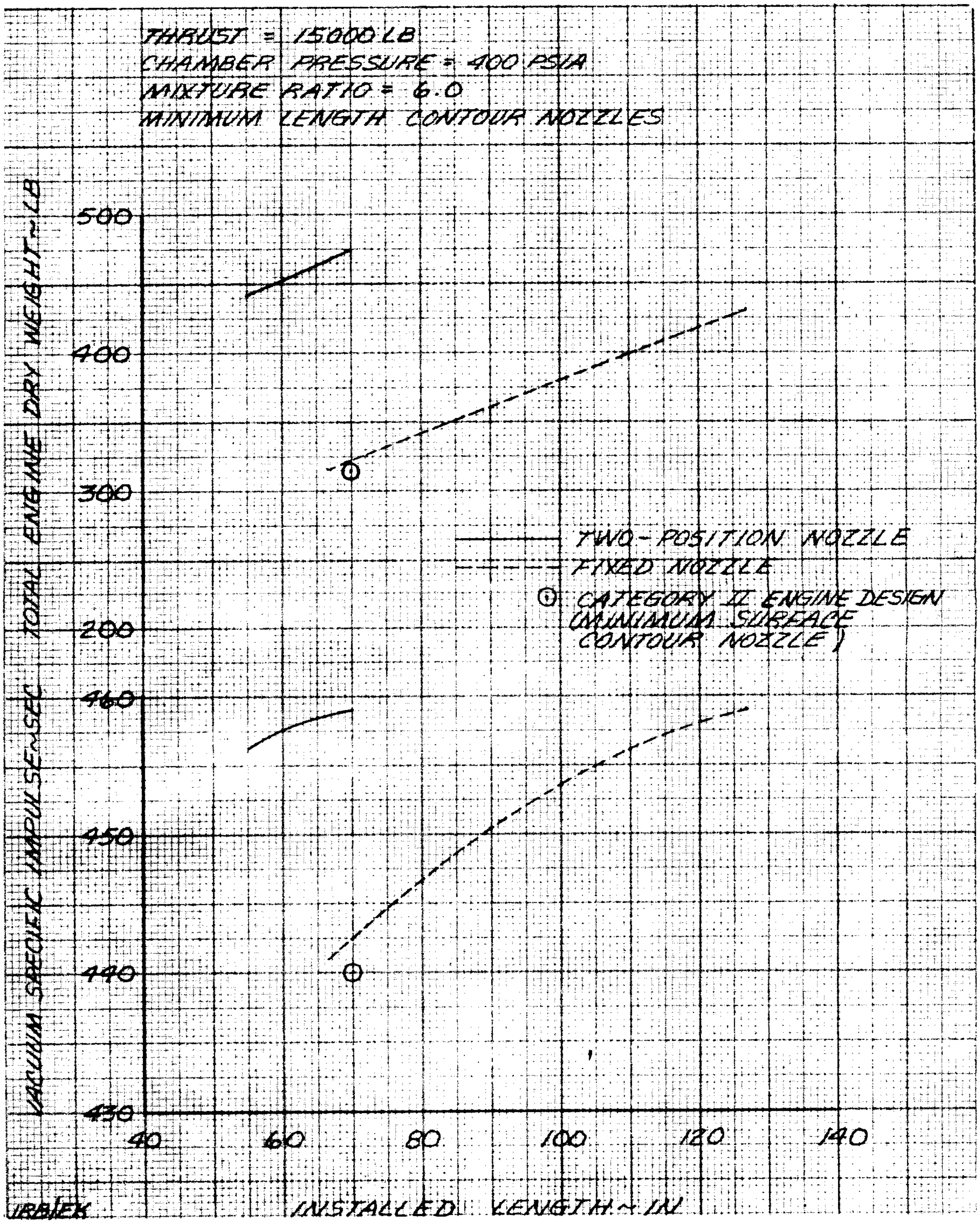


Figure B-40. Effect of Two-Position Nozzle on Performance

DF 97682

Table B-9. Payload Comparison for Two-Position Nozzle vs Fixed Position Nozzle

	Increase for Two-Position Nozzle (Over F. P. Nozzle)	Estimated Effect on Retrieval Payload**
Specific Impulse	16.5 sec	+1110 lb
Engine Weight	152. lb	- 243 lb
Net Payload Effect		+ 867 lb

**Payload effects are based on sensitivities of +67 lb payload/sec I_{sp} and -1.6 lb payload/lb inert weight.

5.4 Translating Nozzle Coolant Flow

It was assumed in the previous study that the secondary nozzle could be adequately cooled by the use of the existing gearbox leakage from the RL10A-3-3 turbopump (approximately 0.07 lb/sec). During the Critical Elements Evaluation, an analysis of the secondary nozzle coolant requirements was accomplished. The results of this study showed that for dump cooled high area ratio secondary nozzles, the required coolant flow was considerably greater than the 0.07 assumed in the previous study. Figure B-41 presents coolant flow as a function of nozzle area ratio for a Category II engine operating at a mixture ratio of 6.0 and shows that a nozzle having an area ratio of 260 would require a coolant flow of 0.3 lb/sec. The additional coolant is obtained by increasing the gearbox coolant flowrate as defined in Section C4.1.3.

5.5 Conclusion

A translating nozzle system can be designed to satisfy engine and vehicle/mission requirements and provide a significant gain in payload over a fixed nozzle engine of equal length. The study indicates the best design is one that uses ballscrew actuators driven by two electric motors. Thrust nozzle transition joint hot gas sealing can best be made with the double finger leaf seal.

6. SPECIFIC IMPULSE PREDICTIONS

Since the Space Tug vehicle design will be affected by small changes in main engine specific impulse it is important that performance of the RL10 Derivative engines be predicted as accurately as possible. In the previous study specific impulse values were predicted by estimating the changes in performance that should result from differences in the engine configuration from the RL10A-3-3 engine, e.g., chamber pressure, chamber/nozzle contour and expansion ratio, etc., and applying them as adjustments to the measured RL10A-3-3 specific impulse level. In most cases the adjustments were calculated using computer programs developed by Pratt & Whitney Aircraft. It was believed, however, that the definition of specific impulse could be improved by using the JANNAF computer programs because of the increased number of chamber/nozzle performance losses evaluated. Figure B-42 schematically depicts the JANNAF method and identifies the programs used for determining the individual performance losses. Appendix II includes a detailed description of the JANNAF methodology and the individual JANNAF programs.

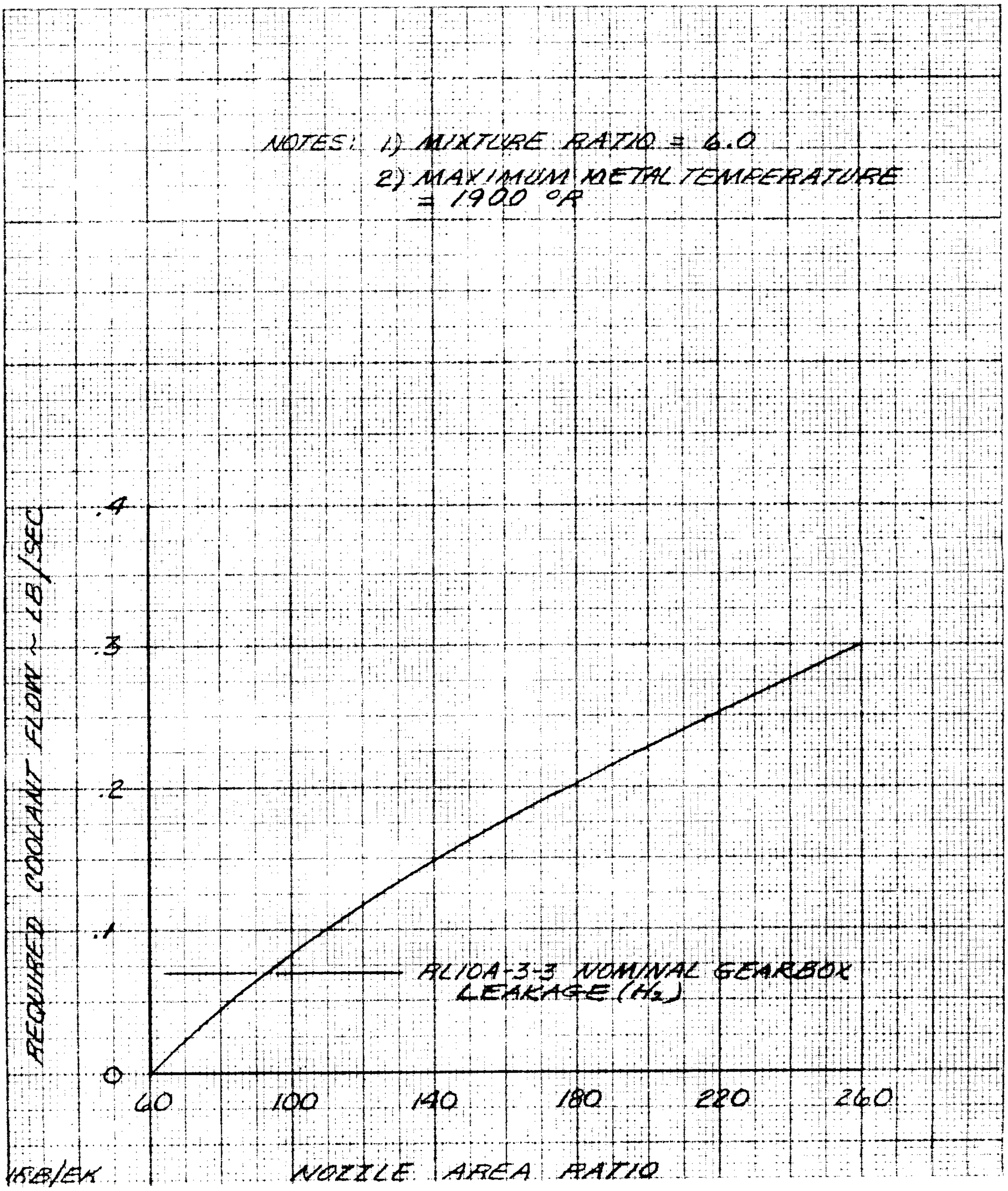
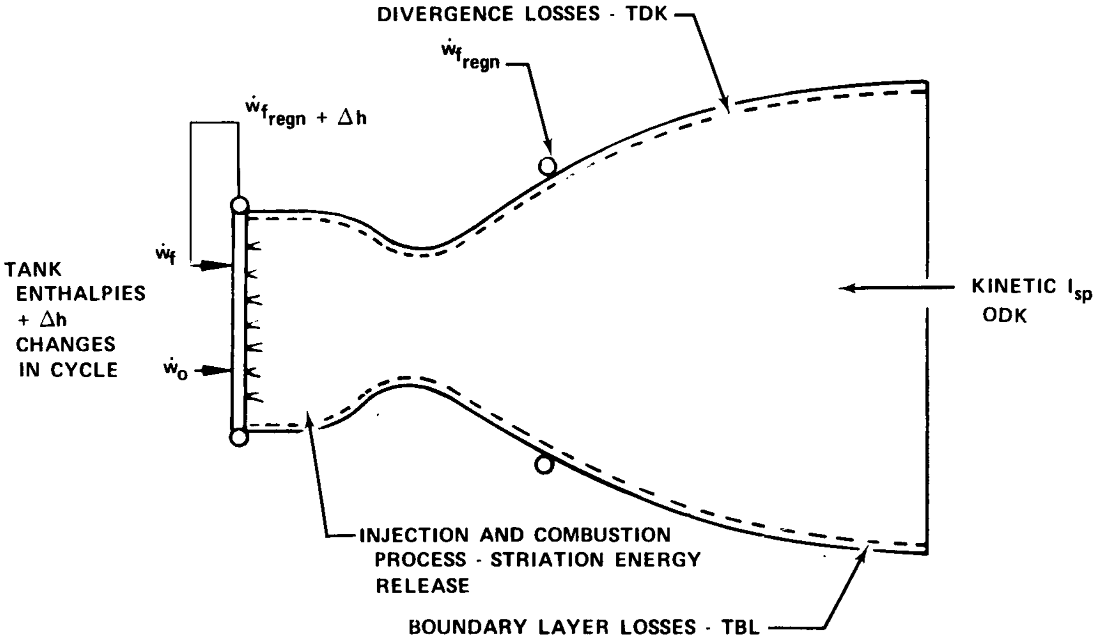


Figure B-41. Dump Cooled Nozzle Cooling Requirements Derivative II Engines DF 97678


DIVERGENCE LOSSES - TDK
$\dot{w}_{f_{regn}}$
$\dot{w}_{f_{regn}} + \Delta h$
TANK ENTHALPIES + Δh CHANGES IN CYCLE
$\dot{w}_f$
$\dot{w}_o$
KINETIC I_{sp} ODK
INJECTION AND COMBUSTION PROCESS - STRIATION ENERGY RELEASE
BOUNDARY LAYER LOSSES - TBL

$$I_{SP} = \frac{\left[I_{SP\,ODK} \times \eta_{DIV} \times \dot{W}P_{MS} \times \eta_{ER}\right] - \Delta F_{BL} + I_{SP_{DC}} \times \dot{W}P_{DC}}{\dot{W}P_{MS} + \dot{W}P_{DC} + \dot{W}P_{LEAKAGE}}$$

WHERE:

$I_{SP_{ODK}}$	ONE-DIMENSIONAL KINETIC SPECIFIC IMPULSE FROM JANNAF ODK PROGRAM (INCLUDES STRIATION EFFECTS)
η_{DIV}	DIVERGENCE EFFICIENCY FROM JANNAF TDK PROGRAM RUN IN PERFECT GAS MODE
$\dot{W}P_{MS}$	TOTAL PROPELLANT FLOW RATE FOR MAIN STREAM FROM ENGINE CYCLE ANALYSIS
ΔF_{BL}	THRUST LOSS DUE TO BOUNDARY LAYER EFFECTS FROM JANNAF TBL PROGRAM
η_{ER}	ENERGY RELEASE EFFICIENCY FROM P&WA INJECTOR AND COMBUSTION SYSTEM ANALYSIS
$I_{SP_{DC}}$	SPECIFIC IMPULSE OF DUMP COOLANT FROM JANNAF ODE PROGRAM AND ESTIMATED EFFICIENCY
$\dot{W}P_{DC}$	DUMP COOLANT FLOW RATE FROM ENGINE CYCLE AND DESIGN ANALYSIS
$\dot{W}P_{LEAKAGE}$	OVERBOARD LEAKAGE FLOW RATE FROM ENGINE CYCLE AND DESIGN ANALYSIS

Figure B-42. JANNAF Methodology FD 75871

6.1 Comparison of Predicted and Measured Specific Impulse Values

To provide a reference and establish the validity of the JANNAF performance calculations, specific impulse values were calculated for the RL10A-3-3 engine at mixture ratios of both 5.0 and 6.0 and compared to the measured value at the same mixture ratios.

Since the RL10A-3-3 engine is designed to operate at a mixture ratio of 5.0 the largest and most representative sample of data was collected for that mixture ratio. A nominal measured specific impulse value of 443.9 sec was established for a mixture ratio of 5.0 using the data shown in table B-10. Some testing with the RL10 has also been accomplished at mixture ratios of 6.0 and higher. These data were used to establish a nominal slope of specific impulse vs mixture ratio. A nominal measured value of 437.9 sec for a mixture ratio of 6.0 was then established by fitting that slope through the 443.9 sec established for the mixture ratio of 5.0 data. The nominal RL10 specific impulse vs mixture ratio slope is shown in figure B-43.

Table B-10. Demonstrated RL10A-3-3 I_{sp} at 5.0 MR

I_{sp} Demo Tests 3 Engines, 30 Runs	443.3 sec	3σ ±1.3 sec
Production Engines 56 Engines, 193 Runs	442.8 sec	3σ ±3.0 sec
FRDC I_{sp} Measurement Uncertainty	±3.0 sec	
Centaur Flights (GDCA Reconstruction) 9 Flights, 18 Engines	445.5 sec	3σ ±3.5 sec
Combined (Production and Flight)	443.9 sec	

After having established the nominal value of measured specific impulse for the RL10A-3-3 as a base, the JANNAF computer programs were used to predict the specific impulse values for the RL10A-3-3 configuration. Since the JANNAF Distributed Energy Release program was not available, a P&WA injector and combustion system characterization program was used to analyze the RL10A-3-3 combustion system and predict JANNAF energy release and striation losses.

The results of these RL10A-3-3 JANNAF specific impulse calculations are shown in table B-11. The differences between the JANNAF calculated specific impulse values and test data levels are 4.3 sec at a mixture ratio of 5.0 and 1.9 sec at a mixture ratio of 6.0. These differences, while within the 1% projected accuracy of JANNAF methodology, are believed to be partly due to inaccuracies in the TBL boundary layer loss predictions. JANNAF methodology uses an ideal specific impulse value that includes the effect of enthalpy gain in the regenerative nozzle. The boundary layer loss calculation accounts for heat removed from the boundary layer by regenerative cooling as well as friction effects. As indicated in table B-11 the net differences between the enthalpy gains and boundary layer losses are small and at a mixture ratio of 5.0 there is a gain rather than a loss. It appears unreasonable that the gains would exceed the losses making the boundary layer calculations suspect. Also, limited cases run at MSFC with the new proposed JANNAF Boundary Layer Integral Matrix Procedure (BLIMP) Program indicate that the losses predicted by TBL may be low. The BLIMP Program was not available to Pratt & Whitney Aircraft at the time of the contract, however, making it necessary that the TBL Program be used for the extensive number of JANNAF calculations.

DF 97935

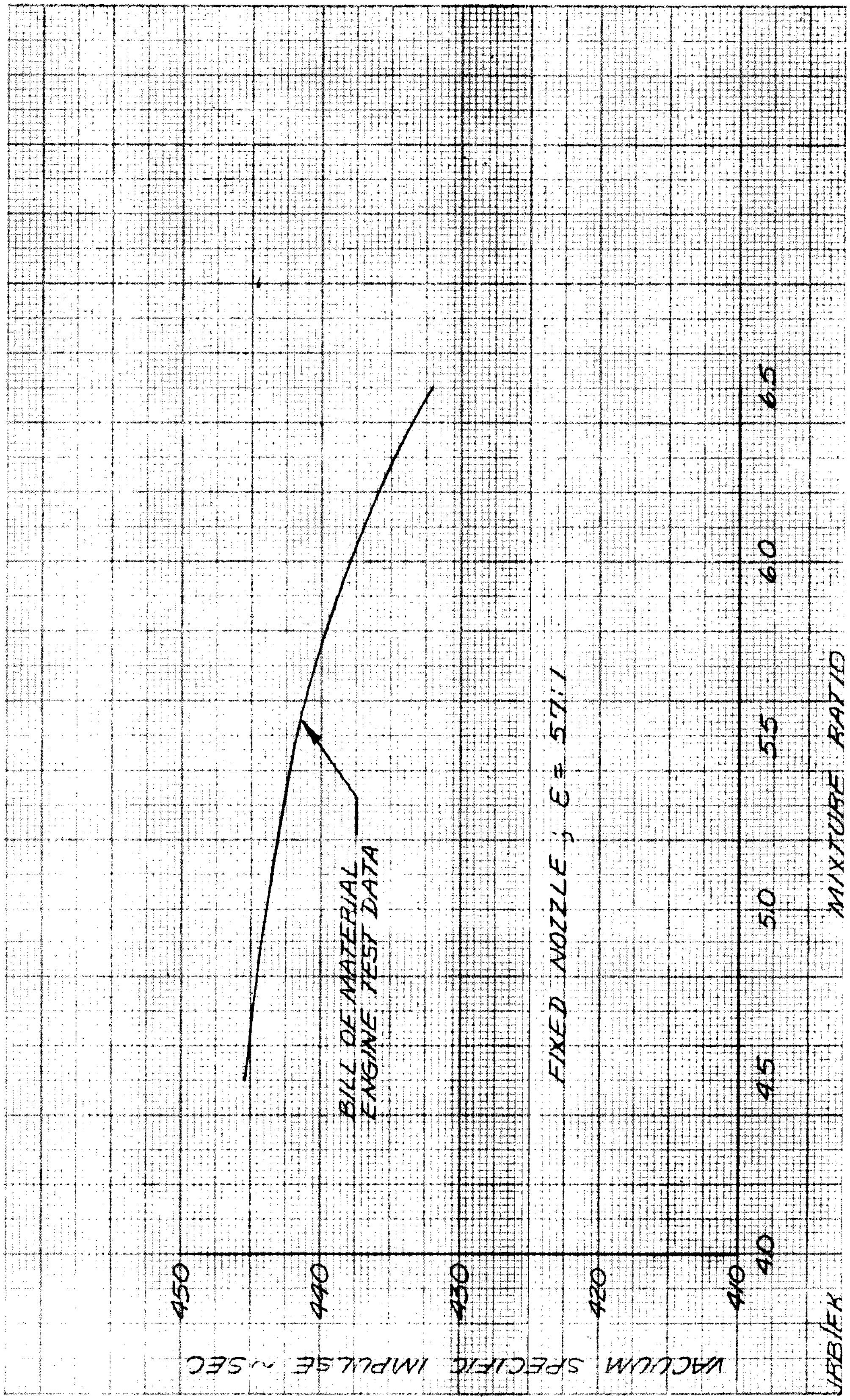


Figure B-43. RL10A-3-3 Engine Specific Impulse

Table B-11. Calculation of RL10A-3-3 JANNAF Specific Impulse Area Ratio = 57, P_c = 400 psia

Overall O/F	5.0	6.0
Chamber O/F	5.06	6.08
I_{vac}' at Chamber O/F and Inlet Enthalpies (ODE)	460.5 sec	457.2 sec
ΔI_{vac} Due to Regen Δh (ODE)	+7.7	+7.5
ΔI_{vac} Kinetics (ODK)	-2.7	-3.7
ΔI_{vac} Divergence (TDK)	-6.2	-6.1
ΔI_{vac} Boundary Layer (TBL)	-7.3	-8.0
ΔI_{vac} Leakage	-0.9	-0.9
ΔI_{vac} Striation	-0.1	-0.2
ΔI_{vac} Energy Release	-2.8 (0.9941)	-6.0 (0.9870)
I_{vac} Predicted	448.2	439.8
I_{vac} Test Data	443.9	437.9
Difference	4.3 sec	1.9 sec

Although the boundary layer losses calculated by the TBL Program for the RL10A-3-3 engine were somewhat questionable, it was not known what part of the difference between JANNAF and test data was due to boundary layer calculations and what part was due to other effects. It was therefore decided that, for those RL10 derivative engines which use chamber/nozzle components similar to those of the RL10A-3-3, specific impulse should be based upon JANNAF predictions corrected for test results. Since all derivative engines were designed to operate at a mixture ratio of 6.0, it was also felt that their predicted specific impulse values should be related to the RL10A-3-3 test values at that mixture ratio. At this mixture ratio, JANNAF gave a value of 439.8 sec whereas the test data value was 437.9 sec. To make the levels consistent, the JANNAF specific impulse values for the Category II and III engines were reduced by the ratio of these values (0.996). For a relatively new design such as the Category IV it was felt that this specific impulse penalty was not warranted and the JANNAF predicted values were used. These methods were used for all subsequent impulse calculations.

6.2 Specific Impulse Predictions for Derivative Engines

Specific impulse values were determined for the Category II, III, and IV engines using the same JANNAF methodology and computer programs that were used in the evaluation of RL10A-3-3 specific impulse. For these predictions, it was assumed that the injector could be optimized at its design mixture ratio to give an energy release loss no worse than that of the RL10 injector at a mixture ratio of 5.0 (2.8 sec loss) and that injector striation losses could be reduced to less than 0.1 sec.

The Category II engine, as defined in the previous study, uses the same chamber and nozzle as the RL10A-3-3 and the only significant component difference which would affect performance is the reoptimization of the injector for a mixture ratio of 6.0. Table B-12 presents a comparison of the JANNAF specific impulse losses for the Category II engine with the value determined in the previous study (Reference 1). The difference in specific impulse of 1.3 sec between the two predictions is due to higher energy release efficiency calculated by the JANNAF method.

The Category III engine, as defined in the previous study (Reference 1), is uprated in chamber pressure from the Category II design and it employs a new recontoured, two-position nozzle. Table B-13 presents a comparison of the JANNAF values determined for it during this study with the predictions from the previous study. The difference in specific impulse for the two predictions is 1.4 sec. Most of this difference is due to different net boundary layer loss/enthalpy gain effect than was used for the earlier predictions and to the higher energy release efficiency calculated by the JANNAF method.

The Category IV engine is a new design optimized for a higher chamber pressure having an injector-chamber-nozzle configuration similar to that of the Category III. Table B-14 presents a comparison of the specific impulse values calculated by JANNAF study with those determined in the previous study (Reference 1). The difference of 3.9 sec between the two predictions is caused by energy release, boundary layer and enthalpy differences.

6.3 Conclusions

As a result of the Critical Elements Evaluation of specific impulse the following conclusions were reached: (1) JANNAF methodology should be used for all subsequent performance calculations, (2) for engines which have many hardware similarities to the RL10A-3-3 engine, the JANNAF values should be adjusted downward by the ratio of RL10 test data to JANNAF values, (3) for a new design such as the Category IV engine, unmodified JANNAF values should be used, and (4) the JANNAF TBL predicted boundary layer losses are questionable.

Table B-12. Comparison of Previous Study and JANNAF Impulse Predictions

	Category II JANNAF Prediction
I_{vac}' at Chamber O/F and Injector Enthalpies (ODE)	464.7
ΔI_{vac} Kinetics (ODK)	-3.7
ΔI_{vac} Divergence (TDK)	-6.1
ΔI_{vac} Boundary Layer (TBL)	-8.0
ΔI_{vac} Leakage	-0.9
ΔI_{vac} Striation	0
ΔI_{vac} Energy Release	-2.8
ΔI_{vac} Correction to Measured RL10 Levels	-1.9
I_{vac} Delivered	441.3 sec

Table B-12. Comparison of Previous Study and JANNAF Impulse Predictions (Continued)

Previous Study Prediction

Adjustments Applied to Category I I_{sp}

$\Delta I_{vac}'$ (ϵ and P_c)	0
ΔI_{vac} Kinetics	0
ΔI_{vac} C_s (Divergence and Boundary Layer)	0
ΔI_{vac} η_c*	+2.0
ΔI_{vac} Leakage	0
I_{sp} Delivered (Category I)	438.0
I_{sp} Delivered	440.0 sec

Table B-13. Comparison of Previous Study and JANNAF Impulse Predictions

Category III

JANNAF Prediction

I_{vac}' at Chamber O/F and Injector Enthalpies (ODE)	492.3
ΔI_{vac} Kinetics (ODK)	-4.6
ΔI_{vac} Divergence (TDK)	-6.3
ΔI_{vac} Boundary Layer (TBL)	-8.9
ΔI_{vac} Leakage	0
ΔI_{vac} Striation	0
ΔI_{vac} Energy Release	-2.8
ΔI_{vac} Dump Cooling	-0.2
ΔI_{vac} Correction to Measured RL10 Levels	-1.9
I_{vac} Delivered	467.6 sec

Previous Study Prediction

Adjustments Applied to Category I I_{sp}

$\Delta I_{vac}'$ ODE (ϵ and P_c)	+26.5
ΔI_{vac} Kinetics (ODK)	-1.1
ΔI_{vac} C_s (Divergence and Boundary Layer)	<0.1
ΔI_{vac} η_c*	+2.0
ΔI_{vac} (Use Leakage for Dump Cooling)	+1.0
ΔI_{vac} (Dump Cooling I_{sp} Loss)	-0.2
I_{sp} Delivered (Category I)	438.0
I_{sp} Delivered	466.2 sec

Table B-14. Comparison of Previous Study and JANNAF Impulse Predictions

Category IV	
JANNAF Prediction	
I_{vac} at Chamber O/F and Injector Enthalpies (ODE)	493.9
ΔI_{vac} Kinetics (ODK)	-4.2
ΔI_{vac} Divergence (TDK)	-6.4
ΔI_{vac} Boundary Layer (TBL)	-9.0
ΔI_{vac} Leakage	0
ΔI_{vac} Striation	0
ΔI_{vac} Energy Release	-2.8
ΔI_{vac} Dump Cooling	-0.2
I_{vac} Delivered	471.3 sec
Previous Study Prediction	
Adjustments Applied to Category I I_{sp}	
$\Delta I_{vac}'$ ODE (ϵ and P_c)	+26.7
ΔI_{vac} Kinetics (ODK)	-0.1
ΔI_{vac} C_s (Divergence and Boundary Layer)	<0.1
ΔI_{vac} η_c^*	+2.0
ΔI_{vac} (Use Leakage for Dump Cooling)	+1.0
ΔI_{vac} (Dump Cooling I_{sp} Loss)	-0.2
I_{sp} Delivered (Category I)	438.0
I_{sp} Delivered	467.4 sec

7. CATEGORY III ENGINE

The baseline Category III engine was defined during the previous study (Reference 1) as a Category II engine uprated in chamber pressure. The engine operated at a chamber pressure of 700 psia and achieved high performance through the use of a 350:1 area ratio, two-position nozzle, as well as recontoured chamber and primary nozzle. With its reduced throat area, the engine delivered 16,800 lb of thrust at 700 psia chamber pressure, and mixture ratio of 6.0. The engine operated in tank head idle and pumped idle, and provided gaseous oxygen and hydrogen for tank pressurization.

A life evaluation of critical engine components and a reevaluation of the Category III engine cycle was undertaken during this Critical Element Evaluation. Life analysis was divided into fatigue life and wear life and the critical components were identified and evaluated for each. The thrust chamber/nozzle design was evaluated in terms of allowable thermal cycles and allowable chamber pressure limits. Components subject to a high degree of wear such as the turbomachinery bearings, gears and face seals were evaluated in terms of allowable operating time in hours. The engine cycle capability was reevaluated based on component characteristics and limitations defined during the life evaluation of the critical engine components.

7.1 Turbopump Design Analysis

For the Category III engine to achieve the operational objectives of 700 psia chamber pressure, the turbopump is required to operate at the following conditions compared to the RL10A-3-3 engine:

a. Fuel Pump

1. Increase speed from 31,000 to 40,000 rpm
2. Increase discharge pressure from 1030 to 1640 psia
3. Increase horsepower from 600 to 860.

b. Oxidizer Pump

1. Increase speed from 12,400 to 16,000 rpm
2. Increase discharge pressure from 620 to 999 psia
3. Increase horsepower from 95 to 193.

Increasing the pump rotational speeds and pressures impacts the turbopump rotor stresses, critical speed, bearing life and seal life. To evaluate these effects, specific stresses, seal loads, Hertz stresses, etc., were estimated by scaling up known levels of similar items recorded for the RL10A-3-3 engine. The results of this evaluation are summarized in table B-15.

Table B-15. Evaluation of Impact of Pump Speed Increase

Item	Conclusion	Remarks
Fuel Pump		
Critical Speed	Acceptable	27% Margin (25% required)
Impeller	Acceptable	Stress 40% under allowables tip speed = 1170 ft/sec (1600 ft/sec allowable)
Turbine	Modification required	Overstressed in web (beef up web, use same forging)
Bearings	Acceptable	Increase in thrust (can be adjusted for in metering plug at interstage bleed)
Shaft	Acceptable	
Oxidizer Pump		
Critical Speed	Acceptable	62% Margin (25% required)
Impeller	Acceptable	Tip speed = 300 ft/sec (1600 ft/sec allowable)
Thrust Bearing	Acceptable	No axial thrust increase - radial thrust increased, B_{10} life 340 hr, (100 hr allow)

Table B-15. Evaluation of Impact of Pump Speed Increase (Continued)

Item	Conclusion	Remarks
Roller Bearing	Acceptable	Radial thrust increased, B_{10} life 271 hr (100 hr allow)
Seals	Modification required	Increase bellows spring load and/or change pressure unbalance
Housings	Acceptable	
Gears	Modification required	Life of unmodified gearbox estimated at 0.8 hr. Hertz stress too high - lower to 55,000 psi by increased face width 60% - coat per PWA 550. Also include single bearing idler gear, and closer gear face alignment. Estimated life of modified gearbox is 10 hr.

7.1.1 Impeller Stresses

The RL10A-3-3 1st-stage stress summaries shown on figure B-44 for the front face and figure B-45 for the backface were reviewed. The peak stresses were selected and ratioed by the speed change ratio squared. Similarly, the second stage was studied using figures B-46 and B-47 for the front and backface stresses, respectively.

The critical stresses of this study are summarized in table B-16.

Table B-16. Impeller Stress Analysis

Type of Stress and Location	RL10A-3-3 Stress (psi)	Category III Stress (psi)
Bore Tangential		74,000 Allowable (yield)
1st-Stage Impeller	20,000	33,300
2nd-Stage Impeller	18,000	30,000
Radial		81,400 Allowable (1.1 yield)
1st-Stage Impeller	25,000	41,600
2nd-Stage Impeller	26,000	43,300
Maximum Tangential		74,000 Allowable (yield)
1st-Stage Impeller	25,000	41,600
2nd-Stage Impeller	25,000	41,600

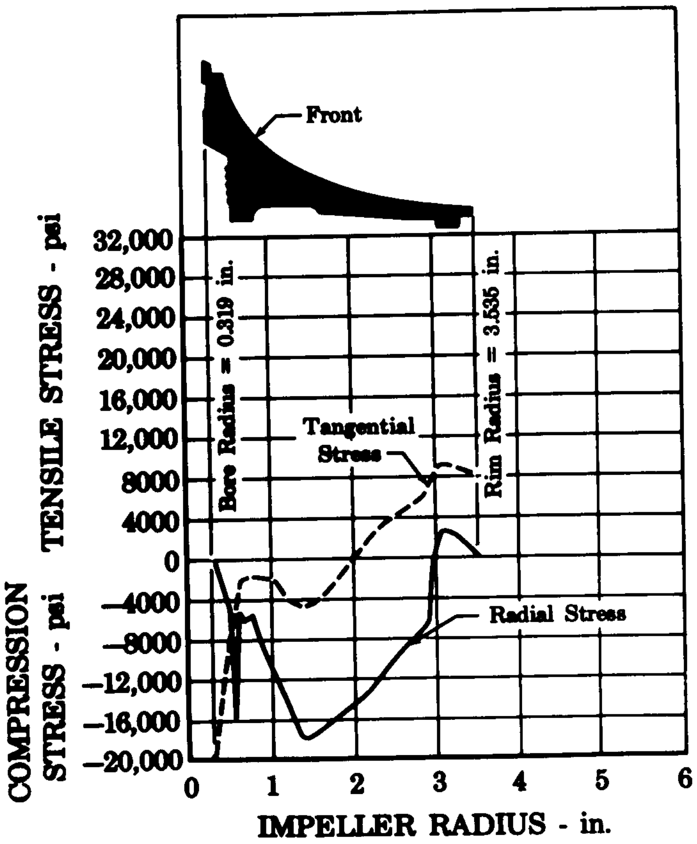


Figure B-44. Calculated First-Stage Fuel Pump Impeller Stresses (Front Face) FD 10769A

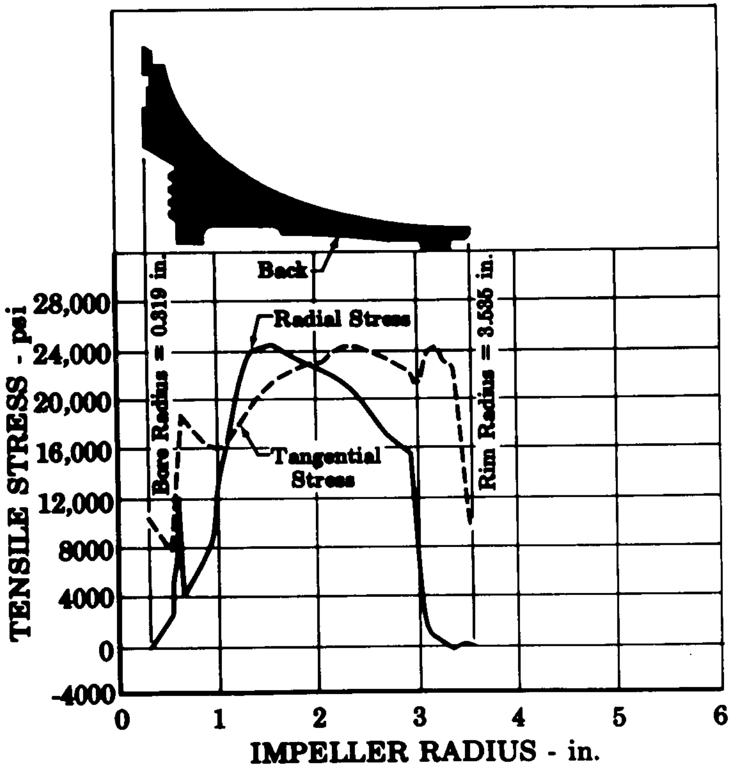


Figure B-45. Calculated First-Stage Fuel Pump Impeller Stresses (Back Face) FD 10833A

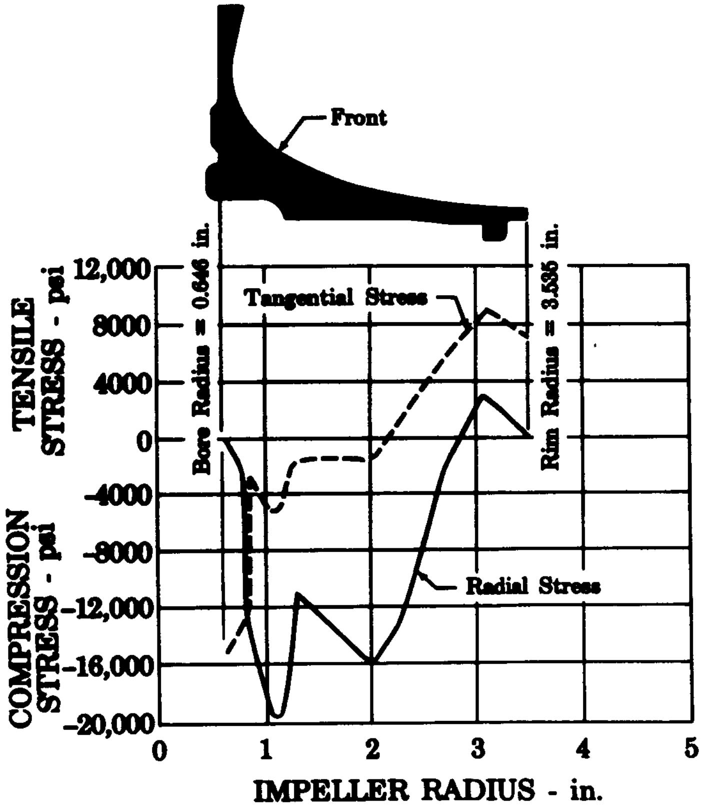


Figure B-46. Calculated Second-Stage Fuel Pump Impeller Stresses (Front Face) FD 10958A

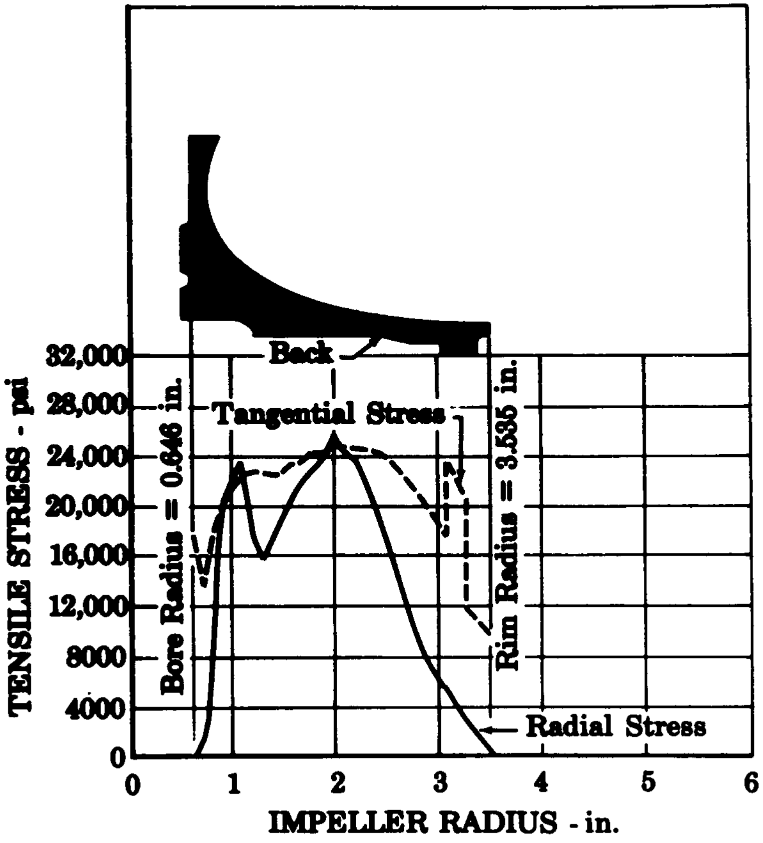


Figure B-47. Calculated Second-Stage Fuel Pump Impeller Stresses (Back Face) FD 10957A

Shrouds are needed on both stages to provide the necessary performance increases. Shroud tip speed is 1170 ft/sec which figure is below the estimated brazed shroud tip speed allowable of 1600 ft/sec. The impellers will be structurally suitable for use at Category III operating conditions.

7.1.2 Turbine Stresses

In a similar manner the turbine stresses of the RL10A-3-3 shown in figure B-48 were ratioed and their resultant values given in table B-17.

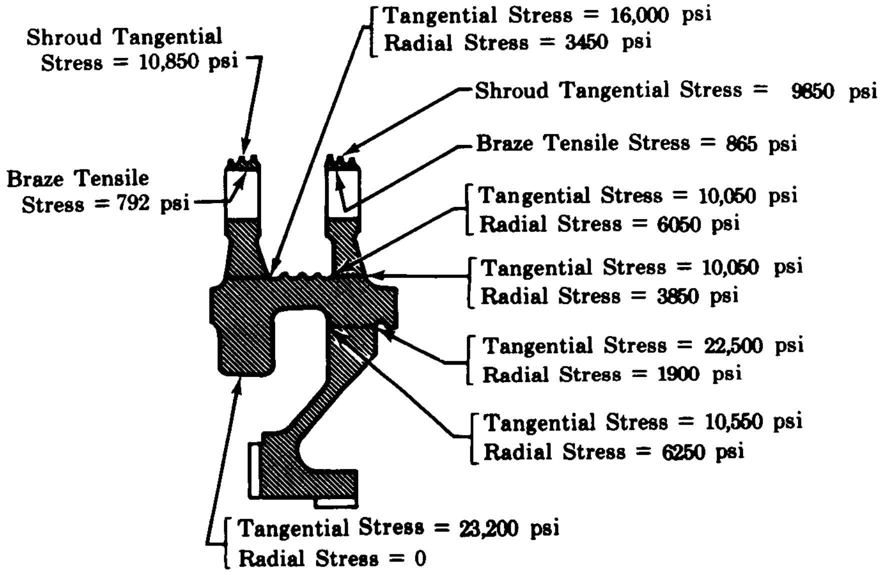

Shroud Tangential Stress = 10,850 psi
Braze Tensile Stress = 792 psi
Tangential Stress = 16,000 psi
Radial Stress = 3450 psi
Shroud Tangential Stress = 9850 psi
Braze Tensile Stress = 865 psi
Tangential Stress = 10,050 psi
Radial Stress = 6050 psi
Tangential Stress = 10,050 psi
Radial Stress = 3850 psi
Tangential Stress = 22,500 psi
Radial Stress = 1900 psi
Tangential Stress = 10,550 psi
Radial Stress = 6250 psi
Tangential Stress = 23,200 psi
Radial Stress = 0

Figure B-48. Calculated Turbine Rotor Stresses FD 10956A

Table B-17. Turbine Stress Analysis

Type of Stress and Location	RL10A-3-3 Stress (psi)	Category III Stress (psi)
Maximum Tangential Bore	23,200	38,600
Maximum Tangential Web	22,500	37,500
Tangential Allowable	---	36,000 (yield)
Maximum Radial	6,250	10,400
Radial Allowable	--	39,600 (1.1 x Yield)
Shroud Average Tangential	10,850	18,100
Shroud Allowable	--	20,000 (0.5 x ultimate)

The predicted web tangential stress and the bore tangential stress were over the allowable levels, indicating that plastic growth is encountered. These stresses can be reduced to acceptable levels by an increase in the disk web. Sufficient material is available in the existing forging to provide the additional web material.

7.1.3 Thrust Balance

The axial thrust unbalance contributed by the fuel and oxidizer pump impellers is not affected. The fuel pump impeller with two stages back to back and the oxidizer impeller with a single stage incorporating backface pumping blades, are balanced for large ranges of flow-to-speed ratios. The Category III flow-to-speed ratio is within the demonstrated range of the RL10A-3-3 flow-to-speed ratio and will require only minor orifice area trimming. The increased pressure drop across the shaft seals and the turbine disk causes an increase in axial load, however, and this increase must therefore be compensated by increasing the orifice size on the metering plug located between the first- and second-stage fuel pump impellers. Increasing the orifice size causes the pressure on the backface of the second-stage impeller to be decreased, and the pressure on the backface of the first-stage impeller to be increased. This increases the forward impeller thrust which compensates for the increased turbine and seal thrust. Analytical and test data illustrate that the classes of metering plugs available for use on the RL10A-3-3 have an axial load range greater than the predicted thrust increase.

7.1.4 Bearing Loads

Radial thrust increase is expected on the oxidizer thrust and roller bearings. Since the oxidizer impeller is a single-stage design with a single volute, radial thrust is encountered at off-design conditions. This radial thrust is a function of impeller geometry, head rise, and relative flowrates (off-design to nominal design). An increase in pressure (head rise) increases off-design radial load. Since the impeller is overhung from the thrust bearing, the thrust bearing radial load increases by a factor of 2.03, whereas the roller bearing increases by a factor of 1.03. This results in an estimated B_{10} life of 340 hr and 271 hr for the thrust and roller bearings. The design goal B_{10} life is 100 hr for the Category III turbopump. The B_{10} life is estimated by calculating life based on the standard rating life formula used for roller or ball bearings operating in oil and then adjusting this level in bearing life for operation in cryogenic fluids. The adjustment was applied by dividing the B_{10} life obtained from the rating life formula by a factor of 10 established during cryogenic bearing testing.

7.1.5 Shaft Torsion

The increase in horsepower on the shaft produced torsional stresses 80% below the torsional allowable levels.

7.1.6 Pump Housings

RL10A-3-3 turbopump housings are designed to limit wall deflections and as such are not stress limited at the Category III conditions. Deflection analysis at Category III conditions was not attempted.

7.1.7 Critical Speed

The Category III engine fuel turbopump and oxidizer pump rotors are estimated to have the following first, second and third critical speeds, respectively.

Fuel	Oxidizer
50,600 rpm	26,000 rpm
55,700 rpm	76,800 rpm
87,100 rpm	183,500 rpm

These critical speeds of the Category III engine were estimated to be the same as those of the RL10A-3-3 engine because of the basic turbopump similarity.

The fuel turbopump operating speed is 40,000 rpm, or 26.5% less than the first critical speed for the fuel pump rotor. A 25% margin is desirable.

The oxidizer pump operating speed is 16,000 rpm which is 62.5% less than the first critical speed for the oxidizer pump rotor. Here again, 25% margin is desirable.

7.1.8 Gears

To estimate gear life, a curve shown in figure B-49, was generated plotting Hertz stress versus cycles. Data were obtained using a gear similar in geometry to the RL10 gear except that it was oil lubricated. These data generated a straight line when plotted on a log scale. Extending the data line to one cycle establishes a point at which an oil lubricated gear and hydrogen cooled gear would both fail. While the stress at this point is well beyond that encountered in operation, it does establish commonality between gears of similar geometry. An estimate of the gear life characteristics for hydrogen cooled gears was obtained by drawing a connecting line between the commonality point at one cycle and the cycles demonstrated by the RL10A-3-3 maximum-life gear. This line establishes a conservative gear life characteristic for the RL10A-3-3 hydrogen cooled gears, since the RL10A-3-3 data point was obtained from a nonfailed high life gear.

The RL10A-3-3 engine gears which transmit power from the fuel turbine shaft to the oxidizer shaft cannot provide the 10-hr life required for the Category III turbopump. Estimated life is 48 minutes for the existing gears functioning at Category III conditions. Hertz stress has increased 27% and pitch line velocity 33% over that of the RL10A-3-3 engine. Several methods were investigated to increase gear life. The most productive were: (1) reduce Hertz stress by increasing face width, (2) coat gear with PWA 550, an antigalling compound consisting of molybdenum disulfide plus antimony oxide with a silicon binder, (3) incorporate a single bearing idler gear in place of the double bearing idler gear. Testing has shown that gear wear rate might be decreased by 100% or more by substituting PWA 550 for the gear dry film lubricant presently used in the RL10A-3-3 engine (PWA 61). The maximum allowable Hertz stress for 10 hours life at 40,000 rpm (2.4 x 10^7 cycles) is 55,000 psi when using PWA 550. (See figure B-49.) To reduce gear Hertz stress from 71,600 psi (existing gear geometry at 40,000 rpm with increased horsepower) to 55,000 psi, the gear face width must be increased approximately 60%.

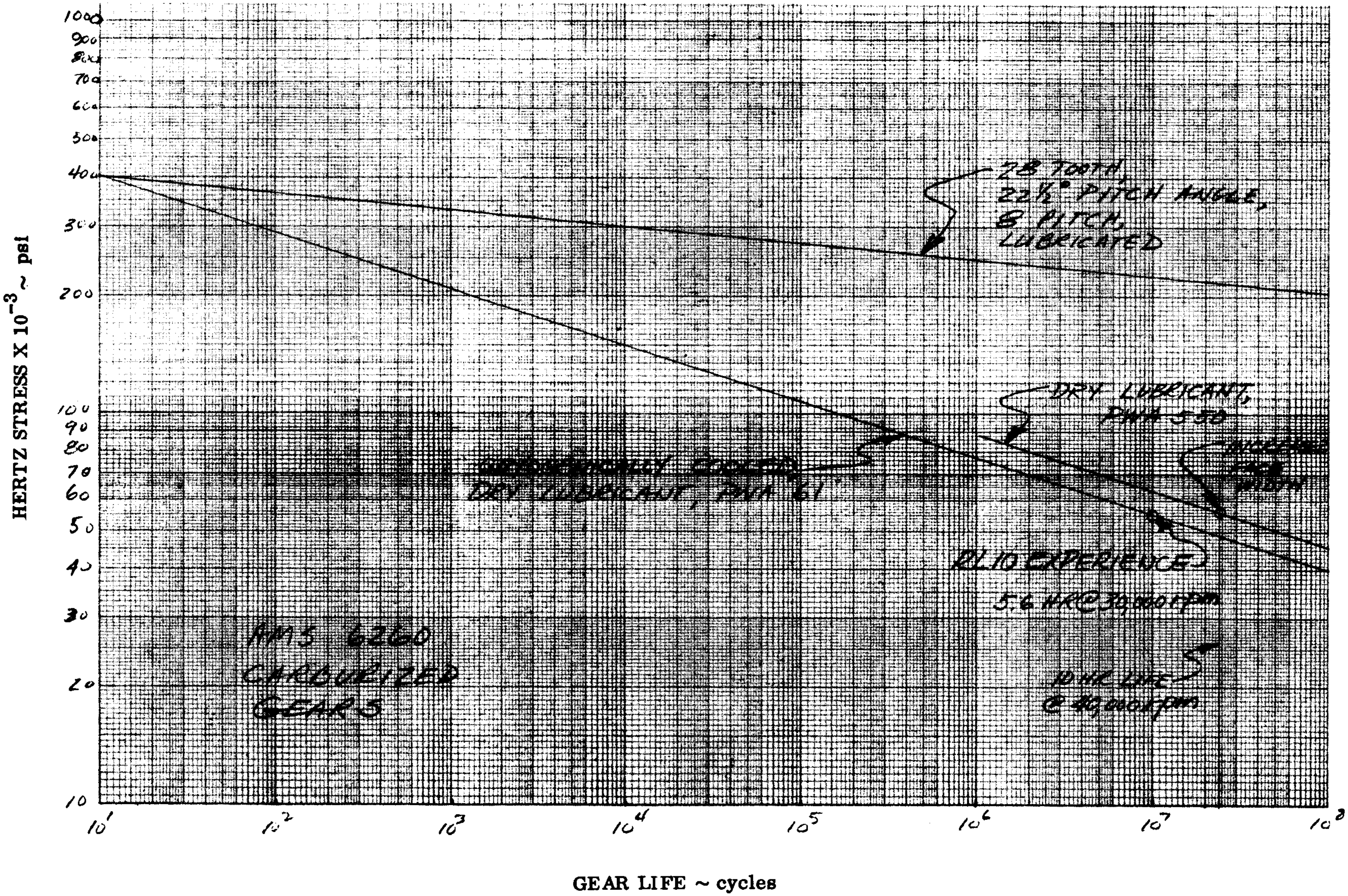
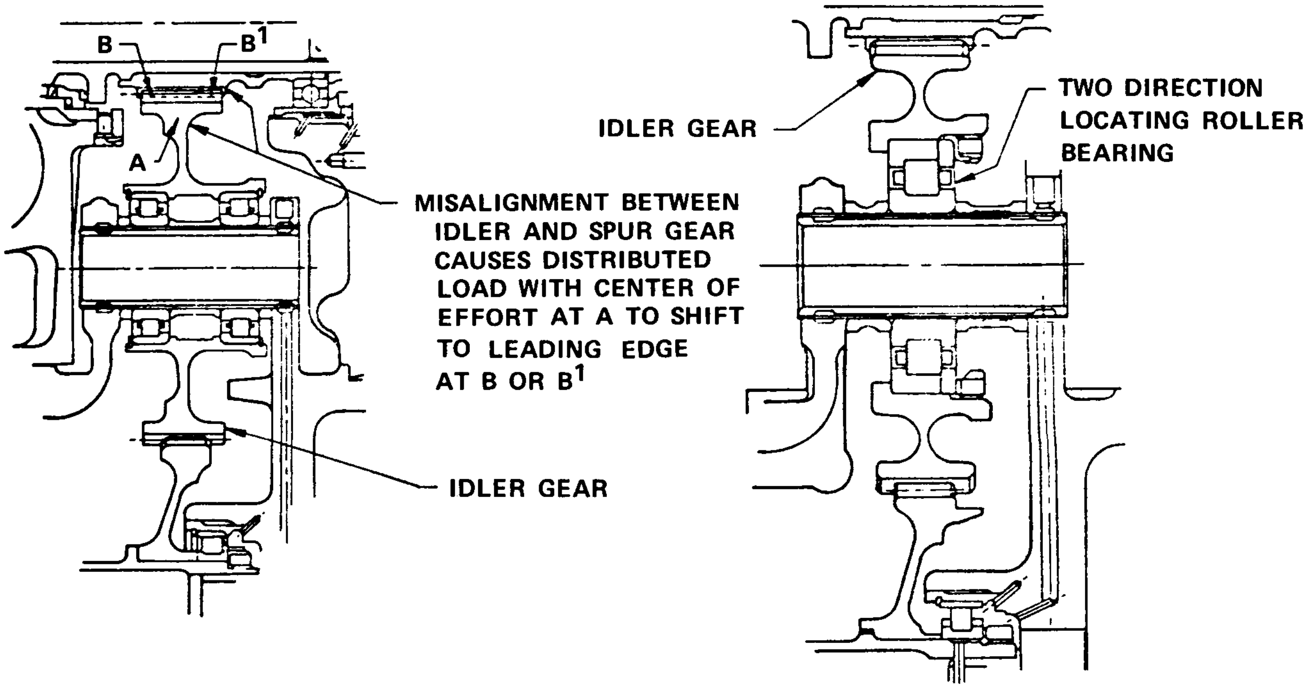


Figure B-49. Category III Engine Gear Life

DF 98270

A single bearing idler gear, figure B-50, increases gear life by improving alignment. This configuration gives the idler gear freedom to adjust its plane of revolution thereby maximizing the amount of line contact between the gear teeth in the event of shaft misalignment and minimizing the amount of load shift due to misalignment. The single bearing idler gear arrangement was qualified for the RL10 but was not incorporated into production engines. The effect on gear life of incorporating a single bearing idler gear could not be estimated; however, its incorporation should increase life.


B
B1
A
MISALIGNMENT BETWEEN IDLER AND SPUR GEAR CAUSES DISTRIBUTED LOAD WITH CENTER OF EFFORT AT A TO SHIFT TO LEADING EDGE AT B OR B1
IDLER GEAR
IDLER GEAR
TWO DIRECTION LOCATING ROLLER BEARING

SUPPORTED IDLER GEAR
RL10 TURBOPUMP ASSEMBLY

SINGLE BEARING IDLER GEAR
ADVANCED TURBOPUMP ASSEMBLY

Figure B-50. Single Bearing Idler Gear FD 75870

7.1.9 Seals

Turbopump seal wear rates were investigated to determine projected seal wear for the 10-hr requirement. Wear rate data for 8 seals in the RL10A-3-3 turbopump assembly were increased by the ratio of the pump speed between the Category III and RL10A-3-3 to account for the increased speed and similar pressure loading. The resultant wear rate in in./hr was multiplied by the 10-hr life requirement for the Category III engine to obtain the resultant wear. This wear reflects the amount of spring expansion that the seal loading bellows or wave washer will experience during its 10-hr operation. Spring expansion will in turn cause a seal face load reduction that, if excessive, will unseat the seal and allow excessive leakage.

Seven of the eight turbopump seals are projected to wear less than 0.030 in. for 10 hr and despite the seal load loss associated with this wear, unseating of the seal is not predicted. The LOX bellows seals, however, has a projected wear of 0.218 in., and excessive amount considering the seal geometry. The use of an improved RL10A-3-3 LOX bellows seal (Chicago Rawhide type AMS 5540 Inconel) with teflon impregnated carbon was investigated. This seal projected a wear of 0.010 in. for 10 hr, a vast improvement but still excessive for this particular seal. To keep the seal seated, the bellows spring-load must either be increased or the pressure load decreased by changing the pressure unbalance, both changes requiring a seal redesign.

7.1.10 Conclusions

All turbopump components appear to be suitable for Category III engine operation at 700 psia chamber pressure except for the turbine disk, the gears and the oxidizer seal. All three components can be easily modified without major assembly changes.

7.2 Thrust Chamber Design Analysis

During the previous study (Reference 1), Category III engine thrust chamber life was predicted by comparing estimated tube temperatures at the geometric throat with that of the RL10A-3-3 engine. A comparison of Category III predictions with the RL10A-3-3 data is given in table B-18 below:

Table B-18. Comparison of Thrust Chamber Parameters (Previous Study)

Parameter	Category III Engine	RL10A-3-3 Engine
Tube OD, in.	0.0824 *	0.0911
Tube Wall Thickness, in.	0.0100	0.01275
Chamber Pressure, psia	700	396
Mixture Ratio, O/F	6.14	5.06
Coolant Temperature, °R	224	228
Hot Wall Temperature, °R	2357	2051
Cold Wall Temperature, °R	1547	1387
Estimated Cycle Life	>160	>200

---

*Minimum practical tube size using current RL10 manufacturing procedures.

Based on this comparison of throat conditions, it was concluded during the previous study that a Category III engine could operate at 700 psia chamber pressure with a SS 347 tubular thrust chamber with only slightly less chamber life than the RL10A-3-3 engine, since the predicted Category III engine tube wall temperatures and thermal gradients were only slightly higher than those of the RL10A-3-3. It was recognized, however, that because the previous study (Reference 1) was limited to the throat plane, the actual life of the Category III engine could be less than the RL10A-3-3 engine, and that a more detailed analysis would be required.

7.2.1 Thermodynamic Analysis

An expanded definition of the Category III thrust chamber thermal environment was established during the Critical Elements Evaluation which included predictions of tube temperatures along the entire chamber walls. This analysis revealed that tube temperatures in the chamber convergent section were significantly higher than those predicted at the throat. In fact the temperature exceeded the allowable material temperature of SS 347.

The high tube temperatures in the convergent section of the thrust chamber, illustrated in figure B-51 resulted from low flow velocity in the tubes. The 180 round cooling tubes assumed for this analysis must expand in area at the same rate as the chamber to fill the chamber circumference. While the chamber area at the injector is the same as the RL10A-3-3, the chamber throat area is significantly smaller because of the increase in operating chamber pressure. This increase in chamber contraction ratio requires a similar increase in tube flow area ratio resulting in a decrease in flow velocity in the convergent section when circular tubes are used.

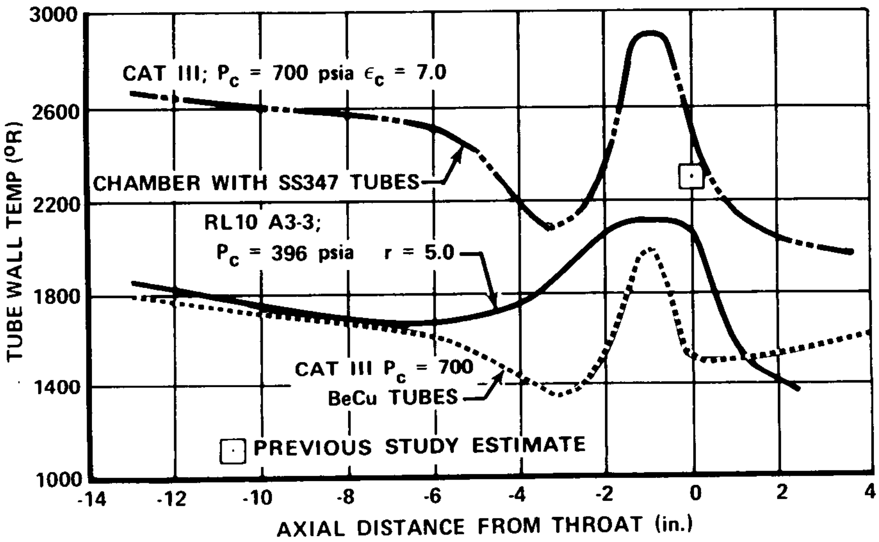


Figure B-51. Chamber Tube Temperatures, Category III Engine

FD 71329

Three potential solutions for reducing the high tube temperatures were investigated. The first study evaluated the effect of replacing the round cooling tubes with tubes having custom shaped cross sections. These tubes provided increased internal velocity while satisfying the combustion chamber geometric requirement. The second study evaluated the effect of using a high thermal conductivity material (berylium copper) to lower the thermal resistance between the hot wall and the coolant. The third study evaluated the effect of lowering chamber pressure to reduce heat flux.

7.2.2 Effects of Reconfigured Cooling Tubes

Increasing the coolant velocity in the chamber convergent section by reconfiguring the coolant tube cross section was rejected as a possible solution because of the high coolant pressure losses that accompanied only a moderate reduction in tube temperature (a 100% increase in coolant jacket ΔP resulted in only a 250°R reduction in maximum tube temperature).

7.2.3 Effect of High Conductivity Tube Material

A large reduction in tube temperatures can be realized if the SS 347 tubes are replaced by BeCu tubes as previously shown in figure B-51. The high thermal conductivity of BeCu significantly reduces the thermal resistance between the hot combustion gases and the coolant and results in lowering wall temperatures to 1800°R at a chamber pressure of 700 psia. Berylium copper is also attractive from a manufacturing standpoint because it can easily be substituted for 347 SST in the manufacturing operations and therefore seemed an attractive candidate for the Category III tubular chamber.

A review of BeCu material properties revealed that insufficient data were available to calculate tube cycle life directly; therefore, alternate criteria were used to define the maximum allowable thermal loads. The criteria chosen required tube wall margin to be less than or equal to that of the RL10A-3-3 margin which is 200°R. Tube wall margin is defined as the difference in temperature between the wall temperature and the temperature corresponding to the ultimate material stress. To further account for lack of data credibility, the anticipated stress level has been increased by a factor of 4. This stress level (10,000 psi), when used in conjunction with the available material properties, defines a maximum allowable hot wall temperature of 1310°R.

Since the maximum tube wall temperature at 700 psia chamber pressure was predicted to be approximately 1800°R, a tube wall margin of 200 deg cannot be maintained. A reduction in chamber pressure is therefore required.

7.2.4 Effects of Reducing Chamber Pressure

The maximum tube temperature (slightly upstream of throat) of the Category III chamber is predicted to decrease with decreasing chamber pressure (heat flux) for both SS 347 and BeCu tubes as shown in figure B-52. These data showed that with the BeCu alloy chamber pressure is limited to levels below 515 psia by the estimated maximum tube temperature of 1310°R. Chamber pressure with the SS 347 tubular chamber is limited to a level below 550 psia if tube wall temperatures and chamber life are to remain similar to that of the RL10A-3-3. The Category III SS 347 chamber is capable of operating at higher chamber pressure than the RL10A-3-3 because of the use of tubes of smaller diameter and thinner wall material as described in the introduction.

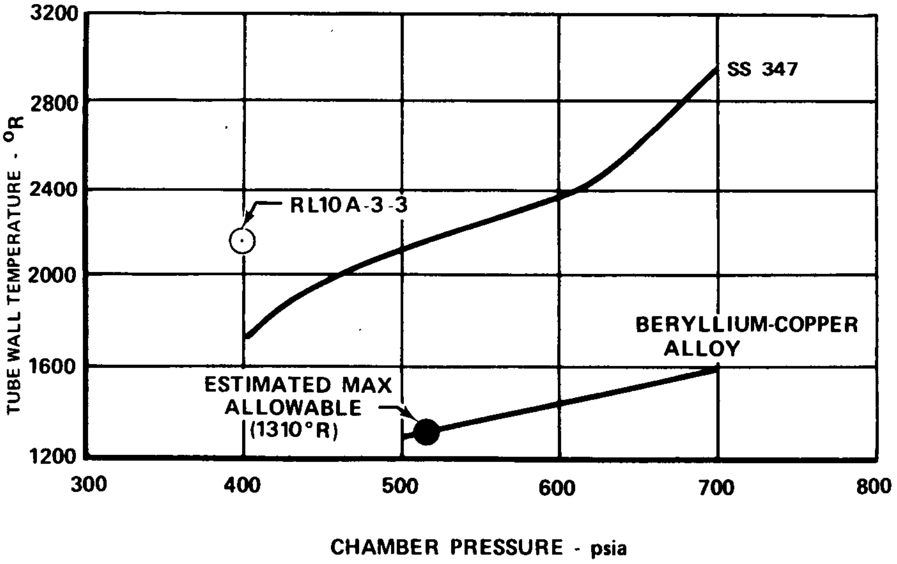


Figure B-52. Category III RL10 Maximum Tube Temperature vs Chamber Pressure FD 71324A

7.2.5 Conclusion

In view of the limited information on BeCu alloy, it was decided to retain SS 347 as the Category III chamber tube material and to reduce chamber pressure to a level that would ensure acceptable engine cycle life capability. Hence, the Category III chamber pressure level was reduced to 550 psia.

7.3 Engine Cycle Analysis

After mechanical and thermodynamic evaluation of the critical Category III engine components (chamber/nozzle and turbopump), the design point cycle was updated by including the new component characteristics and limitations into the cycle. To reduce the maximum wall temperature of the tubes to an acceptable level, the design chamber pressure was reduced from the original level of 700 psia to 550 psia. Analysis of the engine cycle operating characteristics shows that by reducing thrust from 16,800 lb to 15,000 lb, and by recontouring the nozzle, it would be possible to retain the extended nozzle area ratio of 350 within the engine installed length of 70.1 in. A comparison of the updated Category III engine operating characteristics with those defined in the previous study (Reference 1) is presented in table B-19.

A detailed cycle printout for the new Category III engine is presented in figure B-53. The significant changes in the updated cycle are due to the reduction in chamber pressure from 700 to 550 psia. The fuel pump operation point shown in figure B-54 for the new Category III cycle is closer to the RL10A-3-3 operating point but it is still in a potentially unstable region. As a result, fuel pump modifications identified in the previous study to shift the head/flow operating characteristics are still required, but are less extensive.

Table B-19. Category III Engine Critical Element Evaluation

	Previous Study	Updated
Thrust, lb	16,800	15,000
Overall Mixture Ratio	6.0	6.0
Area Ratio	105/350	97/350
Specific Impulse (EXTRAP), sec	466.2	462.1
Chamber Pressure, psia	700	550
Fuel Pump Speed, rpm	40,000	32,400
Fuel Pump Discharge Pressure, psia	1640	1070
Main Chamber ΔP, psia	300	200
Main Chamber ΔT, °R	374	445
Engine Length (Retracted/Extended), in.	70/127	70/128
Engine Diameter (Primary/Secondary), in.	40/73	41/78

7.4 Conclusion

The maximum chamber pressure level of the Category III engine is limited to 550 psia. Since the performance improvement over that of the basic RL10 engine operating at 400 psia chamber pressure is only approximately 1%, this engine concept should not be pursued further.

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-----------------------------------------------------------------------
                  MODIFIED RL10 OFF-DESIGN DECK

      CATEGORY  III  BASELINE  RM=6.0

-----------------------------------------------------------------------


                          INLET CONDITIONS
      ******************************************************
            FUEL                                    LOX
      PRESSURE      17.60                 PRESSURE      20.54
      TEMP           36.9                 TEMP          163.8
      NPSP           1.60                 NPSP           4.54
      FLOW           4.64                 FLOW          27.82


                                    FUEL PUMP          LOX PUMP
                                    ****************   ****************
                                    SPEED     32214.   SPEED     12886.

                                    FLOW       4.637   FLOW      27.823
                                    INLET GPM  470.9   POWER     107.47
                                                       EFF       0.6079
                                                       INLET P    20.54
                                    *  1ST  STAGE  *   INLET T    163.8
                                    POWER     230.62   DISCH P   655.51
                                    EFF       0.6215   DISCH T    167.9
  FUEL TURBINE                      INLET P    17.60   RHO IN    70.898
****************                    DISCH P   536.74   RHO OUT   70.802
FLOW        4.323                   DISCH T   44.796   INLET GPM  176.2
POWER         0.0                   RHO IN     4.421
EFF        0.7926                   RHO OUT    4.396
INLET P    840.71
INLET T     498.6                   *  2ND  STAGE  *
DIS P(S)   653.48                   POWER     257.46
DELH ACT     97.4                   EFF       0.5856
M. VEL R    0.473                   INLET P   536.74
ACD        0.9780                   DISCH P  1066.54
         p
TDIS MIX    473.9                   DISCH T     54.6
HP TRANS    107.5                   RHO OUT    4.330
P/P         1.287


 FUEL INJECTOR      LOX INJECTOR    ***********************************
****************   ****************  *                                 *
DELTA P     57.83  DELTA P    46.04  *  MIXTURE RATIO          6.000  *
INLET P    608.28  INLET P   596.50  *  THRUST                15000.  *
INLET T     473.9  INLET T    168.2  *  IMPULSE               462.10  *
ACD         1.942  ACD        0.730  *  CHAMBER PRESSURE      550.46  *
MV         45.417  RHO       70.705  *                                 *
                   MV        15.006  ***********************************
```

Figure B-53(1). Category III Engine Cycle

```
       JACKET          LEAKAGE & BLEED     RM CONTROL VLV     THRUST CONTROL
***************** ***************** ***************** *****************
FLOW        4.55   WLEAK       0.087   DELTA P    59.01  ACD          0.0479
INLET P  1049.21   WT/P-FUEL   0.0     ACD       0.6440  WTBY/WF      5.000
INLET T     54.6   WT/P-LOX    0.0     K FACTOR  5.3973  WTBY         0.228
DELTA PJ 200.000                                         P/P          1.369
DELTA TJ 444.000



 SYSTEM PRESSURE LOSSES
***********************

I
PUMP INTR STG       0.0
PUMP DIS LINE      12.68
CAV VENTURI         0.00
JAC IN LINE         4.653
JAC DIS LINE        0.0
FUEL TURB IN        8.492
FUEL TURB DIS      21.158
INJ IN LINE        12.199
```

Figure B-53(2). Category III Engine Cycle

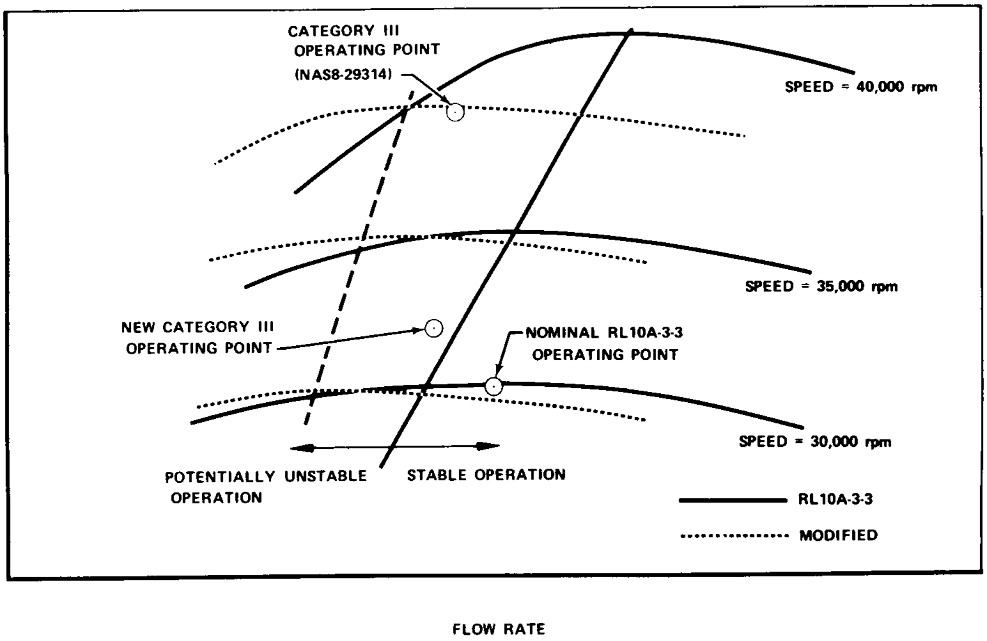


Figure B-54. Category III Engine Fuel Pump Characteristics

FD 75867

8. CATEGORY IV ENGINE

The baseline Category IV engine defined during the previous study uses the expander cycle and much of the RL10A-3-3 technology and, with the exception of pump inlet diameters, is interchangeable with the RL10. It is sized for 15,000-lb thrust, operates at a chamber pressure of approximately 870 psia, and is optimized for operation at a mixture ratio of 6. It includes both tank head and pumped idle capabilities and is capable of autogenous pressurization of both propellant tanks. The engine has gear driven low speed inducers. This is a "clean sheet" engine requiring high speed, high pressure turbomachinery to achieve the targeted goal of 870 psia chamber pressure. Analysis of various mechanical and cycle balance items associated with the increased speed and pressures were completed during this Critical Elements Evaluation. Those mechanical items which were studied include chamber life and chamber construction, rotor critical speeds, rotor thrust balance and bearing load/life analysis, high speed gear capability, rotor burst margins, and pump seal design. The engine cycle power balance was re-evaluated based upon component characteristics and limitations defined during the mechanical evaluation of critical elements.

8.1 Thrust Chamber Design Analysis

During the previous studies of the Category IV engine, only throat region hot wall temperature was determined. This temperature was approximately 2500°R and appeared to indicate that a SS 347 tubular chamber similar to that of the RL10A-3-3 could be used. During these Critical Element Evaluation studies, the entire thrust nozzle temperature profile was determined as shown in figure B-55. It was apparent from this data that a tubular thrust chamber made of SS was not practical as predicted wall temperatures exceeded the material allowable temperatures. Furthermore, chamber designs of tubular construction using copper based materials were studied for Category III engines (Section B7) and results showed that chamber pressures above 515 psi were not practical. It was therefore apparent that a chamber design using nontubular construction, as shown in figure B-56, was necessary.

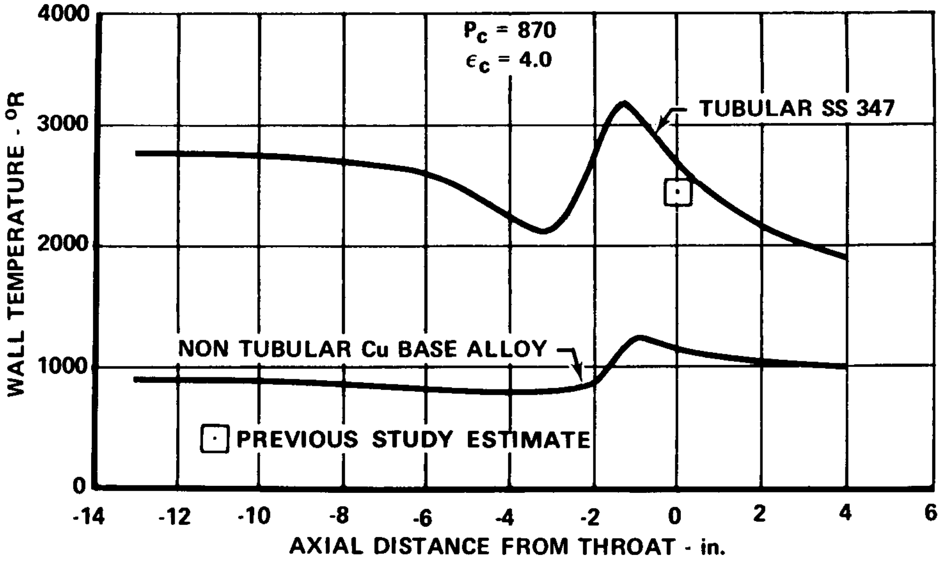


Figure B-55. RL10 Category IV Chamber Wall Temperatures FD 71359

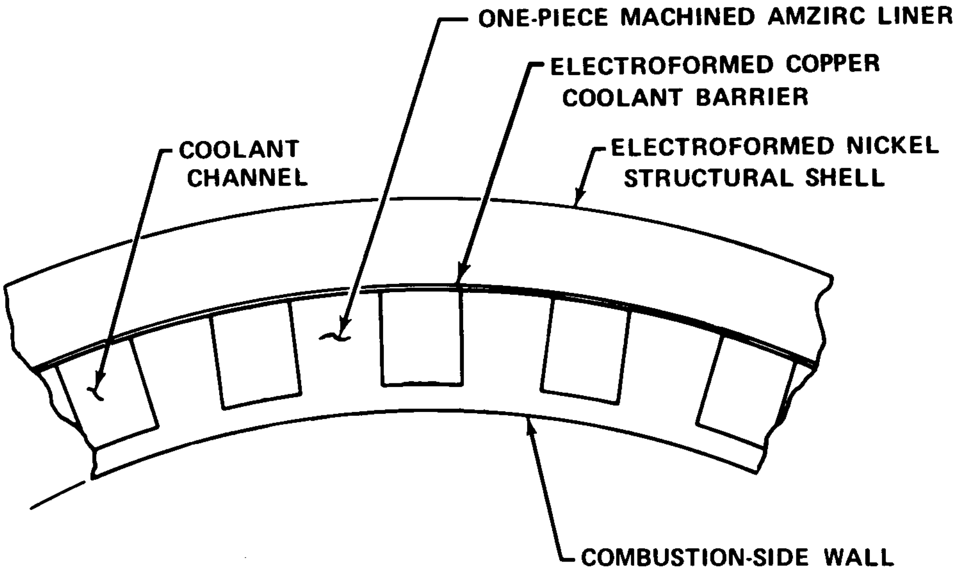

ONE-PIECE MACHINED AMZIRC LINER
ELECTROFORMED COPPER COOLANT BARRIER
COOLANT CHANNEL
ELECTROFORMED NICKEL STRUCTURAL SHELL
COMBUSTION-SIDE WALL

Figure B-56. Chamber Design Category IV Engine FD 68839

The nontubular thrust nozzle design shown uses the oxygen free copper and Zirconium alloy (AMZIRC). Axial cooling passages are milled in the copper thrust chamber OD and passages are closed with electrodeposited nickel as the strength-carrying member and outer wall. The nontubular construction begins at the injector face and terminates downstream of the throat at an area ratio of approximately six where the heat flux is low enough to allow the use of standard tubular construction.

A thermal analysis of the nontubular chamber showed that at an operating chamber pressure level of 870 psia, chamber wall temperature remained below the maximum allowable level of 1310°R established for copper based alloys in Section B7. The resultant predicted wall temperature characteristic is previously shown on figure B-55. Analysis of the internal cooling passage characteristics showed that the coolant pressure loss and temperature rise would be approximately 400 psia and 500°R, respectively.

8.1.1 Thrust Chamber Design Summary

The Category IV engine chamber nozzle should be capable of operation at chamber pressures of approximately 900 psia with a cycle life of approximately 160 cycles. Operation at this level will require the use of nontubular chamber configuration of copper based alloy material.

8.2 Turbopump Design Analysis

Since this is an advanced engine targeted to operate at high pressure levels, additional investigation was needed to assess turbopump performance and mechanical design limits. While performance levels were evaluated, analysis of various mechanical items associated with the increased speed and pressure predominate.

8.2.1 Critical Speed

Critical speeds of the Category IV turbopump were determined using lump-mass analysis based on the preliminary rotor cross sections. Bearing and bearing support spring rates were selected based upon previous analysis of similar hardware. Using this analysis method the following results were determined.

First critical mode for the oxidizer pump was predicted to be 48,000 rpm with a bearing support spring rate of 300,000 lb/in. This provides a 28% margin over the maximum shaft speed of 37,450 rpm. The fuel pump, with a bearing support spring rate of 200,000 lb/in., was predicted to have the 1st and 2nd critical modes (both bounce modes) at 49,000 rpm and 58,000 rpm, respectively. These are greater than 21% below the full thrust operations speed. The 3rd critical mode (178,000 rpm) is 140% over the maximum operating speed of 75,140 rpm.

8.2.2 Thrust Balance

The axial support for pump rotors is provided by self-compensating, thrust pistons. The thrust pistons utilize pump discharge flow and are sized for the maximum amount of unbalanced rotor thrust caused by differences in pump and turbine loads. To satisfy these unbalanced loads, the fuel and oxidizer pump thrust piston flows are estimated to be approximately 7.9% and 4.7% of total pump flow, respectively.

Turbopump axial load unbalance, thrust piston load capability and required flowrate were determined by scaling results obtained during the detailed analysis of similar turbopumps. The fuel pump thrust piston was estimated to have a 463-lb load capability and requires a flowrate of 0.35 lb/sec. The maximum fuel pump thrust unbalance was predicted to be 244 lb. The oxidizer pump thrust piston was estimated to have a 2900-lb load capability and require a flowrate of 1.3 lb/sec. The maximum thrust unbalance was predicted to be 2500 lb. The oxidizer turbopump unbalanced load is significantly higher than the hydrogen turbopump unbalance because the hydrogen pump has two impeller stages back to back, each tending to negate the other's axial thrust load. The oxidizer pump has only one impeller and the turbine offsetting load is relatively small.

8.2.3 Gears

Category IV engine main oxidizer and fuel pumps are driven by separate turbines but their relative rotational speed is synchronized by gearing the two pumps together. The gearing assures simultaneous breakaway at start and positive tracking during the transient.

Gear life is a critical item on the Category IV engine due to the high rotational speed of the main pump gears running in hydrogen. Since hydrogen offers no lubrication, the gears must be lubricated by a dry film lubricant. RL10A-3-3 engine experience has been obtained with a P&WA lubricant (PWA 61) containing molybdenum disulfide in a varnish binder. Recent laboratory tests have shown that newly developed molybdenum disulfide lubricants with antimony oxide (PWA 550) would provide twice the gear life of PWA 61. The gear life prediction system reflecting the increased life potential of the PWA 550 dry film lubricant, discussed in the Critical Element Evaluation of the Category III engine, was used for predicting the life of the Category IV engine gears.

The most highly loaded gear is the drive pinion for the synchronizer gear located on the hydrogen pump rotor shaft. This shaft rotates at 75,000 rpm which is equivalent to 4.5×10^7 cycles during 10.0 hr of operation. A maximum allowable design gear Hertz stress was estimated from figure B-57 to be 50,000 psi which is equivalent to 46 horsepower at 75,000 rpm. Since this allowable horsepower level is almost twice that needed to provide turbopump tracking, a 10-hr gear life should be obtainable.

8.2.4 Seals

The RL10A-3-3 turbopump includes face and split ring rubbing seals requiring a start inlet pressure of approximately 11 psia to overcome the turbopump breakaway torque. The Category IV turbopump assembly, which includes the fuel and oxidizer main pumps plus gear driven fuel and oxidizer low speed inducers, contains an increased number of gears and bearings, and requires an additional seal package in the oxidizer low speed inducer. In addition, because the Category IV engine is of a higher chamber pressure design, the torque available at start is reduced by over 40% for the same inlet conditions. Since the potential for drag is increased and the potential for starting torque is reduced, low torque floating ring and hydrodynamic face seals were selected in the oxidizer main and low speed inducer to ensure the ability to start at low inlet pressure.

8.2.5 Turbopump Performance

The Category IV engine design point generated during the previous study was based upon the use of Worthington curves to determine pump efficiency and the Balje method to determine turbine efficiency. A review of these performance prediction methods during the Critical Elements Evaluations concluded that, while the pump performance predictions were well within the required accuracy for this preliminary evaluation, turbine performance should be reviewed using a more rigorous analysis technique.

The oxidizer turbine analysis was conducted using a full admission turbine mean line design loss system. This system predicts the effects of turbine geometry and speed on profile loss, trailing edge loss, secondary flow loss and blade tip leakage. Geometry was selected to minimize the total overall loss. The analysis of the partial admission fuel turbine, considered the above losses in addition to partial admission leakage losses. These inter-row leakages are a major portion of the overall loss and accounted for in the partial admission turbine mean line design loss system which was used for this analysis.

Results of the design analysis showed that while the oxidizer turbine efficiency level originally predicted (77%) was confirmed the fuel turbine efficiency level would have to be reduced from approximately 71% to 66% as shown in table B-20.

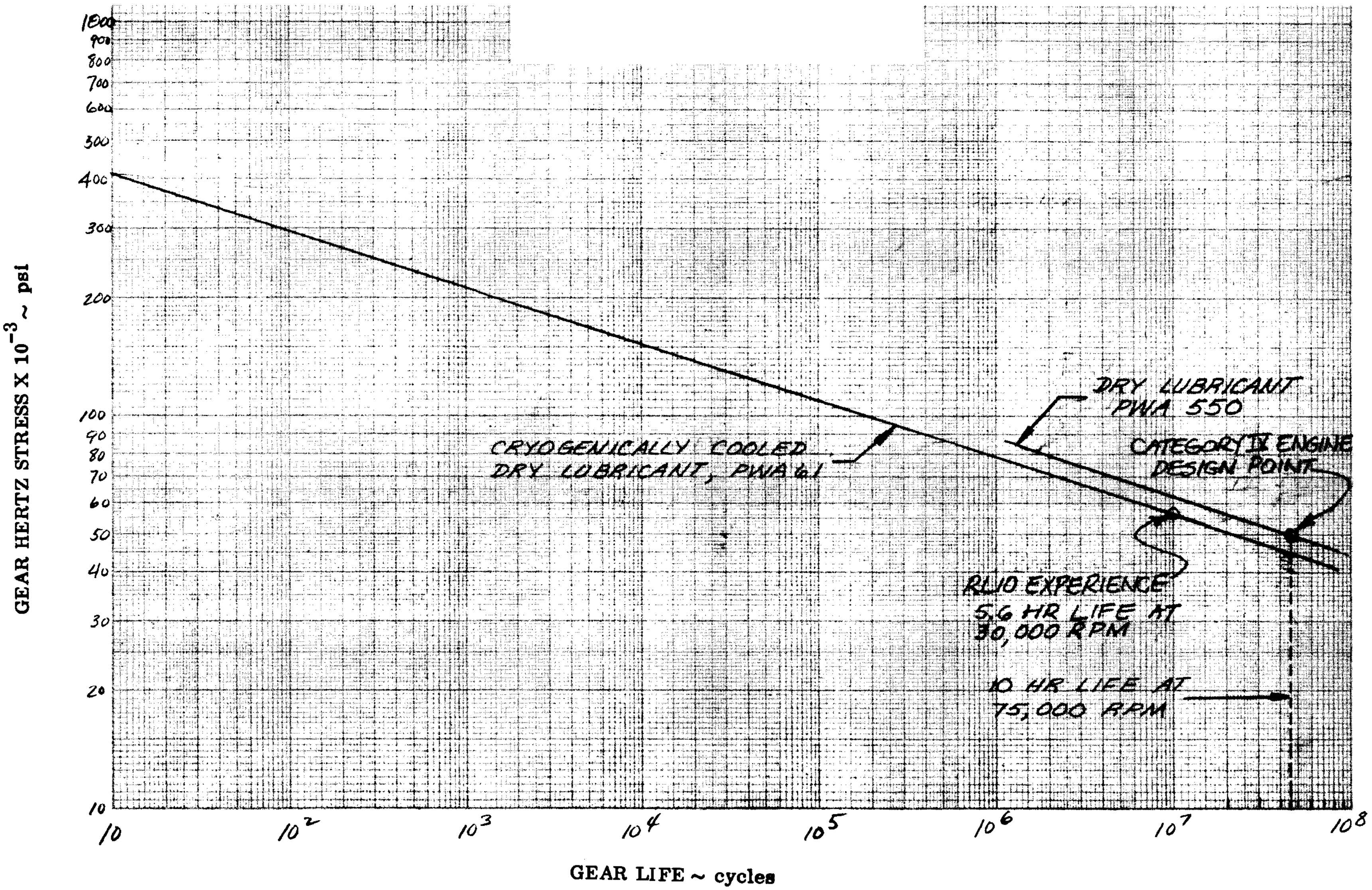


Figure B-57. Category IV Engine Gear Life

DF 98271

Table B-20. Category IV Turbine Performance

	Oxidizer		Fuel	
Parameter	Previous Study	Critical Analysis	Previous Study	Critical Analysis
Inlet Flowrate, lb/sec	4.234	4.234	4.234	4.234
Inlet Total Pressure, psi	1,086	1,086	1,767	1,767
Inlet Total Temperature, °R	493	493	535	535
Inlet Flow Parameter, $W\sqrt{T}/P$	0.865	0.865	0.0554	0.0554
Number of Stages	1	1	2	2
Mean Diameter, in.	4.60	5.26	3.44	3.44
Percent Admission	100	100	64.5	64.5
Blade Height, in.	0.32	0.32	0.32	0.32
Rotor Speed, rpm	37,380	37,380	75,030	75,030
Turbine Work, Btu/lb	39.2	39.2	163	131/32.2
Efficiency, T-T/T-S	/77.0	79.5/77.1	/70.9	66.6/65.4
Mean Isentropic u/c, T-T/T-S	/0.470	0.546/0.538	/0.469	0.454/0.451
Pressure Ratio, T-T/T-S	/1.107	1.109/1.113	/1.547	1.57/1.61
Reaction, Root/Mean	-	27%/34%	-	10% (First Stage)
Axial Chord, Vane/Blade	-	0.30/0.30	-	0.30/0.30
Number of Foils, Vane/Blade	-	42/64	-	32, 114/62, 32
Vane Mean Abs Gas Angle, Inlet/Exit, deg	-	90.0/14.7	-	90/13.1
Blade Mean Abs Gas Angle, Inlet/Exit, deg	-	41.4/19.6	-	22.2/17.4
Second Vane Mean Abs Gas Angle, Inlet/Exit, deg	-	NA	-	39.6/30.8
Second Blade Mean Rel. Gas Angle, Inlet/Exit, deg	-	NA	-	118/22.6

8.3 Engine Cycle Analysis

After evaluation of the critical Category IV engine components (chamber/nozzle and turbopump designs), the design point cycle was updated by including the new component design characteristics into the cycle. The most significant changes were: (1) the use of a nontubular copper chamber rather than the original stainless steel tubular design which resulted in an increase in both nozzle coolant pressure loss and nozzle coolant temperature rise; (2) a reduction in fuel turbine efficiency due to a more thorough analysis of the partial admission design; (3) an increase in the flowrate required to cool the secondary nozzle; and (4) an increase in engine installed length to improve interchangeability between the Category IV and Derivative II engines. Modified pump characteristics which would permit pumping at full thrust with two-phase propellants were also included. The addition of this capability had little influence on the cycle power balance because of the relatively low power requirements of the low speed inducers.

The cycle program was updated with the new component characteristics, and a new design point was generated. A comparison of some of the more significant design point characteristics with those defined at the time of the previous study (Reference 1) is shown in table B-21. A detailed cycle sheet for the updated cycle is also presented in figure B-58. The maximum obtainable chamber pressure increased from 870 to 905 psia. This increase occurred because the cycle power losses from the higher nozzle coolant pressure losses and decreased fuel turbine efficiency were more than offset by the increased nozzle coolant temperature rise. These changes in nozzle pressure loss and temperature rise also cause an increase in fuel discharge pressure. The increased secondary nozzle coolant flow caused the chamber mixture ratio to shift from 6.15 to 6.43. Nozzle area ratio was increased because of the increased engine length and increased chamber pressure. This area ratio increase is the primary reason for the higher specific impulse.

8.4 Conclusion

It is feasible to expect that an advanced expander cycle engine can operate at approximately 900 psia with a life capability of 160 cycles and 10 hr. The design of such an engine must be based upon the use of nontubular chamber construction of copper based alloy materials.

Table B-21. Category IV Engine Critical Element Evaluation Results

	Previous Study	Updated
Thrust, lb	15,000	15,000
Overall Mixture Ratio	6.0	6.0
Chamber Mixture Ratio	6.15	6.43
Area Ratio (Primary/Overall)	125/350	160/435
Specific Impulse (JANNAF), sec	471.3	473.4
Chamber Pressure, psia	870	905
Fuel Turbopump Speed, rpm	75,000	75,000
Fuel Pump Discharge Pressure, psia	2,125	2,515
Oxidizer Turbopump Speed, rpm	37,400	38,700
Oxidizer Pump Discharge Pressure, psia	1,250	1,300
Main Chamber ΔP, psi	316	400
Main Chamber ΔT, °R	461	500
Engine Inlet NPSH, Oxidizer/Fuel, ft	2/15	2-Phase Capability
Engine Length, in. (Retracted/Extended)	54/106	57/114
Engine Diameter, in.	37/62	41/68

```
-----------------------------------------------------------------------
                RL10 EXTENSION OFF-DESIGN DECK
-----------------------------------------------------------------------
     CATEGORY IV BASE CASE
-----------------------------------------------------------------------


                     INLET CONDITIONS
     ***********************************************
              FUEL                              LOX
     PRESSURE       16.00             PRESSURE       16.00
     TEMP           36.9              TEMP           163.8
     NPSP           0.0               NPSP            0.0


  FUEL LSI           LOX LSI            FUEL PUMP          LOX PUMP
***************   ***************    ***************    ***************
SPEED    30347.   SPEED      3000.   SPEED     75000.   SPEED      38666.
FLOW       4.52   FLOW       27.14   FLOW       4.524   FLOW       27.142
POWER       7.0   POWER        1.9   INLET GPM  459.4   POWER       180.6
EFF      0.6446   EFF       0.6538                      EFF         .6933
DISCH P   33.08   DISCH P    28.10                      INLET P      27.8
RHO IN    4.404   RHO IN    70.894   *  1ST  STAGE  *   INLET T     164.0
                                     POWER      610.2   DISCH P    1300.3
                                     EFF       0.5658   DISCH T     171.7
 FUEL TURBINE       LOX TURBINE      INLET P    32.96   RHO IN     70.735
***************   ***************    DISCH P   1272.2   RHO OUT     70.79
FLOW      4.012   FLOW       4.012   DISCH T     57.1   INLET GPM   172.3
POWER    1219.7   POWER      218.0   RHO IN     4.423
EFF       .5757   EFF       0.7755   RHO OUT    4.305
INLET P  2067.9   INLET P   1121.2
INLET T   583.3   INLET T    527.2   *  2ND  STAGE  *
DIS P(S) 1180.2   DIS P(S)  1022.7   POWER      634.5
DELH ACT  215.0   DELH ACT    38.4   EFF        .5658
M. VEL R   .470   M. VEL R    .470   INLET P   1260.5
ACD        .347   ACD        1.058   DISCH P   2513.9
PCT  HP    96.9   PCT  HP    115.6   DISCH T     83.3
HP TRANS    25.   P/P        1.096   RHO OUT    4.170
P/P       1.752


 FUEL INJECTOR      LOX INJECTOR     *************************************
***************   ***************    *                                   *
DELTA P    86.1   DELTA P    135.5   *  MIXTURE RATIO            6.000   *
INLET P   991.2   INLET P   1040.6   *  THRUST                  15000.   *
INLET T   521.5   INLET T    172.6   *  IMPULSE                 473.40   *
ACD       1.271   ACD         .398   *  CHAMBER PRESSURE        905.11   *
                  RHO       70.374   *                                   *
                                     *************************************
```

Figure B-58(1). Category IV Engine Cycle

```
       JACKET         LEAKAGE & BLEED     RM CONTROL VLV      THRUST CONTROL
****************    ****************    ****************    ****************
FLOW          4.22  WLEAK        0.300  DELTA P      259.7  ACD          0.0503
INLET P     2490.7  WT/P-FUEL    0.0    ACD           .299  WTBY/WF        5.02
INLET T       83.3  WT/P-LOX     0.0    K FACTOR     24.99  WTBY          0.212
DELTA PJ     399.8                                          P/P           2.108
DELTA TJ     501.6


SYSTEM PRESSURE LOSSES
************************
OB/P DIS LINE        0.3
FB/P DIS LINE        0.1
PUMP INTR STG       11.7
PUMP DIS LINE       23.2
CAV VENTURI          0.0
JAC IN LINE          0.0

JAC DIS LINE         0.0
FUEL TURB IN        22.8
FUEL TURB DIS       40.2
FUEL INTR LINE      18.8
OX TURB IN           0.0
OX TURB DIS         31.5
INJ IN LINE          0.0
```

Figure B-58(2). Category IV Engine Cycle

SECTION C
BASELINE ENGINE DESIGN

1. SELECTED ENGINE OPTIONS

Three baseline RL10 Derivative engine configurations were selected by NASA/MSFC to be carried into the Design Definition phase of this study. The primary considerations used in making selections were the results of the Critical Elements Evaluation and the operational requirements for the Space Tug as they were defined at that time. The features assigned to each of the three engines were chosen because of their potential application in the Space Tug vehicle. The three engines selected for baseline evaluation were designated the Derivative IIA and IIB and Category IV engines. The specific features assigned to each of the baseline engines are herein defined.

1.1 RL10 Derivative IIA Engine

The Derivative IIA engine was to be derived from the RL10A-3-3 engine, with increased performance and operating flexibility for use in the Space Tug. With a nominal full thrust level of 15,000 lb (in vacuum) at a mixture ratio of 6.0:1, the Derivative IIA engine is defined as the RL10A-3-3 engine with the following changes:

- Add two-position nozzle and recontour primary section to give a large increase in specific impulse with no increase in engine installed length. Engine installed length is therefore, limited to 70 inches. With a truncated two-position nozzle installed, this engine has to be able to be installed and tested in the existing test facilities at FRDC

- Reoptimize RL10A-3-3 injector for operation at a full thrust mixture ratio of 6.0:1

- Add tank head idle mode (THI) of operation. THI is pressure fed mode without turbopump rotation. Propellants supplied from the vehicle tanks at saturation pressure. Propellant conditions at the engine inlets can vary from superheated vapor, through mixed phase, to liquid. The objectives are to supply a low thrust to settle propellants and also to obtain useful impulse from the propellants used to condition the engine and vehicle feed systems

- Add maneuver thrust (MT) mode of operation. MT provides low thrust in pumped mode, without significant impact on the engine's design.

- Add two-phase pumping capability. Allows operation at both full and maneuver thrust levels with saturated propellants in the vehicle tanks and without tank pressurization system or vehicle mounted boost pumps

- Add capability for both H_2 and O_2 autogenous pressurization. May be required on very long burn planetary missions in order to avoid excessively low propellant vapor pressure.

1.2 RL10 Derivative IIB Engine

The RL10 Derivative IIB is similar to the Derivative IIA engine except that it does not have the requirement for two-phase pumping capability at full thrust. The RL10 Derivative IIB is defined as the RL10A-3-3 engine with the following changes:

- Two-position nozzle, with recontoured primary section
- Reoptimized injector
- Tank head idle mode
- Pumped idle mode, with saturated propellants in vehicle tanks, and bootstrap autogenous pressurization. This mode of operation allows the RL10A-3-3 Bill-of-Material turbopump to be run at a low enough speed that pressurization to subcool the propellants at the pump inlets is not required in pumped idle. Then, by using the engine's boostrap autogenous pressurization capability, the tanks can be prepressurized to satisfy the engine's full thrust pump inlet NPSH requirements prior to acceleration to full thrust.

1.3 RL10 Category IV Engine

The RL10 Category IV engine uses the expander cycle and much of RL10 technology, but it is a "clean sheet" design to provide maximum Space Tug capability. The engine, as defined below, provides the same operational features as the Derivative IIA engine and is constrained by the same test facility and installed length constraints.

- 15,000 lb thrust, mixture ratio of 6.0
- Two-position nozzle
- Tank head idle mode
- Maneuver Thrust
- Two-phase pumping capability
- Autogenous pressurization capability

2. ENGINE TECHNICAL DATA SUMMARY

2.1 Introduction

The three RL10 derivative engines which were defined during the design definition phase of this study are all regeneratively cooled, turbopump fed engines with a rated vacuum thrust level of 15,000 lb. Engine power is derived from the expander cycle in which fuel, heated by regeneratively cooling the primary nozzle and thrust chamber, is expanded through the turbine(s) to drive the pumps. All three engines are designed for operation at three power levels, tank head idle, maneuver thrust (pumped idle), and full thrust. The engine propellants are oxygen and hydrogen which are consumed at a nominal mixture ratio of 6.0:1 at full thrust. Off-design operational capability of ±0.5 mixture ratio units is also available at full thrust for propellant utilization purposes. These engines are designed such that the mechanical connections between the engine and vehicle are the same where possible, to provide maximum interchangeability between engines.

The expander cycle is particularly suitable for a Space Tug engine, because it has inherent high performance, since all propellants are burned in the thrust chamber before being expanded through the nozzle, and high reliability and life, since the turbine operates at approximately room temperature. No active closed-loop control system is necessary for safe engine operation, even for engines with three different operating modes. Purges are not required to ensure safe engine start or shutdown and are only needed to prevent moisture contamination of the engine between missions. Electrical control power and helium for valve actuation are supplied by the Space Tug vehicle.

The technical data presented in this subsection is a summary of the mechanical and operational data contained in Volume III (Interface Control Document) and Section C.4 (Mechanical Concept Definition).

2.1.1 Basis for Engine Technical Data

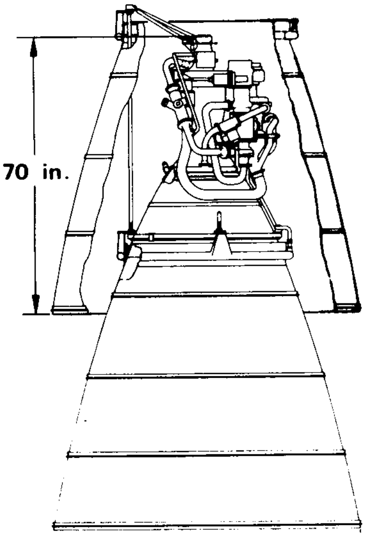
Engine specific impulse is obtained by using JANNAF methodology and bias correction to agree with RL10A-3-3 test data.

The engine dry weight includes instrumentation, solenoid and tank pressurization valves. It does not include TVC actuators, PU actuator, accessory drive equipment or nonpropulsive vents.

Life and operating limits are based on engine system and component experimental data for existing components. All new components are designed to meet a life of 10 hours/300 firings.

2.2 Derivative IIA Engine

2.2.1 Engine Description



THRUST : 15,000 lb
CHAMBER PRESSURE : 400 psia
AREA RATIO : 66.2/262
I_{sp} : 459 sec AT 6.0 MR
OPERATION : FULL THRUST (SATURATED PROPELLANTS)
: MANEUVER THRUST (SATURATED PROPELLANTS)
CONDITIONING : TANK HEAD IDLE
WEIGHT : 513 lb
LIFE : 190 FIRINGS/5 HOURS
ENVELOPE : LENGTH 70/127 in.
: NOZZLE EXIT DIAMETER 40/79.6 in.

FD 74122A

Ⓓ

The Derivative IIA engine principal components and their arrangement are shown schematically in figure C-1. To enable pumping of two-phase propellants the engine is configured with a large diameter fuel high speed inducer and a gear driven oxidizer low speed inducer. A GO_2 heat exchanger is utilized to simplify engine operation in THI and provide oxidizer autogenous pressurization capability. Solenoid valves and control valves give the engine its capability to operate in three thrust modes. A dual ignition system gives improved ignition reliability in THI. The nozzle is contoured for maximum performance in a fixed engine length and a jackscrew operated two-position dump cooled extendible nozzle reduces stowed engine length. The primary nozzle exit diameter is fixed at 40 in., since this is the limiting diameter for the extendible nozzle to be retracted over the engine's power head and is also the largest size which allows installation with a truncated extendible nozzle in E-6 and E-7 test stands at FRDC. The injector is optimized to give improved performance at 6.0 mixture ratio.

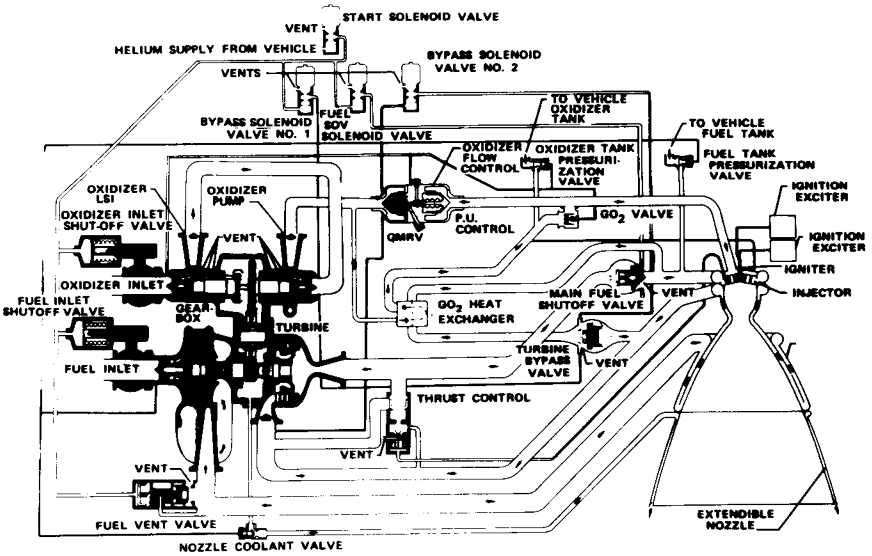

START SOLENOID VALVE
VENT
HELIUM SUPPLY FROM VEHICLE
BYPASS SOLENOID VALVE NO. 2
VENTS
TO VEHICLE OXIDIZER TANK
BYPASS SOLENOID VALVE NO. 1
FUEL SOV SOLENOID VALVE
TO VEHICLE FUEL TANK
OXIDIZER FLOW CONTROL
OXIDIZER TANK PRESSURIZATION VALVE
FUEL TANK PRESSURIZATION VALVE
IGNITION EXCITER
OXIDIZER LSI
OXIDIZER PUMP
OXIDIZER INLET SHUT-OFF VALVE
VENT
P.U. CONTROL
GMRV
GO$_2$ VALVE
IGNITION EXCITER
IGNITER
OXIDIZER INLET
MAIN FUEL SHUTOFF VALVE
VENT
INJECTOR
FUEL INLET SHUTOFF VALVE
GEAR-BOX
GO$_2$ HEAT EXCHANGER
TURBINE
TURBINE BYPASS VALVE
VENT
FUEL INLET
THRUST CONTROL
VENT
VENT
FUEL VENT VALVE
EXTENDIBLE NOZZLE
NOZZLE COOLANT VALVE

Figure C-1. RL10 Derivative IIA Propellant Flow Schematic FD 74112A

The dry weight of the engine and its subassemblies is summarized in the following table. Of the total 513 lb engine weight, 31% is weight of existing hardware, 58% is calculated from layout drawings and 11% is estimated.

RL10 Derivative IIA Engine Weight

Turbopumps and Gearbox	99 lb
Thrust Chamber and Primary Nozzle	110 lb
Extendible Nozzle Actuation System	43 lb
Extendible Nozzle	104 lb
GO_2 Heat Exchanger	13 lb
Controls, Valves and Actuators	83 lb
Plumbing and Miscellaneous Hardware	46 lb
Ignition System	15 lb
TOTAL	513 lb

Installation drawings of the Derivative IIA engine are shown in figure C-2.

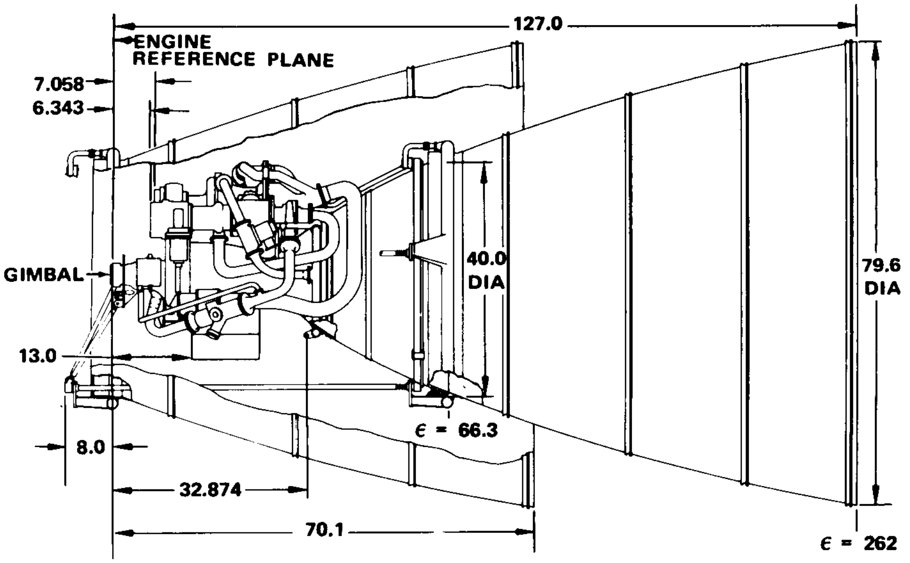

127.0
ENGINE
REFERENCE PLANE
7.058
6.343
GIMBAL
13.0
8.0
32.874
70.1
40.0
DIA
ε = 66.3
79.6
DIA
ε = 262

Figure C-2(1). Derivative IIA Engine Installation Drawing FD 72055B

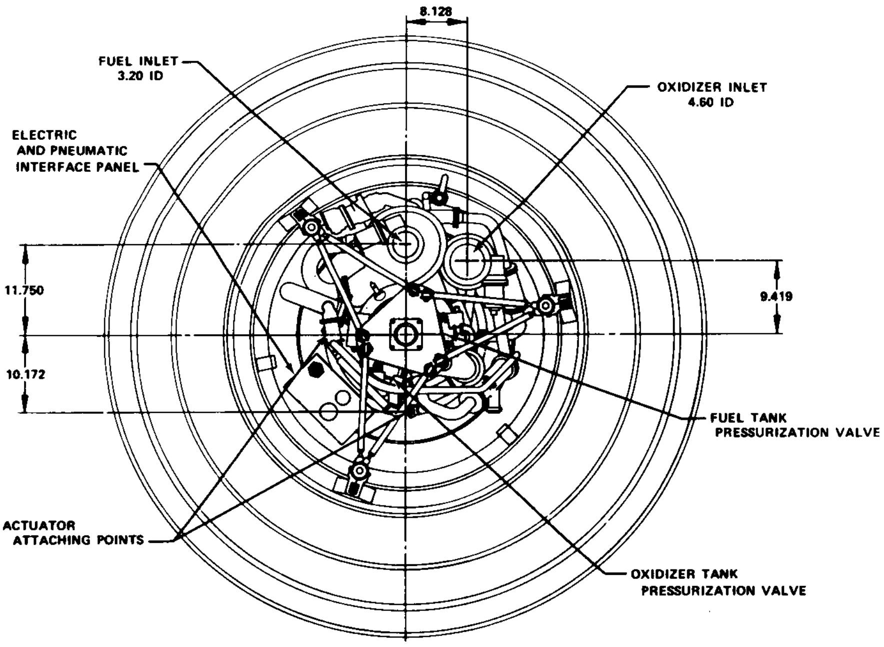

8.128
FUEL INLET
3.20 ID
OXIDIZER INLET
4.60 ID
ELECTRIC
AND PNEUMATIC
INTERFACE PANEL
11.750
10.172
9.419
FUEL TANK
PRESSURIZATION VALVE
ACTUATOR
ATTACHING POINTS
OXIDIZER TANK
PRESSURIZATION VALVE

Figure C-2(2). Derivative IIA Engine Installation Drawing FD 72077B

2.2.2 Component Description

a. Turbopump Assembly

The turbopump assembly illustrated in figure C-3 consists of (1) a liquid hydrogen pump and a hydrogen powered impulse turbine mounted on a common main shaft, (2) a liquid oxygen pump mounted beside and driven by the hydrogen pump, and (3) an oxygen low speed inducer which is mounted in line with and geared off the oxygen pump shaft. All gears and bearings in the assembly are hydrogen or oxygen cooled.

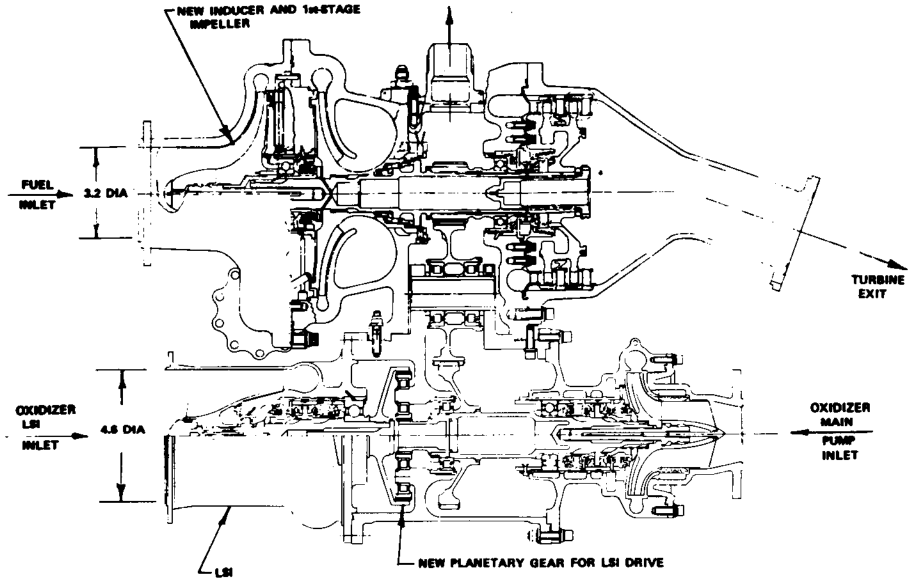


Figure C-3. Derivative IIA Engine Turbopump Assembly FD 76009

The fuel pump is an RL10A-3-3 engine design modified to provide two-phase pumping capability. It is a two-stage centrifugal pump, with back-shrouded impellers mounted back-to-back. A three-bladed axial flow high speed inducer located upstream of the first-stage impeller is designed to provide two-phase pumping capability. Recovery of impeller velocity head is accomplished in a single straight conical diffuser connected to a volute collector.

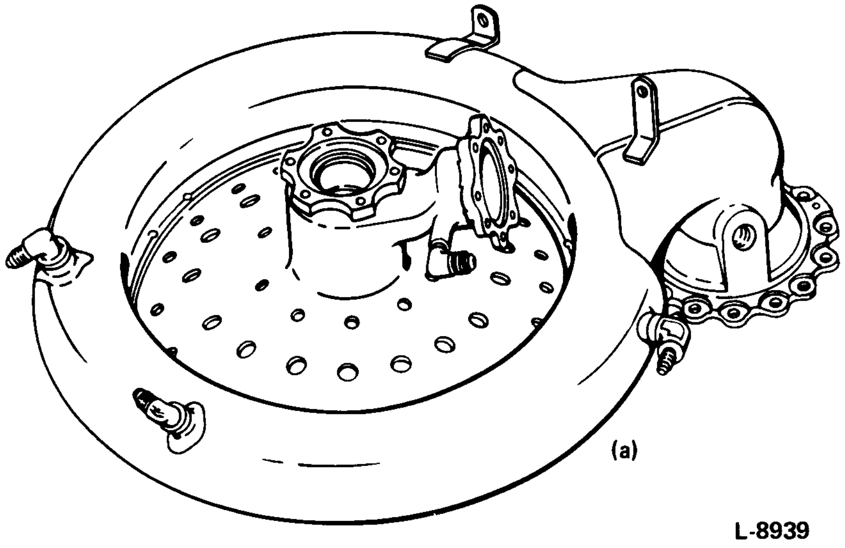
The oxidizer pump is of the same design as the RL10A-3-3 engine oxidizer pump. It is a fully shrouded, single-stage centrifugal pump. A three-bladed, axial flow, partially shrouded high speed inducer is located upstream of the impeller. Impeller velocity head recovery is accomplished, as in the fuel pump, through a single conical diffuser and volute collector.

The oxidizer low speed inducer is a three-bladed axial flow pump driven from the oxidizer pump shaft through a planetary gear system. It is designed to provide two-phase pumping capability.

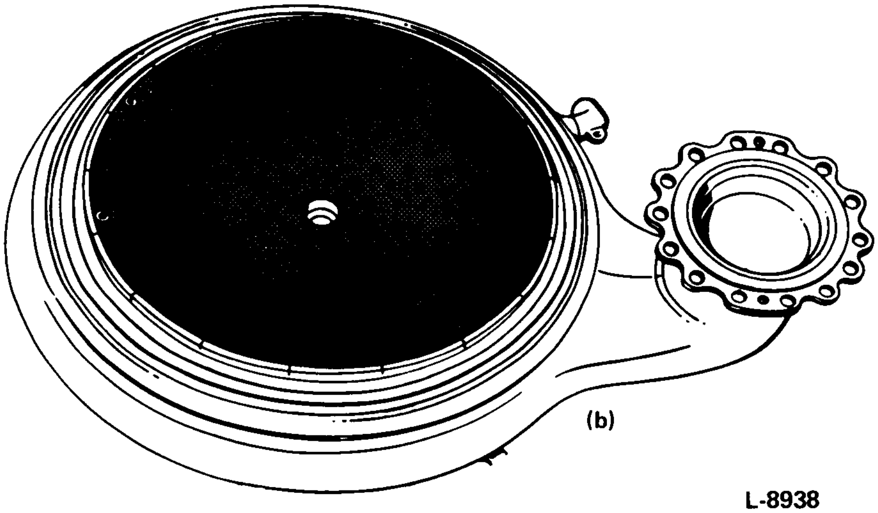
The turbine is a pressure compounded, full-admission, two-stage design with exit guide vanes to minimize discharge swirl losses. Both blade stages are fully shrouded.

b. Propellant Injector

The propellant injector shown in figure C-4 consists of 216 elements arranged in eight equally spaced concentric circles. Each element consists of an oxidizer orifice and a concentric fuel orifice, and all elements except those in the inner and outer rows incorporate swirlers which aid in the dispersion of the oxidizer. Approximately 5% of the hydrogen flow is used to transpiration cool the injector face.



Typical - Front View



Typical - Rear View

Figure C-4. Propellant Injector FD 47660

c. Primary Nozzle and Thrust Chamber

The primary nozzle and thrust chamber shown in figure C-5 is a regeneratively cooled, furnace brazed assembly consisting of a fuel inlet manifold, 180 short, single-tapered tubes, a turnaround manifold, 180 full length, double-tapered tubes, a fuel exit manifold, and various stiffeners and component supports. It provides a chamber of converging-diverging design for the combustion and expulsion of propellants at high velocity to produce thrust, and it serves as a heat exchanger to supply turbine power for the propellant pumps.

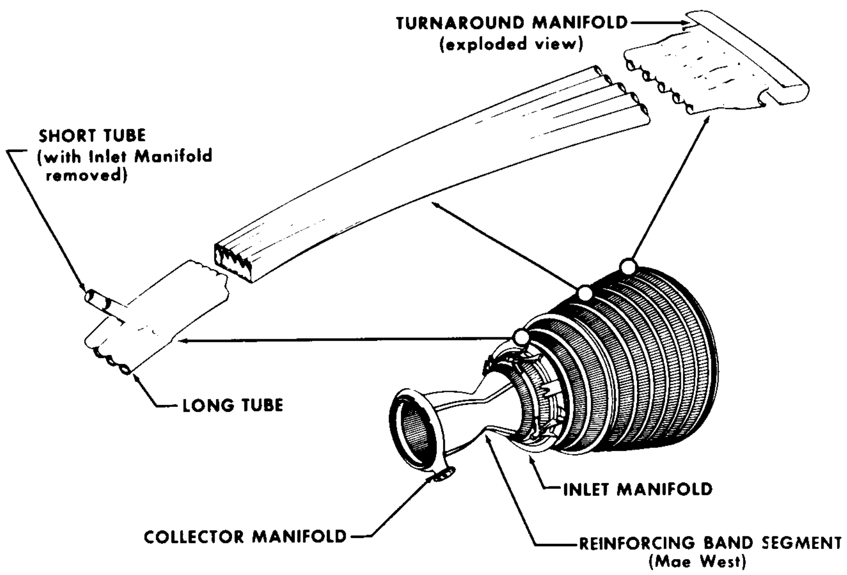


Figure C-5. Thrust Chamber Construction FD 4269

Hydrogen coolant flow enters the inlet manifold downstream of the throat, and immediately flows into 180 single-tapered short tubes interleaved between 180 double-tapered, full length tubes. The full length tubes form the full periphery of the combustion chamber, throat, and nozzle down to the junction of the short tubes. The periphery of the remainder of the nozzle is formed by all 360 tubes. The hydrogen flows aft in the short tubes to the turnaround manifold where it enters the 180 full-length tubes and then travels forward the entire length of the chamber to the exit manifold.

d. Extendible Nozzle

The extendible nozzle shown in figure C-6 provides increased specific impulse with no increase in installed engine length. The dump cooled extendible nozzle is formed by a smooth outer skin and a corrugated inner skin that are resistance seam welded longitudinally. The corrugations form coolant passages for hydrogen which enters at an inlet manifold located at an engine area ratio of 66 and discharges to the atmosphere after passing through exit nozzles formed by dimples in the corrugations at an overall engine area ratio of 262. The extendible nozzle coolant supply originates at the turbomachinery gearbox and is supplied to the inlet manifold of the extendible nozzle by a quick disconnect feed valve.

The nozzle is translated by means of a jackscrew actuation system, which consists of three ballscrew jackshafts which are attached on the aft end of the primary nozzle by individual drive gearboxes and bearing assemblies, and supported at their forward end by an adjustable link. The nozzle drive/synchronization is provided by two redundant electric motors and three interconnecting flexible cables which transmit motor torque to three gear transmissions which drive the ballscrew shafts. The interface between the primary nozzle and extendible nozzle is sealed by the use of finger leaf seals.

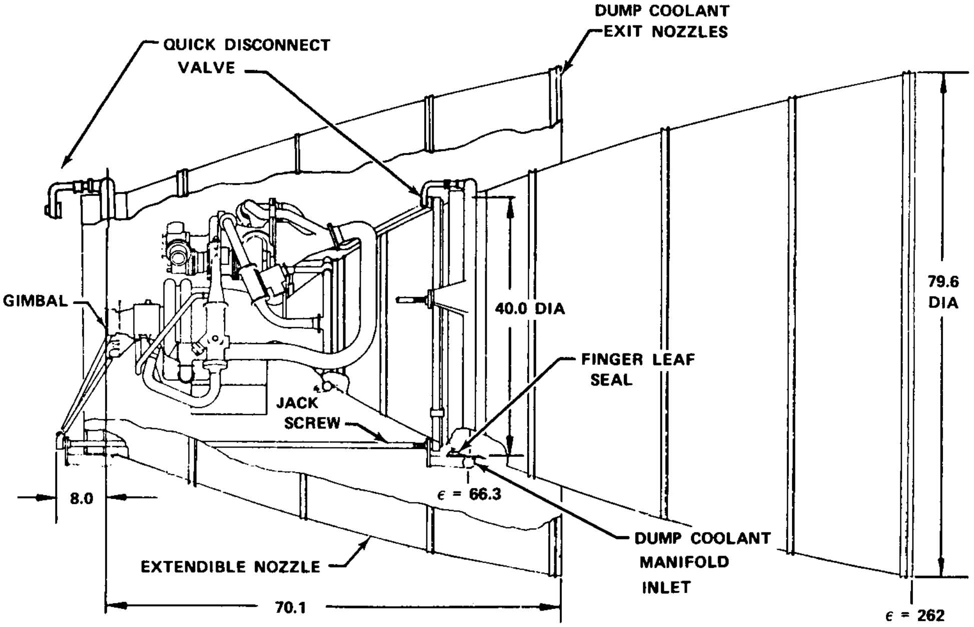

DUMP COOLANT EXIT NOZZLES
QUICK DISCONNECT VALVE
GIMBAL
40.0 DIA
79.6 DIA
FINGER LEAF SEAL
JACK SCREW
8.0
ϵ = 66.3
DUMP COOLANT MANIFOLD INLET
EXTENDIBLE NOZZLE
70.1
ϵ = 262

Figure C-6. Derivative IIA and IIB Engines Extendible Nozzle FD 72043C

e. Oxygen Heat Exchanger

The oxygen heat exchanger shown in figure C-7 is a compact laminated heat exchanger assembly. It is a single-pass hydrogen, double-pass oxygen cross flow heat exchanger employing double wall separation for safety, and copper fins for increased heat transfer. It gasifies all of the oxygen flow during tank head idle operation and provides gaseous oxygen for autogenous tank pressurization during pumped engine operation.

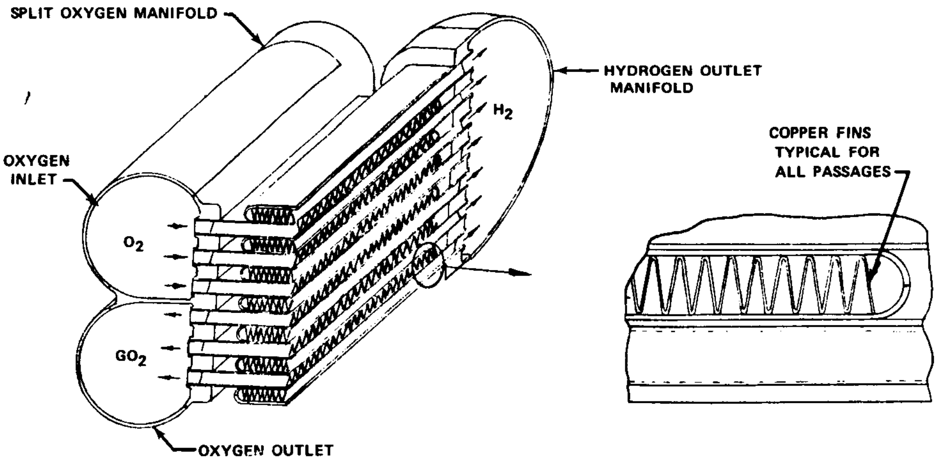

SPLIT OXYGEN MANIFOLD
HYDROGEN OUTLET MANIFOLD
H_2
COPPER FINS TYPICAL FOR ALL PASSAGES
OXYGEN INLET
O_2
GO_2
OXYGEN OUTLET

Figure C-7. Gaseous Oxidizer Heat Exchanger Design Derivative IIA Engine FD 76010

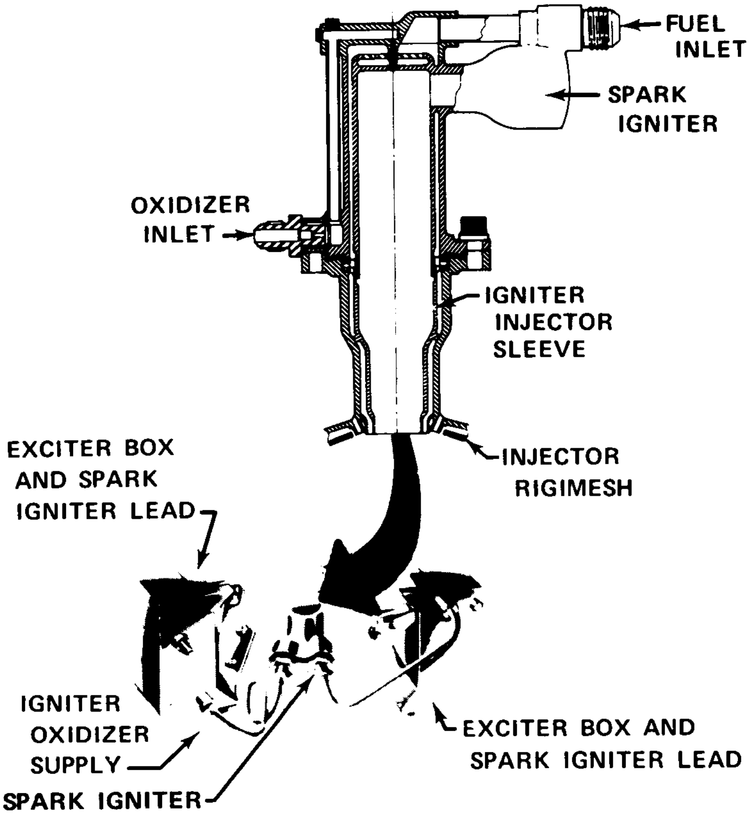

FUEL INLET
SPARK IGNITER
OXIDIZER INLET
IGNITER INJECTOR SLEEVE
EXCITER BOX AND SPARK IGNITER LEAD
INJECTOR RIGIMESH
IGNITER OXIDIZER SUPPLY
SPARK IGNITER
EXCITER BOX AND SPARK IGNITER LEAD

Figure C-8. Dual Ignition System FD 47508A

f. Control System

The engine control system previously shown schematically in figure C-1 is composed of the following components.

(1) Start Solenoid, Bypass Solenoid No. 1, Bypass Solenoid No. 2, and Fuel SOV Solenoid Valves

These identical RL10A-3-3 engine valves are solenoid actuated, direct acting, 3-way valves with double-ended poppets. Actuated, the solenoid valves close the overboard vent port and open the helium supply inlet port providing helium for engine valve actuation. When deactivated the helium inlet supply port is closed and the overboard vent port is opened allowing helium (actuating the engine valves) to vent overboard.

(2) Fuel and Oxidizer Inlet Shutoff Valves

The fuel and oxidizer inlet shutoff valves are spring-loaded, normally closed, helium operated, rank and pinion actuated, two-position ball valves which provide a shutoff between the vehicle propellant lines and the engine turbopumps when the engine is not in operation. A double seal configuration is used to minimize leakage.

(3) Fuel Vent Valve

This RL10A-3-3 engine valve is a spring-loaded, two-position, normally open, helium actuated sleeve valve. The valve is used to relieve fuel system pressure at shutdown. Helium pressure closes the valve at start and it remains closed until the helium is vented at shutdown.

(4) Turbine Bypass Valve

The turbine bypass valve is a spring-loaded, normally closed, three-position, bullet type annular gate valve. The valve meters fuel turbine bypass flow to set tank head idle and maneuvering thrust levels. The valve is actuated by helium pressure in THI and hydrogen pressure during maneuvering thrust operation.

(5) Nozzle Coolant Valve

The nozzle coolant valve is a spring-loaded, normally closed, line pressure activated, poppet valve. It is located at the gearbox vent and supplies the hydrogen coolant flow from the gearbox to the dump cooled extendible nozzle.

(6) Main Fuel Shutoff Valve

This RL10A-3-3 engine control is a spring-loaded, normally closed, helium operated, two-position annular gate valve. The valve prevents fuel from flowing through the turbine while the engine is operating at tank head idle, and it provides a rapid cutoff of fuel flow to the combustion chamber at engine shutdown.

(7) Fuel and Oxidizer Tank Pressurization Valves

The tank pressurization valves are spring-loaded, normally closed, two-position, line pressure operated, poppet type valves. Both valves are identical in design and they allow gaseous propellants to be bled from the fuel injector and the oxygen heat exchanger discharge for autogenous tank pressurization during pumped engine operation.

(8) GO_2 Valve

The GO_2 valve is a spring-loaded, normally open, line pressure activated, sleeve valve. The valve is open only during tank head idle operation. It is closed by the increasing GO_2 heat exchanger discharge pressure during the acceleration to pumped idle.

(9) Oxidizer Flow Control

The oxidizer flow control is a spring-loaded, normally closed, line pressure activated valve. The oxidizer flow control is divided into two functional sections. The forward section contains two spring-loaded poppet valves used to meter oxidizer flow during maneuver thrust and full thrust. The aft section contains a pintle valve used for propellant utilization control. During tank head idle operation, the valve is closed.

(10) Thrust Control

This RL10A-3-3 engine control is a normally closed, servo operated, closed loop, variable-position valve used to control full engine thrust by regulation of turbine bypass flow. The control senses chamber pressure and regulates turbine bypass flow. This changes turbine power and speed which in turn regulates combustion chamber pressure. A secondary function of the control is to limit engine thrust overshoot during the transient from maneuver thrust to full thrust. This is accomplished with a reference pressure lag system allowing the turbine port to open early in the start transient reducing turbine power prior to attainment of steady-state chamber pressure.

g. Ignition System

The ignition system consists of dual, one-half joule electrical spark systems installed in a torch igniter chamber shown in figure C-8. The hydrogen cooled igniter combustion chamber is supplied gaseous oxygen from the GOX heat exchanger discharge and gaseous hydrogen from the fuel injector.

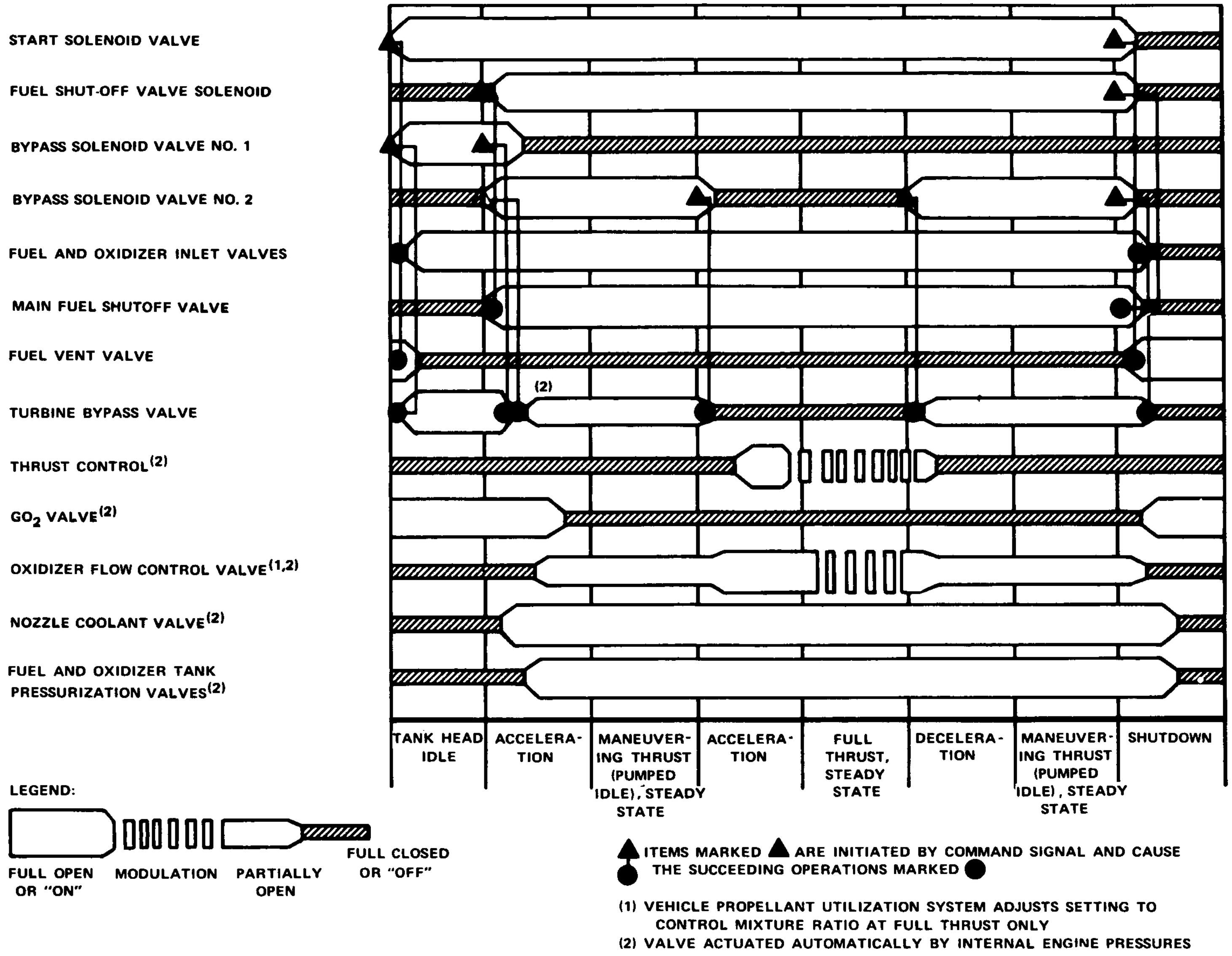
2.2.3 Operation and Performance Characteristics

Operation of the engine is achieved through sequencing of the control solenoids by the application and removal of vehicle supplied electric power. The operational sequence of the Derivative IIA engine is shown in figure C-9.

The Derivative II engine can be started in tank head idle mode with propellants supplied in vapor, mixed, or liquid phases. Engine start is initiated when electrical power from the vehicle energizes the start solenoid valve and bypass solenoid No. 1 to allow helium to open the propellant inlet shutoff valves and turbine bypass valve and close the fuel vent valve. The fuel shutoff, nozzle coolant, oxidizer flow control, and tank pressurization valves remain closed whereas the GO_2 valve remains open. The valves remain fixed in these positions until pump cooldown is complete and the vehicle signal is given for the engine to accelerate to the maneuvering thrust level.

With the inlet shutoff valves open, fuel flows through the pumps, thrust chamber cooling jacket, turbine bypass valve and the GO_2 heat exchanger before passing through the fuel injector into the combustion chamber. Simultaneously, oxidizer flows through the oxidizer pump, GO_2 heat exchanger and GO_2 valve before entering the oxidizer injector. Since the oxidizer flow control valve and the main fuel shutoff valves are closed during THI, all the oxidizer flow passes through the GO_2 heat exchanger system and all of the fuel flow bypasses the turbine. The engine operating conditions shown in figure C-10 were predicted for a thermally conditioned engine, with liquid propellants supplied at 16 psia.

Problems often experienced in tank head idle mode in the past include combustion inefficiency and instability because of the low flowrates and chamber overheating due to sudden increases in mixture ratio, usually caused by phase change in the oxidizer system during chilldown. The GO_2 heat exchanger ensures that, even when the pump is chilled down, only superheated oxygen is supplied to the injector. As a result, there are no sudden shifts in mixture ratio due to phase changes at the injector, and despite the low flowrate, a high injector pressure drop is obtained. In addition, since the oxygen is heated by the thrust chamber coolant fuel discharge flow, there is a stabilizing negative feedback effect (an increase in O_2 flow raises mixture ratio $\longrightarrow$ higher fuel coolant temperature $\longrightarrow$ more heat input to the O_2 $\longrightarrow$ lower O_2 density $\longrightarrow$ reduced O_2 flow).


START SOLENOID VALVE
FUEL SHUT-OFF VALVE SOLENOID
BYPASS SOLENOID VALVE NO. 1
BYPASS SOLENOID VALVE NO. 2
FUEL AND OXIDIZER INLET VALVES
MAIN FUEL SHUTOFF VALVE
FUEL VENT VALVE
TURBINE BYPASS VALVE
THRUST CONTROL(2)
GO2 VALVE(2)
OXIDIZER FLOW CONTROL VALVE(1,2)
NOZZLE COOLANT VALVE(2)
FUEL AND OXIDIZER TANK PRESSURIZATION VALVES(2)
(2)
TANK HEAD IDLE
ACCELERATION
MANEUVERING THRUST (PUMPED IDLE), STEADY STATE
ACCELERATION
FULL THRUST, STEADY STATE
DECELERATION
MANEUVERING THRUST (PUMPED IDLE), STEADY STATE
SHUTDOWN
LEGEND:
FULL OPEN OR "ON"
MODULATION
PARTIALLY OPEN
FULL CLOSED OR "OFF"

▲ ITEMS MARKED ▲ ARE INITIATED BY COMMAND SIGNAL AND CAUSE
● THE SUCCEEDING OPERATIONS MARKED ●

(1) VEHICLE PROPELLANT UTILIZATION SYSTEM ADJUSTS SETTING TO CONTROL MIXTURE RATIO AT FULL THRUST ONLY
(2) VALVE ACTUATED AUTOMATICALLY BY INTERNAL ENGINE PRESSURES

Figure C-9. Engine Operation Valve Sequence Derivative IIA and IIB

FD 75262

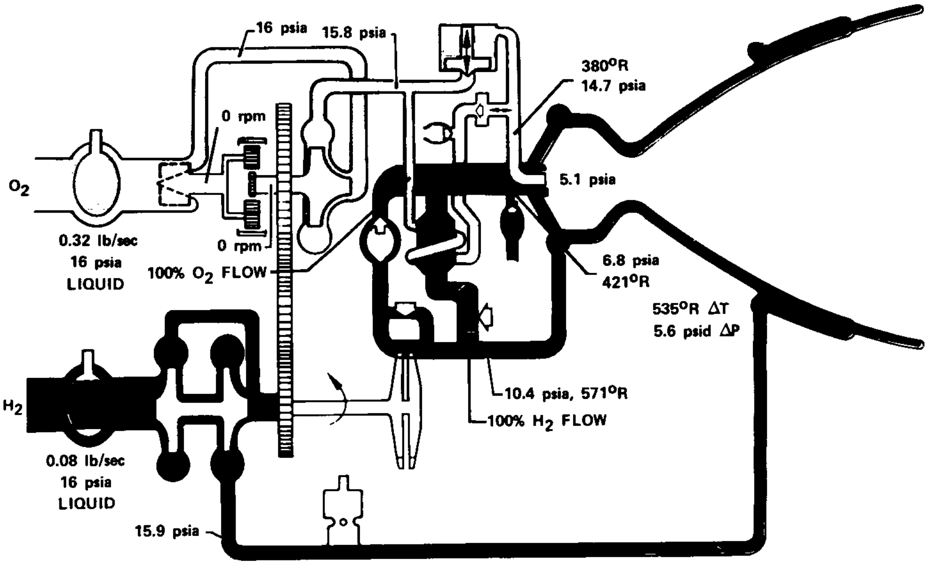


Figure C-10. Tank Head Idle Propellant Flow Schematic Derivative IIA Engine FD 76246B

System resistance, propellant initial conditions, and heat transfer rates between the components and propellants, determine the mixture ratio and chamber pressure characteristics during THI. Predicted thrust and mixture ratio characteristics for the Derivative IIA engine during a typical Space Tug tank head idle cooldown are shown in figure C-11. These characteristics include the effect of settling propellants in the tank and the associated propellant phase changes.

After pump conditioning in tank head idle mode is complete, the engine can be operated at maneuver thrust level for low ΔV maneuvers. To start turbopump rotation, the main fuel shutoff valve is opened and the turbine bypass valve is closed momentarily to give a high initial turbine torque and is then reopened to the maneuver thrust position. The transition from THI to maneuvering thrust is initiated when vehicle electrical energy is supplied to the fuel shutoff valve solenoid and the bypass solenoid valve No. 2 and removed from bypass solenoid valve No. 1. As the pumps accelerate the poppet in the liquid oxidizer flow control valve is opened and the GO_2 valve is closed by the increasing pressure rise across the oxidizer pump. With both the turbine bypass valve and the oxidizer flow control valve opening as a function of the engine acceleration the engine makes a smooth transition to the maneuvering thrust level. The fuel and oxidizer tank pressurization valves which are spring loaded closed, are opened by the increasing pressure at the injector manifolds whereas the spring-loaded closed nozzle coolant valve is opened as gearbox pressure increases. Thrust and mixture ratio characteristics expected during this transient are shown in figure C-12. The predicted steady-state engine operating conditions at the 25% maneuver thrust level are shown in figure C-13.

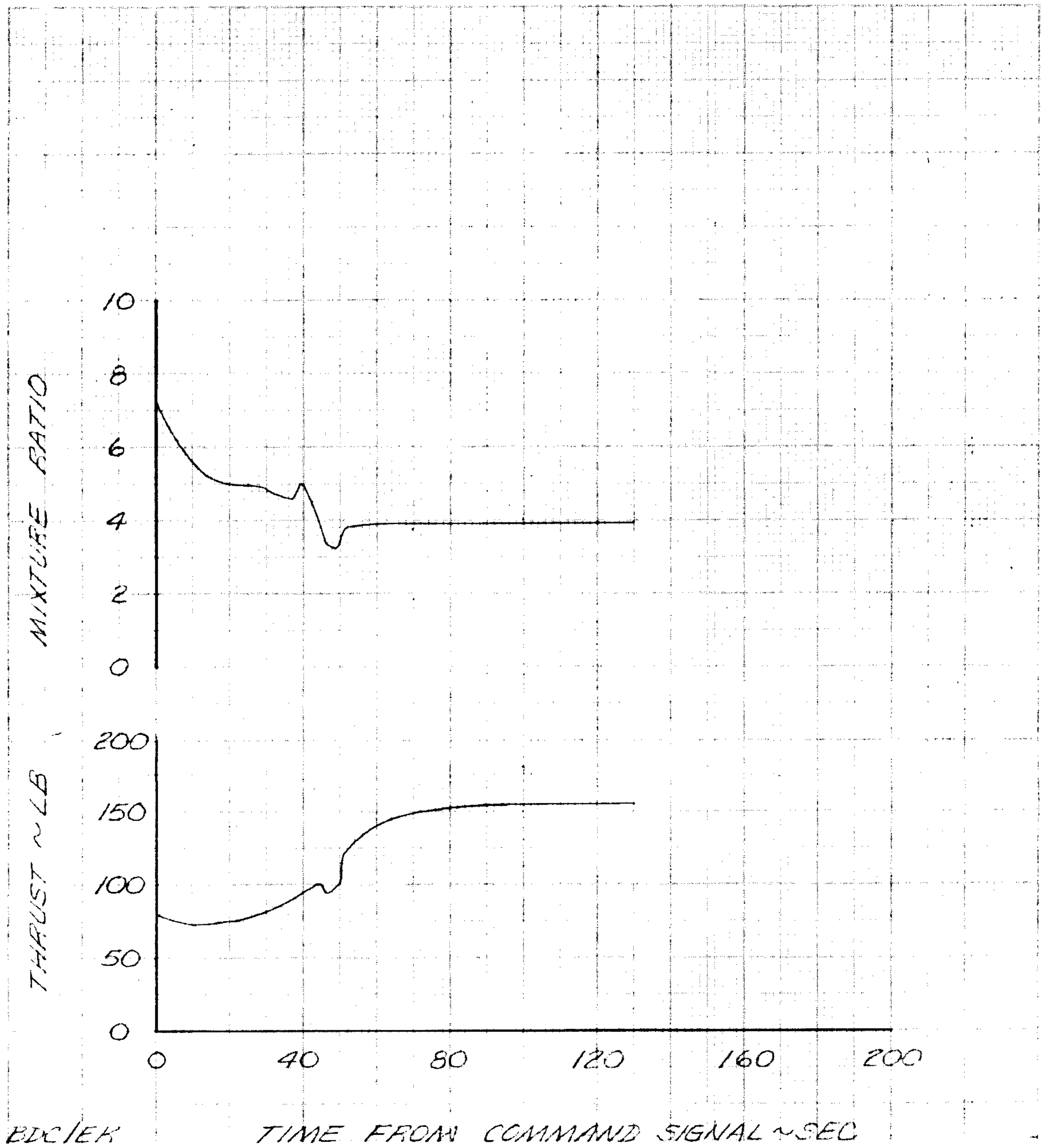


Figure C-11. Typical Cooldown Transient Derivative IIA Engine Thrust and Mixture Ratio

DF 97604

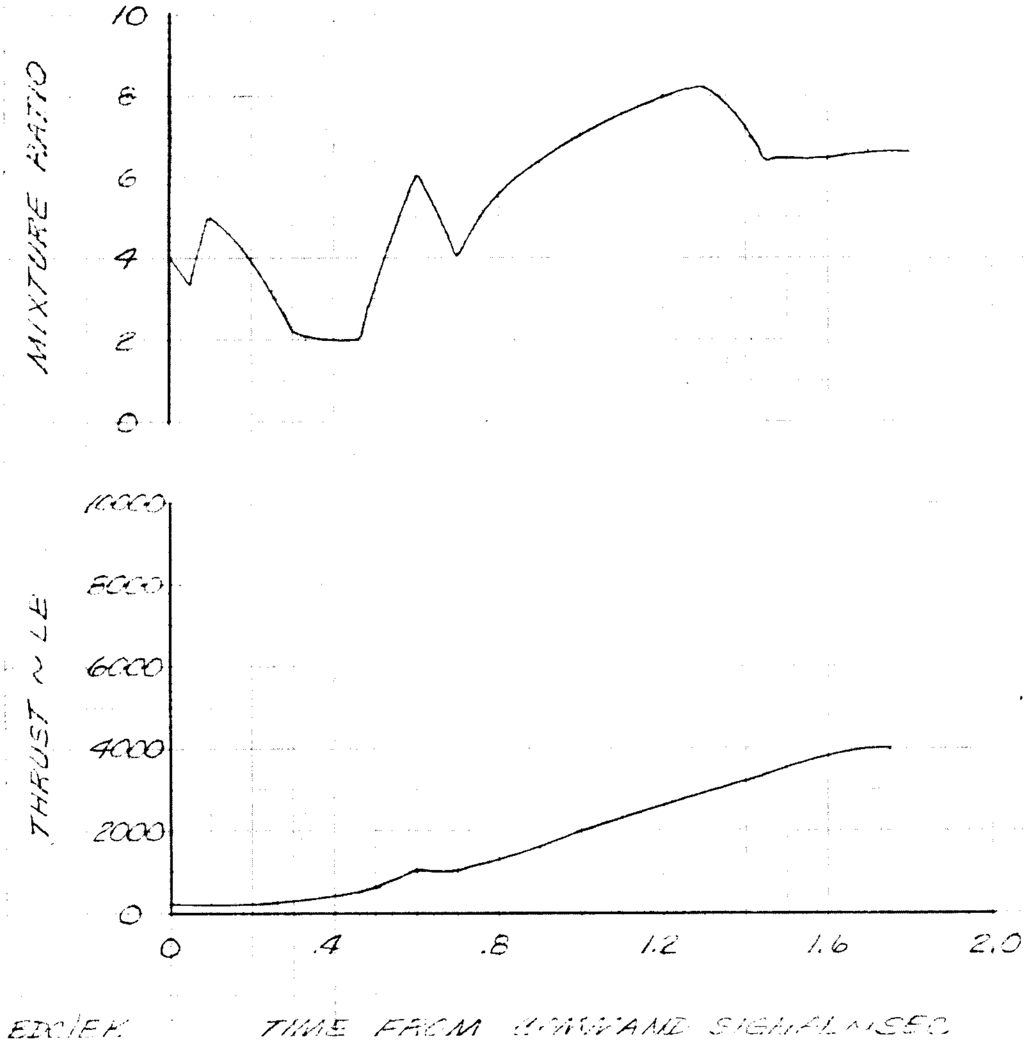


Figure C-12. Nominal Transient from Tank Head Idle to Maneuver Thrust (Pumped Idle) Thrust and Mixture Ratio (Derivative IIA and IIB)

DF 97602

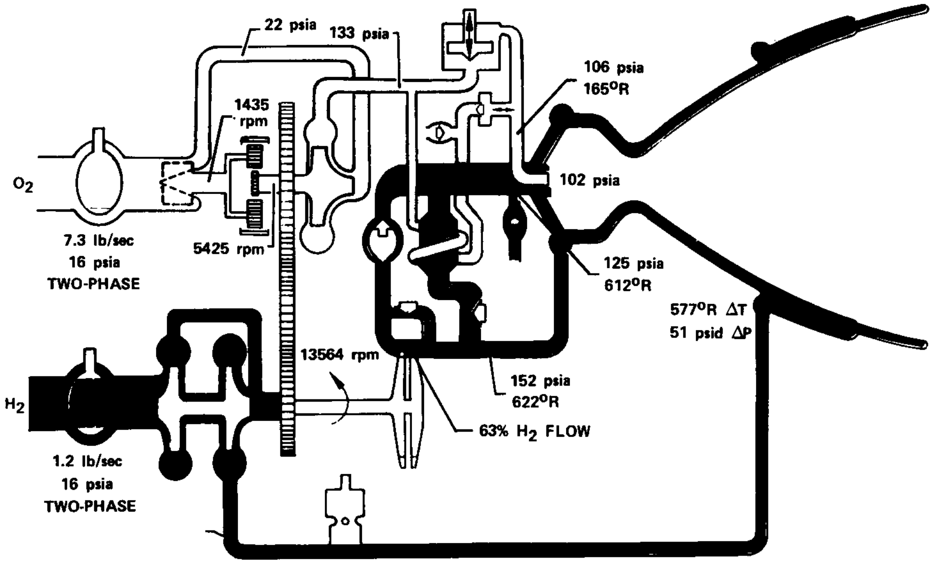


Figure C-13. Maneuver Thrust Propellant Flow Schematic Derivative IIA Engine FD 76247B

The transient from maneuvering thrust to full thrust occurs when electrical power from the vehicle to the bypass solenoid valve No. 2 is removed. This action closes the turbine bypass valve, which increases turbine power and causes the engine to accelerate to full thrust. As the engine accelerates, the increasing oxidizer pump pressure opens the main poppet in the oxidizer flow control valve, providing increasing oxidizer flow for mixture ratio control during the acceleration. The thrust control opens a turbine bypass path limiting thrust overshoot as chamber pressure approaches its desired full thrust level.

Upon thrust control opening, the turbine bypass area is modulated as required to maintain the desired full thrust chamber pressure level. Nominal thrust and mixture ratio characteristics for this transient are provided in figure C-14. Predicted operating characteristics of the engine at full thrust and 6.0 mixture ratio are shown in figure C-15.

A reduction in engine thrust can be accomplished from any of the three modes of operation by either a direct shutdown or by a valve sequence that allows the engine to decelerate to a lower operating mode. The deceleration sequence is the opposite of that used during engine acceleration. Direct shutdown is initiated by removing the vehicle voltage from all solenoids. The removal of the voltage closes inlet shutoff valves, opens the fuel vent valve and closes the main fuel shutoff valve. Closing of the main fuel shutoff valve stops all fuel flow to the turbine and combustion chamber causing chamber pressure to rapidly decay.

Hydrogen flow, trapped in the system between the fuel inlet shutoff valve and the main fuel shutoff valve, bleeds overboard through the fuel vent valve. The oxidizer flow control valve, tank pressurization valves and nozzle coolant valve closes automatically as internal pressures in the engine decay. When the oxidizer pump pressure decays sufficiently, the GO_2 valve opens and the oxidizer remaining in the system bleeds overboard through the combustion chamber. Because fuel flow to the combustion chamber is abruptly cut off, thrust decays to 5% of full thrust in approximately 0.12 sec. Deceleration and direct shutoff transient characteristics for the Derivative IIA engine are provided in figure C-16.

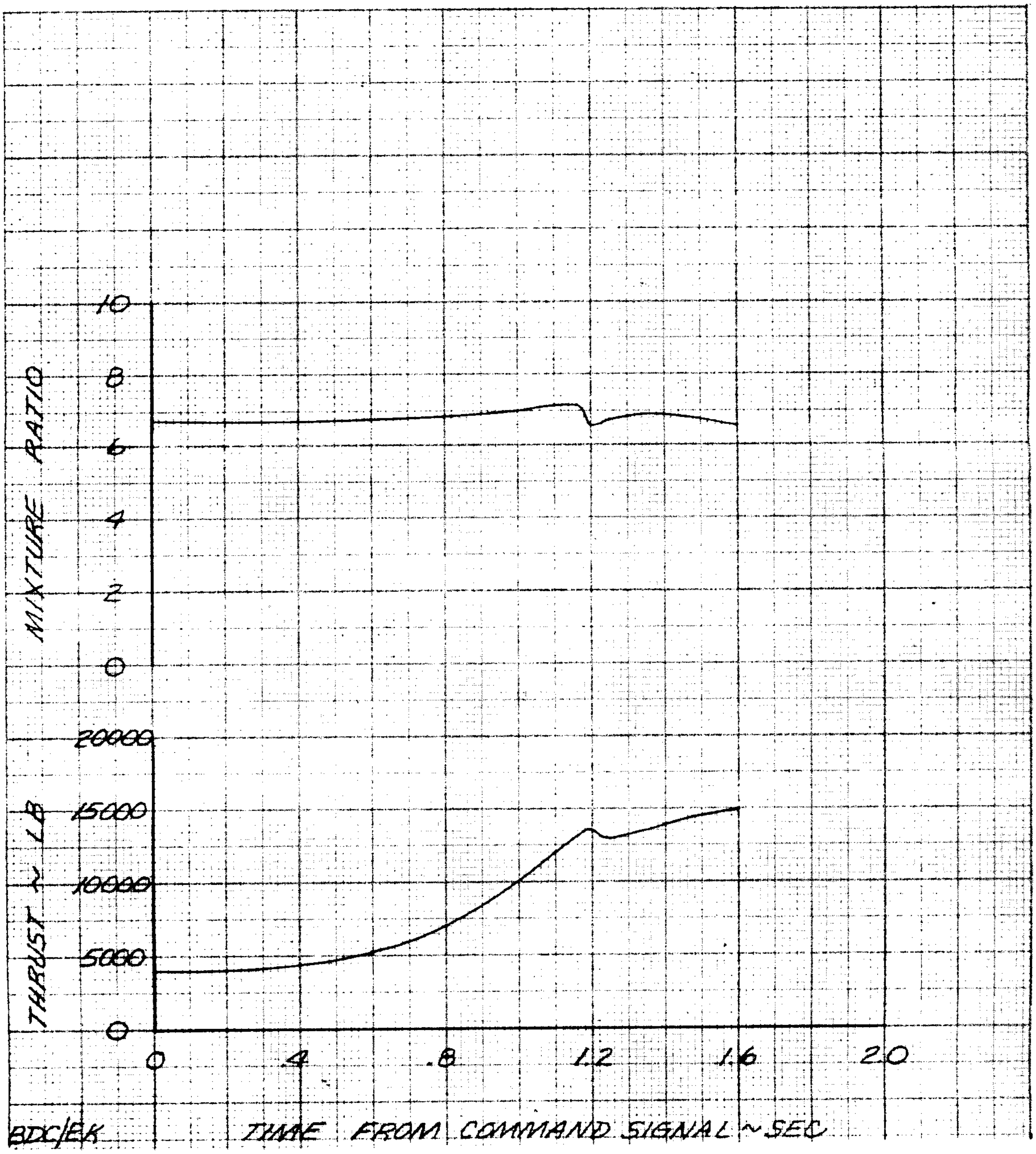


Figure C-14. Nominal Transient from Maneuver Thrust (Pumped Idle) to Full Thrust Derivative IIA and IIB Engines Thrust and Mixture Ratio

DF 97603

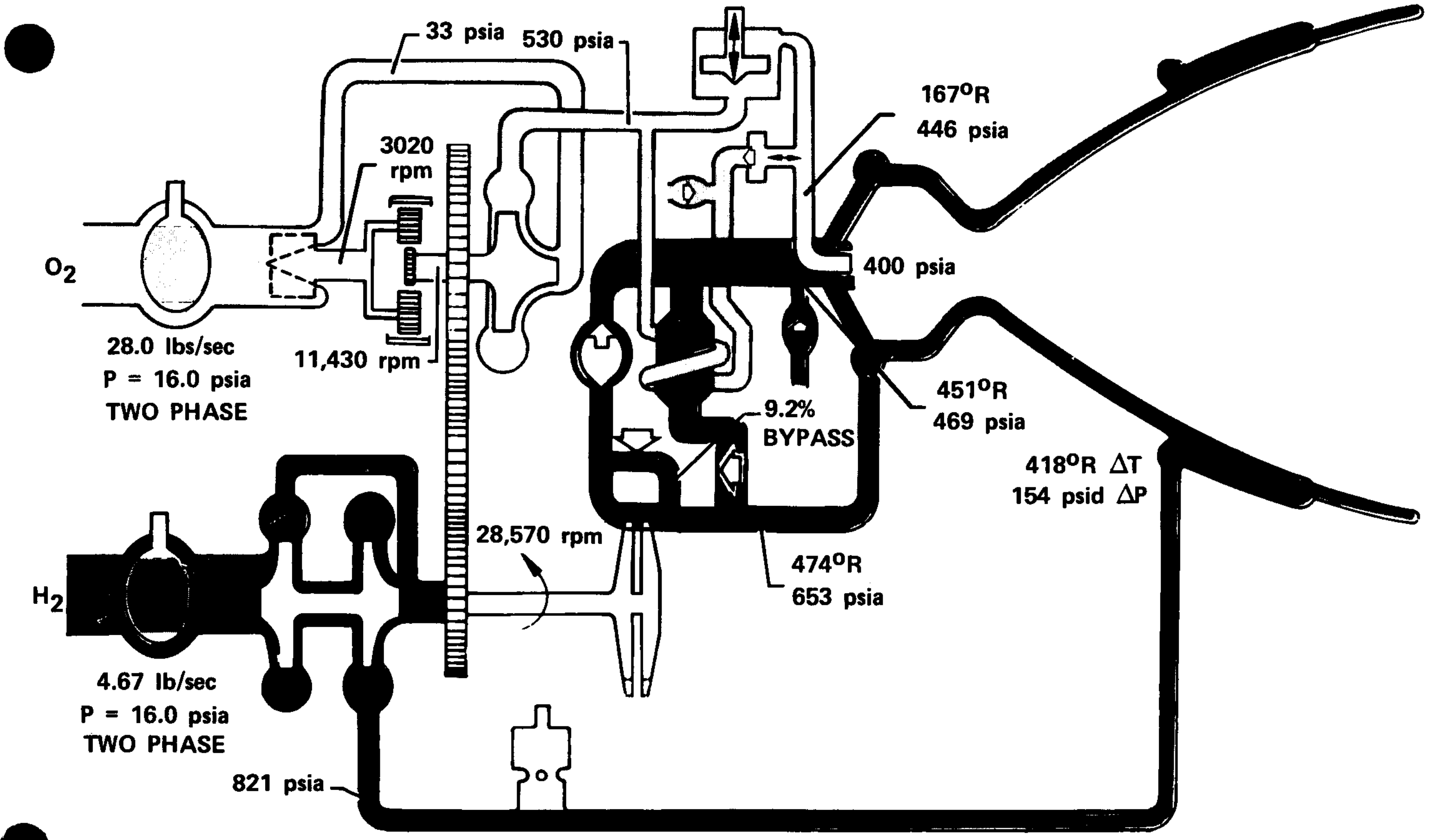
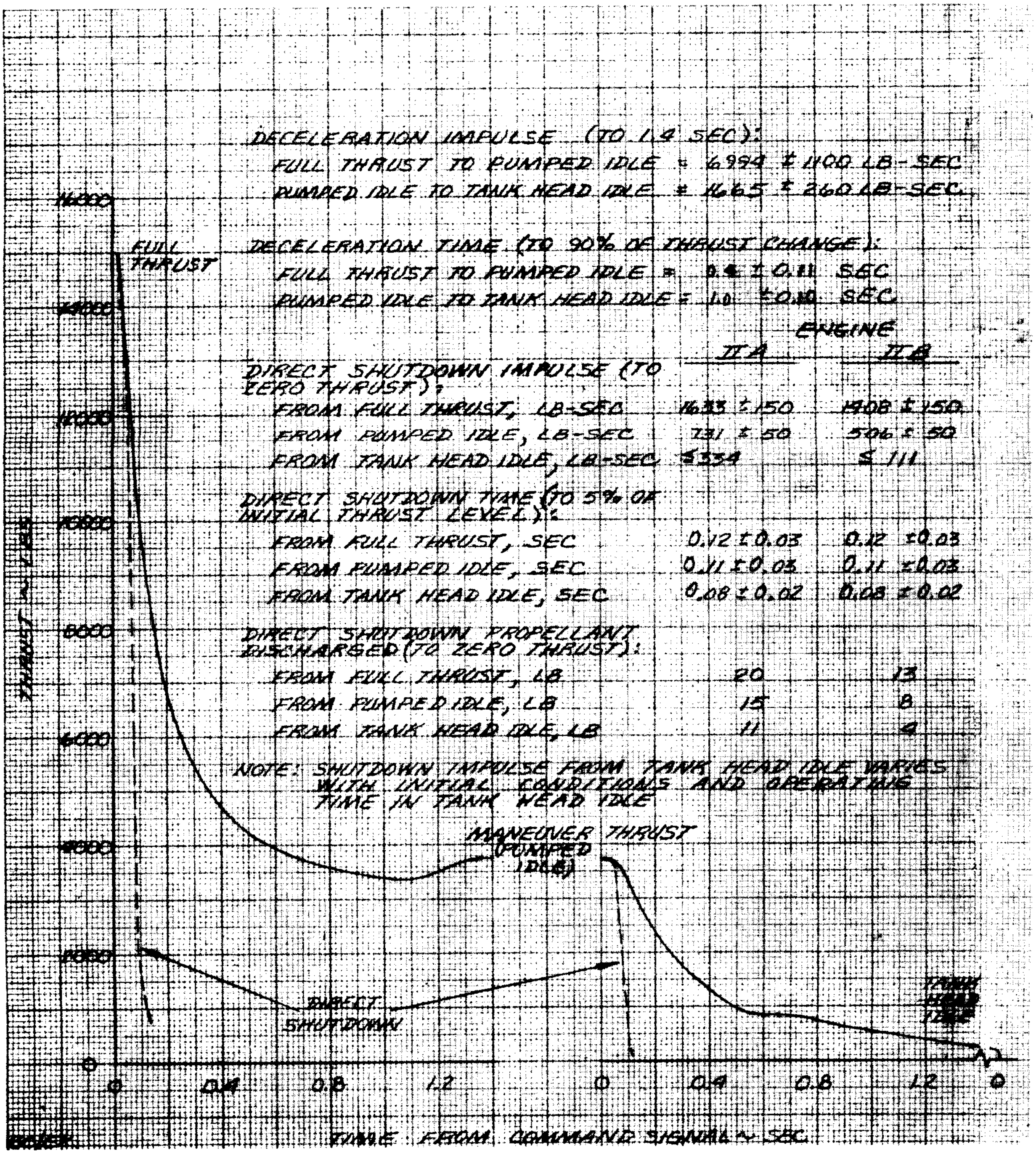

33 psia
530 psia
3020 rpm
11,430 rpm
167°R
446 psia
400 psia
O_2
28.0 lbs/sec
P = 16.0 psia
TWO PHASE
9.2%
BYPASS
451°R
469 psia
418°R ΔT
154 psid ΔP
28,570 rpm
474°R
653 psia
H_2
4.67 lb/sec
P = 16.0 psia
TWO PHASE
821 psia

Figure C-15. Derivative IIA Propellant Flow Schematic, Full Thrust, MR = 6.0 FD 72916B



DECELERATION IMPULSE (TO 1.4 SEC):
FULL THRUST TO PUMPED IDLE = 6994 ± 1100 LB-SEC
PUMPED IDLE TO TANK HEAD IDLE = 1665 ± 260 LB-SEC

DECELERATION TIME (TO 90% OF THRUST CHANGE):
FULL THRUST TO PUMPED IDLE = 0.4 ± 0.11 SEC
PUMPED IDLE TO TANK HEAD IDLE = 1.0 ± 0.10 SEC

	ENGINE	
	IIA	IIB
DIRECT SHUTDOWN IMPULSE (TO ZERO THRUST):		
FROM FULL THRUST, LB-SEC	1633 ± 150	1908 ± 150
FROM PUMPED IDLE, LB-SEC	731 ± 50	506 ± 50
FROM TANK HEAD IDLE, LB-SEC	≤ 334	≤ 111
DIRECT SHUTDOWN TIME (TO 5% OF INITIAL THRUST LEVEL):		
FROM FULL THRUST, SEC	0.12 ± 0.03	0.12 ± 0.03
FROM PUMPED IDLE, SEC	0.11 ± 0.03	0.11 ± 0.03
FROM TANK HEAD IDLE, SEC	0.08 ± 0.02	0.08 ± 0.02
DIRECT SHUTDOWN PROPELLANT DISCHARGED (TO ZERO THRUST):		
FROM FULL THRUST, LB	20	13
FROM PUMPED IDLE, LB	15	8
FROM TANK HEAD IDLE, LB	11	4

NOTE: SHUTDOWN IMPULSE FROM TANK HEAD IDLE VARIES WITH INITIAL CONDITIONS AND OPERATING TIME IN TANK HEAD IDLE

Figure C-16. Deceleration and Shutdown Transients from Full Thrust to Tank Head Idle Derivative IIA and IIB Engines

DF 96623

The steady-state and transient performance characteristics of the RL10 Derivative IIA engine are summarized in tables C-1 and C-2. Performance at full thrust over the range of mixture ratio anticipated for propellant utilization control is shown in figures C-17 and C-18.

Table C-1. Steady-State Performance Summary (Derivative IIA Engine)

Full Thrust Performance	
Thrust, lb vac	15,000
Mixture Ratio	6.0
Chamber Pressure, psia	400
Specific Impulse, sec	459.2
Required Inlet Condition	
Fuel	< 40% Vapor
Oxidizer	< 40% Vapor
Maneuvering Thrust Performance	
Thrust, lb vac	3,750
Mixture Ratio	6.0
Chamber Pressure, psia	102.0
Specific Impulse, sec	437.5
Required Inlet Condition	
Fuel	≤ 70% Vapor
Oxidizer	≤ 70% Vapor
Tank Head Idle Performance	
Thrust, lb vac	157
Mixture Ratio	4.0
Specific Impulse, sec	387

Table C-2. Transient Performance Summary (Derivative IIA and IIB Engines)

Typical Tank Head Idle Transient	
Initial Thrust, lb	82
Final Thrust, lb	157
Cooldown Time in sec(4) (IIA/IIB)	89/90
Start Transient	
Tank Head to Maneuver Thrust Time, sec(1)	1.56 ± 0.30
Impulse, lb-sec(2)	3,880 ± 1200
Maneuver Thrust to Full Thrust Time, sec(1)	1.31 ± 0.12
Impulse, lb-sec(2)	20,608 ± 1500

---

(1) To 90% of Thrust Change
(2) 2.0 seconds duration
(3) 1.4 seconds duration
(4) Tank Pressure = 16 psia, initial Engine Temperature = 500°R, Cold Inlet Lines

Table C-2. Transient Performance Summary (Derivative IIA Engine) (Continued)

Deceleration Transient		
Full Thrust to Maneuver Thrust Time, sec(1)	0.4	± 0.11
Impulse, lb-sec(3)	6,994	± 1100
Maneuver Thrust to Tank Head Idle Time, sec(1)	1.0	± 0.10
Impulse, lb-sec(3)	1,665	± 260

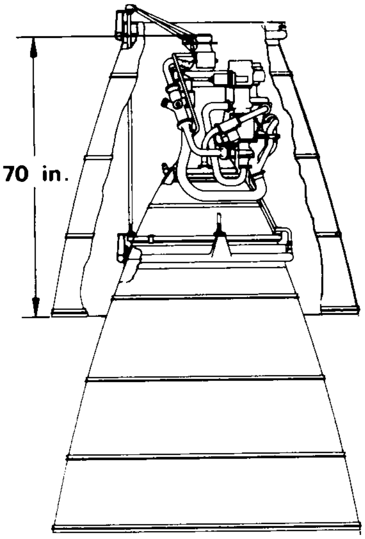
	Derivative IIA	Derivative IIB
Shutdown Transient		
From Full Thrust Time, sec(5)	0.12 ± 0.03	0.12 ± 0.03
Impulse, lb-sec	1633 ± 150	1408 ± 150
Propellants Discharged, lb	20	13
From Pumped Idle Time, sec(5)	0.11 ± 0.03	0.11 ± 0.03
Impulse, lb-sec	731 ± 50	506 ± 50
Propellants Discharged, lb	15	8
From Tank Head Idle Time, sec(5)	0.08 ± 0.02	0.08 ± 0.02
Impulse, lb-sec(6)	≤334	≤111
Propellants Discharged, lb	11	4

---

(5) To 5% of Initial Thrust Level

(6) Shutdown impulse from THI varies with initial conditions and operating time in THI.

2.3 Derivative IIB Engine



THRUST : 15,000 lb
CHAMBER PRESSURE : 400 psia
AREA RATIO : 66.3/262
I_{sp} : 459 sec AT 6.0 MR
OPERATION : FULL THRUST (LOW NPSH)
: MANEUVER THRUST (SATURATED PROPELLANTS)
CONDITIONING : TANK HEAD IDLE
WEIGHT : 474 lb
LIFE : 190 FIRINGS/5 HOURS
ENVELOPE : LENGTH 70/127 in.
: NOZZLE EXIT DIAMETER 40/79.6 in.

FD 74123A

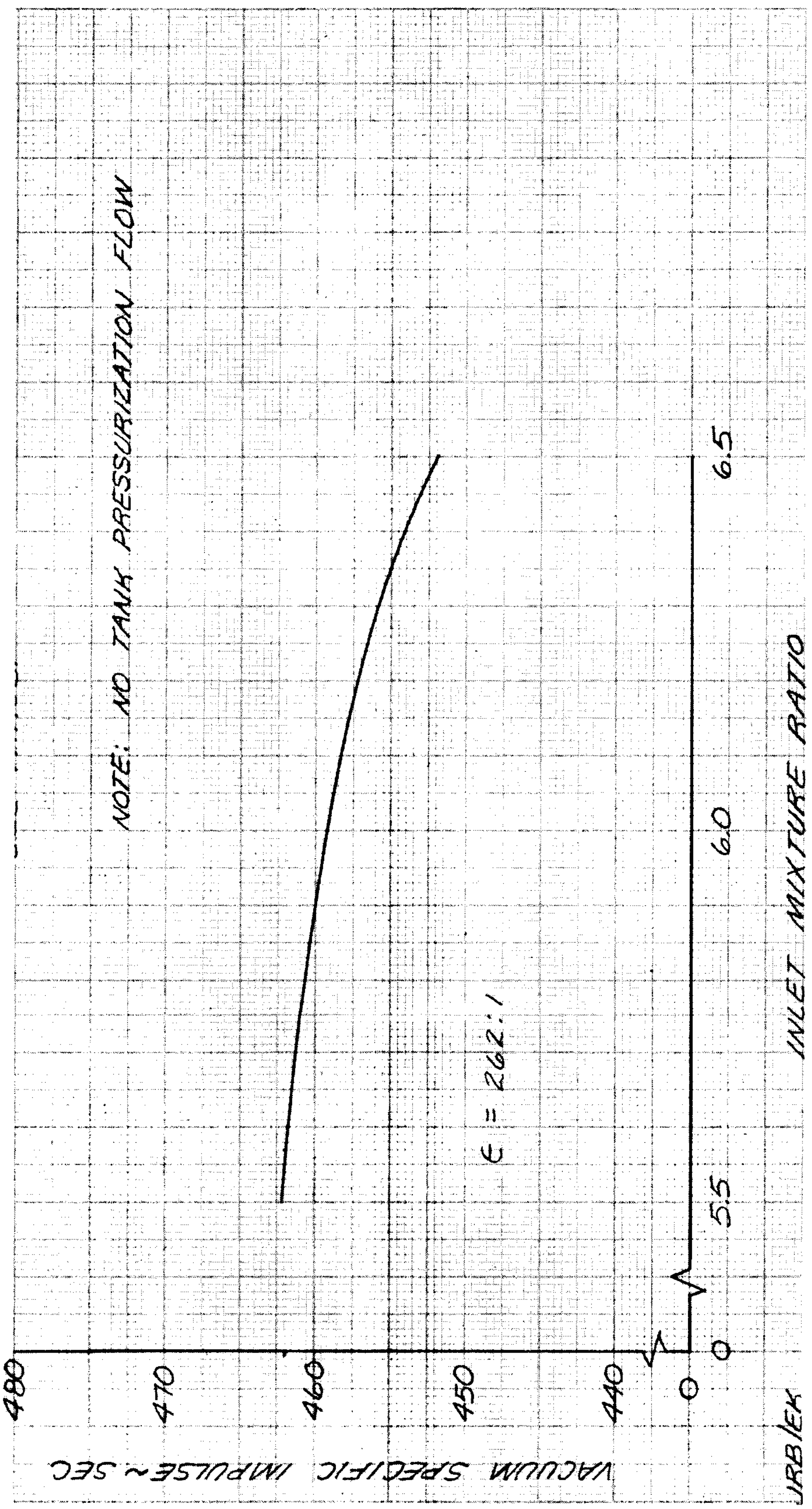


Figure C-17. Estimated Effect of Inlet Mixture Ratio on Vacuum Specific Impulse, Derivative IIA and IIB Engines, Full Thrust

DF 96978

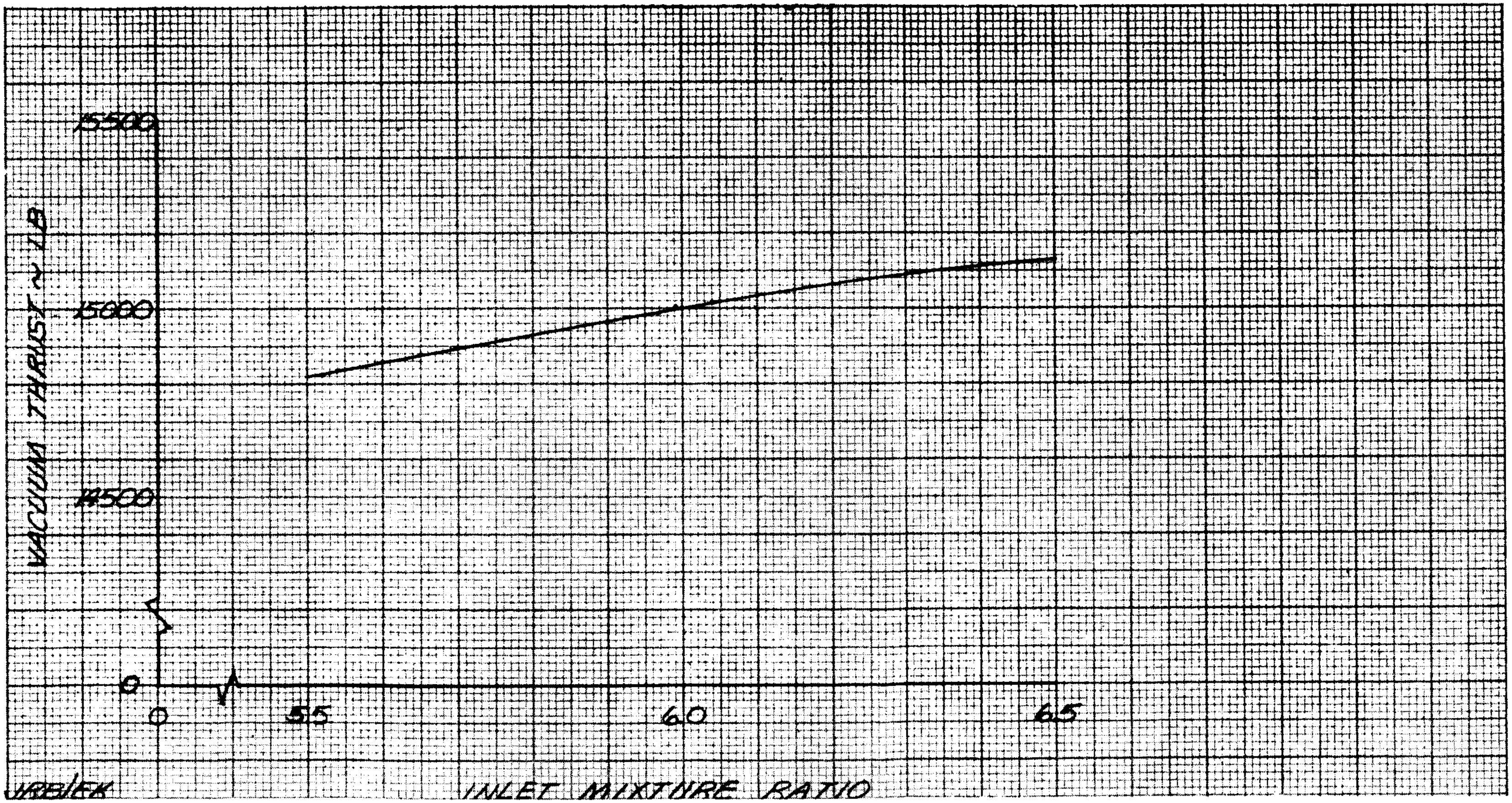


Figure C-18. Estimated Effect of Inlet Mixture Ratio on Vacuum Thrust, Derivative IIA and IIB Engines, Full Thrust

DF 96979

2.3.1 Engine Description

The Derivative IIB engine principal components and their arrangement are shown schematically in figure C-19. The RL10 Derivative IIB is similar to the Derivative IIA engine except that it is not designed to provide two-phase pumping capability at full thrust. It is, however, designed to operate in a pumped idle mode, with saturated propellants in the vehicle tanks, and provide boostrap autogenous pressurization. This mode of operation allows the RL10A-3-3 Bill-of-Material turbopump to be run at a low speed where prepressurization to subcool the propellants at the pump inlets is not required. By using the engine's bootstrap autogenous pressurization capability, the tanks can be prepressurized to satisfy the engine's full thrust pump inlet NPSH requirements prior to acceleration to full thrust.

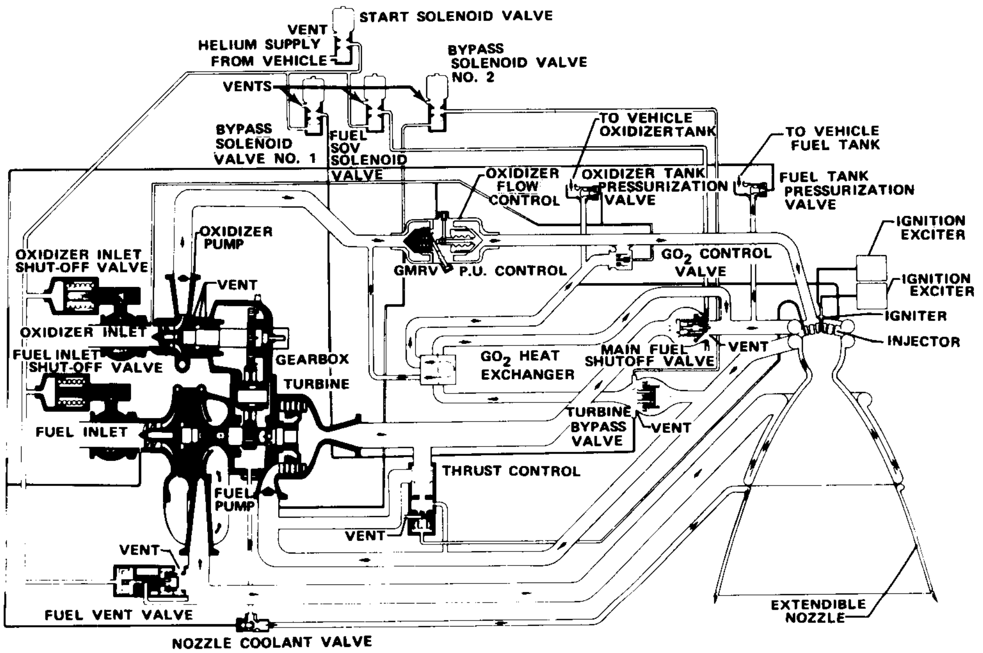

START SOLENOID VALVE
VENT
HELIUM SUPPLY FROM VEHICLE
BYPASS SOLENOID VALVE NO. 2
VENTS
BYPASS SOLENOID VALVE NO. 1
FUEL SOV SOLENOID VALVE
TO VEHICLE OXIDIZER TANK
TO VEHICLE FUEL TANK
OXIDIZER FLOW CONTROL
OXIDIZER TANK PRESSURIZATION VALVE
FUEL TANK PRESSURIZATION VALVE
IGNITION EXCITER
OXIDIZER PUMP
OXIDIZER INLET SHUT-OFF VALVE
VENT
GMRV
P.U. CONTROL
GO_2 CONTROL VALVE
IGNITION EXCITER
IGNITER
INJECTOR
OXIDIZER INLET
FUEL INLET SHUT-OFF VALVE
GEARBOX
TURBINE
GO_2 HEAT EXCHANGER
MAIN FUEL SHUTOFF VALVE
VENT
TURBINE BYPASS VALVE
VENT
FUEL INLET
THRUST CONTROL
FUEL PUMP
VENT
VENT
FUEL VENT VALVE
NOZZLE COOLANT VALVE
EXTENDIBLE NOZZLE

Figure C-19. RL10 Derivative IIB Propellant Flow Schematic

FD 74111A

The dry weight of the engine and its subassemblies are summarized in the following table. Of the total engine weight of 474 lb, 36% is weight of existing hardware, 53% is calculated from layout drawing and 11% is estimated.

RL10 Derivative IIB Engine Weight

Turbopump and Gearbox	79 lb
Thrust Chamber and Primary Nozzle	110 lb
Extendible Nozzle Actuator System	43 lb
Extendible Nozzle	104 lb
GO_2 Heat Exchanger	13 lb
Controls, Valve and Actuators	66 lb
Plumbing and Miscellaneous Hardware	44 lb
Ignition System	15 lb
Total Dry Weight	474 lb

Installation drawings of the Derivative IIB engine are shown in figure C-20.

2.3.2 Component Description

The Derivative IIB engine uses RL10A-3-3 Bill-of-Material inlet valves and turbopumps described below. In all other respects the components are the same as those described previously for the Derivative IIA engine.

a. Fuel and Oxidizer Inlet Valves

The fuel and oxidizer inlet valves are spring-loaded, normally closed, helium operated, rack and pinion actuated, two-position ball valves. The valve designs differ from those of the Derivative IIA engine only in size and seal arrangement. The valves are smaller in diameter and are a single seal configuration.

b. Turbopump Assembly

The turbopump assembly shown in figure C-21 consists of a liquid hydrogen pump and hydrogen powered impulse turbine mounted on a common main shaft and a liquid oxygen pump mounted adjacent to the fuel pump and driven through a gear train by the hydrogen pump turbine shaft. The turbopump is basically the same as the Derivative IIA engine turbopump except that removing the requirement to pump two-phase propellants at full thrust eliminates the need for an oxidizer low speed inducer and large diameter fuel high speed inducer.

2.3.3 Operation and Performance Characteristics

The performance of the Derivative IIB engine is the same as that of the Derivative IIA and propellant settling and engine conditioning in THI is carried out in the same way. The pumped idle thrust level for the Derivative IIB engine is the same as the maneuver thrust level of the Derivative IIA engine and acceleration to this operating mode is carried out in the same way. A wide range of prepressurization flowrates can be supplied in pumped idle mode with little change in engine thrust, even though no active control valves are used.

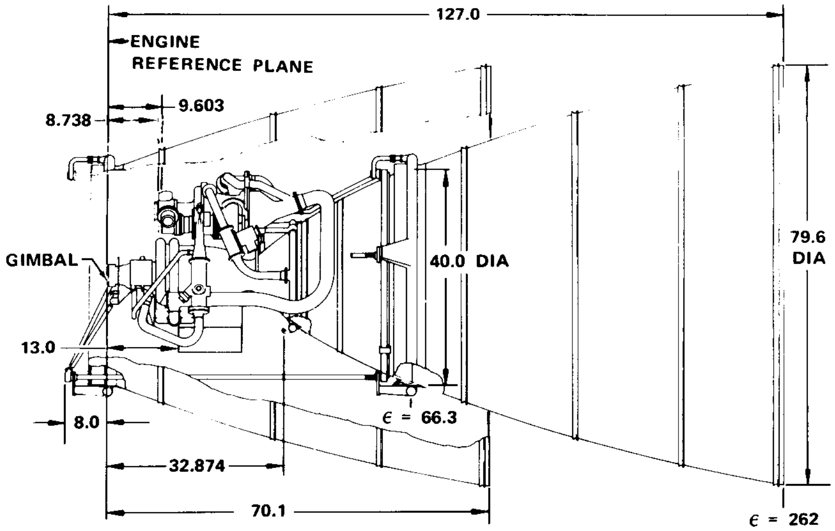

127.0
ENGINE
REFERENCE PLANE
9.603
8.738
GIMBAL
40.0 DIA
79.6
DIA
13.0
8.0
ε = 66.3
32.874
70.1
ε = 262

Figure C-20(1). Derivative IIB Engine Installation Drawing FD 72043B

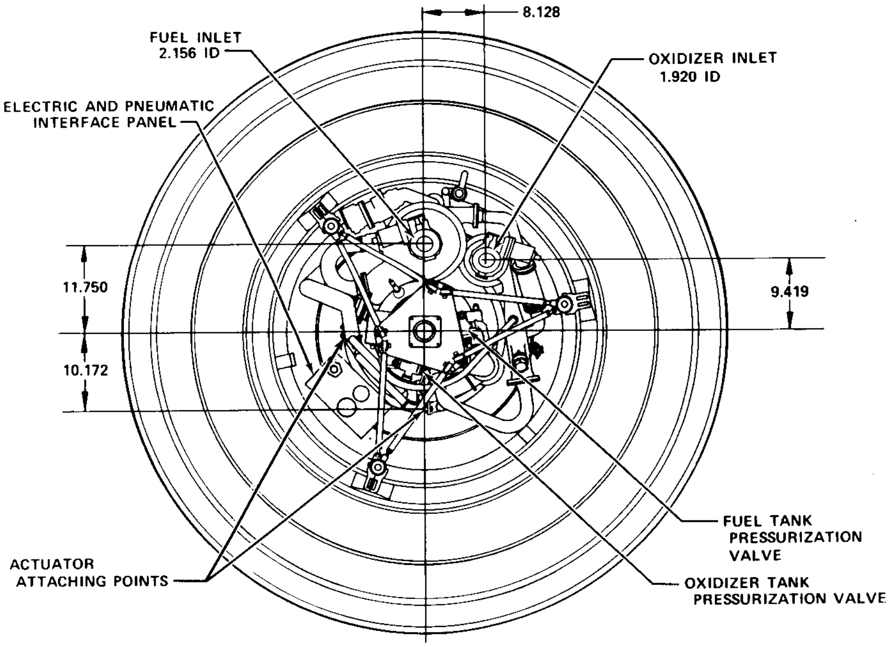

8.128
FUEL INLET
2.156 ID
OXIDIZER INLET
1.920 ID
ELECTRIC AND PNEUMATIC
INTERFACE PANEL
11.750
10.172
9.419
FUEL TANK
PRESSURIZATION
VALVE
ACTUATOR
ATTACHING POINTS
OXIDIZER TANK
PRESSURIZATION VALVE

Figure C-20(2). Derivative IIB Engine Installation Drawing FD 72045A

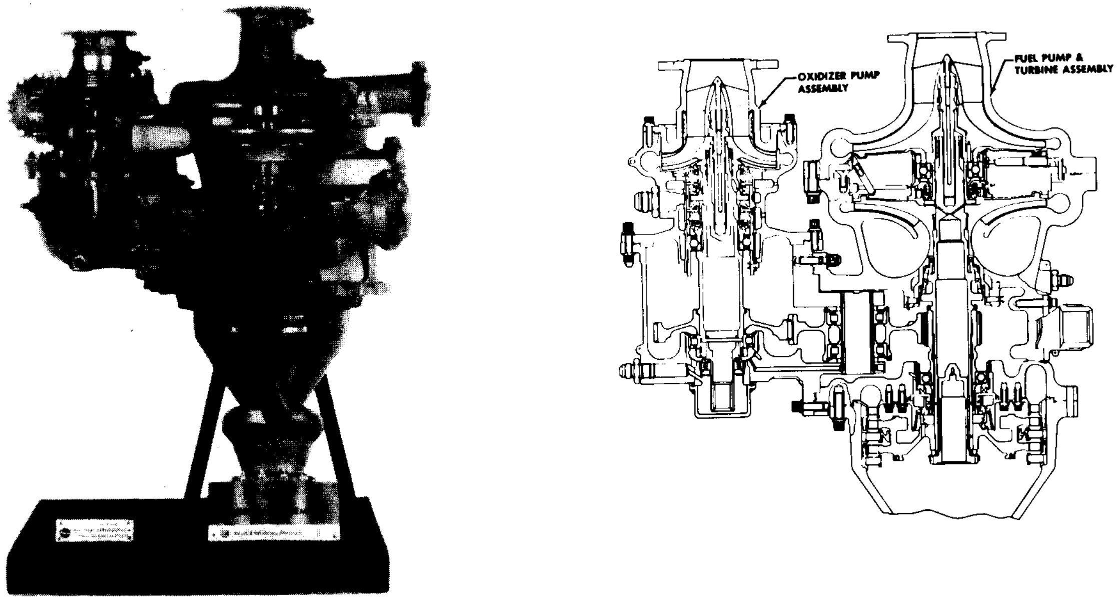

OXIDIZER PUMP ASSEMBLY
FUEL PUMP & TURBINE ASSEMBLY

Figure C-21. RL10 Turbopump Assembly

FD 47657

Acceleration to full thrust and operation at this level is the same as that of the Derivative IIA engine, except that propellants with positive NPSH have to be supplied to the engine, either by using the engine's autogenous pressurization system or with some vehicle supplied system (i.e., boost pumps, helium pressurization, etc.).

The steady-state performance characteristics of the RL10 Derivative IIB engine are summarized in figures C-17, C-18, C-22, C-23 and C-24 and in table C-3.

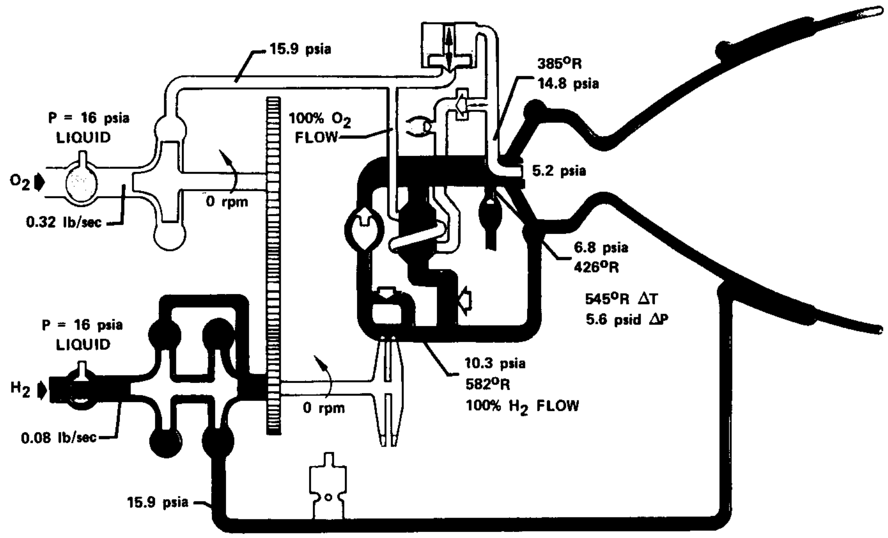

15.9 psia
P = 16 psia
LIQUID
O_2
0.32 lb/sec
0 rpm
100% O_2 FLOW
385°R
14.8 psia
5.2 psia
6.8 psia
426°R
545°R ΔT
5.6 psid ΔP
P = 16 psia
LIQUID
H_2
0.08 lb/sec
0 rpm
10.3 psia
582°R
100% H_2 FLOW
15.9 psia

Figure C-22. Tank Head Idle Propellant Flow Schematic Derivative IIB Engine FD 76248B

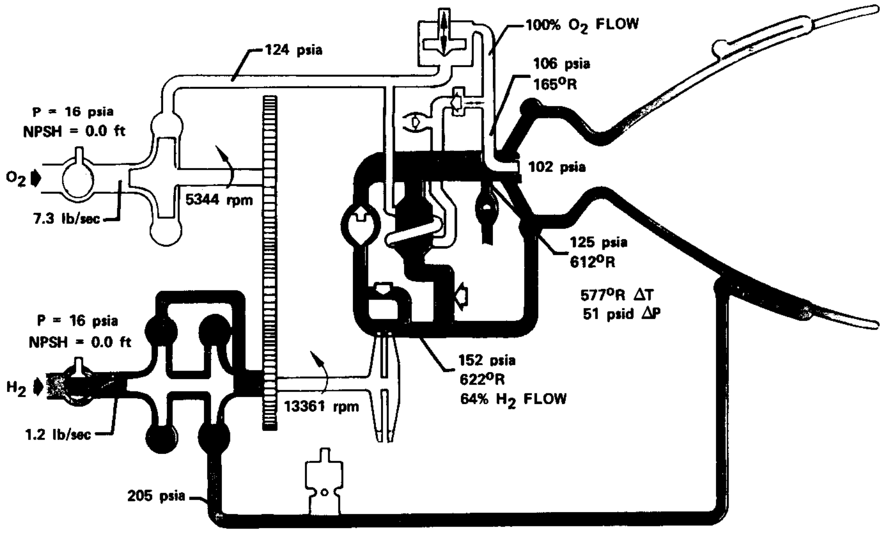

124 psia
P = 16 psia
NPSH = 0.0 ft
O_2
7.3 lb/sec
5344 rpm
100% O_2 FLOW
106 psia
165°R
102 psia
125 psia
612°R
577°R ΔT
51 psid ΔP
P = 16 psia
NPSH = 0.0 ft
H_2
1.2 lb/sec
13361 rpm
152 psia
622°R
64% H_2 FLOW
205 psia

Figure C-23. Pumped Idle Propellant Flow Schematic Derivative IIB Engine FD 76249B

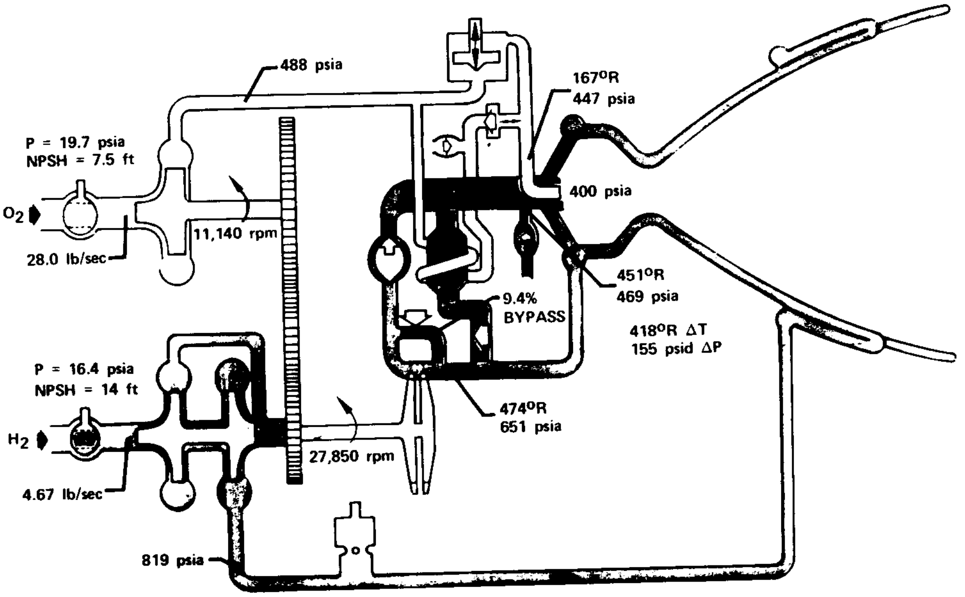

488 psia
P = 19.7 psia
NPSH = 7.5 ft
O_2
28.0 lb/sec
11,140 rpm
167°R
447 psia
400 psia
451°R
469 psia
9.4%
BYPASS
418°R ΔT
155 psid ΔP
P = 16.4 psia
NPSH = 14 ft
H_2
4.67 lb/sec
27,850 rpm
474°R
651 psia
819 psia

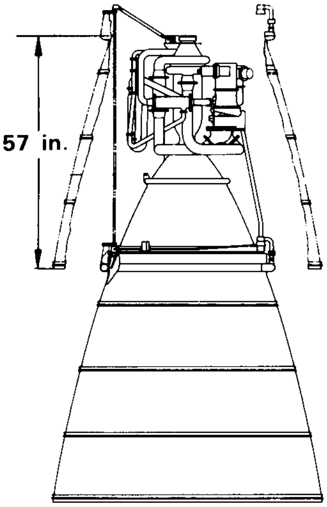
Figure C-24. Derivative IIB Engine Propellant Flow Schematic Full Thrust, MR = 6.0 FD 72880B

Transient performance characteristics are predicted to be essentially the same as that defined for the Derivative IIA engine in figures C-11, C-12, C-14 and C-16 and table C-2.

Table C-3. Derivative IIB Engine Steady-State Performance

Full Thrust Performance	
Thrust, lb vac	15,000
Mixture Ratio	6.0
Chamber Pressure, psia	400
Specific Impulse, sec	459.2
Required Inlet Condition	
Fuel	>14' NPSH
Oxidizer	>7.5' NPSH
Pumped Idle Performance	
Thrust, lb vac	3,750.0
Mixture Ratio	6.0
Chamber Pressure, psia	102.0
Specific Impulse, sec	437.5
Required Inlet Condition	
Fuel	≤65% Vapor
Oxidizer	≤45% Vapor
Tank Head Idle Performance	
Thrust, lb vac	157
Mixture Ratio	4.0
Specific Impulse, sec	387

2.4 Category IV Engine



THRUST : 15,000 lb
CHAMBER PRESSURE : 915 psia
AREA RATIO : 125/401
I_{sp} : 470 sec AT 6.0 MR
OPERATION : FULL THRUST (SATURATED PROPELLANTS)
: MANEUVER THRUST (SATURATED PROPELLANTS)
CONDITIONING : TANK HEAD IDLE
WEIGHT : 424 lb
LIFE : 300 FIRINGS/10 HOURS
ENVELOPE : LENGTH 57/114 in.
: NOZZLE EXIT DIAMETER 37/66 in.

2.4.1 Engine Description

The general component arrangement of the RL10 Category IV engine is shown schematically in figure C-25. Unlike the Derivative II baseline engines, which are modified versions of the RL10A-3-3, the RL10 Category IV engine is a "clean sheet" design. However, it is not an advanced technology engine, since it uses the expander power cycle and design concepts of the RL10. Basically, it is an update of the RL10 design which has been optimized specifically for use in the Space Tug. The Category IV engine is designed to be interchangeable with the RL10 Derivative IIA engine and is capable of the same operating modes, i.e., tank head idle mode, maneuver thrust, and two-phase pumping at full thrust. It has a design life of 300 firings and 10 hr duration, and operates at a thrust level of 15,000 lb at a mixture ratio of 6.0.

The dry weight of the engine and its subassemblies are summarized in the following table. Of the total engine weight of 424 lb, 61% is calculated from layout drawings and 39% is estimated.

RL10 Category IV Engine Weight

Turbopumps and Gearbox	91 lb
Thrust Chamber and Primary Nozzle	90 lb
Extendible Nozzle Actuation System	35 lb
Extendible Nozzle	76 lb
GO_2 Heat Exchanger	13 lb
Controls, Valves and Actuators	74 lb
Plumbing and Miscellaneous Hardware	38 lb
Ignition System	7 lb
Total Dry Weight	424 lb

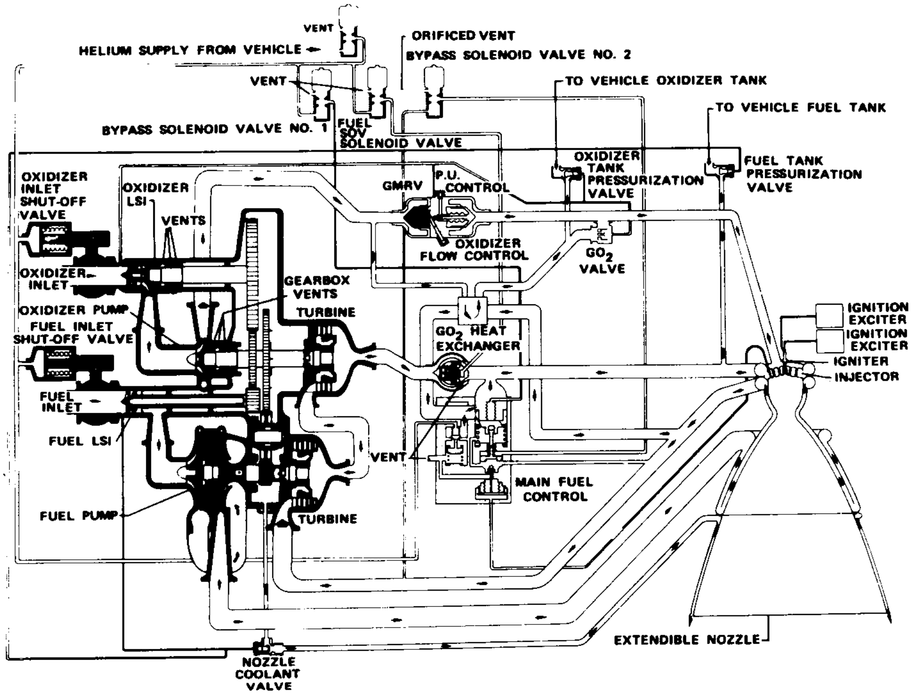

VENT
HELIUM SUPPLY FROM VEHICLE
ORIFICED VENT
BYPASS SOLENOID VALVE NO. 2
VENT
TO VEHICLE OXIDIZER TANK
TO VEHICLE FUEL TANK
BYPASS SOLENOID VALVE NO. 1
FUEL SOV SOLENOID VALVE
OXIDIZER TANK PRESSURIZATION VALVE
FUEL TANK PRESSURIZATION VALVE
OXIDIZER INLET SHUT-OFF VALVE
OXIDIZER LSI
GMRV
P.U. CONTROL
VENTS
OXIDIZER FLOW CONTROL
GO_2 VALVE
OXIDIZER INLET
GEARBOX VENTS
TURBINE
OXIDIZER PUMP
FUEL INLET SHUT-OFF VALVE
GO_2 HEAT EXCHANGER
IGNITION EXCITER
IGNITION EXCITER
IGNITER
INJECTOR
FUEL INLET
FUEL LSI
VENT
MAIN FUEL CONTROL
FUEL PUMP
TURBINE
EXTENDIBLE NOZZLE
NOZZLE COOLANT VALVE

Figure C-25. RL10 Category IV Propellant Flow Schematic FD 74113A

The Category IV engine is interchangeable with the RL10 Derivative IIA and the installed length was selected to be the same as the minimum length Derivative IIA engine. Installation drawings of the Category IV engine are shown in figure C-26.

2.4.2 Component Description

The Category IV engine operates at an increased chamber pressure level requiring some design concept changes from that of the derivative engines. The control system is basically the same as that of the Derivative II engines, except that the main fuel control valve combines the functions of the thrust controller turbine bypass and fuel vent valves. The GO_2 heat exchanger design is the same as that used on the Derivative II engines. The igniter system is also similar but of a more updated light weight design. The chamber/nozzle and turbopump assemblies differ from that of the Category IIA engine sufficiently to require some additional description.

a. Turbopump Assembly

To obtain two-phase pumping capability at full thrust, gear driven, low speed fuel and oxidizer inducers are used as shown in figure C-27. These inducers, which are of the same suction design as the inducers on the RL10 Derivative IIA, are gear driven from the high speed oxidizer pump. As with the current RL10, a two-stage fuel pump with a two-stage turbine and a single-stage oxidizer pump are used. The fuel impellers are fully shrouded to obtain high efficiency, since high tip speeds are not required. A single-stage oxidizer turbine is used in series with the fuel turbine to reduce the power transmitted through the gears so that their stresses are lower than in the RL10A-3-3. By gearing the pumps and inducers together, control problems during transient operation are avoided. The combination of nickel alloy turbines and the expander power cycle given turbine life margin.

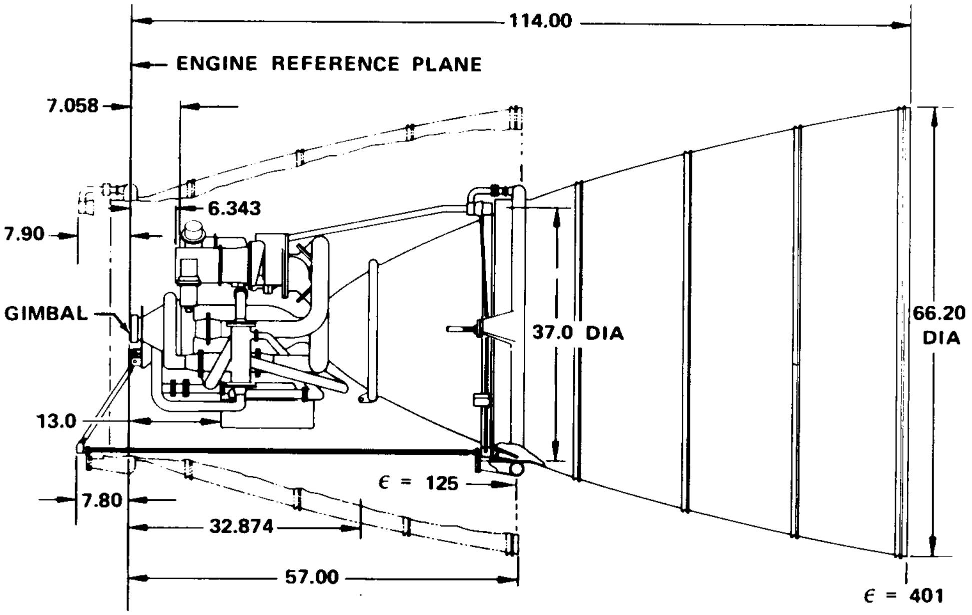

114.00
ENGINE REFERENCE PLANE
7.058
6.343
7.90
GIMBAL
37.0 DIA
66.20 DIA
13.0
ϵ = 125
7.80
32.874
57.00
ϵ = 401

Figure C-26(1). Category IV Engine Installation Drawing FD 72042A

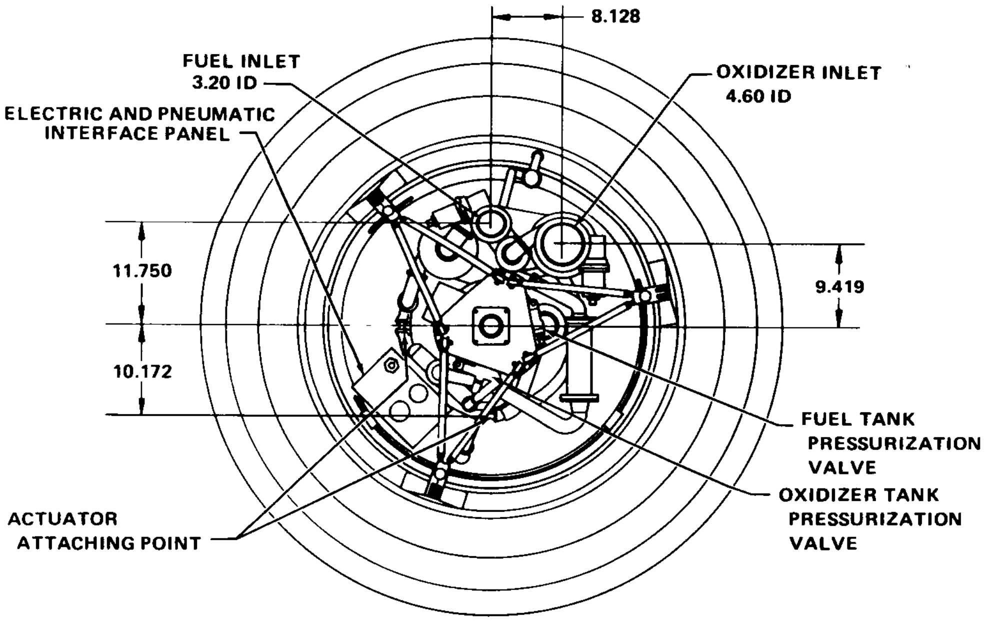

8.128
FUEL INLET 3.20 ID
OXIDIZER INLET 4.60 ID
ELECTRIC AND PNEUMATIC INTERFACE PANEL
11.750
10.172
9.419
FUEL TANK PRESSURIZATION VALVE
OXIDIZER TANK PRESSURIZATION VALVE
ACTUATOR ATTACHING POINT

Figure C-26(2). Category IV Engine Installation Drawing FD 72044A

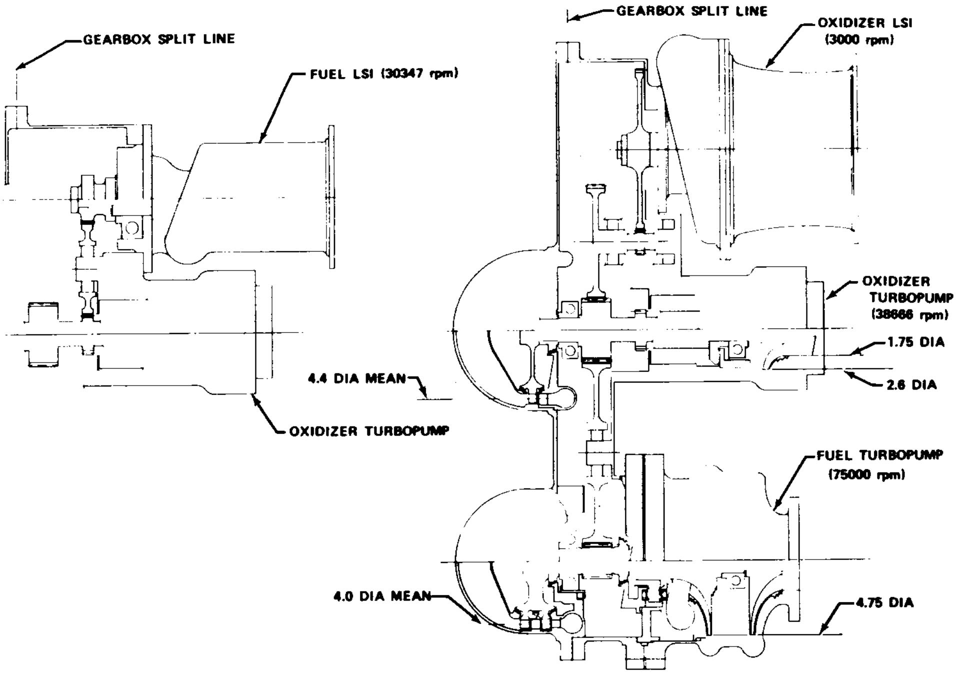

GEARBOX SPLIT LINE
FUEL LSI (30347 rpm)
4.4 DIA MEAN
OXIDIZER TURBOPUMP
GEARBOX SPLIT LINE
OXIDIZER LSI (3000 rpm)
OXIDIZER TURBOPUMP (38666 rpm)
1.75 DIA
2.6 DIA
FUEL TURBOPUMP (75000 rpm)
4.0 DIA MEAN
4.75 DIA

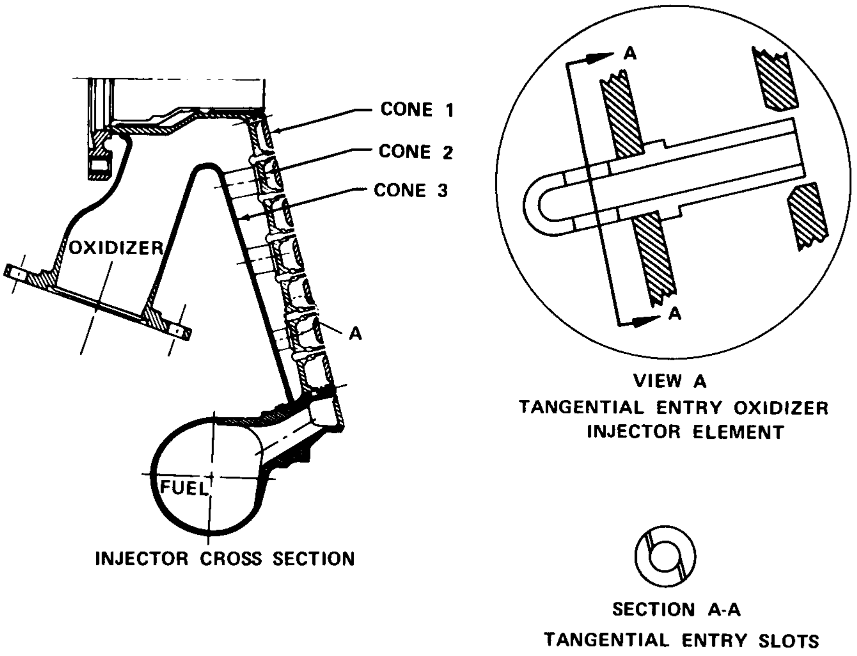
Figure C-27. Category IV Engine Turbopump Assembly FD 72073

b. Thrust Chamber and Nozzle Assembly

The Category IV engine thrust chamber/nozzle assembly consists of four major sections: (1) an injector, (2) a nontubular, regeneratively cooled section from the injector to an area ratio of 6, (3) a tubular regeneratively cooled nozzle from an area ratio of 6 to 125 and (4) a dump cooled extendible nozzle from an area ratio of 125 to 401. The design concepts of the extendible nozzle and tubular nozzle section are the same as that described for the Derivative IIA engine. The chamber sections and injector differ in design concept and are described below.

1. The propellant injector consists of multiple injector elements arranged in equally spaced concentric circles. Each element consists of an oxidizer tube and a concentric fuel orifice. The oxidizer is admitted to the injector tube through two tangential slots as shown in figure C-28. This swirls the oxidizer flow and promotes mixing with the hydrogen flow at the end of the element. Liquid oxygen enters the injector through the oxidizer injector manifold, flows into the cavity between cones 2 and 3, and then flows out of the oxidizer orifices and into the combustion chamber.

   Gaseous hydrogen enters the peripheral fuel injector manifold and flows into the cavity between cones 1 and 2. Most of the hydrogen flows out through the annular orifices around the elements, into the combustion chamber where it mixes with the oxidizer. Some of the hydrogen passes through cone 1, which consists of a porous welded, steel mesh plate. This flow provides transpiration cooling of the injector face (cone 1).



INJECTOR CROSS SECTION

VIEW A
TANGENTIAL ENTRY OXIDIZER
INJECTOR ELEMENT

SECTION A-A
TANGENTIAL ENTRY SLOTS

Figure C-28. Category IV Engine Injector FD 15541

2. The combustion chamber section has been fitted with a milled channel chamber, shown in figure C-29, to meet the thrust chamber life requirements. These channels vary in width and depth along their axial length to achieve the desired local coolant flow velocities. The chamber is manufactured from a copper alloy containing 99.85% oxygen free copper and 0.15% Zirconium which has improved thermal fatigue and strength properties over pure copper with similar thermal conductivity. A nickel outer wall is electroformed over the slotted passages of the copper alloy chamber to provide a structural shell and complete the cooling jacket. A copper alloy annular manifold serves as a transition piece between the nontubular nozzle with 90 flow passages, and the tubular nozzle with 180 flow passages.

2.4.3 Operation and Performance Characteristics

The operation of this engine is basically the same as that of the Derivative IIA engine as shown in the sequence chart in figure C-30. With the start solenoid and bypass solenoid the No. 1 energized, thermal conditioning in tank head idle mode is carried out with the engine operating as a pressure fed system without the turbopumps rotating. By de-energizing the bypass solenoid valve No. 1 and energizing the other two solenoids, the main fuel shutoff valve is opened and the turbine bypass valve in the main fuel control is first closed, diverting all fuel through the turbines, and as turbine inlet pressure builds up, reopened to allow the engine to stabilize at maneuver thrust level. De-energizing bypass solenoid valve No. 2 closes this bypass, which accelerates the engine to full thrust, where the thrust controller elements of the main fuel control regulate turbine bypass flow. The steady-state performance characteristics of the Category IV engine are summarized in figures C-31 through C-35 and in table C-4. Transient performance characteristics are defined in figures C-36 through C-39, and table C-5.

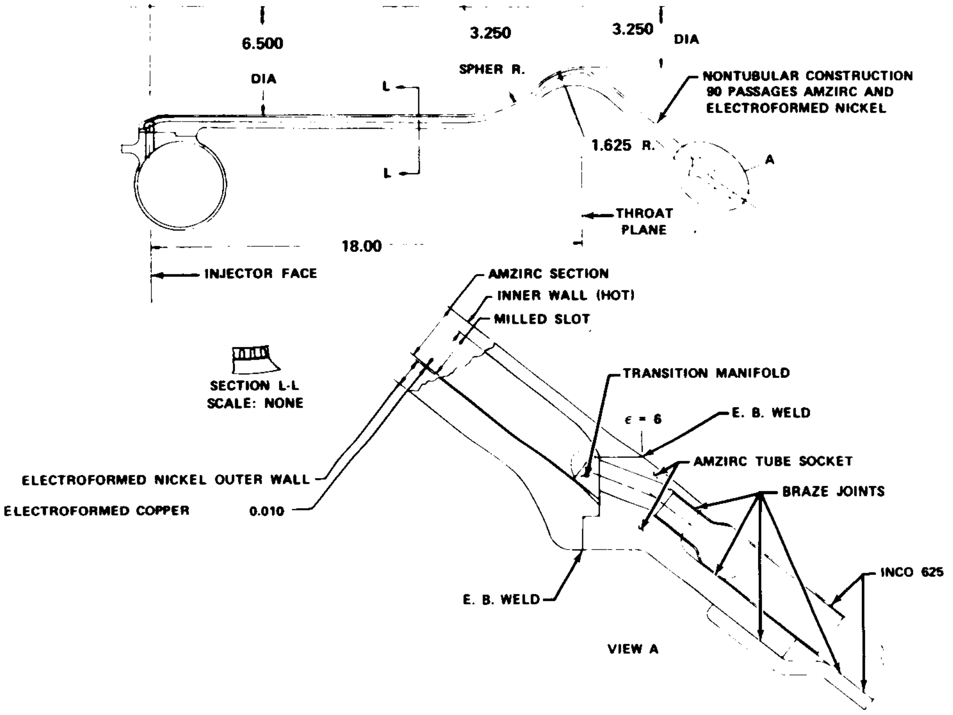
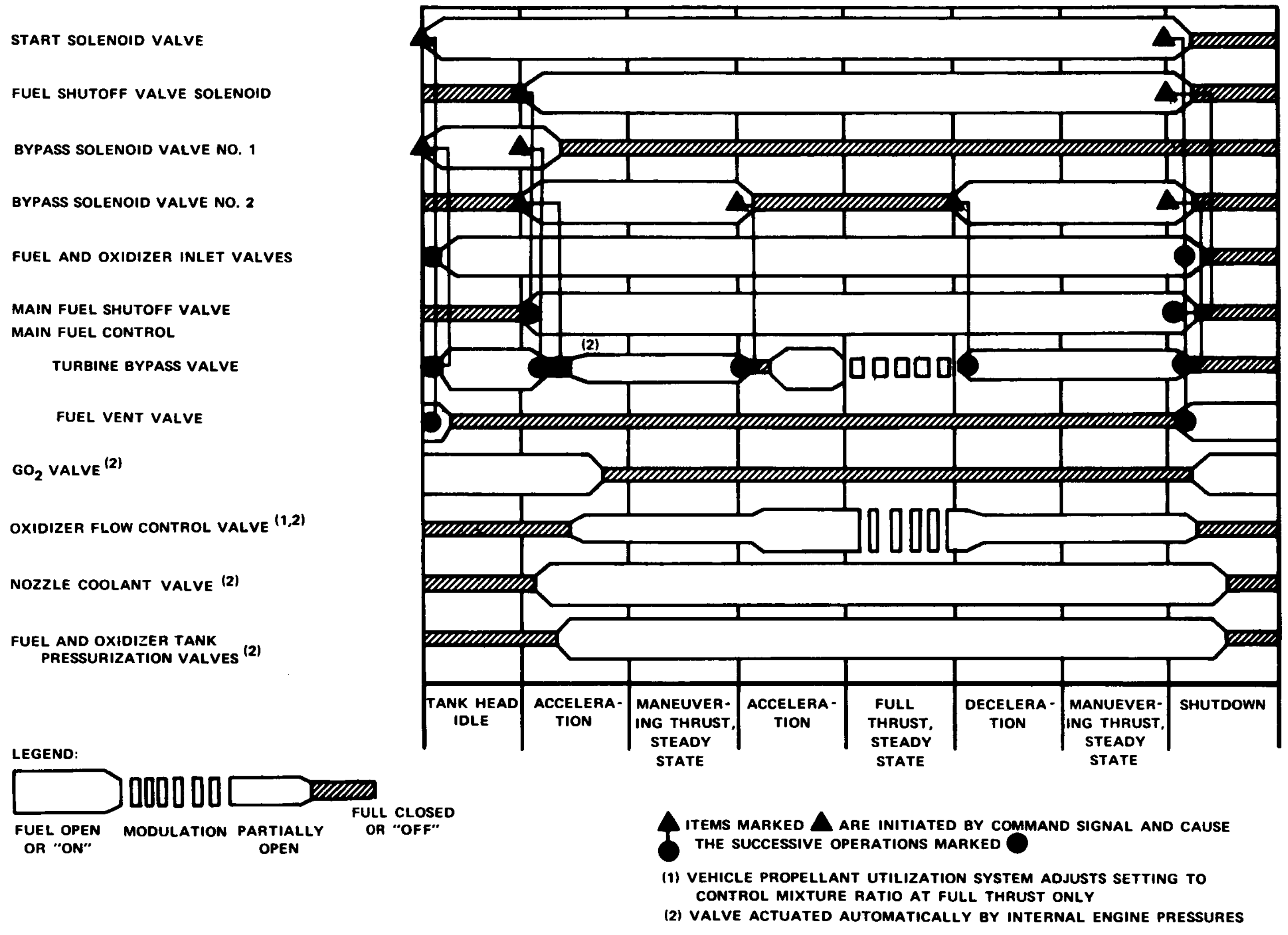

6.500
DIA
L
L
3.250
SPHER R.
3.250 DIA
NONTUBULAR CONSTRUCTION
90 PASSAGES AMZIRC AND
ELECTROFORMED NICKEL
1.625 R.
A
THROAT
PLANE
18.00
INJECTOR FACE
AMZIRC SECTION
INNER WALL (HOT)
MILLED SLOT
SECTION L-L
SCALE: NONE
TRANSITION MANIFOLD
ε = 6
E. B. WELD
AMZIRC TUBE SOCKET
BRAZE JOINTS
ELECTROFORMED NICKEL OUTER WALL
ELECTROFORMED COPPER 0.010
INCO 625
E. B. WELD
VIEW A

Figure C-29. Category IV Thrust Chamber FD 68747C


START SOLENOID VALVE
FUEL SHUTOFF VALVE SOLENOID
BYPASS SOLENOID VALVE NO. 1
BYPASS SOLENOID VALVE NO. 2
FUEL AND OXIDIZER INLET VALVES
MAIN FUEL SHUTOFF VALVE
MAIN FUEL CONTROL
TURBINE BYPASS VALVE
FUEL VENT VALVE
GO_2 VALVE (2)
OXIDIZER FLOW CONTROL VALVE (1,2)
NOZZLE COOLANT VALVE (2)
FUEL AND OXIDIZER TANK PRESSURIZATION VALVES (2)
(2)
TANK HEAD IDLE
ACCELERATION
MANEUVERING THRUST, STEADY STATE
ACCELERATION
FULL THRUST, STEADY STATE
DECELERATION
MANEUVERING THRUST, STEADY STATE
SHUTDOWN
LEGEND:
FUEL OPEN OR "ON"
MODULATION
PARTIALLY OPEN
FULL CLOSED OR "OFF"

▲ ITEMS MARKED ▲ ARE INITIATED BY COMMAND SIGNAL AND CAUSE THE SUCCESSIVE OPERATIONS MARKED ●

(1) VEHICLE PROPELLANT UTILIZATION SYSTEM ADJUSTS SETTING TO CONTROL MIXTURE RATIO AT FULL THRUST ONLY

(2) VALVE ACTUATED AUTOMATICALLY BY INTERNAL ENGINE PRESSURES

Figure C-30. Engine Operation Valve Sequence Category IV Engines

FD 75263

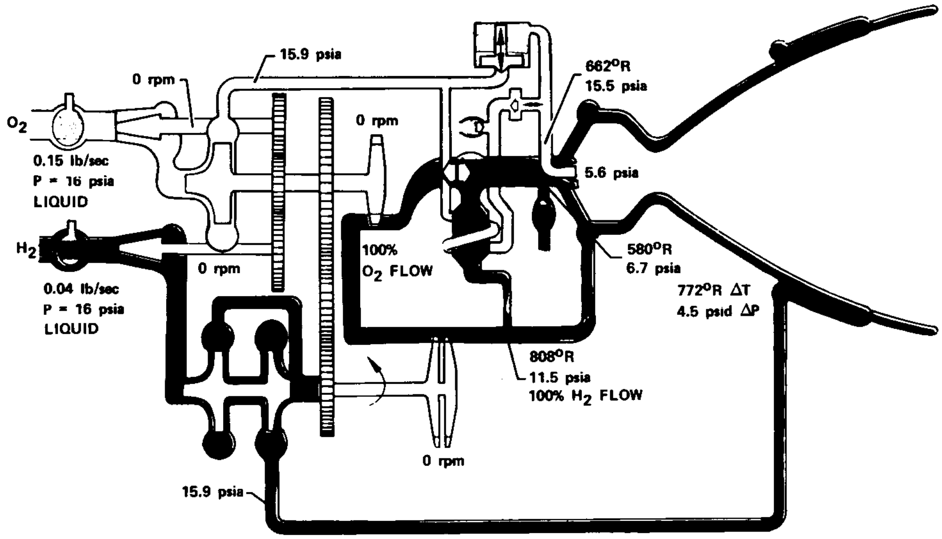

15.9 psia
0 rpm
O_2
0.15 lb/sec
P = 16 psia
LIQUID
0 rpm
662°R
15.5 psia
5.6 psia
H_2
0 rpm
100%
O_2 FLOW
0.04 lb/sec
P = 16 psia
LIQUID
580°R
6.7 psia
772°R ΔT
4.5 psid ΔP
808°R
11.5 psia
100% H_2 FLOW
0 rpm
15.9 psia

Figure C-31. Tank Head Idle Propellant Flow Schematic Category IV Engine FD 76251B

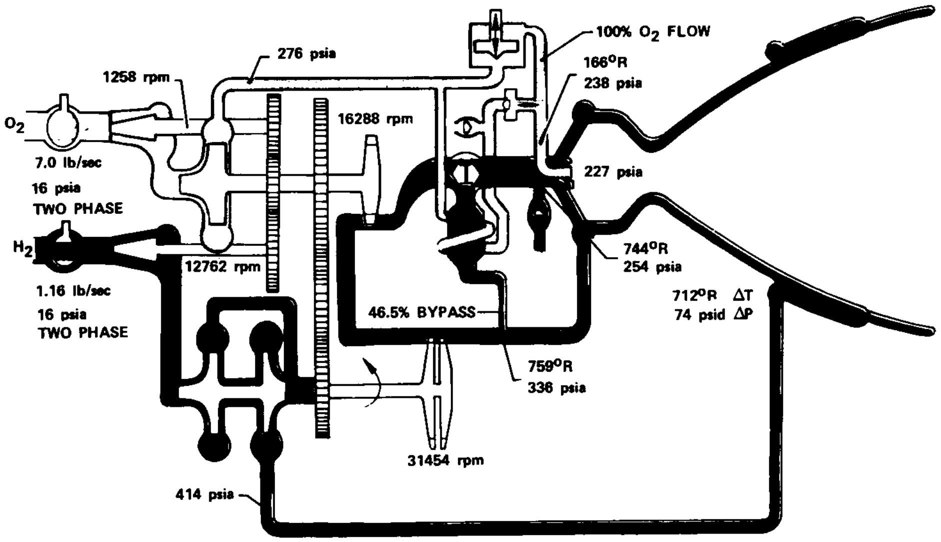

100% O_2 FLOW
276 psia
1258 rpm
166°R
238 psia
O_2
16288 rpm
7.0 lb/sec
16 psia
TWO PHASE
227 psia
H_2
12762 rpm
744°R
254 psia
1.16 lb/sec
16 psia
TWO PHASE
712°R ΔT
74 psid ΔP
46.5% BYPASS
759°R
336 psia
31454 rpm
414 psia

Figure C-32. Maneuver Thrust Propellant Flow Schematic Category IV Engine FD 76252B

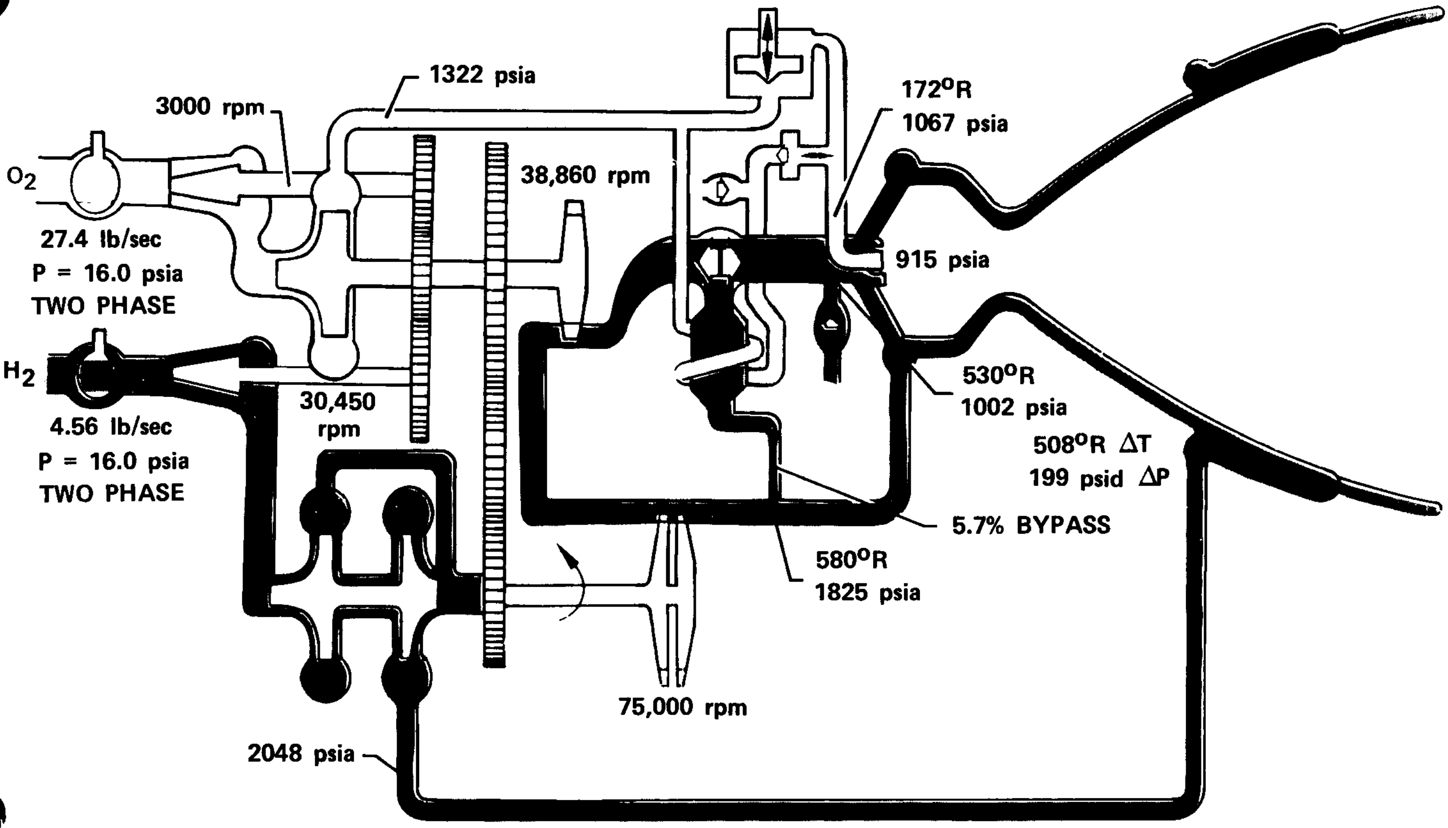
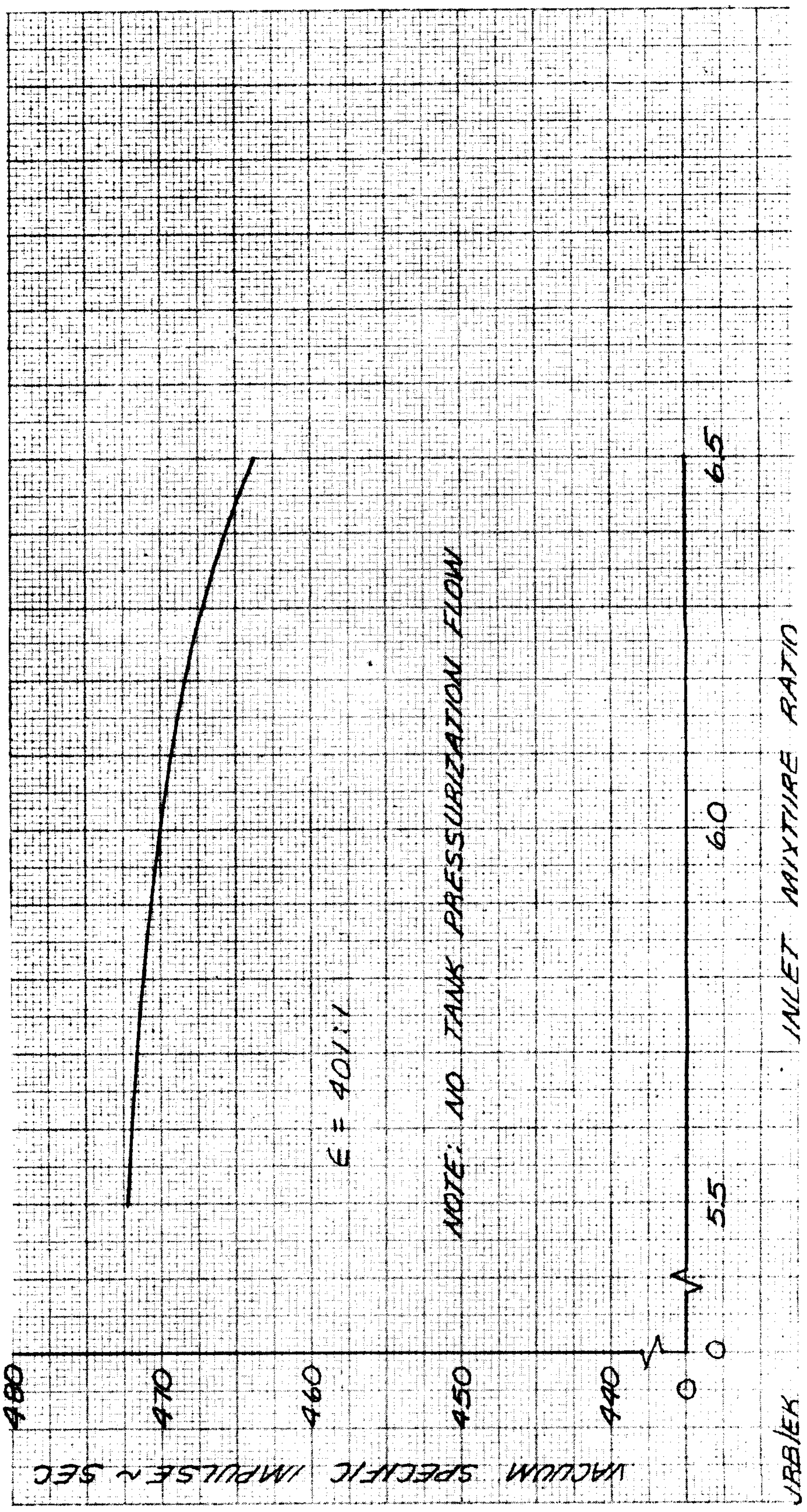

1322 psia
3000 rpm
172°R
1067 psia
O_2
38,860 rpm
27.4 lb/sec
P = 16.0 psia
TWO PHASE
915 psia
H_2
530°R
1002 psia
30,450 rpm
4.56 lb/sec
P = 16.0 psia
TWO PHASE
508°R ΔT
199 psid ΔP
5.7% BYPASS
580°R
1825 psia
75,000 rpm
2048 psia

Figure C-33. Category IV Engine Propellant Flow Schematic Full Thrust, MR = 6.0 FD 72919A



DF 96980

Figure C-34. Estimated Effect of Inlet Mixture Ratio on Vacuum Specific Impulse Category IV Engine Full Thrust

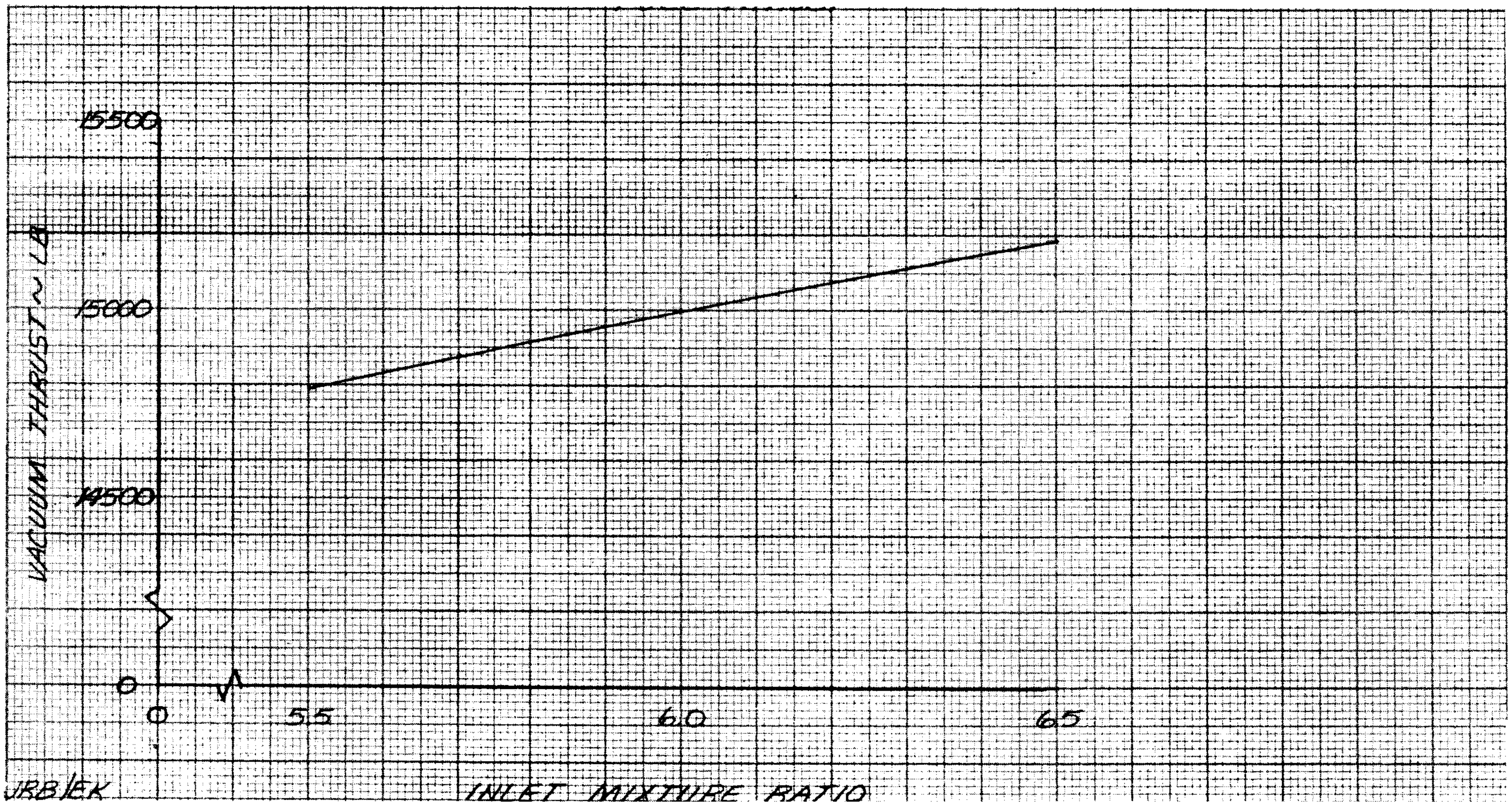


Figure C-35. Estimated Effect of Inlet Mixture Ratio on Vacuum Thrust Category IV Engine Full Thrust

DF 96981

Table C-4. Category IV Engine Steady-State Performance Summary

Full Thrust Performance	
Thrust, lb vac	15,000
Mixture Ratio	6.0
Chamber Pressure, psia	915
Specific Impulse, sec	470.0
Required Inlet Condition	
Fuel	<40% Vapor
Oxidizer	<40% Vapor
Maneuvering (Pumped Idle) Performance	
Thrust, lb vac	3,750.0
Mixture Ratio	6.0
Chamber Pressure, psia	234.0
Specific Impulse, sec	447.0
Required Inlet Condition	
Fuel	≤70% Vapor
Oxidizer	≤70% Vapor

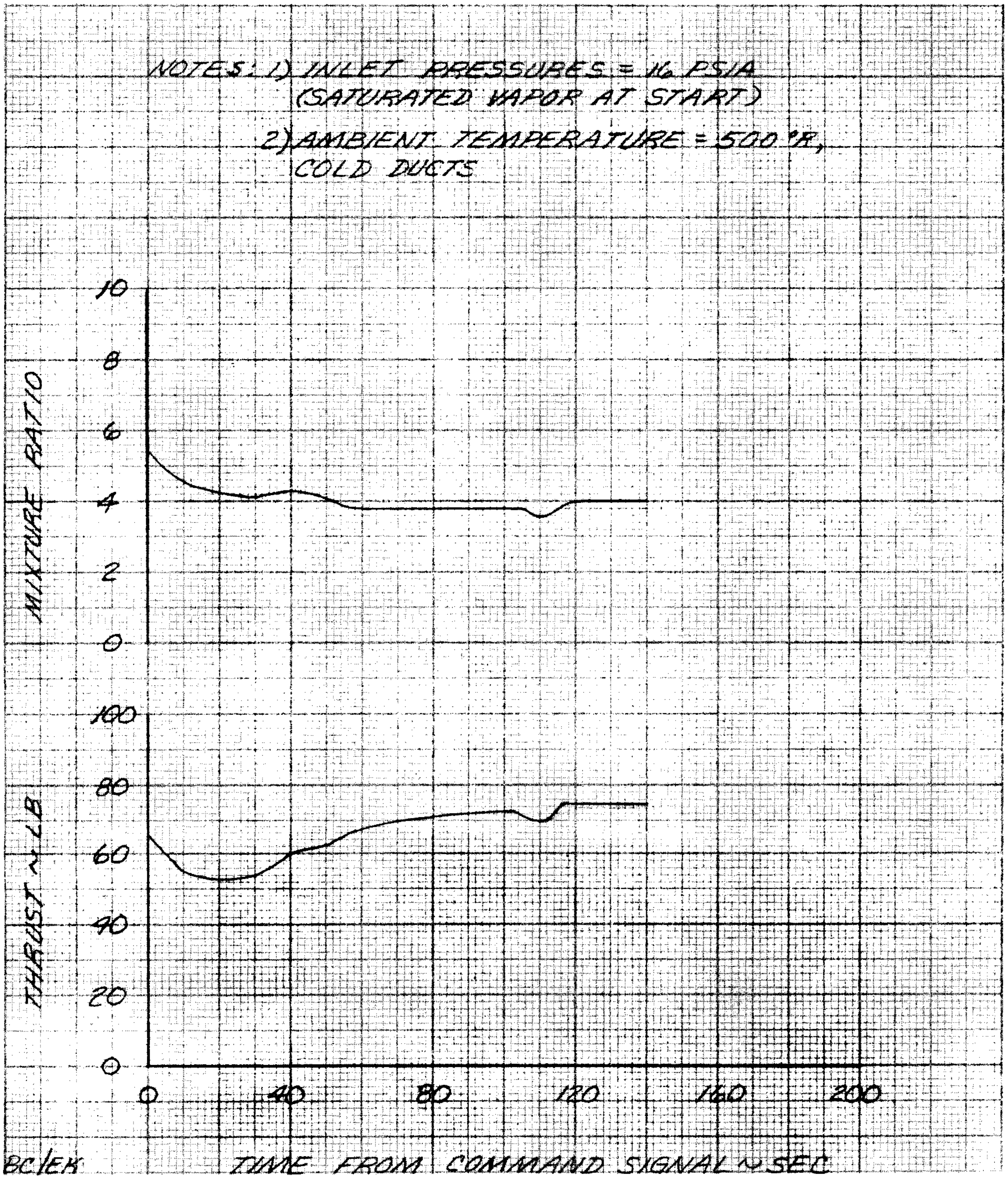


Figure C-36. Typical Cooldown Transient Category IV Engine Thrust and Mixture Ratio

DF 97607

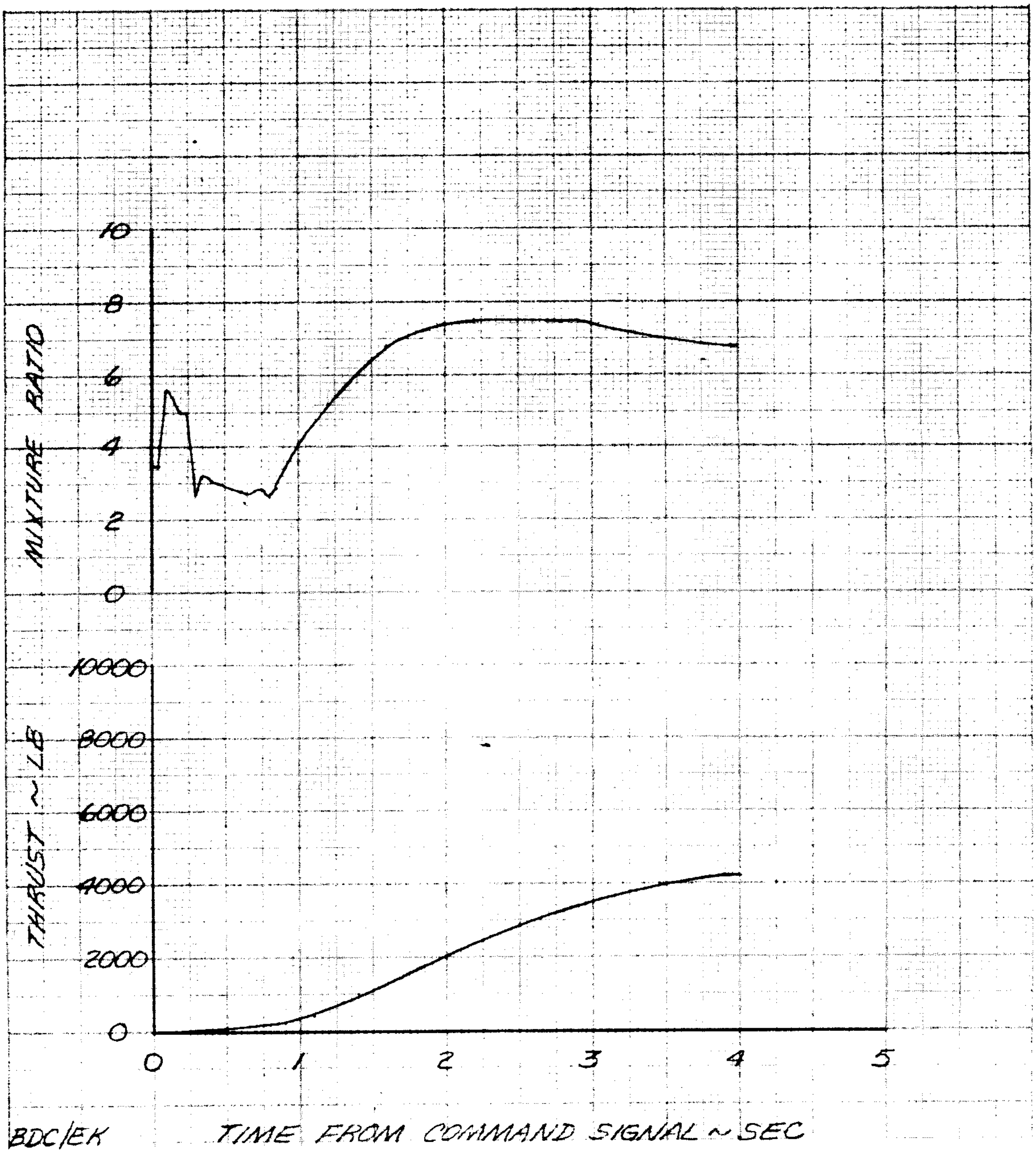


Figure C-37. Nominal Transient from Tank Head Idle to Maneuver Thrust Category IV Engine Thrust and Mixture Ratio

DF 97608

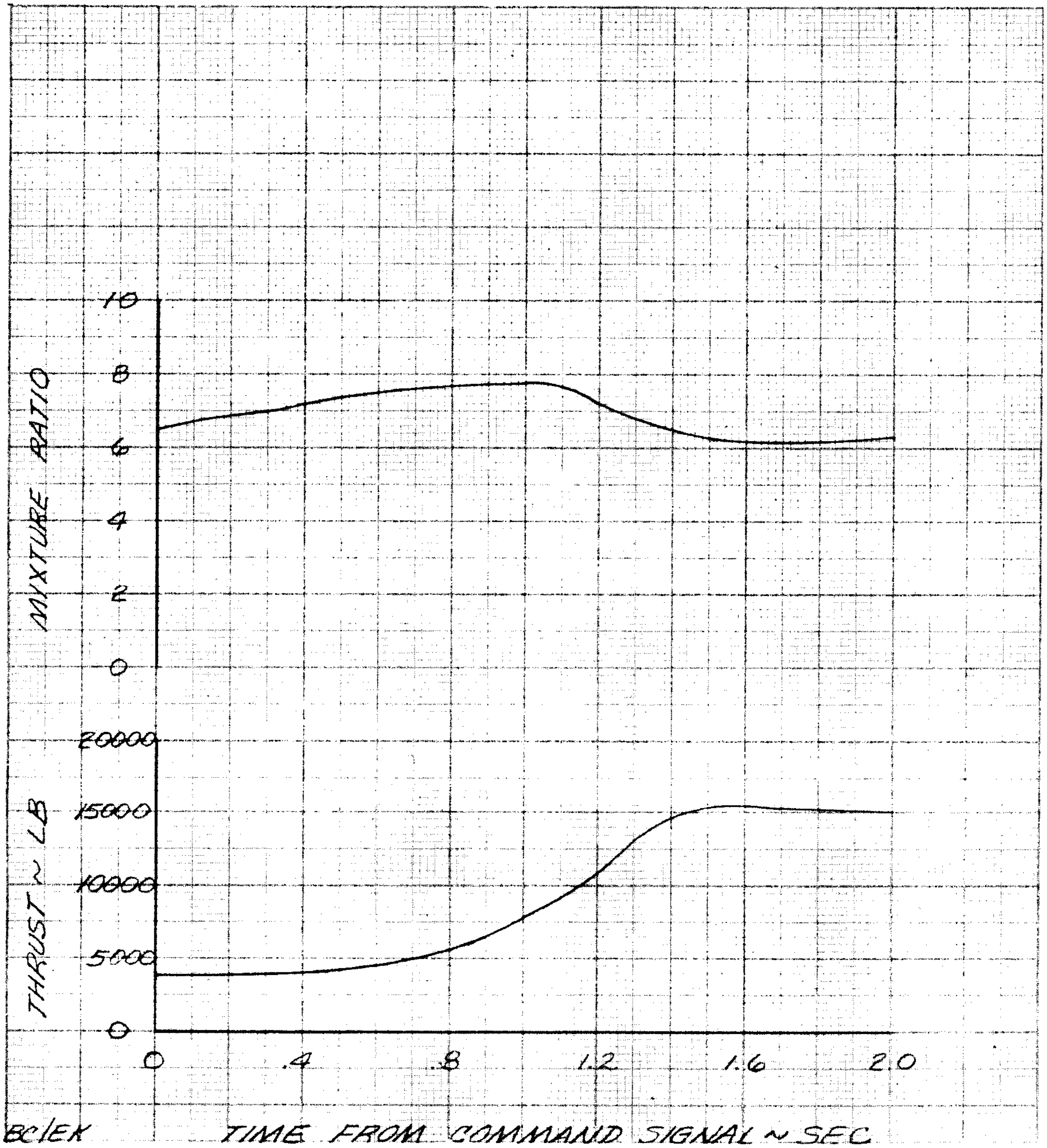
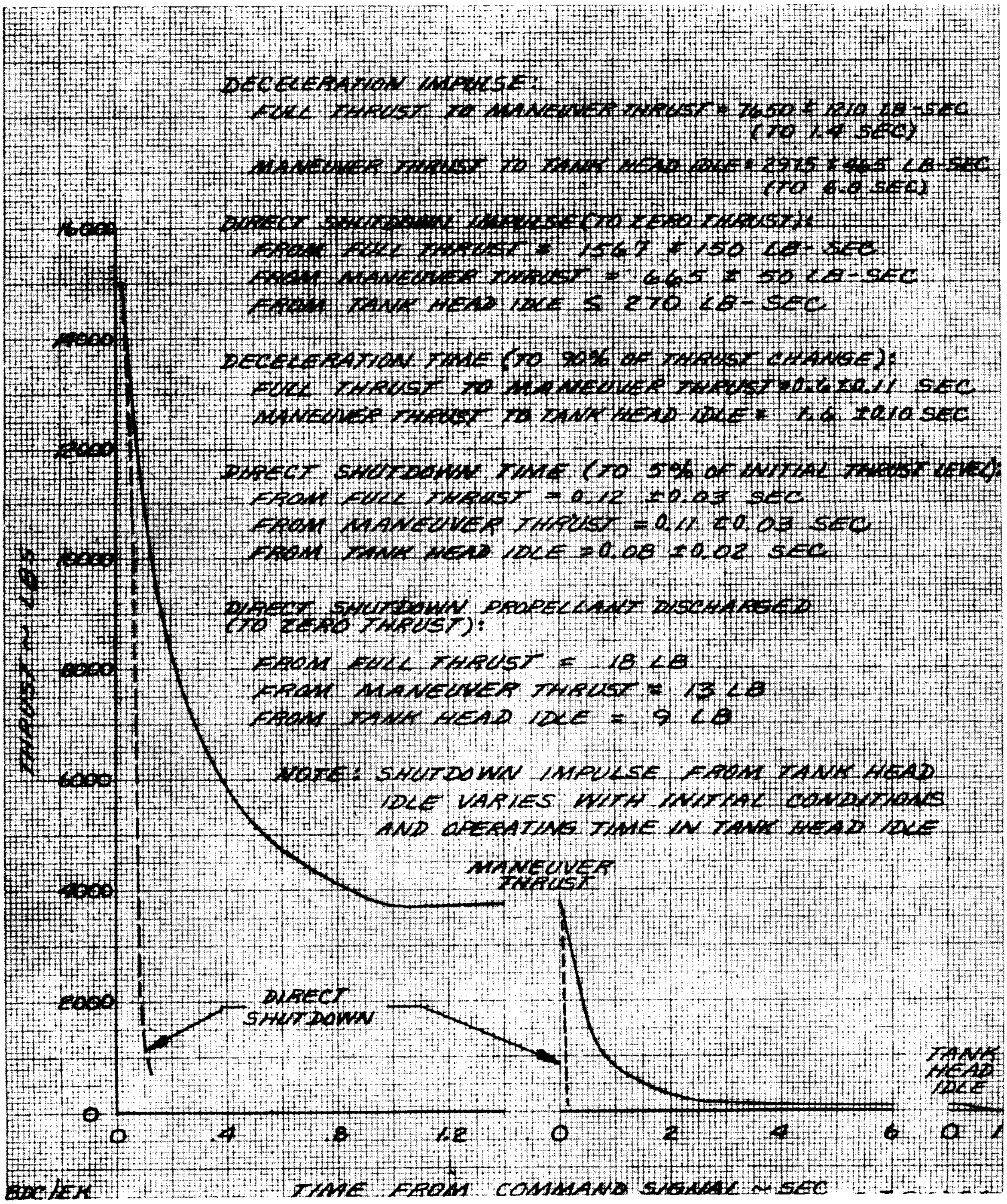


Figure C-38. Nominal Transient from Maneuver Thrust to Full Thrust Category IV Engine Thrust and Mixture Ratio

DF 97609



DECELERATION IMPULSE:
FULL THRUST TO MANEUVER THRUST = 7650 ± 1210 LB-SEC
(TO 1.4 SEC)
MANEUVER THRUST TO TANK HEAD IDLE = 2915 ± 465 LB-SEC
(TO 6.8 SEC)

DIRECT SHUTDOWN IMPULSE (TO ZERO THRUST):
FROM FULL THRUST = 1567 ± 150 LB-SEC
FROM MANEUVER THRUST = 665 ± 50 LB-SEC
FROM TANK HEAD IDLE ≤ 270 LB-SEC

DECELERATION TIME (TO 90% OF THRUST CHANGE):
FULL THRUST TO MANEUVER THRUST = 0.6 ± 0.11 SEC
MANEUVER THRUST TO TANK HEAD IDLE = 1.6 ± 0.10 SEC

DIRECT SHUTDOWN TIME (TO 5% OF INITIAL THRUST LEVEL):
FROM FULL THRUST = 0.12 ± 0.03 SEC
FROM MANEUVER THRUST = 0.11 ± 0.03 SEC
FROM TANK HEAD IDLE = 0.08 ± 0.02 SEC

DIRECT SHUTDOWN PROPELLANT DISCHARGED
(TO ZERO THRUST):
FROM FULL THRUST = 18 LB
FROM MANEUVER THRUST = 13 LB
FROM TANK HEAD IDLE = 9 LB

NOTE: SHUTDOWN IMPULSE FROM TANK HEAD
IDLE VARIES WITH INITIAL CONDITIONS
AND OPERATING TIME IN TANK HEAD IDLE

THRUST ~ LBS
16000
14000
12000
10000
8000
6000
4000
2000
0
MANEUVER THRUST
DIRECT SHUTDOWN
TANK HEAD IDLE
0
.4
.8
1.2
0
2
4
6
0
1
TIME FROM COMMAND SIGNAL ~ SEC
DDC/EK

Figure C-39. Estimated Deceleration and Shutdown Transients from Full Thrust to Tank Head Idle Category IV Engine

DF 97084

Table C-5. Transient Performance Summary Category IV Engine

Typical Tank Head Idle Transient		
Initial Thrust, lb	61	
Final Thrust, lb	73	
Cooldown Time in sec(4)	120	
Start Transient		
Tank Head to Maneuver Thrust Time, sec(1)	2.78	± 0.36
Impulse, lb-sec(7)	4,296	± 1330
Maneuver Thrust to Full Thrust Time, sec(1)	1.38	± 0.010
Impulse, lb-sec(2)	18,075	± 1350
Deceleration Transient		
Full Thrust to Maneuver Thrust Time, sec(1)	0.6	± 0.11
Impulse, lb-sec(3)	7,650	± 1210
Maneuver Thrust to Tank Head Idle Time, sec(1)	1.6	± 0.10
Impulse, lb-sec(6)	2,975	± 465
Shutdown Transient		
From Full Thrust Time, sec(5)	0.12	± 0.03
Impulse, lb-sec	1,567	± 150
Propellants Discharged, lb	18	
From Pumped Idle Time, sec(5)	0.11	± 0.03
Impulse, lb-sec	665	± 50
Propellants Discharged, lb	13	
From Tank Head Idle Time, sec(5)	0.08	± 0.02
Impulse, lb-sec(8)	≤ 270	
Propellants Discharged, lb	9	

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(1) To 90% of Thrust Change

(2) 2.0-sec Duration

(3) 1.4-sec Duration

(4) Tank Pressure = 16 psia, Initial Engine Temperature = 500°R, Cold Inlet Lines

(5) To 5% of Initial Thrust Level

(6) 6-sec Duration

(7) 3-sec Duration

(8) Shutdown impulse from THI varies with initial conditions and operating time in THI.

3. REQUIREMENTS DEFINITION

The previous study (Reference 1) and Critical Elements Evaluation efforts were primarily directed toward the establishment of general capabilities and limitations needed to select the baseline engine design goals. While they provided general "yes" and "no" answers relative to the engine capability and vehicle system requirements they did not provide design requirements data in sufficient detail to allow the completion of conceptual design layout drawings required under this contract. Additional data were needed in many areas. This data were provided by an iterative design procedure between transient and cycle studies and mechanical design. Component requirements were initially defined using engine cycle and transient results from the previous studies. Mechanical and analytical design studies of these components were then made using these initial requirements as guidelines. As soon as the component characteristics were defined by the mechanical and analytical design studies they were incorporated into the cycle and transient computer programs and evaluated. Where required, this iteration was continued until no further redesigns were required.

This section presents the studies and methods used to define the final baseline engine configurations. Both steady-state and transient analysis are addressed.

3.1 Steady-State Engine Cycle Studies

The engine cycle computer programs used in this study were nonlinear representations of the complete engines and they defined in detail the various engine component design point and off-design characteristics and the interaction effects of these components. They permitted detailed studies of the engine/component characteristics to be made over the complete range of operating conditions including a definition of control valve requirements. These programs also provided the capability of evaluating the effect of differences in component operating characteristics and component location within the engine to optimize engine operation.

Discrete programs were used for each engine. The Derivative IIB engine cycle program was constructed primarily of component characteristics of the RL10A-3-3 engine obtained over years of RL10 engine and component testing. The cycle program used for the Derivative IIA engine was essentially the same as the Derivative IIB program. However, it reflected the change in fuel pump performance characteristics (also obtained from RL10 test data) required to obtain two-phase fuel pumping capability and it reflected the addition of the low speed inducer located upstream of the oxidizer pump.

To define the Category IV engine cycle characteristics, two cycle computer programs were used. A design point cycle program which selected the optimum design point characteristics for the components was used initially, and then an off-design program was used to obtain off-design characteristics. The design point computer program was initially generated using theoretical component characteristic curves; then as design studies were made of each of the critical components, the characteristics from these studies were incorporated. After the design point was fully defined, an off-design cycle program was generated by combining off-design component characteristics determined analytical studies and various RL10 component and engine tests, with the design point cycle characteristics defined by the design point computer program.

Of the more important steady-state cycle studies nine were selected for inclusion in this report and are described in the following sections. These studies cover the following areas:

a. Derivative IIB Engine NPSH Requirements
b. GO_2 Heat Exchanger Location
c. Derivative II Engine Power
d. Pumped Idle Thrust Level
e. Extendible Nozzle Coolant Flow Rate
f. Category IV Engine Cycle Optimization
g. Off-Design Engine Operating Characteristics
h. Specific Impulse Estimates
i. Instrumentation

3.1.1 Derivative IIB Engine NPSH Requirements

Derivative IIB engine NPSH requirements were defined using suction specific speed correlations of RL10A-3-3 engine and pump rig test data. Suction Specific Speed (N_{SS}) defines inlet suction capability as a function of pump speed, flowrate and fluid properties as defined below:

$$\text{Suction Specific Speed} = \frac{N\sqrt{Q}}{(\text{NPSH} + \text{TSH})^{3/4}}$$

N = pump rotational speed

Q = pump volumetric flowrate

NPSH = net positive suction head

TSH = thermodynamic suppression head at inlet conditions

Since the suction performance of pumps using cryogenic fluids varies with fluid inlet temperature, the test values of NPSH must be corrected for fluid thermodynamic suppression head (TSH) prior to correlating the data. The TSH values shown in figures C-40 and C-41 were taken from RL10 and other pump data available in published reports. The correction for TSH was accomplished as shown in the equation above by adding the predicted TSH value to the measured NPSH value. The final suction performance correlations are respectively shown in figures C-42 and C-43 for the fuel and oxidizer pumps.

The Derivative IIB engine suction requirements were defined using predicted engine pump speeds and inlet volumetric flowrates and estimated vehicle inlet conditions. Minimum NPSH requirements were calculated from

$$\text{NPSH} = \left(\frac{N\sqrt{Q}}{N_{SS}}\right)^{4/3} - \text{TSH}$$

A 50% margin was applied to the minimum NPSH values to account for pump variation and TSH uncertainty. The recommended values of NPSH for the Derivative IIB engine at full thrust are 14 ft fuel and 7.5 ft oxidizer as shown in table C-6.

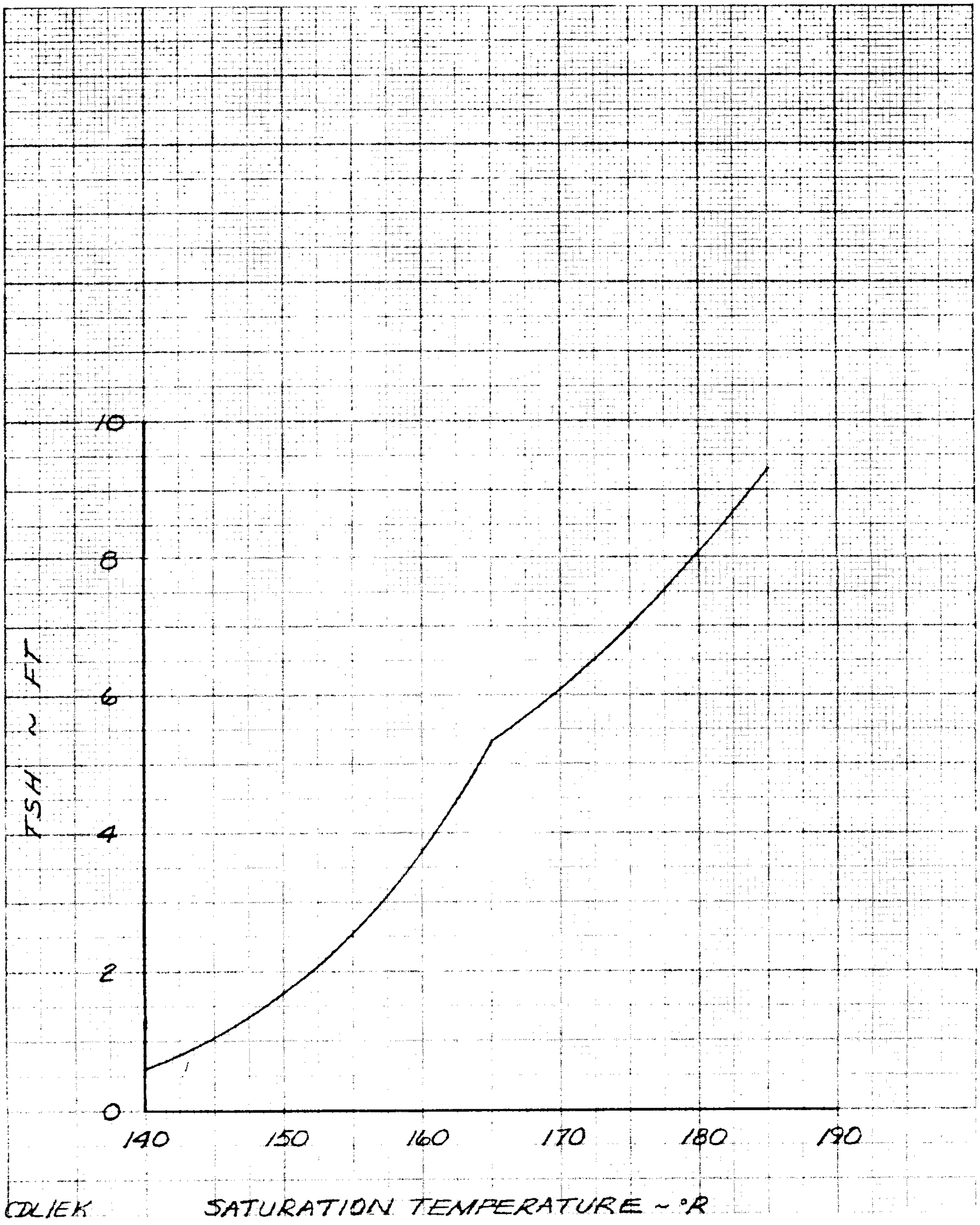


Figure C-40. Thermodynamic Suppression Head (TSH) Adjustment for Oxygen

DF 97683

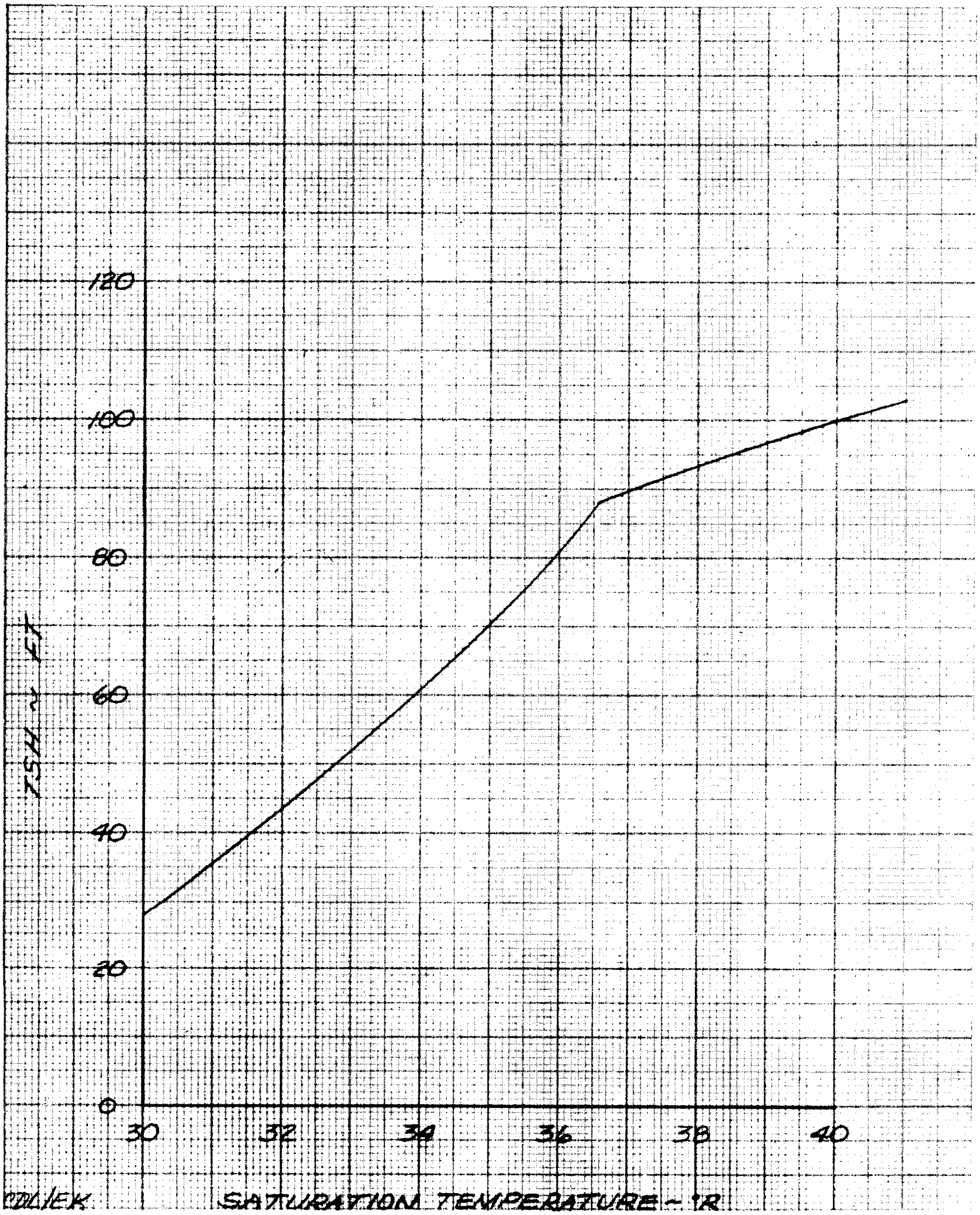


Figure C-41. Thermodynamic Suppression Head (TSH) Adjustment for Hydrogen DF 97684

$$N_{SS} = \frac{N\sqrt{Q}}{(NPSH + TSH)^{3/4}}$$

N_{SS} = SUCTION SPECIFIC SPEED
N = SPEED, RPM
Q = FLOWRATE, GPM
$NPSH$ = MEASURED NET POSITIVE SUCTION HEAD
TSH = THERMODYNAMIC SUPPRESSION HEAD

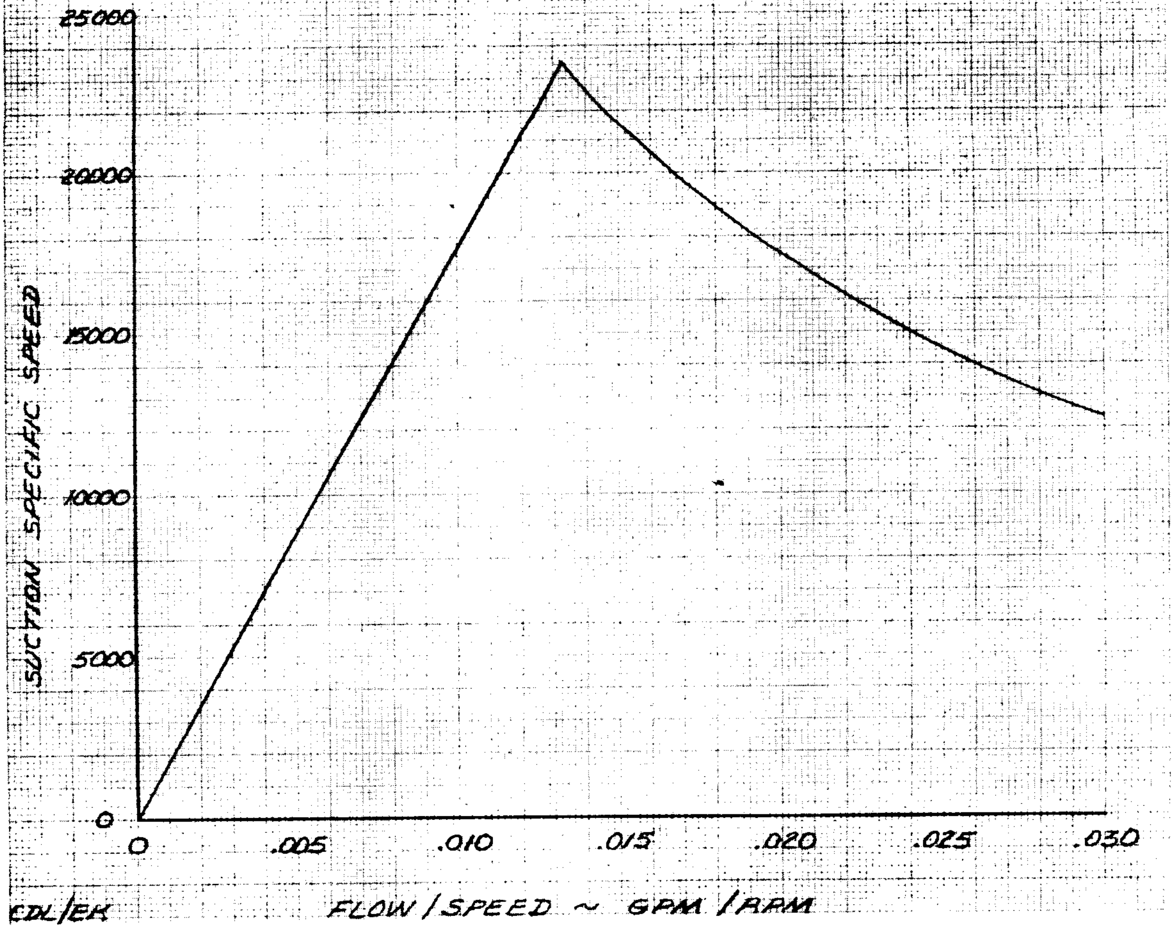


Figure C-42. RL10A-3-3 Fuel Pump Suction Specific Speed and Characteristics

DF 97089

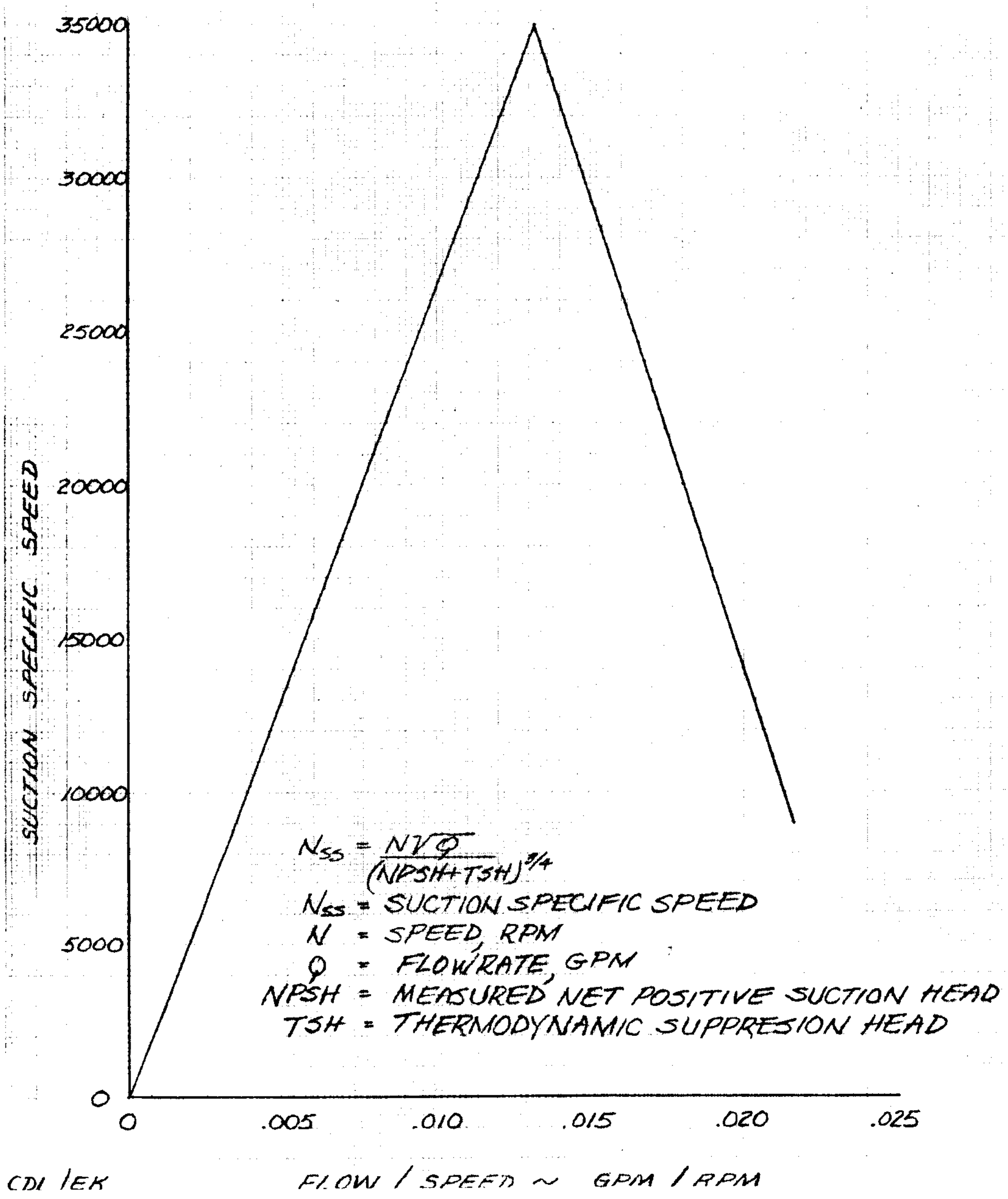


Figure C-43. RL10A-3-3 Oxidizer Pump Suction Specific Speed and Characteristics DF 97088

Table C-6. Derivative IIB NPSH Values

	Hydrogen	Oxygen
N, rpm	28,380	11,350
Q, gpm	478.8	179
$Temp_{inlet}$ °R	36.9	163.8
N_{SS}	19,800	27,000
TSH, ft	89.7	5.1
NPSH, * ft	13.8	7.5

*with 50% operating margin

3.1.2 GO_2 Heat Exchanger Location

For the purposes of feasibility evaluation the GO_2 heat exchanger was placed in the turbine discharge line during the previous study and the Critical Elements Evaluation portion of this study. Since all of the engine fuel flow is routed through the heat exchanger in this location, as shown in figure C-44, the heat exchanger design required a large effective fuel flow area to minimize fuel pressure loss at full thrust. An excessive pressure loss could not be tolerated because it significantly increased turbine back pressure and reduced engine power. A relatively bulky, inefficient heat exchanger design resulted.

Cycle studies were conducted using the Derivative II engine cycle computer programs to determine if a better location could be found for the GO_2 heat exchanger. These studies evaluated operation with the heat exchanger in the turbine inlet line, turbine discharge line and the turbine bypass line. While the turbine inlet location was preferable to the turbine discharge location because of the increase in allowable pressure drop for the same loss in turbine power, it still utilized total engine flow at full thrust. The studies indicated that the optimum location for the heat exchanger is in the turbine bypass line as shown in figure C-45.

At tank head idle when the fuel flow is low but the heat transfer requirements per unit of fuel flow are high, all of the fuel flow (0.08 lb/sec) would go through the turbine bypass line and through the heat exchanger. At pumped idle when total fuel flow is higher but heat transfer requirements per unit of total fuel flow are lower only a portion (0.41 lb/sec) would be going through the heat exchanger. The flowrate required at full thrust was estimated to be only 1% of the total fuel flow and it was determined that this amount could bypass the turbine without significantly affecting the engine power capability. The oxidizer heat transfer requirements for both the pumped idle and tank head idle operation conditions were also evaluated and it was found that the amount of fuel going through the heat exchanger under both conditions would be sufficient to meet the heat transfer requirements.

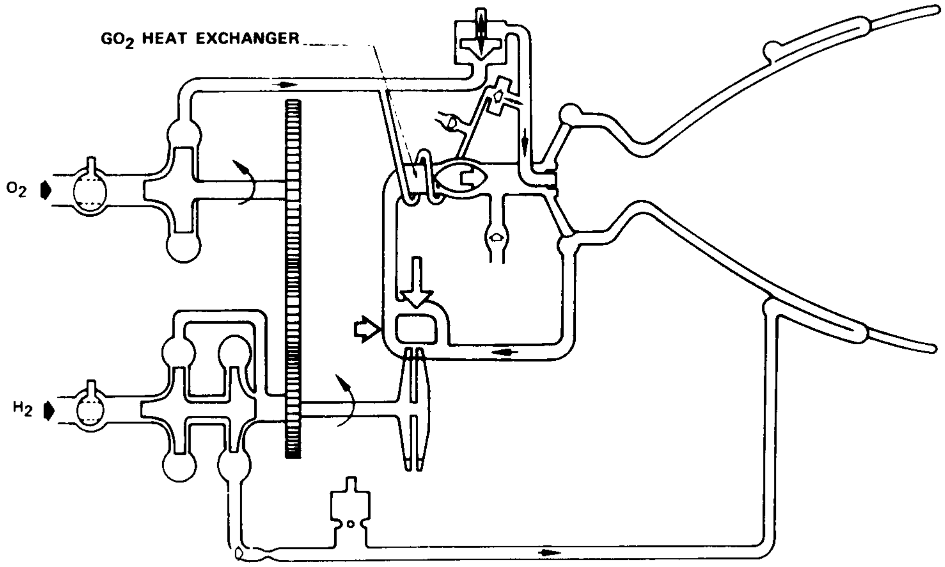

GO_2 HEAT EXCHANGER
O_2
H_2

Figure C-44. Derivative IIB Propellant Flow Schematic - GO_2 Heat Exchanger in Turbine Discharge Line FD 72586B

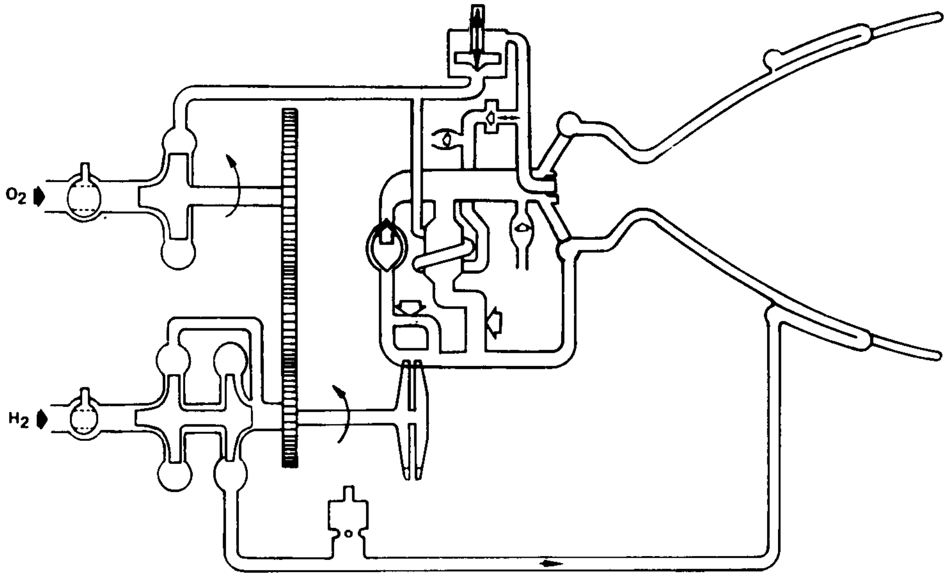


Figure C-45. Derivative IIB Propellant Flow Schematic - GO_2 Heat Exchanger in Turbine Bypass Valve FD 72587

Since the heat exchanger did not have to handle high fuel flowrates at any of three operating modes, and since it could be designed with relatively high pressure losses, a much more compact, highly efficient design was possible. With the resulting design, oxidizer tank pressurization flowrate capability increased from 0.5 lb/sec to 2.0 lb/sec during pumped idle operation, the heat exchanger weighed 5 lb less, and its volume was reduced by 270 cu inch. Design of this heat exchanger is discussed in Section C.4.1.4.

3.1.3 Derivative II Engine Power

The Derivative II engine primary nozzle design surface area is less than that of the RL10A-3-3 engine. While primary nozzle area ratio is equivalent (66 vs 57), the nozzle expands to the primary area ratio much more rapidly and surface area is therefore reduced as depicted in figure C-46. This reduction in nozzle surface area significantly reduces the potential heat pickup which in turn reduces available turbine power. Cycle studies were conducted to determine the best method of recovering engine power to retain the same turbine excess power margin during off-design, full thrust operation.

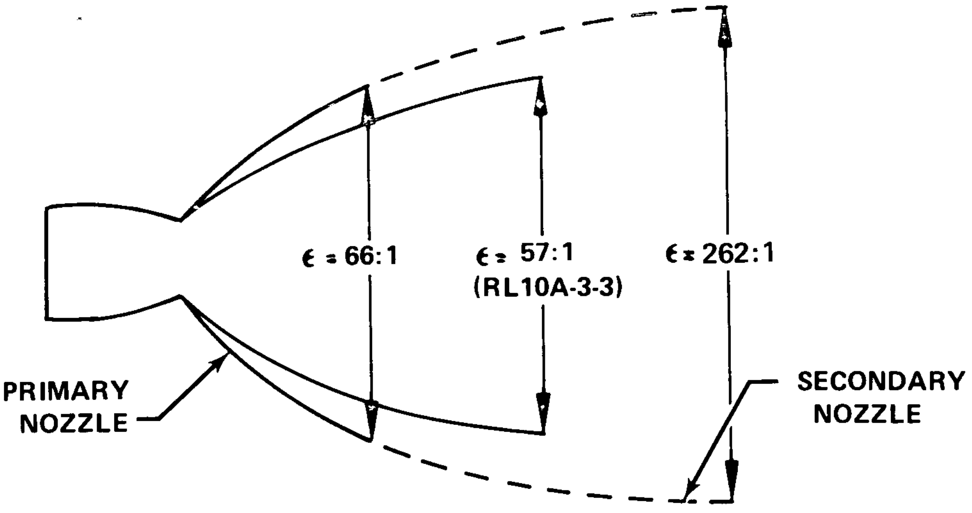


Figure C-46. Primary Nozzle Surface Area Comparison FD 76258

These studies identified two potential methods of recovering the power. They were to (1) increase the thrust chamber coolant temperature rise by increasing combustion chamber length 2.25 in., or (2) reduce the effective area of the turbine 23% to better match the turbine to the reduced fuel flowrate encountered at a mixture ratio of 6.0. The Derivative II engine is designed to use the RL10A-3-3 turbine originally sized for operation at a mixture ratio of 5.0.

The chamber length modification was selected to minimize changes to the RL10A-3-3 components. The RL10A-3-3 engine chamber/nozzle must be redesigned to match the Derivative II nozzle contour, as described above, and since the primary nozzle is shorter than the extendible nozzle, additional chamber length could be added without increasing engine installed length. In addition, the increase in chamber length should aid in attaining an increase in combustion performance for the engine at mixture ratio of 6.0. The heat transfer gain as a function of chamber length is discussed in Section 4.1.1.

3.1.4 Pumped Idle and Maneuver Thrust Level

Analysis of pumped idle operating characteristics during the Critical Elements Evaluation showed that, at 10% thrust, the engine would encounter some low frequency combustion instability (±4.5 psi). Fuel system instability caused by boiling in the regeneratively cooled thrust chamber could be avoided through the use of a cavitating venturi. In addition the oxidizer tank pressurization flow was limited to 0.5 lb/sec, a level below that defined for optimum vehicle operation.

Cycle studies were conducted using the Derivative IIB engine simulation to determine the effect on the engine cycle and operating characteristics of increasing the pumped idle thrust level. These studies revealed that it would be advantageous to increase the Derivative IIB pumped idle thrust level to 25%. This level was selected because RL10A-3-7 engine data showed that chugging instability would not occur at that thrust level and a cavitating venturi would not be required, since fuel pump discharge conditions would be above the critical pressure of hydrogen. These stability characteristics are depicted in figure C-47.

In addition, with the higher thrust level, oxidizer tank pressurization flowrate could be increased to 2 lb/sec and specific impulse could be increased by 10 points.

The only disadvantage noted in raising thrust level is a small reduction in the vapor pumping capabilty of the engine at pumped idle. This disadvantage did not appear significant, however, since studies showed that with this thrust level the vehicle acceleration would provide propellants above saturated liquid conditions. Table C-7 summarizes the results of increasing the pumped idle thrust level to 25% for the Derivative IIB engine.

The 25% thrust level was also established for the Derivative IIA and Category IV engines during maneuver thrust operation to improve phase development.

3.1.5 Extendible Nozzle Coolant Flow

During the previous study (Ref 1) the extendible nozzle was assumed to be cooled by normal gearbox leakage levels. Analysis during Critical Elements Evaluation showed that this flow was inadequate and would have to be supplemented to provide the required nozzle coolant. Increased coolant flow was obtained by increasing the flow area of the gearbox coolant passages and increasing the leakage through the fuel pump seals.

Cycle studies were conducted to determine the effect on the engine cycle of these increased nozzle coolant flowrates. These studies indicated that engine power is essentially unaffected. The higher chamber mixture ratio due to increased nozzle coolant flowrate increased the thrust chamber temperature rise compensating for the decrease in available turbine flow and resulting in insignificant change in engine power. The effect on specific impulse at full thrust was also evaluated and it was determined that engine specific impulse would be decreased approximately 2 seconds. Most of the loss in specific impulse was caused by the higher chamber mixture ratio which increased from 6.08 to 6.36.

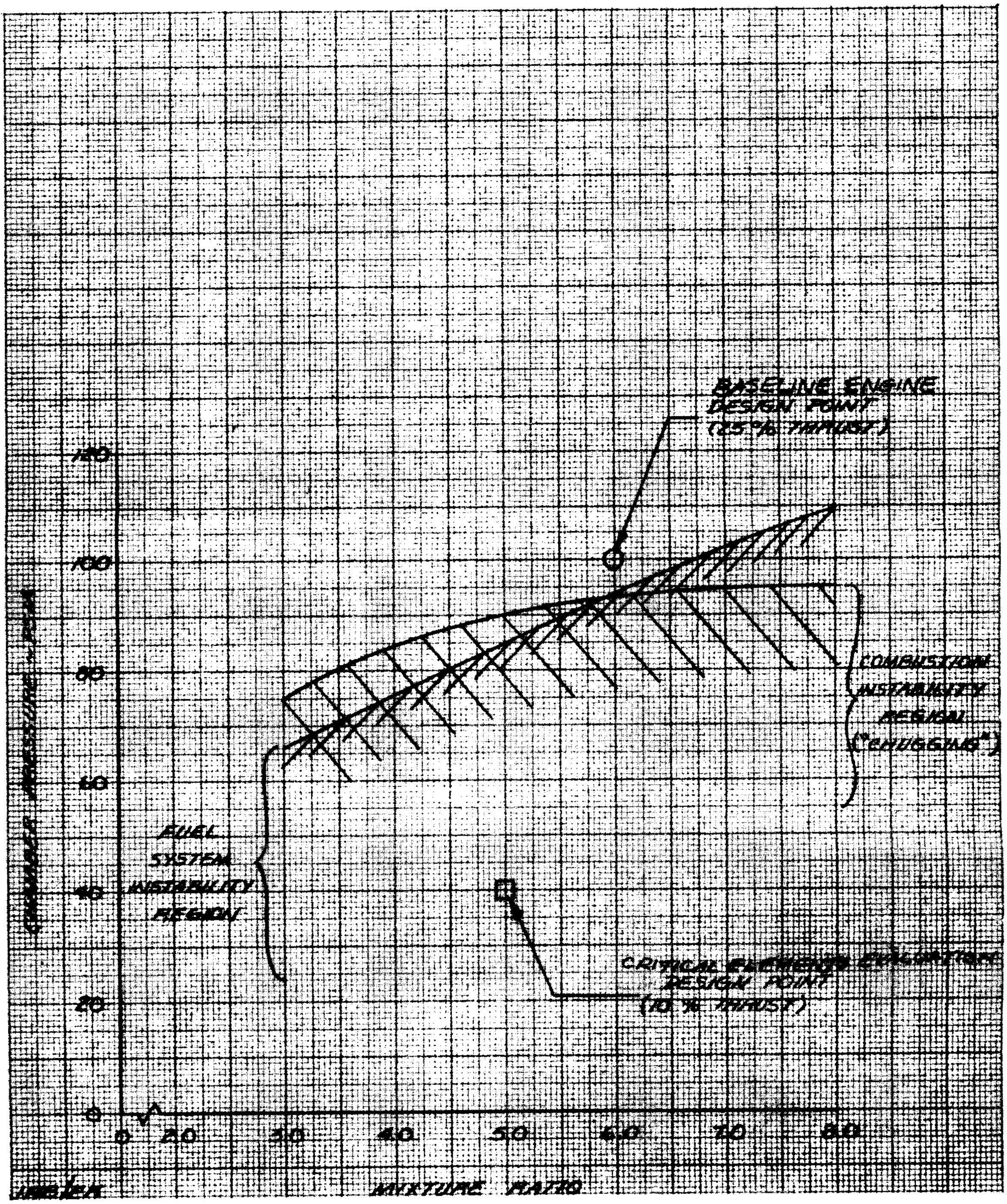


Figure C-47. Estimated Engine Stability Limits Derivative II DF 96512

Table C-7. Pumped Idle Characteristics Improved Derivative IIB

	10% Thrust	25% Thrust
Specific Impulse - sec	433	443
Max GOX Flowrate - lb/sec/°R	0.5/240*	2.0/230
Combustion Instability	±4.5 psi At 150 cps	None
Fuel Instability	None (Cav Venturi)	None
Vapor Pumping Capability		
H_2 Pump	74%	65%
O_2 Pump	67%	47%

*GOX heat exchanger located at turbine discharge

3.1.6 Category IV Engine Optimization

The Category IV engine configuration from the previous study (Ref 1) was modified during this study to improve engine interchangeability and optimize engine performance. These changes are discussed in the following paragraphs.

a. Interchangeability

The installed engine length for the Category IV engine was set at 57 in. to be compatible with the minimum length Derivative II engine option. Two-phase pumping capability similar to that for the Derivative IIA was also baselined for the engine. Identical inlet valves, inducer speeds, and inlet diameters were used, to those designed for the Derivative IIA engine. A maneuvering thrust level of 25% of full thrust was also selected for this engine.

b. Optimum Performance

The engine thrust chamber configuration was selected to optimize performance within the defined 57-in. installed length, and a minimum primary nozzle exit diameter of 37 in., required to ensure that the secondary nozzle can retract over the powerhead.

Engine length is composed primarily of nozzle length and chamber/injector assembly length. Optimum performance within a fixed overall length is obtained with the expander cycle engine by selecting the correct proportions of these two lengths. A short nozzle and a long chamber produces an engine with a small nozzle exit area as shown in figure C-48, but thrust chamber regenerative heat pickup is high, available chamber pressure is high and the throat area is small. Area ratio and specific impulse therefore vary with the chamber/nozzle proportions chosen.

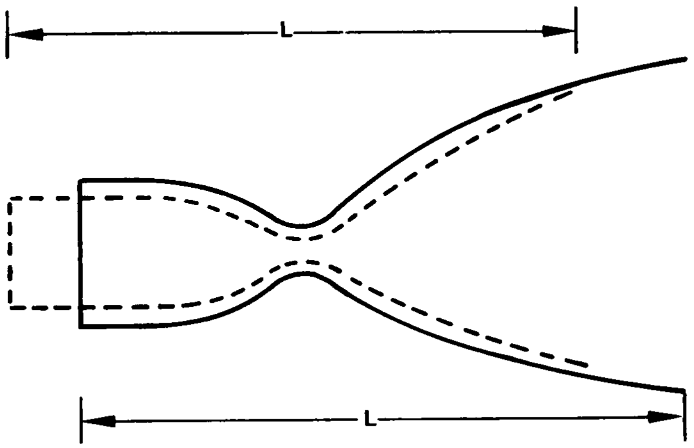


Figure C-48. Optimization at Constant Engine Length FD 72049

Analytical heat transfer studies were made for the Category IV thrust chamber to determine the effect of chamber length on nozzle coolant at a fixed overall chamber/nozzle length and the Category IV design point computer program was used to determine the effect of chamber length (ΔP and ΔT) on design point chamber pressure. The results of this engine cycle study appear in figure C-49.

The effect of chamber length on overall area ratio, primary nozzle exit diameter, and engine weight was also determined. Chamber throat area was calculated as a function of chamber pressure. Overall engine area ratio was obtained from parametric nozzle design data generated using a two-dimensional method of characteristics nozzle design program and correlated as a function of primary nozzle length, total nozzle length, and chamber throat diameter. "Minimum length" nozzle truncation contours were used throughout the study because they provide the maximum specific impulse for a given retracted nozzle length. The effect of varying nozzle/chamber length proportions on engine performance is shown in figure C-50.

Highest performance was obtained with the longer chamber lengths, even though area ratios were slightly lower. Specific impulse increased slightly with increasing chamber length because of the higher chamber pressure, and increased propellant enthalpy levels. Increasing chamber length also results in a decrease in engine weight. An 18-in. chamber length was selected to provide the greatest performance within the 37-in. primary exit diameter constraint. Using this chamber length, design chamber pressure was 915 psia, design area ratio was 401:1 and design engine weight was 424 lb as depicted in figures C-49 and C-50.

3.1.7 Off-Design Engine Operating Characteristics

Engine off-design operating characteristics were defined for the Derivative IIA, IIB and Category IV engines to verify the soundness of the baseline design over the range of conditions expected during Space Tug operation and to provide the vehicle contractors with engine/vehicle interface data needed during the evaluations. This interface data may be found in Volume III, Part 1. The effect of variations in operating point, inlet conditions and tank pressurization flowrates was established as presented in the following paragraphs.

DF 97671

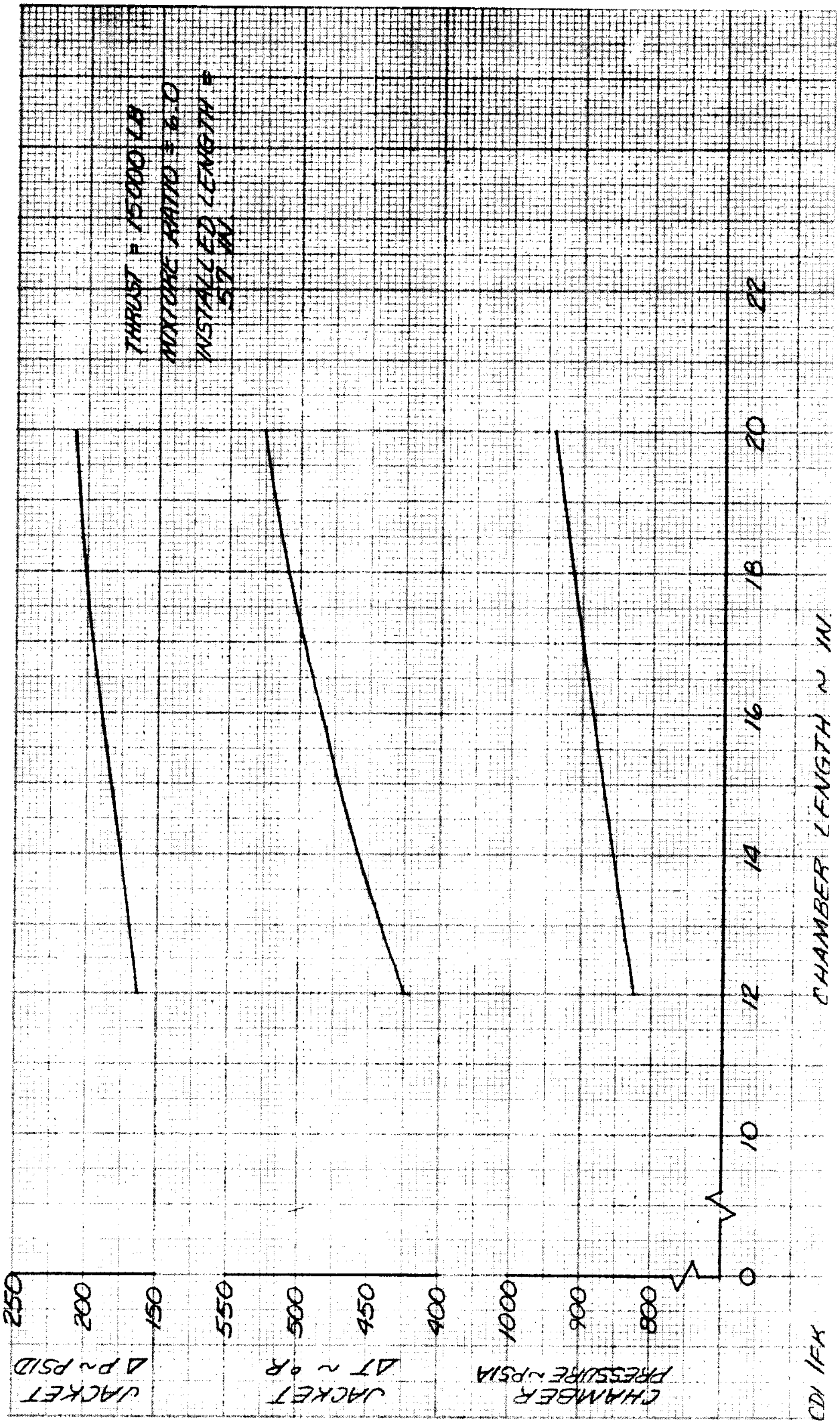


Figure C-49. Effect of Varying Chamber Length on Engine Power Category IV Engine

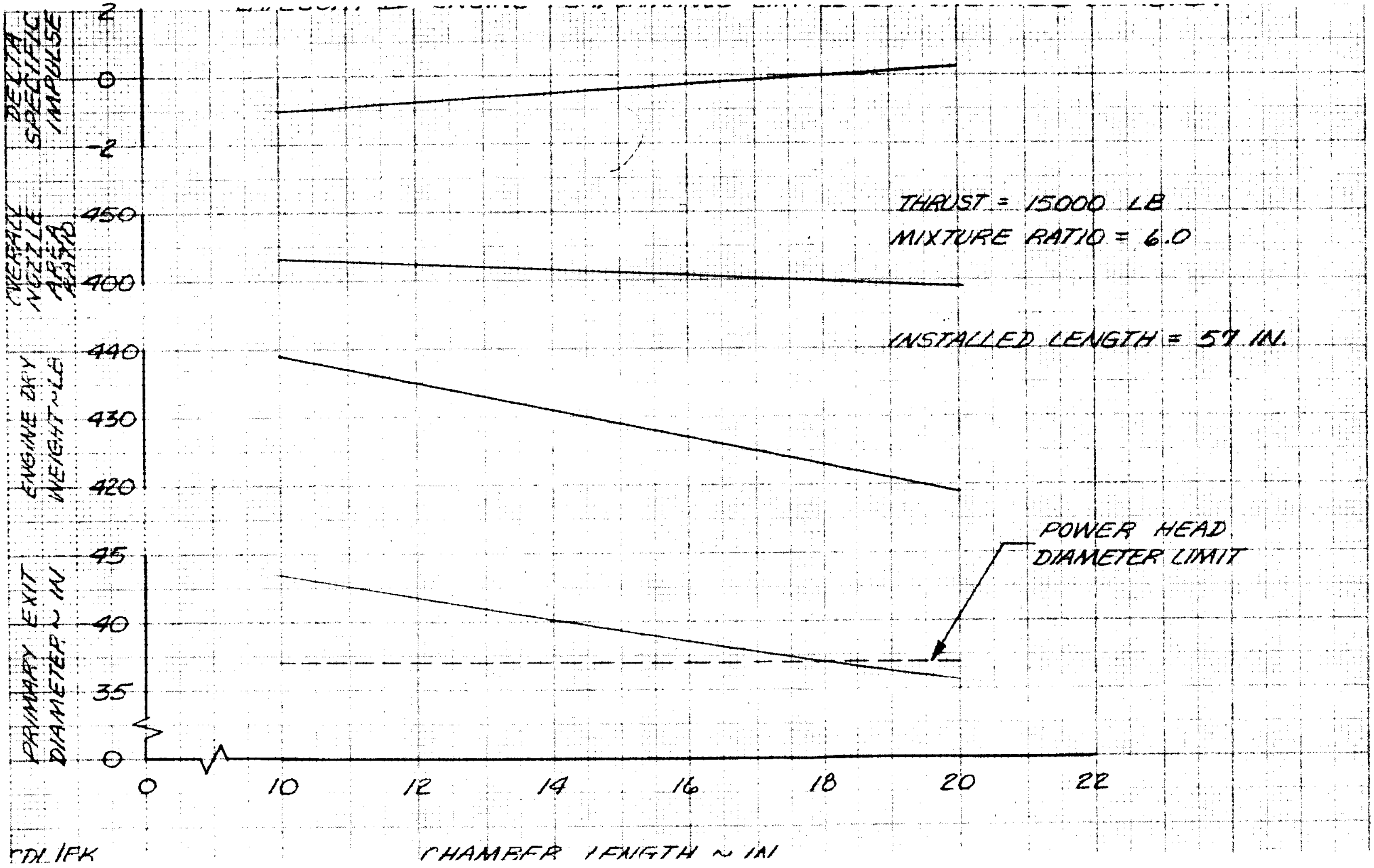


Figure C-50. Category IV Engine Performance Limited by Power Head Diameter

DF 97670

a. Operating Point Variations

Cycle balance points were generated for full thrust at mixture ratios of 5.5, 6.0 and 6.5, for maneuvering thrust at a mixture ratio of 6.0, and for tank head idle at a mixture ratio of 4.0. The data were generated with constant inlet conditions and without tank pressurization.

The off-design operating characteristics of the engines at full thrust show that the engine is capable of operating satisfactorily over the mixture ratio range of 5.5 to 6.5. Sufficient power margin is maintained over this mixture ratio range to provide control capability for the engines even with possible variations in component performance. An 8.3% bypass turbine flow is predicted for Derivative II engines, and 2.8% turbine bypass flow for the Category IV engine at the worst operating condition. Thrust change over this operating range is small due to operation of the thrust control, although impulse significantly decreases at the higher mixture ratios. The suction requirement of the engines varied over this operating range, but the change in requirements was small. Turbomachinery stresses were well within the safe operating limits of the turbomachinery and chamber. Thrust, specific impulse, and NPSH characteristics over the mixture ratio range are shown in Volume III, Part I (ICD) figures C-13, C-14, C-16, C-17 and E-4.

Characteristics of the engines at maneuvering thrust and tank head idle indicate that the engines are capable of operating satisfactorily at reduced thrust levels. The engine components are not subjected to environmental conditions, or operating conditions outside their operating limits although a mixture ratio of 4.0:1 was used at tank head idle to keep chamber temperatures within acceptable limits. It was also found that the suction capability of the engines gets better as thrust is reduced; the Derivative IIB engine achieving two-phase pumping capability at the pumped idle thrust level. The operating characteristics of the engines at the reduced thrust levels are presented in Section C.2.

b. Inlet Condition Variation Effects

The effects of variation in the propellant inlet conditions on each of the three engines' characteristics at full thrust and at maneuvering thrust (pumped idle) at a mixture ratio of 6.0 were predicted using the off-design cycle decks.

The inlet pressures were found to have a negligible effect on engine performance at full thrust, because the thrust control compensates for changes in flowrates. The effect on engine performance at full thrust is similar for the Derivative IIA and Category IV engines. However, they are slightly different for the Derivative IIB engine. This difference is caused by operation of the Derivative IIA and Category IV engines with saturated propellants at full thrust. For those engines, temperatures and pressure vary to hold saturated conditions while for the Derivative IIB engine, temperature is constant while pressure varies.

These studies indicated that simultaneous changes in both inlet pressures at maneuvering thrust (pumped idle), such as would occur during tank pressurization, tended to offset each other. The resultant changes in engine performance, i.e., thrust, mixture ratio and specific impulse, is small. Oxidizer inlet pressure was found to have a greater effect on engine performance at pumped idle than fuel pressure, because with constant control valve areas, changes in fuel pressure change turbine power and speed and this results in a corresponding change in oxidizer flow. Oxidizer inlet pressure on the other hand, has no direct effect on pump speed, and oxidizer flow varies almost independent of fuel flow.

Curves showing the effect of inlet pressure variations on engine operation at full thrust and maneuvering thrust (pumped idle) are shown in Volume III, Part 1 of this report, Interface Control Document (ICD) figures C-18 through C-21 (Derivative IIA), figures C-22 through C-25 (Derivative IIB) and figures C-28 through C-31 (Category IV).

c. Tank Pressurization Variation Effects

The effects on engine operating characteristics of variations in tank pressurization flowrates were determined for these three engines at full thrust and for the Derivative IIB engine at pumped idle using off-design cycle decks.

The effects of variations in fuel and oxidizer tank pressurization flow on full thrust engine performance were found to be less than 0.5%. These effects are presented in Volume III, Part 1 of this report, (ICD) figure C-27 (Derivative IIA and IIB), and figure C-32 (Category IV).

An evaluation of oxygen pressurant gas characteristics at full thrust showed that the engines can supply gaseous oxygen at flowrates as high as 1.0 lb/sec. While the gas supply temperature decreased from approximately 450°R to 250°R, the volumetric flowrate increased over the entire range of flow permitting effective use of the pressurant gas. Hydrogen pressurant gas characteristics at full thrust showed very little change in temperature over the range of flowrates evaluated, up to 0.1 lb/sec. The full thrust GO_2 and GH_2 pressurant gas temperatures as a function of pressurization flowrates over the range of engine mixture ratios (5.5 to 6.5) are presented in Volume III, Part 1 of this report (ICD) figures C-34 and C-35 (Derivative IIA), in figures C-38 and C-39 (Derivative IIB) and in figures C-40 and C-41 (Category IV).

The most significant effect of varying GH_2 pressurization flowrate at pumped idle appeared as an increase in engine thrust of approximately 15%. This increase is caused by an increase in hydrogen turbine flow and an increase in turbopump efficiencies. The engine inlet mixture ratio decreased by approximately 7% due to the higher fuel flow. Engine specific impulse increased approximately 1.1 sec primarily due to the increased chamber pressure levels. These effects are shown in Volume III, Part 1 of this report (ICD) figure C-26. The only significant effect of oxidizer pressurization flowrate on engine performance occurs in inlet mixture ratio. No significant effects appeared in thrust and specific impulse since the oxidizer pump is operating at a reduced flow coefficient and increasing flowrate increases the pump efficiency. The net effect essentially is no change in oxidizer pump horsepower.

An evaluation of oxygen pressurant gas characteristics at pumped idle for the Derivative IIB engine showed that GO_2 flowrates up 2.0 lb/sec could be supplied by the engine for use in tank pressurization. At slightly higher flowrates, the oxygen pressurant becomes two-phase and at that point it would not be an effective pressurant fluid since the volumetric flowrate supplied to the tank would decrease with increasing mass flowrate. Hydrogen pressurant gas characteristics at pumped idle showed very little change in temperature over the range of flowrates evaluated (up to 0.15 lb/sec). The pumped idle GO_2 and GH_2 pressurant gas temperatures as a function of pressurization flowrate are presented in Volume III, Part 1 of this report, figures C-36 and C-37.

3.1.8 Specific Impulse Estimates

The baseline Derivative IIA and IIB and Category IV engines specific impulse characteristics were defined using the JANNAF methodology developed during the Critical Elements Evaluation discussed in Section B6. Engine specific impulse values were established at the full thrust design point (6.0 mixture ratio), full thrust off-design mixture ratio (5.5 and 6.5), the maneuver thrust (pumped idle) operating point, and at the final conditioned tank head idle operating point. The JANNAF methodology and programs used to define specific impulse are described in detail in Appendix II.

Internal thrust chamber conditions were established considering the effects of regenerative cooling heat transfer and the effect of dump cooling and leakage flowrates. The enthalpies gained in the regenerative nozzles significantly increased the enthalpies used to calculate the main chamber specific impulse values and the dump cooling and leakage flowrates reduced the main chamber flowrates and increased the chamber mixture ratios.

The injector was assumed to be optimized for design point operation at a mixture ratio of 6.0. It was assumed, therefore, that the energy release losses would be the same at this mixture ratio as currently encountered by the RL10 at its design point mixture ratio of 5.0 ($\eta ER = 0.994$). In addition, it was assumed that striation losses could be reduced to less than 0.1 second. Additional energy release losses should occur during off-design operation. Figure C-51 shows the energy release efficiency characteristics used for these calculations. These characteristics were based on a combination of RL10 test data and analytical predictions.

The thrust chamber/nozzle contours were selected to provide maximum specific impulse within a given length. They were obtained using two-dimensional method of characteristics analyses.

It was decided during the Critical Elements Evaluation that since the Derivative II engines are similar to the RL10A-3-3 engine, e.g., similar chamber pressure, injector area, chamber length, etc., the JANNAF predicted impulse values would be adjusted downward by the ratio of RL10A-3-3 measured test data to predicted JANNAF performance. It was further decided that since the Category IV engine is a completely new engine design, its performance would be based on JANNAF predictions unbiased by RL10 test data. These procedures were used for these baseline engine specific impulse predictions.

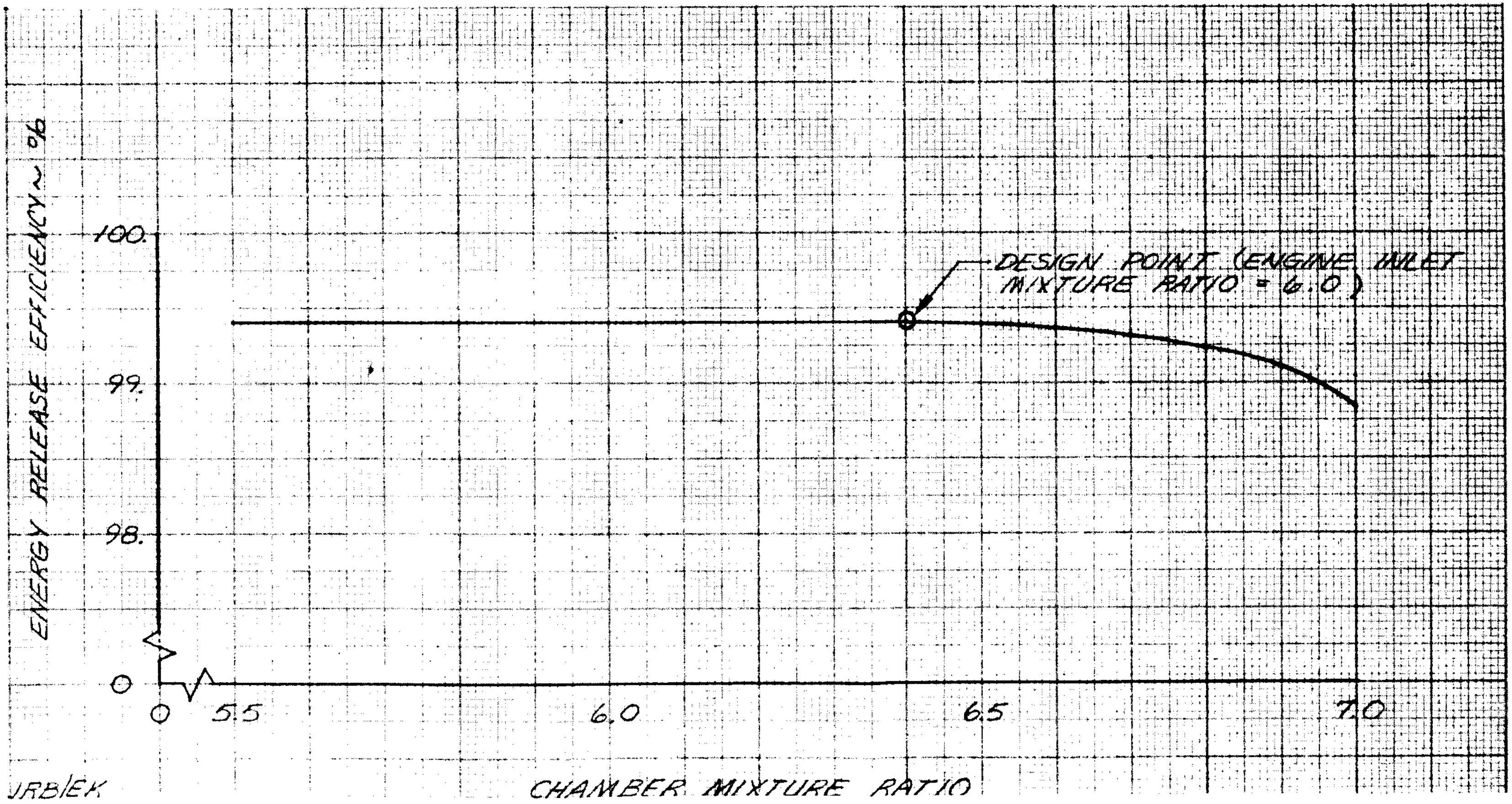


Figure C-51. Energy Release Efficiency as a Function of Mixture Ratio Off-Design Operation Derivative IIA and IIB Category IV Engine

DF 97537

The results of the specific impulse calculations for the Derivative II and Category IV engines are shown respectively in table C-8. The performance levels for the Derivative IIA and IIB engines are identical. The falloff in specific impulse as mixture ratio increases at full thrust is due primarily to the decreasing ideal specific impulse values and the increasing kinetic losses. The effect of mixture ratio on kinetic losses is quite significant with the high area ratio nozzles used on these engines. The other effects (boundary layer losses, divergence losses, energy release losses, etc.) are not appreciably affected by mixture ratio changes over this range. The lower specific impulse levels for the maneuvering thrust (pumped idle) and tank head idle points are primarily due to significant increases in the nozzle kinetic and boundary layer losses. The results obtained for the baseline engines from the individual JANNAF performance computer programs are presented in Appendix II.

3.1.9 Instrumentation

The flight instrumentation parameters selected for the operational Derivative IIA, IIB and Category IV engines are required to provide the basis for inflight and postflight monitoring of engine operation. These parameters were selected to provide an instantaneous measure of engine conditions as well as provide the basis for long term engine health trend analysis. The instrumentation parameters may be classified into several group by function; interface parameters, overall performance parameters, health trend analysis, engine conditioning during tank head idle and malfunction analysis. As often as possible, the parameters were selected to provide information in as many of these functional categories as possible.

The interface parameters such as engine inlet conditions and helium supply pressure are used to verify that the vehicle conditions remain within specification levels. These parameters are vehicle supplied and are not included as a part of the delivered engine system.

Overall engine performance parameters, such as chamber pressure, are used to indicate engine performance at any time during engine operation. The engine health trend analysis parameters such as the thrust control position indicator, are used in combination with the performance parameters, to determine performance deterioration trends during the engine service life. Engine conditioning parameters, such as pump housing temperatures, are used to evaluate the condition of the engine prior to operation and to evaluate the effectiveness of the tank-head idle cooldown.

Should a malfunction occur, all instrumentation data would be reviewed. This review would consist of comparing the data during and prior to the malfunction with previous test levels. It would have the goal of isolating the malfunction, if possible, to a particular component within the engine or vehicle. For example, pump discharge pressure could be used in combination with rotational speed to determine if the pump characteristics deviated from those recorded as a part of the engine health trend analysis.

A list of the individual selected instrumentation parameters and their use is presented in table C-9.

Table C-8. Derivative II and Category IV Specific Impulse Breakdown

Derivative II	Base Point	Off Design			
Thrust, lb	15,000	14,821	15,129	3,750	157
Chamber Pressure, psia	400	400	400	102	5.2
Engine Mixture Ratio	6.0	5.5	6.5	6.0	4.0
Chamber Mixture Ratio	6.4	5.8	6.9	6.6	4.25
Area Ratio	263	263	263	263	263
I_{vac}' (Inlet Conditions) at Chamber MR, sec	479.3	480.6	477.2	477.0	475.4
Δh Regen, Btu/lb H_2	1562	1485	1721	2178	1872
$\Delta I_{vac}'$ from Regen Δh, sec	8.0	8.5	7.8	10.0	14.9
I_{vac}' (with Δh) at Chamber MR, sec	487.3	489.1	485.0	487.0	490.3
ΔI_{vac} Kinetics (ODK), sec	-7.8	-6.1	-10.7	-24.4	-42.8
ΔI_{vac} Divergence (TDK), sec	-6.6	-6.8	-6.3	-4.3	-8.5
ΔI_{vac} Boundary Layer (TBL), sec	-8.9	-9.1	-9.9	-15.7	-44.7
ΔI_{vac} Energy Release, sec	-2.8 (.9941)	-2.8 (.9941)	-4.3 (.9908)	-2.9 (.9937)	-2.8 (.9941)
ΔI_{vac} Striation, sec	<0.1	<0.1	<0.1	<0.1	<0.1
ΔI_{vac} Overboard Leakage, sec	<0.1	<0.1	<0.1	<0.1	<0.1
ΔI_{vac} Dump Cooling, sec	-0.2	-0.2	-0.2	-0.6	-2.9
I_{vac} Delivered (JANNAF)	461.0	464.1	453.6	439.1	388.8
Ratio of RL10 Measured to JANNAF I_{sp}	0.996	0.996	0.996	0.996	0.996
I_{vac} Delivered, sec	459.2	462.2	451.8	437.3	387.2

Table C-8. Derivative II and Category IV Specific Impulse Breakdown (Continued)

Category IV	Base Point	Off Design			
Thrust, lb	15,000	14,794	15,186	3,750	73
Chamber Pressure, psia	915	915	915	234	5.6
Engine Mixture Ratio	6.0	5.5	6.5	6.0	4.0
Chamber Mixture Ratio	6.4	5.9	7.0	6.7	4.75
Area Ratio	401	401	401	401	401
I_{vac}' (Inlet Conditions) at Chamber MR, sec	484.9	485.3	483.3	483.0	480.9
Δh Regen, Btu/lb H_2	1920	1772	2039	2620	2637
ΔI_{vac}' from Regen Δh, sec	9.9	10.4	9.4	12.3	18.6
I_{vac}' (with Δh) at Chamber MR, sec	494.8	495.7	492.7	495.3	499.5
ΔI_{vac} Kinetics (ODK), sec	-4.5	-3.2	-5.9	-20.0	50.4
ΔI_{vac} Divergence (TDK), sec	-5.1	-5.4	-4.8	-5.9	7.3
ΔI_{vac} Boundary Layer (TBL), sec	-12.2	-11.5	-12.6	-18.2	49.4
ΔI_{vac} Energy Release, sec	-2.8 (.9941)	-2.9 (.9941)	-5.4 (.9889)	-3.3 (.9931)	-2.8 (.9941)
ΔI_{vac} Striation, sec	<0.1	<0.1	<0.1	<0.1	<0.1
ΔI_{vac} Overboard Leakage, sec	<0.1	<0.1	<0.1	<0.1	<0.1
ΔI_{vac} Dump Cooling, sec	-0.3	-0.4	-0.3	-1.0	-4.8
I_{vac} Delivered (JANNAF)	470.0	472.3	463.7	446.9	384.8

Table C-9. Flight Instrumentation for Operational Engines Derivative IIA and IIB and Category IV Engines

	Parameter	Use
Recommended Engine Supplied		
1.	Fuel Pump Housing Temperature	Used to indicate the thermal condition of the fuel pump, and allows evaluation of the effectiveness of tank head idle cooldown
2.	Fuel Pump Discharge Pressure	Used with speed to indicate fuel pump performance deterioration
3.	Oxidizer Pump Housing Temperature	Used to indicate the thermal condition of the oxidizer pump, and allows evaluation of the effectiveness of tank head idle cooldown
4.	Oxidizer Pump Discharge Pressure	Used with speed to indicate oxidizer pump performance deterioration
5.	Oxidizer Pump Rotational Speed	Used with discharge pressures to indicate turbopump performance deterioration. Also provides a check for possible engine overspeed
6.	Chamber Pressure	Indicates overall engine performance
7.	Low Range Chamber Pressure	Provides a check on ignition characteristics and used to indicate tank head idle performance
8.	Chamber Coolant Discharge Temperature	Provides a check for possible chamber/nozzle overtemperature
9.	Nozzle Extension Contact Switches	Provides check that extendible nozzle is properly extended or retracted
10.	Igniter Exciter Monitor Voltage	Provides indications of times that igniters are energized and provides check on voltages supplied to them
11.	Solenoid Current Monitor	Provides indications of times that various solenoids are energized and provides check on voltages supplied to engine solenoids
12.	Thrust Control Position Indicator	Used to indicate overall turbopump performance deterioration
13.	Gearbox Vibration Accelerometer	Used to indicate deterioration of rotating parts due to normal wear or FOD.

Table C-9. Flight Instrumentation for Operational Engines Derivative IIA and IIB and Category IV Engines (Continued)

Parameter	Use
Recommended Vehicle Supplied	
1. Engine Fuel Inlet Pressure	Used with inlet temperature to determine condition of propellant at fuel pump inlet
2. Engine Fuel Inlet Temperature	Used with inlet pressure to determine condition of propellant at fuel pump inlet
3. Engine Oxidizer Inlet Pressure	Used with inlet temperature to determine condition of propellant at oxidizer pump inlet
4. Engine Oxidizer Inlet Temperature	Used with inlet pressure to determine condition of propellant at oxidizer pump inlet
5. Propellant Utilization Valve Angle	Used to determine engine mixture ratio
6. Helium Supply Pressure	Provides check that sufficient helium pressure is available to operate the engine valves
7. Vehicle Supply Voltage	Provides check that proper voltage is available to operate the engine solenoid valves and igniter exciters.

3.2 Transient and Control Studies

In the Critical Elements Evaluation it was established that acceptable engine transients could be obtained using an open loop control system. During this, the Baseline Engine Definition effort, transient characteristics for the Derivative IIA and IIB and Category IV engines were evaluated in more detail and the specific control requirements were defined.

Tank Head Idle, acceleration and deceleration transients were simulated for each of the engines to establish the control requirements. Where interactions were important, the vehicle system was also simulated. The specific control system items defined include the type of valves, their location in the engine, the actuation methods used and the area schedules required. Control system simplicity was one of the major considerations in selecting the control requirements.

Certain criteria had to be met in establishing the requirements for all of the transients. The control system had to be capable of controlling mixture ratio within acceptable limits in order to prevent flameout and/or thrust chamber overheating. During transients involving turbopump rotation, engine acceleration rates could not cause pump cavitation. During the pressurized tank head idle mode, thrust had to be great enough to settle propellants, and engine conditioning had to be accomplished in a reasonable period of time. These transient characteristics were obtained with a minimum of control system complexity. Except for the thrust control, used to limit thrust overshoot and control the full thrust level, adequate regulation could be obtained with fixed area or open loop controls.

Studies performed to establish the transient characteristics and control system requirements and the transient simulation used are discussed in the following sections. A more detailed description of the transient computer programs and the results of the transient simulations are presented in Appendix III.

3.2.1 Transient Simulations

Transient characteristics and control system requirements for the baseline engines were established using nonlinear engine representations containing detailed engine and vehicle component characteristics defined in Appendix III. These digital computer programs use the standard transient simulation techniques as well as many dynamic characteristic routines which have been developed from extensive RL10 engine experience.

These computer programs calculate transient performance by numerically integrating time dependent differential equations. They contain the component characteristic routines used in the steady-state cycle deck as well as dynamic routines necessary to simulate process such as volume filling, turbopump mass acceleration, and heat transfer to and from component masses. Heat transfer routines are contained that simulate the cooldown characteristics of the inlet lines, inlet valves and turbopumps and the heat addition to the combustion chamber and GO_2 heat exchanger masses. With the exception of the GO_2 heat exchanger simulation these heat transfer routines were developed for the RL10A-3-3 engine and have been checked out using RL10 test data generated under simulated space conditions at the NASA Plumb Brook and FRDC facilities.

These simulation programs have the capability of evaluating, either separately or simultaneously, the effect of many independent variables such as variations in engine propellant inlet pressures and temperatures and initial engine temperatures. Options are also provided in the programs to calculate steady-state engine performance and control areas through iterative power balances with Newton-Raphson rapid convergence techniques.

These programs also simulate the vehicle systems to establish interaction effects between the engines and vehicle systems. During these studies, vehicle/engine interactions were evaluated during tank head idle cooldown and during the engine acceleration transients by mating vehicle inlet line and propellant tank simulations with the engine simulations.

3.2.2 Control System Requirements Definition

Control system simplicity was of primary concern in setting design requirements for these engines. As a result, attempts were made to actuate as many of the valves as possible by internal engine pressures. Valves actuated by internal engine pressures do not require coordination of external pneumatic and electric supplies and they tend to normalize the engine transients. An attempt was also made to operate the control valves in an open loop mode. This was done with the exception of the thrust control. Since the thrust control is an existing RL10 valve, its inclusion on the derivative engines was not felt to be a significant departure from the goal.

The flowpaths of the Derivative IIA, IIB and Category IV engine cycles were similar enough to use the same basic control and valve concepts for all of the engines. The fuel control valve on the Category IV engine is unique to that engine, but it performs the same functions as the fuel vent, turbine bypass and thrust control valves on the Derivative IIA and IIB engines. The actuation methods used for it are the same as for the separate valves.

A discussion of each of the individual valve requirements is given in the following paragraphs.

a. Oxidizer and Fuel Inlet Shutoff Valves

The oxidizer and fuel inlet valves are used to seal off the propellants from the engine. They must be two-position, spring-loaded, normally closed, helium actuated valves. They must shut off flow in the closed position and cause a minimum pressure drop when opened. They must be located upstream of the pumps to minimize the engine propellant leakages during coast. They must be helium actuated because they must open at start prior to the development of internal engine pressures. The valve must fail closed in order to shut off propellants to the engine in case of vehicle or electrical supply failure. The required valve areas are defined in table C-10 below.

Table C-10. Inlet Valve Effective Area - $in.^2$

Engine	THI	Maneuvering Thrust (Pumped Idle)	Full Thrust	Coast/ Shutdown
		Fuel Inlet Valve		
IIA	8.04	8.04	8.04	0
IIB	3.65	3.65	3.65	0
IV	8.04	8.04	8.04	0
		Oxidizer Inlet Valve		
IIA	16.6	16.6	16.6	0
IIB	2.89	2.89	2.89	0
IV	16.6	16.6	16.6	0

b. Fuel Vent Valve

The fuel vent valve is used to vent trapped fuel overboard at shutdown. It must be a two-position, spring-loaded, normally open, helium actuated valve. It must shut off flow when closed and provide sufficient area when opened to limit the fuel pressure buildup at shutdown to an acceptable level. It must be located between the fuel pump discharge and the main fuel shutoff valve. The valve must be placed downstream of the fuel pump at the same location as on the RL10A-3-3 engine for the Derivative engines to permit the use of an existing RL10 engine valve. The Category IV engine fuel vent valve must be packaged within the fuel control valve housing to reduce controls weight. The valves must be helium actuated because they must close at start prior to the buildup of internal engine pressure. They must fail open to ensure adequate ventage of trapped fuel in the event of a vehicle helium or electric supply failure. The required valve areas are defined in table C-11 below.

Table C-11. Fuel Vent Valve Effective Area - in.^{2}

Engine	THI	Maneuvering Thrust (Pumped Idle)	Full Thrust	Coast/ Shutdown
IIA	0	0	0	0.5
IIB	0	0	0	0.5
IV	0	0	0	0.5

c. Nozzle Coolant Valve

The nozzle coolant valve is used to set the nozzle coolant flow during tank head idle and pumped operation. The valve must be a two-position, spring-loaded, normally closed valve. It must be located between the gearbox and the extendible dump cooled nozzle. It can be actuated open by gearbox pressure during the acceleration to maneuvering thrust (pumped idle). The valve area in the open position must set the required coolant flowrate during pumped operation and the flow area in the closed position must supply the required coolant flowrates at THI. The required valve areas and actuation schedule are defined in figure C-52.

ENGINE	TANK HEAD IDLE	MANEUVER THRUST (PUMPED IDLE)	FULL THRUST	COAST/ SHUTDOWN
DERIVATIVE IIA	0.037	0.30	0.30	0.037
DERIVATIVE IIB	0.037	0.30	0.30	0.037
CATEGORY IV	0.037	0.30	0.30	0.037

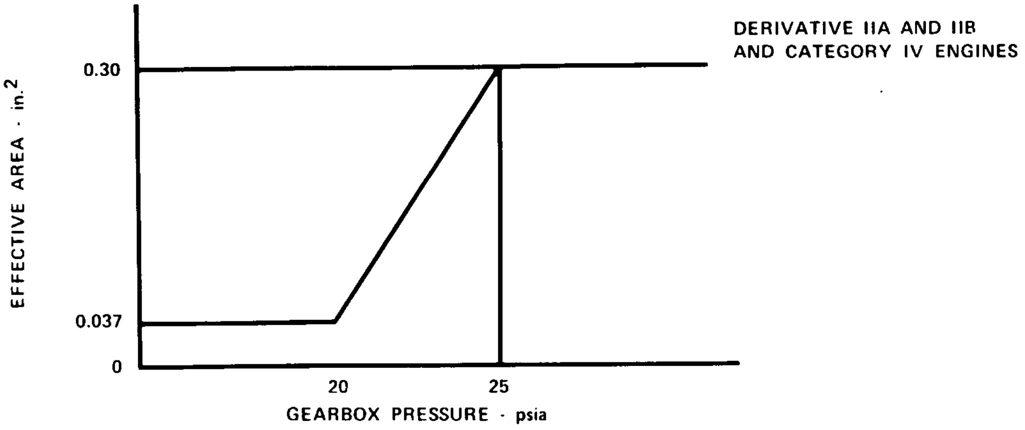


Figure C-52. Nozzle Coolant Valve Effective Area - in.^{2} FD 76369

d. Fuel and Oxidizer Tank Pressurization Valves

The fuel and oxidizer tank pressurization valves are used to limit the bleed flow extracted from the engine and avoid backflow from the tanks when the engine is operated in THI or not operating. They must be two-position, spring-loaded closed valves. They may be actuated open by internal engine pressure during the acceleration to maneuvering thrust (pumped idle). The fuel valve should be located downstream of the turbine in order to extract engine power from the flow prior to supplying it to the vehicle tank. The oxidizer valve must be located downstream of the GO_2 heat exchanger to provide the warm oxygen. The valve must shut off flow in the closed position and the area in the opened position must limit bleed flow to a level acceptable to the engine cycle. The required valve areas and actuation schedules are defined in figure C-53.

ENGINE	TANK HEAD IDLE	MANEUVER THRUST (PUMPED IDLE)	FULL THRUST	COAST/ SHUTDOWN
DERIVATIVE IIA	0	0.34	0.34	0
DERIVATIVE IIB	0	0.34	0.34	0
CATEGORY IV	0	0.17	0.17	0

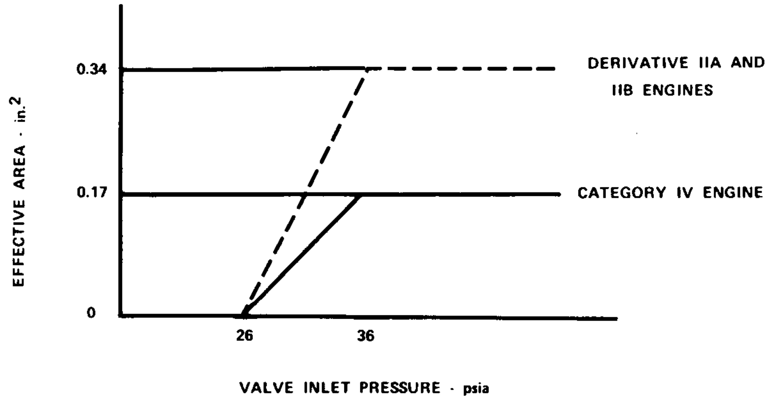


Figure C-53. Fuel and Oxidizer Tank Pressurization Valve Effective Area - in^2 FD 76994

e. Oxidizer Flow Control Valve

The oxidizer control valve is used to regulate liquid oxidizer flowrate. It must be a three-position, spring-loaded, normally closed valve. It may be actuated by oxygen system pressure and must be located between the oxidizer pump and the oxidizer injector. The valve must be closed at THI, must open to a fixed area at maneuver thrust (pumped idle) and a second fixed area at full thrust. The area schedule, as a function of oxidizer system actuation pressure, must control the movement of the valve during the transients from THI and from maneuver thrust (pumped idle) in a manner to provide an acceptable engine mixture ratio and thrust characteristics. The oxidizer flow control must be capable of varying engine mixture ratio at full thrust for propellant utilization. The required valve areas and actuation schedule are shown in figure C-54.

ENGINE	TANK HEAD IDLE	MANUEVER THRUST (PUMPED IDLE)	FULL THRUST	COAST/ SHUTDOWN
DERIVATIVE IIA	0	0.25	0.54	0
DERIVATIVE IIB	0	0.30	0.78	0
CATEGORY IV	0	0.20	0.30	0

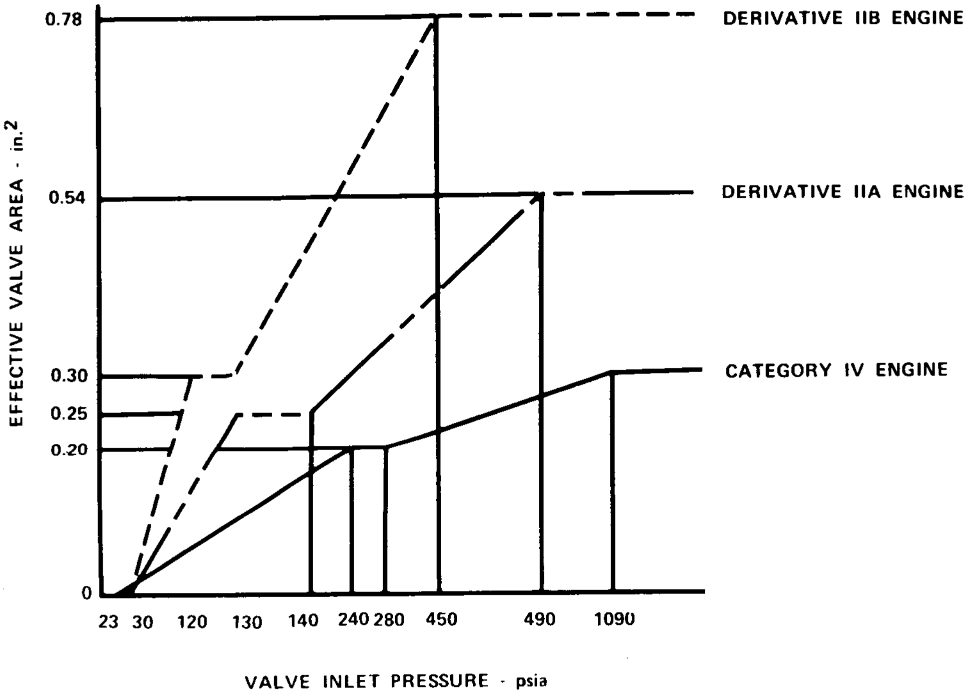


Figure C-54. Oxidizer Flow Control Valve Effective Area - in^2 FD 76371

f. GO_2 Valve

The GO_2 control valve is needed to meter gaseous oxygen flow to the engine injector during tank head idle and during the transient to maneuver thrust (pumped idle). It must be a two-position, spring-loaded, normally open valve. The valve may be actuated closed by oxidizer pressure during the acceleration from tank head idle to maneuvering thrust. The valve must shut off gaseous flow in the closed position at and above maneuver thrust (pumped idle). It must meter the correct oxidizer flow when opened in THI and must provide the correct area vs oxidizer pressure schedule to regulate engine mixture ratio during the transient to maneuver thrust (pumped idle). The GO_2 valve must be located downstream of the GO_2 heat exchanger so that it can be used to control the flow going to the injector in THI and shut off the flow in that leg during operation above THI. The valve must be designed to fail open to provide an oxidizer system vent path at shutdown in case of helium or electrical failure. The valve area requirements and actuation schedule are shown in figure C-55.

ENGINE	TANK HEAD IDLE	MANEUVER THRUST (PUMPED IDLE)	FULL THRUST	COAST/ SHUTDOWN
DERIVATIVE IIA	2.0	0	0	2.0
DERIVATIVE IIB	2.0	0	0	2.0
CATEGORY IV	2.0	0	0	2.0

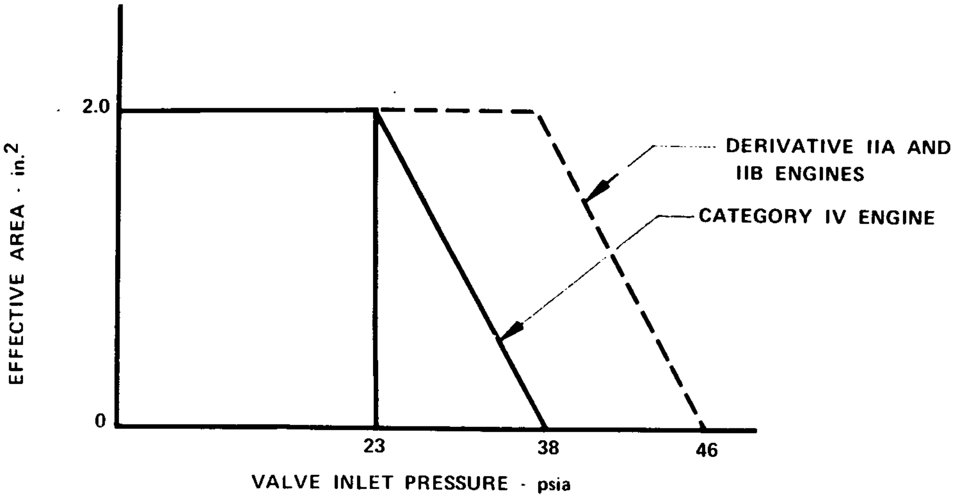


Figure C-55. GO_2 Control Valves Effective Area - in^2 FD 76372

g. Main Fuel Shutoff Valve

The main fuel shutoff valve restricts turbine flow during THI and provides a cutoff of injector fuel flow at engine shutdown. It must be a two-position, spring-loaded, normally closed, helium operated valve. The valve must shut off flow in the closed position and must provide minimum pressure loss when open to reduce turbine back pressure. The valve must be located downstream of the turbine(s) and as close to the fuel injector as practical to provide a rapid clean shutdown. The valve must be helium actuated because the valve must be opened prior to the buildup of engine internal pressures. The valve must fail closed to cut off engine power in the event of a helium or electric supply failure. The valve area requirements are shown below.

Main Fuel Shutoff Valve Effective Area - in^2

Engine	THI	Maneuvering Thrust (Pumped Idle)	Full Thrust	Coast/ Shutdown
IIA	0	4.55	4.55	0
IIB	0	4.55	4.55	0
IV	0	4.55	4.55	0

h. Turbine Bypass Valve

The turbine bypass valve is needed to meter turbine bypass fuel flow during pumped operation and total fuel flow during THI. The valve must be a three-position, spring loaded, normally closed valve. It must be actuated by both helium and fuel system pressures. While the valve must be nearly closed at full thrust, it must meter sufficient fuel flow through the GO_2 heat exchanger to provide the heat to gasify the tank pressurant. In maneuver thrust (pumped idle), the area must bypass sufficient flow to set 25% thrust. The valve should provide a minimum of pressure loss in the fully open position at THI. The valve must be actuated by helium pressure because it must be opened at start signal prior to the generation of engine pressure. The valve may be actuated open to the intermediate area with fuel system pressure during the acceleration to maneuver thrust (pumped idle). The required valve areas and actuation schedules are shown in figure C-56.

ENGINE	TANK HEAD IDLE	MANEUVER THRUST (PUMPED IDLE)	FULL THRUST	COAST/ SHUTDOWN
DERIVATIVE IIA	1.5	0.614	0.011	0.011
DERIVATIVE IIB	1.5	0.614	0,011	0.011
CATEGORY IV	0.69	0.320	0.023	0,011

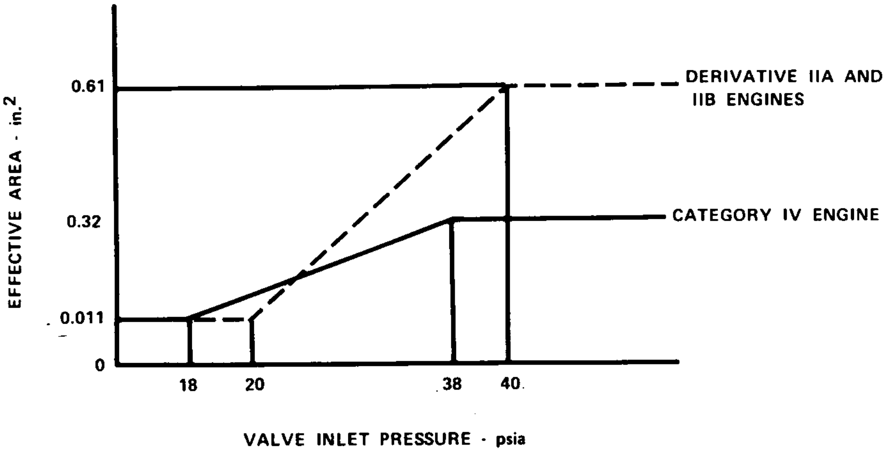


Figure C-56. Turbine Bypass Valve

FD 76373

i. Thrust Control

The thrust control is used to reduce thrust overshoot and control full thrust steady-state chamber pressure by controlling turbine bypass flow. The valve should be a spring loaded, normally closed, variable area, closed loop control. It should be actuated by engine fuel pressure and should control sensed chamber pressure. The valve can be actuated by engine system pressures because the valve is not designed to open until engine operation nears full thrust. An existing RL10 thrust control can be used for this purpose on the Derivative IIA and IIB engines. In the Category IV engine this thrust control function should be achieved in combination with the Turbine Bypass Valve.

j. Solenoid Valves

Four identical solenoid valves are required to sequence the helium actuated engine valves to achieve the various engine operating modes. They should be spring-loaded, normally closed, solenoid operated valves. The start solenoid valve, when energized, must route helium to the other three solenoid valves, to the fuel vent valve and to the inlet shutoff valves. The fuel shutoff solenoid valve when actuated must route helium to the main fuel shutoff valve. The bypass solenoid No. 1 must route helium to the turbine bypass valve. The bypass solenoid No. 2 must route fuel turbine inlet pressure to the turbine bypass valve so that it can be opened during the acceleration to the maneuvering thrust (pumped idle) position. The only action required by the vehicle to change engine operation from one mode to another is to simultaneously energize and de-energize various combinations of these four solenoids.

3.2.3 Transient Characteristics

3.2.3.1 Tank Head Idle Operating Characteristics

Tank head idle studies were accomplished for the baseline engines to determine control system requirements and cooldown and operating characteristics for each of the engines. During the Critical Elements Evaluation portion of this study it was determined that satisfactory THI characteristics could be obtained with fixed area controls. This conclusion was reached by simulating only the extremes in expected engine inlet condition (i.e., all liquid or all gas). It was recognized that under flight conditions the inlet quality will actually vary from gas to liquid and that the cooldown times and the operating characteristics would have to be re-established considering the vehicle/engine interactions. This was accomplished during this Design Definition Task.

In flight, when THI is initiated, the propellants in the tanks are unsettled and saturated vapor conditions will most likely exist at the entrance to the propellant feed lines. As thrust from the engine accelerates the vehicle, mixed phase and later liquid propellants will flow out of the tanks into the propellant feed lines. The effect of propellant settling and inlet line conditioning on tank head idle cooldown, and the effect of inlet line pressure losses and g pressure gains on the transient operation were determined using the combined simulations. Tank settling time was calculated using the following equation:

$$\text{settling times} = K\sqrt{\frac{2SWt}{g\,Fg}}$$

where

S = distance vehicle has to travel after initiation of thrust to theoretically settle propellants, ft

Wt = total vehicle weight, lb

Fg = thrust, lb

K = correction factor for nontheoretical process = 5.0

The percent vapor at the engine inlet was assumed to vary as a function of settling time as shown in figure C-57.

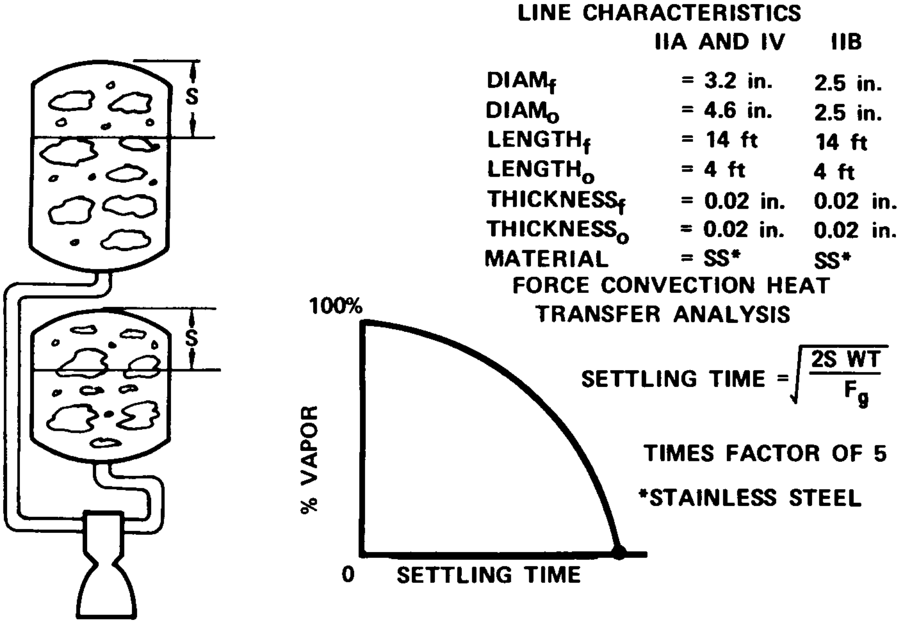

LINE CHARACTERISTICS
IIA AND IV IIB
DIAM$_f$ = 3.2 in. 2.5 in.
DIAM$_o$ = 4.6 in. 2.5 in.
LENGTH$_f$ = 14 ft 14 ft
LENGTH$_o$ = 4 ft 4 ft
THICKNESS$_f$ = 0.02 in. 0.02 in.
THICKNESS$_o$ = 0.02 in. 0.02 in.
MATERIAL = SS* SS*
FORCE CONVECTION HEAT TRANSFER ANALYSIS
S
S
100%
% VAPOR
0
SETTLING TIME
SETTLING TIME = $\sqrt{\frac{2S\ WT}{F_g}}$
TIMES FACTOR OF 5
*STAINLESS STEEL

Figure C-57. Total System Simulated FD 72050

3.2.3.1.1 Space Tug Environmental Effects

Studies were initially made using the Derivative IIB engine simulation to establish the engine characteristics over the range of inlet pressures, initial suction line temperatures and initial pump housing temperatures expected for the Space Tug. During these studies the engine was assumed to have an orifice between the oxidizer pump and GO_2 heat exchanger to slow the oxidizer cooldown rate and better match the cooldown times for the two pumps. This orifice was later removed to increase the oxidizer flowrate at start and improve the engine ignition characteristics. While this configuration change influenced the absolute level of the engine operating characteristics, the effect of variations in the Space Tug environment remains valid.

Simulations were made with fixed control areas sized to give a mixture ratio of 4.0 with saturated liquid inlet conditions at the end of engine conditioning. Cooldown times for these studies were defined as the time required to condition the fuel pump impellers and housings (both stages) and the oxidizer impeller and pump housing to within 4°R of the respective saturated tank temperatures. Propellant consumption was defined as the propellants consumed between the start of THI and the time when both the oxidizer pump and the fuel pump are fully conditioned. Therefore, the cooldown time and propellant consumption requirements are set by the pump that requires the longest time to be conditioned.

a. Effect of Inlet Pressure Level

Increasing the inlet pressures from 16 psia to 20 psia caused cooldown time to decrease 9 sec and the total propellant flow required to condition the pumps to decrease by 0.7 lb. The mixture ratio characteristics during the transients were not significantly affected. The inlet pressure levels affect the absolute levels of thrust throughout the THI transient and they shift the general shape of the characteristics with time. However, they do not affect the minimum or maximum mixture ratios which occur. The effect of inlet pressures on thrust and mixture ratio characteristics for the Derivative IIB engine is shown in figure C-58. The cooldown and performance levels are summarized in table C-12.

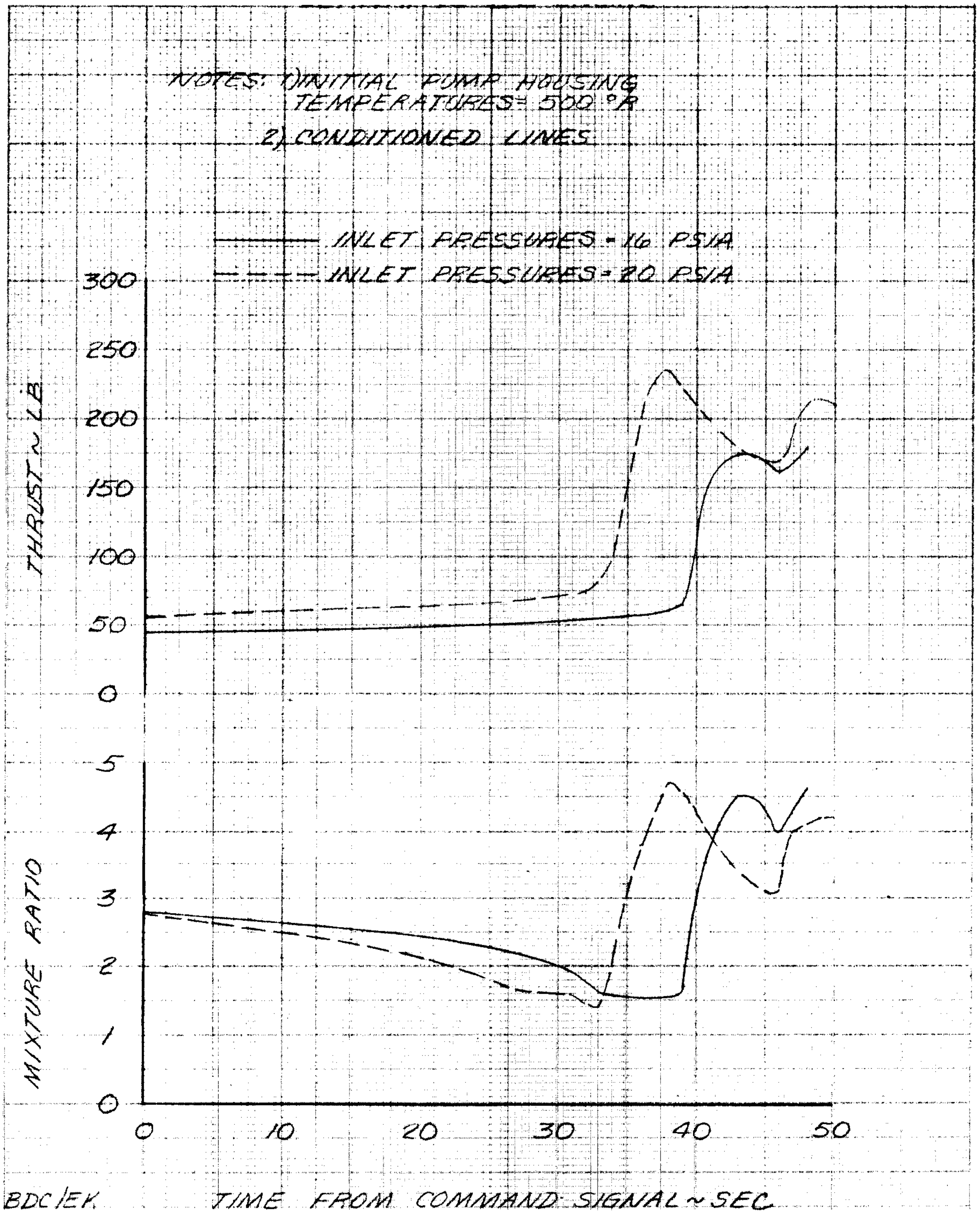


Figure C-58. Effects of Inlet Pressures on Typical Cooldown Transient Derivative IIB Engine DF 97606

Table C-12. Effect of Inlet Pressures on Derivative IIB Engine Performance and Cooldown Characteristics

	Inlet Pressures, psia	16	20
A.	Performance at Tank Head Idle After Conditioning Complete		
	Thrust, lb vac	157	203
	Mixture Ratio	4.0	4.0
	Specific Impulse, sec	387	392
B.	Cooldown Parameters		
	Cooldown Time, sec		
	Oxidizer	40	36
	Fuel	45	36
	Cooldown Consumption, lb		
	Oxidizer	6.3	5.6
	Fuel	2.5	2.5
	Total	8.8	8.1

Note: Initial line temperature = saturation temperature
Initial pump housing temperatures = 350°R

b. Effect of Initial Inlet Line Temperature

Analysis was made with suction lines assumed to be preconditioned to propellant saturation conditions and with unconditioned suction lines with initial temperatures of 500°R.

The use of colder suction lines increased the propellant consumption and the time required to condition the engine. This occurred because the colder lines allowed the oxidizer pump to cool down 12 sec earlier than with hot lines. Once the oxidizer pump was conditioned, oxidizer flow increased significantly causing mixture ratio and chamber pressure to increase. The higher mixture ratio increased fuel heat transfer and the higher chamber pressure increased fuel system back pressure causing a reduction in fuel flow and a 4-sec longer time to condition the fuel pump. Since, in this case, the cooldown time was set by the fuel pump and the oxidizer pump was conditioned considerably earlier, total propellant consumption increased 6.4 lb with the colder suction lines. A comparison of the Derivative IIB engine characteristics with the hot and cold suction lines is shown in figure C-59. Table C-13 also summarizes the effects of suction line conditioned on cooldown.

c. Effect of Initial Pump Housing Temperatures

Initial pump housing temperatures were found to have a significant effect on cooldown time and total propellants consumed during THI. A 38-sec longer time period was predicted to condition the fuel pump when the initial pump housing temperatures were 500°R instead of 350°R. In addition, approximately 11.9 lb more propellants were consumed during such a cooldown. Although the initial pump housing temperatures affected the cooldown characteristics the thrust and mixture ratio extremes were not significantly affected by the housing temperature level. Figure C-60 compares the engine mixture and thrust characteristics for the two cases. Table C-14 summarizes the effects of the initial pump housing temperature levels on cooldown.

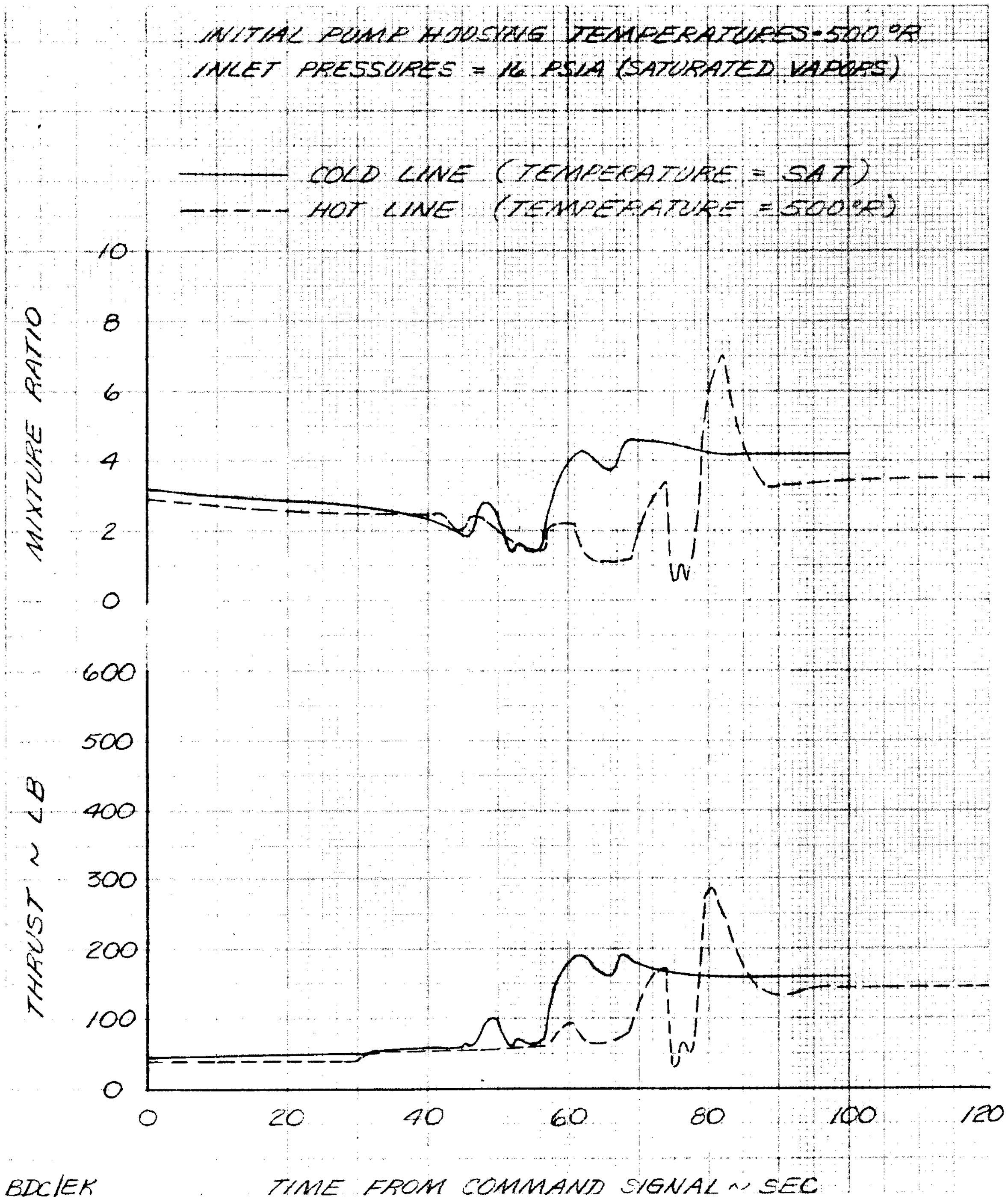


Figure C-59. Effect of Suction Line Initial Temperatures on Tank Head Idle Transient Derivative IIB Engine

DF 97675

Table C-13. Effect of Initial Inlet Line Temperatures on Derivative IIB Engine Cooldown Characteristics

Initial Suction Line Temperatures	Cooldown Time, sec Oxidizer	Cooldown Time, sec Fuel	Propellant Consumption, lb Oxidizer	Propellant Consumption, lb Fuel	Propellant Consumption, lb Total
500°R	71	79	9.7	4.6	14.3
Saturation Temperature	59	83	15.8	4.9	20.7

Notes:

(1) Initial pump housing temperatures = 500°R

(2) Inlet pressures = 16 psia

Table C-14. Effect of Initial Pump Housing Temperatures on Derivative IIB Engine Cooldown Characteristics

Initial Pump Housing Temperatures	Cooldown Time, sec Oxidizer	Cooldown Time, sec Fuel	Propellant Consumption, lb Oxidizer	Propellant Consumption, lb Fuel	Propellant Consumption, lb Total
500°R	59	83	15.8	4.9	20.7
350°R	40	45	6.3	2.5	8.8

Notes:

(1) Initial suction line temperature - propellant saturation condition

(2) Inlet pressures = 16 psia

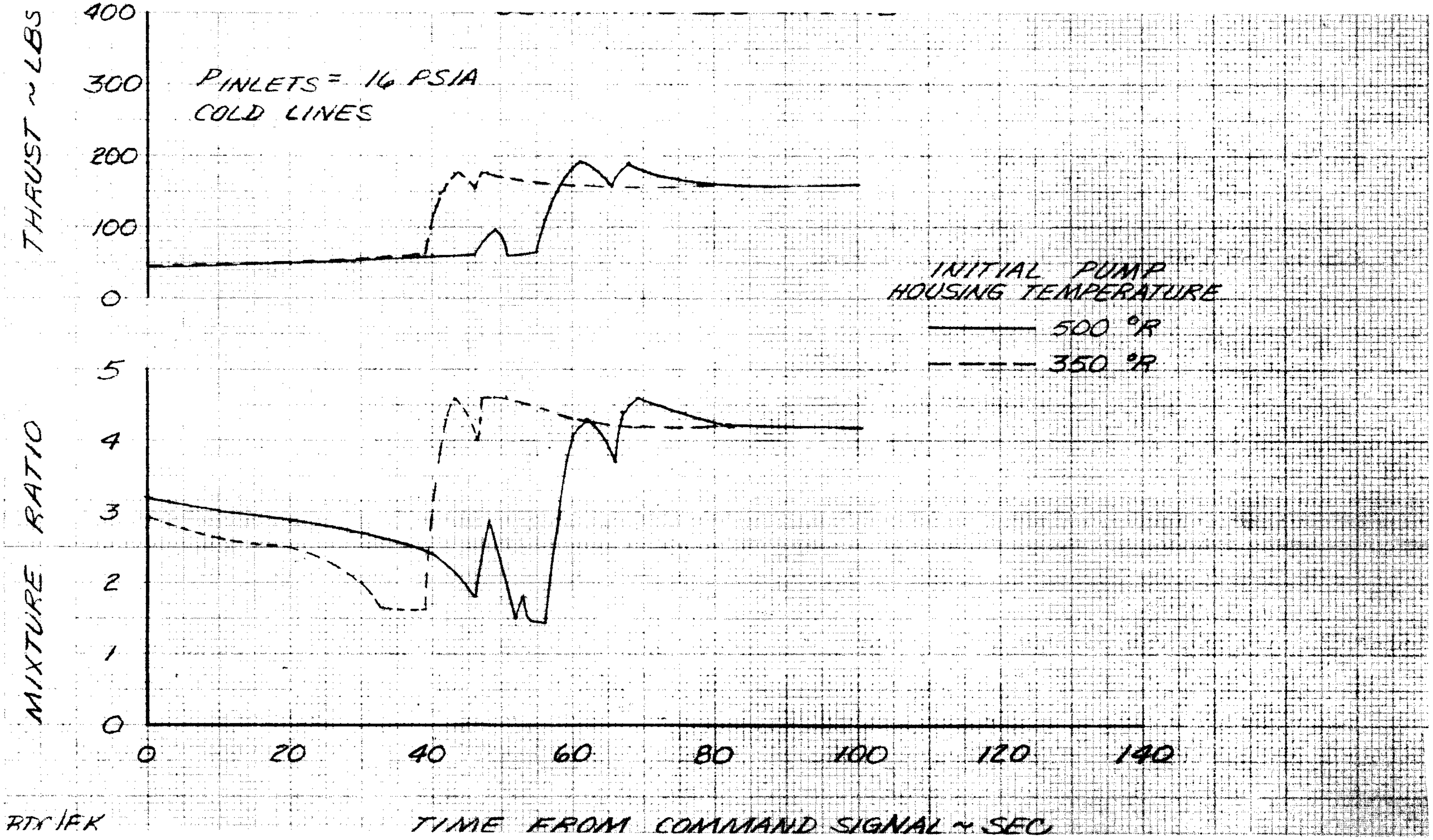


Figure C-60. Effect of Initial Engine Temperature on Tank Head Idle Transients Derivative IIB Engine DF 97965

3.2.3.1.2 Final Tank Head Idle Operating Characteristics

During an evaluation of the engine ignition characteristics it was determined that the orifice downstream of the oxidizer pump restricts oxidizer flow making ignition conditions marginal. Since the orifice was not a necessity - it had only been included in the oxidizer system to better match the cooldown times for the two pumps - it was removed from the configuration for the establishment of the final engine baseline characteristics. The control areas were sized to provide a mixture ratio of 4.0 with saturated liquid conditions and the pumps conditioned. Simulations were run and evaluated for each of the engines using nominal conditions (16 psia inlet pressures, 500°R initial pump housing temperatures and cold suction lines). The thrust and mixture ratio characteristics obtained for these transients are shown in figures C-61 through C-63 for the Derivative IIA, Derivative IIB and Category IV engines.

Removing the orifice increased the initial thrust levels for the Derivative IIB engines approximately 38 lb and cooldown time increased slightly. The Derivative IIA cooldown time was essentially the same as that predicted for the Derivative IIB engine. The Category IV engine required approximately 30 sec longer for cooldown than the Derivative II engines because the smaller chamber throat area reduces THI flowrates. The engine low speed inducers were predicted to cool down prior to the main pumps, therefore, the main pumps set the cooldown times. The final nominal tank head idle operating characteristics are summarized in table C-15.

3.2.3.2 Acceleration, Deceleration and Shutdown Transient Operating Characteristics

Acceleration and deceleration transients were simulated for the Derivative IIA and IIB and Category IV engines to establish transient characteristics and control system requirements. The required valve types, locations, schedules and actuation methods described in paragraph 3.2.2 above were established for the Derivative IIB engine, and similar control systems were used for the other engines from these data.

Because transient operation is essentially the same for the Derivative IIA and IIB engines, control requirements and transient engine characteristics for both were established using the Derivative IIB engine simulation. However, the vapor volumes available and pumping capabilities of the Derivative IIA engine were determined using the Derivative IIA inlet line simulation and the Derivative IIA pump characteristics. The Category IV simulation were used to establish nominal transient characteristics and valve and control requirements for that engine.

In addition to establishing the nominal transient characteristics, expected transient variations were defined. These were determined by defining the variations expected for the various valves and controls and determining the effect that these variations would have on the nominal transient characteristics. The nominal transient times and impulses and the variations expected for them are summarized in table C-16.

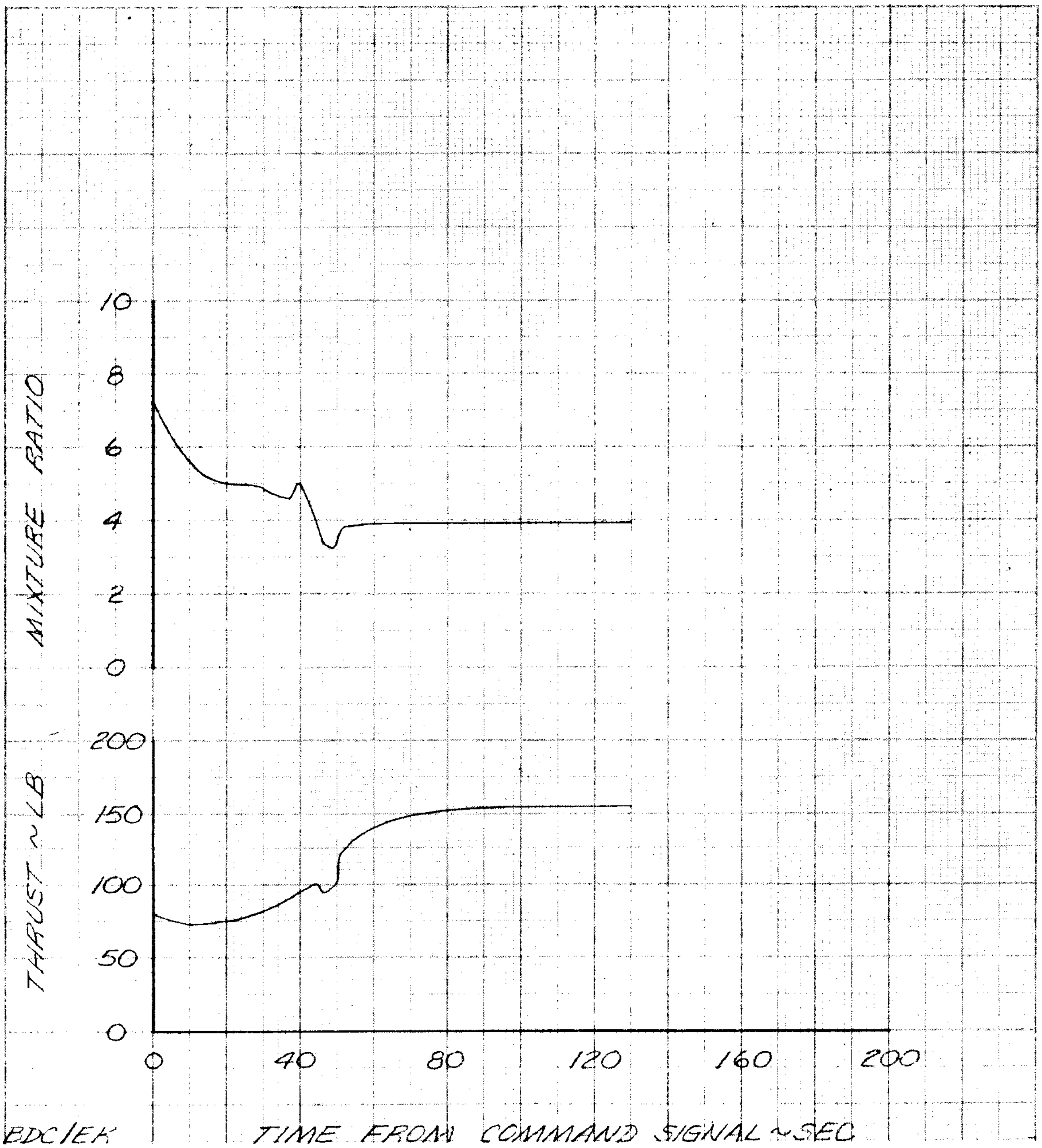


Figure C-61. Typical Cooldown Transient Derivative IIA Engine Thrust and Mixture Ratio DF 97604

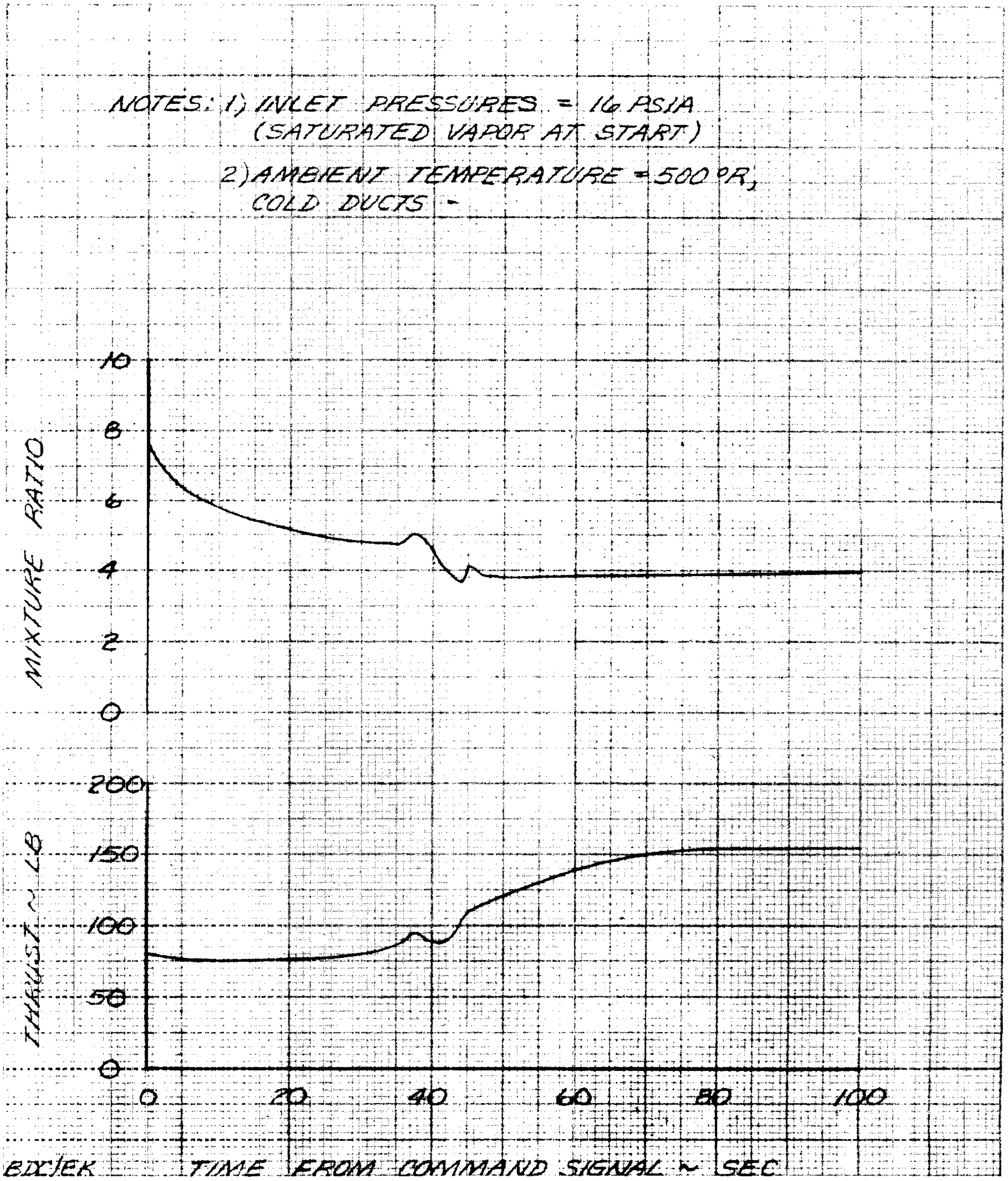


Figure C-62. Typical Cooldown Transient Derivative IIB Engine Thrust and Mixture Ratio

DF 97605

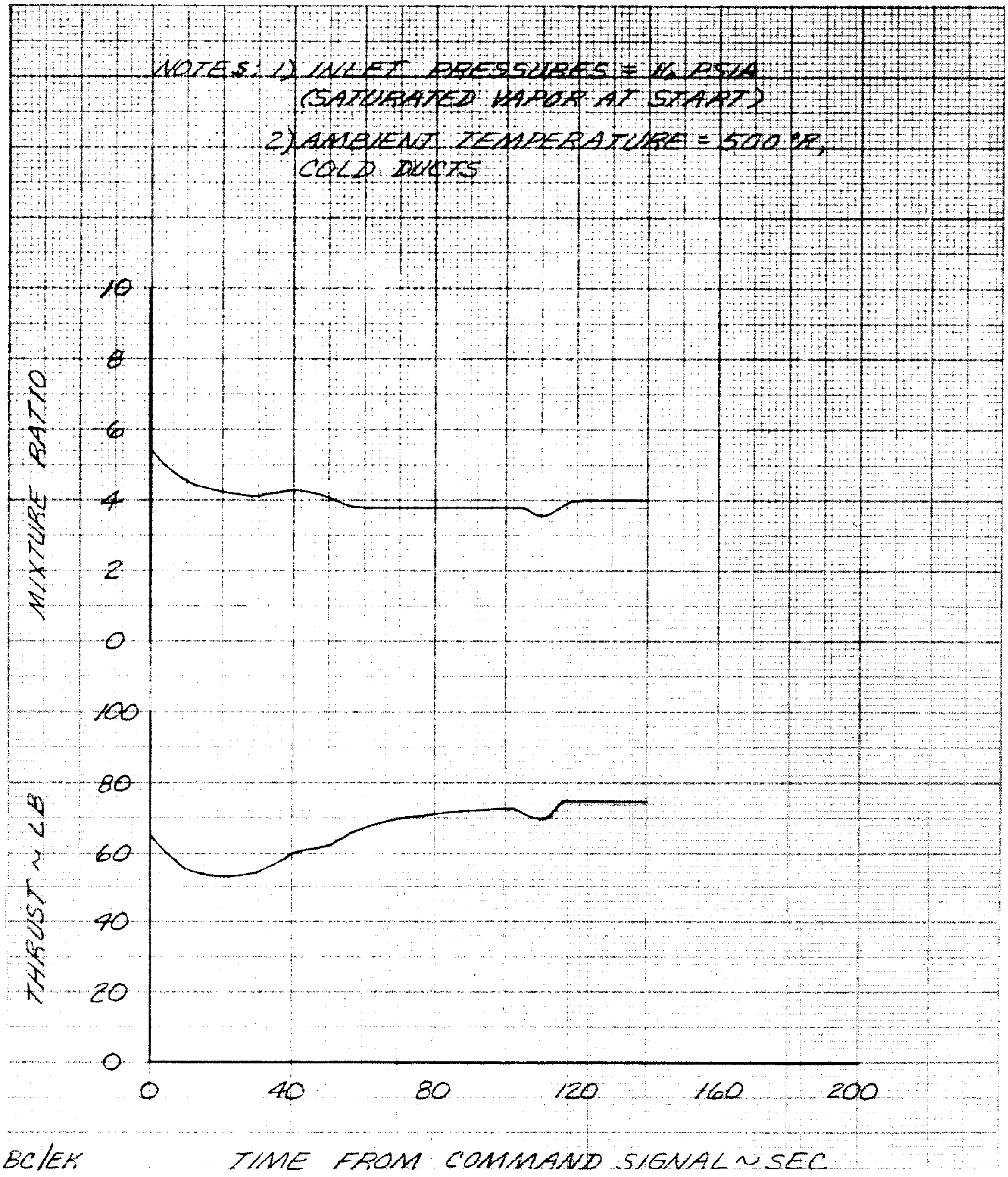


Figure C-63. Typical Cooldown Transient Category IV Engine Thrust and Mixture Ratio DF 97607

Table C-15. Final Baseline Engine Operating Characteristics

	IIA	IIB	IV
A. Times Required to Condition Pumps - Seconds			
Fuel Low Speed Inducer	-	-	88
Fuel Pump	89	90	95
Oxidizer Low Speed Inducer	22	-	33
Oxidizer Pump	51	46	120
B. THI Performance			
Initial			
Thrust, lb vac	82	82	61
Mixture Ratio	7.4	7.4	5.5
Specific Impulse, sec	337	337	355
Final			
Thrust, lb vac	157	157	73
Mixture Ratio	4.0	4.0	4.0
Specific Impulse, sec	387	387	385
Cooldown Time, sec	89	90	120
Cooldown Consumption	26.1	27.1	20.1

Note: (1) Inlet pressure = 16 psia
(2) Initial pump housing temperature = 500°R
(3) Initial inlet line temperature = saturation conditions

Table C-16. Transient Performance Summary

	Derivative IIA and IIB	Category IV
Start Transient		
Tank Head to Maneuver Thrust		
Time, sec (1)	1.56 ± 0.30	2.78 ± 0.36
Impulse, lb-sec (2) (6)	3880 ± 1200	8200 ± 2560
Maneuver Thrust to Full Thrust		
Time, sec (1)	1.31 ± 0.12	1.33 ± 0.10
Impulse, lb-sec (2)	20608 ± 1500	18500 ± 1380
Deceleration Transient		
Full Thrust to Maneuver Thrust		
Time, sec (1)	0.4 ± 0.11	0.6 ± 0.11
Impulse, lb-sec (3)	6994 ± 1100	7650 ± 1210
Maneuver Thrust to Tank Head Idle		
Time, sec (1)	1.0 ± 0.1	1.6 ± 0.1
Impulse, lb-sec (3) (4)	1665 ± 260	2975 ± 465

Table C-16. Transient Performance Summary (Continued)

	Derivative IIA	Derivative IIB	Category IV
Shutdown Transient			
From Full Thrust			
Time, sec⁽⁵⁾	0.12 ± 0.03	0.12 ± 0.03	0.12 ± 0.03
Impulse, lb-sec	1633 ± 150	1408 ± 150	1567 ± 150
Propellants Discharged, lb	20	13	18
From Pumped Idle			
Time, sec⁽⁵⁾	0.11 ± 0.03	0.11 ± 0.03	0.11 ± 0.03
Impulse, lb-sec	731 ± 50	506 ± 50	665 ± 50
Propellants Discharged, lb	15	8	13
From Tank Head Idle			
Time, sec⁽⁵⁾	0.08 ± 0.02	0.08 ± 0.02	0.08 ± 0.02
Impulse, lb-sec⁽⁷⁾	≤334	≤111	≤270
Propellants Discharged, lb	11	4	9

---

(1) To 90% of Thrust Change

(2) 2.0 sec Duration

(3) 1.4 sec Duration

(4) Category IV, 6 sec Duration

(5) To 5% of Initial Thrust Level

(6) Category IV, 4 sec Duration

(7) Shutdown impulse from THI varies with initial conditions and operating time in THI.

a. Acceleration from THI to Maneuvering Thrust (Pumped Idle)

During the transient from tank head idle to maneuvering thrust (pumped idle) the engine must (1) develop adequate torque to break away the turbopump, (2) operate at mixture ratios that prevent flameout and/or primary nozzle overheating, and (3) accelerate at a rate slow enough to prevent the two-phase propellant vapor volumes at the pump inlets from exceeding the capabilities of the pumps.

To accelerate from THI to maneuvering thrust (pumped idle), the engine must first develop sufficient torque to break away the turbopump. Breakaway torque was maximized by fully closing the turbine bypass valve at the start of this transient to route all of the fuel flow through the turbine. With 16 psia inlet pressures, the predicted turbine breakaway torques generated by the Derivative IIA and IIB, and the Category IV engines were 94 and 75 in.-lb, respectively. Required starting torque levels were also established for the Derivative IIA and IIB and Category IV engines. The required values for the Derivative IIA and IIB engines were determined from breakaway torque tests on 28 RL10 production engines. These engines had a nominal breakaway torque of 56 in.-lb with a three sigma variation of ±11 in.-lb. Required starting Torque for the Category IV engine which uses floating ring seals in the turbopump was analytically determined to be only 6 in.-lb. The nominal starting torques developed by these engines exceeded the required levels with 16 psia inlet pressures.

During this transient the turbine bypass valve must open to the area required for maneuvering thrust (pumped idle) operation. Variations in the time that the turbine bypass valve remains fully closed and variations in the rate that the valve opens to the maneuvering thrust area were investigated. The study indicated that the transient characteristics would not be significantly affected by small variations (±100 millisec) in the time that the valve operates. Like the RL10 transients, the acceleration rates of these engines immediately after turbopump breakaway are primarily set by the turbopump moments of inertia and the inlet conditions. The nozzle coolant valve and the tank pressurization valves also open during this transient and the simulations showed that the engine operations were insensitive to valve actuation characteristics.

Mixture ratio characteristics are primarily set by the combined action of the GO_2 valve (closes) and oxidizer flow control (opens). Valve coordination was maintained by actuating both valves by oxidizer system pressures.

Satisfactory transients between THI and maneuvering thrust (pumped idle) were obtained for all the engines. Mixture ratio control was adequate and the engine acceleration rates were slow enough to assure satisfactory pumping during the entire transient. Mixture ratio, thrust and turbopump speed characteristics for the engines are shown in figures C-64 and C-65. Peak mixture ratios during the transient were 8.3 and 7.5 for the Derivative IIA and IIB and the Category IV engines, respectively. These momentary mixture ratio levels are not expected to present any problem since for the Derivative IIA or IIB engines the peak predicted fuel coolant temperature at the exit of the primary nozzle was only 650°R for Derivative IIA and IIB engines whereas for the Category IV it was only 830°R. The Category IV engine temperature level is higher because of the higher heat flux of the engine. These temperatures are below those experienced during RL10 testing and they should be acceptable for the transients. Minimum mixture ratios during the transient were 2.0 and 2.5 for the Derivative IIA and IIB and Category IV engines, respectively. These minimum levels are within the flameability limits established for these engines.

Thrust and turbopump speeds were predicted to reach the maneuvering thrust (pumped idle) levels in 1.6 sec on the Derivative IIA and IIB engines whereas the Category IV may require 3.2 sec. With these relatively low acceleration rates the predicted level of vapor volumes at the engine inlets were very low throughout the transient for all of the engines. As shown in figures C-66 and C-67, the vapor volume pumping capabilities predicted for the pumps were considerably greater than that required. The transient vapor volume characteristics of the Category IV engine were not generated because of the geometric similarity to the Derivative IIA engine.

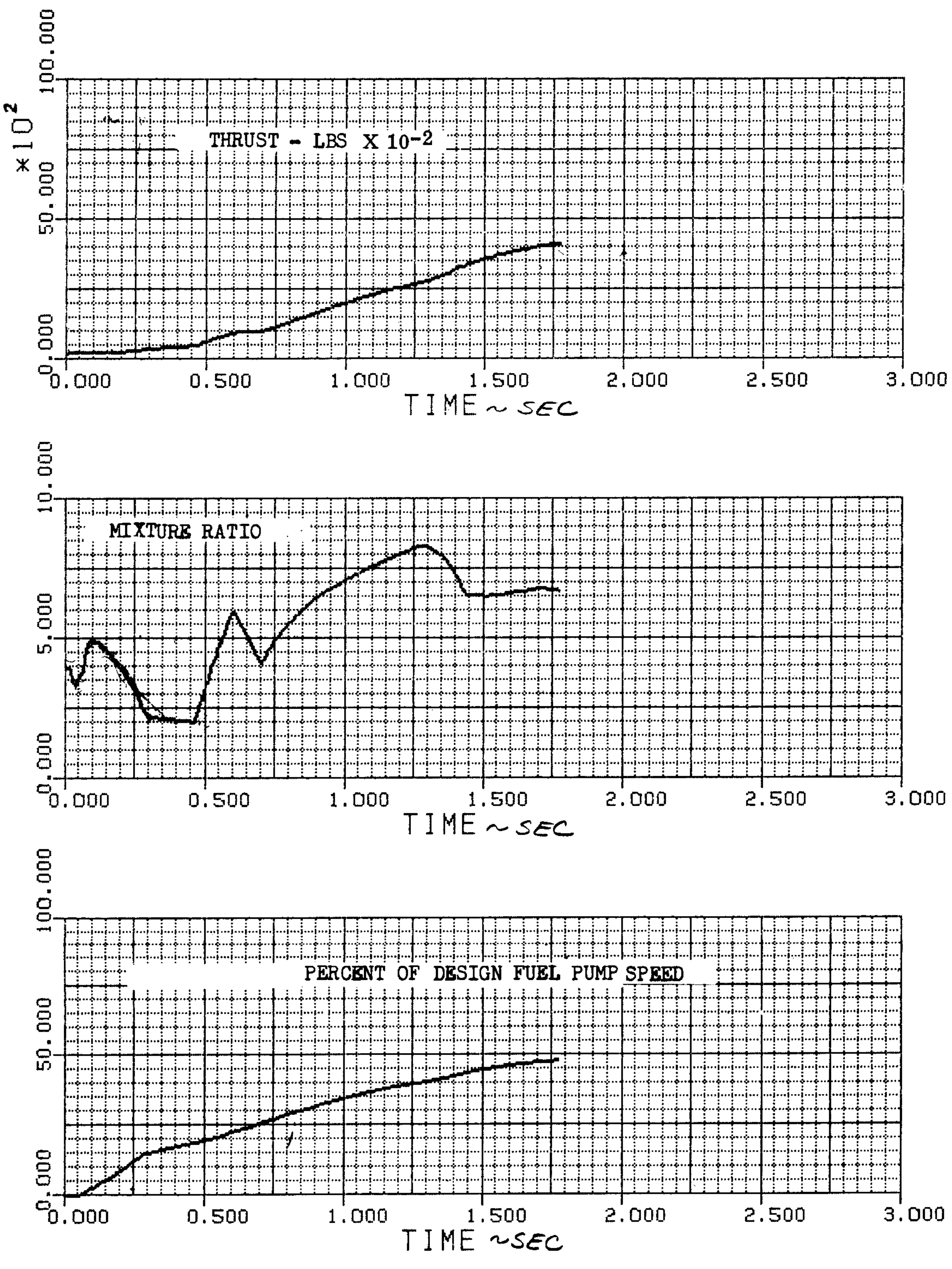


Figure C-64. Simulated Start Transient from Tank Head Idle to Maneuvering Thrust Derivative IIA and IIB Engines

DF 97027

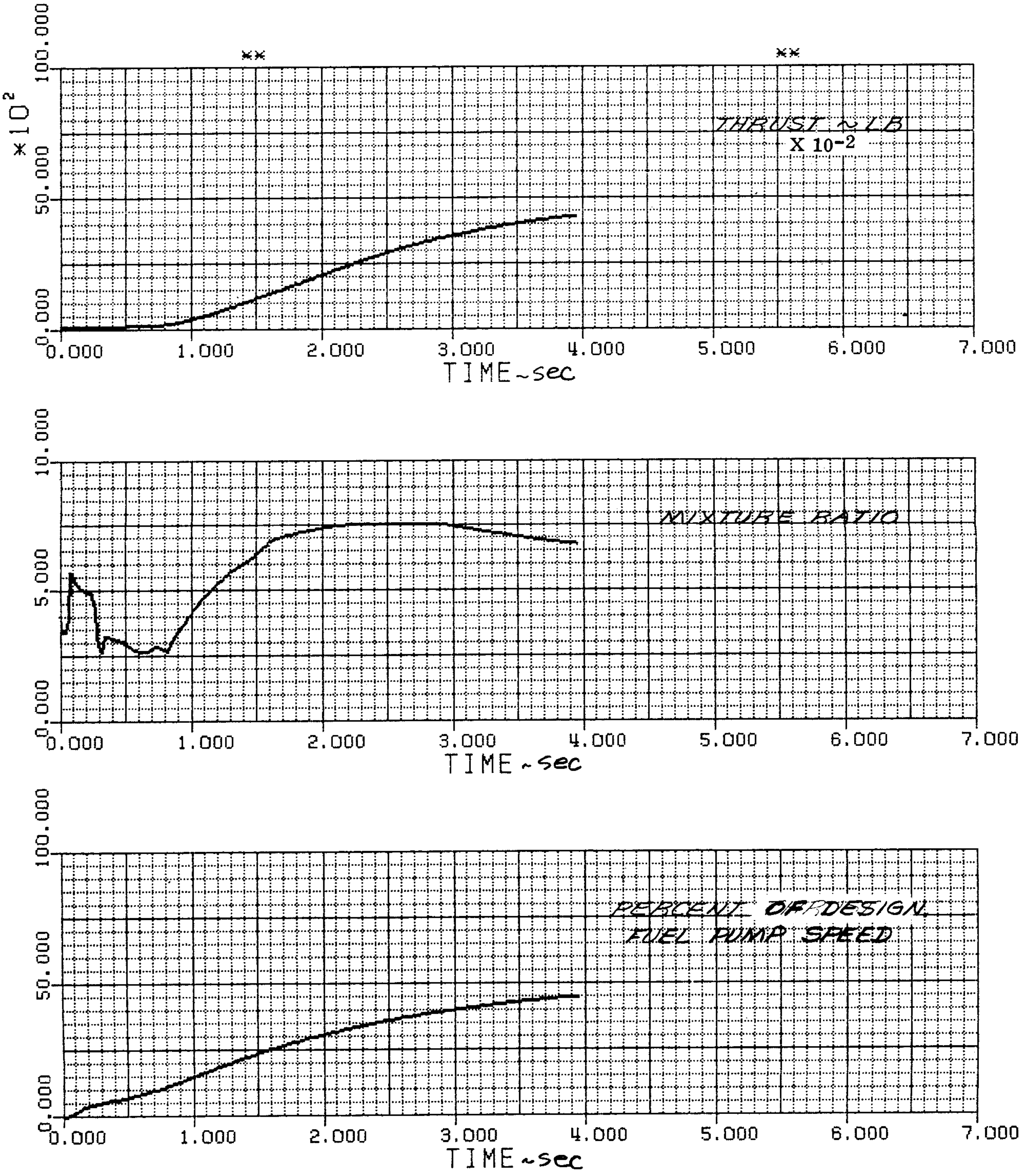


Figure C-65. Simulated Start Transient from Tank Head Idle to Maneuver Thrust Category IV Engine

DF 97092

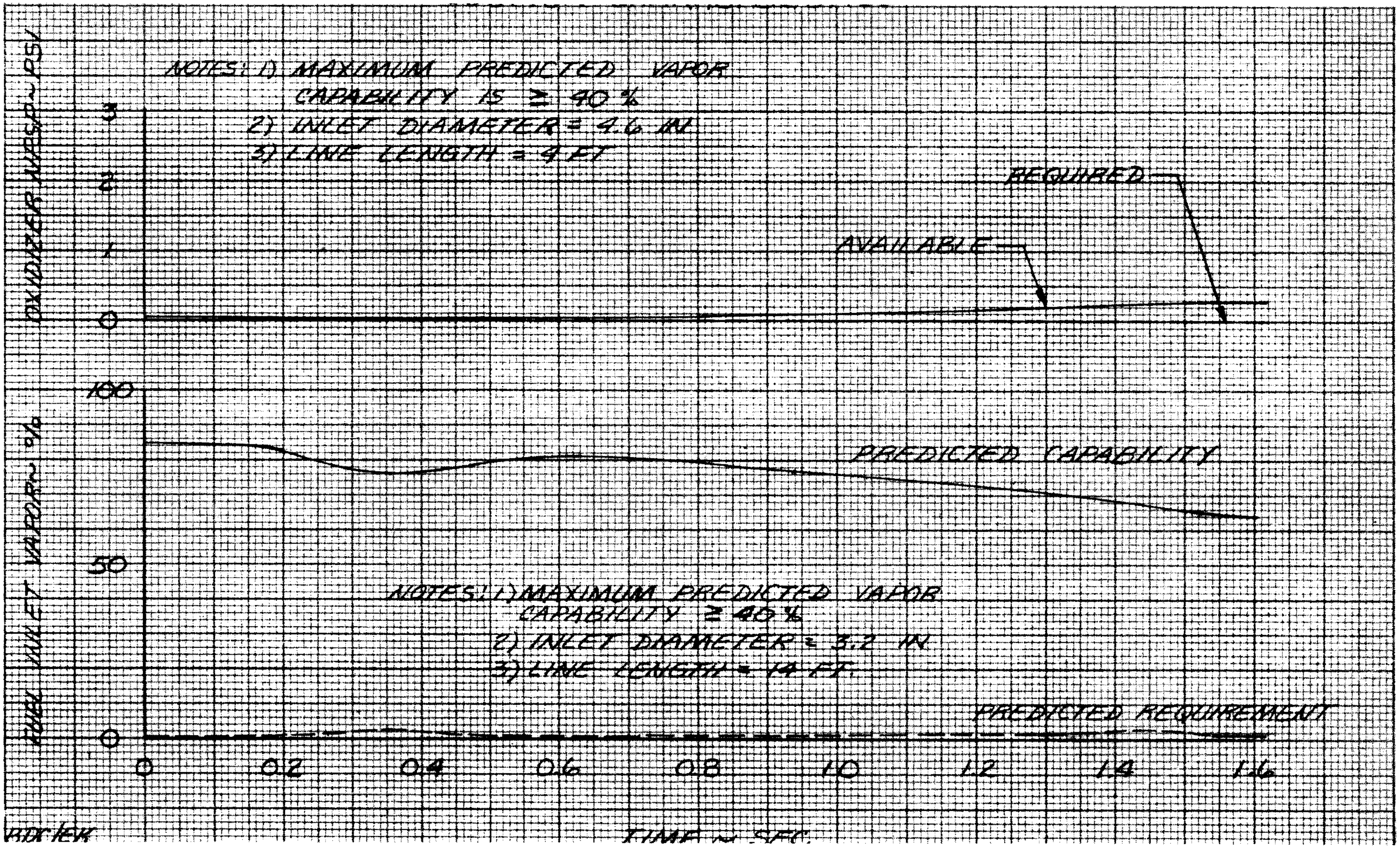


Figure C-66. Tank Head Idle to Maneuver Thrust Transient Derivative IIA Engine Suction Characteristics

DF 97933

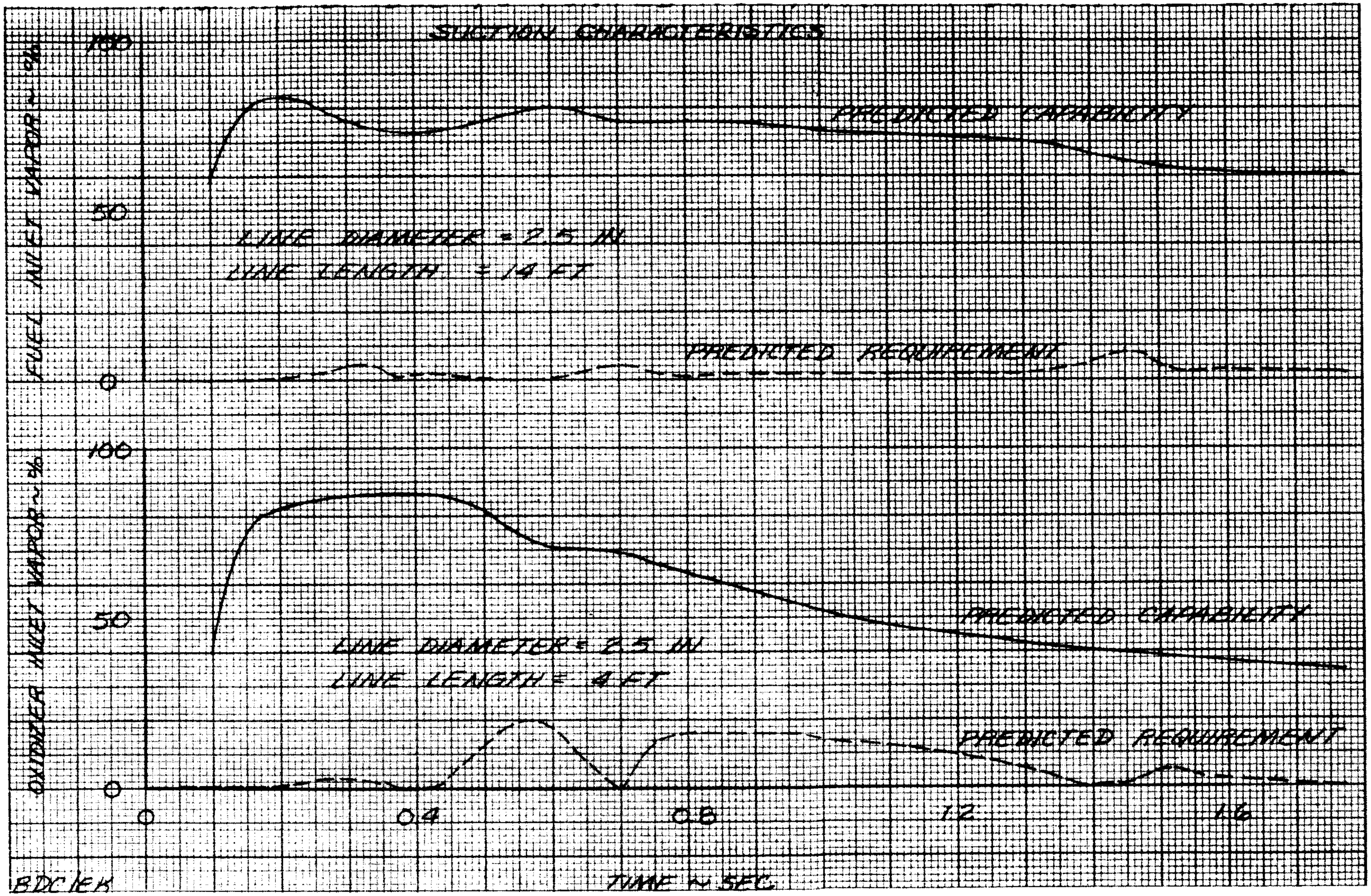


Figure C-67. Tank Head Idle to Pumped Idle Transient Derivative IIB Engine Suction Characteristics

DF 97931

The power being transmitted through the Category IV engine idler gear was evaluated during the transient. An excessive horsepower level and/or rapid changes in the direction of the horsepower being transmitted could cause excessive gear wear. As shown in figure C-68, power was always being transmitted in the same direction during the transient and less than 3 horsepower was transferred from the oxidizer turbine to the fuel turbine shaft. The gears are designed to transfer over 20 horsepower during 10-hr steady-state, full thrust operation.

b. Maneuvering Thrust (Pumped Idle) to Full Thrust Transient

The primary effort during the maneuvering thrust (pumped idle) to full thrust transient studies was to establish control system requirements which would provide satisfactory engine mixture ratios and adequate engine/vehicle suction characteristics. Mixture ratio has to stay within safe operating limits during the transient and the acceleration should be slow enough to minimize suction line pressure losses and keep the tank NPSP requirements to a minimum (for the Derivative IIB engine) and keep the two-phase propellant vapor volumes (for the Derivative IIA and Category IV engines) from exceeding the capabilities of the pumps. It is desirable that tank NPSP levels required for transient operation for the Derivative IIB engine not exceed that required for full thrust steady-state operation.

During the Critical Elements Evaluation it was determined that an active thrust control was needed to reopen the bypass near full thrust and reduce thrust overshoot. Without it the thrust overshoot was setting the engine NPSP requirements.

In this study for the Derivative IIA and IIB engines, a closed loop RL10 thrust control was used to open a turbine bypass path and limit overshoot. The oxidizer control valve was scheduled open slowly by the rise in oxidizer pump differential pressure in a manner which would permit it to control engine mixture ratio.

The transient characteristics obtained for the Derivative IIA and IIB engines using these control schedules were entirely satisfactory. The thrust, mixture ratio and turbopump speed characteristics are shown in figure C-69. The engine required approximately 1.3 sec to reach full thrust, and mixture ratio stayed between 6.6 and 7.2. The maximum temperature of the coolant leaving the primary nozzle was only 610°R.

With these acceleration characteristics the NPSP levels required during the Derivative IIB transient were no greater than the levels required for steady-state operation. The NPSP levels required by the engine and the levels that would be available assuming they are set by steady-state requirements are compared in figure C-70. The engine flowrate, speeds and thrust characteristics from this transient were also used with the vehicle inlet lines and pump characteristics for a Derivative IIA engine to predict the allowable and expected vapor volumes. Again, the maximum allowable vapor volumes at the pump inlets far exceeded the vapor volumes that were predicted to exist. The two are compared in figure C-71.

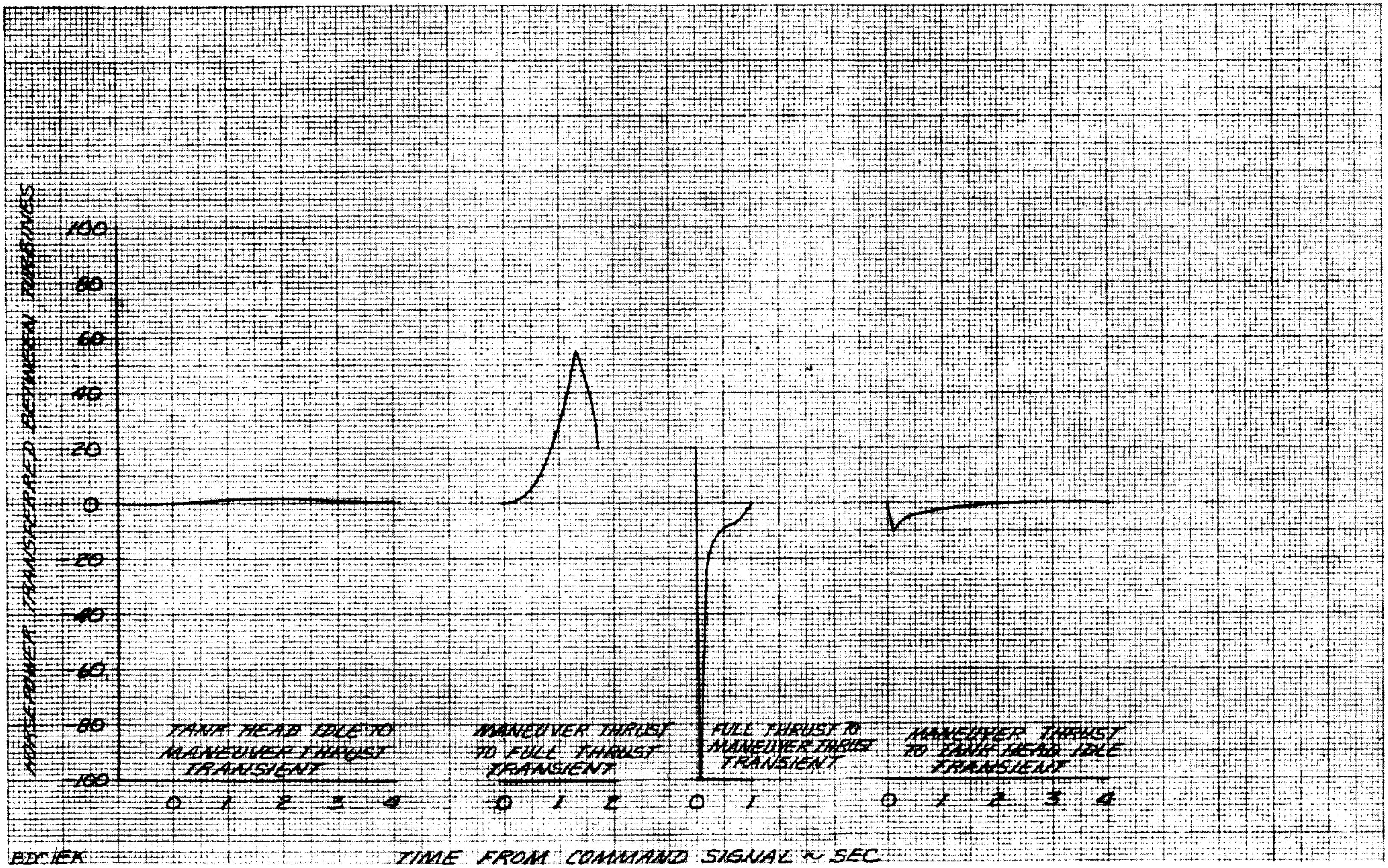


Figure C-68. Horsepower Transferred By the Category IV Engine Gear System

DF 97667

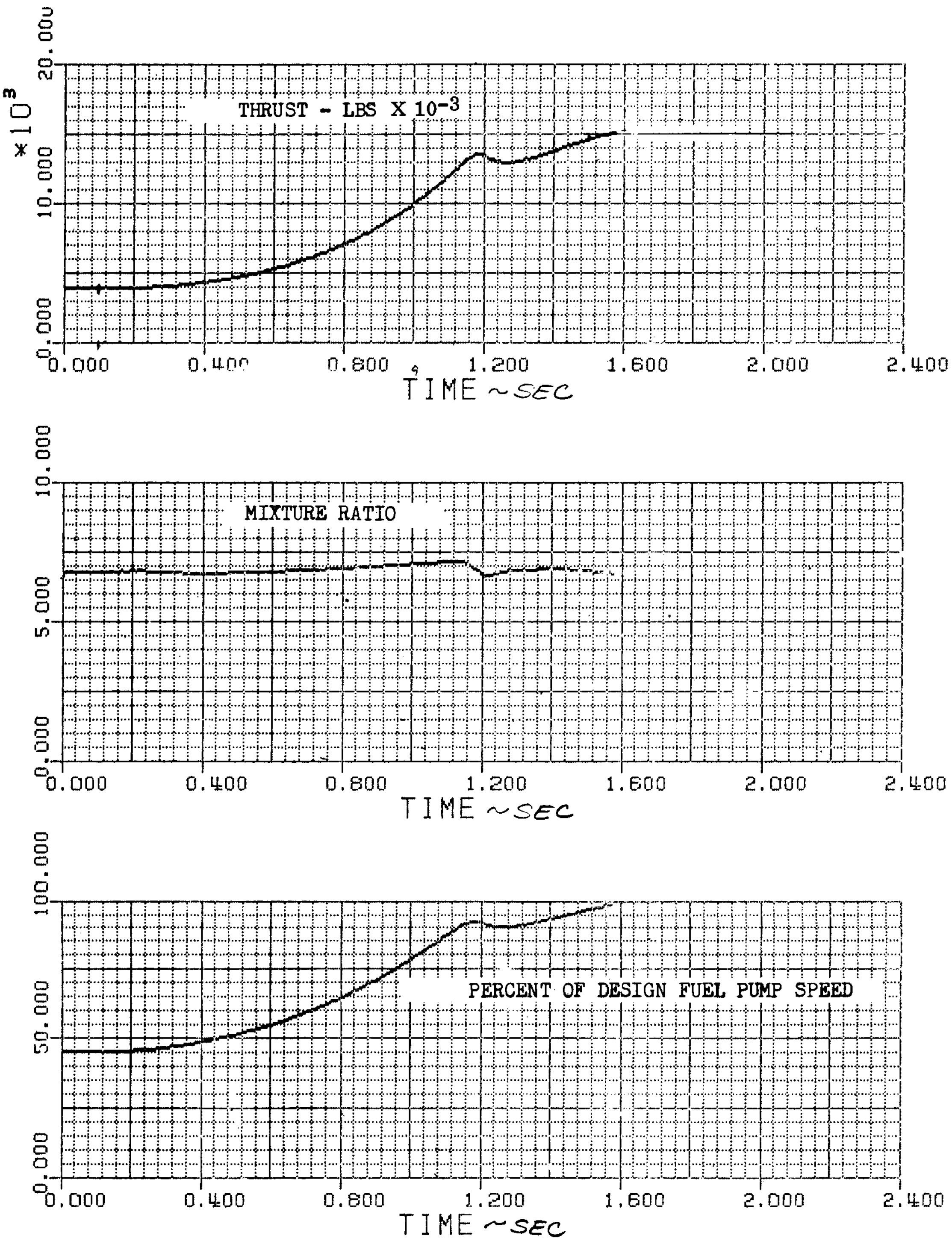


Figure C-69. Simulated Start Transient from Maneuvering Thrust to Full Thrust Derivative IIA and IIB Engines DF 97029

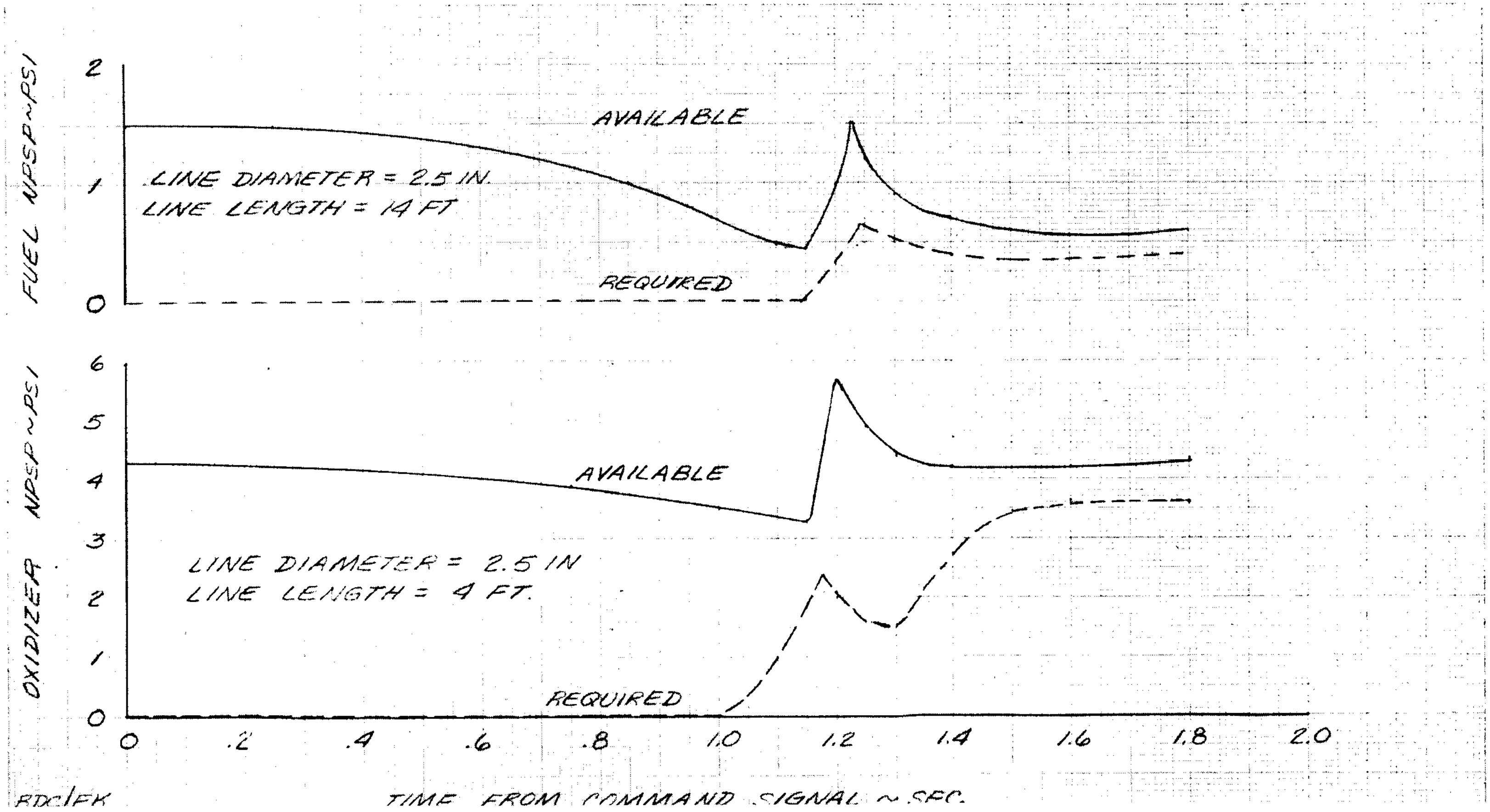


Figure C-70. Pumped Idle to Full Thrust Transient Suction Characteristics Derivative IIB Engine (Active Thrust Control)

DF 97940

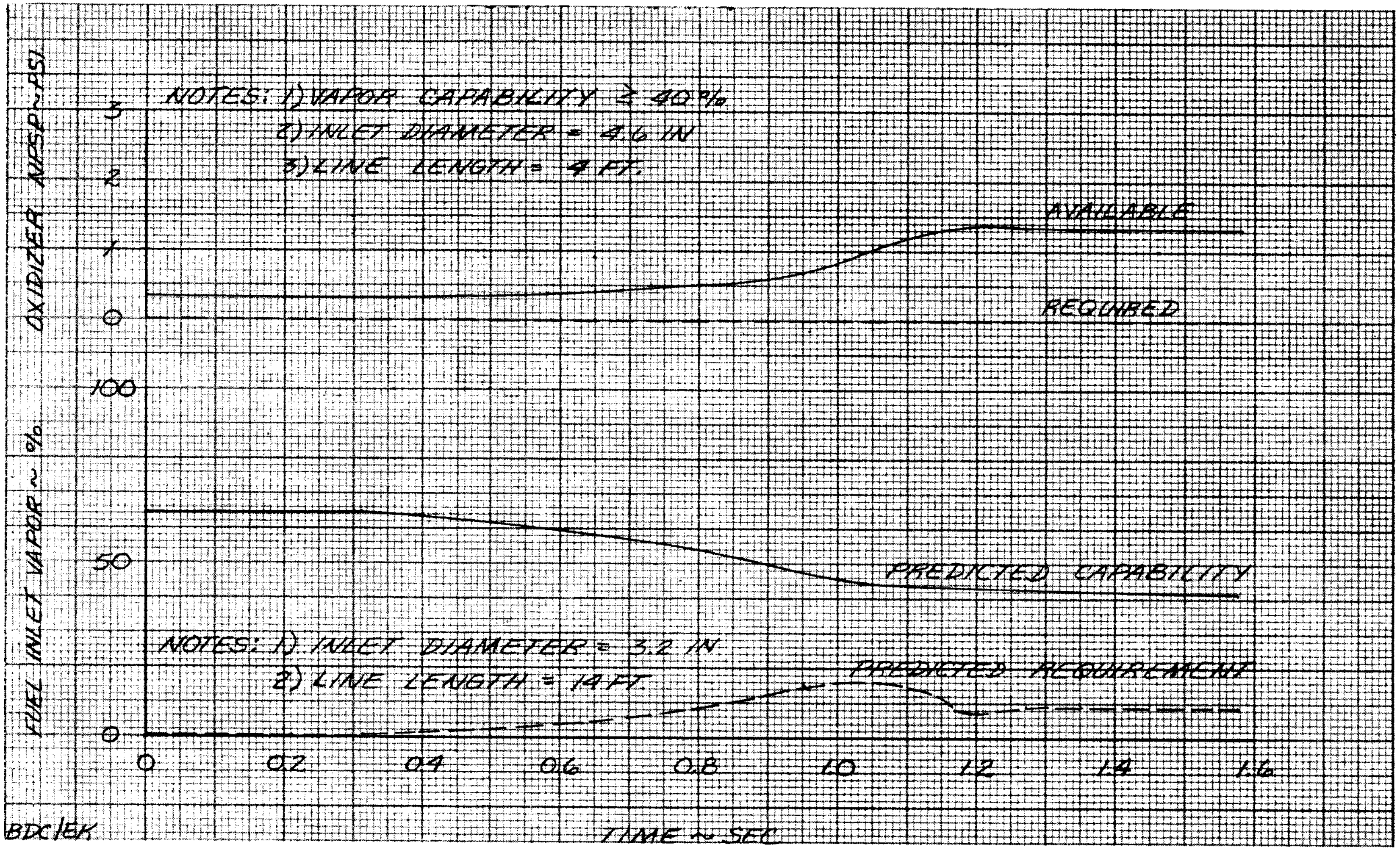


Figure C-71. Maneuver Thrust to Full Thrust Transient Derivative IIA Engine Suction Characteristics DF 97932

Since the valve scheduling selected for the Derivative IIA and IIB engines provided satisfactory transient characteristics, similar schedules were used for the Category IV engine. The acceleration characteristics obtained for this engine during the transient were very similar to those obtained for the Derivative II engines. A time of 1.33 sec was required to accelerate the Category IV engine, whereas the Derivative IIA and IIB engines required 1.31 sec.

Mixture ratio, fuel pump speed, and thrust characteristics for this transient were also similar. They are shown in figure C-72. Mixture ratio peaked at 7.8 and the maximum temperature at the exit of the primary nozzle remained below 750°R.

The transient vapor volume characteristics of the Category IV engine were not specifically determined because both inducers and the pump inlet diameters on the Category IV engine are similar to those in the Derivative IIA. Since the Category IV acceleration rate was essentially the same as for the Derivative IIA, it was felt that the results obtained for the Derivative IIA engine would be applicable.

The horsepower transferred by the idler gear on the Category IV engine during this transient was also evaluated. As previously shown in figure C-68, a peak level of approximately 55 horsepower was transmitted from the oxidizer turbine to the fuel turbine shaft. The figure also shows that power is always transmitted in the same direction during the transient. Although the peak horsepower level exceeded the normal steady-state level by a significant amount, it lasts for a very short period of time. As a result, it was felt that gear life should not be adversely affected.

c. Deceleration Transient From Full Thrust to Maneuvering Thrust (Pumped Idle)

The primary area of concern in establishing the control requirements for this transient was in being able to control the range of mixture ratios that the engines would encounter. Deceleration transients were simulated for the Derivative II and Category IV engines, and satisfactory transients were obtained which met the mixture ratio requirements.

Deceleration of the engines from full thrust to maneuvering thrust (pumped idle) was accomplished by ramping open the turbine bypass valve to its maneuvering thrust (pumped idle) position. The oxidizer control valve was allowed to close along the same pump pressure area schedule used during the acceleration. Mixture ratios remained within 5.3 to 6.4 for the Derivative IIA and IIB engines and within 4.4 to 6.4 for the Category IV engine. The thrust and mixture ratio characteristics are shown in figures C-73 and C-74. The shape and ranges of mixture ratio as thrust decayed were very similar for all of the engines. This is not unexpected since the valve schedules used were similar. Mixture ratio characteristics were primarily set by the oxidizer control valve and since the oxidizer control valves closed along the same pressure-area schedules used during the start transient, mixture ratios stayed within a narrow band during the deceleration transient. The Category IV engine required 0.6 sec to decelerate to 90% of the required thrust change while the Derivative II engines required 0.4 sec to decelerate to that thrust level.

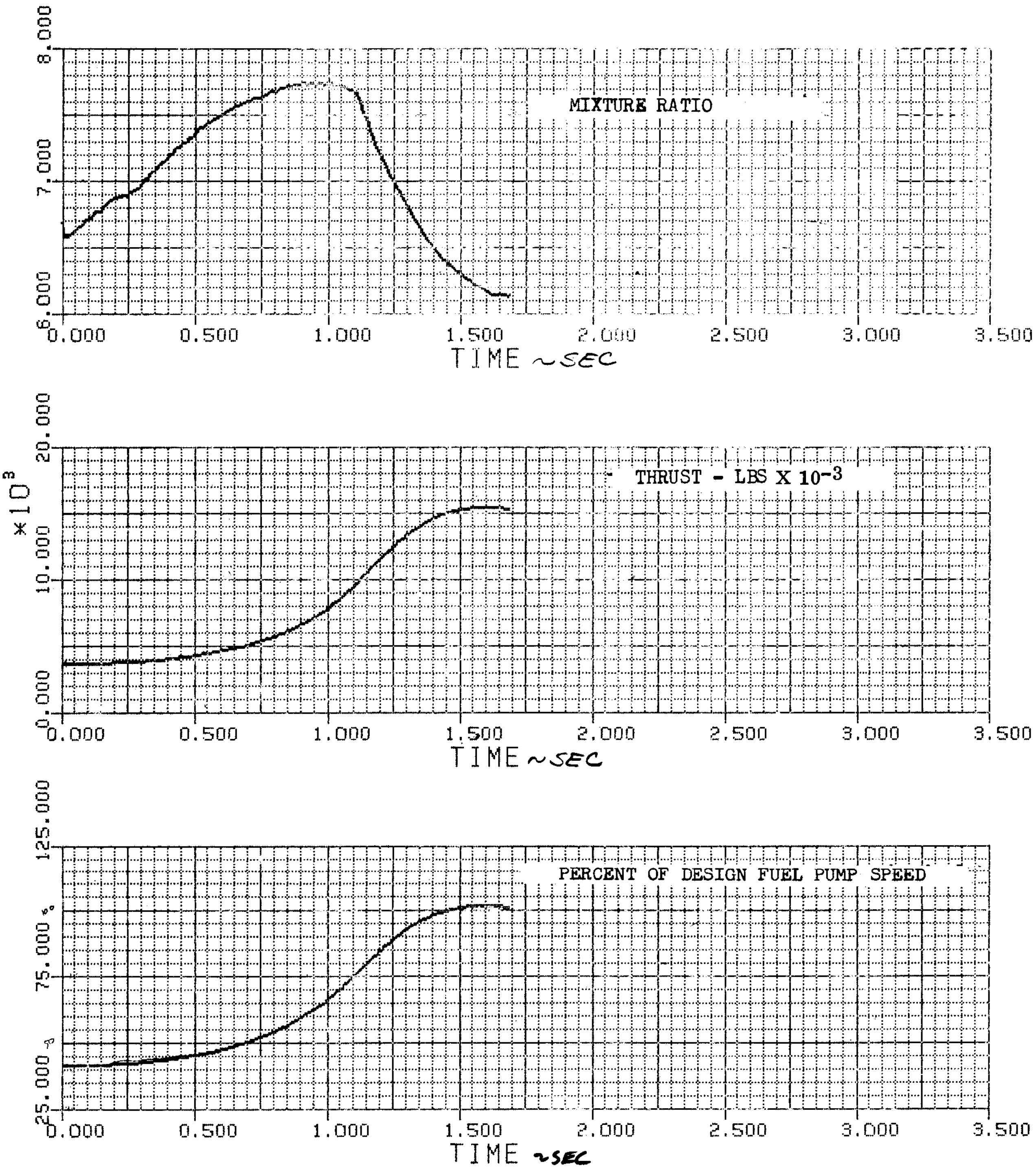


Figure C-72. Simulated Start Transient from Maneuvering Thrust to Full Thrust Category IV Engine

DF 97026

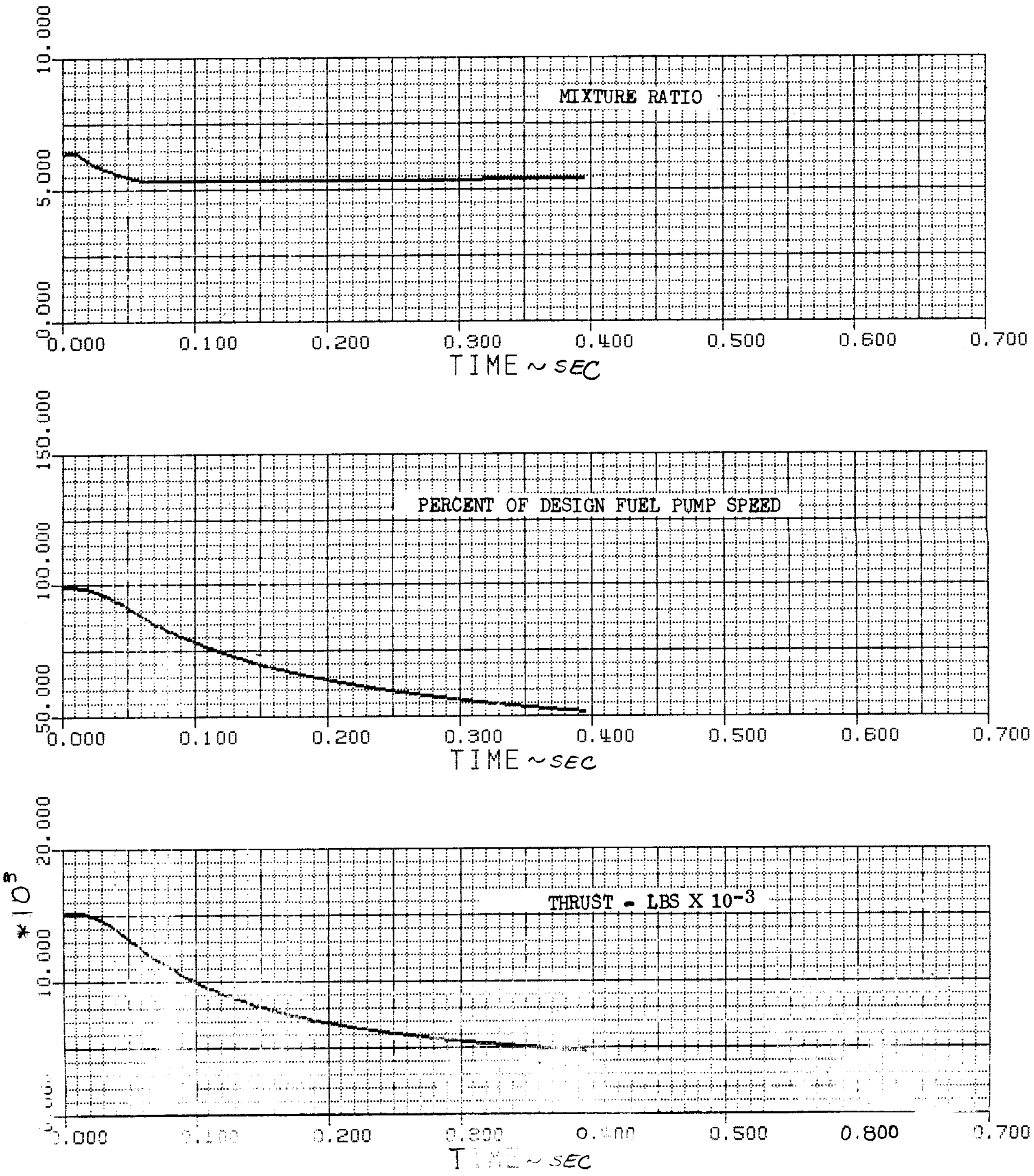


Figure C-73. Simulated Transient from Full Thrust to Maneuvering Thrust Derivative IIA and IIB Engines

DF 98272

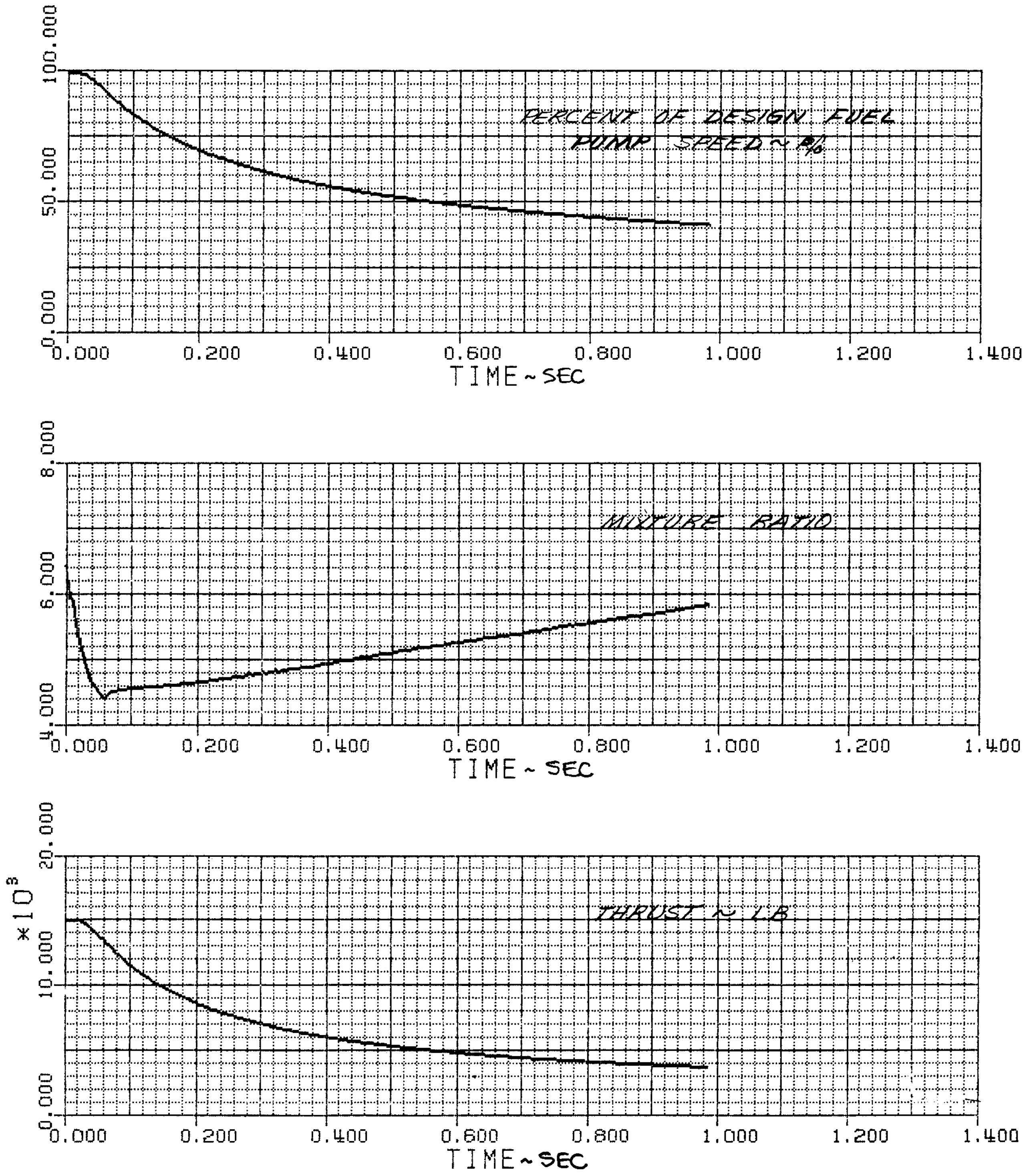


Figure C-74. Simulated Transient from Full Thrust to Maneuver Thrust Category IV Engine DF 97441

The Category IV engine is predicted to transmit approximately 100 hp from the fuel turbine to the oxidizer turbine shaft immediately after deceleration is initiated, as shown in figure C-68. The load was in a different direction than during the acceleration. However, during the deceleration the load always remained in this direction. Because the high level of horsepower occurs over such a short period of time, it is not expected to have a significant effect on gear life.

d. Deceleration Transient From Maneuvering Thrust (Pumped Idle) to THI

Maintaining satisfactory mixture ratio control was again the major area of concern in establishing control requirements for the deceleration transient from Maneuvering Thrust (pumped idle) to THI. Satisfactory deceleration transients were obtained which met the mixture ratio requirements.

This transient is accomplished by opening the turbine bypass valve to its THI position and closing the main fuel shutoff valve. As the engine decelerates, the nozzle coolant and tank pressurization valves also close automatically with the decreasing engine pressures. The oxidizer flow control valve closes slowly as oxidizer pump pressure rise decays, and when the pressure across the oxidizer pump decays sufficiently, the GO_2 control valve opens.

The mixture ratio characteristics during this transient for the Derivative IIA and IIB and the Category IV engines are shown in figures C-75 and C-76, respectively. Mixture ratios are primarily set by the actuation rates of the two oxidizer valves. Minimum and maximum mixture ratios obtained for this transient were again similar to those obtained during the acceleration transients. This is because both of the oxidizer valves followed the same pressure-area schedules as were used for the acceleration transients. Peak coolant temperatures at the exit of the primary nozzle for the Derivative II and Category IV engines were only 720°R and 775°R, respectively.

Thrust characteristics during these transients for the Derivative II and Category IV engines are shown in figures C-75 and C-76, respectively. The Derivative IIA and IIB engines decelerated to 90% of the thrust change within 1.0 sec whereas the Category IV engine required 1.6 sec to decelerate to that level. The Category IV engine decelerates slower than the Derivative II engines because it has a lower turbine bypass area at THI. The peak horsepower transmitted between the turbines during this transient was only 10 hp as shown in figure C-68, and no changes in direction occurred.

e. Direct Shutdowns From Any Mode

The valve sequencing used on these engines for a direct shutdown is identical to the shutdown sequence presently used on RL10 engines. In view of this, transient simulations to evaluate this type of shutdown were not considered necessary. Therefore, thrust decay characteristics were estimated from RL10 data that were available. The thrust decay characteristics for this type of shutdown for the Derivative IIA or IIB and for the Category IV engine are shown in figures C-77 and C-78, respectively.

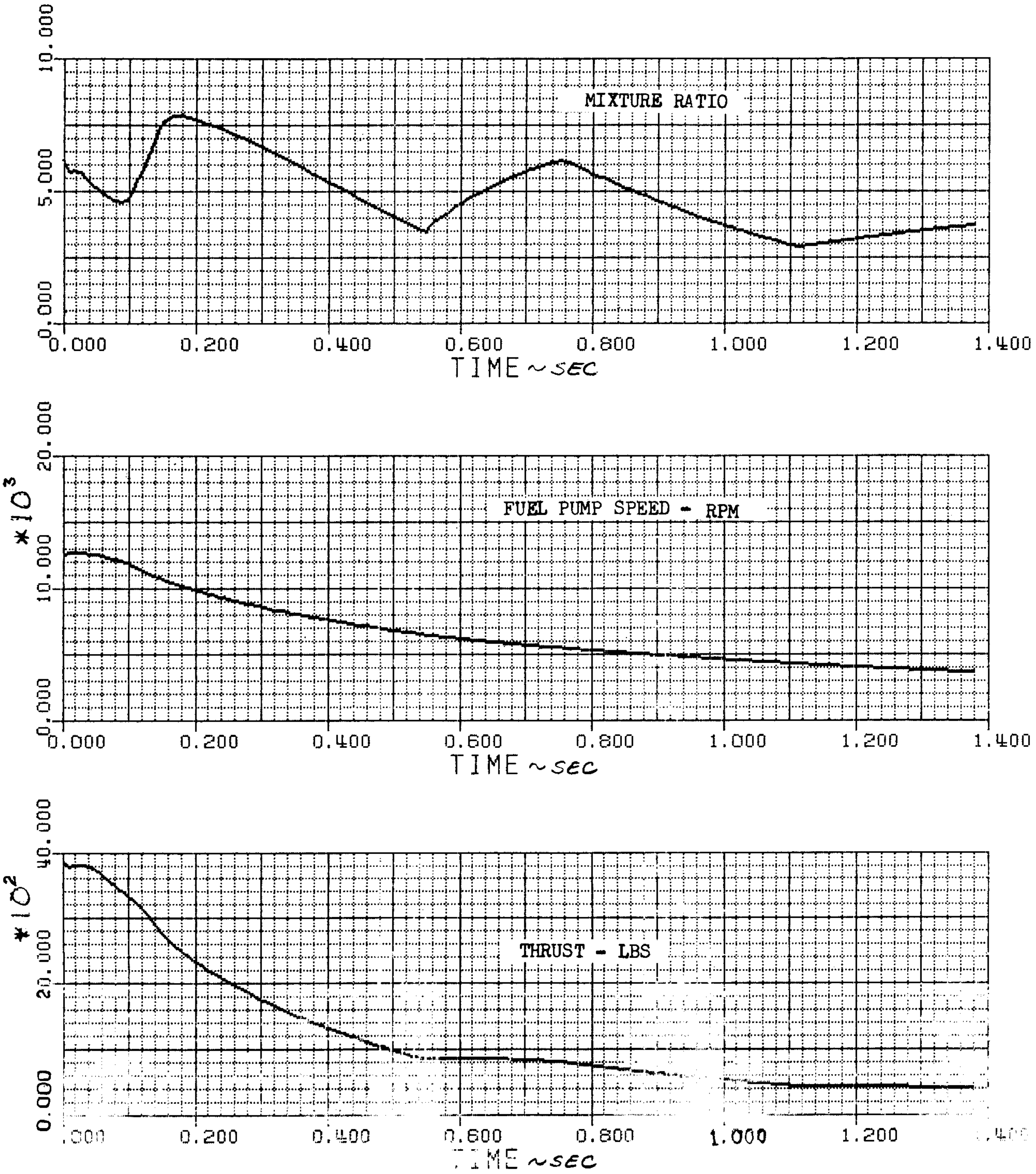


Figure C-75. Simulated Transient from Maneuvering Thrust to Tank Head Idle Derivative IIA and IIB Engines DF 98273

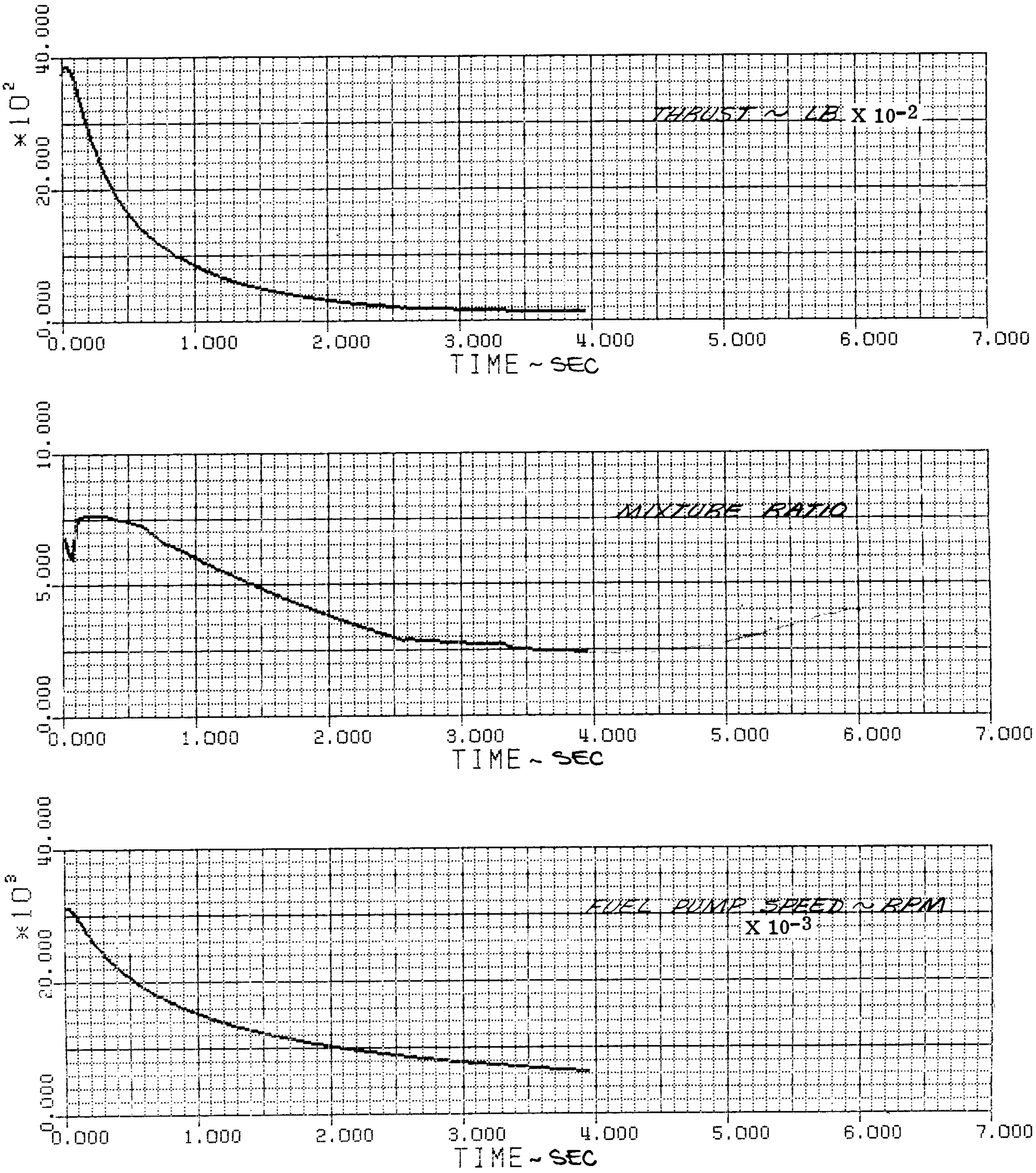
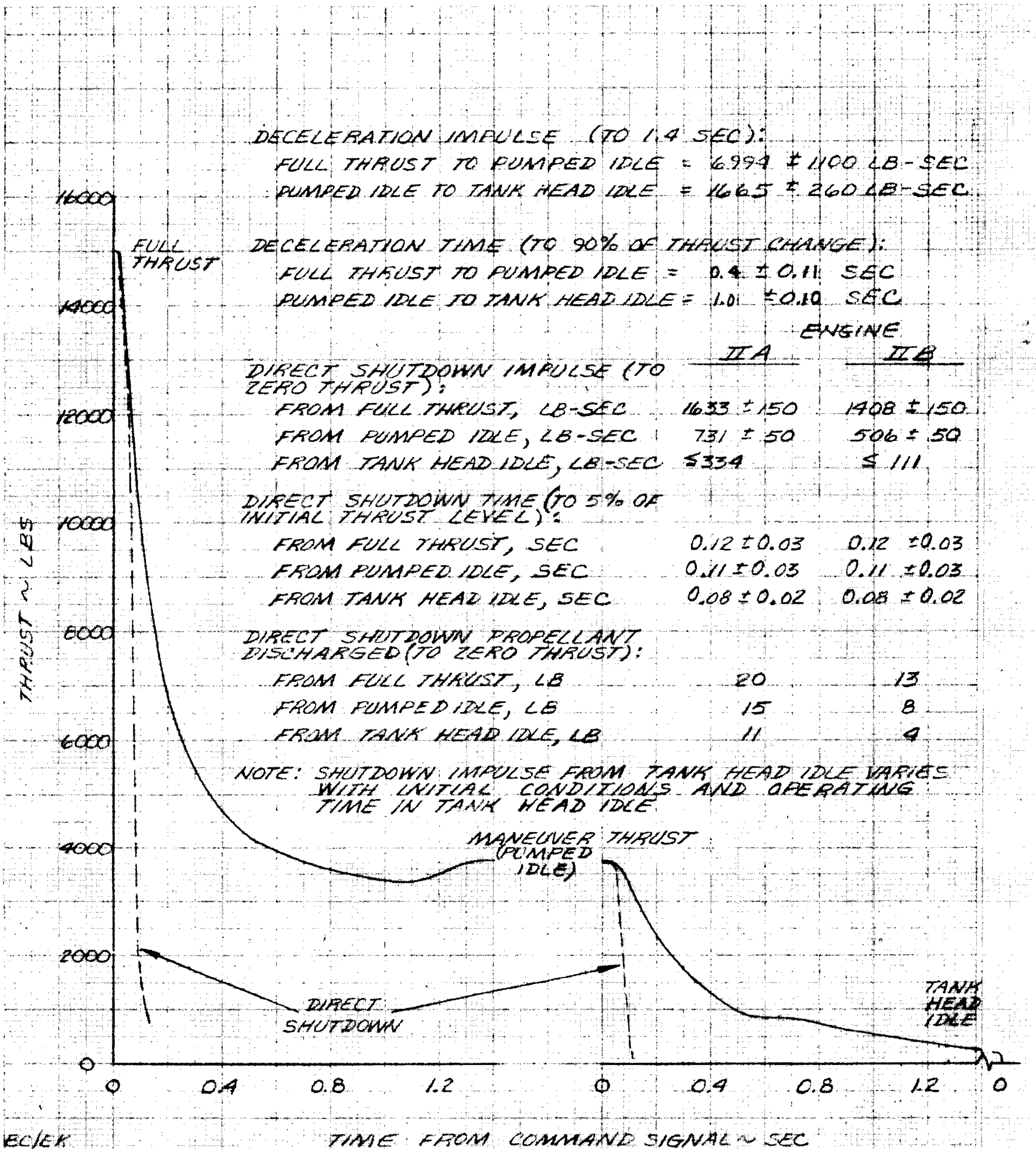


Figure C-76. Simulated Transient from Maneuver Thrust to Tank Head Idle Category IV Engine

DF 97091



DECELERATION IMPULSE (TO 1.4 SEC):
FULL THRUST TO PUMPED IDLE = 6994 ± 1100 LB-SEC
PUMPED IDLE TO TANK HEAD IDLE = 1665 ± 260 LB-SEC

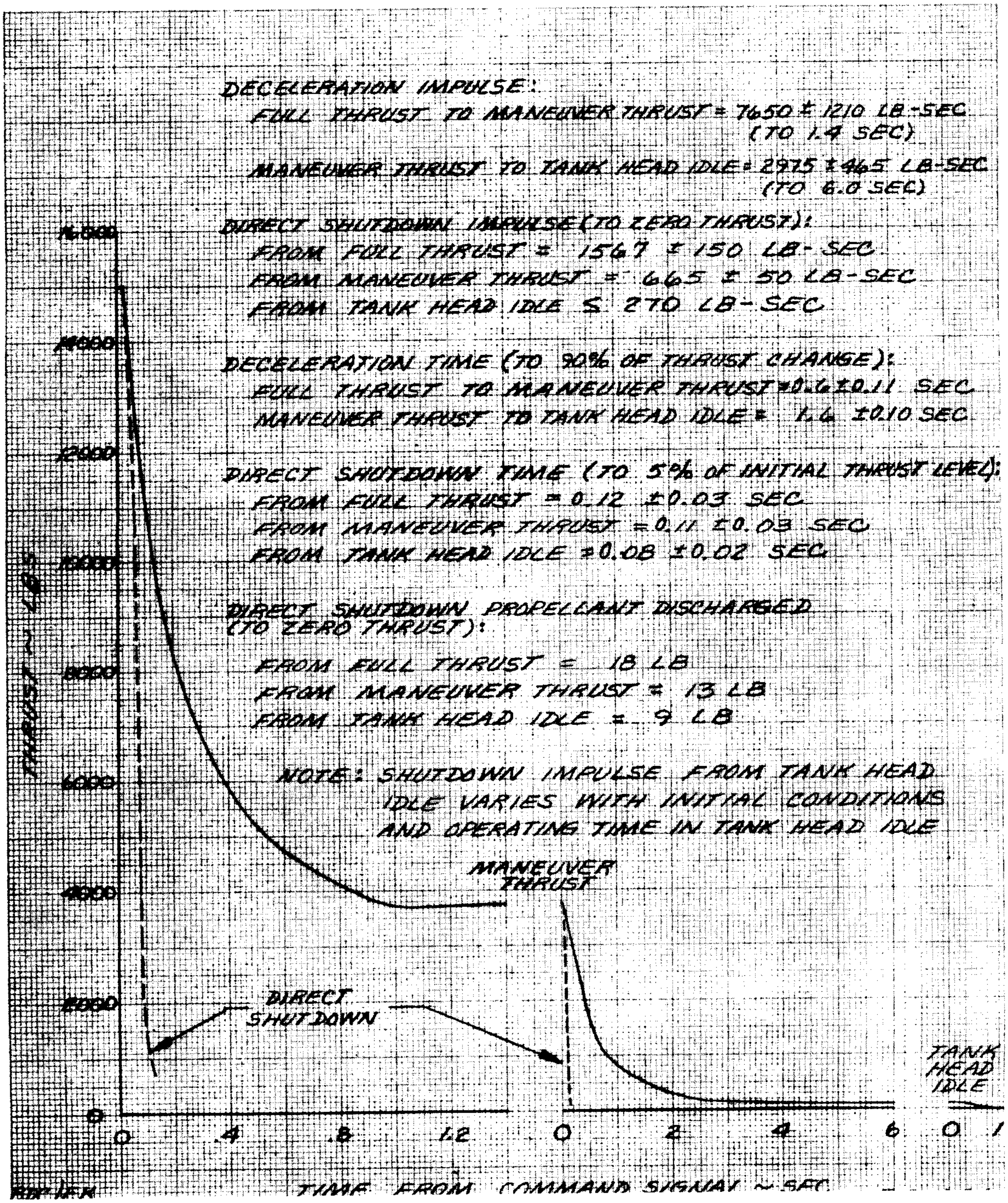
DECELERATION TIME (TO 90% OF THRUST CHANGE):
FULL THRUST TO PUMPED IDLE = 0.4 ± 0.11 SEC
PUMPED IDLE TO TANK HEAD IDLE = 1.0 ± 0.10 SEC

	ENGINE IIA	ENGINE IIB
DIRECT SHUTDOWN IMPULSE (TO ZERO THRUST):		
FROM FULL THRUST, LB-SEC	1633 ± 150	1408 ± 150
FROM PUMPED IDLE, LB-SEC	731 ± 50	506 ± 50
FROM TANK HEAD IDLE, LB-SEC	≤334	≤ 111
DIRECT SHUTDOWN TIME (TO 5% OF INITIAL THRUST LEVEL):		
FROM FULL THRUST, SEC	0.12 ± 0.03	0.12 ± 0.03
FROM PUMPED IDLE, SEC	0.11 ± 0.03	0.11 ± 0.03
FROM TANK HEAD IDLE, SEC	0.08 ± 0.02	0.08 ± 0.02
DIRECT SHUTDOWN PROPELLANT DISCHARGED (TO ZERO THRUST):		
FROM FULL THRUST, LB	20	13
FROM PUMPED IDLE, LB	15	8
FROM TANK HEAD IDLE, LB	11	4

NOTE: SHUTDOWN IMPULSE FROM TANK HEAD IDLE VARIES WITH INITIAL CONDITIONS AND OPERATING TIME IN TANK HEAD IDLE

16000
14000
12000
10000
8000
6000
4000
2000
0
THRUST ~ LBS
FULL THRUST
MANEUVER THRUST (PUMPED IDLE)
DIRECT SHUTDOWN
TANK HEAD IDLE
0
0.4
0.8
1.2
0
0.4
0.8
1.2
0
TIME FROM COMMAND SIGNAL ~ SEC
EC/EK

Figure C-77. Deceleration and Shutdown Transients from Full Thrust to Tank Head Idle Derivative IIA and IIB Engines DF 96623



DECELERATION IMPULSE:

FULL THRUST TO MANEUVER THRUST = 7650 ± 1210 LB-SEC
(TO 1.4 SEC)

MANEUVER THRUST TO TANK HEAD IDLE = 2975 ± 465 LB-SEC
(TO 6.0 SEC)

DIRECT SHUTDOWN IMPULSE (TO ZERO THRUST):

FROM FULL THRUST = 1567 ± 150 LB-SEC
FROM MANEUVER THRUST = 665 ± 50 LB-SEC
FROM TANK HEAD IDLE ≤ 270 LB-SEC

DECELERATION TIME (TO 90% OF THRUST CHANGE):

FULL THRUST TO MANEUVER THRUST = 0.6 ± 0.11 SEC
MANEUVER THRUST TO TANK HEAD IDLE = 1.6 ± 0.10 SEC

DIRECT SHUTDOWN TIME (TO 5% OF INITIAL THRUST LEVEL):

FROM FULL THRUST = 0.12 ± 0.03 SEC
FROM MANEUVER THRUST = 0.11 ± 0.03 SEC
FROM TANK HEAD IDLE = 0.08 ± 0.02 SEC

DIRECT SHUTDOWN PROPELLANT DISCHARGED
(TO ZERO THRUST):

FROM FULL THRUST = 18 LB
FROM MANEUVER THRUST = 13 LB
FROM TANK HEAD IDLE = 9 LB

NOTE: SHUTDOWN IMPULSE FROM TANK HEAD IDLE VARIES WITH INITIAL CONDITIONS AND OPERATING TIME IN TANK HEAD IDLE

THRUST ~ LBS
16000
14000
12000
10000
8000
6000
4000
2000
0
MANEUVER THRUST
DIRECT SHUTDOWN
TANK HEAD IDLE
0
.4
.8
1.2
0
2
4
6
0
1
TIME FROM COMMAND SIGNAL ~ SEC

Figure C-78. Estimated Deceleration and Shutdown Transients from Full Thrust to Tank Head Idle Category IV

DF 97084

3.3 Engine Design Criteria

The designs of all new engine components were based upon the design criteria defined below:

General Structure Criteria

$$\text{Minimum Yield Factor of Safety} = \frac{\text{Yield Strength}}{\text{Limit Load}} \geq 1.1$$

$$\text{Minimum Ultimate Factor of Safety} = \frac{\text{Ultimate Strength}}{\text{Limit Load}} \geq 1.4$$

Special Structural Criteria For Pressure Vessels

Proof Pressure	=	Proof factor x limit pressure at the design temperature
Burst Pressure	=	1.5 x limit pressure at the design temperature
Proof Criteria		Component membrane stress not to exceed 0.2% yield strength at proof pressure and maximum component cycle temperature.
Burst Criteria		Component must withstand burst pressure at the maximum component cycle temperature without failure.
Limit Pressure	=	Maximum component operating pressure. The maximum operating pressure is to include transient overshoots, pressure steady-state tolerances and vehicle acceleration effects on the propellant in the pressure vessel.
Proof Factor		The proof factor shall be 1.2 or a higher factor as determined by fracture mechanics analysis.
Fatigue Criteria		
Low Cycle Fatigue (LCF)		All components shall be designed to have an LCF factor of 4 (on cycles) based on minimum life. Miner's rule will be used to determine the allowable strain range for LCF limited components.
High Cycle Fatigue (HCF)		All components shall be designed to have a HCF factor of 10. A goodman diagram will be used to determine allowable vibratory stress.
Life Limited Components		All RL10A-3-3 engine components that are redesigned and all Category IV components will be designed for 10 hr life (or 160 cycles) at full-rated thrust.

Turbopump Design Criteria

1. Disk burst speed will be at least 20% above the maximum operating speed. The maximum effective stress at 5% above the maximum operating speed will not exceed the allowable 0.2% yield stress.

2. Rim growth will not exceed 0.1% (plastic + creep strain).

3. Shroud effective stress (direct plus bending) will not exceed 1.1 times 0.2% yield strength.

4. Average tangential stress of the shroud will not exceed 90% of the disk allowable average tangential stress.

5. Critical Speed

    a. Rotor bending frequency will be at least 25% above the rotor maximum operating speed.

    b. A minimum margin of 20% will be maintained between rotor rigid body critical speeds and rotor steady-state operating speeds.

6. Rolling element bearings will be used at DN levels below the maximum values listed below:

	LOX	LH_2	GH_2
Roller	1.5×10^6	2.0×10^6	0.5×10^6
Ball	1.5×10^6	2.0×10^6	1.2×10^6

B_{10} life 100 hr

7. Face seal designs will use no more than the maximum values listed below:

	LOX	LH_2	GH_2
PV factor	25,000	50,000	20,000
FV factor	2,000	4,000	1,500
P_fV factor	60,000	200,000	50,000

PV = Unit load times rubbing velocity (lb/in^2 x ft/sec)

FV = Face load per unit length times rubbing velocity (lb/in. x ft/sec)

PfV = Fluid pressure differential times rubbing velocity (psig x ft/sec)

8. Gears (Hydrogen Gas Cooled)

Pitch line velocity (maximum)	20,000 fpm
Hertz stress (maximum)	77,000 psi
Material	AMS 6260

9. Impeller burst speed will be at least 20% above the maximum operating speed. Maximum effective stress at 5% above the maximum operating speed will not exceed the allowable 0.2% yield stress.

10. Blade root steady stress will not exceed the allowable 1% 10-hr creep stress. Blade natural frequencies will be kept at least 15% away from known sources of excitation at steady-state speed.

4. MECHANICAL CONCEPT DEFINITION

4.1 Derivative IIA Engine

4.1.1 Main Chamber and Primary Nozzle

The primary function of the thrust chamber and primary nozzle is to provide a converging/diverging section for the combustion and expulsion of propellants at high velocity to produce thrust. It also serves as a heat exchanger to supply turbine power for the propellant pumps and as a support for the extendible nozzle and actuation system. In the main chamber of the Derivative II engine, propellants are burned at a nominal mixture ratio of 6.36 to provide a chamber pressure of 400 psi at full thrust. Because the main chamber/nozzle operates at conditions similar to that of the RL10A-3-3 engine the design has been based upon concepts proven during RL10 operation. The requirements for the Derivative II engine chamber/nozzle are listed in table C-17.

4.1.1.1 Mechanical Description

The propellant injector is shown schematically in figure C-79. The function of the injector is to atomize the oxidizer and promote thorough mixing of the fuel and oxidizer to provide the correct conditions for efficient combustion of the propellants.

The propellant injector consists of 216 elements arranged in 8 equally spaced concentric circles. Each element consists of an oxidizer orifice and a concentric fuel orifice. All elements except those in the inner and outer rows incorporate swirlers which aid in the dispersion of the oxidizer.

Liquid oxygen enters the injector through the oxidizer injector manifold, flows into the cavity between cones 2 and 3, and then flows out of the oxidizer orifices and into the combustion chamber.

Gaseous hydrogen enters the peripheral fuel injector manifold and flows into the cavity between cones 1 and 2. The hydrogen flows out through the annular orifices around the elements, into the combustion chamber where it mixes with the oxidizer. Hydrogen also passes through cone 1, which consists of a porous-welded, steel mesh plate. This flow provides transpiration cooling of the injector face (cone 1) and amounts to approximately 10% of the total hydrogen flow.

Table C-17. Derivative IIA Chamber/Nozzle Requirements

Parameter	Tank Head Idle	Maneuver Thrust	Full Thrust
Injector			
Fuel Inlet Pressure, psia	6.8	125.	469.
Oxidizer Inlet Pressure, psia	14.5	106.	447.
Fuel ΔP, psi	1.6	22.5	68.6
Oxidizer ΔP, psi	9.3	3.2	46.7
Fuel Inlet Temperature, °R	426.	612.	451.
Oxidizer Inlet Temperature, °R	385.	165.	167.
Combustion Chamber & Primary Nozzle			
Chamber Pressure, psia	5.2	102.	400.
Chamber Mixture Ratio	4.32	6.58	6.36
Coolant Tube Inlet Pressure, psia	15.9	205.	814.
Coolant Tube Inlet Temperature, °R	36.8	45.	55.7
Coolant Tube ΔP, psi	5.6	51.	154.
Coolant Tube ΔT, °R	545.	577.	419.
Mechanical Requirements			
Throat Diameter, in.			4.87
Primary Nozzle Exit ID, in.			40.0
Primary Nozzle Area Ratio			66.
Primary Nozzle Length (Throat to Exit) in.			30.9

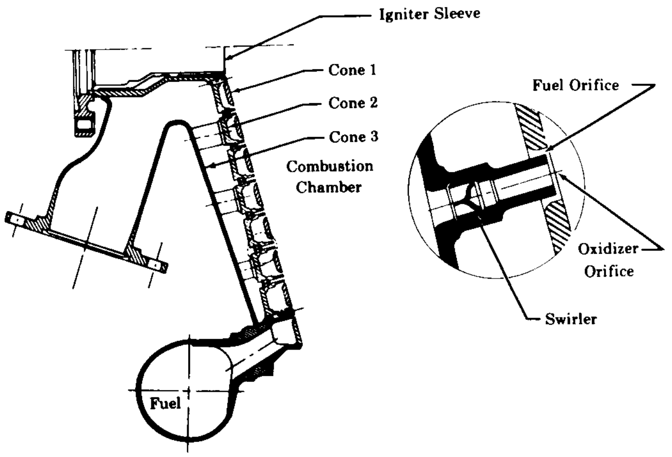

Igniter Sleeve
Cone 1
Cone 2
Cone 3
Combustion Chamber
Fuel Orifice
Oxidizer Orifice
Swirler
Fuel

Figure C-79. Propellant Injector FD 1554F

Immediate contact between oxidizer and fuel is made at each element as the oxidizer and fuel leave the face of the injector and enter the combustion chamber.

The thrust chamber shown schematically in figure C-80 is a regeneratively cooled, furnace brazed assembly consisting of a fuel inlet manifold; 180 short, single-tapered tubes; a turnaround manifold; 180 full length, double-tapered tubes; a fuel exit manifold; and various stiffeners and component supports.

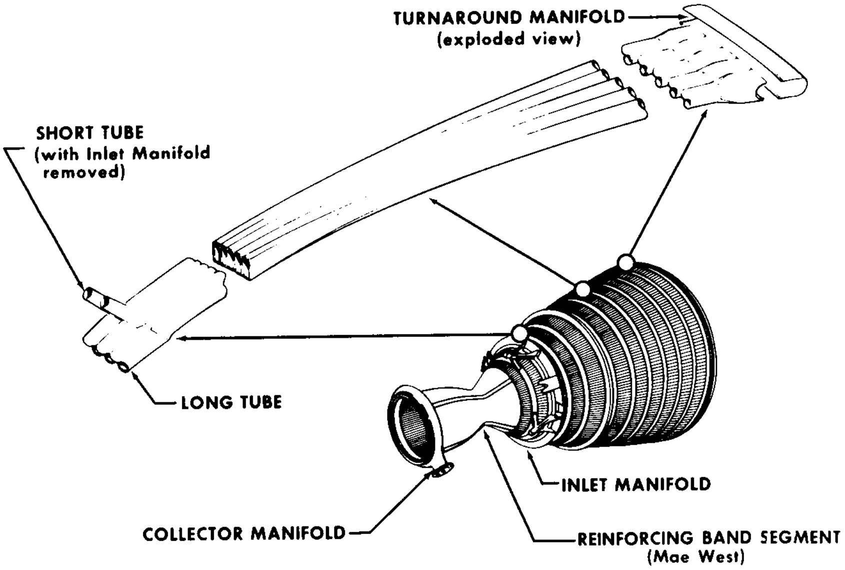


Figure C-80. Thrust Chamber FD 4269

In this chamber design, some of the heat released during combustion is transferred to the chamber coolant flowing in the tubes, and is used to provide energy for driving the turbopump.

Hydrogen enters the thrust chamber at the inlet manifold downstream of the throat, and immediately flows into 180 single-tapered short tubes that are interleaved between 180 double-tapered, full length tubes. The full length tubes form the full periphery of the combustion chamber, the throat, and the nozzle down to the junction of the short tubes. The periphery of the remainder of the nozzle is formed by all 360 tubes. The hydrogen flows rearward in the short tubes to the turnaround manifold where it enters the 180 full length tubes and then travels forward the entire length of the chamber to the exit manifold. This partial two-pass method of chamber construction was adopted to achieve high coolant velocity and heat transfer, and low tube-wall temperature.

Both full length and short tubes are brazed together to form a seal, and are structurally supported by stiffener bands to carry the chamber hoop loads. These bands also minimize the effect of any flow-induced vibration.

A torch igniter system provides thrust chamber ignition. This system, shown in figure C-81, provides a metered flow of hydrogen and oxygen, which are mixed in an igniter chamber, ignited by a spark, and passed into the combustion chamber to ignite the main propellants. Increased reliability is accomplished by providing dual exciters and spark igniters and by eliminating the need for igniter propellant shutoff valves. The dual spark and exciter configuration provides a fail-safe energy source and designing the igniter to operate at rated thrust with oxidizer and fuel igniter flows eliminates the possibility of igniter damage due to valve leakage.

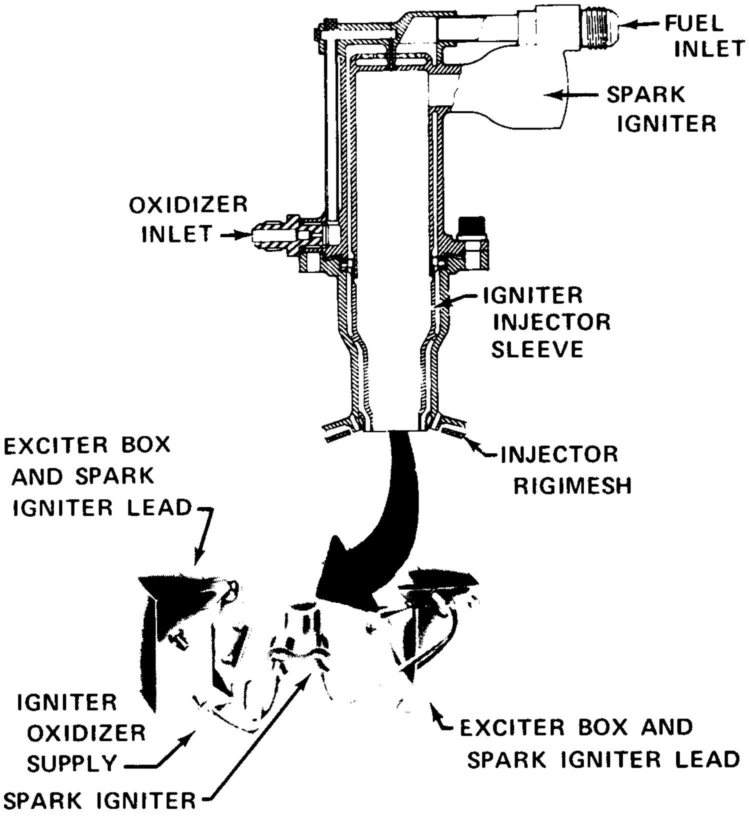

FUEL INLET
SPARK IGNITER
OXIDIZER INLET
IGNITER INJECTOR SLEEVE
EXCITER BOX AND SPARK IGNITER LEAD
INJECTOR RIGIMESH
IGNITER OXIDIZER SUPPLY
SPARK IGNITER
EXCITER BOX AND SPARK IGNITER LEAD

Figure C-81. RL10 Dual Ignition System FD 47508

The fuel and oxidizer is ignited by a spark exciter assembly which provides a minimum of 20 sparks/sec at an energy level of 0.5 joules. The total oxidizer flow is injected into the igniter through a single oxidizer element located in the upper end of the igniter chamber. Fuel flow is split. Part of the flow is delivered to a concentric slot surrounding the oxidizer injector element and the remainder is used for igniter barrel cooling. The burned propellants are discharged into the main chamber through the igniter injector sleeve.

This igniter configuration was standard equipment on the RL10A-3-7 engine and has been tested extensively under conditions from tank head idle start to full thrust. Ignition studies of Derivative engines are reported in Section E and a history of RL10 igniter experience may be found in Appendix I.

4.1.1.2 Design Studies

The nozzle contour design is based on a method of characteristic solution for ideal expansion that minimizes the formation of strong shock waves. The nozzle is shorter than the ideal length to optimize performance within a given length.

Because the predicted operating environment within the chamber/nozzle of the Derivative II engine is nearly identifical to the RL10A-3-3 engine, many of the concepts shown in the design layout drawing shown in figure C-82 were obtained directly from the RL10A-3-3 design. Certain areas required an original design effort, however, to provide features unique to Derivative II engine requirements. These special requirements include designing for:

1. Heat loads in tank head idle
2. Sufficient heat pickup at full thrust with shortened nozzle length
3. Increased mixture ratio operation
4. Extendible nozzle load support.

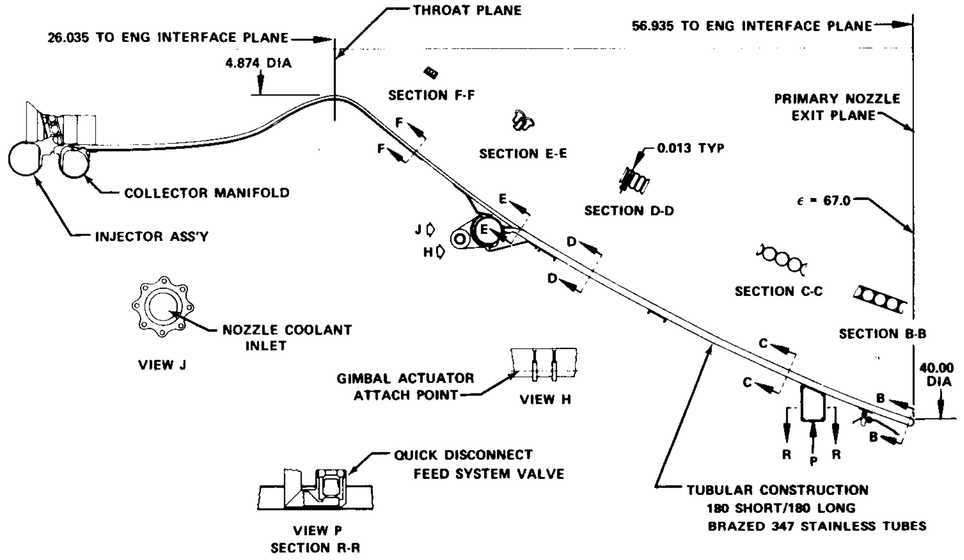

THROAT PLANE
26.035 TO ENG INTERFACE PLANE
4.874 DIA
56.935 TO ENG INTERFACE PLANE
SECTION F-F
F
F
SECTION E-E
0.013 TYP
PRIMARY NOZZLE EXIT PLANE
COLLECTOR MANIFOLD
E
SECTION D-D
ε = 67.0
INJECTOR ASS'Y
J
H
E
D
D
SECTION C-C
NOZZLE COOLANT INLET
VIEW J
SECTION B-B
C
C
40.00 DIA
GIMBAL ACTUATOR ATTACH POINT
VIEW H
B
B
R
P
R
QUICK DISCONNECT FEED SYSTEM VALVE
TUBULAR CONSTRUCTION 180 SHORT/180 LONG BRAZED 347 STAINLESS TUBES
VIEW P SECTION R-R

Figure C-82. Primary Nozzle Derivative II Engines

FD 72062

Tank head idle testing of the RL10A-3-7 engine in 1967 produced hot sidewall temperatures near the injector face of sufficient magnitude to debraze the chamber tubes as they enter the exit manifold at a location shown in figure C-83a. An experimental design produced at that time eliminated the problem by placing the braze joint inside the manifold and away from the hot wall as shown in figure C-83b. This configuration known as the "hooked tube design" was successfully tested during subsequent development of the RL10A-3-7 engine. This design feature has been included in the design concept of the Derivative II engine to provide additional design margin during tank head idle operation.

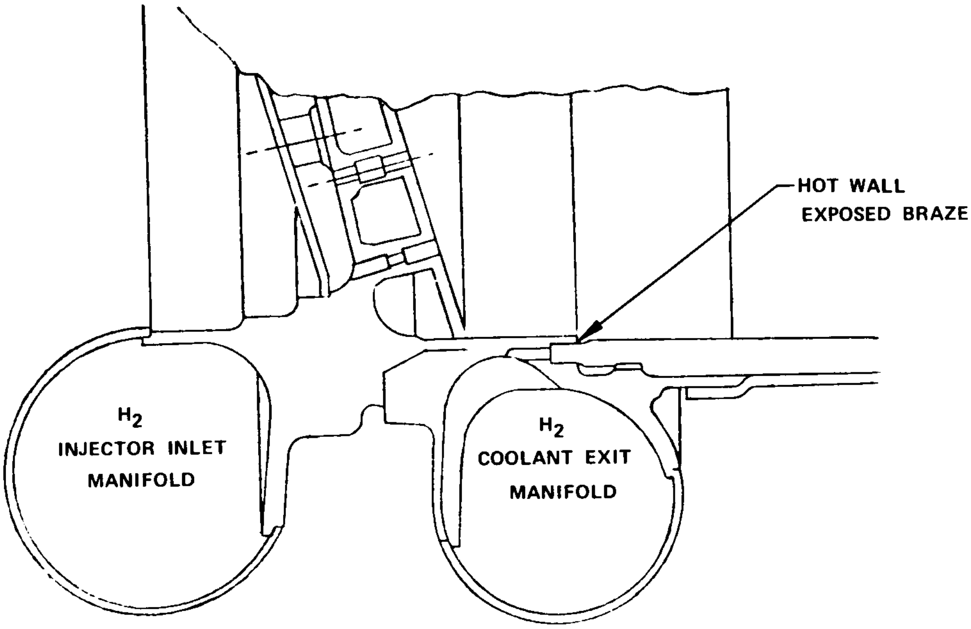

HOT WALL EXPOSED BRAZE
H_2 INJECTOR INLET MANIFOLD
H_2 COOLANT EXIT MANIFOLD

Figure C-83a. RL10A-3-3 Coolant Exit Manifold With Hot Wall Exposed Brazement FD 76261

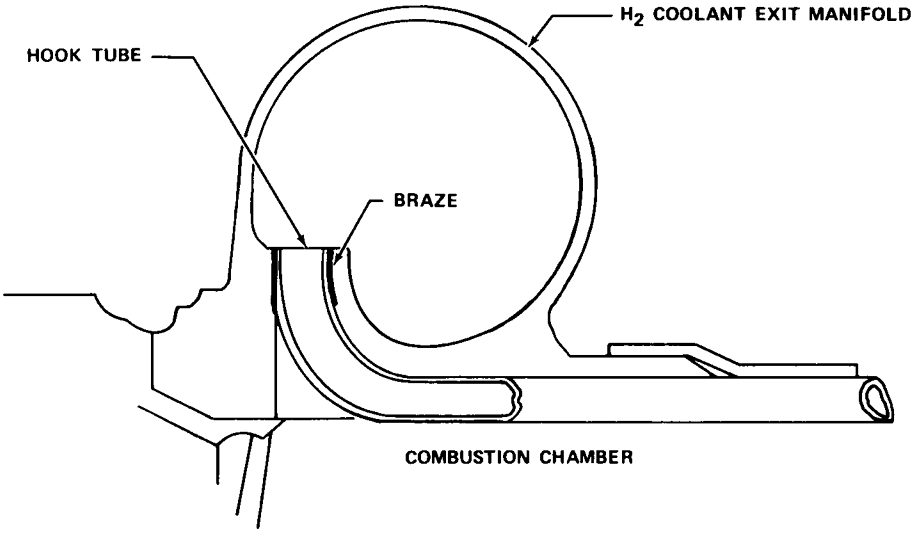

H_2 COOLANT EXIT MANIFOLD
HOOK TUBE
BRAZE
COMBUSTION CHAMBER

Figure C-83b. Hook Tube Detail FD 27529A

While the predicted heat flux levels for the Derivative II engine thrust chamber and nozzle, as shown in figure C-84, are essentially the same as that encountered by the production RL10A-3-3, the primary thrust nozzle length of the Derivative II engine is shorter by 15.3 in. (See 3.1.3). This decrease in nozzle surface area would result in a reduction in heat pickup of about 700 Btu/sec during full thrust operation. Since this loss in heat would cause a 50 deg decrease in available turbine inlet temperature, it represented an unacceptable loss in turbine power. To recover the heat needed for turbine power, a chamber length was selected which was 2.25 in. longer than that of the RL10A-3-3 chamber. This length provided the additional heat necessary to balance the Derivative II engine cycle with the required excess power as shown in figure C-85.

The injector, as previously shown in figure C-79, will require injector element optimization in the final design to optimize specific impulse for a mixture ratio change from 5 to 6. The exact form that these changes will take will depend on the results of injector cold flow and hot firing tests to be conducted during engine development. The changes, as currently conceived, will consist of refinements in flow distribution with no basic concept changes anticipated.

The nozzle interface with the translating nozzle will have to be sealed to prevent hot gas leakage. The seal that is mounted on the aft end of the primary nozzle, as shown in figure C-82, is described in detail in Section 3.2.1.2. Mounting the seal will be accomplished after the nozzle brazement is complete. It will be riveted to a continuous ring flange brazed on the tubes near the exit diameter. This ring replaces a stiffness band used on the RL10A-3-3. A box ring has also been added to support the nozzle jackscrew gearboxes and drive motors. The same structure is used to hold the quick-disconnect valve used to supply hydrogen coolant to the translating portion of the thrust nozzle.

4.1.1.3 Life

The life of the Derivative II engine thrust chamber is predicted to be limited by coolant tube fatigue just slightly upstream of the chamber throat. RL10A-3-1 engines have demonstrated as many as 345 start cycles and the RL10A-3-3 engine has demonstrated over 130 start cycles. Six RL10 engines have been tested over 150 times without tube failure. Based upon this data the life of the Derivative II engine is predicted to be greater than 190 cycles.

4.1.1.4 Materials

The materials for the Derivative II thrust chamber and nozzle were selected based upon favorable RL10 experience. These materials are shown in table C-18.

Table C-18. Derivative II Materials Selection

Machined Portion	Stainless Steel Forging	AMS 5646
Formed Portion	Stainless Steel Sheet	AMS 5512
Reinforcing Bands	Stainless Steel Sheet	AMS 5512
Porous Injector Face	Heat-Resistant Alloy Wire	AMS 5794
Brackets	Stainless Steel Sheet	AMS 5512

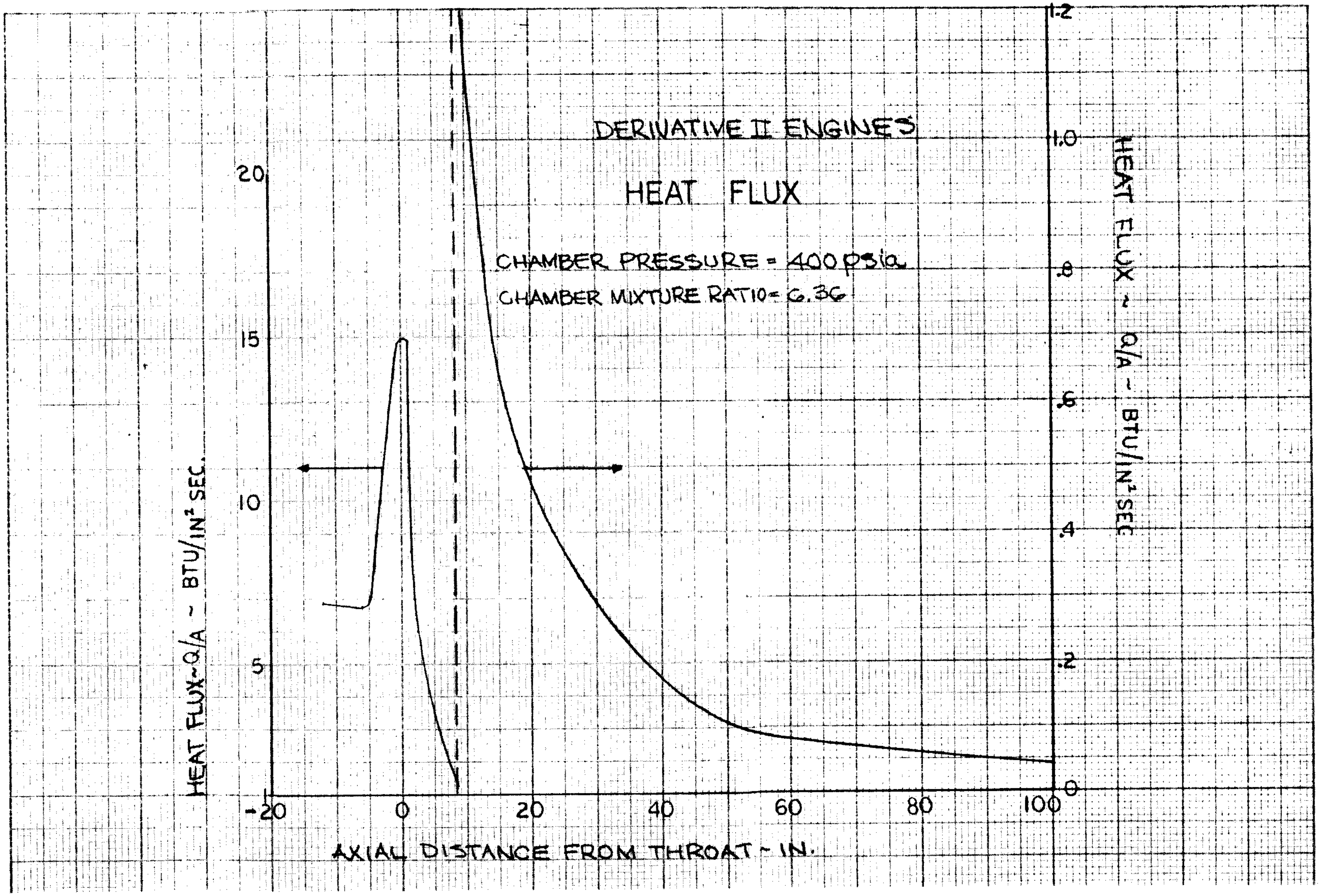


Figure C-84. Derivative II Engine Heat Flux

DF 98274

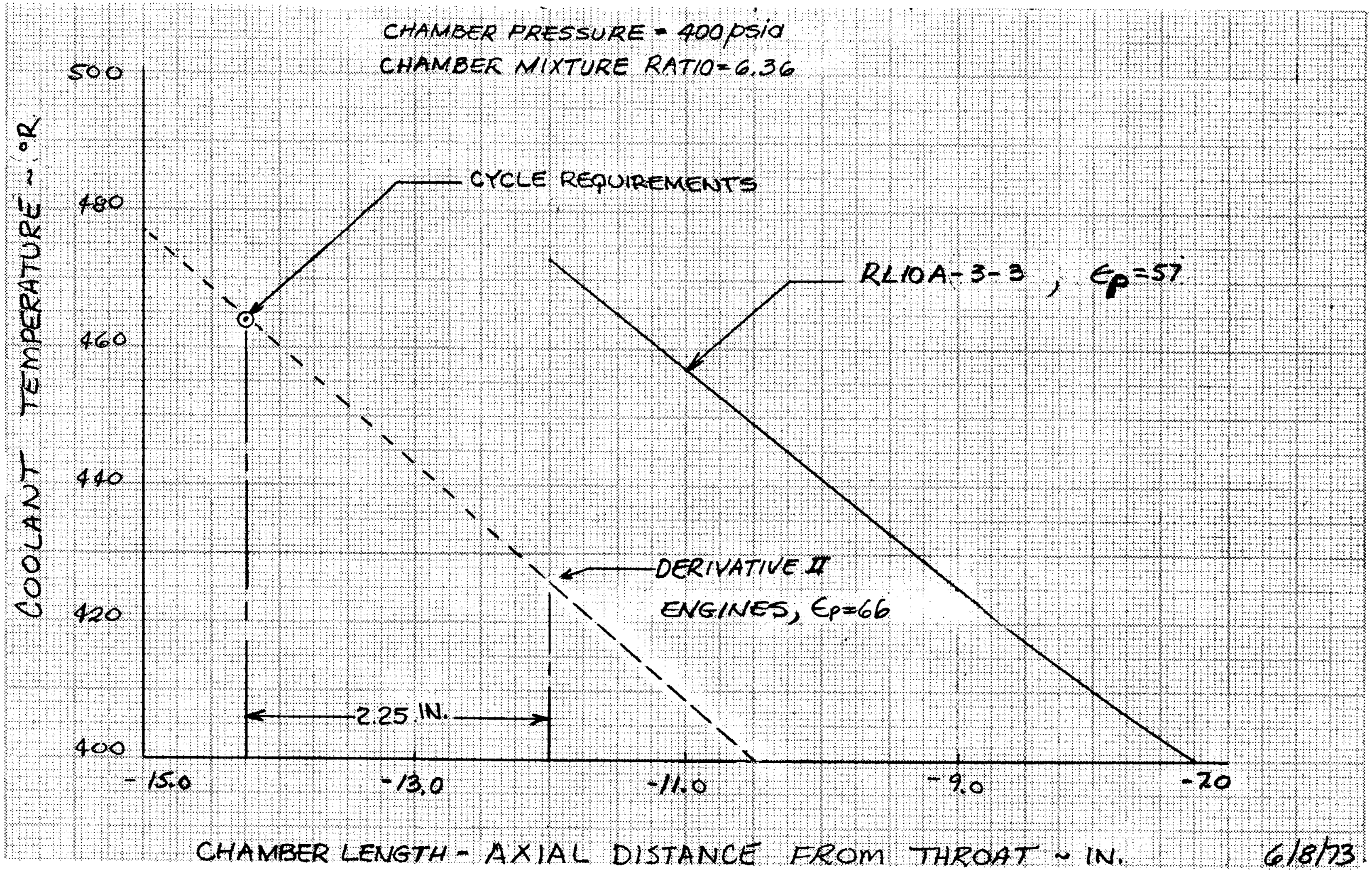


Figure C-85. Chamber Length Required for Derivative II Engines

DF 98275

4.1.1.5 Weight

The weight of the Derivative II chamber and nozzle was based upon a combination of calculated and weighed parts. The injector, inlet and exit manifolds are the same as RL10 components and have been weighed. The remainder of the weights have been calculated from the chamber and nozzle layout drawings. The estimated weights of major parts are shown in table C-19 below:

Table C-19. Weights of Major Components

Component	Weight, lb
Injector Assembly	14.9
Thrust Chamber/Nozzle	83.0
Support Ring and Seal	12.0
Total	109.9

4.1.2 Extendible Nozzle

The primary function of the extendible nozzle is to provide increased engine specific impulse without increasing engine length. It must translate forward over the primary thrust nozzle to provide a short engine package. The extendible nozzle must be a lightweight dependable design and extend or retract in a reasonable length of time, and be cooled by low pressure hydrogen from the gearbox.

4.1.2.1 Mechanical Description

The nozzle is translated by means of a jackscrew actuation system. The translating structure consists of three ballscrew jackshafts which are attached on the rear end of the primary nozzle by individual drive gearboxes and bearing assemblies, and are supported at their forward end by an adjustable link, as shown in figures C-86 and C-87. The ballscrew shaft is supported on anti-friction bearings at both the front and rear locations. These ball bearings take radial and axial loads and are housed in a spherical ball joint which compensates for shaft misalignment as great as two degrees.

The nozzle drive/synchronization system consists of two electric motors and three interconnecting flexible cables which transmit motor torque to three gear transmissions which drive the ballscrew shafts as shown in figure C-88. A locking mechanism at the base of one ballscrew assembly locks the nozzle in either the extended or retracted position. The lock, shown in figure C-89, is a spring-loaded normally locked mechanism.

A spline is added to the cable at the lock location so that engaging the spline stops travel. The engagement is ensured by dual helical springs loading the spline locks. When electric power is provided to the drive motor, the electric solenoids within the lock are energized and movement of the solenoid rotates the cam, and depresses the cam follower and springs within the lock mechanism. This disengages the spline lock and allows drive shaft rotation. The fixed spline lock is always the first to engage and it moves with the shaft pulling the floating spline into the locked position. Rotation torque is taken out by the spline lock pivot pin. The redundant lock solenoids are actuated during nozzle translation only.

The nozzle is attached to the translating mechanism at three equally spaced points through a nozzle attachment bracket to the ballscrews. The nozzle attachment bracket consists of a split circular ring and two-piece yoke as shown in figure C-90. The ballnut gimbal attachment provides two degrees of swivel freedom to prevent transferring bending loads from the nozzle attachment bracket to the ballnut.

The extendible nozzle is dump cooled, and is formed by a smooth outer skin and a corrugated inner skin that are resistance seam welded longitudinally as shown in figures C-91 and C-92. The corrugations form coolant passages for hydrogen which enters at an inlet manifold located at an area ratio of 66 and discharges to the atmosphere after passing through exit nozzles formed by dimples in the corrugations at an overall engine nozzle area ratio of 262.

The hydrogen coolant is uniformly distributed to the dump cooled nozzle by the inlet manifold shown in figure C-93, consisting of a circular torus machined integral with a cylinder and a cone. Holes are drilled such that the torus supplies hydrogen to the dump cooled nozzle corrugations and also to a seal land manifold which supplies coolant to the internozzle seals.

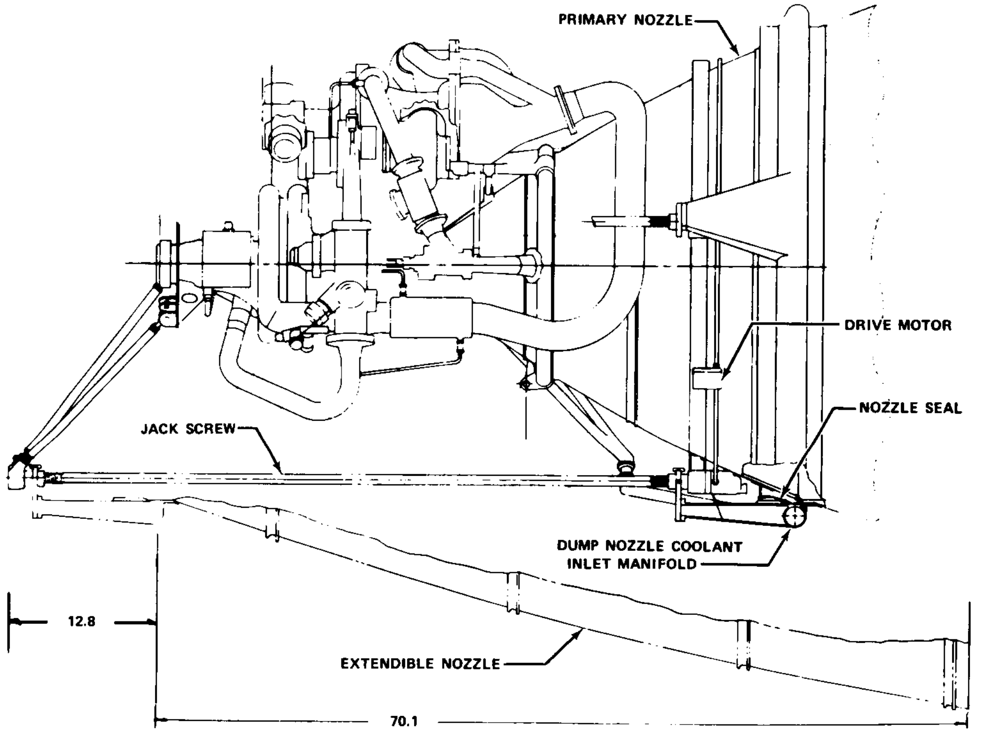


Figure C-86. Nozzle Translation System FD 71302A

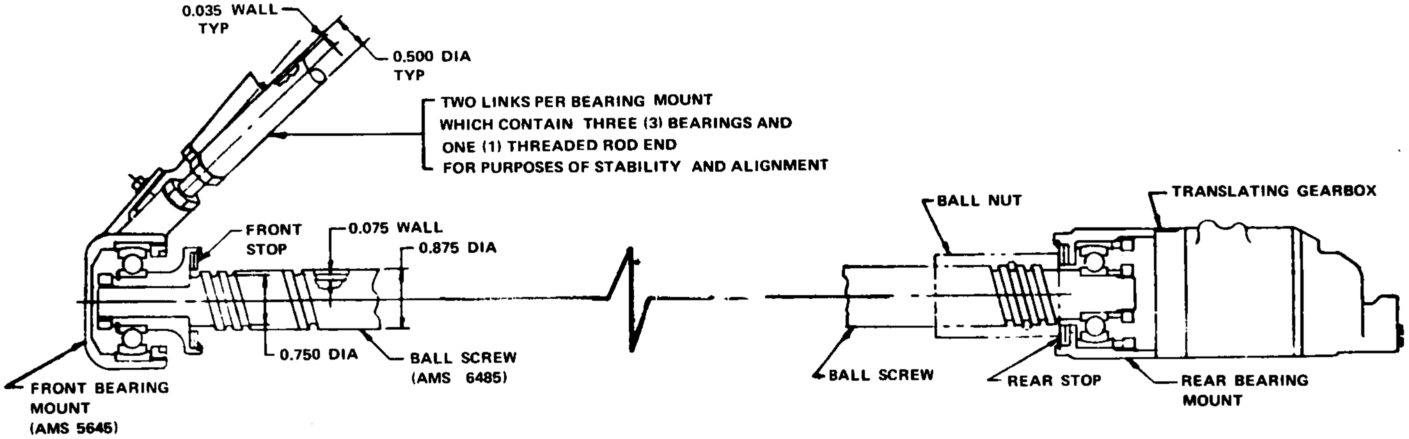

0.035 WALL
TYP
0.500 DIA
TYP
TWO LINKS PER BEARING MOUNT
WHICH CONTAIN THREE (3) BEARINGS AND
ONE (1) THREADED ROD END
FOR PURPOSES OF STABILITY AND ALIGNMENT
FRONT
STOP
0.075 WALL
0.875 DIA
0.750 DIA
BALL SCREW
(AMS 6485)
FRONT BEARING
MOUNT
(AMS 5645)
BALL NUT
TRANSLATING GEARBOX
BALL SCREW
REAR STOP
REAR BEARING
MOUNT

Figure C-87. Jackscrew Mounting Configuration

FD 76008

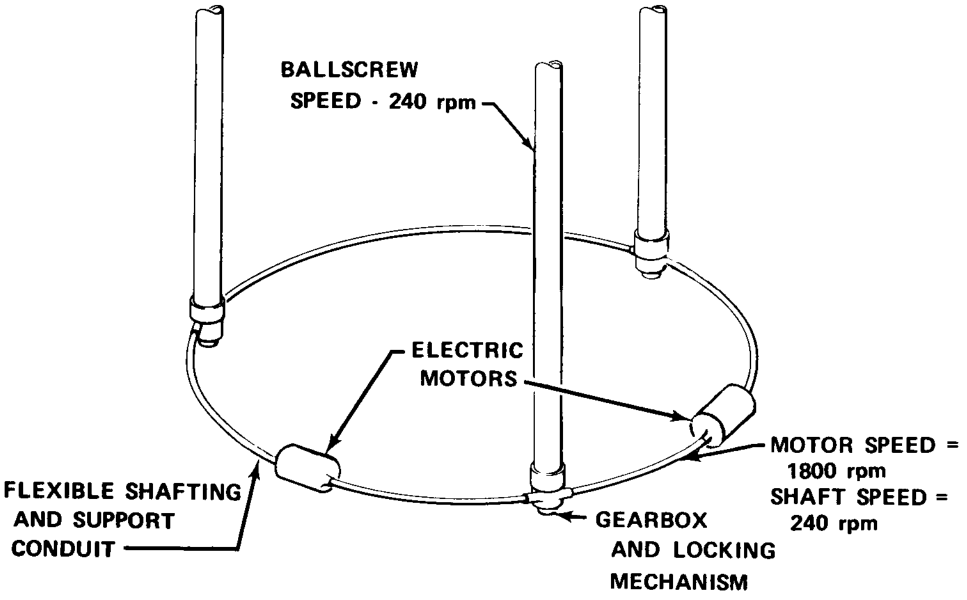

BALLSCREW SPEED - 240 rpm
ELECTRIC MOTORS
MOTOR SPEED = 1800 rpm
SHAFT SPEED = 240 rpm
FLEXIBLE SHAFTING AND SUPPORT CONDUIT
GEARBOX AND LOCKING MECHANISM

Figure C-88. Drive/Synchronization System FD 46296A

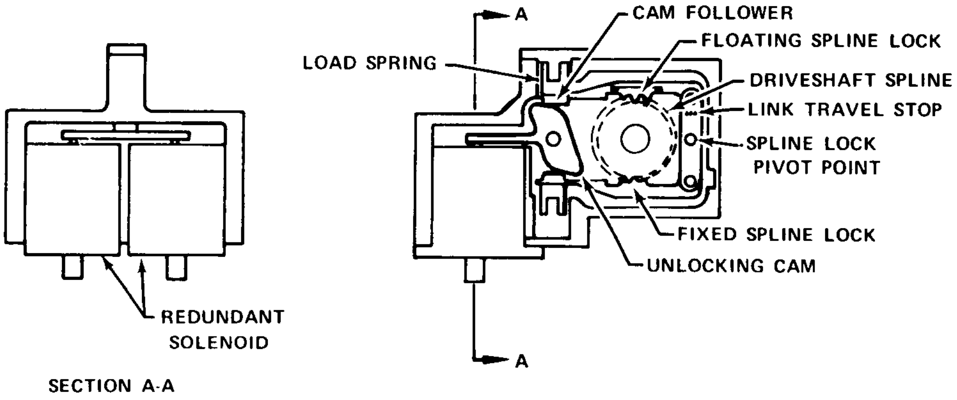

A
CAM FOLLOWER
FLOATING SPLINE LOCK
LOAD SPRING
DRIVESHAFT SPLINE
LINK TRAVEL STOP
SPLINE LOCK PIVOT POINT
FIXED SPLINE LOCK
UNLOCKING CAM
A
REDUNDANT SOLENOID
SECTION A-A

Figure C-89. Solenoid Actuated Brake for Ballscrew Actuators FD 75864

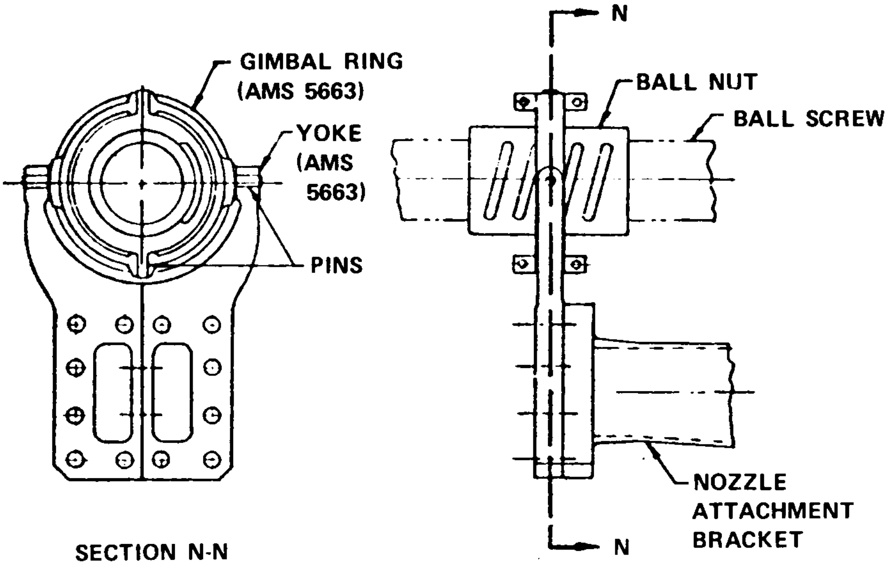

N
GIMBAL RING (AMS 5663)
YOKE (AMS 5663)
PINS
BALL NUT
BALL SCREW
NOZZLE ATTACHMENT BRACKET
SECTION N-N
N

Figure C-90. Gimbaled Ballnut FD 76901

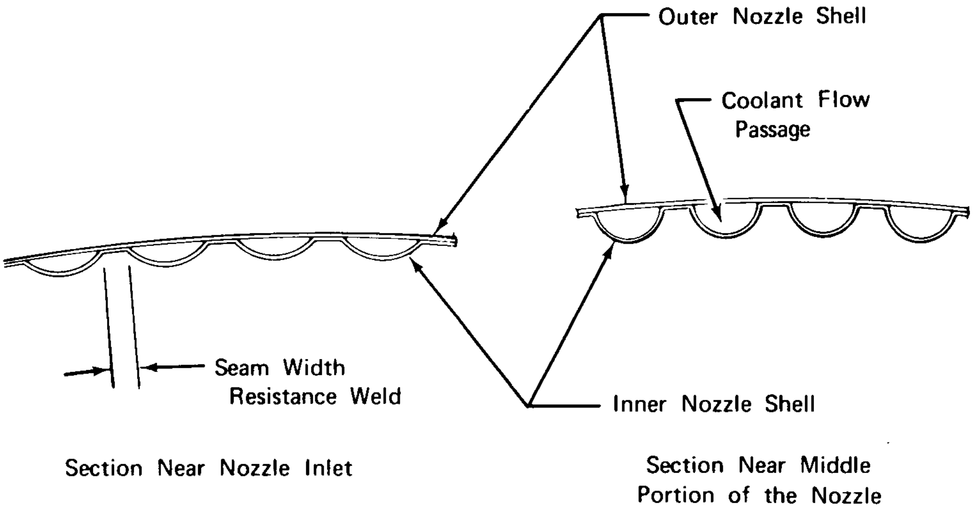
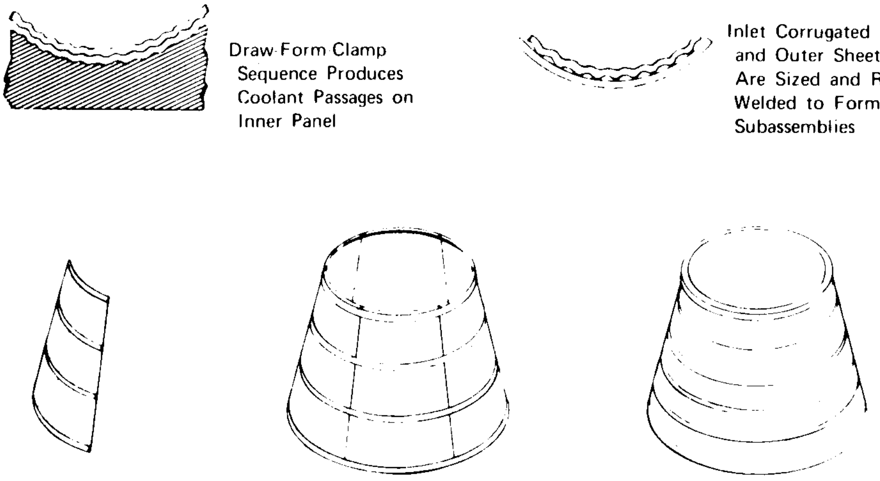

Outer Nozzle Shell
Coolant Flow Passage
Seam Width Resistance Weld
Inner Nozzle Shell
Section Near Nozzle Inlet
Section Near Middle Portion of the Nozzle

Figure C-91. Representative Corrugation Sections for Dump Cooled Nozzle FD 51227A



Draw Form Clamp Sequence Produces Coolant Passages on Inner Panel

Inlet Corrugated Panels and Outer Sheet Panels Are Sized and Resistance Welded to Form Subassemblies

Subassemblies Are Butt Welded Forming Sets of Panels

Panels are Butt Welded to Form the Basic Nozzle. Reinforcing Bands Are Induction Brazed in Place

Coolant Manifold Is Induction Brazed Completing the Dump Nozzle

Figure C-92. Dump Cooled Nozzle Fabrication Sequence FD 51226A

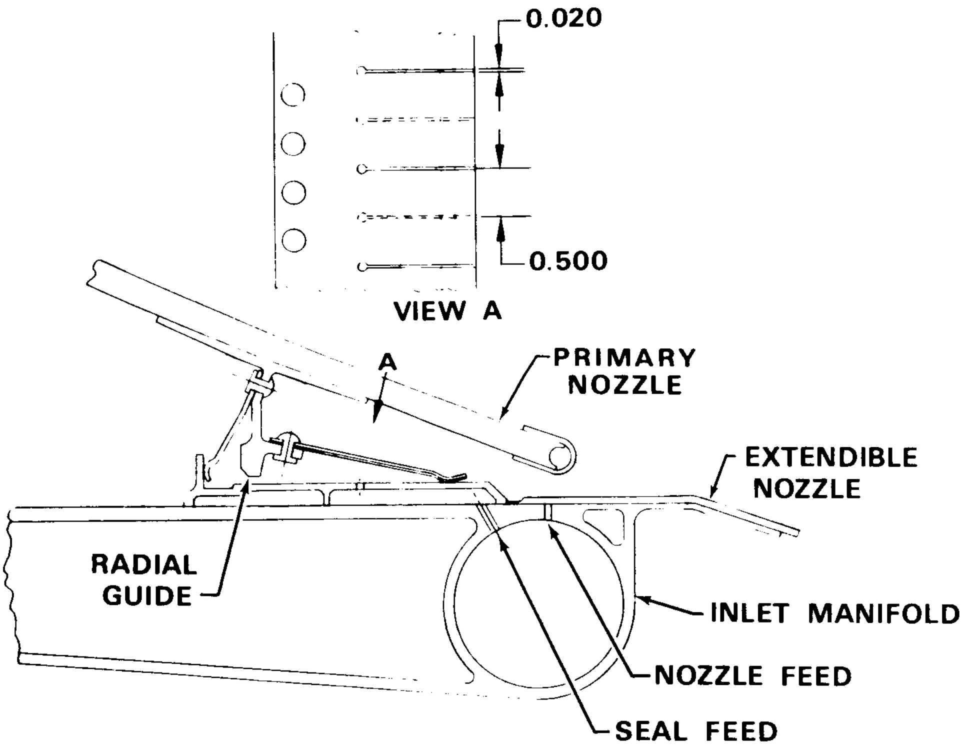

0.020
0.500
VIEW A
A
PRIMARY NOZZLE
EXTENDIBLE NOZZLE
RADIAL GUIDE
INLET MANIFOLD
NOZZLE FEED
SEAL FEED

Figure C-93. Double Finger Leaf Nozzle Seal Arrangement FD 72061

The extendible nozzle coolant supply originates at the turbomachinery gearbox and is supplied to the inlet manifold of the extendible nozzle by a quick-disconnect feed valve depicted in figure C-94. The coolant flow is supplied only after the nozzle is extended.

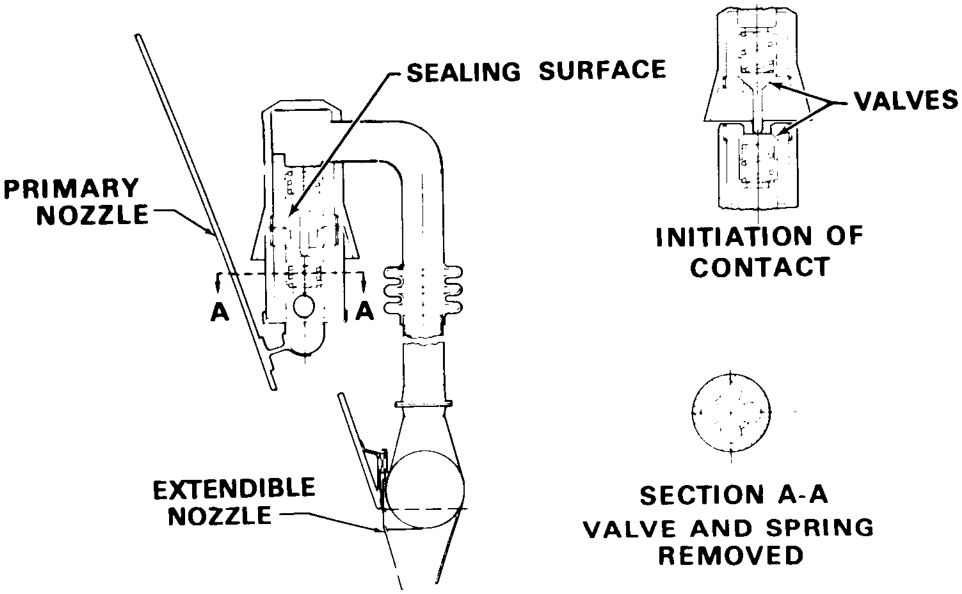


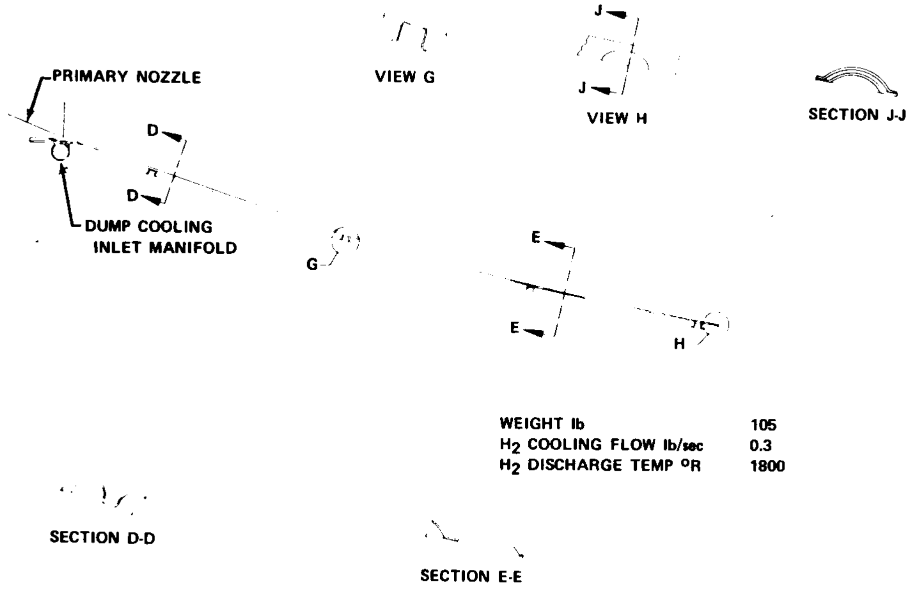
Figure C- 94. Extendible Nozzle Coolant Supply FD 72060A

The quick-disconnect valve is made up of two parts; one portion is attached to the primary nozzle and the other is attached to the extendible nozzle. Each portion contains a valve which is spring-loaded closed to prevent contamination of the fuel system plumbing when the engine is in the stored position and the nozzle is retracted. The normally closed valves are opened when the nozzle is translated to the extended position and the nose of one valve displaces the other. A spherical seat seal prevents valve leakage during operation.

Reliable engagement of the two parts is ensured by a flared skirt with an internal ramp guide located on the valve portion attached to the extendible nozzle. A bellows section, attached to the extendible nozzle, radially deflects to aid alignment and to provide an axial loading force between the spherical seats. The slow rate of nozzle extension provides adequate time to allow the guides and bellows to compensate for misalignment and dynamic mismatches between the mating parts.

The interface between the primary nozzle and extendible nozzle is sealed by the use of finger leaf seals. Each seal consists of a two-ply sheet metal ring with radial keyhole slots in each ply. The slots in each ply are positioned to seal the slots in the mating ply as previously shown in figure C-93. Both plies are welded to a supporting ring for attachment to the primary nozzle. The assembly radially or axially seals against a land on the extendible nozzle. The seal is radially cut in one place to simplify assembly and replacement, and the two plies overlap at this location to prevent excessive leakage. The seal assembly is riveted to the primary nozzle so that it can be readily replaced during overhaul.

These seals are supplied coolant flow from a choked orifice on the extendible nozzle inlet manifold. It is sized to supply 0.002 lb/sec of hydrogen should a seal finger be bent or broken. With the seals in their nominal position, leakage between the seal fingers is estimated to be 0.0002 lb/sec.



WEIGHT lb	105
H_2 COOLING FLOW lb/sec	0.3
H_2 DISCHARGE TEMP °R	1800

Figure C- 95. Dump Cooled Extendible Nozzle FD 71305

4.1.2.2 Design Studies

The corrugated dump-cooled design shown in figure C-95 has been used for the large surface area section of the nozzle because it has the following advantages over the tubular type construction: (1) lighter weight; (2) resistance welded flat-sheet construction is less costly than brazed tubular construction; (3) coolant passage cross section is readily varied to achieve desired coolant velocity profiles; and (4) hoop stresses are carried by the cooled outer wall. Corrugated sheet construction was demonstrated in firing tests of a modified RL10A-3-3 thrust chamber conducted in August 1967.

As shown in figure C-95, four inverted hat-section stiffening rings are incorporated on the dump-cooled section of the nozzle. These rings have been sized and positioned to provide support for loads from gimbaling, maneuvering, vibration and nozzle pressure differential in a vacuum environment.

The ballnut and ballscrew assembly supports the extendible nozzle and is designed to accommodate all the applied loads. The maximum axial, radial, and tangential loads are induced in this structure when a 4 g sideload is applied to the nozzle in the retracted position during Space Shuttle vehicle reentry. The upper support rods, the jackshafts, the nozzle attachment brackets and the nozzle skin are all sized for compressive buckling under this loading. The actual operating and translating loads are generally symmetrical and are much more easily resisted than the design load, resulting in a structure that is inherently rigid.

Expected nozzle tilt due to the combined factors of drive cable windup, ballnut backlash, gear backlash, and bearing clearances during translating or engine operation is less than one-tenth of one degree. Any two actuators are kept within 0.014-in. axial displacement because of the low windup 0.023°/in.-lb/in. of the synchronizing drive cable. The maximum predicted thrust alignment error is 0°-2° between the main and translating thrust nozzles.

Nozzle eccentricity and distortion will be held to a minimum by a radial guide and bumper strip arrangement shown in figure C-93 to resist sideloads. This will also act to always keep relative nozzle positions within the limits of the seal design. All axial loads are transmitted into the primary nozzle, while sideloads are resisted by both the primary nozzle and the forward rod assembly.

The nozzle outer wall thickness was set at 0.010 in. minimum to facilitate manufacturing handling of large thin sheets. The 0.010 in. wall thickness starts at the front of the nozzle and tapers linearly to 0.013 in. at the exit. This increase in wall thickness is required to account for the loss in strength of the Inco 625 material as the temperature of the coolant increases to a maximum of 1900°R at the exit. The inner corrugated wall previously shown in figure C-91 was maintained constant at 0.008 in. due to the manufacturing handling problems with the large sheets. The 360 corrugated passages were varied in cross section along the dump nozzle to minimize the cooling flow required while maintaining metal temperatures at the coolant discharge (nozzle exit) below 1900°R. Predicted passage temperatures along the nozzle are given in figure C-96. Indentations near each passage exit form restrictions to choke the flow in each passage for uniform flow distribution. The nozzles formed by these indentations increase the impulse from the heated, dump coolant.

4.1.2.3 Life

The extendible nozzle is not predicted to limit the life of the engine. The rotating members move so slowly that high wear rates are not anticipated. The ballscrew system is sized for nondynamic loads such as booster takeoff and landing, thus are oversized for the operational loads encountered while translating the nozzle.

The double finger seals at the end of the primary nozzle which stop hot gas leakage are the only nonlubricated wear component but these are sized to handle 300 cycles without failure. Should a finger seal bend such that it is not an efficient seal, hydrogen coolant from the extendible nozzle coolant inlet manifold will be directed through the leak preventing potential secondary damage due to hot gas leakage.

The dump-cooled nozzle does not limit life because the practical wall thickness of the materials used was selected heavy enough that the material yield is not realized. Only after temperatures near the end of the nozzle approached 1900°R did the stresses approach the allowable for Inco 625 material, and then wall thicknesses were increased to keep them below the allowable.

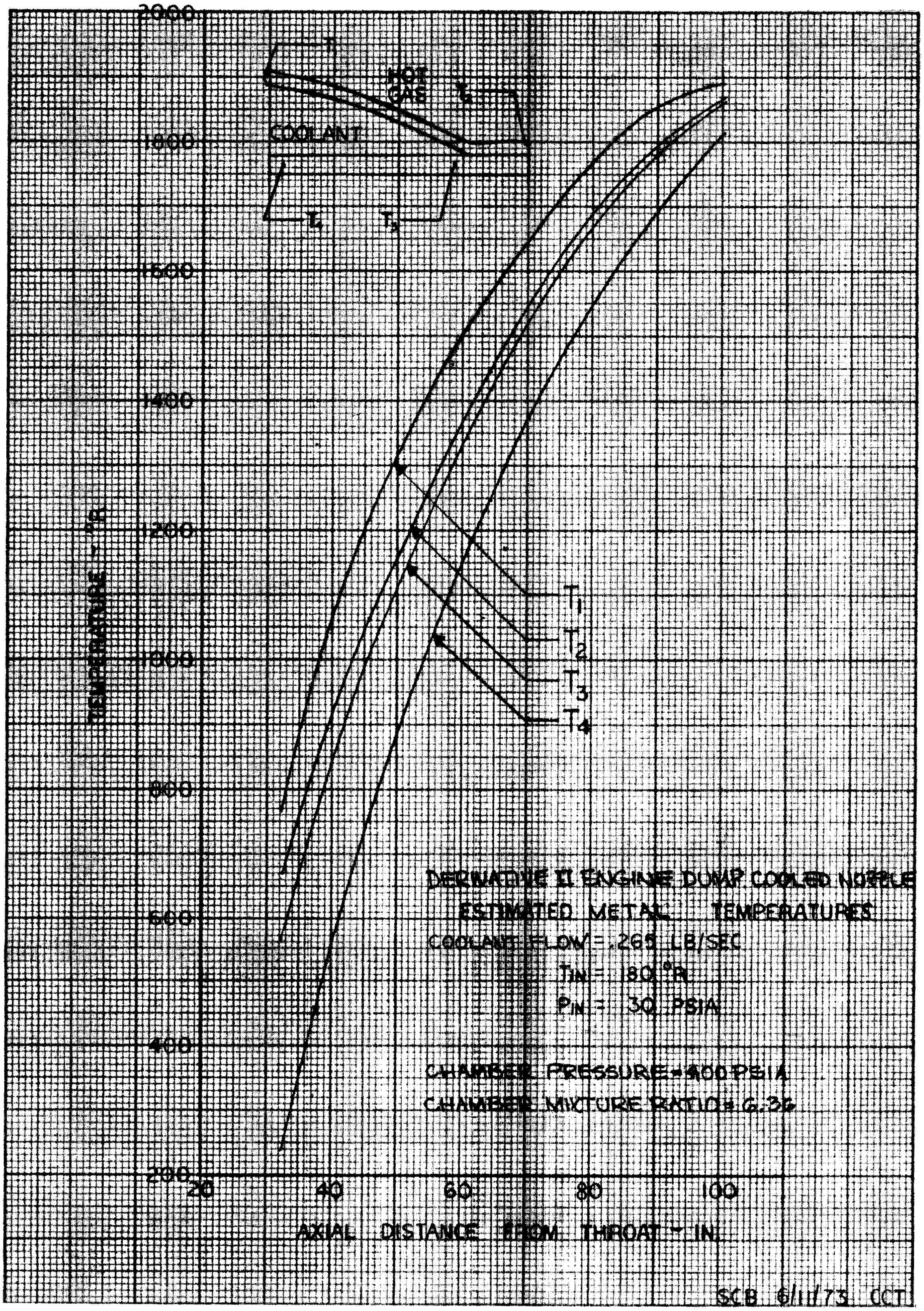


Figure C-96. Derivative II Engine Dump Cooled Nozzle Estimated Metal Temperatures

DF 98278

4.1.2.4 Materials

Materials for use on the Derivative II extendible nozzle were selected for their compatibility with hot hydrogen and their ease of manufacture. The large thin panels of the nozzle require special attention in regard to ease of forming, welding and compatibility with hydrogen.

Component	Material	Type
Thrust Nozzle	Inconel 625	AMS 5666 AMS 5599
Ballscrew Actuator	Steel	AMS 6485
Support Structure	Aluminum	TBD

4.1.2.5 Weight

The weight of this system is approximately 147 lb with the nozzle being 104 lb of the total and the remainder in ballscrew actuator systems and support hardware. The weight was calculated using layout drawings of the various components.

4.1.3 Turbopump

The function of the turbomachinery is to supply the oxygen and hydrogen propellants to the engine combustion chamber at the required pressure level and flowrates. The Derivative IIA engine turbopump must provide this function with two-phase oxygen and hydrogen at the respective oxidizer low speed inducer and fuel pump inlets. The turbomachinery design is the same as that of the production RL10A-3-3 engine except for the addition of an oxidizer low speed inducer and an enlargement of the hydrogen pump first-stage inducer and impeller. These changes are required to provide two-phase pumping capability. The Derivative IIA engine turbopump was designed to meet the requirements shown in table C-20 for operation at full thrust and a mixture ratio of 6.0.

4.1.3.1 Mechanical Description

The turbopump assembly shown in figure C-97, consists of: (1) a liquid hydrogen pump powered by a hydrogen-driven turbine mounted on a common main shaft; (2) a liquid oxygen pump mounted beside the turbine and driven through a gear train by the hydrogen pump turbine shaft; and (3) an oxygen low speed inducer mounted inline with the oxygen pump driven by a gear train on the oxygen pump shaft. All rotating assemblies in the turbopump assembly are mounted on unlubricated, hydrogen or oxygen-cooled ball and roller bearings.

a. Pumping Elements

The fuel pump is of a two-stage centrifugal configuration, with the two back-shrouded impellers mounted back-to-back to minimize thrust unbalance. Recovery of velocity head is accomplished through a straight conical diffuser connected to a volute collector. A three-bladed axial flow inducer on the same shaft is located upstream of the first-stage impeller. The inducer baldes have swept back leading edges. The large inlet diameter and inlet tip blade angle of 7 deg provides two-phase pumping with up to 40% vapor content.

Table C-20. Turbopump Design Requirements, (Derivative IIA) at MR = 6.0

Item	
Oxidizer Low Speed Inducer	
Inlet Total Pressure, psia	16.00
Inlet Temperature, °R	163.8
Inlet Density, lb/ft^3	70.9
Flowrate, lb/sec	28.0
Head Rise, psi	17.3
Speed, rpm	3022
Efficiency, percent	64.8
Horsepower	2.7
Discharge Pressure, psia	33.3
Oxidizer Pump	
Inlet Temperature, °R	164
Inlet Density, lb/ft^3	70.9
Head Rise, psi	498
Speed, rpm	11,428
Efficiency, %	63.4
Horsepower	81.2
Discharge Pressure, psia	531
Fuel Pump	
Inlet Total Pressure, psia	16.0
Inlet Temperature, °R	36.9
Inlet Density, lb/ft^3	4.40
Discharge Density, lb/ft^3	4.16
Flowrate, lb/sec	4.67
Head Rise, psia (1st Stage/2nd Stage)	389/416
Speed, rpm	28,571
Efficiency, percent (1st Stage/2nd Stage)	51.7/49.0
Horsepower (1st Stage/2nd Stage)	208/252
Discharge Pressure, psia (1st Stage/2nd Stage)	405/821
Turbine	
Inlet Total Pressure, psia	653
Inlet Total Temperature, °R	474
Discharge Static Pressure, psia	487
Efficiency, %	71.6
Horsepower	541
Turbine Flow, lb/sec	3.96

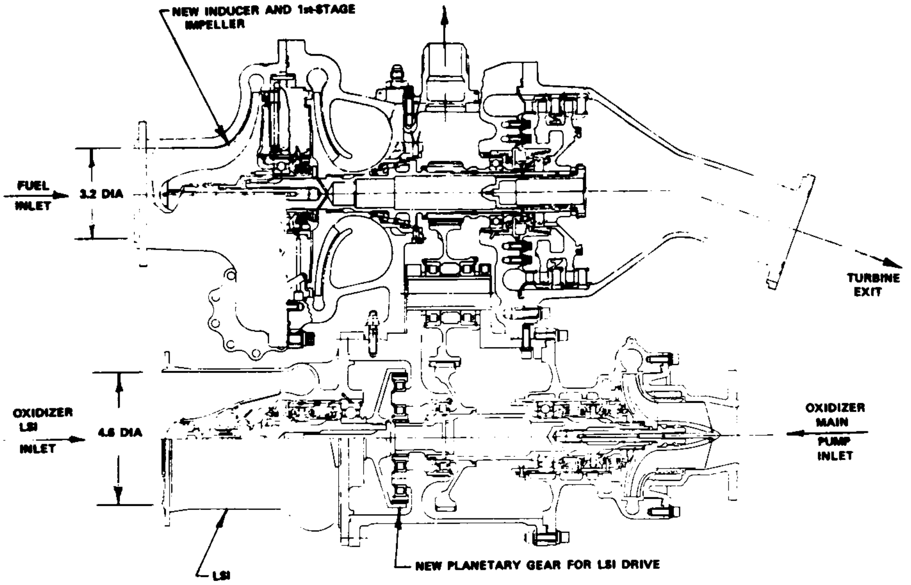


Figure C- 97. Derivative IIA Turbomachinery FD 76009

The first- and second-stage impellers incorporate 22-1/2 deg and 90 deg blade exit angles, respectively. This arrangement provides the optimum match between stall margin, which is improved with increased sweep angle, and required head rise, which decreases with decreased angle. The first- and second-stage impellers run with 0.043-in. and 0.086-in. nominal clearance respectively, between blade and housing contours. They are machined from AMS 4135 aluminum alloy, which has a 0.2% yield strength of 54,000 psi at room temperature.

The fuel pump is designed with an interstage bleed as shown in figure C-98 that may be set to the area required for obtaining the desired net axial load on the thrust bearing. The bleed permits fuel flow from the second-stage pump discharge to flow through an orifice with the flow area controlled by a classified metering plug into the backface cavity of the first-stage fuel pump impeller. Increasing this flow increases the thrust load toward the first-stage impeller by decreasing the pressure on the backface of the second-stage impeller and increasing the pressure on the backface of the first-stage impeller.

The oxidizer pump is a single-stage centrifugal pump which supplies oxygen to the engine combustion chamber. The fully shrouded impeller design permits adequate clearance between impeller and housing contours to eliminate the possibility of impeller rub. Velocity head recovery is accomplished, as in the fuel pump, through a conical diffuser and volute collector. A three-bladed, axial-flow, partially shrouded inducer on the oxidizer pump shaft is located upstream of the impeller. The inducer shroud incorporates a labyrinth seal to minimize flow recirculation.

The oxidizer low speed inducer is a three-bladed, axial-flow unshrouded pump. The blades are of a "J" blade configuration with a 9-deg blade tip inlet angle. The inducer is contained in an aluminum housing which is bolted directly to the gearbox. Velocity head recovery is accomplished in the collector volute.

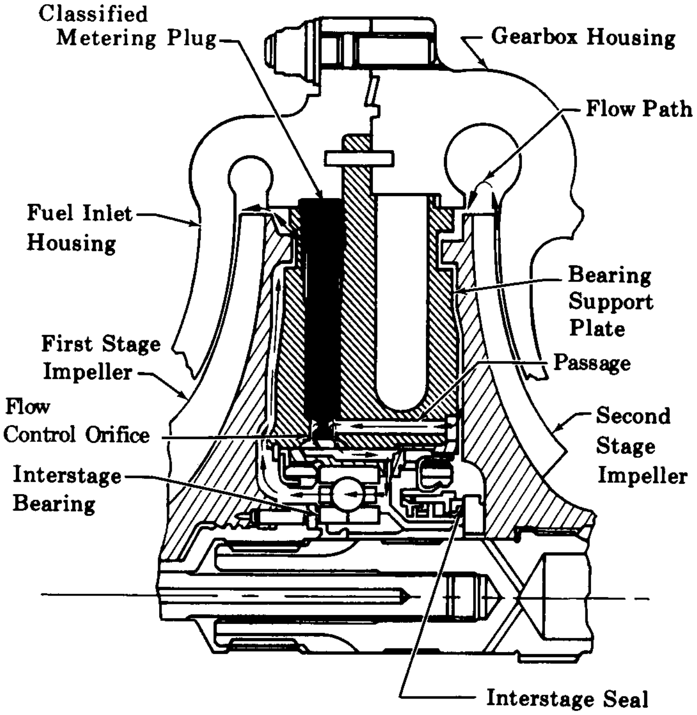

Classified Metering Plug
Gearbox Housing
Flow Path
Fuel Inlet Housing
Bearing Support Plate
First Stage Impeller
Passage
Flow Control Orifice
Second Stage Impeller
Interstage Bearing
Interstage Seal

Figure C-98. RL10A-3-3 Fuel Pump Controllable Interstage Leakage FD 18427A

b. Turbine

The turbine provides power to drive the fuel and oxidizer pumps by utilizing the energy in the chamber/nozzle hydrogen coolant. The turbine is of pressure compounded, full-admission, two-stage design with exit guide vanes to minimize discharge swirl losses.

Both blade stages are fully shrouded, and labyrinth seals are incorporated to minimize interstage and tip leakage. The turbine rotor with shroud is shown in figure C-99. The conical web between the back disk and bore is designed to absorb disk growth, minimize hub distortion, and prevent unbalance.

c. Gearbox

The gearbox contains all of the turbomachinery gearing as previously shown in figure C-97. It also provides for mounting of all pumps and the drive turbine. The two gear trains in the gearbox are cooled by spent bearing coolant discharged into the gearbox as a cold gas. The main pump gear train consists of a shaft mounted pinion on the hydrogen pump shaft, a double bearing mounted idler gear and a main oxidizer pump shaft mounted gear. This train reduces turbine shaft speed 2.5 to 1 to drive the oxidizer pump.

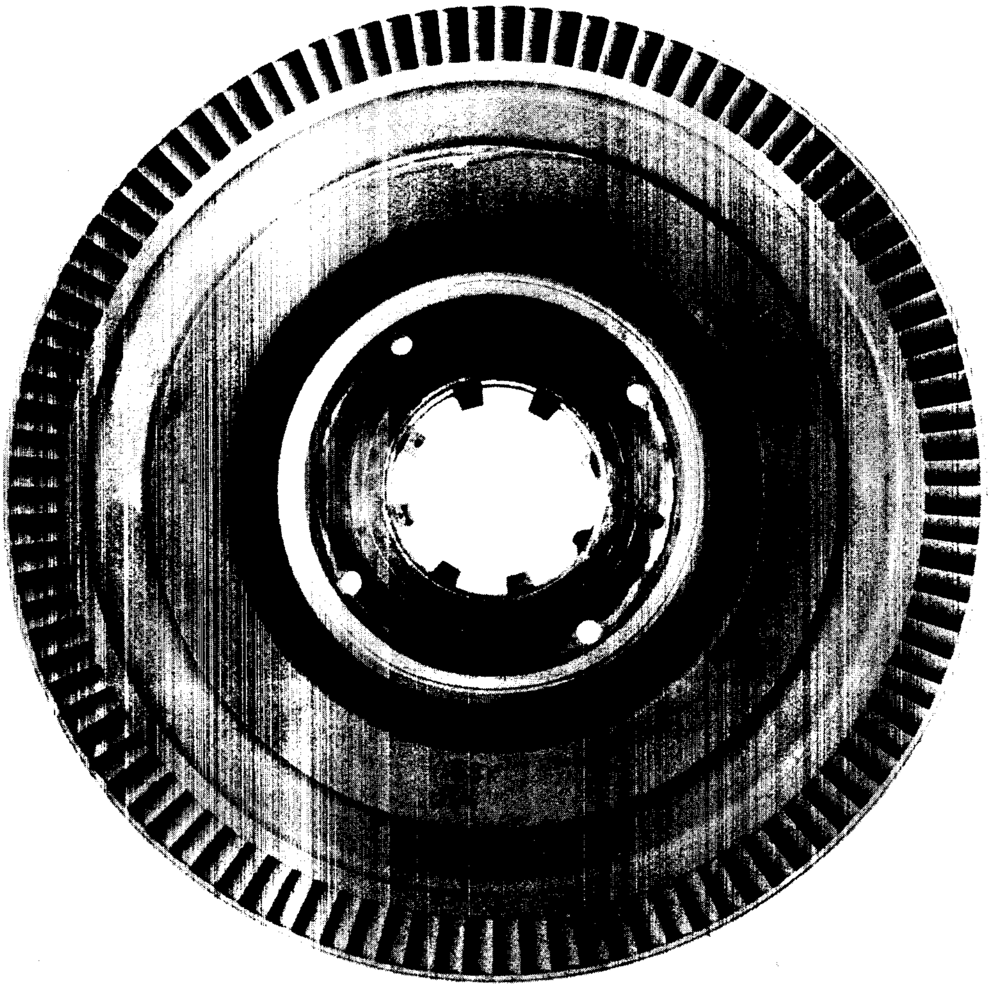


Figure C-99. Turbine Rotor With Shroud FE 46939

The second gear train reduces the oxidizer pump shaft speed by 3.85 to 1 to drive the oxidizer low speed inducer. The gear assembly consists of a geared coupling shaft pinion, three planetary idler gears and a low speed inducer mounted drive ring gear. The coupling shaft is piloted to the oxidizer main pump shaft and is axially and circumferentially retained by a pin. The gear at the end of this shaft engages planetary idler gears single bearing mounted to the gearbox housing. These planetary idler gears drive a ring gear directly coupled to the oxidizer low speed inducer.

Lubrication for all gears is provided by a moly-disulfide dry film lubricant in a varnish binder.

d. Bearings

The main hydrogen drive shaft incorporates passages for hydrogen coolant flow to the turbine bearing. This coolant is bled from the second stage of the fuel pump. Liquid hydrogen coolant is supplied to the oxidizer pump thrust bearing through a drilled passage in the fuel pump housing. The coolant flow through this passage is supplied from the first-stage pump contour. All other hydrogen cooled bearings in the turbopump are cooled by conduction and low-pressure hydrogen flow through the gearbox cavity.

The oxidizer low speed inducer front roller bearing is cooled by the oxidizer inducer labyrinth seal leakage directed through the bearing and out the shaft nose piece where it is mixed with the incoming flow. The rear bearing absorbs the hydraulic load of the pumping elements and is cooled by hydrogen supplied from the first-stage pump contour and directed through the ball bearing where it is vented to the gearbox.

The ball bearing at the turbine is preloaded by a spring washer that assures proper thrust loading of the shaft bearings. The fuel pump and turbine combined thrust load is transferred to the main pump housing by the ball thrust bearing located between the fuel pump stages. The loads on the oxidizer pump shaft are supported by a ball thrust bearing located in the oxidizer pump housing and a cylindrical roller bearing. The idler gear radial load is carried by a pair of identical cylindrical roller bearings mounted on a nonrotating shaft. All bearings and races are made of consumable-electrode vacuum-melted, AMS 5630 corrosion-resistant steel and are designed to operate unlubricated at 38°R to 158°R. The ball bearings incorporate split inner races and inner race riding cages of aluminum armored plastic, with a bearing spin/roll ratio of 19%.

e. Seals

All carbon face seals on the fuel pump shaft are of similar construction. The carbon seal is held against the rotating seal face by a spring-loaded retainer. A metal ring seal in the retainer limits leakage past the seal housing.

The fuel pump interstage seal shown in figure C-100 is designed to limit leakage between pump stages, while a two-step, carbon-face seal, figure C-101, limits leakage of hydrogen into the gearbox chamber. The turbine seal is designed to limit leakage of hydrogen from the turbine area into the gearbox chamber.

All interstage leakage within the turbine itself is controlled by labyrinth seals between stages as illustrated in figure C-102.

The oxidizer pump and LSI shaft seal assemblies are identical and are located between the pumps and the gearbox as depicted in figure C-103. The seal assembly consists of two bellows-type, carbon-face, primary seals that minimize the leakage of hydrogen and oxygen. Overboard vents are provided for leakage past these seals. The bellows is splined to a retainer that absorbs torque and provides functional damping but permits axial movement. Two carbon ring seals, which are loaded by spring washers, are used as backup seals to prevent mixing of propellants in case of a primary seal failure. The backup seals are vented to a separate overboard port.

The oxidizer low speed inducer forward seal is a three lip labyrinth seal sized to minimize net shaft thrust unbalance and control bearing coolant flowrate.

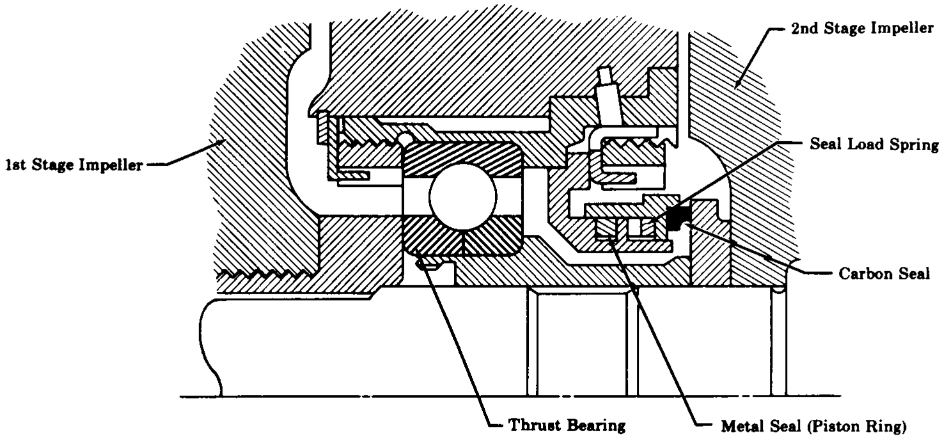

2nd Stage Impeller
Seal Load Spring
1st Stage Impeller
Carbon Seal
Thrust Bearing
Metal Seal (Piston Ring)

Figure C-100. Fuel Pump Interstage Seal FD 3150A

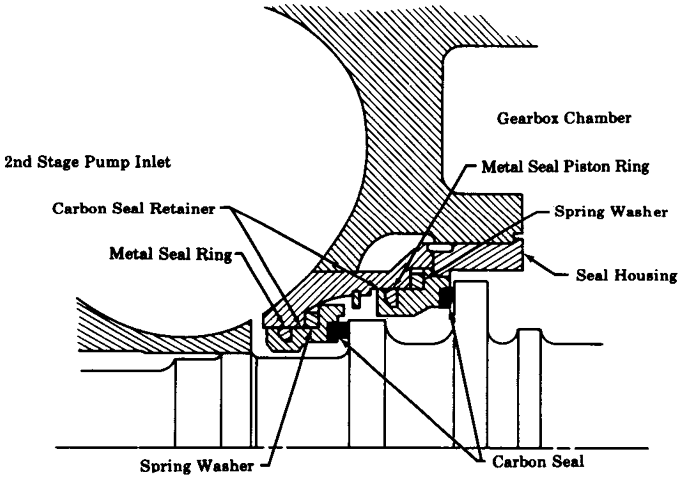

Gearbox Chamber
2nd Stage Pump Inlet
Metal Seal Piston Ring
Carbon Seal Retainer
Spring Washer
Metal Seal Ring
Seal Housing
Spring Washer
Carbon Seal

Figure C-101. Fuel Pump Face Seal FD 3148A

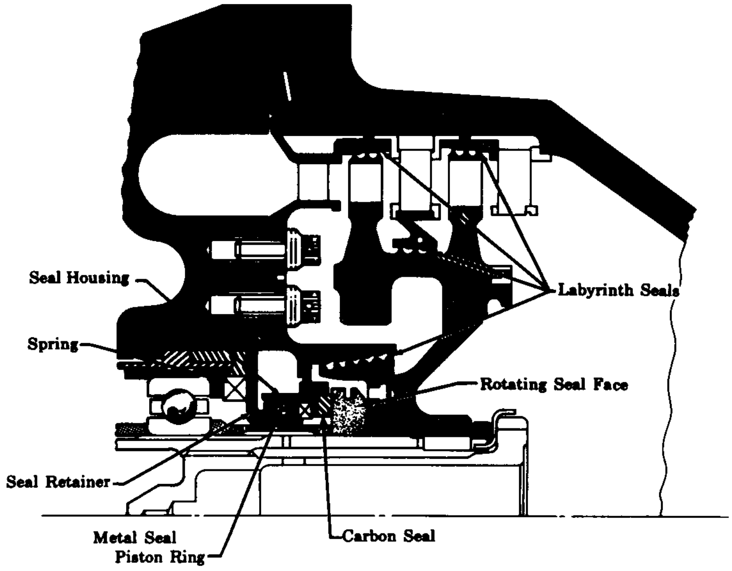

Seal Housing
Labyrinth Seals
Spring
Rotating Seal Face
Seal Retainer
Metal Seal Piston Ring
Carbon Seal

Figure C-102. Turbine Rotor Seal FD 10742B

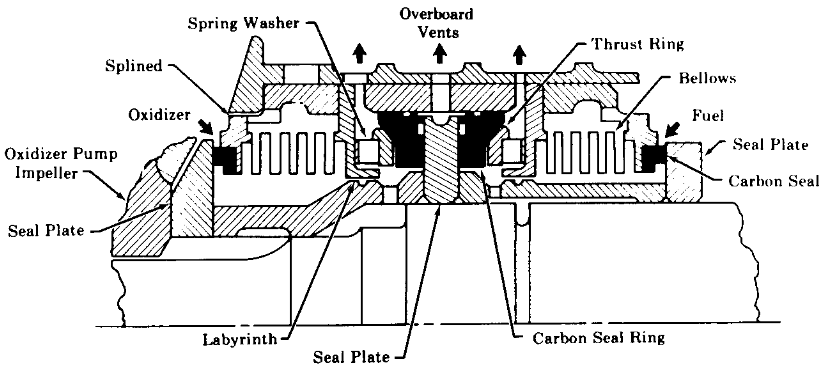


Figure C-103. Oxidizer Pump Seal FD 3151C

4.1.3.2 Design Study

A design study of the Derivative IIA turbopump showed that all important requirements could be met by modifying the RL10A-3-3 turbopump to provide two-phase pumping capability. Modification was limited to the addition of an oxidizer low speed inducer and the substitution of a new first-stage fuel pump inducer, impeller and housing. Hydrodynamic analysis was completed to define the blade forms necessary to provide two-phase pumping capability with sufficient head rise to match the suction requirements of the main pumps. The results of these studies and their impact on the mechanical design is detailed in the following paragraphs.

a. Turbopump Hydraulic Design

The fuel inducer geometry given in table C-21 which provides two-phase pumping capability for the Derivative IIA engine was based upon the design analyses completed during the Critical Elements Evaluation described in Section B4. This study showed that optimum suction performance is provided by a fuel high speed inducer mounted with the first-stage impeller having an inlet diameter of approximately 3.2 in., and an inlet tip blade angle of 7 deg. Since this configuration coincides with the RL10A-3-3 "Mod 2" inducer design which has been successfully tested under two-phase operation at rated performance level, the hydraulic design configuration is identical to that of the "Mod 2".

The design analysis of the oxidizer system conducted during the Critical Elements Evaluation was used to define the low speed inducer inlet diameter, blade angle and rotational speed. As discussed in Section B4, an inducer was selected with an inlet diameter of 4.6 in., an inlet blade tip angle of 9 deg, and an operating speed of 3000 rpm. Additional hydraulic analysis was completed to define inducer blading which would provide the head rise to satisfy the main oxidizer pump suction requirements and inlet line pressure loss. The Derivative IIA engine main oxidizer pump suction requirement dictated the use of a design point head coefficient of 0.3. This head coefficient is achieved with a cambered blade having a mean discharge angle of approximately 20 deg. While an inducer head falloff of approximately 20% is predicted during two-phase operation, the delivered NPSH is more than twice that required by the main oxidizer pump. A J-blade angle distribution, with helical front section and cambered rear section provides suction performance. A swept back leading edge is also used to improve suction performance.

Table C-21. Inducer Design Parameters Derivative IIA

	Fuel Inducer	Oxidizer Inducer
Inlet Tip Diameter	3.2	4.6
Inlet Hub Diameter	0.96	1.38
Inlet Tip Blade Angle	7°	9°
Discharge Tip Diameter	3.2	4.6
Overall Head Coefficient	0.17	0.3
Inlet Tip Flow Coefficient	0.0629	0.0978
Vapor Fraction - Capability	0.4	0.4
Inlet Mach No.	0.38	0.55
Incidence/Blade Angle	0.5	0.56
Cavitation Number	0.024	0.07

b. Mechanical Design Considerations

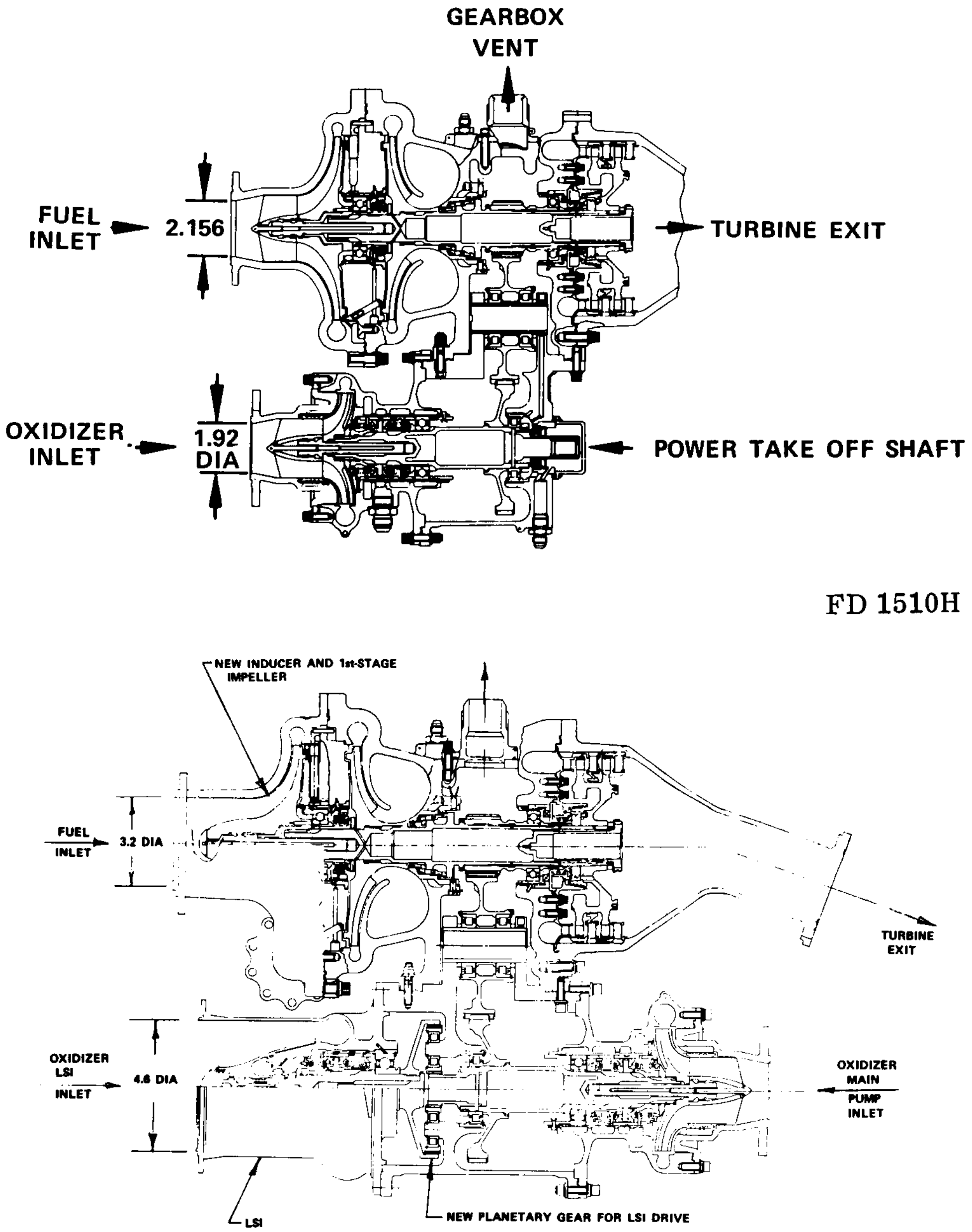
To maximize engine interchangeability a turbopump arrangement was selected which allowed the pump inlet flange location to remain the same as the production RL10A-3-3 engine by mounting the oxidizer low speed inducer on the gearbox housing at the current RL10A-3-3 power takeoff location and rotating the entire oxidizer pump gearbox housing 180°. This locates the oxidizer low speed inducer inlet flange at the same axial and radial position as the current RL10A-3-3 main oxidizer pump. Since the Mod 2 fuel pump configuration is interchangeable with the B/M fuel pump, no further design effort was required. Sketches of the RL10A-3-3 and Derivative IIA turbomachinery are provided in figure C-104 for ease of comparison.

The Derivative IIA main oxidizer pump rotational direction is opposite that of the RL10A-3-3 engine. While this change will require the modification or replacement of some manufacturing tooling, the basic design remains unchanged. Retooling will be necessary to produce nonsymmetrical parts, such as the swept impeller blades and single volute discharge housing.

The pump shaft requires an internal spline on the gear end to couple with the planetary gear set. This spline requirement can be cut in the shaft as currently designed. All other components of the main oxidizer pump are suitable for use with the pump in the inverted position.

The oxidizer low speed inducer and housing can be adapted to the existing gearbox with minimal change. The adapter flange allows direct removal of the LSI without removing other components. The seal package was selected to be the same as that used in the main oxidizer pump in an effort to minimize cost and development time.

While the fuel turbopump portion of the gearbox can be retained the same as the RL10A-3-3, the oxidizer portion must be changed to accommodate the LSI and main oxidizer pump mounts. A new oxidizer gearbox casting design provides new bearing supports for the planetary gear set, and the oxidizer pump shaft and the pump mount access holes are sized to allow removal of drive gearing of both oxidizer pumps. Gears used in the power gear train between the oxidizer pump shaft and the hydrogen pump shaft are the same as the RL10A-3-3, and require no modification. The new gears used to reduce the oxidizer pump shaft speed 3.85 to 1 are designed for 10 hr life using the design criteria shown in table C-22. A gear face contact Hertz stress of 60,000 psi is used in the design to minimize gear face width, volume and weight. All gears of this design will be lubricated with molybdenum disulfide and graphite powder in a varnish binder (PWA 61), a dry film sprayed and baked onto the gear teeth.


GEARBOX VENT
FUEL INLET
2.156
TURBINE EXIT
OXIDIZER INLET
1.92 DIA
POWER TAKE OFF SHAFT
NEW INDUCER AND 1st-STAGE IMPELLER
FUEL INLET
3.2 DIA
TURBINE EXIT
OXIDIZER LSI INLET
4.6 DIA
OXIDIZER MAIN PUMP INLET
LSI
NEW PLANETARY GEAR FOR LSI DRIVE

FD 1510H

Figure C-104. Comparison of RL10A-3-3 and Derivative IIA Turbomachinery

FD 76009

Table C-22. Oxidizer Low Speed Inducer Cryogenically Cooled Gear Design Criteria

	Design Parameter	Criteria	Design
1.	Pitch line velocity	20,000 max	3615
2.	Pressure angle	22-1/2 deg	22-1/2 deg
3.	Hertz stress level	77,000 max	60,000
4.	Maximum reduction per mesh	5:1 max	3.84:1
5.	Gear configuration for 5:1 or less reduction	Spur	Spur
6.	Ratio of face width/pitch diameter	≤1:1	≤1:3
7.	Size coolant for gear heat generation of 1.5% of HP transmitted/mesh		
8.	Number of teeth in gear and pinion to have no common factors		

The hydrogen cooled rear ball bearing (25 x 62 mm) of the low speed inducer was sized for 10 hr life when subjected to the anticipated radial bearing loads. The oxygen cooled forward roller bearing (17 x 40 mm) was also sized by the anticipated radial loads. The DN values of these rear and forward bearings is 0.075 x 10^6 and 0.05 x 10^6 mm-rpm, respectively, and are low relative to the Design Criteria Requirements listed in Section C.3.3.

The planetary idler gear roller bearings sized to support gear sideloads, (12 x 32 mm) operate at a DN level of 0.1 x 10^6.

The hydrogen flow used to cool the gears and bearings is collected in the gearbox cavity and is supplied to the dump cooled extendible nozzle through the nozzle coolant valve. The cooling requirement of the extendible nozzle was found, during the Critical Elements Evaluation, to be greater than the flow presently provided by the RL10A-3-3 turbopump flow passages (0.07 lb/sec). For the Derivative II engine, these flow passages were resized to provide the desired coolant flow of 0.265 lb/sec. Of this flow, approximately 62% is provided by flow tapped off the fuel pump first stage and the remaining flow is provided by flow passing the fuel pump shaft. Figure C-105 illustrates the coolant flowpaths in the turbopump. In addition to providing the required dump nozzle coolant flow the increased flowrate could extend turbopump life.

4.1.3.3 Life

Life of the Derivative IIA turbopump is predicted to be limited by gear wear. Since the RL10A-3-3 engine components have demonstrated over 5 hr of gear life without gear failure it is expected that the Derivative IIA turbopump will have life of at least this magnitude. All new components have been designed for 10 hr life.

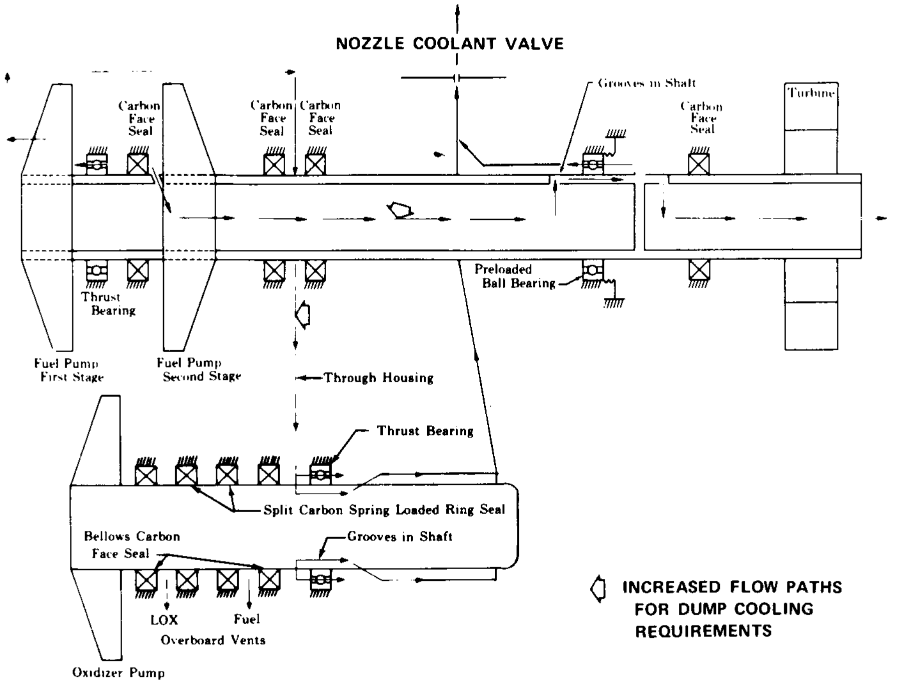

NOZZLE COOLANT VALVE
Carbon Face Seal
Carbon Face Seal
Carbon Face Seal
Grooves in Shaft
Carbon Face Seal
Turbine
Thrust Bearing
Preloaded Ball Bearing
Fuel Pump First Stage
Fuel Pump Second Stage
Through Housing
Thrust Bearing
Split Carbon Spring Loaded Ring Seal
Bellows Carbon Face Seal
Grooves in Shaft
LOX
Fuel
Overboard Vents
Oxidizer Pump
INCREASED FLOW PATHS FOR DUMP COOLING REQUIREMENTS

Figure C-105. Turbopump Coolant Schematic FD 6167F

4.1.3.4 Materials

The materials listed below are selected for use on the Derivative IIA engine turbopump based on their successful use in the RL10A-3-3 engine.

Component	Material	Type
Fuel Pump Gearbox Housing	Aluminum Alloy Casting	AMS 4215
Housings	Aluminum Alloy Forging	AMS 4130
Oxidizer Impellers	Stainless Steel Forging	AMS 5646
Fuel Impellers	Aluminum Alloy Forging	AMS 4135
Shaft	Nickel Alloy Bar	AMS 5667
Turbine Rotor	Aluminum Alloy Forging	AMS 4127
Gears	Carburizing Steel	AMS 6260
Bearings	Stainless Steel Bar and Forging	AMS 5630
Bearing Liners	Stainless Steel Forging	AMS 5646

4.1.3.5 Weight

The weight of the Derivative IIA engine turbopump is 99 lb, approximately 20 lb over that of the RL10A-3-3 engine turbopump. The weight change is primarily due to the addition of the oxidizer low speed inducer and its planetary gear drive with a small increase due to the larger diameter fuel pump inducer and minor gearbox revisions. Approximately 85% of the weight is based upon weighed hardware and the remainder is based upon calculated values using the component layout drawings.

4.1.4 GO_2 Heat Exchanger

The function of the oxidizer heat exchanger is to gasify oxygen before entry into the injector during tank head idle operation and to provide gaseous oxygen for tank pressurization during pumped idle and full thrust operation. The heat exchanger performance at each engine operating point was defined by engine cycle and vehicle tank system studies, whose important characteristics are given in table C-23.

Table C-23. GO_2 Heat Exchanger Requirements

	Full Thrust	Pumped Idle	Tank Idle
Hydrogen Flowrate, lb_m/sec	0.04	0.41	0.070
Hydrogen Inlet Temperature, °R	474.	622.	582.
Hydrogen Exit Temperature, °R	251.	523.	426.
Hydrogen Inlet Pressure, psia	657.	154.	10.3
Hydrogen Pressure Loss, psia	<1	1.09	0.780
Oxygen Flowrate, lb_m/sec	0.500	1.50	0.32
Oxygen Inlet Temperature, °R	167.	165.	164.
Oxygen Exit Temperature, °R	265.	245.	385.
Oxygen Inlet Pressure, psia	588.	124.	15.9
Oxygen Pressure Loss, psia	<1	2.02	0.741
Heat Flux, Btu/sec	36.1	146.	39.5

4.1.4.1 Mechanical Description

The heat exchanger is a compact, laminated assembly having alternating layers of hydrogen and oxygen flow passages as shown in figure C-106. Each passage is formed by two parallel plates which are closed on two sides and joined by a series of copper fins oriented in the direction of flow. Special provisions are made to ensure complete isolation of the working fluids.

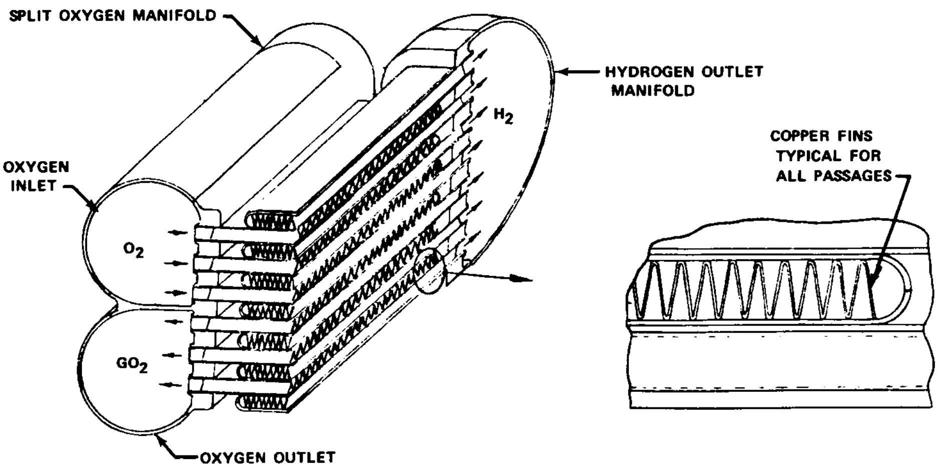


Figure C-106. Gaseous Oxidizer Heat Exchanger Design Derivative IIA Engine FD 76010

Warm hydrogen enters the heat exchanger through a flanged inlet where a semicircular distribution manifold directs the incoming hydrogen into seven parallel finned passageways. Hydrogen convects heat into the fins and facesheets as it flows through the passageways. The hydrogen flow is then collected in a manifold and discharged through a flanged exit.

The oxidizer flow makes two passes through the heat exchanger. The flow enters the oxidizer inlet distribution manifold through an end mounted flange. The flow is routed through three flow passages, collected in a turnaround manifold and rerouted through three additional passages before exiting from the heat exchanger. Heat is added to the oxygen as it flows through the fins and between the facesheets of each passageway.

Flow passages shown in figure C-107 for the hydrogen and oxygen are slightly different in length and width. Both are constructed with 0.010-in. thick AISI 347 stainless steel facesheets and 0.005-in. thick copper fins. All passageways are 0.100-in. high with 0.043 in. between fins. They are constructed by welding two facesheets into a leakproof assembly and brazing in the preformed (corrugated) copper fins. The core is created by stacking alternate layers of hydrogen and oxygen passageways. A furnace braze is used to assemble the core and form a thermal bond between the outer surfaces of adjacent facesheets.

Isolation of the working fluids is provided by assembling each hydrogen and oxygen passage separately and then braze assembling the passages along their flat sides. The hydrogen and oxygen manifold are also completely independent to prevent internal leaks. All leak paths are directed to vacuum environment.

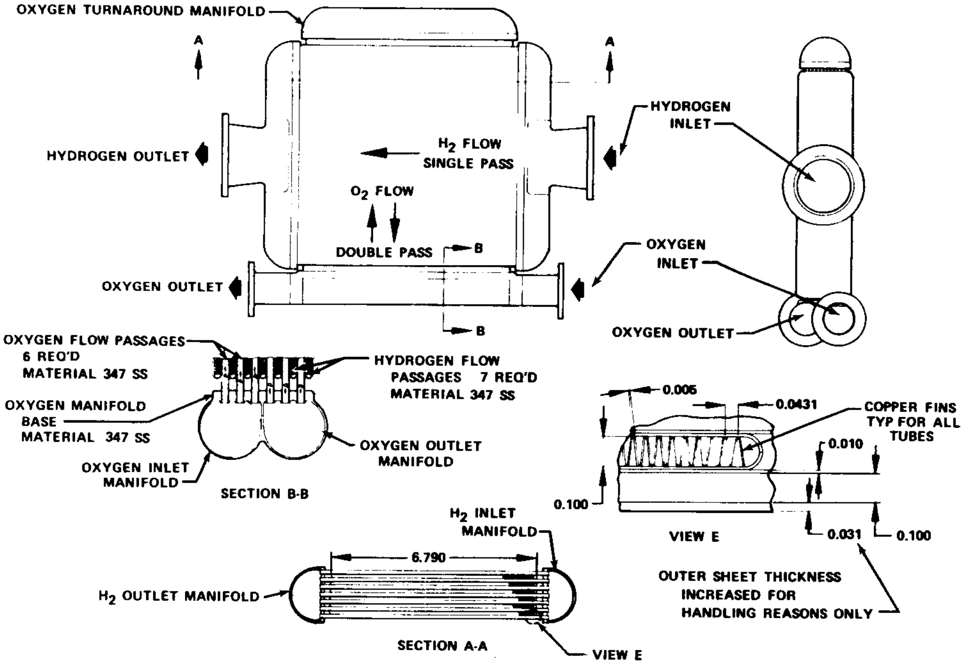

OXYGEN TURNAROUND MANIFOLD
A
A
HYDROGEN INLET
HYDROGEN OUTLET
H_2 FLOW SINGLE PASS
O_2 FLOW
DOUBLE PASS
B
OXYGEN INLET
OXYGEN OUTLET
B
OXYGEN OUTLET
OXYGEN FLOW PASSAGES 6 REQ'D MATERIAL 347 SS
HYDROGEN FLOW PASSAGES 7 REQ'D MATERIAL 347 SS
OXYGEN MANIFOLD BASE MATERIAL 347 SS
OXYGEN OUTLET MANIFOLD
OXYGEN INLET MANIFOLD
SECTION B-B
0.005
0.0431
COPPER FINS TYP FOR ALL TUBES
0.010
0.100
VIEW E
0.031
0.100
H_2 INLET MANIFOLD
6.790
H_2 OUTLET MANIFOLD
SECTION A-A
VIEW E
OUTER SHEET THICKNESS INCREASED FOR HANDLING REASONS ONLY

Figure C-107. RL10, IIA and IIB GO_2 Heat Exchanger

FD 72057

4.1.4.2 Design Studies

The feasibility of an oxidizer heat exchanger was confirmed during the Critical Element Evaluation. However, the heat exchanger design conceived at that time was large and subsequent studies described in Section C3.1.2 were undertaken to investigate methods of reducing the heat exchanger volume. These studies provided the design requirements shown in the Introduction. Based upon these design requirements, the thermodynamic and mechanical design studies were completed as detailed in the following paragraphs.

The finned plate configuration of the heat exchanger was chosen from Reference 3 for its favorable high thermal flux and low volume features. This geometry provides over 1300 ft^2 of heat transfer area for every cubic foot of core volume. The thermodynamic analysis was performed utilizing friction factors and Colburn factors experimentally determined for this particular core geometry to predict heat transfer capability and fluid pressure losses.

The hydrogen-oxygen heat exchanger must provide complete isolation of the working fluids, eliminating any possibility of a hydrogen to oxygen or oxygen to hydrogen leak. This is done by weld assembling each flow passage on an individual basis and braze-assembling hydrogen passages to oxygen passages only along their respective flatsheet sides. The hydrogen and oxygen manifolds are also completely independent to prevent a leak between them as shown in figure C-107. Every joint enveloping the flowpaths of hydrogen or oxygen is exposed to the environment; therefore, any leak is an overboard leak. Internal pressure acting on the large flat surfaces of the heat exchanger is resisted by tensile forces in the copper fins. Therefore, the face sheets are pressure-loaded over a corrugation span (0.043 in.) rather than the 7.2 in. width of the core. This allows a thin lightly loaded structure. The maximum expected face sheet stress due to pressure loading is 6,300 psi. Copper fin maximum stress is 3,000 psi with the braze joint stress slightly less. The outer face sheets are designed with an increased thickness to avoid damage in handling.

Individual flow passage assembly simplifies manufacturing and allows inspection and testing prior to braze assembly of the core. Flow passages are created by butt welding passage halves together, inserting the formed copper fins with foil braze material (NICORO 80) and furnace brazing. These passages can then be pressure tested and X-ray inspected. The maximum allowable braze void is approximately 0.150-in. diameter. The heat exchanger core is then created by stacking alternating layers of hydrogen and oxygen flow passages and furnace brazing with a copper-silver braze alloy. This braze joint promotes heat conduction between passages and the minimum braze coverage is set by heat transfer requirements. Hydrogen manifold bases are welded onto the hydrogen flow passages with a continuous weld around each passage. After a pressure check, the remainder of the hydrogen manifold is welded into place. The oxygen manifold is then attached in a similar manner.

4.1.4.3 Life

Due largely to thin wall construction, little thermal stress is anticipated in the heat exchanger core. Independent manifolds permit thermal growth in the warm core. There is a significant thermal gradient between the core metal temperature and the oxygen inlet temperature which may cause up to 0.3% strain in three thin-walled coolant passages. LCF data for 347 SST indicates a 0.6%

strain level for a 3,000 cycle life at 200°F. Any facesheet strain occurs across the copper fins such that it does not induce strain in the copper fins or the braze joints. Therefore, a life of 10 hr is easily predicted for this heat exchanger configuration.

4.1.4.4 Materials

Copper is required as the fin material to meet the high conduction coefficient needed in this compact heat exchanger. 347 SST was chosen for the facesheets and manifolds because of its thermal compatibility with copper, ductility, good welding and brazing properties, and compatibility with hydrogen and oxygen.

The following is a summary of the materials selected for the oxidizer heat exchanger:

Component	Material	Type
Manifolds	Stainless Steel	AMS 5646
Facesheets	Stainless Steel	AMS 5512
Fins	Copper	AMS 4501
Fin Braze	NICORO 80	TBD
Facesheet Braze	Copper-Silver	TBD

4.1.4.5 Weight

Weight is minimized in the GO_2 heat exchanger by using materials which are 0.005 and 0.010-in. thick, the practical manufacturing limit for thin sheet metal. Stresses are quite low even with these very thin structural members; only the manifolds are stress limited. Weight is determined by the large total surface area required for high thermal efficiency. The current weight estimate is 13 lb.

4.1.5 Controls and Instrumentation

The RL10 Derivative II propellant control and instrumentation system consists of the following components: fuel inlet shutoff valve, oxidizer inlet shutoff valve oxidizer flow control, fuel vent valve, turbine bypass valve, thrust control, main fuel shutoff valve, gaseous oxidizer valve, oxidizer tank pressurization valve, fuel tank pressurization valve, nozzle coolant valve, 4 solenoid valves, engine instrumentation and connecting cables. A description of the valve effective area schedules has been previously provided in Section C.3.2.2. A schematic of the propellant system is shown in figure C-108 in which the control components are identified and the location in the engine system defined. A mechanical description of the control/instrumentation system is provided in the following paragraphs.

4.1.5.1 Propellant Control Valves

a. Propellant Inlet Shutoff Valves

These valves are spring-loaded, normally closed, helium-operated, two-position ball valves, which provide a seal between the vehicle propellant tank and the engine pumps when the engine is not in operation. Both valves are located just upstream of their respective pump inlets and are 3.2 in. and 4.6 in. diameter, respectively, the same diameter as the fuel and oxidizer pump inlets. The valves are actuated by helium operating on a piston/bellows assembly. The linear motion of the actuator is translated into rotary motion at the ball valve by a rack and pinion. Figure C-109 shows both valves and a cross section of the actuator.

Ball valve sealing is accomplished with dual pressure-loaded fluorocarbon rub seals. The valves incorporate a vented cavity between dual seals such that any leakage past the closed valve is vented overboard.

The fuel inlet shutoff valve estimated weight is 12.5 lb and the oxidizer inlet shutoff valve estimated weight is 15.3 lb based upon layout drawings. The weight of each valve is minimized by use of aluminum for the valve housing, ball and actuation housing. The rack and pinion, return spring, and other valve hardware are designed of stainless steel.

b. Oxidizer Flow Control

The oxidizer flow control shown in figure C-110 is a spring-loaded, normally closed, line pressure actuated valve located in the main oxidizer line between the oxidizer pump and the injector. The valve meters liquid oxidizer flow at all operating levels above tank head idle. The valve is configured to provide ground trim of propellant mixture ratio, and provide in-flight oxidizer propellant utilization control during full thrust operation.

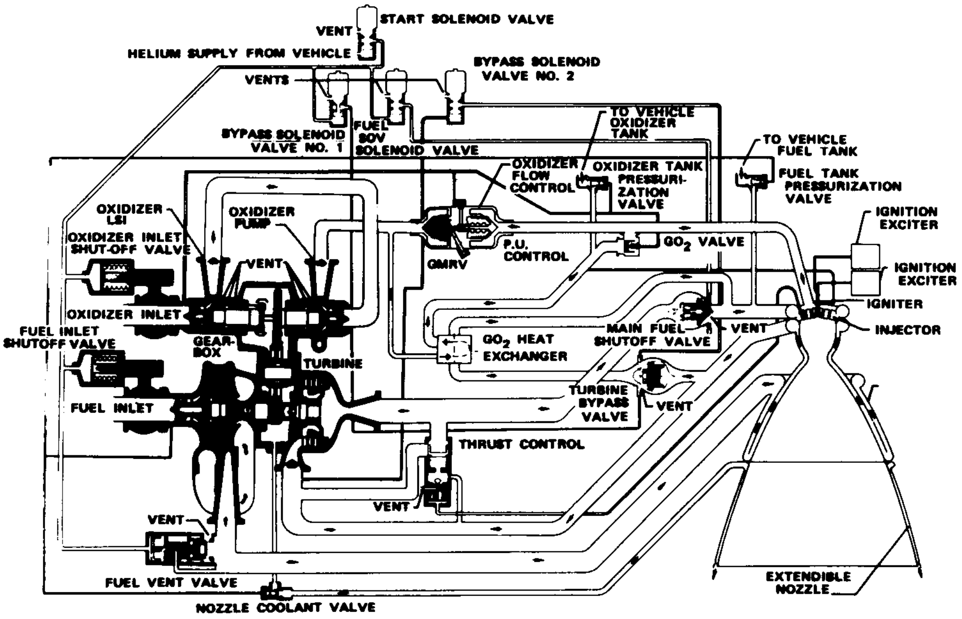
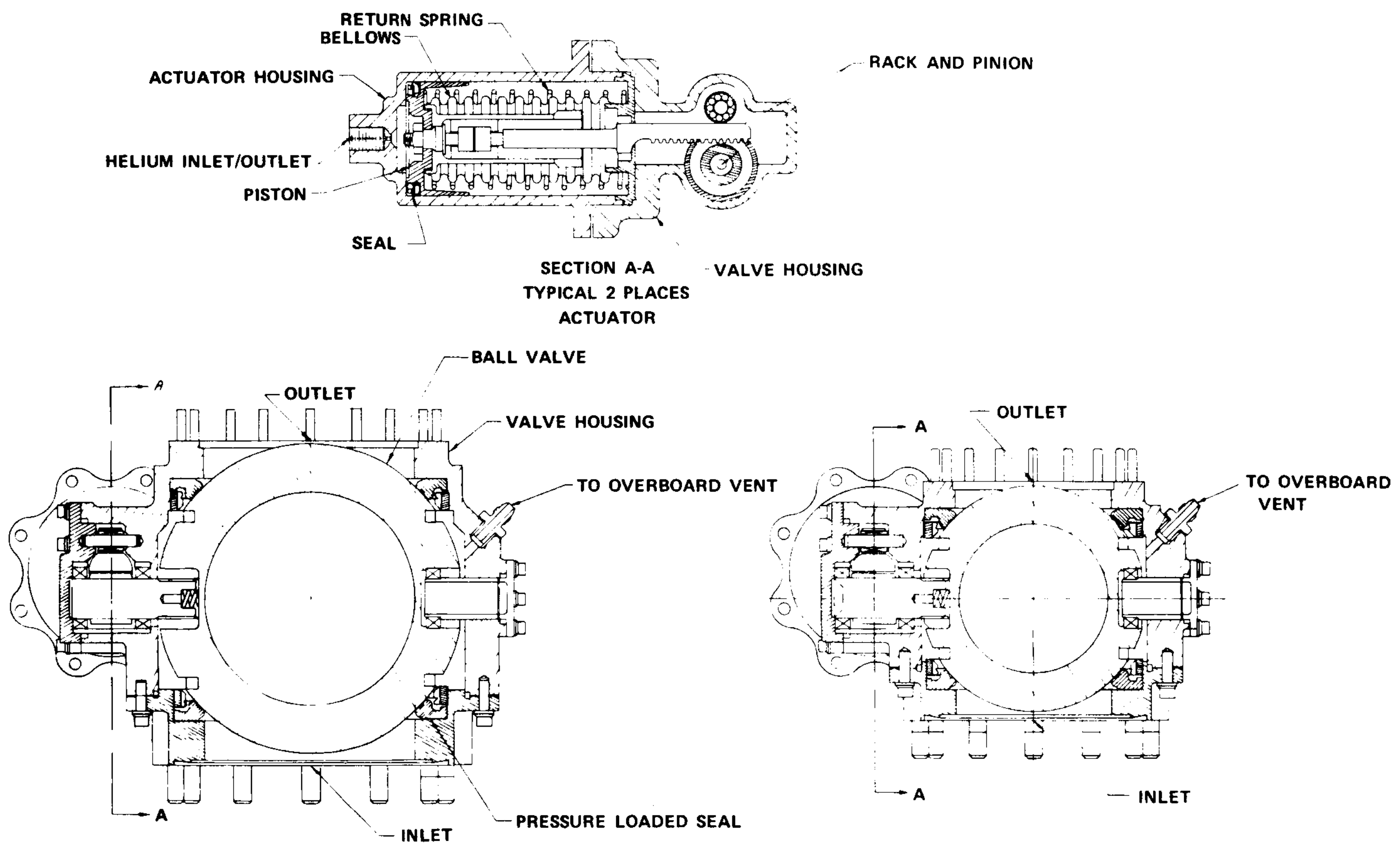

START SOLENOID VALVE
VENT
HELIUM SUPPLY FROM VEHICLE
BYPASS SOLENOID VALVE NO. 2
VENTS
TO VEHICLE OXIDIZER TANK
BYPASS SOLENOID VALVE NO. 1
FUEL SOV SOLENOID VALVE
TO VEHICLE FUEL TANK
OXIDIZER FLOW CONTROL
OXIDIZER TANK PRESSURIZATION VALVE
FUEL TANK PRESSURIZATION VALVE
IGNITION EXCITER
OXIDIZER LSI
OXIDIZER PUMP
OXIDIZER INLET SHUT-OFF VALVE
VENT
P.U. CONTROL
GMRV
GO$_2$ VALVE
IGNITION EXCITER
IGNITER
OXIDIZER INLET
FUEL INLET SHUTOFF VALVE
GEAR-BOX
MAIN FUEL SHUTOFF VALVE
VENT
INJECTOR
GO$_2$ HEAT EXCHANGER
TURBINE
TURBINE BYPASS VALVE
VENT
FUEL INLET
THRUST CONTROL
VENT
VENT
FUEL VENT VALVE
EXTENDIBLE NOZZLE
NOZZLE COOLANT VALVE

Figure C-108. RL10 Derivative IIA Propellant Flow Schematic

FD 74112A


RETURN SPRING
BELLOWS
ACTUATOR HOUSING
HELIUM INLET/OUTLET
PISTON
SEAL
RACK AND PINION
SECTION A-A
TYPICAL 2 PLACES
ACTUATOR
VALVE HOUSING
BALL VALVE
OUTLET
VALVE HOUSING
TO OVERBOARD VENT
A
A
PRESSURE LOADED SEAL
INLET
OUTLET
A
TO OVERBOARD VENT
A
INLET

OXIDIZER INLET SHUTOFF VALVE

FUEL INLET SHUTOFF VALVE

Figure C-109. Inlet Valves

FD 76003

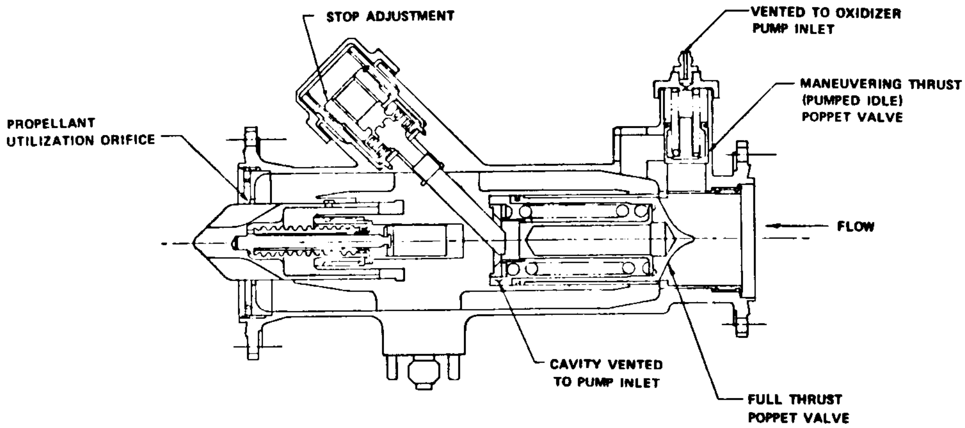

STOP ADJUSTMENT
VENTED TO OXIDIZER PUMP INLET
MANEUVERING THRUST (PUMPED IDLE) POPPET VALVE
PROPELLANT UTILIZATION ORIFICE
FLOW
CAVITY VENTED TO PUMP INLET
FULL THRUST POPPET VALVE

Figure C-110. Oxidizer Flow Control Valve FD 76005

The oxidizer flow control valve is divided into two functional sections. The forward section contains two spring-loaded poppet valves used to meter oxidizer flow during maneuver thrust (pumped idle) and full thrust and regulate flow during the engine transient to full thrust. The aft section contains a pintle valve used for propellant utilization control. The two poppet valves in the forward section are normally spring-loaded closed and open as a function of the pressure differential between valve inlet pressure and a pressure within the valve cavity which has been vented to pump inlet pressure. During tank head idle operation, the poppets are closed and no liquid oxidizer flow is allowed to enter the injector. As oxidizer line pressure increases during the acceleration from tank head idle to maneuver thrust (pumped idle), the bypass poppet valve fully opens to the metering area required for maneuver thrust operation. When the engine accelerates from maneuver thrust (pumped idle) to full thrust operation, the main poppet valve is also opened as a function of the differential pressure between valve inlet and pump inlet pressure. The bypass and main poppet valves both remain open during full thrust operation and the combined areas meter the required oxidizer flow. The full open position of the main poppet valve can be ground trimmed by a threaded mechanical stop to ground adjust engine mixture ratio.

The total valve oxidizer flow exits through the propellant utilization orifice and pintle at the aft end of the valve. Engine mixture ratio can be varied by applying torque to an actuation shaft which positions the discharge pintle utilizing a rack and pinion mechanism. The actuation shaft is sealed by a bellows assembly. The pinion shaft incorporates stops to limit shaft rotation and therefore, engine mixture ratio range within allowable limits.

This valve is very similar to the RL10A-3-3 oxidizer flow control valve in size and operation. Flow areas of the main poppet valve and the propellant utilization valve have been increased to accommodate operation at a higher mixture ratio (i.e., 6.0 vs 5.0) and the bypass poppet valve has been added for maneuver thrust (pumped idle) operation. The valve is designed to be manufactured of aluminum with stainless steel hardware. The calculated weight, based on the valve layout drawing, and RL10A-3-3 valve weight, is 9 pounds.

c. Turbine Bypass Valve

The turbine bypass valve shown in figure C-111 is a spring-loaded, normally closed, three-position, bullet-type annular gate valve located in the turbine bypass line. The valve is similar to the RL10A-3-3 main fuel shutoff valve in size, materials used, and operation. The valve is pressure actuated to allow the setting of three discrete areas for metering turbine bypass hydrogen flow during tank head idle, maneuver thrust (pumped idle) and full thrust operation. During tank head idle operation the valve is activated to the full open position of 1.5 in.^{2} by helium operating on a bellows actuator. During maneuver thrust operation the valve is actuated to an intermediate area of 0.614 in.^{2} by gaseous hydrogen operating on a concentric secondary bellows actuator. During full thrust operation the gate is spring-loaded into its normally closed position and a 0.114 in.^{2} area is provided by notches in the annular gate. The valve is designed to be made of aluminum and stainless steel and is estimated to weigh 4.2 lb based upon the valve layout drawing.

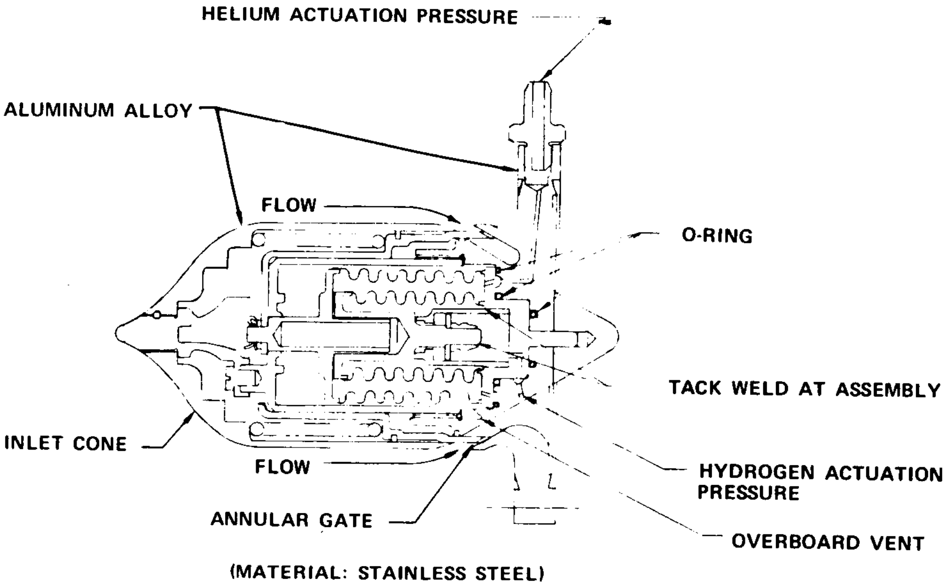
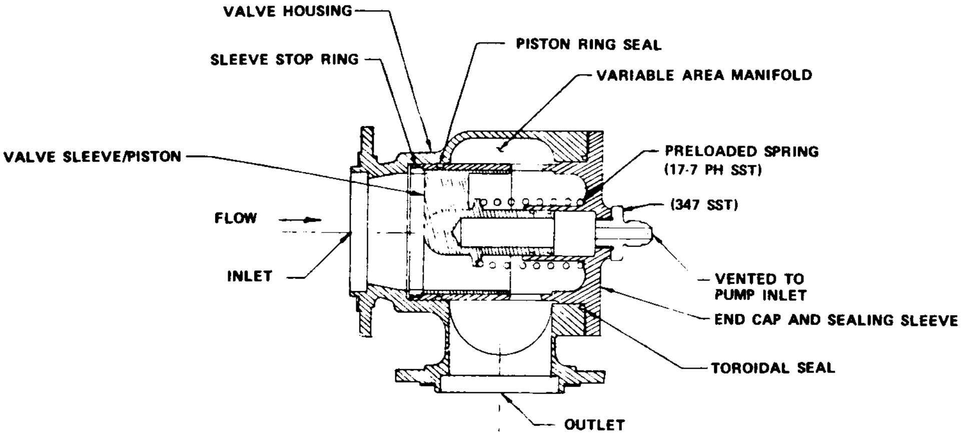


Figure C-111. Turbine Bypass Valve FD 76004

d. Gaseous Oxidizer Valve

The gaseous oxidizer valve shown in figure C-112 is a two-position, spring-loaded, normally open, pressure activated, sleeve valve located between the GOX heat exchanger and the injector. This valve meters gaseous oxygen flow during tank head idle and regulates oxygen flow during the transient to maneuver thrust. The pressure differential used for valve actuation is maintained on the sleeve piston by valve inlet pressure on the high side, and by venting a low pressure valve cavity to the oxidizer pump inlet. As line pressure increases during the acceleration to maneuver thrust, the valve closes from 2.0 in.^{2} effective area against the preloaded spring. The valve is designed with an aluminum housing and valve sleeve. The estimated valve weight is 2.0 lb based upon the valve layout drawing.


VALVE HOUSING
SLEEVE STOP RING
PISTON RING SEAL
VARIABLE AREA MANIFOLD
VALVE SLEEVE/PISTON
PRELOADED SPRING
(17-7 PH SST)
(347 SST)
FLOW
INLET
VENTED TO
PUMP INLET
END CAP AND SEALING SLEEVE
TOROIDAL SEAL
OUTLET

(MATERIAL: ALUMINUM ALLOY)

Figure C-112. Gaseous Oxidizer Valve FD 76002A

e. Tank Pressurization Valves

Propellant tank pressurization valves shown in figure C-113 are spring-loaded, normally closed, line pressure actuated, two-position poppet valves that supply propellants for fuel and oxidizer tank pressurization. These valves limit but do not regulate the tank pressurant flowrate. Flow regulation is provided within the vehicle tank pressurization system. When the differential between line pressure and an internal cavity vented to pump inlet pressure increase to a predetermined value, the total force acting on the poppet overcomes the spring load and the valve fully opens to an effective area of 0.34 in.^{2} The propellant tank pressurization valves are designed of aluminum with stainless steel fittings with a mylar poppet seal. The estimated weight of each valve is 2 lb based upon the layout drawing.

f. Nozzle Coolant Valve

The nozzle coolant valve shown in figure C-114 is a spring-loaded, normally closed, line pressure actuated, two-position poppet valve which utilizes the same housing and design as the tank pressurization valves. The valve poppet is orificed to provide a minimum area of 0.037 in.^{2} in the closed position to meter coolant flow to the dump cooled nozzle during tank head idle operation. As line pressure increases to a level greater than 20 psia, the total force acting on the poppet overcomes the spring preload and the valve fully opens (0.30 in.^{2} area) for maneuver and full thrust operation. The nozzle coolant valve estimated weight is 2 lb.

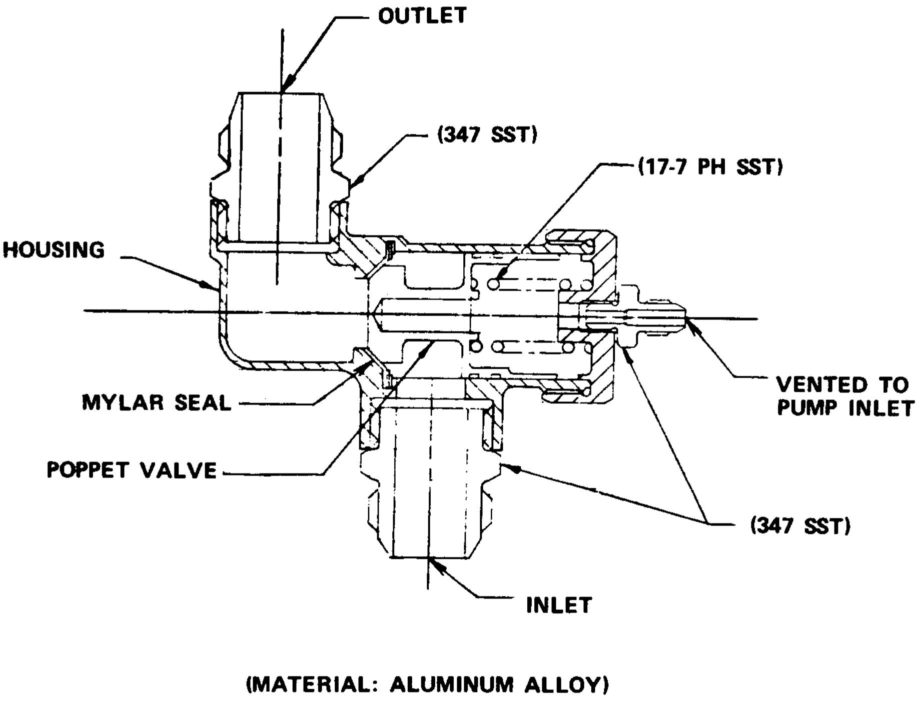

OUTLET
(347 SST)
(17-7 PH SST)
HOUSING
VENTED TO PUMP INLET
MYLAR SEAL
POPPET VALVE
(347 SST)
INLET
(MATERIAL: ALUMINUM ALLOY)

Figure C-113. Tank Pressurization Valve FD 76007A

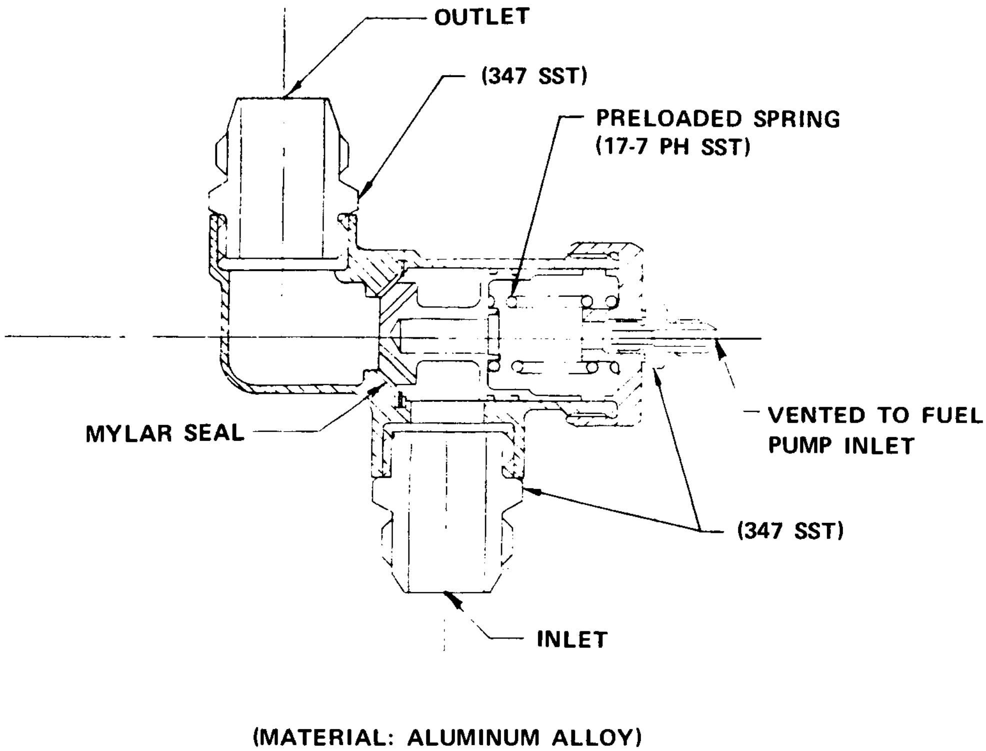

OUTLET
(347 SST)
PRELOADED SPRING (17-7 PH SST)
MYLAR SEAL
VENTED TO FUEL PUMP INLET
(347 SST)
INLET
(MATERIAL: ALUMINUM ALLOY)

Figure C-114. Nozzle Coolant Valve FD 76006A

g. Thrust Control Valve

The thrust control valve, shown in figure C-115 is an RL10A-3-3 component. The valve is a normally closed, servo-operated, closed-loop, variable-position bypass valve used to control engine thrust at full thrust by the regulation of the turbine power. Control of engine thrust is provided by combustion chamber pressure acting through a motor bellows and spring carrier against a reference spring load and reference bellows pressure load to actuate a servo lever exposing a shear orifice. Exposure of the shear orifice bleeds servo chamber pressure which is supplied from engine turbine upstream pressure. The bypass valve position feedback signal is mechanically transmitted through the feedback spring carrier and spring to the servo lever. As combustion chamber pressure varies from the desired value, the action of the control allows the turbine bypass section to vary fuel flow through the turbine. This regulates turbine power and speed which in turn regulates combustion chamber pressure. A secondary function of the control is to limit engine thrust overshoot during the start transient. This is accomplished through a reference bellows pressure lag system that prevents reference pressure from rising at the same rate as combustion chamber pressure. This allows the control bypass valve to open early in the start transient and reduces turbine power before attainment of steady-state chamber pressure. The thrust control valve position is monitored by a linear variable differential transformer (LVDT) device and the data are interpreted as a degree of engine health. The valve is constructed of aluminum and weighs 5.3 pounds.

h. Fuel Vent Valve

The fuel vent valve, shown in figure C-116, is an RL10A-3-3 component. It is a pressure operated, two-position, sleeve type valve that is spring-loaded open to provide pressure relief of the fuel system lines during the engine shutdown. The valve is maintained in the closed position during all three active modes of engine operation.

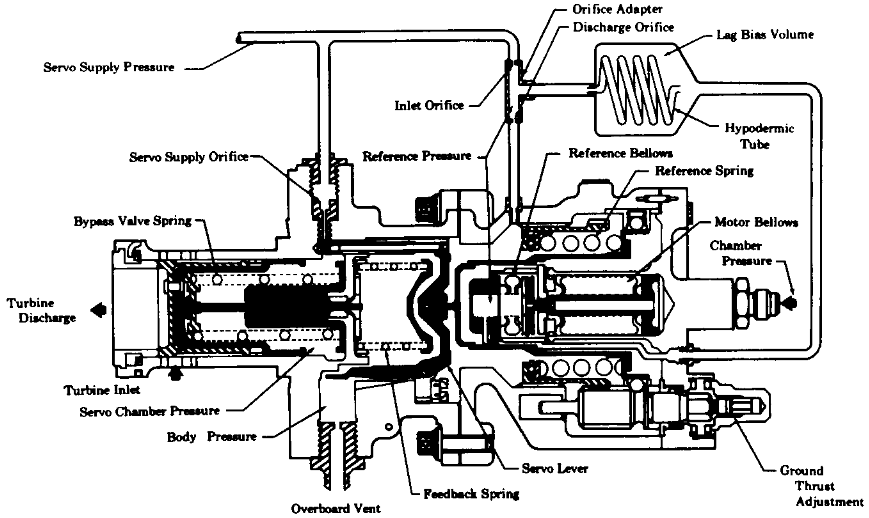

Orifice Adapter
Discharge Orifice
Lag Bias Volume
Servo Supply Pressure
Inlet Orifice
Hypodermic Tube
Servo Supply Orifice
Reference Pressure
Reference Bellows
Reference Spring
Bypass Valve Spring
Motor Bellows
Chamber Pressure
Turbine Discharge
Turbine Inlet
Servo Chamber Pressure
Body Pressure
Servo Lever
Ground Thrust Adjustment
Feedback Spring
Overboard Vent

Figure C-115. Thrust Control Valve

FD 10744

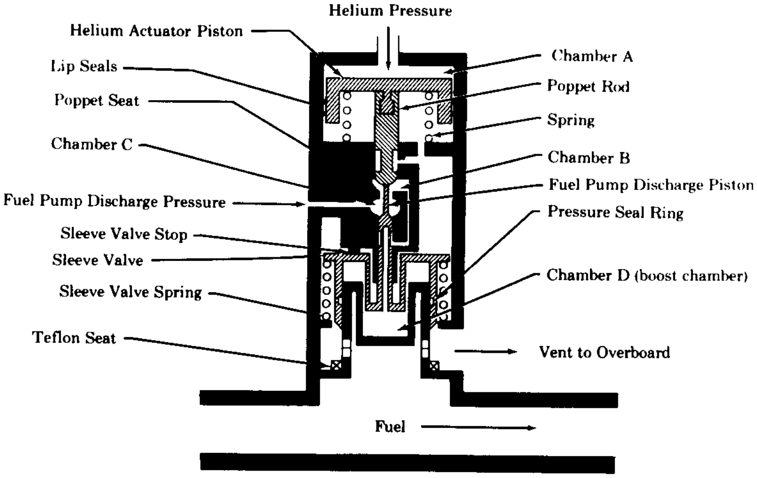

Helium Pressure
Helium Actuator Piston
Lip Seals
Poppet Seat
Chamber C
Fuel Pump Discharge Pressure
Sleeve Valve Stop
Sleeve Valve
Sleeve Valve Spring
Teflon Seat
Chamber A
Poppet Rod
Spring
Chamber B
Fuel Pump Discharge Piston
Pressure Seal Ring
Chamber D (boost chamber)
Vent to Overboard
Fuel

Figure C-116. Pressure-Boosted Fuel Pump Cool-down Bleed and Pressure Relief Valve FD 2666C

At the start signal helium pressure actuates the valve piston assembly moving the sleeve valve to close the overboard vent port. As the engine accelerates, fuel pump discharge pressure is used to actuate the fuel pump discharge piston to force the sleeve valve against the Teflon seat to provide a positive seal during high pressure operation. At shutdown, when helium pressure is removed, the vent port fully opens, relieving fuel pressure in the fuel system lines. In addition to being opened by spring loads, the valves are boosted open with fuel pump discharge pressure to ensure rapid opening of the vent port.

The fuel vent valve is constructed of aluminum and weighs 6.3 pounds.

i. Main Fuel Shutoff Valve

The main fuel shutoff valve, shown in figure C-117, is an RL10A-3-3 component. It is a helium operated, two-position, normally closed, annular gate valve. The valve serves to prevent the flow of fuel through the fuel pump turbine during tank head idle operation and provides a rapid cutoff of fuel flow to the combustion chamber at engine shutdown.

The shutoff valve gate is opened by helium pressurization of the bellows assembly to allow the flow of fuel through the turbine at all operating modes above tank head idle. The compressed shutoff valve spring returns the gate to its normally closed position when helium pressure is vented at engine shutdown. Sealing is accomplished by the seating of the spherical surface of the gate against a conical surface on the valve housing and by the gate seal ring.

The valve is constructed primarily of steel and weighs 3.4 pounds.

j. Solenoid Valves

The solenoid valves, shown in figure C-118, are RL10A-3-3 components. They are solenoid-actuated, direct acting, 3-way valves with double-ended poppets that supply helium or hydrogen actuation fluid to the various propellant valves.

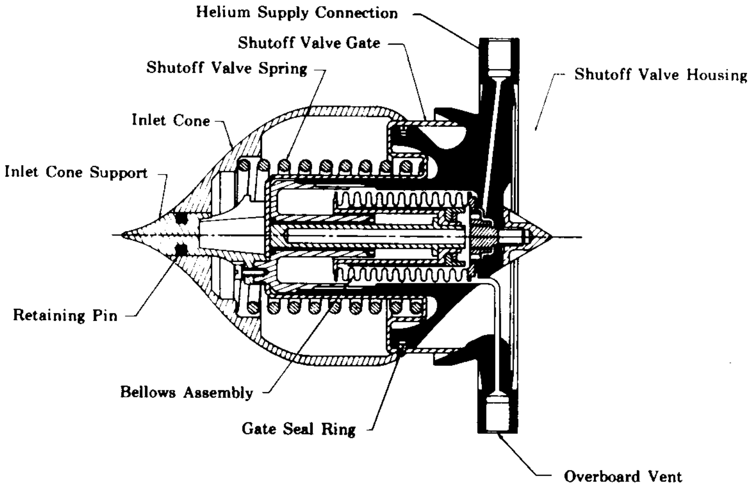

Helium Supply Connection
Shutoff Valve Gate
Shutoff Valve Spring
Shutoff Valve Housing
Inlet Cone
Inlet Cone Support
Retaining Pin
Bellows Assembly
Gate Seal Ring
Overboard Vent

Figure C-117. Main Fuel Shutoff Valve Schematic FD 1551D

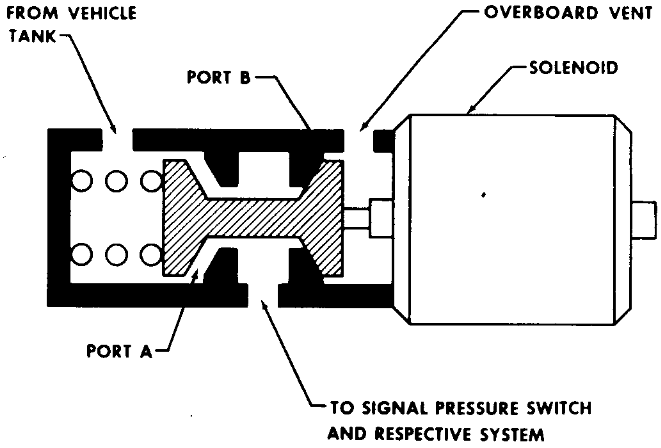

FROM VEHICLE TANK
OVERBOARD VENT
PORT B
SOLENOID
PORT A
TO SIGNAL PRESSURE SWITCH AND RESPECTIVE SYSTEM

Figure C-118. Solenoid Valve Schematic FD 4444

The four solenoid valves used in the Derivative II engine are identical in design and function. The start solenoid valve controls the actuator helium supply to the propellant inlet shutoff valves and fuel vent valve. The fuel shutoff solenoid valve controls the actuator helium supply to the main fuel shutoff valve. The bypass solenoid valve No. 1 controls the actuator helium supply to the turbine bypass valve. The bypass solenoid valve No. 2 controls the actuator hydrogen supply to the turbine bypass valve. In the deenergized position, valve port A is closed and valve port B is ambient vented. The poppet is positioned by the valve spring force on the poppet valve body. Upon an electrical signal from the vehicle, the respective solenoid valve is energized by dc electrical supply from the vehicle. The plunger rod moves the poppet valve, opening port A and closing port B. Helium/hydrogen flows through port A into the control valve actuators. The solenoid is deenergized at engine shutdown and the spring returns the poppet valve to its original position closing port A and opening port B, through which the pneumatic media in the engine valve system is vented overboard.

4.1.5.2 Instrumentation and Harness System

The instrumentation and command system on the RL10 Derivative engine consists of an instrumentation enclosure, command harness, instrumentation harness, and various sensing units which monitor engine parameters listed in table C-24.

Table C-24. RL10 Derivative II Engines Instrumentation and Monitored Parameters

Parameter	Range and Units	Uncertainty(1)
Fuel Pump		
Housing Temperature	35-660°R	±6°R
Discharge Pressure	0-1200 psia	±2%
Oxidizer Pump		
Housing Temperature	150-660°R	±6°R
Discharge Pressure	0-800 psia	±2%
Rotational Speed	0-15,000 rpm	±4%
Thrust Chamber		
Chamber Pressure	0-500 psia	±2%
Low Range Chamber Pressure	0-15 psia	±2%
Chamber Coolant Discharge Temperature	200-1200°R	±15°R
Miscellaneous		
Nozzle Extension Contact	Switch	N/A
Thrust Control Position	0-100%	±5%
Gearbox Vibration	0-5 g	±10%
Exciter Monitor	0-10 vdc	±5%
Solenoid Monitor	0-20 mvdc	±5%

---

(1) (Bias + $t_{.95}S$) in percent of full scale.

The instrumentation enclosure contains the solenoids, pressure transducers, completion networks, and other signal conditioning units for the temperature and vibration sensors. Since these instruments require a controlled temperature environment for operation, the instrumentation enclosure also contains an electrical heater and a thermocouple to monitor the internal temperature. Mounted on the instrumentation enclosure are 2 interface connectors which mate with the vehicle cables. One connector provides command signals from the vehicle computer to the engine solenoids, exciters, and nozzle motors. The other connector provides 28 vdc excitation to the engine instrumentation and returns the instrumentation output signals to the vehicle. Also mounted on the instrumentation enclosure are 2 connectors which mate with the engine command and engine instrumentation harnesses.

The engine command harness provides 28 vdc command signals from the vehicle, via the instrumentation enclosure to the nozzle motors and ignition exciters on the engine.

The engine instrumentation harness provides 28 vdc excitation to and output signals from the various temperature, speed, and position sensors on the engine. This harness is also connected to the vehicle cables through the instrumentation box.

4.1.5.3 Design Theory

A conceptual design layout for each of the new valves was generated to define mechanical implementation, materials identification, and physical sizing for weight and envelope defintion. A low risk design approach was taken by utilizing previously proven valve elements, where possible, such as shutoff seals, bellows, static seals and materials combinations. In most cases the entire valve design was based on a resizing or modification of an existing RL10A-3-3 component design. Where possible, common valve elements were utilized.

The electrical wiring layout of the instrumentation system is shown on figure C-119. Command signals are kept separated from instrumentation signals to minimize the effects of noise interference problems.

The harness shielding scheme was designed to meet EMI specifications MIL Spec 461/462. Harnesses outside the instrumentation enclosure have an outer metal braid for high frequency noise suppression. Cables inside the instrumentation enclosure do not have this outer braid because the instrumentation enclosure suppresses high frequency noise by being connected to ground with a bond strap. All harnesses have an inner metal braid for low frequency noise suppression. Each instrumentation signal has its own braid which is continuous to the interface connectors. The braids are electrically floating at the instruments, and separately connected to ground in the instrumentation enclosure, so that noise in one circuit will not affect adjacent circuits. The shield braiding in the vehicle cables are not connected to engine cable braiding. Instead they are connected together and to ground. This is so that any noise from the vehicle will be grounded before it can affect the engine instrumentation signals.

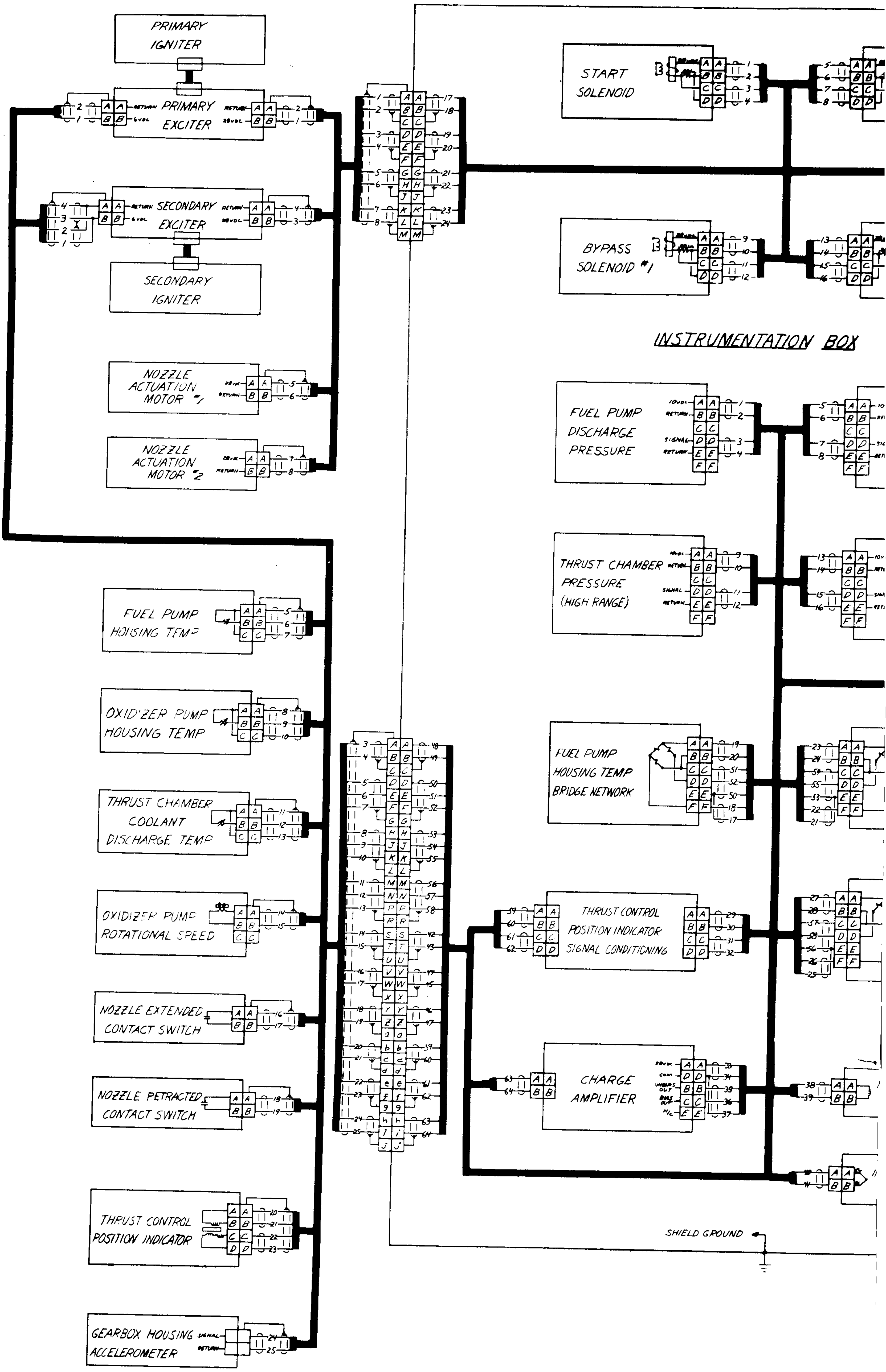
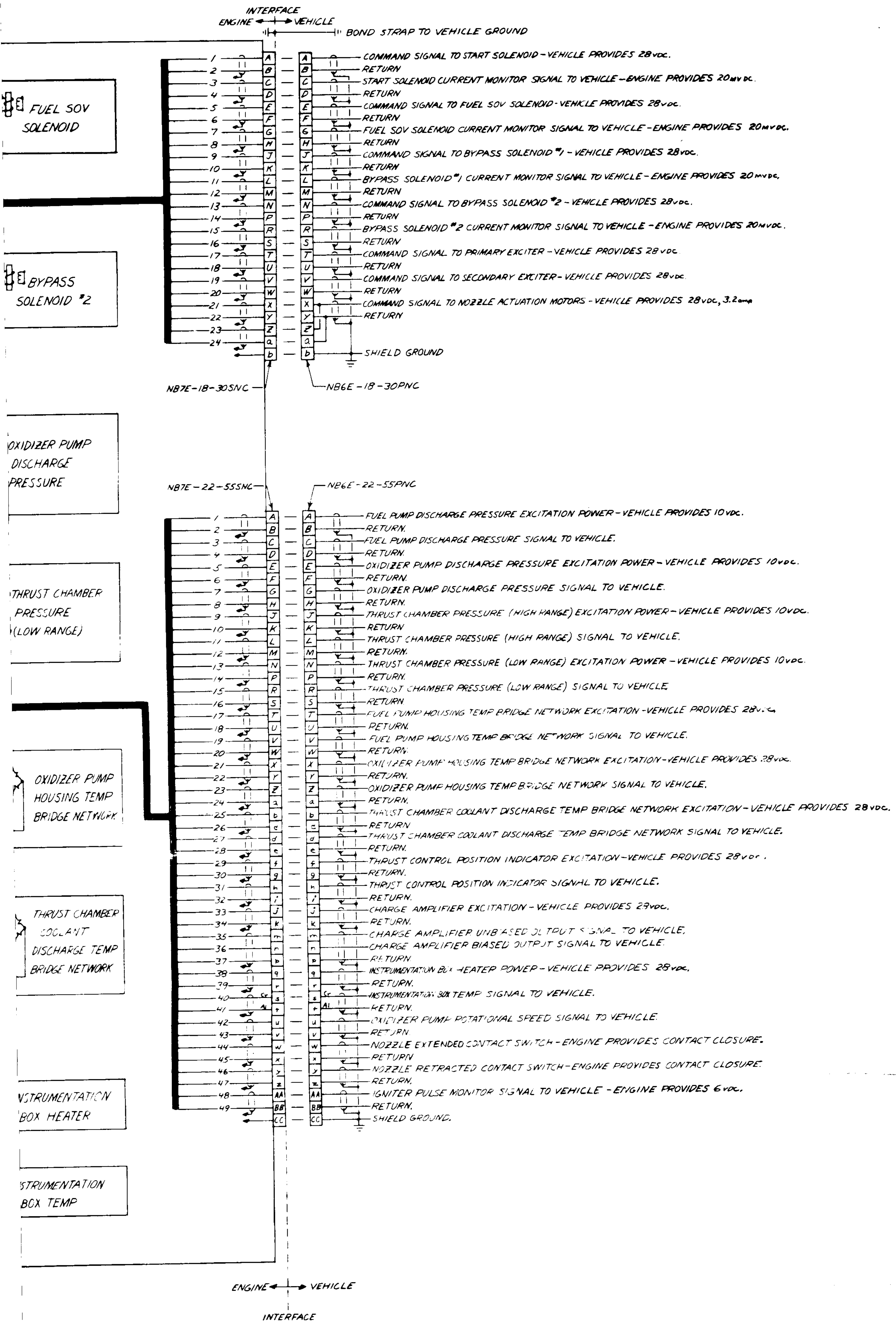

PRIMARY IGNITER
PRIMARY EXCITER
RETURN
6VDC
28VDC
SECONDARY EXCITER
SECONDARY IGNITER
NOZZLE ACTUATION MOTOR #1
NOZZLE ACTUATION MOTOR #2
FUEL PUMP HOUSING TEMP
OXIDIZER PUMP HOUSING TEMP
THRUST CHAMBER COOLANT DISCHARGE TEMP
OXIDIZER PUMP ROTATIONAL SPEED
NOZZLE EXTENDED CONTACT SWITCH
NOZZLE RETRACTED CONTACT SWITCH
THRUST CONTROL POSITION INDICATOR
GEARBOX HOUSING ACCELEROMETER
SIGNAL
START SOLENOID
BYPASS SOLENOID #1
INSTRUMENTATION BOX
FUEL PUMP DISCHARGE PRESSURE
10VDC
THRUST CHAMBER PRESSURE (HIGH RANGE)
FUEL PUMP HOUSING TEMP BRIDGE NETWORK
THRUST CONTROL POSITION INDICATOR SIGNAL CONDITIONING
CHARGE AMPLIFIER
SHIELD GROUND

Figure C-119. Wiring and Instrumentation Schematic


INTERFACE
ENGINE ← → VEHICLE
BOND STRAP TO VEHICLE GROUND
FUEL SOV SOLENOID
BYPASS SOLENOID #2
COMMAND SIGNAL TO START SOLENOID - VEHICLE PROVIDES 28vdc.
RETURN
START SOLENOID CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20mv dc.
RETURN
COMMAND SIGNAL TO FUEL SOV SOLENOID - VEHICLE PROVIDES 28vdc.
RETURN
FUEL SOV SOLENOID CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20mvdc.
RETURN
COMMAND SIGNAL TO BYPASS SOLENOID #1 - VEHICLE PROVIDES 28vdc.
RETURN
BYPASS SOLENOID #1 CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20mvdc.
RETURN
COMMAND SIGNAL TO BYPASS SOLENOID #2 - VEHICLE PROVIDES 28vdc.
RETURN
BYPASS SOLENOID #2 CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20mvdc.
RETURN
COMMAND SIGNAL TO PRIMARY EXCITER - VEHICLE PROVIDES 28vdc
RETURN
COMMAND SIGNAL TO SECONDARY EXCITER - VEHICLE PROVIDES 28vdc
RETURN
COMMAND SIGNAL TO NOZZLE ACTUATION MOTORS - VEHICLE PROVIDES 28vdc, 3.2amp
RETURN
SHIELD GROUND
NB7E-18-30SNC
NB6E-18-30PNC
OXIDIZER PUMP DISCHARGE PRESSURE
THRUST CHAMBER PRESSURE (LOW RANGE)
OXIDIZER PUMP HOUSING TEMP BRIDGE NETWORK
THRUST CHAMBER COOLANT DISCHARGE TEMP BRIDGE NETWORK
INSTRUMENTATION BOX HEATER
INSTRUMENTATION BOX TEMP
NB7E-22-55SNC
NB6E-22-55PNC
FUEL PUMP DISCHARGE PRESSURE EXCITATION POWER - VEHICLE PROVIDES 10vdc.
RETURN.
FUEL PUMP DISCHARGE PRESSURE SIGNAL TO VEHICLE.
RETURN.
OXIDIZER PUMP DISCHARGE PRESSURE EXCITATION POWER - VEHICLE PROVIDES 10vdc.
RETURN.
OXIDIZER PUMP DISCHARGE PRESSURE SIGNAL TO VEHICLE.
RETURN.
THRUST CHAMBER PRESSURE (HIGH RANGE) EXCITATION POWER - VEHICLE PROVIDES 10vdc.
RETURN
THRUST CHAMBER PRESSURE (HIGH RANGE) SIGNAL TO VEHICLE.
RETURN.
THRUST CHAMBER PRESSURE (LOW RANGE) EXCITATION POWER - VEHICLE PROVIDES 10vdc.
RETURN.
THRUST CHAMBER PRESSURE (LOW RANGE) SIGNAL TO VEHICLE.
RETURN.
FUEL PUMP HOUSING TEMP BRIDGE NETWORK EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN.
FUEL PUMP HOUSING TEMP BRIDGE NETWORK SIGNAL TO VEHICLE.
RETURN.
OXIDIZER PUMP HOUSING TEMP BRIDGE NETWORK EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN.
OXIDIZER PUMP HOUSING TEMP BRIDGE NETWORK SIGNAL TO VEHICLE.
RETURN.
THRUST CHAMBER COOLANT DISCHARGE TEMP BRIDGE NETWORK EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN
THRUST CHAMBER COOLANT DISCHARGE TEMP BRIDGE NETWORK SIGNAL TO VEHICLE.
RETURN.
THRUST CONTROL POSITION INDICATOR EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN.
THRUST CONTROL POSITION INDICATOR SIGNAL TO VEHICLE.
RETURN.
CHARGE AMPLIFIER EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN.
CHARGE AMPLIFIER UNBIASED OUTPUT SIGNAL TO VEHICLE.
CHARGE AMPLIFIER BIASED OUTPUT SIGNAL TO VEHICLE.
RETURN.
INSTRUMENTATION BOX HEATER POWER - VEHICLE PROVIDES 28vdc.
RETURN.
INSTRUMENTATION BOX TEMP SIGNAL TO VEHICLE.
Cr
Al
RETURN.
OXIDIZER PUMP ROTATIONAL SPEED SIGNAL TO VEHICLE.
RETURN.
NOZZLE EXTENDED CONTACT SWITCH - ENGINE PROVIDES CONTACT CLOSURE.
RETURN
NOZZLE RETRACTED CONTACT SWITCH - ENGINE PROVIDES CONTACT CLOSURE.
RETURN.
IGNITER PULSE MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 6vdc.
RETURN.
SHIELD GROUND.
ENGINE ← → VEHICLE
INTERFACE

FD 76713

The interface connectors, which mate with the vehicle cables, are mounted in an easily accessible location on the instrumentation enclosure. They are of the bayonet quick disconnect type (Specification MSFC 40M39569) to provide for easy troubleshooting and replacement of the engine or engine components. The connectors which mate with the command and instrumentation harnesses are of the same type and are located on the instrumentation enclosure such that they will not be confused with interface connectors.

The instrumentation enclosure contains equipment which requires a controlled temperature environment for proper operation. The electrical heater is incorporated for this purpose. The vehicle provides 28 vdc, 2 amp to the heater, and monitors the internal temperature of the instrumentation enclosure with a chromel/alumel thermocouple.

The equipment inside the instrumentation enclosure obtains excitation voltage from the vehicle and provides output signals to the vehicle at the interface connectors. The solenoids (start, fuel SOV, Bypass No. 1, and Bypass No. 2) are activated by a 28 vdc, 2.5 amp command signal. A series resistor provides a millivolt output signal to indicate that the command signal was received and that the solenoid coil is operational. The pressure transducers for fuel pump discharge, oxidizer pump discharge, thrust chamber - high range, and thrust chamber - low range are excited by 10 vdc and provide an output voltage proportional to pressure. The bridge completion networks (BCN) for the fuel pump housing temperature, oxidizer pump housing temperature sensors are resistance bridges which are excited by 28 vdc and provide a millivolt signal output which is proportional to temperature. The thrust control position indicator signal conditioning unit converts the 28 vdc excitation into an ac voltage used by the position indicator. The ac output of the position indicator is then converted by this unit into a dc voltage which is proportional to thrust control position. The charge amplifier requires 28 vdc excitation and provides output signals which are proportional to vibration measured at the gearbox.

The equipment on the engine, outside the instrumentation enclosure, obtain excitation from the vehicle and provide output signals to the vehicle through the command and instrumentation harnesses. These harnesses are connected to the instrumentation enclosure which routes the circuits to the interface connectors. The primary and secondary ignition exciters are activated by 28 vdc, 2.5 amp command signals. Each time an exciter activates an igniter, it also provides a 6 vdc output signal. The two 6 vdc output signals are connected in parallel and provided to the vehicle for monitoring normal exciter operation.

The two nozzle actuation motors require a 28 vdc, 3.2 amp command signal. Since the motors are redundant in operation, two sets of lead wires are routed to the interface connector where they are connected in parallel. Two nozzle position switches (nozzle extended and nozzle retracted) provide a contact closure when the nozzle is fully extended or fully retracted. Switch closure does not occur at intermediate nozzle positions. The three temperature sensors (fuel pump housing, oxidizer pump housing, and thrust chamber coolant) are platinum, resistance thermometer types, and are connected to their respective bridge networks using the 3-wire scheme to negate the effect of lead wire resistance.

The oxidizer pump rotational speed unit is a magnetic pickup device that senses the passing blades of the oxidizer pump. Its output frequency is proportional to blade passing speed. The thrust control position indicator is a linear variable differential transformer (LVDT) with a movable core that is connected to thrust control. With an ac excitation from the signal conditioning unit, the LVDT provides ac output proportional to changes in the thrust control. This ac output is then converted to an ac signal by the signal conditioning unit.

The gearbox housing accelerometer is a vibration sensor located on the gearbox and it provides an output to the change amplifier for conditioning.

4.1.6 Engine Installation

The RL10 derivative engines are designed for maximum interchangeability with each other to ease vehicle system integration. The mechanical connections between the engine and vehicle consist of the gimbal thrust mount and actuator attachment points, propellant inlets, pneumatic and electrical supply connections, and tank pressurization flow supply points. Mechanical interfaces for this engine are as shown in the installation drawing representation in figure C-120. Installation dimensions are shown in table C-25. Figure C-121 depicts dimensional references.

In order to clear the retractable nozzle, all components and tubes of the powerhead are packaged within a 20-in. radius of the engine centerline. The envelope of this engine extends forward of the engine/vehicle thrust mount interface by 8 inches. This space was required for the extendible nozzle jackshafts and their support structure and the movable portion of the quick disconnect nozzle feed.

The following is a description of individual vehicle/engine interfaces:

(a) Gimbal Thrust Mount

The gimbal thrust mount is a mechanical connection at the forward end of the engine which provides a universal bearing system to allow gimbaling of the engine for thrust vectoring. The gimbal attachment in figure C-122 consists of an aluminum pedestal with four bolt holes and a conical mount fastened to the engine by six bolts that pass through the bottom and thread into the propellant injector.

The gimbaling action is accomplished by virtue of a steel spider that connects the pedestal to the conical mount. The spider pins are coated with a dry lubricant and pivot in copper-beryllium inserts. These parts permit a gimbal movement of ±4 deg in a square pattern.

(b) Actuator Attachment

The actuator attachments are located on the primary nozzle coolant inlet manifold. The two attachments located at 90 deg to each other are of the clevis type. A bolt through the clevis and actuator provides rotation for gimbaling and absorbs inplane thrust loads. The position of these two clevises is the same as that of the RL10A-3-3 engine for interchangeability convenience.

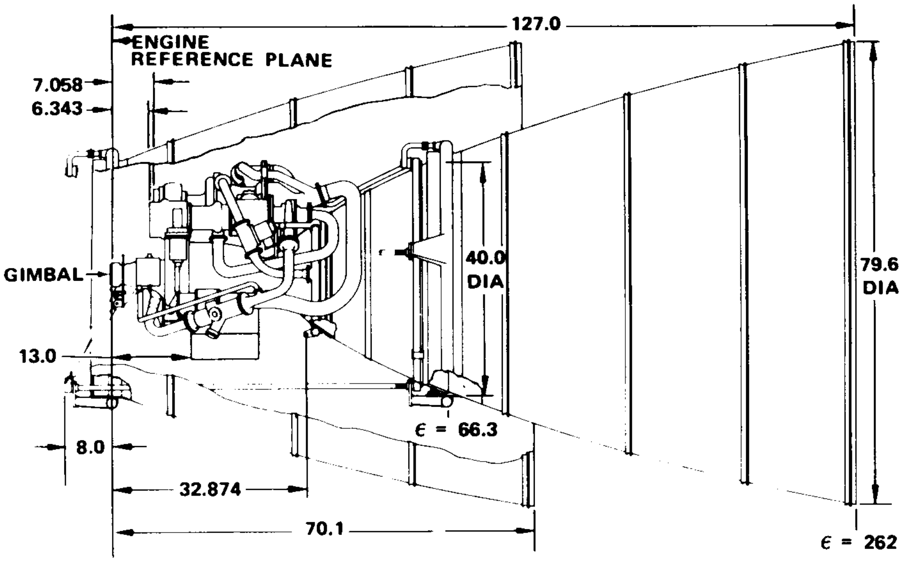

127.0
ENGINE REFERENCE PLANE
7.058
6.343
GIMBAL
13.0
8.0
32.874
70.1
40.0 DIA
ϵ = 66.3
79.6 DIA
ϵ = 262

Figure C-120(1). Derivative IIA Engine Installation Drawing FD 72055B

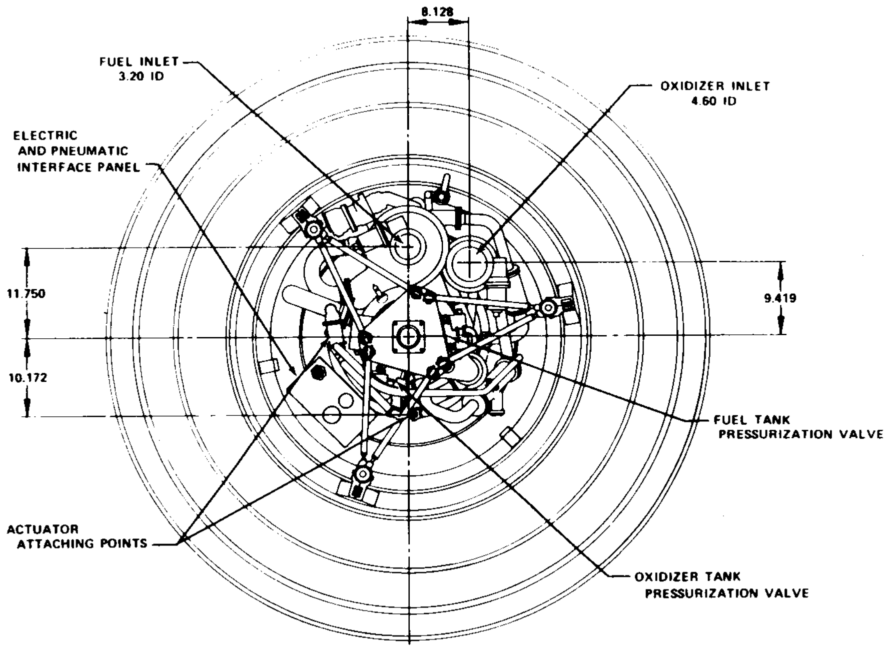

8.128
FUEL INLET 3.20 ID
OXIDIZER INLET 4.60 ID
ELECTRIC AND PNEUMATIC INTERFACE PANEL
11.750
10.172
9.419
FUEL TANK PRESSURIZATION VALVE
ACTUATOR ATTACHING POINTS
OXIDIZER TANK PRESSURIZATION VALVE

Figure C-120(2). Derivative IIA Engine Installation Drawing FD 72077B

Table C-25. Dimensional Characteristics of the Derivative IIA Engine

Dimension	Description	Dimensional Characteristic
A	Y Axis to Fuel Inlet	11.750
A1	Z Axis to Fuel Inlet	0.0
B	Radius to Actuator Attach	10.172
C	Z Axis to Oxidizer Inlet	8.128
D	Y Axis to Oxidizer Inlet	9.419
E	ERP to Oxidizer Inlet	6.343
F	ERP to Fuel Inlet	7.058
G	Interface Diameter	2.876
H	Interface Height	0.240
J	ERP to Gimbal Plane	1.500
K	ERP to Actuator Attachment Point	32.874
L	Engine Length (EXT)	127.0
	(RTC)	70.1
M	Exit Diameter	79.6

All Dimensions in Inches

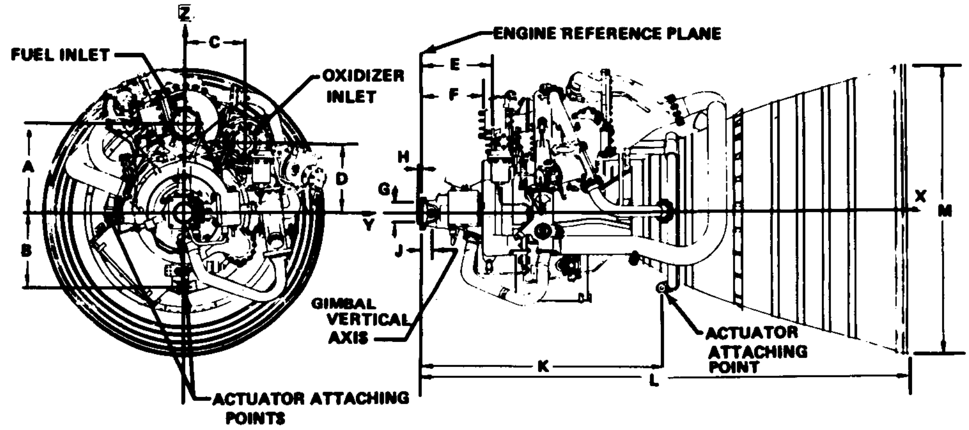

Z
C
FUEL INLET
OXIDIZER INLET
ENGINE REFERENCE PLANE
E
F
A
B
D
H
G
Y
J
X
M
GIMBAL VERTICAL AXIS
ACTUATOR ATTACHING POINT
K
L
ACTUATOR ATTACHING POINTS

Figure C-121. RL10 Outline Installation Drawing FD 68853

GIMBAL ASSEMBLY P/N 2118942 S/N ABE 759 AFTER A 2600 CYCLE TEST AT A 15,000 LB. LOAD AND A VACUUM ENVIRONMENT OF 6 X 10^{-8} TORR AND -160°F

Figure C-122. Gimbal Assembly FD 61074

(c) Propellant Inlets

The propellant inlet connections are made at the fuel and oxidizer inlet shutoff valves. These interfaces are located below the gimbal interface at the same radial location on all RL10 derivative engines. The inlet diameters for the Derivative IIA engine have been sized to supply the large 2 phase flow inducers as shown in figures C-123 and C-124. The axial locations of the inlets are slightly closer to the engine reference plane because of the larger ball diameters of the fuel and oxidizer inlet valves.

(d) Tank Pressurization Connections

The hydrogen and oxygen tank pressurant supply connections are located at the forward end of the engine near the engine centerline as shown in figure C-120. The connections are made directly to the respective tank pressurization valves by use of a male AN-16 fitting.

(e) Electrical-Pneumatic Interfaces

These interfaces are provided by a single interface panel on the instrumentation enclosure in figure C-125. The location of this panel is common to all derivative engines. There is one pneumatic (helium) connection and two electric connectors. The two electric connectors interface the vehicle with the engine command harness, and instrumentation harness. A detailed description of the electric interface design is given in Section 4.1.5.

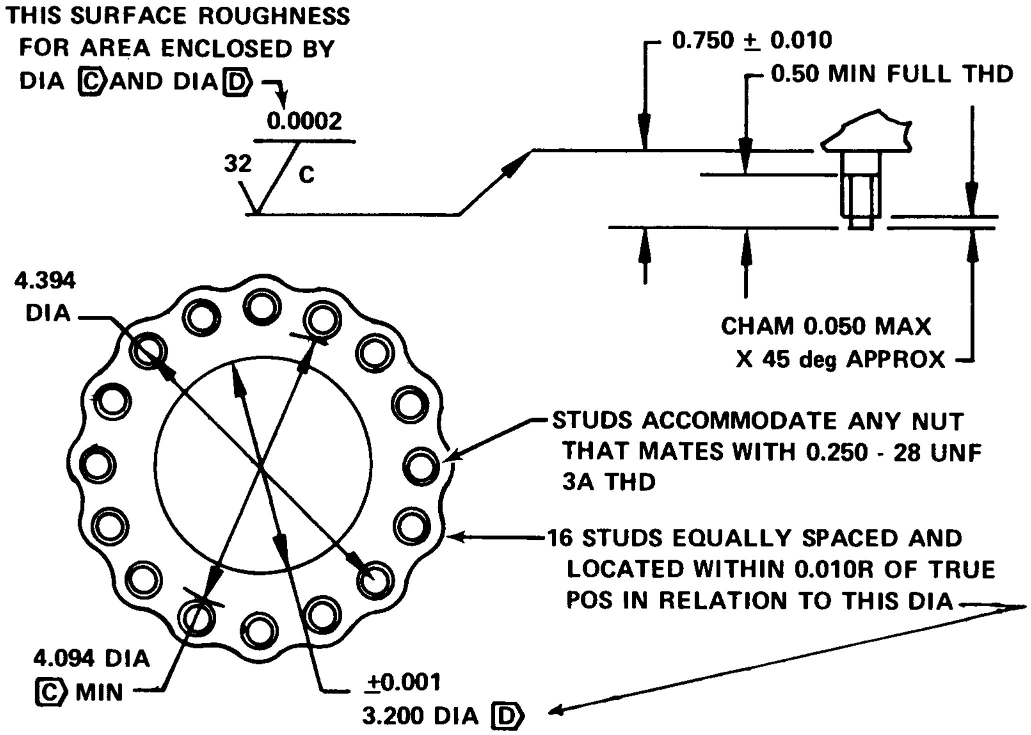

THIS SURFACE ROUGHNESS FOR AREA ENCLOSED BY DIA C AND DIA D
0.0002
32
C
0.750 ± 0.010
0.50 MIN FULL THD
CHAM 0.050 MAX X 45 deg APPROX
4.394 DIA
STUDS ACCOMMODATE ANY NUT THAT MATES WITH 0.250 - 28 UNF 3A THD
16 STUDS EQUALLY SPACED AND LOCATED WITHIN 0.010R OF TRUE POS IN RELATION TO THIS DIA
4.094 DIA C MIN
±0.001
3.200 DIA D

Figure C-123. Engine Fuel Inlet, Derivative IIA FD 75258

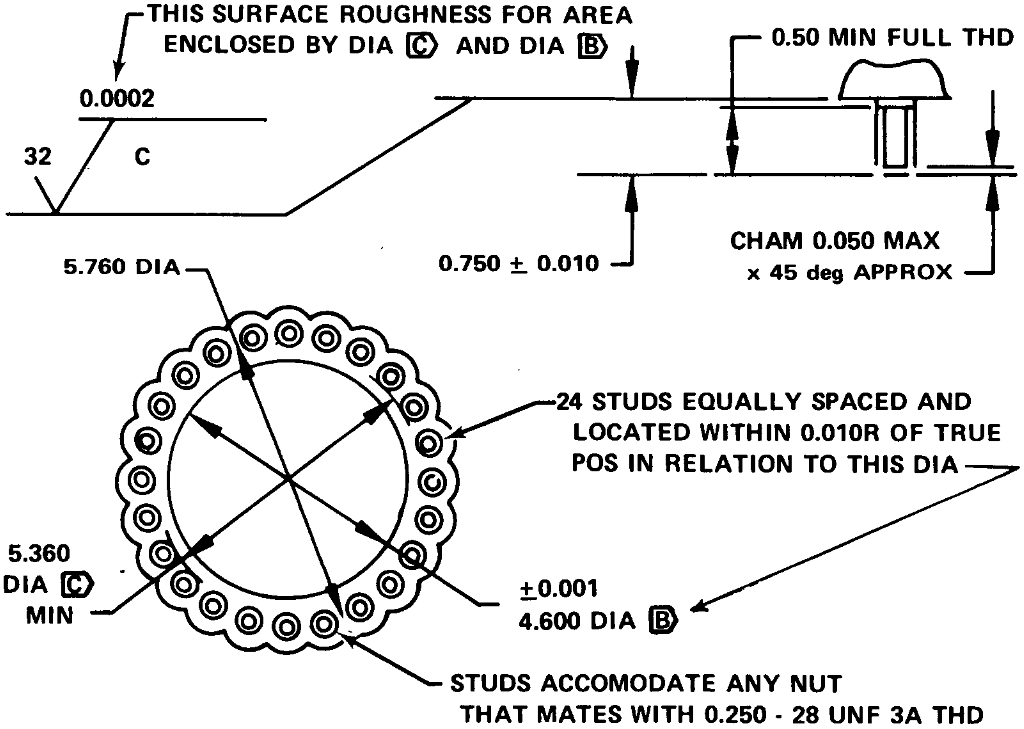

THIS SURFACE ROUGHNESS FOR AREA ENCLOSED BY DIA C AND DIA B
0.0002
32
C
0.50 MIN FULL THD
CHAM 0.050 MAX x 45 deg APPROX
0.750 ± 0.010
5.760 DIA
24 STUDS EQUALLY SPACED AND LOCATED WITHIN 0.010R OF TRUE POS IN RELATION TO THIS DIA
5.360 DIA C MIN
±0.001
4.600 DIA B
STUDS ACCOMODATE ANY NUT THAT MATES WITH 0.250 - 28 UNF 3A THD

Figure C-124. Engine Oxidizer Inlet, Derivative IIA and Category IV Engines FD 75259

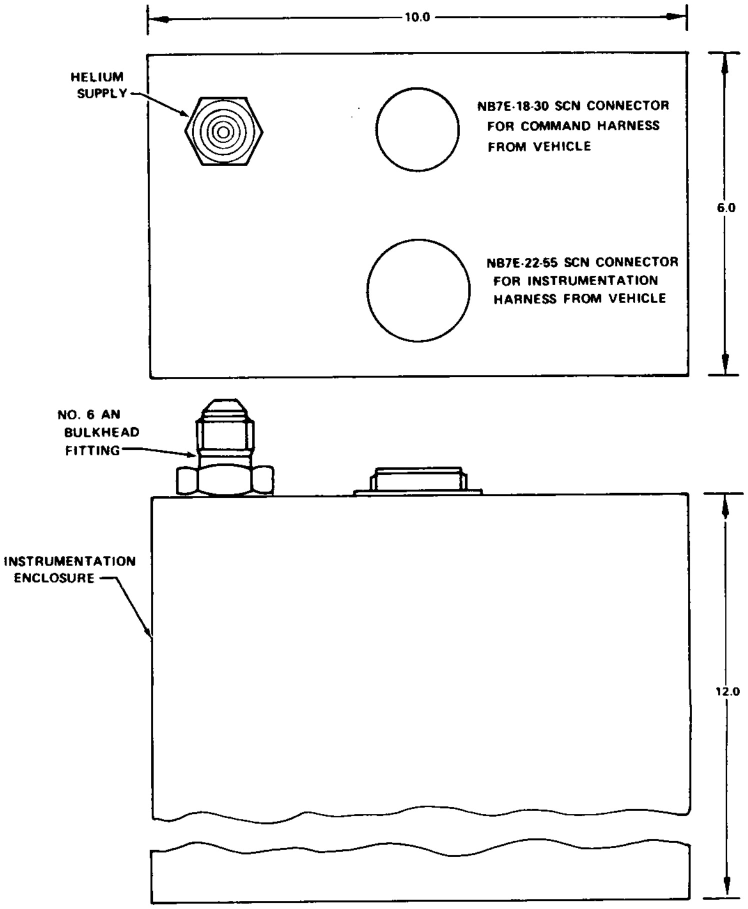

10.0
HELIUM SUPPLY
NB7E-18-30 SCN CONNECTOR FOR COMMAND HARNESS FROM VEHICLE
6.0
NB7E-22-55 SCN CONNECTOR FOR INSTRUMENTATION HARNESS FROM VEHICLE
NO. 6 AN BULKHEAD FITTING
INSTRUMENTATION ENCLOSURE
12.0

Figure C-125. Pneumatic and Electrical Vehicle/Engine Interfaces FD 76011

4.1.7 Weights, Center of Gravity and Moments of Inertia

The weights shown for the Derivative IIA engine have been obtained from known RL10A-3-3 component weights, from weights calculated from conceptual design layout drawings and from estimates based on similar hardware. Items for which weight was estimated include miscellaneous hardware such as small plumbing, connectors, and brackets, and instrumentation. The weight breakdown of the Derivative IIA engine is shown in table C-26 by detail, subassembly and total engine. Sources for the weight are also listed, i.e., estimated, computed or weighed. The total engine weight shown does not include TVC actuators, propellant utilization actuator, accessory drive equipment, or nonpropulsive vents.

Table C-26. Derivative IIA Weight Breakdown

Component		Estimated, Computed, or Weighed	Engine Weight, lb
Thrust Chamber and Primary Nozzle			
Injector Assembly		Weighed	14.9
Thrust Chamber and Nozzle/Primary		31% Weighed 69% Computed	83.0
Support Ring and Seal		Computed	12.0
	Subtotal		109.9
Extendible Nozzle			
Extendible Nozzle		Computed	104.0
	Subtotal		104.0
Extendible Nozzle Actuator System			
Extendible Nozzle Actuation System		Computed	43.0
	Subtotal		43.0
Turbopumps and Gearbox			
Turbopump		15% Computed, 85% Weighed	95.0
Turbopump Mounts		Weighed	4.0
	Subtotal		99.0
GO_2 Heat Exchanger			
GO_2 Heat Exchanger		Computed	13.0
	Subtotal		13.0
Ignition System			
Ignition System		Weighed	15.0
	Subtotal		15.0
Controls, Valves, Actuators			
Oxidizer Inlet Shutoff Valve		Computed	15.3
Fuel Inlet Shutoff Valve		Computed	12.6
Oxidizer Flow Control Valve		Computed	9.0
Gaseous Oxidizer Valve		Computed	2.0
Fuel Vent Valve		Weighed	6.3

Table C-26. Derivative IIA Weight Breakdown (Continued)

Component		Estimated, Computed, or Weighed	Engine Weight, lb
Controls, Valves, Actuators (See Note 1)			
Turbine Bypass Valve		Computed	4.2
Thrust Control Valve		Weighed	5.3
Nozzle Coolant Valve		Computed	2.0
Main Fuel Shutoff Valve		Weighed	3.4
Tank Pressurizing Valves		Computed	4.0
Solenoid Valves		Estimated	10.0
Instrumentation		Estimated	8.8
	Subtotal		82.9
Plumbing and Miscellaneous Hardware			
Tube - Oxidizer Control to Injector		Estimated	2.5
Tube - Main Oxidizer Pump Inlet		Estimated	2.0
Tube - GO_2 Hex Supply and Exit		Estimated	2.0
Tube - Fuel Pump to Vent Valve		Weighed	1.2
Tube - Vent Valve to Thrust Chamber		Weighed	1.6
Tube - Thrust Chamber to Turbine		Estimated	5.5
Tube - Turbine to Main Fuel Shutoff Valve		Estimated	8.4
Tube - Turbine Bypass		Estimated	4.0
Engine Mount		Weighed	8.0
Small Lines		Estimated	2.2
Connecting and Miscellaneous Hardware		Estimated	8.2
	Subtotal		45.6
	Total Engine Weight		512.4

Note 1: Percent Weighed - 31
Percent Computed - 58
Percent Estimated - 11

The estimated maximum dry weight and the estimated wet weight at normal operating conditions are shown below:

Basic Dry Weight, lb	Propellant Weight, lb	Total Wet Weight, lb
513	14	527

Engine center of gravity was determined by the moment method using the weights of table C-26 and the moment arms known, or modified, from the RL10A-3-3. New components had moment arms determined by calculation, or for small parts, estimation.

Axial (X), Nozzle Retracted	Axial (X), Nozzle Extended	Horizontal (Y)	Vertical (Z)
25.6 in.	37.6 in.	1.5 in.	2.8 in.

The gimbaled moment of inertia for the Derivative IIA engine was determined from the summation of the products of the component mass times its cg distance to the gimbal center of rotation squared. While this method ignores the individual component mass moment of inertia, the preliminary nature of the study justifies its use. The estimated gimbaled moments of inertia follow and are in units of in.-lb-sec^2.

I_{xx}	I_{yy}	I_{zz}
505	3610	3585

4.2. Derivative IIB Engine

4.2.1 Main Chamber and Primary Nozzle

The primary function of the thrust chamber and primary nozzle is to provide a converging/diverging section for the combustion and expulsion of propellants at high velocity to produce thrust. It also serves as a heat exchanger to supply turbine power for the propellant pumps and as a support for the extendible nozzle and actuation system. In the main chamber of the Derivative II engine, propellants are burned at a nominal mixture ratio of 6.36 to provide a chamber pressure of 400 psi at full thrust. Because the main chamber/nozzle operates at conditions very similar to that of the RL10A-3-3 engine the design has been based upon concepts proven during RL10 operation. The requirements for the Derivative II engine chamber/nozzle are listed in table C-27.

Table C-27. Derivative IIB Chamber/Nozzle Requirements

Parameter	Tank Head Idle	Maneuver Thrust	Full Thrust
Injector			
Fuel Inlet Pressure (psia)	6.8	125.	469.
Oxidizer Inlet Pressure (psia)	14.5	106.	447.
Fuel ΔP (psi)	1.6	22.5	68.6
Oxidizer ΔP (psi)	9.3	3.2	46.7
Fuel Inlet Temperature (°R)	426.	612.	451.
Oxidizer Inlet Temperature (°R)	385.	165.	167.
Combustion Chamber and Primary Nozzle			
Chamber Pressure (psia)	5.2	102.	400.
Chamber Mixture Ratio	4.32	6.58	6.36
Coolant Tube Inlet Pressure (psia)	15.9	205.	812.
Coolant Tube Inlet Temperature (°R)	36.8	45.	55.4
Coolant Tube ΔP (psi)	5.6	51.	155.
Coolant Tube ΔT (°R)	545.	577.	418.
Mechanical Requirements			
Throat Diameter (in.)			4.87
Primary Nozzle Exit ID (in.)			40.0
Primary Nozzle Area Ratio			66.
Primary Nozzle Length (Throat to Exit) (in.)			30.9

4.2.1.1 Mechanical Description

The propellant injector is shown schematically in figure C-126. The function of the injector is to atomize the oxidizer and promote thorough mixing of the fuel and oxidizer to provide the correct conditions for efficient combustion of the propellants.

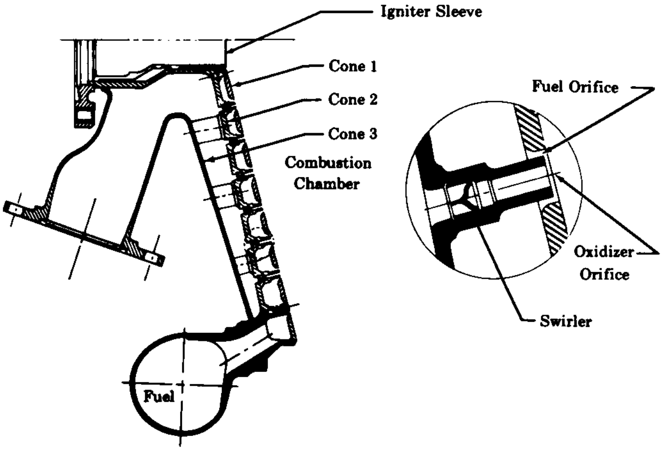


Figure C-126. Propellant Injector

FD 1554F

The propellant injector consists of 216 elements arranged in 8 equally spaced concentric circles. Each element consists of an oxidizer orifice and a concentric fuel orifice. All elements except those in the inner and outer rows incorporate swirlers which aid in the dispersion of the oxidizer.

Liquid oxygen enters the injector through the oxidizer injector manifold, flows into the cavity between cones 2 and 3, and then flows out of the oxidizer orifices and into the combustion chamber.

Gaseous hydrogen enters the peripheral fuel injector manifold and flows into the cavity between cones 1 and 2. The hydrogen flows out through the annular orifices around the elements, into the combustion chamber where it mixes with the oxidizer. Hydrogen also passes through cone 1, which consists of a porous-welded, steel mesh plate. This flow provides transpiration cooling of the injector face (cone 1) and amounts to approximately 10% of the total hydrogen flow.

Immediate contact between oxidizer and fuel is made at each element as the oxidizer and fuel leave the face of the injector and enter the combustion chamber.

The thrust chamber shown schematically in figure C-127 is a regeneratively cooled, furnace brazed assembly consisting of a fuel inlet manifold; 180 short, single-tapered tubes; a turnaround manifold; 180 full length, double-tapered tubes; a fuel exit manifold; and various stiffeners and component supports.

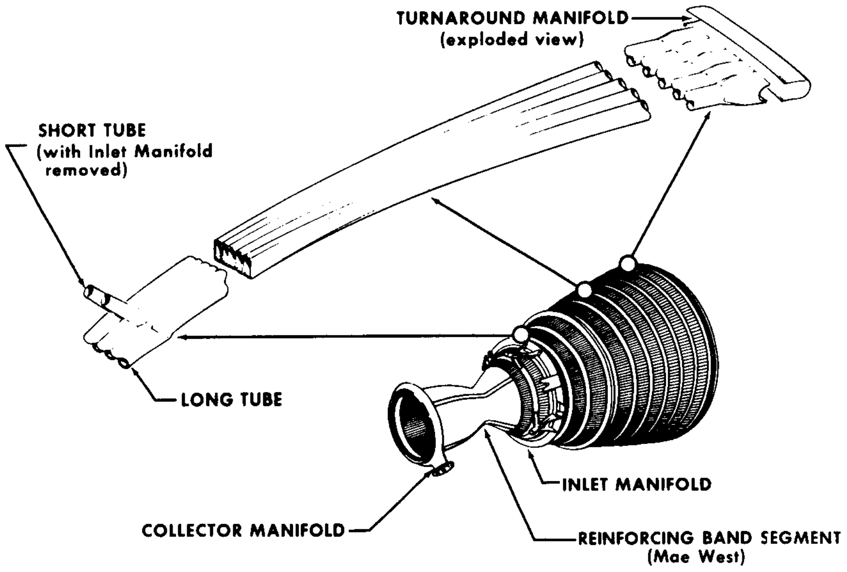


Figure C-127. Thrust Chamber Construction FD 4269

In this chamber design, some of the heat released during combustion is transferred to the chamber coolant flowing in the tubes, and is used to provide energy for driving the turbopump.

Hydrogen enters the thrust chamber at the inlet manifold downstream of the throat, and immediately flows into 180 single-tapered short tubes that are interleaved between 180 double-tapered, full length tubes. The full length tubes form the full periphery of the combustion chamber, the throat, and the nozzle down to the junction of the short tubes. The hydrogen flows rearward in the short tubes to the turnaround manifold where it enters the 180 full length tubes and then travels forward the entire length of the chamber to the exit manifold. This partial two-pass method of chamber construction was adopted to achieve high coolant velocity and heat transfer, and low tube-wall temperature.

Both full-length and short tubes are brazed together to form a seal, and are structurally supported by stiffener bands to carry the chamber hoop loads. These bands also minimize the effect of any flow-induced vibration.

A torch igniter system provides thrust chamber ignition. This system, shown in figure C-128, provides a metered flow of hydrogen and oxygen, mixed in in an igniter chamber, ignited by a spark, and passed into the combustion chamber to ignite the main propellants. Increased reliability is accomplished by providing dual exciters and spark igniters and by eliminating the need for igniter propellant shutoff valves. The dual spark and exciter configuration provides a fail safe energy source and designing the igniter to operate at rated thrust with oxidizer and fuel igniter flows eliminates the possibility of igniter damage due to valve leakage.

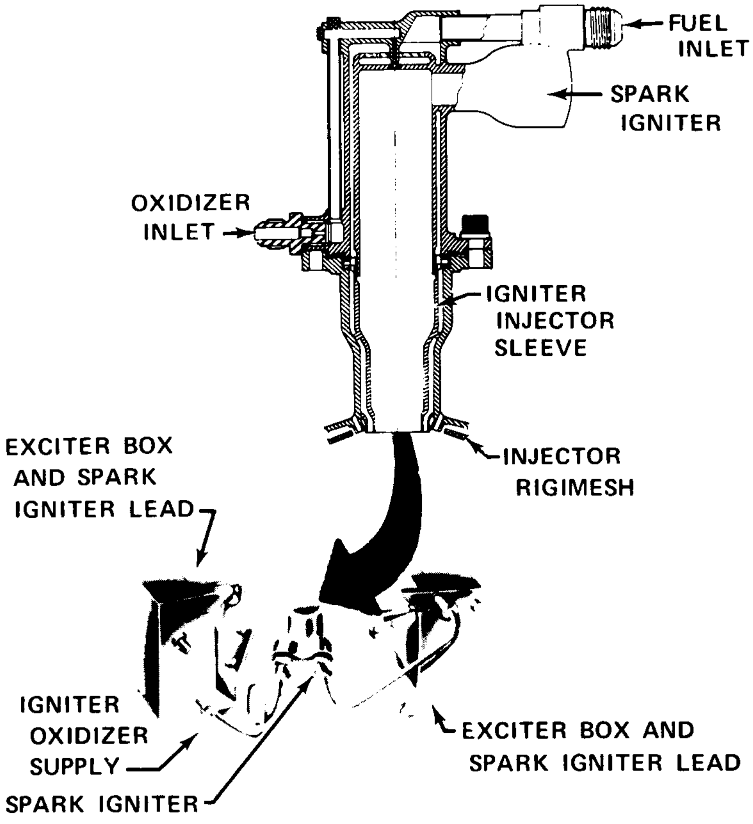

FUEL INLET
SPARK IGNITER
OXIDIZER INLET
IGNITER INJECTOR SLEEVE
INJECTOR RIGIMESH
EXCITER BOX AND SPARK IGNITER LEAD
IGNITER OXIDIZER SUPPLY
SPARK IGNITER
EXCITER BOX AND SPARK IGNITER LEAD

Figure C-128. Dual Ignition System FD 47508

The fuel and oxidizer is ignited by a spark exciter assembly which provides a minimum of 20 sparks/sec at an energy level of 0.5 joules. The total oxidizer flow is injected into the igniter through a single oxidizer element located in the upper end of the igniter chamber. Fuel flow is split. Part of the flow is delivered to a concentric slot surrounding the oxidizer injector element and the remainder is used for igniter barrel cooling. The burned propellants are discharged into the main chamber through the igniter injector sleeve.

This igniter configuration was standard equipment on the RL10A-3-7 engine and has been tested extensively under conditions from Tank Head Idle start to full thrust. Ignition studies of Derivative engines are reported in Section E and a history of RL10 igniter experience may be found in Appendix I.

4.2.1.2 Design Studies

The nozzle contour design is based on a method of characteristic solution for ideal expansion that minimizes the formation of strong shock waves. The nozzle is shorter than the ideal length to optimize performance within a given length.

Because the predicted operating environment within the chamber/nozzle of the Derivative II engine is nearly identical to the RL10A-3-3 engine, many of the concepts shown in the design layout drawing shown in figure C-129 were obtained directly from the RL10A-3-3 design. Certain areas required an original design effort, however, to provide features unique to Derivative II engine requirements. These special requirements include designing for:

a. Heat loads in tank head idle

b. Sufficient heat pickup at full thrust with shortened nozzle length

c. Increased mixture ratio operation

d. Extendible nozzle load support.

Tank head idle testing of the RL10A-3-7 engine in 1967 produced hot sidewall temperatures near the injector face of sufficient magnitude to debraze the chamber tubes as they enter the exit manifold at a location shown in figure C-130. An experimental design produced at that time eliminated the problem by placing the braze joint inside the manifold and away from the hot wall as shown in figure C-131. This configuration known as the "hooked tube design" was successfully tested during subsequent development of the RL10A-3-7 engine. This design feature has been included in the design concept of the Derivative II engine to provide additional design margin during tank head idle operation.

While the predicted heat flux levels for the Derivative II engine thrust chamber and nozzle, as shown in figure C-132, are essentially the same as that encountered by the production RL10A-3-3, the primary thrust nozzle length of the Derivative II engine is shorter by 15.3 in. (See 3.1.3). This decrease in nozzle surface area would result in a reduction in heat pickup of about 700 Btu/sec during full thrust operation. Since this loss in heat would cause a 40 deg decrease in available turbine inlet temperature, it represented an unacceptable loss in turbine power.

To recover the heat needed for turbine power, a chamber length was selected which was 2.25 in. longer than that of the RL10A-3-3 chamber. This length provided the additional heat necessary to balance the Derivative II engine cycle with the required excess power as shown in figure C-133.

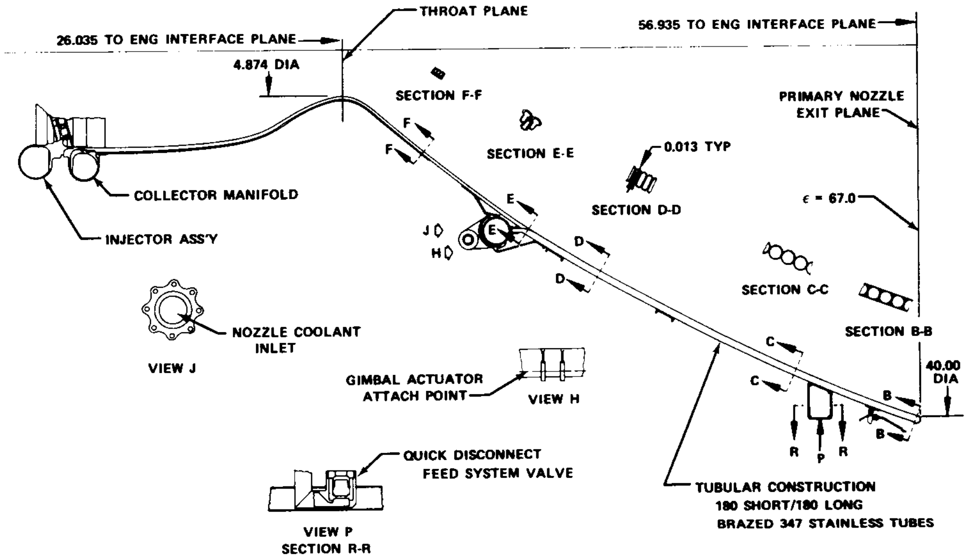

THROAT PLANE
26.035 TO ENG INTERFACE PLANE
56.935 TO ENG INTERFACE PLANE
4.874 DIA
SECTION F-F
PRIMARY NOZZLE EXIT PLANE
F
F
SECTION E-E
0.013 TYP
COLLECTOR MANIFOLD
E
SECTION D-D
ϵ = 67.0
INJECTOR ASS'Y
J
H
E
D
D
SECTION C-C
NOZZLE COOLANT INLET
SECTION B-B
VIEW J
C
40.00 DIA
GIMBAL ACTUATOR ATTACH POINT
VIEW H
C
B
B
R
P
R
QUICK DISCONNECT FEED SYSTEM VALVE
TUBULAR CONSTRUCTION
180 SHORT/180 LONG
BRAZED 347 STAINLESS TUBES
VIEW P
SECTION R-R

Figure C-129. Primary Nozzle Derivative II Engines FD 72062

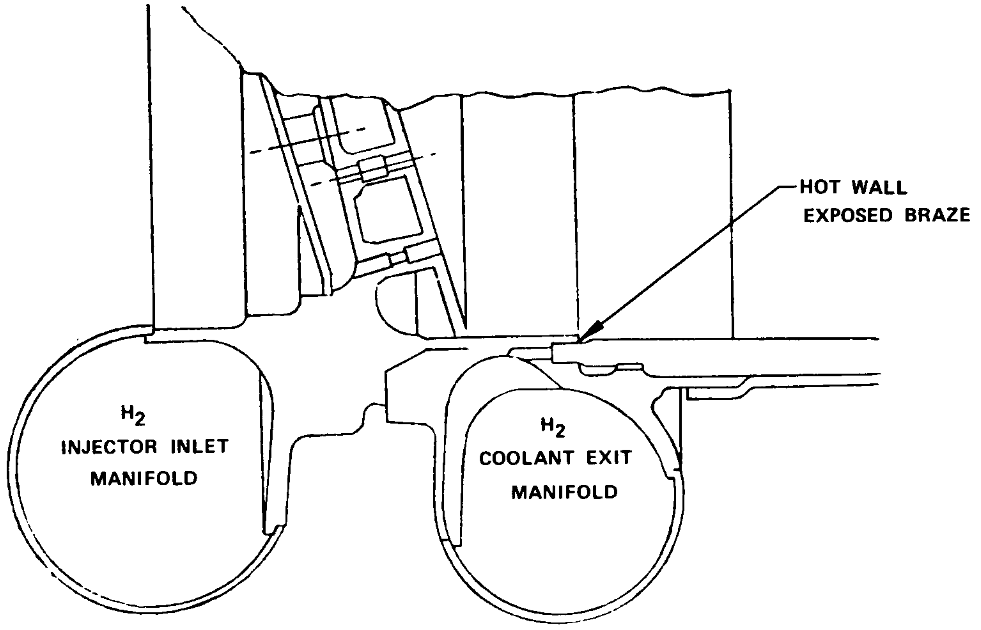

HOT WALL
EXPOSED BRAZE
H_2
INJECTOR INLET
MANIFOLD
H_2
COOLANT EXIT
MANIFOLD

Figure C-130. RL10A-3-3 Coolant Exit Manifold With Hot Wall Exposed Brazement FD 76261

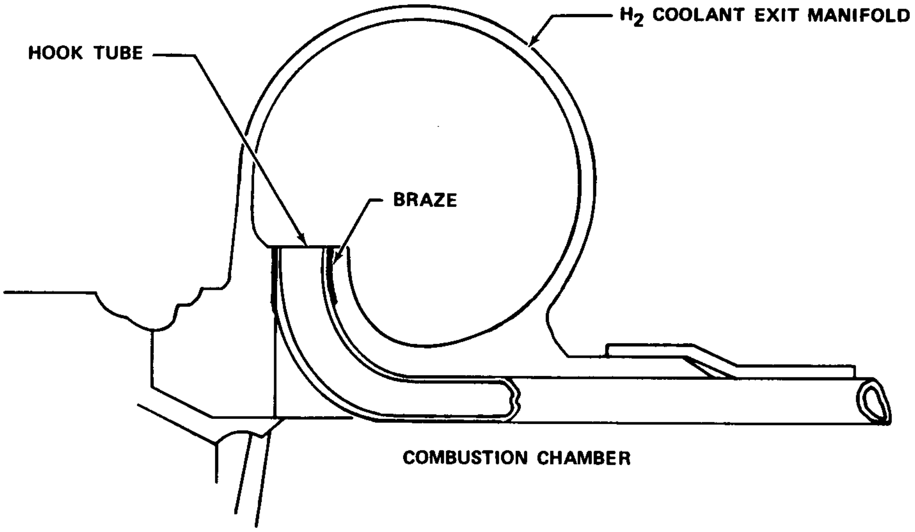

H2 COOLANT EXIT MANIFOLD
HOOK TUBE
BRAZE
COMBUSTION CHAMBER

Figure C-131. Hook-Tube Detail FD 27529A

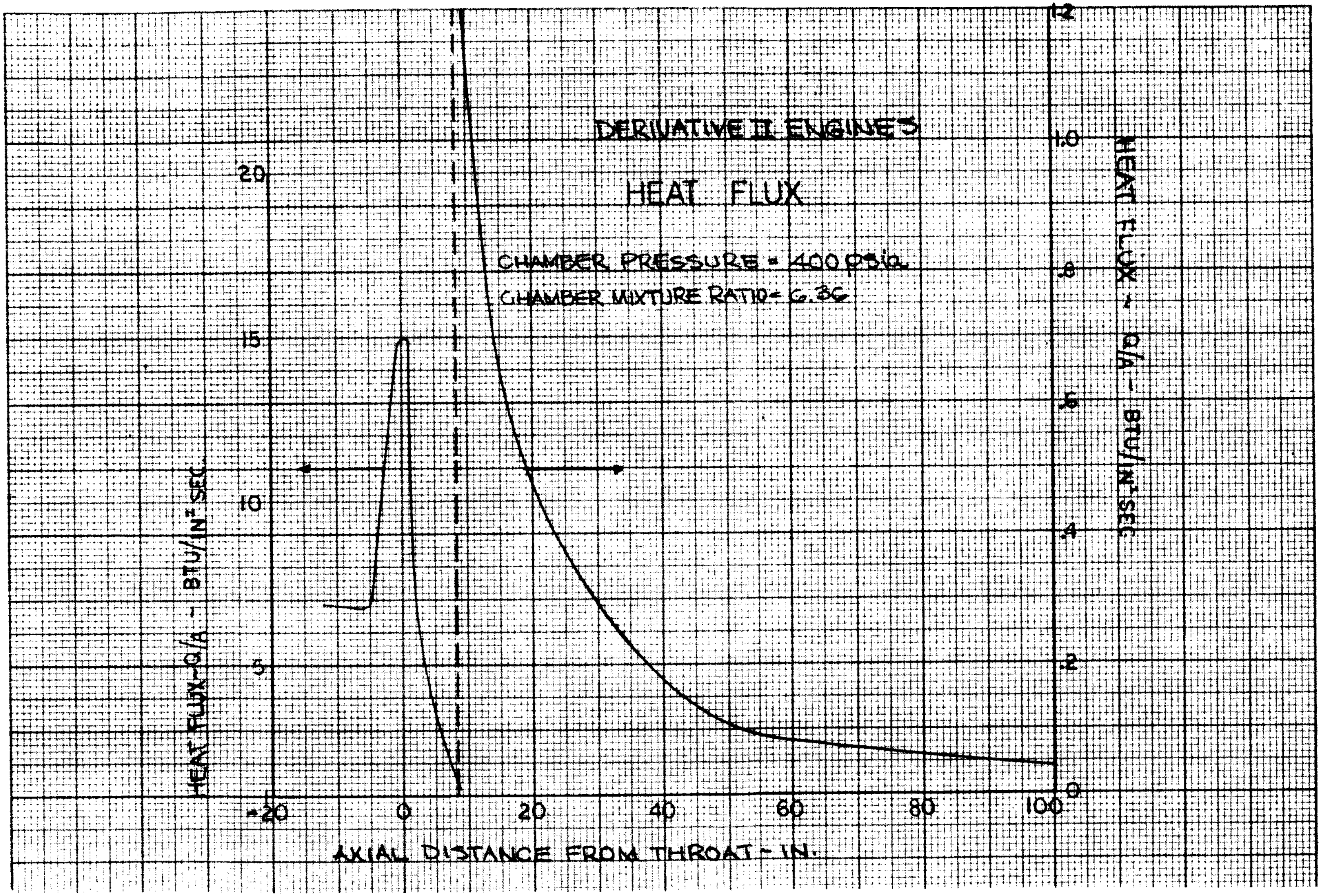


Figure C-132. Derivative II Engines Heat Fluxes

DF 98274

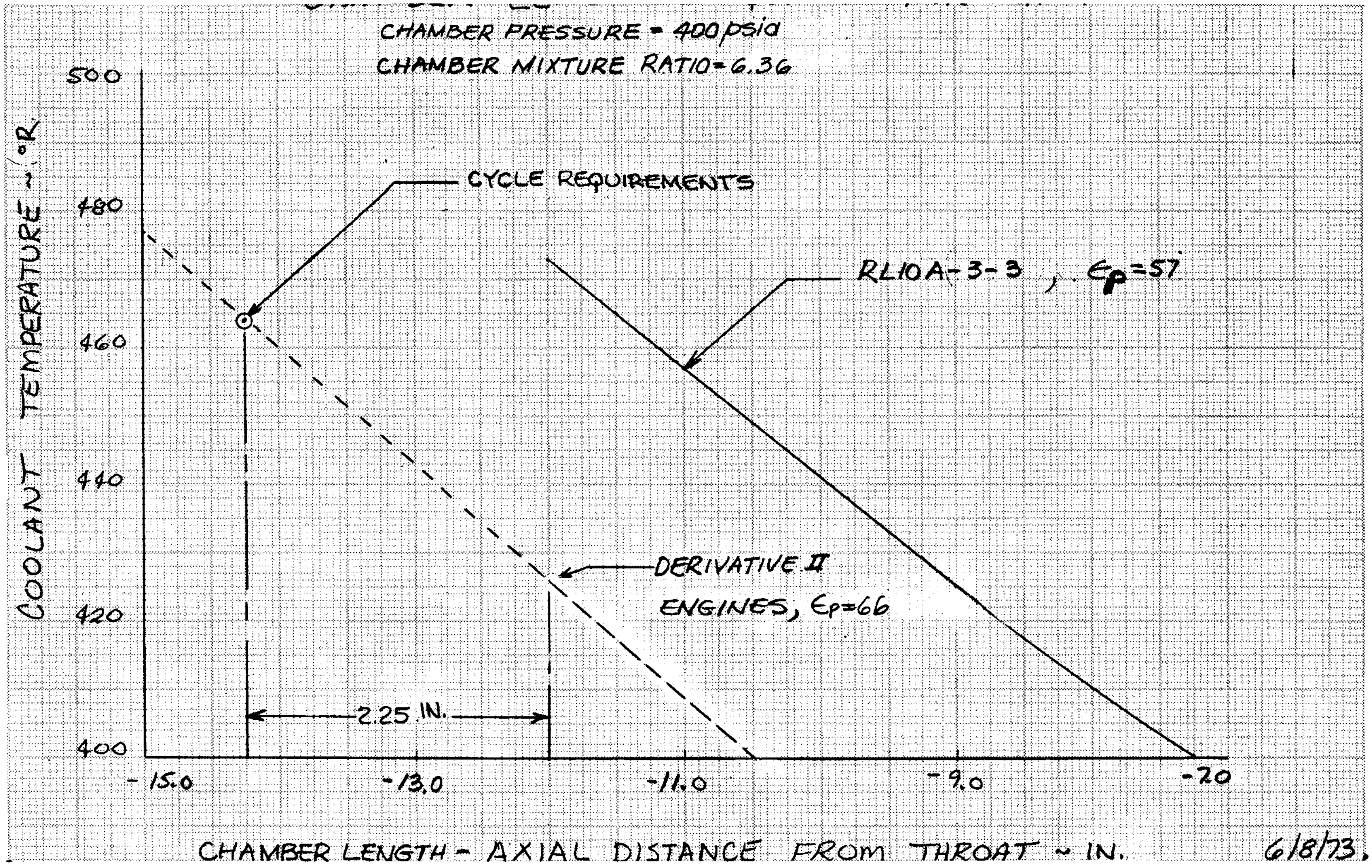


Figure C-133. Chamber Length Required for Derivative II Engines

DF 98275

The injector, as previously shown in figure C-126, will require injector element optimization in the final design to optimize specific impulse for a mixture ratio change from 5 to 6. The exact form that these changes will take will depend on the results of injector cold flow and hot firing tests to be conducted during engine development. The changes, as currently conceived, will consist of refinements in flow distribution with no basic concept changes anticipated.

The nozzle interface with the translating nozzle will have to be sealed to prevent hot gas leakage. The seal that is mounted on the aft end of the primary nozzle, as previously shown in figure C-128, is described in detail in Section 3.2.2.2. Mounting the seal will be accomplished after the nozzle brazement is complete. It will be riveted to a continuous ring flange brazed on the tubes near the exit diameter. This ring replaces a stiffness band used on the RL10A-3-3. A box ring has also been added to support the nozzle jackscrew gearboxes and drive motors. The same structure is used to hold the quick-disconnect valve used to supply hydrogen coolant to the translating portion of the thrust nozzle.

4.2.1.3 Life

The life of the Derivative IIB engine thrust chamber is predicted to be limited by coolant tube fatigue just slightly upstream of the chamber throat. RL10A-3-1 engines have demonstrated as many as 345 start cycles and the RL10A-3-3 engine has demonstrated over 130 start cycles. Six RL10 engines have been tested over 150 times without tube failure. Based upon this data the life of the Derivative II engine is predicted to be greater than 190 cycles.

4.2.1.4 Materials

The materials for the Derivative IIB thrust chamber and nozzle were selected based upon favorable RL10 experience. These materials are shown in table C-28.

Table C-28. Derivative IIB Thrust Chamber and Nozzle Materials Selection

Machined Portion	Stainless steel forging	AMS 5646
Formed Portion	Stainless steel sheet	AMS 5512
Reinforcing Bands	Stainless steel sheet	AMS 5512
Porous Injector Face	Heat-resistant alloy wire	AMS 5794
Brackets	Stainless steel sheet	AMS 5512

4.2.1.5 Weight

The weight of the Derivative IIB chamber and nozzle was based upon a combination of calculated and weighed parts. The injector, inlet and exit manifolds are the same as RL10 components and have been weighed. The remainder of the weights have been calculated from the chamber and nozzle layout drawings. The estimated weights of major parts are shown below:

Component	Weight, lb
Injector Assembly	14.9
Thrust Chamber/Nozzle	83.0
Support Ring and Seal	12.0
Total	109.9

4.2.2 Extendible Nozzle

The primary function of the extendible nozzle is to provide increased engine specific impulse without increasing engine length. It must translate forward over the primary thrust nozzle to provide a short engine package. The extendible nozzle must be a lightweight dependable design and extend or retract in a resonable length of time, and be cooled by low pressure hydrogen from the gearbox.

4.2.2.1 Mechanical Description

The nozzle is translated by means of a jackscrew actuation system. The translating structure consists of three ball screw jackshafts which are attached on the rear end of the primary nozzle by individual drive gearboxes and bearing assemblies, and are supported at their forward end by an adjustable link, as shown in figures C-134 and C-135. The ballscrew shaft is supported on antifriction bearings at both the front and rear locations. These ball bearings take radial and axial loads and are housed in a spherical ball joint which compensates for shaft misalignment as great as two degrees.

The nozzle drive/synchronization system consists of two electric motors and three interconnecting flexible cables which transmit motor torque to three gear transmissions which drive the ballscrew shafts as shown in figure C-136. A locking mechanism at the base of one ballscrew assembly locks the nozzle in either the extended or retracted position. The lock shown in figure C-137, is a spring-loaded normally locked mechanism.

A spline is added to the cable at the lock location so that engaging the spline stops travel. The engagement is ensured by dual helical springs loading the spline locks. When electric power is provided to the drive motor, the electric solenoids within the lock are energized and movement of the solenoid rotates the cam, and depresses the cam follower and springs within the lock mechanism. This disengages the spline lock and allows drive shaft rotation. The fixed spline lock is always the first to engage and it moves with the shaft pulling the floating spline into the locked position. Rotation torque is taken out by the spline lock pivot pin. The redundant lock solenoids are actuated during nozzle translation only.

The nozzle is attached to the translating mechanism at three equally spaced points through a nozzle attachment bracket to the ballscrews. The nozzle attachment bracket consists of a split circular ring and two-piece yoke as shown in figure C-138. The ballnut gimbal attachment provides two degrees of swivel freedom to prevent transferring bending loads from the nozzle attachment bracket to the ballnut.

The extendible nozzle is dump cooled, and is formed by a smooth outer skin and a corrugated inner skin that are resistance seam welded longitudinally as shown in figures C-139 and C-140. The corrugations form coolant passages for hydrogen which enters at an inlet manifold located at an area ratio of 66 and discharges to the atmosphere after passing through exit nozzles formed by dimples in the corrugations at an overall engine nozzle area ratio of 262.

The hydrogen coolant is uniformly distributed to the dump-cooled nozzle by the inlet manifold shown in figure C-141, consisting of a circular torus machined integral with a cylinder and a cone. Holes are drilled such that the torus supplies hydrogen to the dump-cooled nozzle corrugations and also to a seal land manifold which supplies coolant to the internozzle seals.

The extendible nozzle coolant supply originates at the turbomachinery gearbox and is supplied to the inlet manifold of the extendible nozzle by a quick disconnect feed valve depicted in figure C-142. The coolant flow is supplied only after the nozzle is extended.

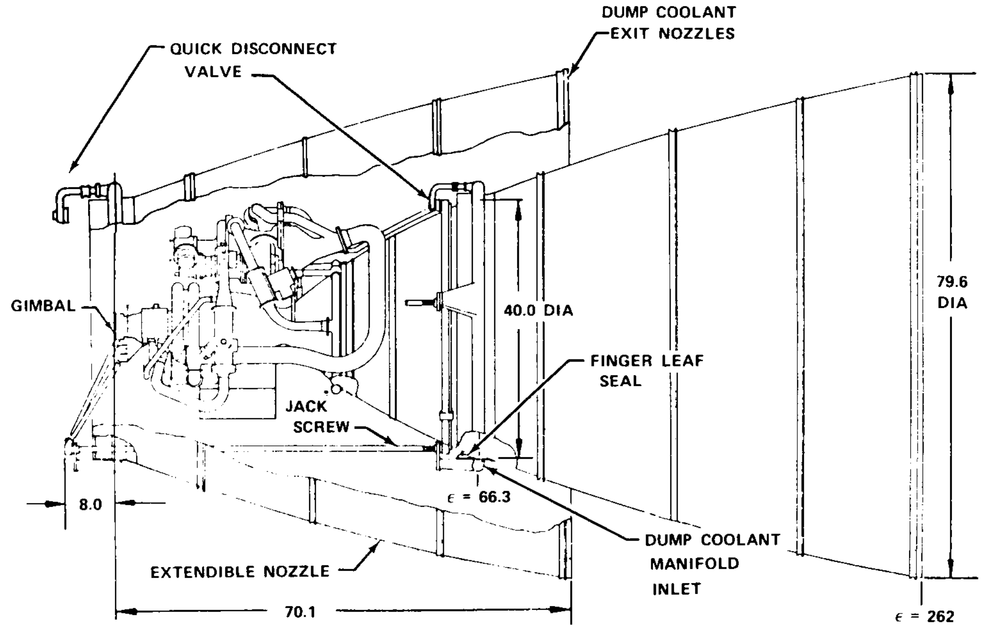


Figure C-134. Derivative IIB Engine Installation Drawing FD 72043C

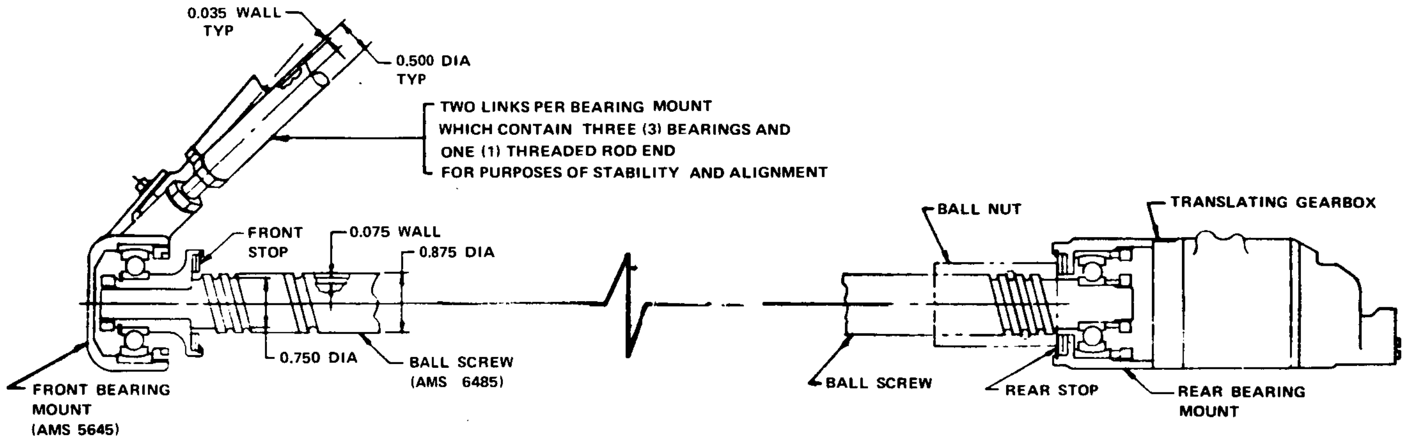

0.035 WALL
TYP
0.500 DIA
TYP
TWO LINKS PER BEARING MOUNT
WHICH CONTAIN THREE (3) BEARINGS AND
ONE (1) THREADED ROD END
FOR PURPOSES OF STABILITY AND ALIGNMENT
FRONT
STOP
0.075 WALL
0.875 DIA
0.750 DIA
BALL SCREW
(AMS 6485)
FRONT BEARING
MOUNT
(AMS 5645)
BALL NUT
TRANSLATING GEARBOX
BALL SCREW
REAR STOP
REAR BEARING
MOUNT

Figure C-135. Jackscrew Mounting Configuration

FD 76008

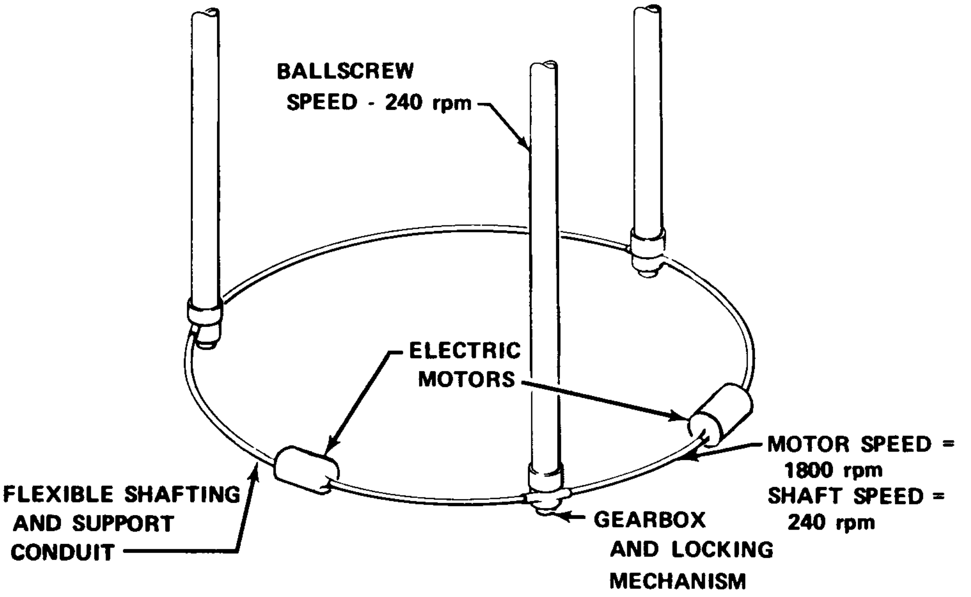

BALLSCREW SPEED - 240 rpm
ELECTRIC MOTORS
MOTOR SPEED = 1800 rpm
SHAFT SPEED = 240 rpm
FLEXIBLE SHAFTING AND SUPPORT CONDUIT
GEARBOX AND LOCKING MECHANISM

Figure C-136. Drive/Synchronization System FD 46296A

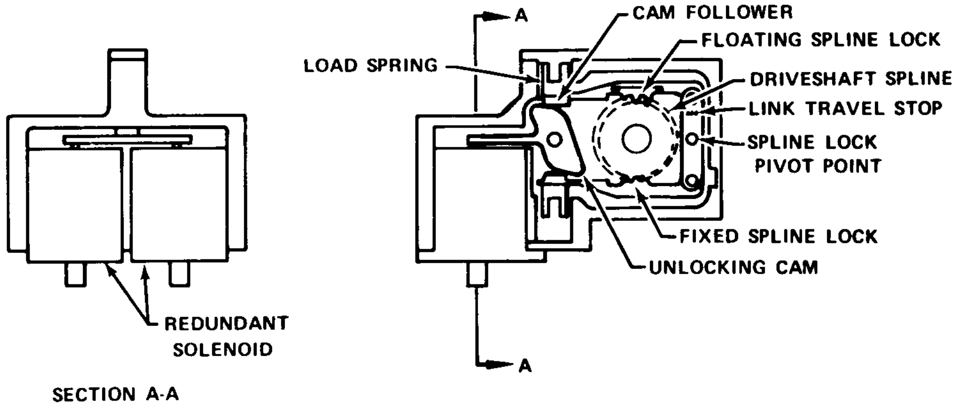

A
CAM FOLLOWER
FLOATING SPLINE LOCK
LOAD SPRING
DRIVESHAFT SPLINE
LINK TRAVEL STOP
SPLINE LOCK PIVOT POINT
FIXED SPLINE LOCK
UNLOCKING CAM
A
REDUNDANT SOLENOID
SECTION A-A

Figure C-137. Solenoid Actuated Brake for Ballscrew Actuators FD 75864

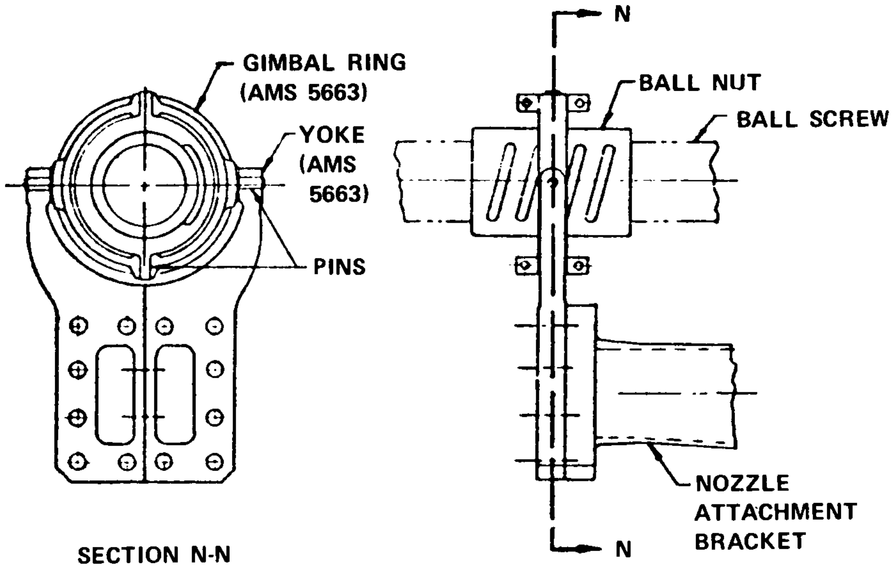

GIMBAL RING (AMS 5663)
YOKE (AMS 5663)
PINS
SECTION N-N
N
BALL NUT
BALL SCREW
NOZZLE ATTACHMENT BRACKET
N

Figure C-138. Gimbaled Ballnut

FD 76901

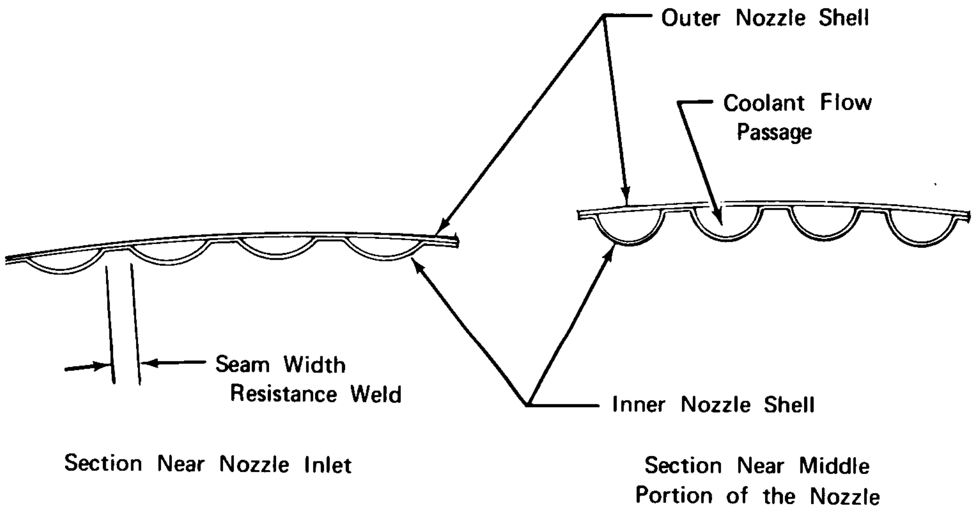
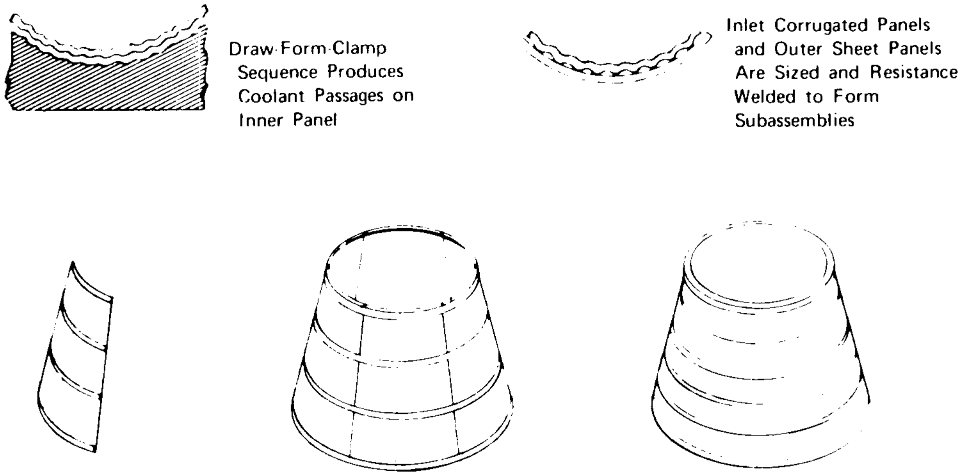

Outer Nozzle Shell
Coolant Flow Passage
Seam Width Resistance Weld
Inner Nozzle Shell
Section Near Nozzle Inlet
Section Near Middle Portion of the Nozzle

Figure C-139. Representative Corrugation Sections for Dump Cooled Nozzle

FD 51227A



Draw-Form-Clamp Sequence Produces Coolant Passages on Inner Panel

Inlet Corrugated Panels and Outer Sheet Panels Are Sized and Resistance Welded to Form Subassemblies

Subassemblies Are Butt-Welded Forming Sets of Panels

Panels are Butt-Welded to Form the Basic Nozzle. Reinforcing Bands Are Induction Brazed in Place

Coolant Manifold Is Induction Brazed Completing the Dump Nozzle

Figure C-140. Dump Cooled Nozzle Fabrication Sequence FD 51226A

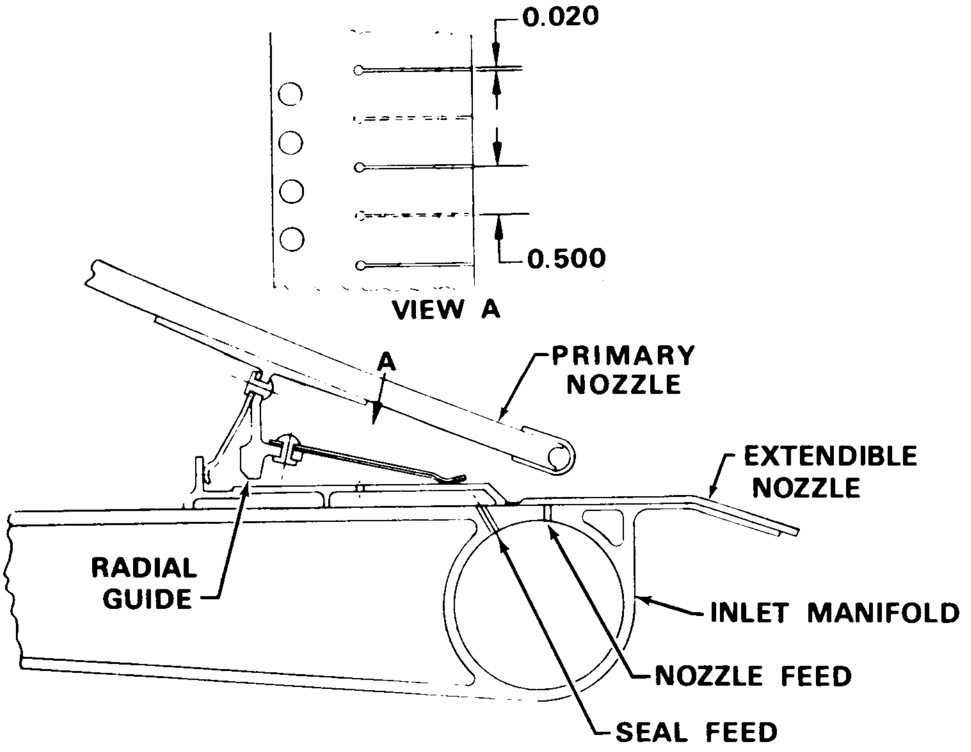

0.020
0.500
VIEW A
A
PRIMARY NOZZLE
EXTENDIBLE NOZZLE
RADIAL GUIDE
INLET MANIFOLD
NOZZLE FEED
SEAL FEED

Figure C-141. Double Finger Leaf Nozzle Seal Arrangement FD 72061A

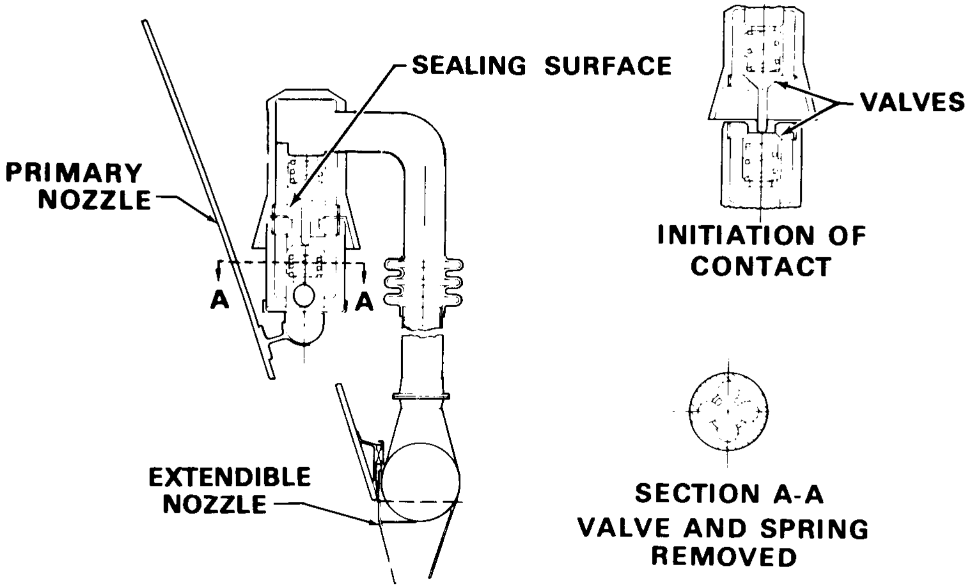


Figure C-142. Extendible Nozzle Coolant Supply FD 72060A

The quick disconnect valve is made up of two parts, one of which is attached to the primary nozzle and the other is attached to the extendible nozzle. Each portion contains a valve which is spring-loaded closed to prevent contamination of the fuel system plumbing when the engine is in the stored position and the nozzle is retracted. The normally closed valves are opened when the nozzle is translated to the extended position and the nose of one valve displaces the other. A spherical seat seal, prevents valve leakage, during operation.

Reliable engagement of the two parts is ensured by a flaired skirt with an internal ramp guide located on the valve portion attached to the extendible nozzle. A bellows section, attached to the extendible nozzle, radially deflects to aid alignment and to provide an axial loading force between the spherical seats. The slow rate of nozzle extension provides adequate time to allow the guides and bellows to compensate for misalignment and dynamic mismatches between the mating parts.

The interface between the primary nozzle and extendible nozzle is sealed by the use of finger leaf seals. Each seal consists of a two-ply sheet metal ring with radial keyhole slots in each ply. The slots in each ply are positioned to seal the slots in the mating ply as previously shown in figure C-141. Both plies are welded to a supporting ring for attachment to the primary nozzle. The assembly radially or axially seals against a land on the extendible nozzle. The seal is radially cut in one place to simplify assembly and replacement, and the two plies overlap at this location to prevent excessive leakage. The seal assembly is riveted to the primary nozzle so that it can be readily replaced during overhaul.

These seals are supplied coolant flow from a choked orifice on the extendible nozzle inlet manifold. It is sized to supply 0.002 lb/sec of hydrogen should a seal finger be bent or broken. With the seals in their nominal position leakage between the seal fingers is estimated to be 0.0002 lb/sec.

4.2.2.2 Design Studies

The corrugated dump-cooled design shown in figure C-143 has been used for the large surface area section of the nozzle because it has the following advantages over the tubular type construction: (1) lighter weight, (2) resistance welded flat sheet construction is less costly than brazed tubular construction (3) coolant passage cross section is readily varied to achieve desired coolant velocity profiles; and (4) hoop stresses are carried by the cooled outer wall. Corrugated sheet construction was demonstrated in firing tests of a modified RL10A-3-3 thrust chamber conducted in August 1967.

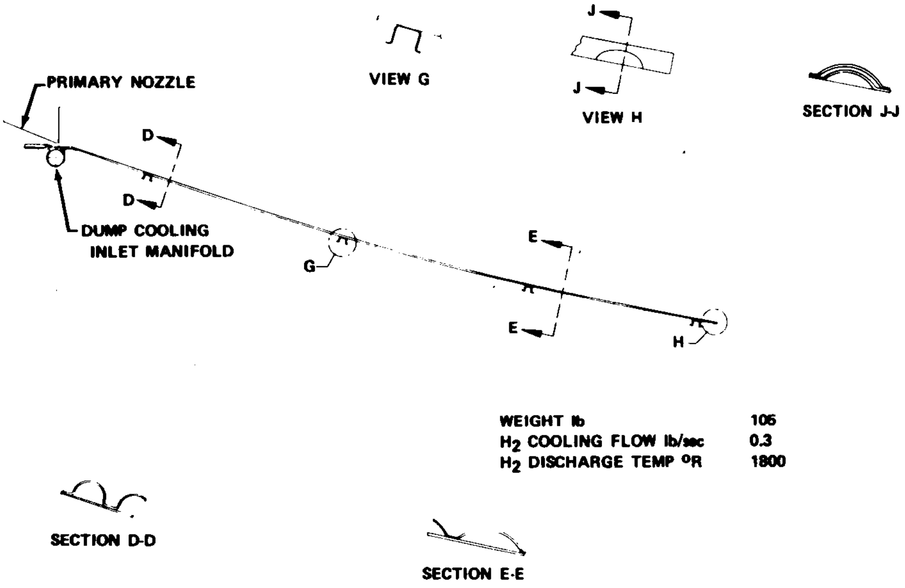

PRIMARY NOZZLE
VIEW G
J
J
VIEW H
SECTION J-J
D
D
DUMP COOLING INLET MANIFOLD
G
E
E
H
WEIGHT lb 105
H_2 COOLING FLOW lb/sec 0.3
H_2 DISCHARGE TEMP °R 1800
SECTION D-D
SECTION E-E

Figure C-143. Dump Cooled Extendible Nozzle FD 72060A

As shown in figure C-143, four inverted hot-section stiffening rings are incorporated on the dump-cooled section of the nozzle. These rings have been sized and positioned to provide support for loads from gimballing, maneuvering, vibration and nozzle pressure differential in a vacuum environment.

The ballnut and ballscrew assembly supports the extendible nozzle and is designed to accommodate all the applied loads. The maximum axial, radial, and tangential loads are induced in this structure when a 4-g sideload is applied to the nozzle in the retracted position during Space Shuttle vehicle reentry. The upper support rods, the jackshafts, the nozzle attachment brackets and the nozzle skin are all sized for compressive buckling under this loading. The actual operating and translating loads are generally symmetrical and are much more easily resisted than the design load, resulting in a structure that is inherently rigid.

Expected nozzle tilt due to the combined factors of drive cable windup, ballnut backlash, gear backlash, and bearing clearances during translating or engine operation is less than one tenth of one degree. Any two actuators are kept within 0.014-in. axial displacement because of the low windup 0.023 deg/in.-lb/in. of the synchronizing drive cable. The maximum predicted thrust alignment error is 0 deg -2 in. between the main and translating thrust nozzles.

Nozzle eccentricity and distortion will be held to a minimum by a radial guide and bumper strip arrangement shown in figure C-141 to resist sideloads. This will also act to always keep relative nozzle positions within the limits of the seal design. All axial loads are transmitted into the primary nozzle, while side loads are resisted by both the primary nozzle and the forward rod assembly.

The nozzle outer wall thickness was set at 0.010 in. minimum to facilitate manufacturing handling of large thin sheets. The 0.010 in. wall thickness starts at the front of the nozzle and tapers linearly to 0.013 in. at the exit. This increase in wall thickness is required to account for the loss in strength of the Inco 625 material as the temperature of the coolant increases to a maximum of 1900°R at the exit. The inner corrugated wall as depicted in figure C-139 was maintained constant at 0.008 in. due to the manufacturing handling problems with the large sheets. The 360 corrugated passages were varied in cross section along the dump nozzle to minimize the cooling flow required while maintaining metal temperatures at the coolant discharge (nozzle exit) below 1900°R. Predicted passage temperatures along the nozzle are given in figure C-144. Indentations near each passage exit form restrictions to choke the flow in each passage for uniform flow distribution. The nozzles formed by these indentations increase the impulse from the heated, dumped coolant.

4.2.2.3 Life

The extendible nozzle is not predicted to limit the life of the engine. The rotating members move so slowly that high wear rates are not anticipated. The ballscrew system is sized for nondynamic loads such as booster takeoff and landing, thus are oversized for the operational loads encountered while translating the nozzle.

The double finger seals at the end of the primary nozzle which stop hot gas leakage are the only nonlubricated wear component but these are sized to handle 300 cycles without failure. Should a finger seal bend such that it is not an efficient seal, hydrogen coolant from the extendible nozzle coolant inlet manifold will be directed through the leak preventing potential secondary damage due to hot gas leakage.

The dump-cooled nozzle does not limit life because the practical wall thickness of the materials used was selected heavy enough that the material yield is not realized. Only after temperatures near the end of the nozzle approached 1900°R did the stresses approach the allowable for Inco 625 material, and then wall thicknesses were increased to keep them below the allowable.

4.2.2.4 Materials

Materials for use on the Derivative IIB extendible nozzle were selected for their compatibility with hot hydrogen and their ease of manufacture. The large thin panels of the nozzle require special attention in regard to ease of forming, welding and compatibility with hydrogen.

Component	Material	Type
Thrust Nozzle	Inconel 625	AMS 5666 AMS 5599
Ballscrew Actuator	Steel	AMS 6485
Support Structure	Aluminum	TBD

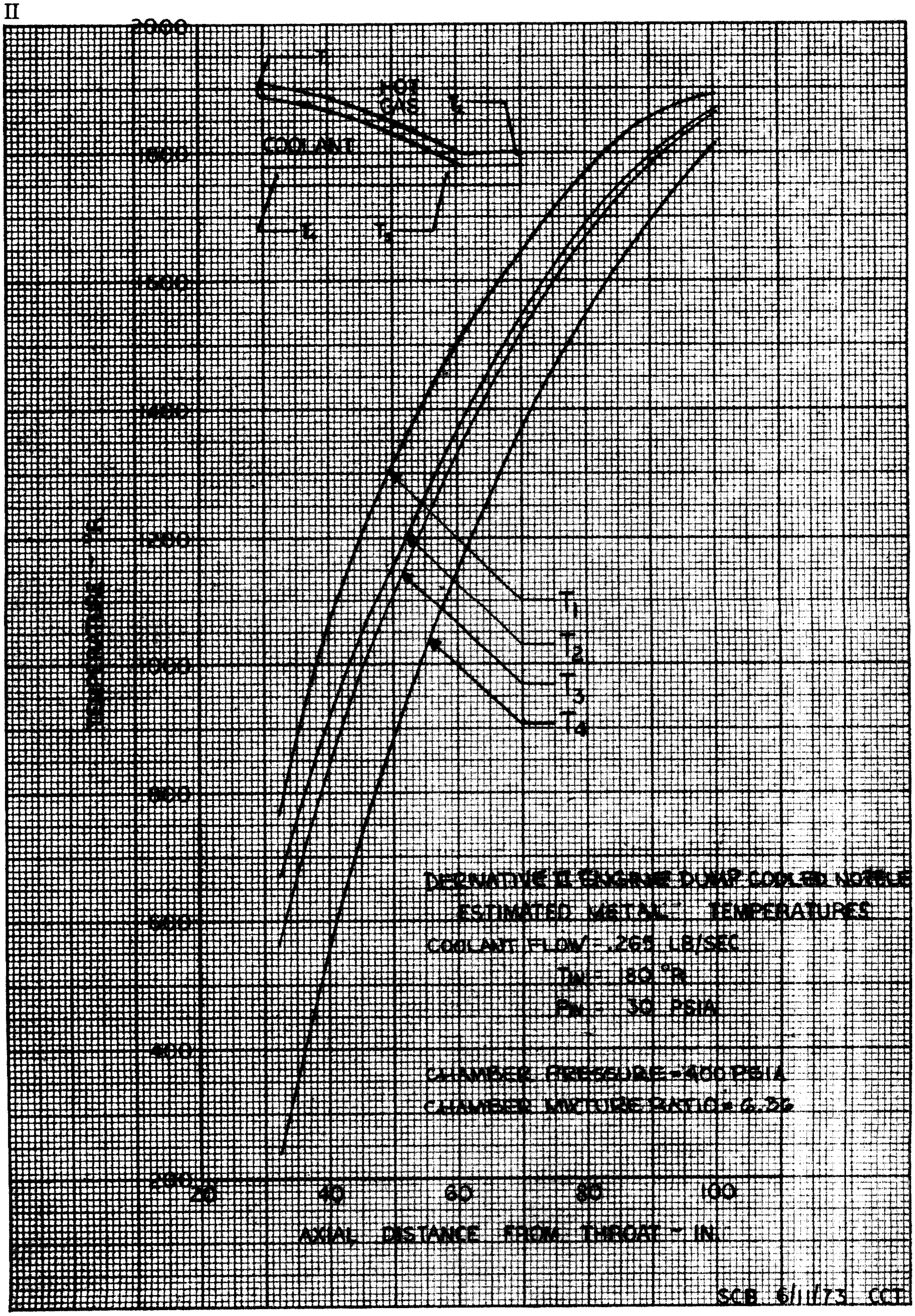


Figure C-144. Derivative II Engine Dump Cooled Nozzle Estimated Metal Temperatures DF 98278

4.2.2.5 Weight

The weight of this system is approximately 147 lb with the nozzle being 104 lb of the total and the remainder in ballscrew actuator systems and support hardware. The weight was calculated using layout drawings of the various components.

4.2.3 Turbopump

The function of turbomachinery is to supply the oxygen and hydrogen propellants to the engine combustion chamber at the required pressure level and flowrates. The turbomachinery design of the Derivative IIB engine is identical to that of the production RL10A-3-3 engine, and meets the requirements shown in table C-29 for operation at full thrust and a mixture ratio of 6.0.

Table C-29. Turbomachinery Design Requirements (Derivative IIB) at MR = 6.0

Item	Full Thrust
Oxidizer Pump	
Inlet total pressure, psia	19.7
Inlet temperature, °R	164.
Inlet density, lb/ft^3	70.9
Head rise, psi	468.
Oxidizer Pump	
Speed, rpm	11,141
Efficiency, percent	63.8
Horsepower	77.
Discharge pressure, psia	488.
Fuel Pump	
Inlet total pressure, psia	16.4
Inlet temperature, °R	36.9
Inlet density, lb/ft^3	4.42
Discharge density, lb/ft^3	4.16
Flowrate, lb/sec	4.67
Head rise, psi (1st stage/2nd stage)	409/394
Speed, rpm	27,052
Efficiency, percent (1st stage/2nd stage)	52.1/49.5
Horsepower (1st stage/2nd stage)	217/236
Discharge pressure, psia (1st stage/2nd stage)	425/819
Turbine	
Inlet total pressure, psia	651.
Inlet total temperature, °R	474.
Discharge static pressure, psia	487.
Efficiency, percent	71.1
Horsepower	530.
Turbine flow, lb/sec	3.95

4.2.3.1 Mechanical Description

The turbopump assembly illustrated in figure C-145 consists of a liquid hydrogen pump powered by a hydrogen-driven turbine mounted on a common main shaft and a liquid oxygen pump mounted beside the turbine and drive through a gear train by the hydrogen pump turbine shaft. All rotating assemblies in the turbopump assembly are mounted on unlubricated, hydrogen cooled ball and roller bearings.

a. Pumping Elements

The fuel pump is of a two-stage centrifugal configuration, with the two back-shrouded impellers mounted back-to-back to minimize thrust unbalance. Recovery of velocity head is accomplished through a straight conical diffuser connected to a volute collector. A three-bladed axial flow inducer on the same shaft is located upstream of the first-stage impeller. The inducer blades have swept-back leading edges. The large inlet diameter and inlet tip blade angle of 7 deg provides 2-phase pumping with up to 40% vapor content.

The first- and second-stage impellers incorporate respective 22-1/2 deg and 90 deg blade exit angles. This arrangement provides the optimum match between stall margin, improved with increased sweep angle, and required head rise that decreases with decreased angle. The 1st- and 2nd-stage impellers run with 0.043- and 0.086-in. nominal clearance, respectively, between blade and housing contours. They are machined from AMS 4135 aluminum alloy, which has a 0.2% yield strength of 54,000 psi at room temperature.

The fuel pump is designed with an interstage bleed as shown in figure C-146 that may be set to the area required for obtaining the desired net axial load on the thrust bearing. The bleed permits fuel flow from the 2nd-stage pump discharge to flow through an orifice with the flow area controlled by a classified metering plug into the backface cavity of the 1st-stage fuel pump impeller. Increasing this flow increases the thrust load toward the 1st-stage impeller by decreasing the pressure on the backface of the 2nd-stage impeller and increasing the pressure on the backface of the 1st-stage impeller.

The oxidizer pump is a single-stage centrifugal pump which supplies oxygen to the engine combustion chamber. The fully shrouded impeller design permits adequate clearance between impeller and housing contours to eliminate the possibility of impeller rub. Velocity head recovery is accomplished, as in the fuel pump, through a conical diffuser and volute collector. A three-bladed, axial-flow, partially shrouded inducer on the oxidizer pump shaft is located upstream of the impeller. The inducer shroud incorporates a labyrinth seal to minimize flow recirculation.

b. Turbine

The turbine provides power to drive the fuel and oxidizer pumps by utilizing the energy in the chamber/nozzle hydrogen coolant. The turbine is pressure compounded, full-admission, two-stage design with exit guide vanes to minimize discharge swirl losses.

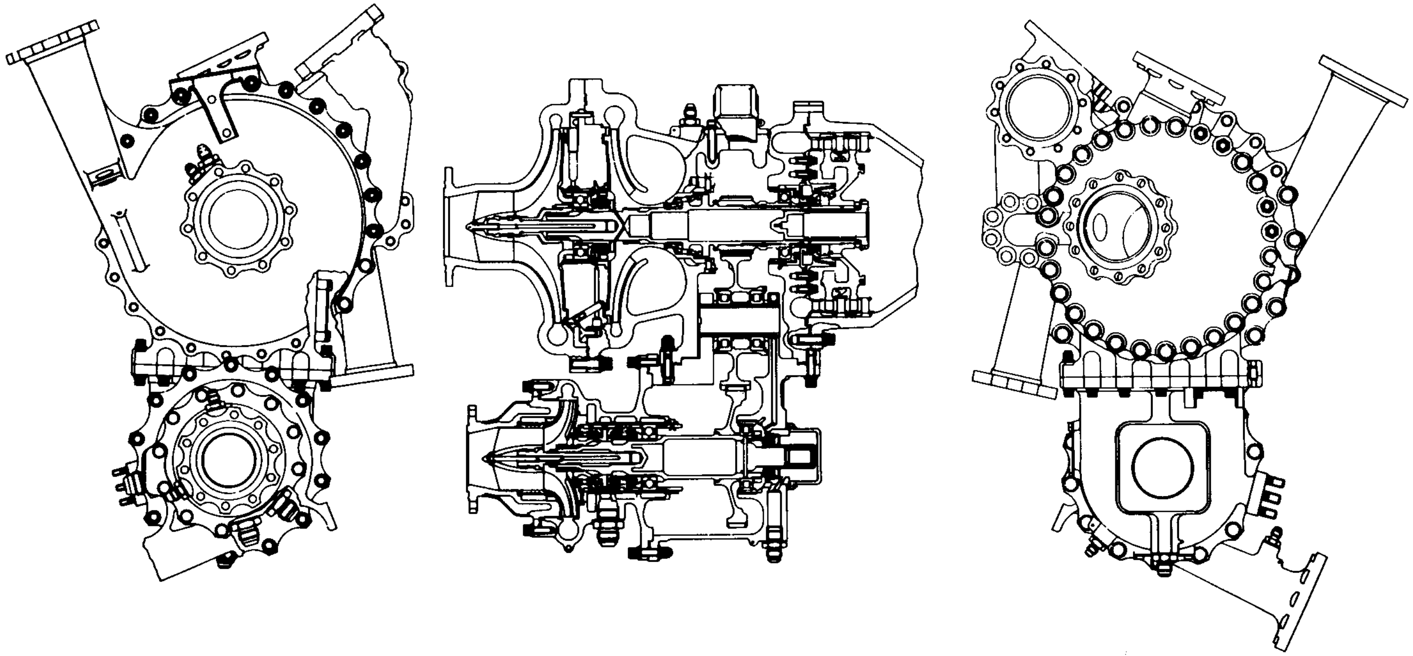


Figure C-145. Turbopump Assembly

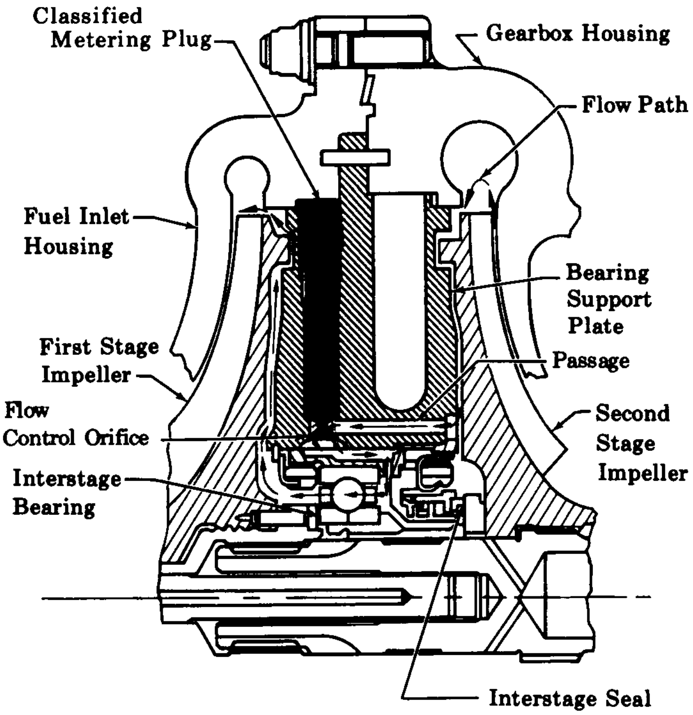

Classified Metering Plug
Gearbox Housing
Flow Path
Fuel Inlet Housing
Bearing Support Plate
First Stage Impeller
Passage
Flow Control Orifice
Second Stage Impeller
Interstage Bearing
Interstage Seal

Figure C-146. RL10A-3-3 Fuel Pump Controllable Interstage Leakage FD 18427A

Both blade stages are fully shrouded, and labyrinth seals are incorporated to minimize interstage and tip leakage. The turbine rotor with shroud is shown in figure C-147. The conical web between the back disk and bore is designed to absorb disk growth, minimize hub distortion, and prevent unbalance.

c. Gearbox

The gearbox contains all of the turbomachinery gearing as previously shown in figure C-145. It also provides for mounting of all pumps and the drive turbine. The gear train in the gearbox is cooled by spent bearing coolant discharged into the gearbox as a cold gas. This coolant flow is collected and passed through the nozzle coolant valve to cool the extendible nozzle. The main pump gear train consists of a shaft mounted pinion on the hydrogen pump shaft, a double bearing mounted idler gear and a main oxidizer pump shaft mounted gear. This train reduces turbine shaft speed 2.5 to 1 to drive the oxidizer pump. A modified AND 20,000 type accessory drive pad is also provided on the aft end of the oxidizer pump shaft. Lubrication for all gears is provided by a moly-disulfide dry film lubricant in a varnish binder.

d. Bearings

The main hydrogen drive shaft incorporates passages for hydrogen coolant flow to the turbine bearing. This coolant is bled from the 2nd-stage of the fuel pump. Liquid hydrogen coolant is supplied to the oxidizer pump thrust bearing through a drilled passage in the fuel pump housing. The coolant flow through this passage is supplied from the 1st-stage pump contour. All other hydrogen cooled bearings in the turbopump are cooled by conduction and low pressure hydrogen flow through the gearbox cavity.

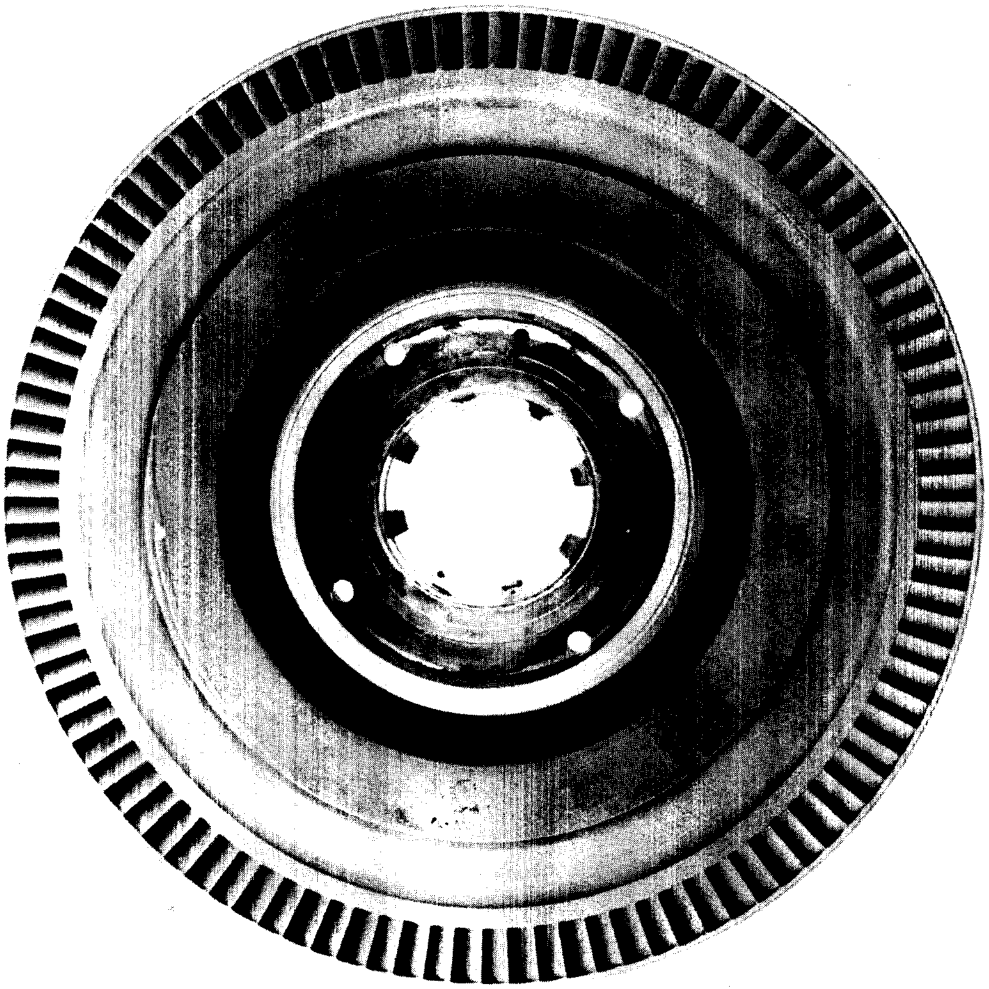


Figure C-147. Turbine Rotor With Shroud FE 46939

The ball bearing at the turbine is preloaded by a spring washer that assures proper thrust loading of the shaft bearings. The fuel pump and turbine combined thrust load is transferred to the main pump housing by the ball thrust bearing through a drilled passage in the fuel pump housing. The coolant flow through this passage is supplied from the 1st-stage pump contour. All other hydrogen cooled bearings in the turbopump are cooled by conduction and low pressure hydrogen flow through the gearbox cavity.

The ball bearing at the turbine is preloaded by a spring washer that assures proper thrust loading of the shaft bearings. The fuel pump and turbine combined thrust load is transferred to the main pump housing by the ball thrust bearing located between the fuel pump stages. The loads on the oxidizer pump shaft are supported by a ball thrust bearing located in the oxidizer pump housing and a cylindrical roller bearing. The idler gear radial load is carried by a pair of identical cylindrical roller bearings mounted on a nonrotating shaft. All bearings and races are made of consumable-electrode vacuum-melted, AMS 5630 corrosion-resistant steel and are designed to operate unlubricated at 38° to 158°R. The ball bearings incorporate split inner races and inner race riding cages of aluminum armored plastic. Bearing spin/roll ratio is 19%.

e. Seals

All carbon-face seals on the fuel pump shaft are of similar construction. The carbon seal is held against the rotating seal face by a spring-loaded retainer. A metal ring seal in the retainer limits leakage past the seal housing.

The fuel pump interstage seal shown in figure C-148 is designed to limit leakage between pump stages, while a two-step, carbon-face seal as shown in figure C-149 limits leakage of hydrogen into the gearbox chamber. The turbine seal is designed to limit leakage of hydrogen from the turbine area into the gearbox chamber.

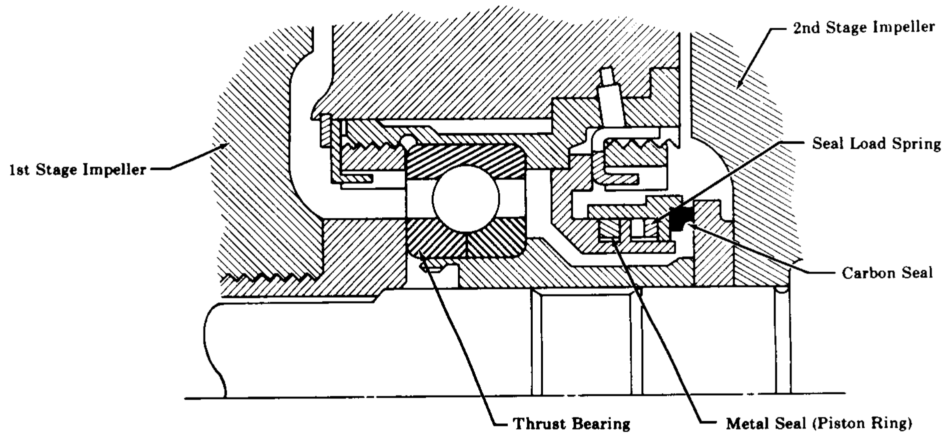

2nd Stage Impeller
Seal Load Spring
1st Stage Impeller
Carbon Seal
Thrust Bearing
Metal Seal (Piston Ring)

Figure C-148. Fuel Pump Interstage Seal FD 3150A

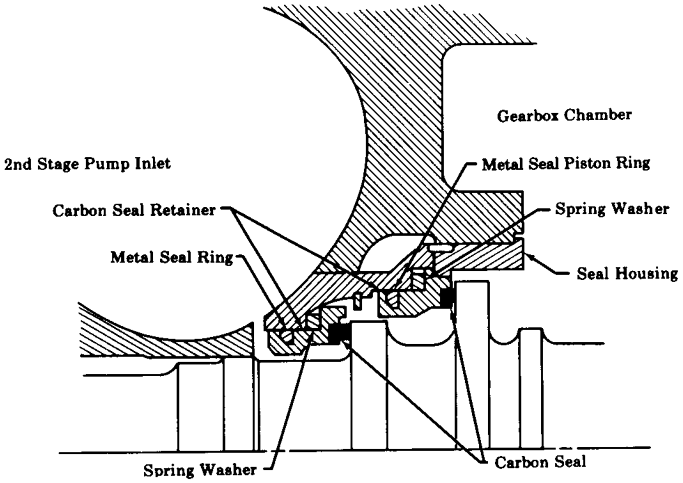

Gearbox Chamber
2nd Stage Pump Inlet
Metal Seal Piston Ring
Carbon Seal Retainer
Spring Washer
Metal Seal Ring
Seal Housing
Spring Washer
Carbon Seal

Figure C-149. Fuel Pump Face Seal FD 3148A

All interstage leakage within the turbine itself is controlled by labyrinth seals between stages. Figure C-150 illustrates these seals.

The oxidizer pump shaft seal assembly is located between the pump and gearbox as depicted in figure C-151. The seal assembly consists of two bellows-type, carbon-face, primary seals that minimize the leakage of hydrogen and oxygen. Overboard vents are provided for leakage past these seals. The bellows is splined to a retainer that absorbs torque and provides functional damping but permits axial movement. Two carbon ring seals, which are loaded by spring washers, are used as backup seals to prevent mixing of propellants in case of a primary seal failure. The backup seals are vented to a separate overboard port.

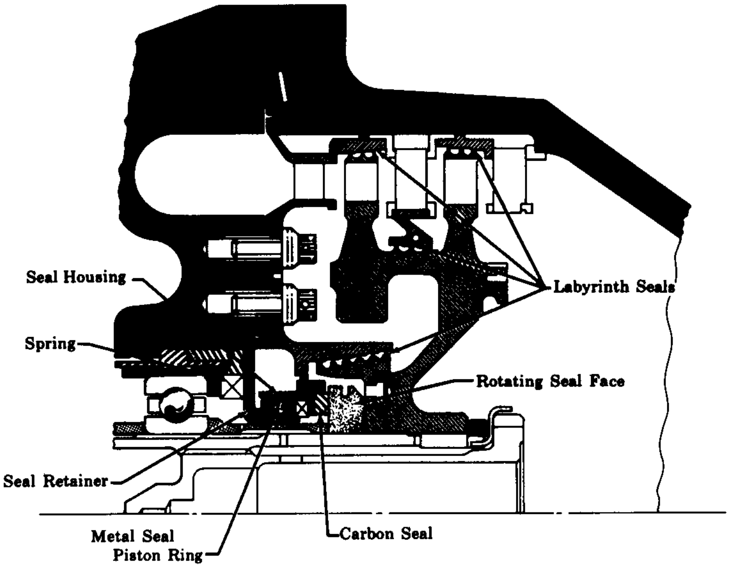

Seal Housing
Spring
Seal Retainer
Metal Seal Piston Ring
Carbon Seal
Rotating Seal Face
Labyrinth Seals

Figure C-150. Turbine Rotor Seal FD 10742B

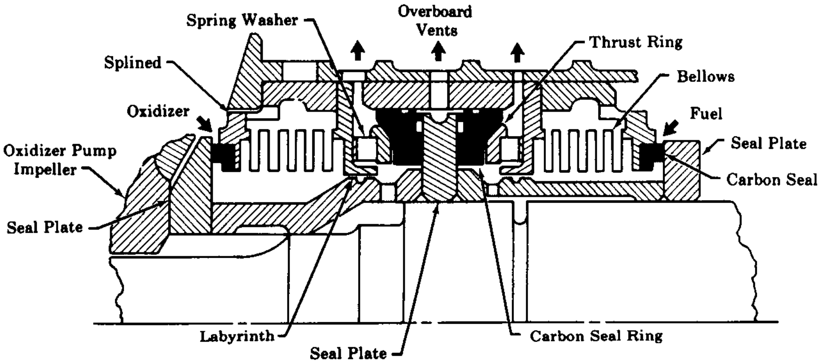

Spring Washer
Overboard Vents
Thrust Ring
Splined
Bellows
Oxidizer
Fuel
Oxidizer Pump Impeller
Seal Plate
Carbon Seal
Seal Plate
Labyrinth
Seal Plate
Carbon Seal Ring

Figure C-151. Oxidizer Pump Seal FD 3151C

The accessory drive pad seal as shown in figure C-152 is also a splined bellows seal which restricts the overboard leakage of hydrogen at that location.

4.2.3.2 Design Studies

The Derivative IIB engine was based upon the utilization of the RL10A-3-3 turbomachinery. Mechanical studies of this turbopump assembly have shown it capable of meeting all requirements while remaining within design limits with the exception of providing sufficient coolant flow to cool the extendible nozzle. The hydrogen flow used to cool the gears and bearings is collected in the gearbox cavity and is supplied to the dump-cooled extendible nozzle through the nozzle coolant valve. The cooling requirement of the extendible nozzle was found, during the Critical Elements Evaluation, to be greater than the flow presently provided by the RL10A-3-3 turbopump flow passages (0.7 lb/sec). For the Derivative II engine, these flow passages were resized to provide the desired coolant flow of 0.265 lb/sec. Of this flow, approximately 62% is provided by flow tapped off the fuel pump first stage and the remaining flow is provided by flow passing the fuel pump shaft. Figure C-153 illustrates the coolant flowpaths in the turbopumps. In addition to providing the required dump nozzle coolant flow the increased flowrate could extend turbopump life.

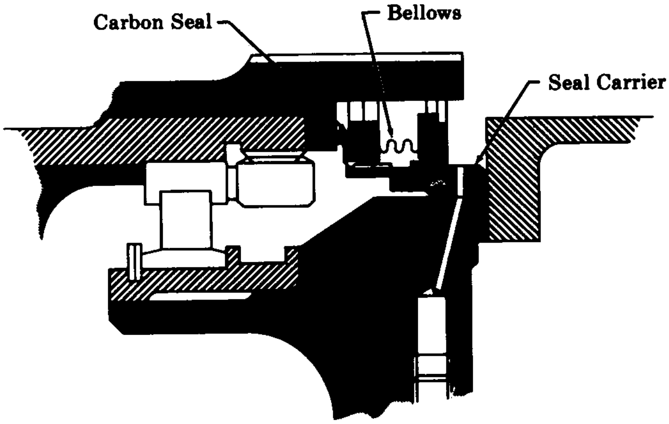

Carbon Seal
Bellows
Seal Carrier

Figure C-152. Accessory Drive Pad Seal FD 10741A

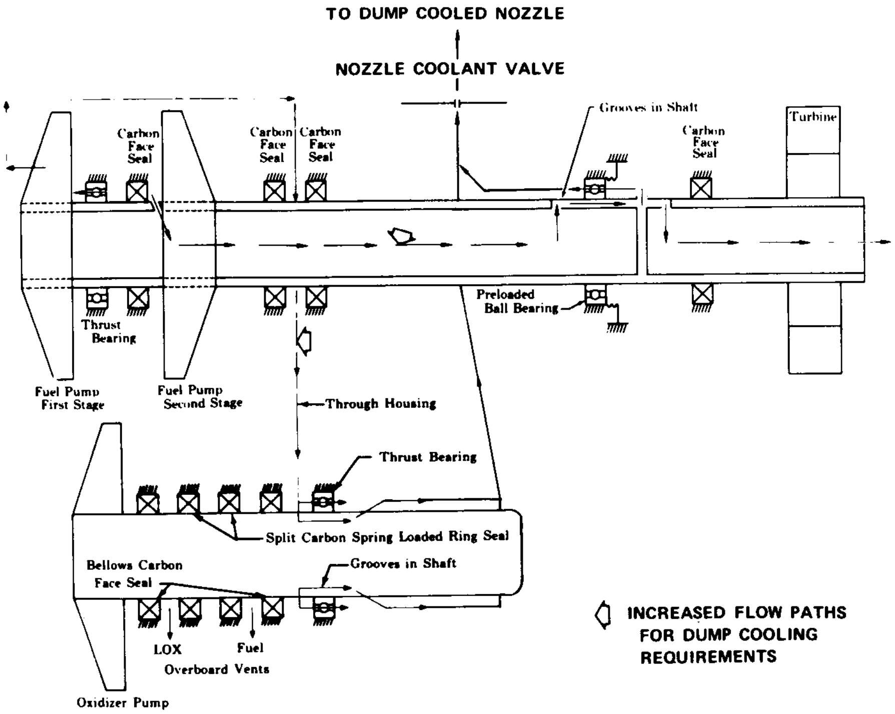

TO DUMP COOLED NOZZLE
NOZZLE COOLANT VALVE
Grooves in Shaft
Turbine
Carbon Face Seal
Carbon Face Seal
Carbon Face Seal
Carbon Face Seal
Thrust Bearing
Preloaded Ball Bearing
Fuel Pump First Stage
Fuel Pump Second Stage
Through Housing
Thrust Bearing
Split Carbon Spring Loaded Ring Seal
Bellows Carbon Face Seal
Grooves in Shaft
LOX
Fuel
Overboard Vents
Oxidizer Pump
INCREASED FLOW PATHS FOR DUMP COOLING REQUIREMENTS

Figure C-153. Turbopump Coolant Schematic FD 6167F

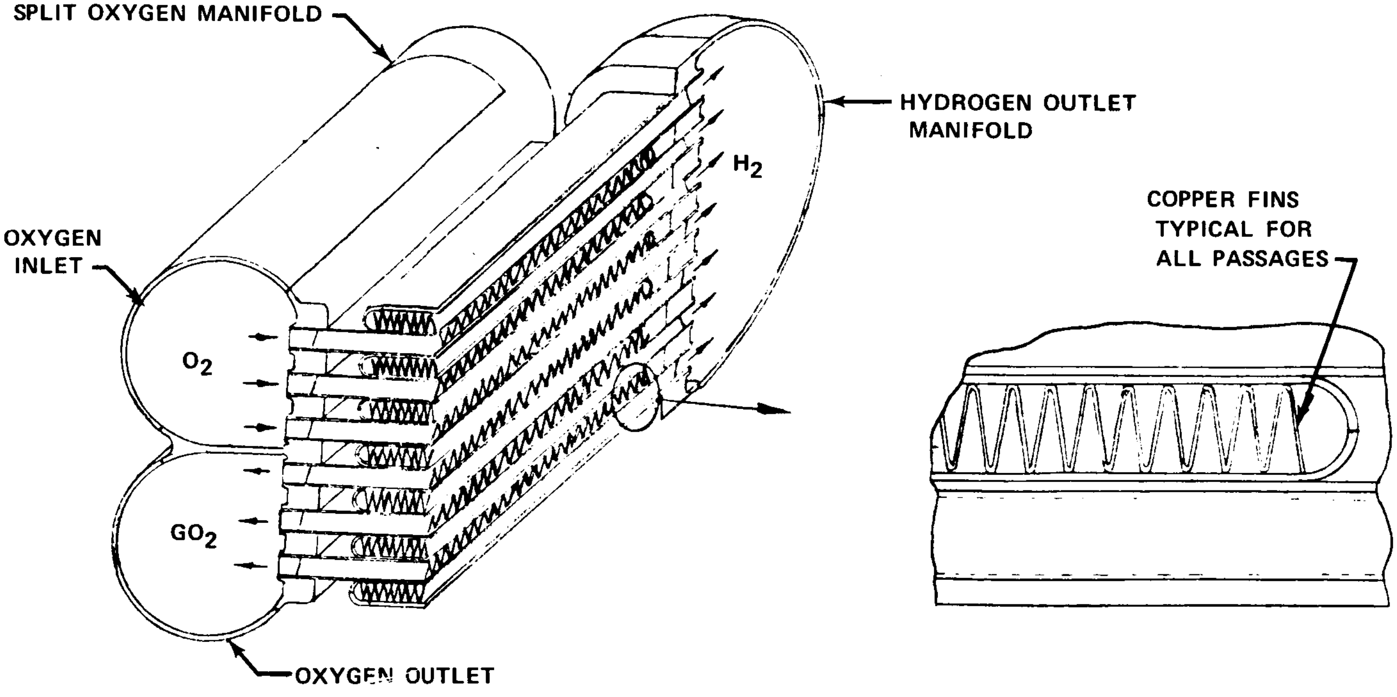

SPLIT OXYGEN MANIFOLD
HYDROGEN OUTLET MANIFOLD
H_2
COPPER FINS TYPICAL FOR ALL PASSAGES
OXYGEN INLET
O_2
GO_2
OXYGEN OUTLET

Figure C-154. Gaseous Oxidizer Heat Exchanger Design Derivative IIA Engine

FD 76010

4.2.3.3 Life

The life limiting components of the turbopump, i.e., the bearings, seals and gears, were studied for their life limits. In the Derivative IIB turbopump, life is limited by the wearing of gears. This turbopump has demonstrated over 5 hr of gear life in its use on the RL10A-3-3.

4.2.3.4 Materials

The materials listed below in table C-30 are selected for use on the Derivative IIB engine turbopump based on their successful use in the RL10A-3-3 engine.

Table C-30. Derivative IIB Selected Materials

Component	Material	Type
Fuel Pump Gearbox Housing	Aluminum alloy casting	AMS 4215
Housings	Aluminum alloy forging	AMS 4130
Oxidizer Impeller	Stainless steel forging	AMS 5646
Fuel Impellers	Aluminum alloy forging	AMS 4135
Shaft	Nickel alloy bar	AMS 5667
Turbine Rotor	Aluminum alloy forging	AMS 4127
Gears	Carburizing steel	AMS 6260
Bearings	Stainless steel bar and forging	AMS 5630
Bearing Liners	Stainless steel forging	AMS 5646

4.2.3.5 Weight

The weight of the Derivative IIB engine turbopump is 79 lb. This weight is the same as that of the RL10A-3-3 engine turbopump.

4.2.4 GO_2 Heat Exchanger

The function of the GO_2 heat exchanger is to gasify oxygen before entry into the injector during tank head idle operation and to provide gaseous oxygen for tank pressurization during pumped idle and full thrust operation. The heat exchanger performance at each engine operating point was defined by engine cycle and vehicle tank system studies, whose important characteristics are shown in table C-31.

4.2.4.1 Mechanical Description

The heat exchanger is a compact, laminated assembly having alternating layers of hydrogen and oxygen flow passages as shown in figure C-154. Each passage is formed by two parallel plates which are closed on two sides and joined by a series of copper fins oriented in the direction of flow. Special provisions are made to ensure complete isolation of the working fluids. Warm hydrogen enters the heat exchanger through a flanged inlet where a semicircular distribution manifolds directs the incoming hydrogen into seven parallel finned passageways. Hydrogen convects heat into the fins and face sheets as it flows through the passageways. The hydrogen flow is then collected in a manifold and discharged through a flanged exit.

Table C-31. Oxidizer Heat Exchanger Requirements

	Full Thrust	Pumped Idle	Tank Idle
Hydrogen Flowrate, lb_m/sec	0.04	0.41	0.070
Hydrogen Inlet Temperature, °R	474.	622.	582.
Hydrogen Exit Temperature, °R	251.	523.	426.
Hydrogen Inlet Pressure, psia	657.	154.	10.3
Hydrogen Pressure Loss, psi	<1	1.09	0.780
Oxygen Flowrate, lb_m/sec	0.500	1.50	0.32
Oxygen Inlet Temperature, °R	167.	165.	164.
Oxygen Exit Temperature, °R	265.	245.	385.
Oxygen Inlet Pressure, psia	588.	124.	15.9
Oxygen Pressure Loss, psi	<1	2.02	0.741
Heat Flux, Btu/sec	36.1	146.	39.5

The oxidizer flow makes two passes through the heat exchanger. The flow enters the oxidizer inlet distribution manifold through an end mounted flange. The flow is routed through three flow passages, collected in a turnaround manifold and rerouted through three additional passages before exiting from the heat exchanger. Heat is added to the oxygen as it flows through the fins and between the face sheets of each passageway.

Flow passages shown in figure C-155 for the hydrogen and oxygen are slightly different in length and width. Both are constructed with 0.010-in. thick AISI 347 stainless steel face sheets and 0.005-in. thick copper fins. All passageways are 0.100 in. high with 0.043 in. between fins. They are constructed by welding two facesheets into a leakproof assembly and brazing in the preformed (corrugated) copper fins. The core is created by stacking alternate layers of hydrogen and oxygen passageways. A furnace braze is used to assemble the core and form a thermal bond between the outer surfaces of adjacent face sheets.

Isolation of the working fluids is provided by assembling each hydrogen and oxygen passage separately and then braze assembling the passages along their flat sides. The hydrogen and oxygen manifold are also completely independent to prevent internal leaks. All leak paths are directed to vacuum environment.

4.2.4.2 Design Studies

The feasibility of an oxidizer heat exchanger was confirmed during the critical element analysis. However, the heat exchanger design conceived at that time was large and subsequent studies described in Section 3.1 were undertaken to investigate methods of reducing the heat exchanger volume. These studies provided the design requirements shown in the Introduction. Based upon these design requirements, the thermodynamic and mechanical design studies were completed as detailed in the following paragraphs.

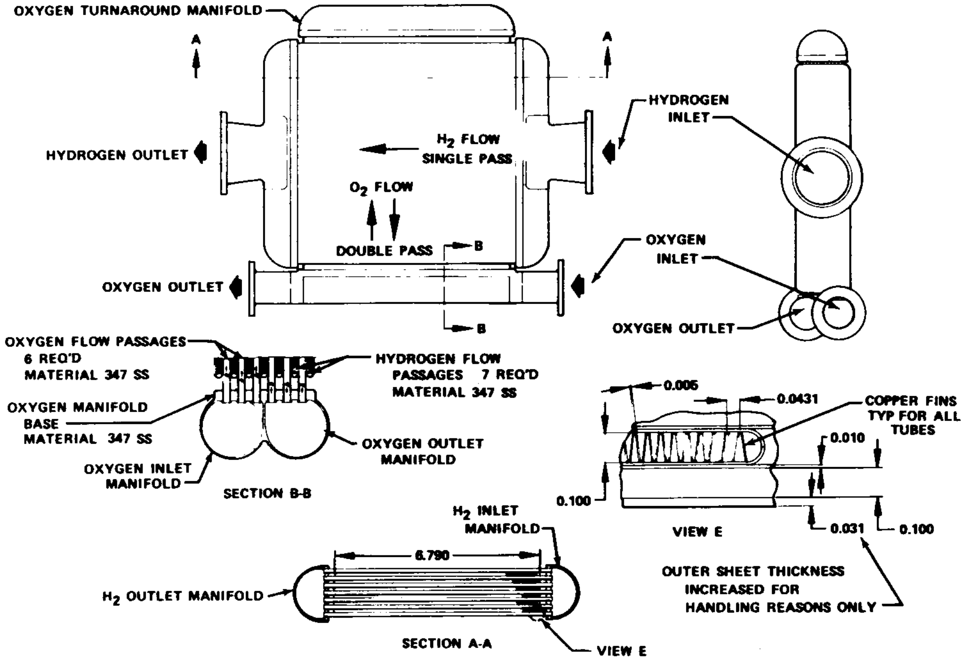

OXYGEN TURNAROUND MANIFOLD
A
A
HYDROGEN OUTLET
H2 FLOW
SINGLE PASS
O2 FLOW
DOUBLE PASS
B
B
OXYGEN OUTLET
HYDROGEN INLET
OXYGEN INLET
OXYGEN OUTLET
OXYGEN FLOW PASSAGES
6 REQ'D
MATERIAL 347 SS
HYDROGEN FLOW PASSAGES 7 REQ'D
MATERIAL 347 SS
OXYGEN MANIFOLD BASE
MATERIAL 347 SS
OXYGEN INLET MANIFOLD
OXYGEN OUTLET MANIFOLD
SECTION B-B
H2 INLET MANIFOLD
6.790
H2 OUTLET MANIFOLD
SECTION A-A
VIEW E
0.006
0.0431
COPPER FINS TYP FOR ALL TUBES
0.010
0.100
VIEW E
0.031
0.100
OUTER SHEET THICKNESS INCREASED FOR HANDLING REASONS ONLY

Figure C-155. RL10, IIA and IIB GO_2 Heat Exchanger FD 72057

The finned plate configuration of the heat exchanger was chosen from Reference 3 for its favorable high thermal flux and low volume features. This geometry provides over 1300 ft^2 of heat transfer area for every cubic foot of core volume. The theromodynamic analysis was performed utilizing friction factors and Colburn factors experimentally determined for this particular core geometry to predict heat transfer capability and fluid pressure losses.

The hydrogen-oxygen heat exchanger must provide complete isolation of the working fluids, eliminating any possibility of a hydrogen-to-oxygen or oxygen-to-hydrogen leak. This is done by weld assembling each flow passage on an individual basis and braze-assembling hydrogen passages to oxygen passages only along their respective flatsheet sides. The hydrogen and oxygen manifold are also completely independent to prevent a leak between them as shown in figure C-155. Every joint enveloping the flowpaths of hydrogen or oxygen is exposed to the environment; therefore, any leak is an overboard leak. Internal pressure acting on the large flat surfaces of the heat exchanger is resisted by tensile forces in the copper fins. Therefore, the facesheets are pressure-loaded over a corrugation span (0.043 in.) rather than the 7.2 in. width of the core. This allows a thin lightly loaded structure. The maximum expected facesheet stress due to pressure loading is 6,300 psi. Copper fin maximum stress is 3,000 psi with the braze joint stress slightly less. The outer facesheets are designed with an increased thickness to avoid damage in handling.

Individual flow passage assembly simplifies manufacturing and allows inspection and testing prior to braze assembly of the core. Flow passages are created by butt welding passage halves together, inserting the formed copper fins with foil braze material (NICORO 80) and furnace brazing. These passages can then be pressure tested and X-ray inspected. The maximum allowable braze void is approximately 0.150-in. diameter. The heat exchanger core is then created by stacking alternating layers of hydrogen and oxygen flow passages and furnace brazing with a copper-silver braze alloy. This braze joint promotes heat conduction between passages and the minimum braze coverage is set by heat transfer requirements. Hydrogen manifold bases are welded onto the hydrogen flow passages with a continuous weld around each passage. After a pressure check, the remainder of the hydrogen manifold is welded into place. The oxygen manifold is then attached in a similar manner.

4.2.4.3 Life

Due largely to thin wall construction, little thermal stress is anticipated in the heat exchanger core. Independent manifolds permit thermal growth in the warm core. There is a significant thermal gradient between the core metal temperature and the oxygen inlet temperature which may cause up to 0.3% strain in three thin-walled coolant passages. LCF data for 347 SST indicates a 0.6% strain level for a 3,000 cycle life at 200°F. Any facesheet strain occurs across the copper fins such that it does not induce strain in the copper fins or the braze joints. Therefore, a life of 10 hr is predicted for this heat exchanger configuration.

4.2.4.4 Materials

Copper is required as the fin material to meet the high conduction coefficient needed in this compact heat exchanger. 347 SST was chosen for the face sheets and manifolds because of its thermal compatibility with copper, ductility, good welding and brazing properties, and compatibility with hydrogen and oxygen.

The following is a summary of the materials selected for the oxidizer heat exchanger:

Component	Material	Type
Manifolds	Stainless Steel	AMS 5646
Face Sheets	Stainless Steel	AMS 5512
Fins	Copper	AMS 4501
Fin Braze	NICORO 80	TBD
Face Sheet Braze	Copper-Silver	TBD

4.2.4.5 Weight

Weight is minimized in the GO_2 heat exchanger by using materials which are 0.005 and 0.010-in. thick, the practical manufacturing limit for thin sheet metal. Stresses are quite low even with these very thin structural members; only the manifolds are stress limited. Weight is determined by the large total surface area required for high thermal efficiency. The current weight estimate is 13 lb.

4.2.5 Controls and Instrumentation

The RL10 Derivative II propellant control and instrumentation system consists of the following components: fuel inlet shutoff valve, oxidizer inlet shutoff valve, oxidizer flow control, fuel vent valve, turbine bypass valve, thrust control, main fuel shutoff valve, gaseous oxidizer valve, oxidizer tank pressurization valve, fuel tank pressurization valve, nozzle coolant valve, 4 solenoid valves, engine instrumentation and connecting cables. A schematic of the propellant system is shown in figure C-156 in which the control components are identified and the location in the engine system defined. A mechanical description of the control/instrumentation system is provided in the following paragraphs. A description of the valve effective area schedules is provided in 3.2.2.

4.2.5.1 Propellant Control Valves

a. Propellant Inlet Shutoff Valves

The fuel and oxidizer inlet shutoff valves, shown in figure C-157 are RL10A-3-3 engine components. The fuel and oxidizer inlet shutoff valves are similar in appearance and operation, differing only in size. They are two-position, normally closed ball valves with helium operated bellows actuators. The linear motion of the actuator is translated to rotary motion by a rack and pinion. The valves provide a seal between the vehicle propellant tanks and the fuel and oxidizer pumps during nonoperational periods of the engines and open fully upon the application of helium pressure to allow propellant flow into the engine. Sealing is accomplished by a pressure loaded fluoro-carbon seal.

b. Oxidizer Flow Control

The oxidizer flow control shown in figure C-158 is a spring-loaded, normally closed, line pressure actuated valve located in the main oxidizer line between the oxidizer pump and the injector. The valve meters liquid oxidizer flow at all operating levels above tank head idle. The valve is configured to provide ground trim of propellant mixture ratio, and provide inflight oxidizer propellant utilization control during full thrust operation.

The oxidizer flow control valve is divided into two functional sections. The forward section contains two spring-loaded poppet valves used to meter oxidizer flow during maneuver thrust (pumped idle) and full thrust and regulate flow during the engine transient to full thrust. The aft section contains a pintle valve used for propellant utilization control. The two poppet valves in the forward section are normally spring-loaded closed and open as a function of the pressure differential between valve inlet pressure and a pressure within the valve cavity which has been vented to pump inlet pressure. During tank head idle operation, the poppets are closed and no liquid oxidizer flow is allowed to enter the injector. As oxidizer line pressure increases during the acceleration from tank head idle to maneuver thrust (pumped idle), the bypass poppet valve fully opens to the metering area required for maneuver thrust operation. When the engine accelerates from maneuver thrust (pumped idle) to full thrust operation, the main poppet valve is also opened as a function of the differential pressure between valve inlet and pump inlet pressure. The bypass and main poppet valves both remain open during full thrust operation and the combined areas meter the required oxidizer flow. The full open position of the main poppet valve can be ground trimmed by a threaded mechanical stop to ground adjust engine mixture ratio.

The total valve oxidizer flow exits through the propellant utilization orifice and pintle at the aft end of the valve. Engine mixture ratio can be varied by applying torque to an actuation shaft which positions the discharge pintle utilizing a rack and pinion mechanism. The actuation shaft is sealed by a bellows assembly. The pinion shaft incorporates stops to limit shaft rotation and therefore engine mixture ratio range within allowable limits.

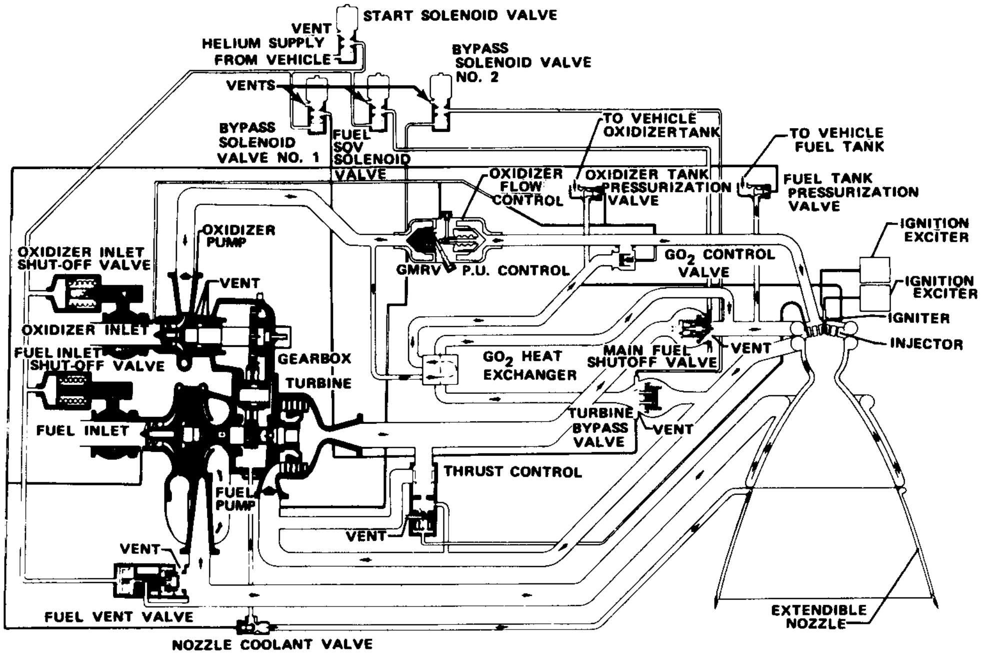
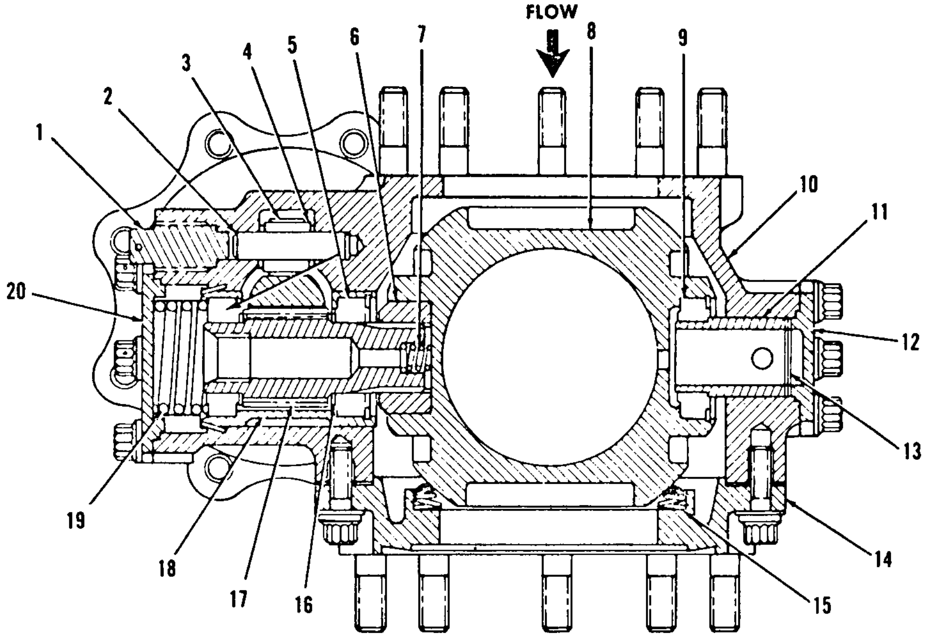

START SOLENOID VALVE
VENT
HELIUM SUPPLY FROM VEHICLE
BYPASS SOLENOID VALVE NO. 2
VENTS
BYPASS SOLENOID VALVE NO. 1
FUEL SOV SOLENOID VALVE
TO VEHICLE OXIDIZER TANK
TO VEHICLE FUEL TANK
OXIDIZER FLOW CONTROL
OXIDIZER TANK PRESSURIZATION VALVE
FUEL TANK PRESSURIZATION VALVE
IGNITION EXCITER
OXIDIZER PUMP
OXIDIZER INLET SHUT-OFF VALVE
GMRV
P.U. CONTROL
GO$_2$ CONTROL VALVE
IGNITION EXCITER
VENT
IGNITER
OXIDIZER INLET
INJECTOR
FUEL INLET SHUT-OFF VALVE
GEARBOX
TURBINE
GO$_2$ HEAT EXCHANGER
MAIN FUEL SHUTOFF VALVE
VENT
FUEL INLET
TURBINE BYPASS VALVE
VENT
THRUST CONTROL
FUEL PUMP
VENT
VENT
FUEL VENT VALVE
NOZZLE COOLANT VALVE
EXTENDIBLE NOZZLE

Figure C-156. RL10 Derivative IIB Propellant Flow Schematic FD 74111A


FLOW
1
2
3
4
5
6
7
8
9
10
11
12
13
14
15
16
17
18
19
20

1.	Plug	6.	Drive Insert	11.	Pivot	16.	Spacer
2.	Roller Pin	7.	Spring	12.	Pivot Cap	17.	Gear
3.	Roller Bearing	8.	Ball	13.	Shim	18.	Sleeve
4.	Spacer	9.	Ball Bearing	14.	Flange Assembly	19.	Spring
5.	Ball Bearing	10.	Body Assembly	15.	Seal Assembly	20.	Cap

Figure C-157. Propellant Inlet Shutoff Valve FD 76884

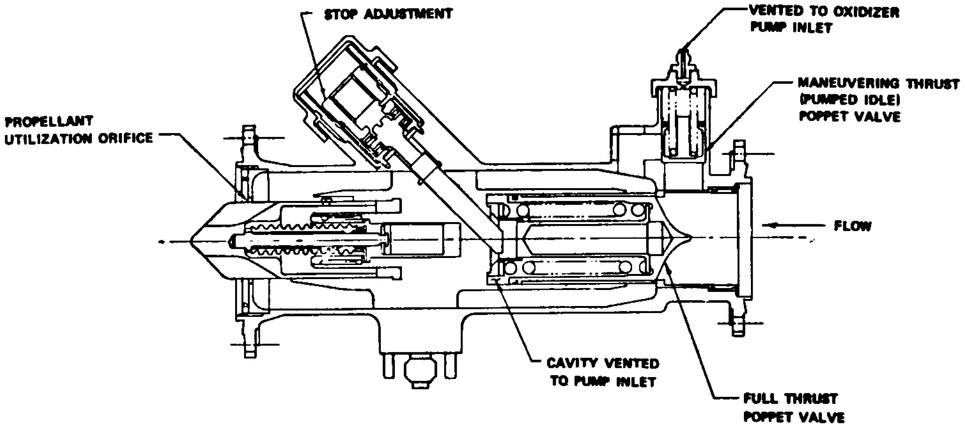

STOP ADJUSTMENT
VENTED TO OXIDIZER PUMP INLET
MANEUVERING THRUST (PUMPED IDLE) POPPET VALVE
PROPELLANT UTILIZATION ORIFICE
FLOW
CAVITY VENTED TO PUMP INLET
FULL THRUST POPPET VALVE

Figure C-158. Oxidizer Flow Control Valve FD 76005

This valve is very similar to the RL10A-3-3 oxidizer flow control valve in size and operation. Flow areas of the main poppet valve and the propellant utilization valve have been increased to accommodate operation at a higher mixture ratio (i.e., 6.0 vs 5.0) and the bypass poppet valve has been added for maneuver thrust (pumped idle) operation. The valve is designed to be manufactured of aluminum with stainless steel hardware. The calculated weight, based on the valve layout drawing, and RL10A-3-3 valve weight, is 9 pounds.

c. Turbine Bypass Valve

The turbine bypass valve shown in figure C-159 is a spring-loaded, normally closed, three-position, bullet-type annular gate valve located in the turbine bypass line. The valve is similar to the RL10A-3-3 main fuel shutoff valve in size, materials used, and operation. The valve is pressure actuated to allow the setting of three discrete areas for metering turbine bypass hydrogen flow during tank head idle, maneuver thrust (pumped idle) and full thrust operation. During tank head idle operation the valve is activated to the full open position of 1.5 in^2 by helium operating on a bellows actuator. During maneuver thrust operation the valve is actuated to an intermediate area of 0.617 in^2 by gaseous hydrogen operating on a concentric secondary bellows actuator. During full thrust operation the gate is spring-loaded into its normally closed position and a 0.114 in^2 area is provided by notches in the annular gate. The valve is designed to be made of aluminum and stainless steel and is estimated to weigh 4.2 lb based upon the valve layout drawing.

d. Gaseous Oxidizer Valve

The gaseous oxidizer valve shown in figure C-160 is a spring-loaded, normally open, pressure activated, sleeve valve located between the GO_2 heat exchanger and the injector. This valve meters gaseous oxygen flow during tank head idle and regulates oxygen flow during the transient to maneuver thrust. The pressure differential used for valve actuation is maintained on the sleeve piston by valve inlet pressure on the high side, and by venting a low pressure valve cavity to the oxidizer pump inlet. As line pressure increases during the acceleration to maneuver thrust, the valve closes from 2.0 in^2 effective area against the preloaded spring. The valve is designed with an aluminum housing and valve sleeve. The estimated valve weight is 2.0 lb based upon the valve layout drawing.

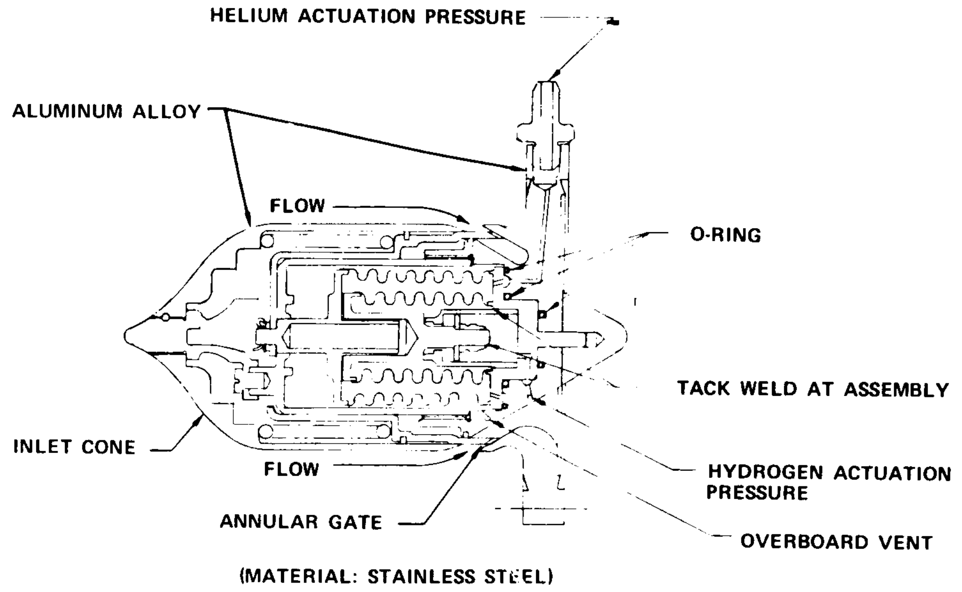

HELIUM ACTUATION PRESSURE
ALUMINUM ALLOY
FLOW
O-RING
TACK WELD AT ASSEMBLY
INLET CONE
FLOW
HYDROGEN ACTUATION PRESSURE
ANNULAR GATE
OVERBOARD VENT
(MATERIAL: STAINLESS STEEL)

Figure C-159. Turbine Bypass Valve FD 76004

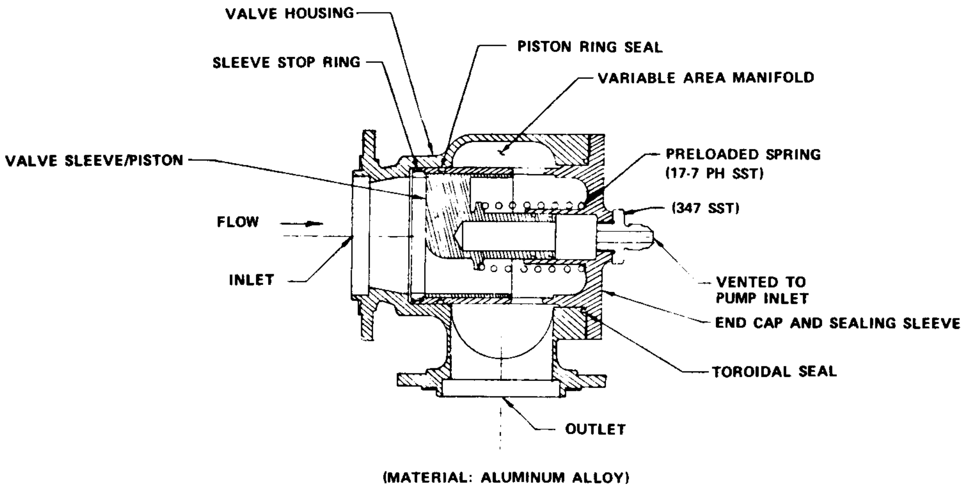
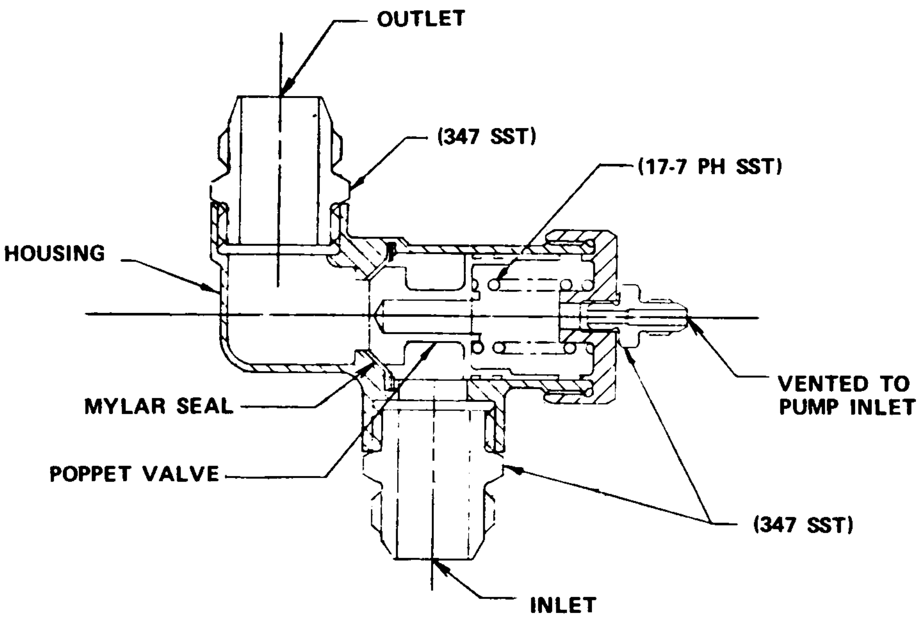

VALVE HOUSING
PISTON RING SEAL
SLEEVE STOP RING
VARIABLE AREA MANIFOLD
VALVE SLEEVE/PISTON
PRELOADED SPRING (17-7 PH SST)
(347 SST)
FLOW
INLET
VENTED TO PUMP INLET
END CAP AND SEALING SLEEVE
TOROIDAL SEAL
OUTLET
(MATERIAL: ALUMINUM ALLOY)

Figure C-160. Gaseous Oxidizer Valve FD 76002A

e. Tank Pressurization Valves

Propellant tank pressurization valves shown in figure C-161 are spring-loaded, normally closed, line pressure actuated, two-position poppet valves that supply propellants for fuel and oxidizer tank pressurization. These valves limit but do not regulate the tank pressurant flowrate. Flow regulation is provided within the vehicle tank pressurization system. When the differential between line pressure and an internal cavity vented to pump inlet pressure increase to a predetermined value, the total force acting on the poppet overcomes the spring-load and the valve fully opens to an effective area of 0.34 $in.^2$ The propellant tank pressurization valves are designed of aluminum with stainless steel fittings with a mylar poppet seal. The estimated weight of each valve is 2 lb based upon the layout drawing.


OUTLET
(347 SST)
(17-7 PH SST)
HOUSING
VENTED TO PUMP INLET
MYLAR SEAL
POPPET VALVE
(347 SST)
INLET

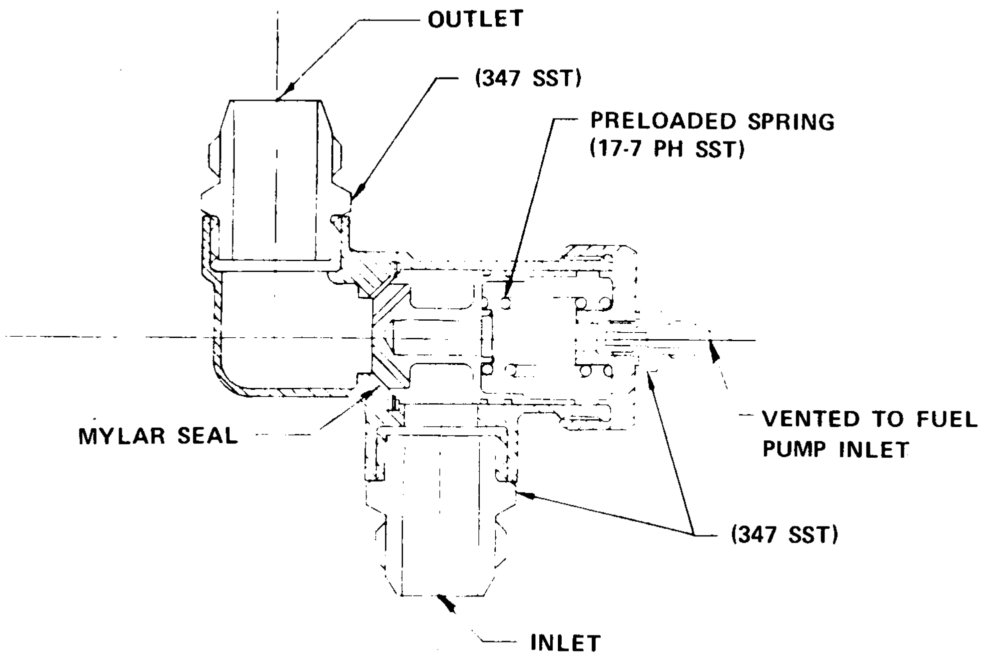
(MATERIAL: ALUMINUM ALLOY)

(TYPICAL BOTH FUEL AND OXIDIZER)

Figure C-161. Tank Pressurization Valve FD 76007A

f. Nozzle Coolant Valve

The nozzle coolant valve shown in figure C-162 is a spring-loaded, normally closed, line pressure actuated, two-position poppet valve which utilizes the same housing and design as the tank pressurization valves. The valve poppet is orificed to provide a minimum area of 0.037 in^2 in the closed position to meter coolant flow to the dump-cooled nozzle during tank head idle operation. As line pressure increases to a level greater than 20 psia, the total force acting on the poppet overcomes the spring preload and the valve fully opens (0.30 in^2 area) for maneuver and full thrust operation. The nozzle coolant valve estimated weight is 2 lb.



(MATERIAL: ALUMINUM ALLOY)

Figure C-162. Nozzle Coolant Valve

FD 76006A

g. Thrust Control Valve

The thrust control valve, shown in figure C-163 is an RL10A-3-3 component. The valve is a normally closed, servo-operated, closed-loop, variable-position bypass valve used to control engine thrust at full thrust by the regulation of the turbine power. Control of engine thrust is provided by combustion chamber pressure acting through a motor bellows and spring carrier against a reference spring load and reference bellows pressure load to actuate a servo lever exposing a shear orifice. Exposure of the shear orifice bleeds servo chamber pressure which is supplied from engine turbine upstream pressure. The bypass valve position feedback signal is mechanically transmitted through the feedback spring carrier and spring to the servo lever. As combustion chamber pressure varies from the desired value, the action of the control allows the turbine bypass section to vary fuel flow through the turbine. This regulates turbine power and speed which in turn regulates combustion chamber pressure. A secondary function of the control is to limit engine thrust overshoot during the start transient. This is accomplished through a reference bellows pressure lag system that prevents reference pressure from rising at the same rate as combustion chamber pressure. This allows the control bypass valve to open early in the start transient and reduces turbine power before attainment of steady-state chamber pressure. The thrust control valve position is monitored by a linear variable differential transformer (LVDT) device and the data are interpreted as a degree of engine health. The valve is constructed of aluminum and weighs 5.3 pounds.

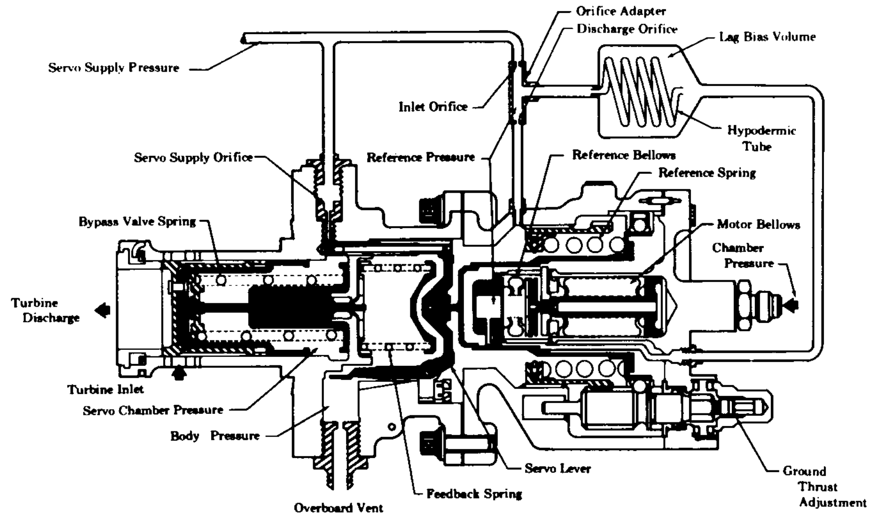

Orifice Adapter
Discharge Orifice
Lag Bias Volume
Servo Supply Pressure
Inlet Orifice
Hypodermic Tube
Servo Supply Orifice
Reference Pressure
Reference Bellows
Reference Spring
Bypass Valve Spring
Motor Bellows
Chamber Pressure
Turbine Discharge
Turbine Inlet
Servo Chamber Pressure
Body Pressure
Servo Lever
Ground Thrust Adjustment
Feedback Spring
Overboard Vent

Figure C-163. Thrust Control Valve FD 10744

h. Fuel Vent Valve

The fuel vent valve, shown in figure C-164, is an RL10A-3-3 component. It is a pressure operated, two-position, sleeve type valve that is spring-loaded open to provide pressure relief of the fuel system lines during the engine shutdown. The valve is maintained in the closed position during all three active modes of engine operation.

At the start signal helium pressure actuates the valve piston assembly moving the sleeve valve to close the overboard vent port. As the engine accelerates, fuel pump discharge pressure is used to actuate the fuel pump discharge piston to force the sleeve valve against the Teflon seat to provide a positive seal during high pressure operation. At shutdown, when helium pressure is removed, the vent port fully opens, relieving fuel pressure in the fuel system lines. In addition to being opened by spring loads, the valves are boosted open with fuel pump discharge pressure to ensure rapid opening of the vent port.

The fuel vent valve is constructed of aluminum and weighs 6.3 pounds.

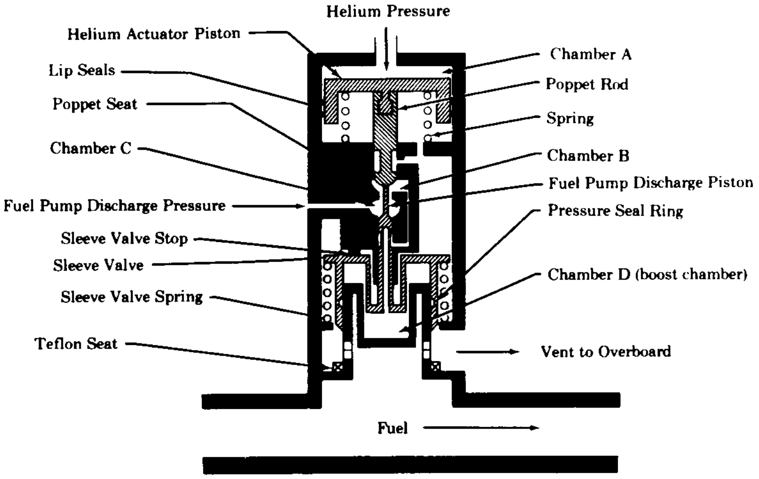

Helium Pressure
Helium Actuator Piston
Lip Seals
Poppet Seat
Chamber C
Fuel Pump Discharge Pressure
Sleeve Valve Stop
Sleeve Valve
Sleeve Valve Spring
Teflon Seat
Chamber A
Poppet Rod
Spring
Chamber B
Fuel Pump Discharge Piston
Pressure Seal Ring
Chamber D (boost chamber)
Vent to Overboard
Fuel

Figure C-164. Fuel Vent Valve FD 2666C

i. Main Fuel Shutoff Valve

The main fuel shutoff valve, shown in figure C-165, is an RL10A-3-3 component. It is a helium operated, two-position, normally closed annular gate valve. The valve serves to prevent the flow of fuel through the fuel pump turbine during tank head idle operation and provides a rapid cutoff of fuel flow to the combustion chamber at engine shutdown.

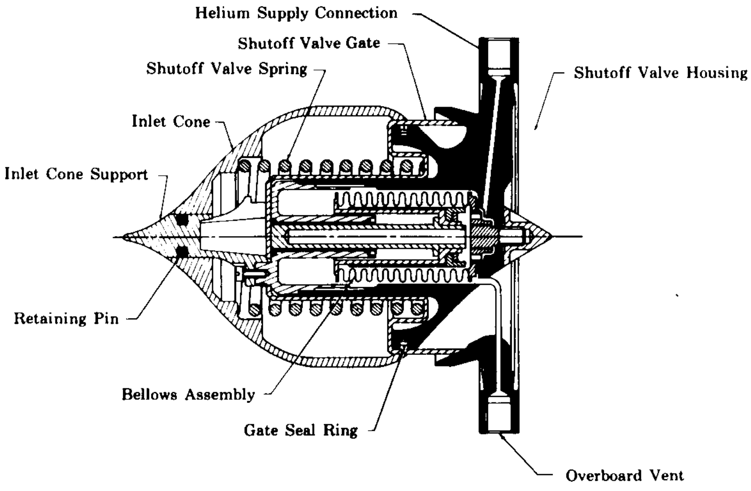


Figure C-165. Main Fuel Shutoff Valve Schematic FD 1551D

The shutoff valve gate is opened by helium pressurization of the bellows assembly to allow the flow of fuel through the turbine at all operating modes above tank head idle. The compressed shutoff valve spring returns the gate to its normally closed position when helium pressure is vented at engine shutdown. Sealing is accomplished by the seating of the spherical surface of the gate against a conical surface on the valve housing and by the gate seal ring.

The valve is constructed primarily of steel and weighs 3.4 lb.

j. Solenoid Valves

The solenoid valves, shown in figure C-166, are RL10A-3-3 components. They are solenoid-actuated, direct acting, 3-way valves with double ended poppets that supply helium or hydrogen actuation fluid to the various propellant valves.

The four solenoid valves used in the Derivative II engine are identical in design and function. The start solenoid valve controls the actuator helium supply to the propellant inlet shutoff valves and fuel vent valve. The fuel shutoff solenoid valve controls the actuator helium supply to the main fuel shutoff valve. The bypass solenoid valve No. 1 controls the actuator helium supply to the turbine bypass valve. The bypass solenoid valve No. 2 controls the actuator hydrogen supply to the turbine bypass valve. In the deenergized position, valve port A is closed and valve port B is ambient vented. The poppet is positioned by the valve spring force on the poppet valve body. Upon an electrical signal from the vehicle, the respective solenoid valve is energized by dc electrical supply from the vehicle. The plunger rod moves the poppet valve, opening port A and closing port B. Helium/hydrogen flows through port A into the control valve actuators. The solenoid is deenergized at engine shutdown and the spring returns the poppet valve to its original position closing port A and opening port B, through which the pneumatic media in the engine valve system is vented overboard.

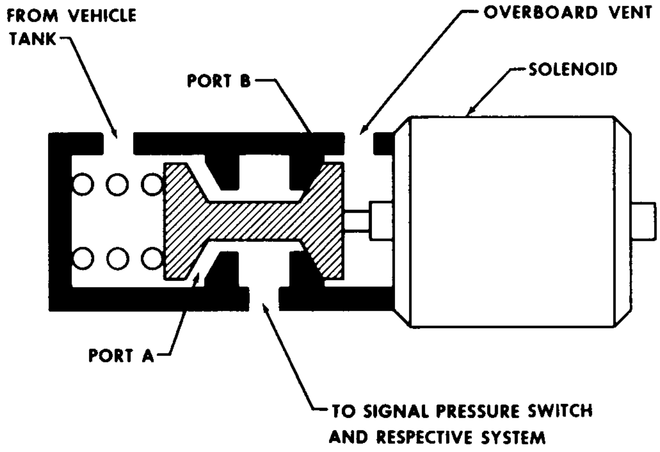


Figure C-166. Solenoid Valve Schematic FD 4444

4.2.5.2 Instrumentation and Harness System

The instrumentation and command system on the RL10 Derivative engine consists of an instrumentation enclosure, command harness, instrumentation harness, and various sensing units which monitor engine parameters listed in table C-32.

The instrumentation enclosure contains the solenoids, pressure transducers, completion networks, and other signal conditioning units for the temperature and vibration sensors. Since these instruments require a controlled temperature environment for operation, the instrumentation enclosure also contains an electrical heater and a thermocouple to monitor the internal temperature. Mounted on the instrumentation enclosure are 2 interface connectors which mate with the vehicle cables. One connector provides command signals from the vehicle computer to the engine solenoids, exciters, and nozzle motors. The other connector provides 28 vdc excitation to the engine instrumentation and returns the instrumentation output signals to the vehicle. Also mounted on the instrumentation enclosure are 2 connectors which mate with the engine command and engine instrumentation harnesses.

The engine command harness provides 28 vdc command signals from the vehicle, via the instrumentation enclosure to the nozzle motors and ignition exciters on the engine.

The engine instrumentation harness provides 28 vdc excitation to and output signals from the various temperature, speed, and position sensors on the engine. This harness is also connected to the vehicle cables through the instrumentation box.

Table C-32. RL10 Derivative Engines Instrumentation and Monitored Parameters

Parameter	Range and Units	Uncertainty (1)
Fuel Pump		
Housing Temperature	35-660°R	±6°R
Discharge Pressure	0-1200 psia	±2%
Oxidizer Pump		
Housing Temperature	150-660°R	±6°R
Discharge Pressure	0-800 psia	±2%
Rotational Speed	0-15,000 rpm	±4%
Thrust Chamber		
Chamber Pressure	0-500 psia	±2%
Low Range Chamber Pressure	0-15 psia	±2%
Chamber Coolant Discharge Temperature	200-1200°R	±15°R
Miscellaneous		
Nozzle Extension Contact	Switch	N/A
Thrust Control Position	0-100%	±5%
Gearbox Vibration	0-5 g	±10%
Exciter Monitor	0-10 vdc	±5%
Solenoid Monitor	0-20 mvdc	±5%

(1) (Bias + $t_{.95}S$) in % of full-scale.

4.2.5.3 Design Theory

A conceptual design layout for each of the new valves was generated to define mechanical implementation, materials identification, and physical sizing for weight and envelope definition. A low risk design approach was taken by utilizing previously proven valve elements, where possible, such as shutoff seals, bellows, static seals and materials combinations. In most cases the entire valve design was based on a resizing or modification of an existing RL10A-3-3 component design. Where possible, common valve elements were utilized.

The electrical wiring layout of the instrumentation system is shown on figure C-167. Command signals are kept separated from instrumentation signals to minimize the effects of noise interference problems.

The harness shielding scheme was designed to meet EMI specifications MIL Spec 461/462. Harnesses outside the instrumentation enclosure have an outer metal braid for high frequency noise suppression. Cables inside the instrumentation enclosure do not have this outer braid because the instrumentation enclosure suppresses high frequency noise by being connected to ground with a bond strap. All harnesses have an inner metal braid for low frequency noise suppression. Each instrumentation signal has its own braid which is continuous to the interface connectors. The braids are electrically floating at the instruments, and separately connected to ground in the instrumentation enclosure, so that noise in one circuit will not affect adjacent circuits. The shield braiding in the vehicle cables are not connected to engine cable braiding. Instead they are connected together and to ground. This is so that any noise from the vehicle will be grounded before it can affect the engine instrumentation signals.

The interface connectors, which mate with the vehicle cables, are mounted in an easily accessible location on the instrumentation enclosure. They are of the bayonet quick disconnect type (Specification MSFC 40M39569) to provide for easy troubleshooting and replacement of the engine or engine components. The connectors which mate with the command and instrumentation harnesses are of the same type and are located on the instrumentation enclosure such that they will not be confused with interface connectors.

The instrumentation enclosure contains equipment which requires a controlled temperature environment for proper operation. The electrical heater is incorporated for this purpose. The vehicle provides 28 vdc, 2 amp to the heater, and monitors the internal temperature of the instrumentation enclosure with a chromel/alumel thermocouple.

The equipment inside the instrumentation enclosure obtains excitation voltage from the vehicle and provides output signals to the vehicle at the interface connectors. The solenoids (start, fuel SOV, Bypass No. 1, and Bypass No. 2) are activated by a 28 vdc, 2.5 amp command signal. A series resistor provides a millivolt output signal to indicate that the command signal was received and that the solenoid coil is operational. The pressure transducers for fuel pump discharge, oxidizer pump discharge, thrust chamber (high range) and thrust chamber (low range) are excited by 10 vdc and provide an output voltage proportional to pressure. The bridge completion networks (BCN) for the fuel pump housing temperature, oxidizer pump housing temperature sensors are resistance bridges which are excited by 28 vdc and provide a millivolt signal output which is proportional to temperature. The thrust control position indicator signal conditioning unit converts the 28 vdc excitation into an ac voltage used by the position indicator. The ac output of the position indicator is then converted by this unit into a dc voltage which is proportional to thrust control position. The charge amplifier requires 28 vdc excitation and provides output signals which are proportional to vibration measured at the gearbox.

The equipment on the engine, outside the instrumentation enclosure, obtain excitation from the vehicle and provide output signals to the vehicle through the command and instrumentation harnesses. These harnesses are connected to the instrumentation enclosure which routes the circuits to the interface connectors. The primary and secondary ignition exciters are activated by 28 vdc, 2.5 amp command signals. Each time an exciter activates an igniter, it also provides a 6 vdc output signal. The two 6 vdc output signals are connected in parallel and provided to the vehicle for monitoring normal exciter operation.

The two nozzle actuation motors require a 28 vdc, 3.2 amp command signal. Since the motors are redundant in operation, two sets of lead wires are routed to the interface connector where they are connected in parallel. Two nozzle position switches (nozzle extended and nozzle retracted) provide a contact closure when the nozzle is fully extended or fully retracted. Switch closure does not occur at intermediate nozzle positions. The three temperature sensors (fuel pump housing, oxidizer pump housing, and thrust chamber coolant) are platinum, resistance thermometer types, and are connected to their respective bridge networks using the 3-wire scheme to negate the effect of lead wire resistance.

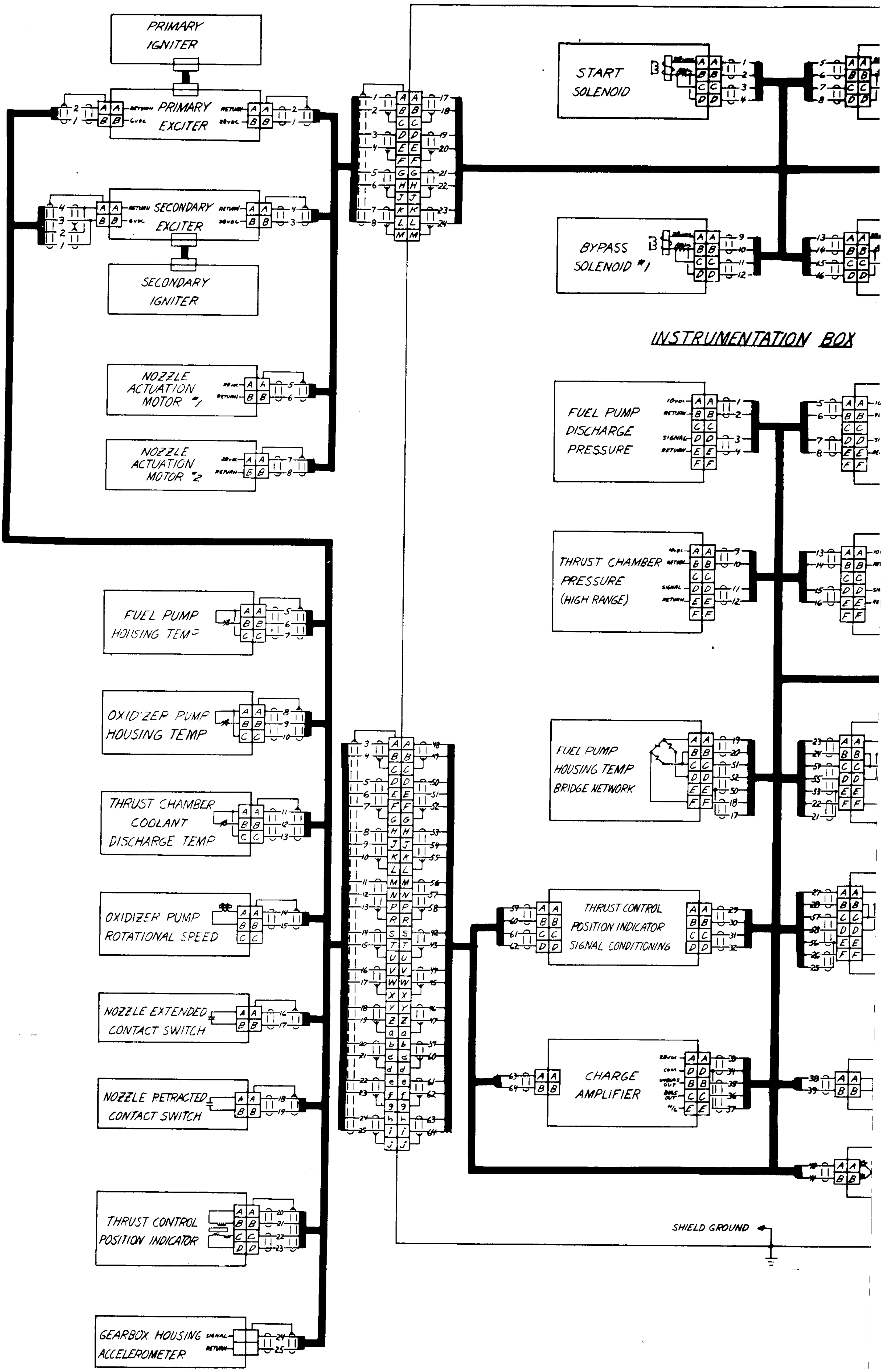
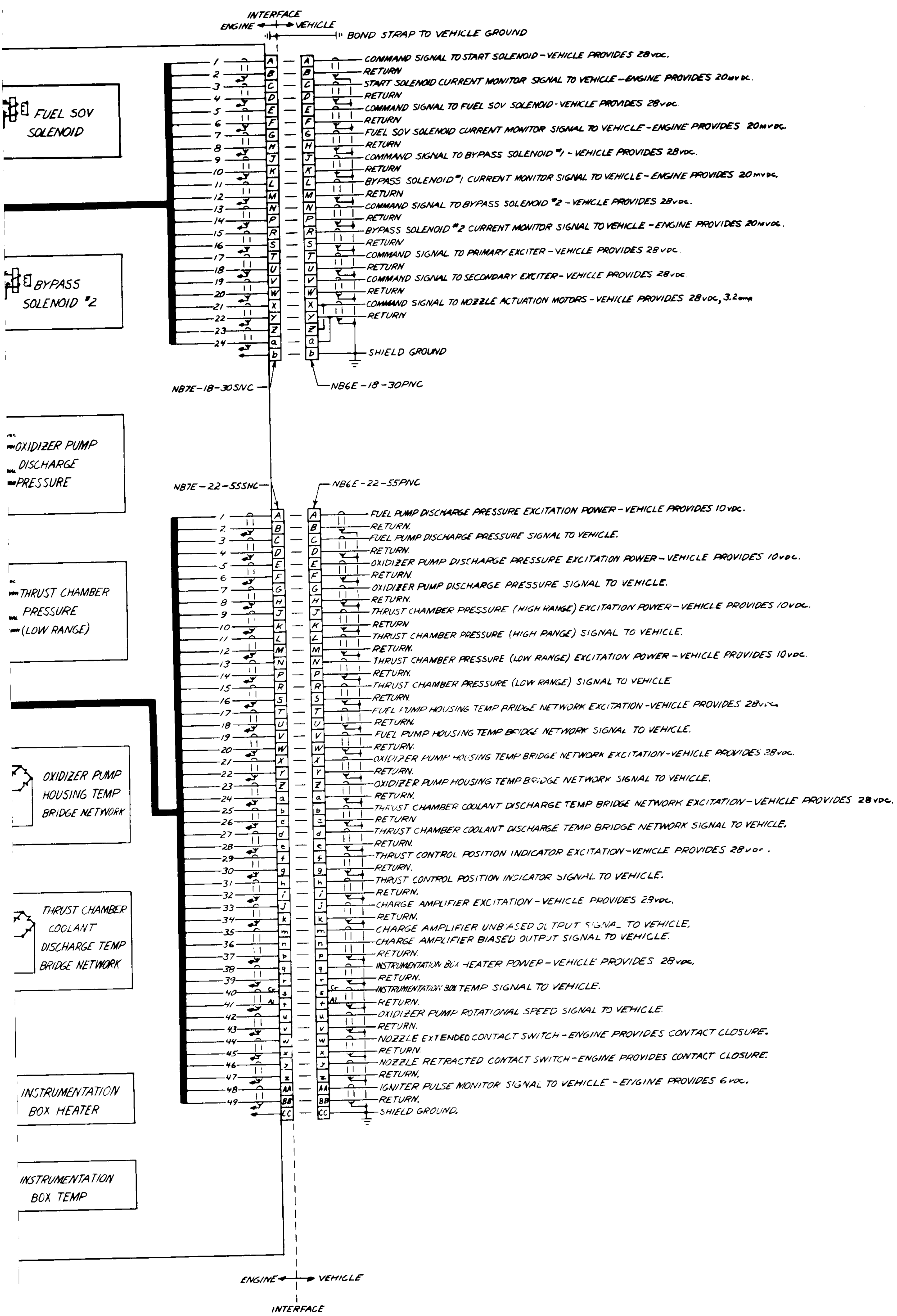

PRIMARY IGNITER
PRIMARY EXCITER
SECONDARY EXCITER
SECONDARY IGNITER
NOZZLE ACTUATION MOTOR #1
NOZZLE ACTUATION MOTOR #2
FUEL PUMP HOUSING TEMP
OXIDIZER PUMP HOUSING TEMP
THRUST CHAMBER COOLANT DISCHARGE TEMP
OXIDIZER PUMP ROTATIONAL SPEED
NOZZLE EXTENDED CONTACT SWITCH
NOZZLE RETRACTED CONTACT SWITCH
THRUST CONTROL POSITION INDICATOR
GEARBOX HOUSING ACCELEROMETER
START SOLENOID
BYPASS SOLENOID #1
INSTRUMENTATION BOX
FUEL PUMP DISCHARGE PRESSURE
THRUST CHAMBER PRESSURE (HIGH RANGE)
FUEL PUMP HOUSING TEMP BRIDGE NETWORK
THRUST CONTROL POSITION INDICATOR SIGNAL CONDITIONING
CHARGE AMPLIFIER
SHIELD GROUND
RETURN
6 VDC
28 VDC
SIGNAL

Figure C-167. Wiring Schematic


INTERFACE
ENGINE
VEHICLE
BOND STRAP TO VEHICLE GROUND
FUEL SOV SOLENOID
BYPASS SOLENOID #2
OXIDIZER PUMP DISCHARGE PRESSURE
THRUST CHAMBER PRESSURE (LOW RANGE)
OXIDIZER PUMP HOUSING TEMP BRIDGE NETWORK
THRUST CHAMBER COOLANT DISCHARGE TEMP BRIDGE NETWORK
INSTRUMENTATION BOX HEATER
INSTRUMENTATION BOX TEMP
COMMAND SIGNAL TO START SOLENOID - VEHICLE PROVIDES 28vdc.
RETURN
START SOLENOID CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20mvdc.
RETURN
COMMAND SIGNAL TO FUEL SOV SOLENOID - VEHICLE PROVIDES 28vdc.
RETURN
FUEL SOV SOLENOID CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20mvdc.
RETURN
COMMAND SIGNAL TO BYPASS SOLENOID #1 - VEHICLE PROVIDES 28vdc.
RETURN
BYPASS SOLENOID #1 CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20mvdc.
RETURN
COMMAND SIGNAL TO BYPASS SOLENOID #2 - VEHICLE PROVIDES 28vdc.
RETURN
BYPASS SOLENOID #2 CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20mvdc.
RETURN
COMMAND SIGNAL TO PRIMARY EXCITER - VEHICLE PROVIDES 28vdc.
RETURN
COMMAND SIGNAL TO SECONDARY EXCITER - VEHICLE PROVIDES 28vdc.
RETURN
COMMAND SIGNAL TO NOZZLE ACTUATION MOTORS - VEHICLE PROVIDES 28vdc, 3.2amp
RETURN
SHIELD GROUND
NB7E-18-30SNC
NB6E-18-30PNC
NB7E-22-55SNC
NB6E-22-55PNC
FUEL PUMP DISCHARGE PRESSURE EXCITATION POWER - VEHICLE PROVIDES 10vdc.
RETURN.
FUEL PUMP DISCHARGE PRESSURE SIGNAL TO VEHICLE.
RETURN.
OXIDIZER PUMP DISCHARGE PRESSURE EXCITATION POWER - VEHICLE PROVIDES 10vdc.
RETURN.
OXIDIZER PUMP DISCHARGE PRESSURE SIGNAL TO VEHICLE.
RETURN.
THRUST CHAMBER PRESSURE (HIGH RANGE) EXCITATION POWER - VEHICLE PROVIDES 10vdc.
RETURN
THRUST CHAMBER PRESSURE (HIGH RANGE) SIGNAL TO VEHICLE.
RETURN.
THRUST CHAMBER PRESSURE (LOW RANGE) EXCITATION POWER - VEHICLE PROVIDES 10vdc.
RETURN.
THRUST CHAMBER PRESSURE (LOW RANGE) SIGNAL TO VEHICLE
RETURN.
FUEL PUMP HOUSING TEMP BRIDGE NETWORK EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN.
FUEL PUMP HOUSING TEMP BRIDGE NETWORK SIGNAL TO VEHICLE.
RETURN.
OXIDIZER PUMP HOUSING TEMP BRIDGE NETWORK EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN.
OXIDIZER PUMP HOUSING TEMP BRIDGE NETWORK SIGNAL TO VEHICLE.
RETURN.
THRUST CHAMBER COOLANT DISCHARGE TEMP BRIDGE NETWORK EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN
THRUST CHAMBER COOLANT DISCHARGE TEMP BRIDGE NETWORK SIGNAL TO VEHICLE.
RETURN.
THRUST CONTROL POSITION INDICATOR EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN.
THRUST CONTROL POSITION INDICATOR SIGNAL TO VEHICLE.
RETURN.
CHARGE AMPLIFIER EXCITATION - VEHICLE PROVIDES 28vdc.
RETURN.
CHARGE AMPLIFIER UNBIASED OUTPUT SIGNAL TO VEHICLE.
CHARGE AMPLIFIER BIASED OUTPUT SIGNAL TO VEHICLE.
RETURN.
INSTRUMENTATION BOX HEATER POWER - VEHICLE PROVIDES 28vdc.
RETURN.
INSTRUMENTATION BOX TEMP SIGNAL TO VEHICLE.
Cr
Al
RETURN.
OXIDIZER PUMP ROTATIONAL SPEED SIGNAL TO VEHICLE.
RETURN.
NOZZLE EXTENDED CONTACT SWITCH - ENGINE PROVIDES CONTACT CLOSURE.
RETURN.
NOZZLE RETRACTED CONTACT SWITCH - ENGINE PROVIDES CONTACT CLOSURE.
RETURN.
IGNITER PULSE MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 6vdc.
RETURN.
SHIELD GROUND.
ENGINE
VEHICLE
INTERFACE

The oxidizer pump rotational speed unit is a magnetic pickup device that senses the passing blades of the oxidizer pump. Its output frequency is proportional to blade passing speed. The thrust control position indicator is a linear variable differential transformer (LVDT) with a movable core that is connected to thrust control. With an ac excitation from the signal conditioning unit, the LVDT provides ac output proportional to changes in the thrust control. This ac output is then converted to an ac signal by the signal conditioning unit.

The gearbox housing accelerometer is a vibration sensor located on the gearbox and it provides an output to the change amplifier for conditioning.

4.2.6 Engine Installation

The RL10 Derivative engines are designed for maximum interchangeability with each other to ease vehicle system integration. The mechanical connections between the engine and vehicle consist of the gimbal thrust mount and actuator attachment points, propellant inlets, pneumatic and electrical supply connections, and tank pressurization flow supply points. Mechanical interfaces for this engine are shown in the installation drawing, figure C-168. Installation dimensions are shown in table C-33 (also see figure C-169 for dimensional references).

In order to clear the retractable nozzle, all components and tubes of the powerhead are packaged within a 20-in. radius of the engine centerline. The envelope of this engine extends forward of the engine/vehicle thrust mount interface by 8 inches. This space was required for the extendible nozzle jackshafts and their support structure and the movable portion of the quick disconnect nozzle feed.

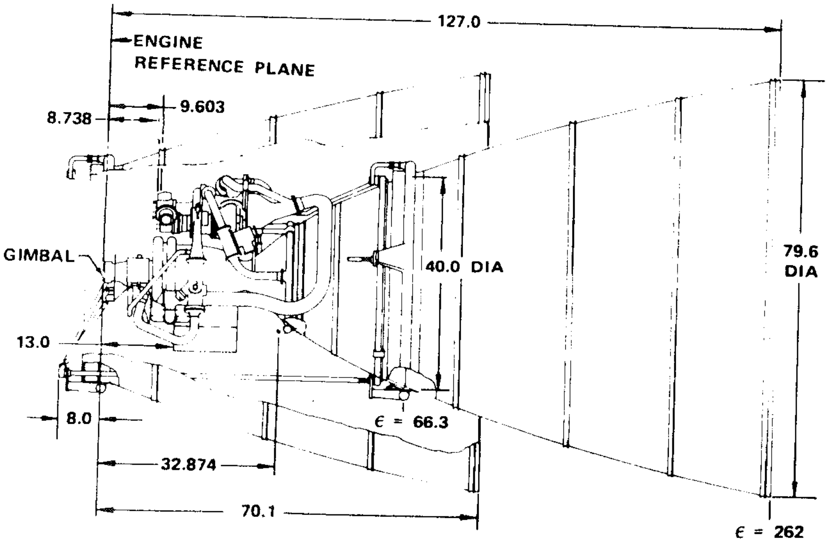


Figure C-168(1). Derivative IIB Engine Installation Drawing (Sheet 1) FD 72043B

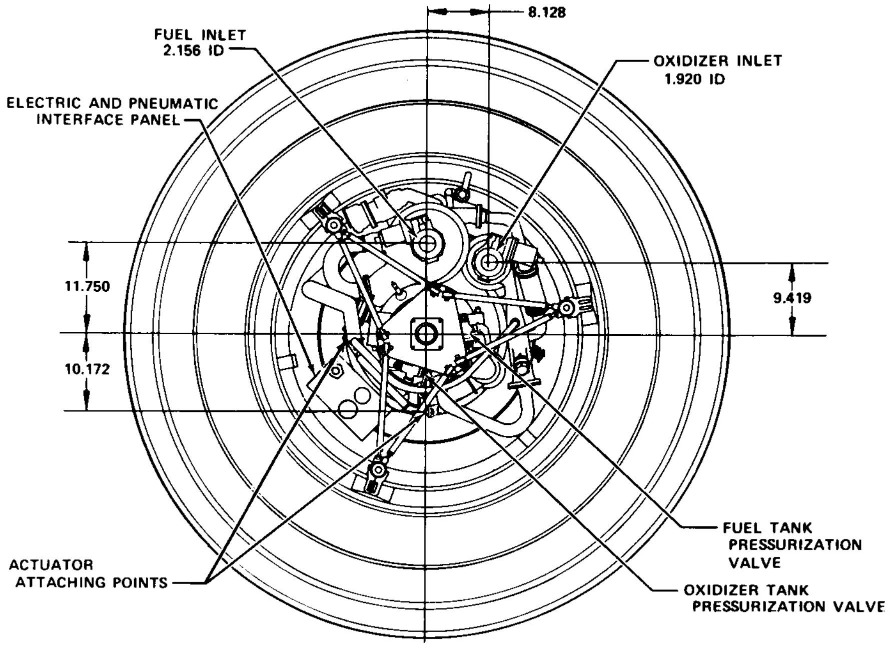

8.128
FUEL INLET
2.156 ID
ELECTRIC AND PNEUMATIC
INTERFACE PANEL
OXIDIZER INLET
1.920 ID
11.750
10.172
9.419
FUEL TANK
PRESSURIZATION
VALVE
ACTUATOR
ATTACHING POINTS
OXIDIZER TANK
PRESSURIZATION VALVE

Figure C-168(2). Derivative IIB Engine Installation Drawing (Sheet 2)

Table C-33. Dimensional Characteristics Derivative IIB

Dimension	Description	
A	Y Axis to Fuel Inlet	11.750
B	Radius to Actuator Attach	10.172
C	Z Axis to Oxidizer Inlet	8.128
D	Y Axis to Oxidizer Inlet	9.419
E	ERP to Oxidizer Inlet	9.603
F	ERP to Fuel Inlet	8.738
G	Interface Diameter	2.876
H	Interface Height	0.240
J	ERP to Gimbal Plane	1.500
K	ERP to Actuator Attachment Point	32.874
L	Engine Length (EXT)	127.0
	(RTC)	70.1
M	Exit Diameter	79.6

All Dimensions in Inches

The following is a description of individual vehicle/engine interfaces:

(a) Gimbal Thrust Mount

The gimbal thrust mount is a mechanical connection at the forward end of the engine and provides a universal bearing system to allow gimbaling of the engine for thrust vectoring. The gimbal attachment shown in figure C-170 consists of an aluminum pedestal with four bolt holes and a conical mount fastened to the engine by six bolts that pass through the bottom and thread into the propellant injector.

The gimbaling action is accomplished by virtue of a steel spider that connects the pedestal to the conical mount. The spider pins are coated with a dry lubricant and pivot in copper-beryllium inserts. These parts permit a gimbal movement of ±4 degrees in a square pattern.

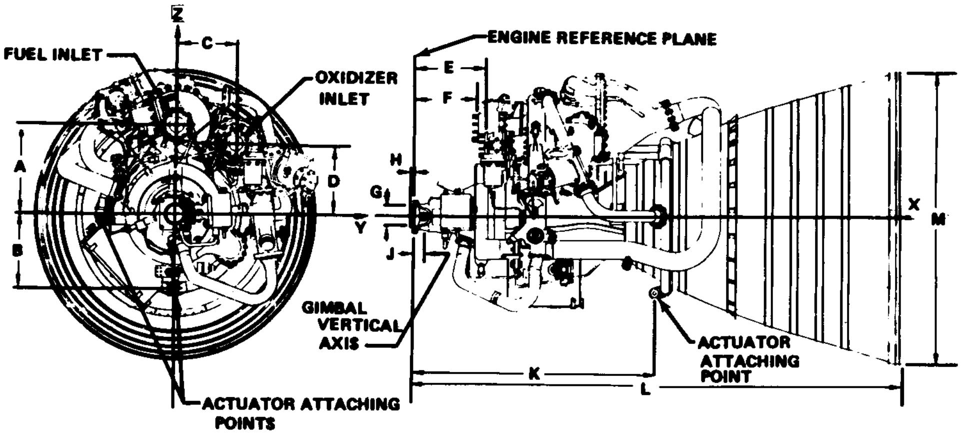
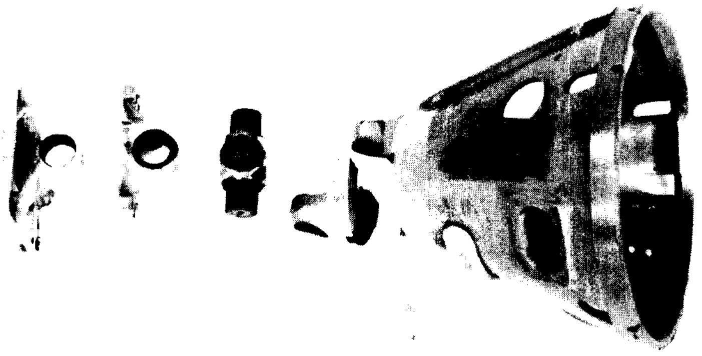

Z
C
FUEL INLET
OXIDIZER INLET
A
B
D
H
G
Y
J
E
F
ENGINE REFERENCE PLANE
X
M
GIMBAL VERTICAL AXIS
ACTUATOR ATTACHING POINTS
K
L
ACTUATOR ATTACHING POINT

Figure C-169. RL10 Outline Installation Drawing FD 68853



GIMBAL ASSEMBLY P/N 2118942 S/N ABE 759 AFTER A 2600 CYCLE TEST AT A 15,000 LB. LOAD AND A VACUUM ENVIRONMENT OF 6 X 10^{-8} TORR AND -160°F

Figure C-170. Gimbal Assembly FE 61074

(b) Actuator Attachment

The actuator attachments are located on the primary nozzle coolant inlet manifold. The two attachments, located 90 deg apart, are of the clevis type. A bolt through the clevis and actuator provides rotation for gimbaling and absorbs inplane thrust loads. The position of these two clevises is the same as that of the RL10A-3-3 engine for interchangeability convenience.

(c) Propellant Inlets

The propellant inlet connections are made at the fuel and oxidizer inlet shutoff valves as shown in figures C-171 and C-172. These interfaces are located below the gimbal interface at the same radial location on all RL10 derivative engines. The inlet diameters and axial locations are identical to those of the RL10A-3-3 engine.

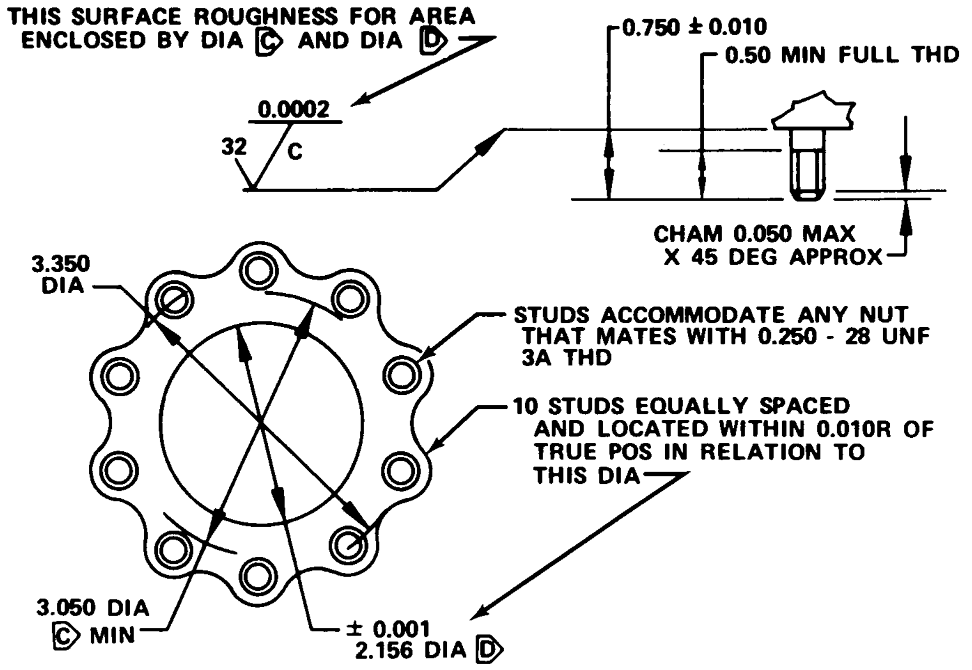

THIS SURFACE ROUGHNESS FOR AREA ENCLOSED BY DIA C AND DIA D
0.0002
32
C
0.750 ± 0.010
0.50 MIN FULL THD
CHAM 0.050 MAX X 45 DEG APPROX
3.350 DIA
STUDS ACCOMMODATE ANY NUT THAT MATES WITH 0.250 - 28 UNF 3A THD
10 STUDS EQUALLY SPACED AND LOCATED WITHIN 0.010R OF TRUE POS IN RELATION TO THIS DIA
3.050 DIA C MIN
± 0.001 2.156 DIA D

Figure C-171. Engine Fuel Inlet, Category I and Derivative IIB Engines FD 66800

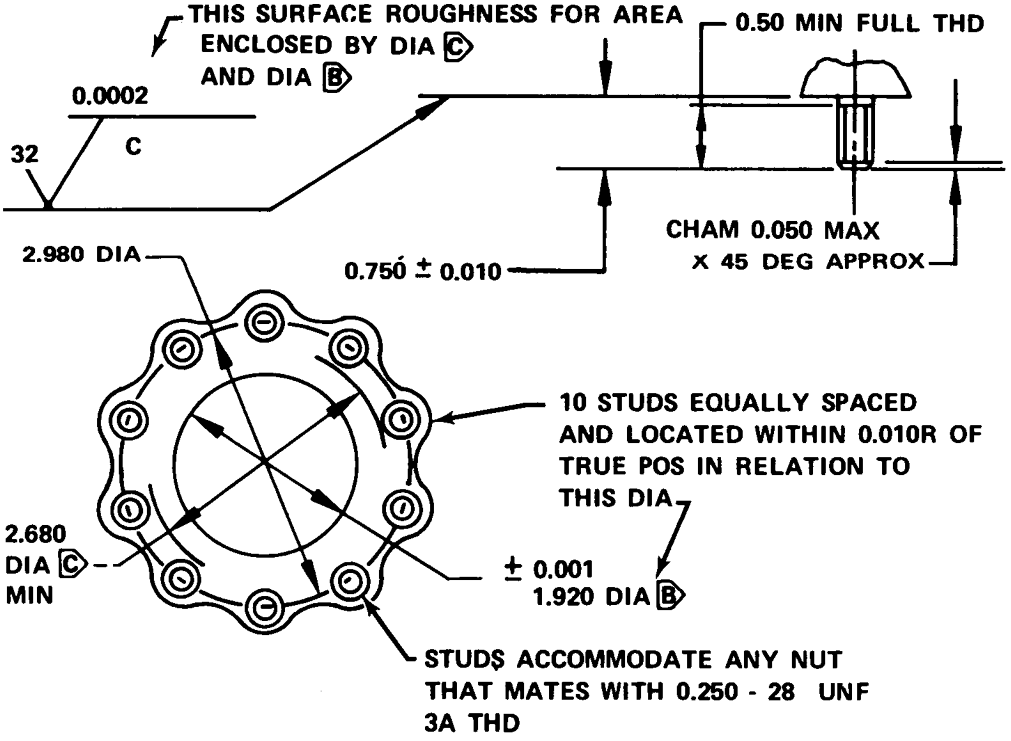

THIS SURFACE ROUGHNESS FOR AREA ENCLOSED BY DIA C AND DIA B
0.0002
32
C
0.50 MIN FULL THD
CHAM 0.050 MAX X 45 DEG APPROX
2.980 DIA
0.750 ± 0.010
10 STUDS EQUALLY SPACED AND LOCATED WITHIN 0.010R OF TRUE POS IN RELATION TO THIS DIA
2.680 DIA C MIN
± 0.001 1.920 DIA B
STUDS ACCOMMODATE ANY NUT THAT MATES WITH 0.250 - 28 UNF 3A THD

Figure C-172. Engine Oxidizer Inlet, Category I and Derivative IIB Engine FD 66801

(d) Tank Pressurization Connection

The hydrogen and oxygen tank pressurant supply connections are located at the forward end of the engine near the engine centerline as previously shown in figure C-168. The connections are made directly to the respective tank pressurization valves by use of a male AN-16 fitting.

(e) Electrical-Pneumatic Interfaces

These interfaces are provided by a single interface panel on the instrumentation enclosure as shown in figure C-173. The location of this panel is common to all derivative engines. There is no pneumatic (helium) connection and two electric connectors. The two electric connectors interface the vehicle with the engine command harness, and instrumentation harness. A more detailed description of the electric interface design may be found in Section 4.2.5.

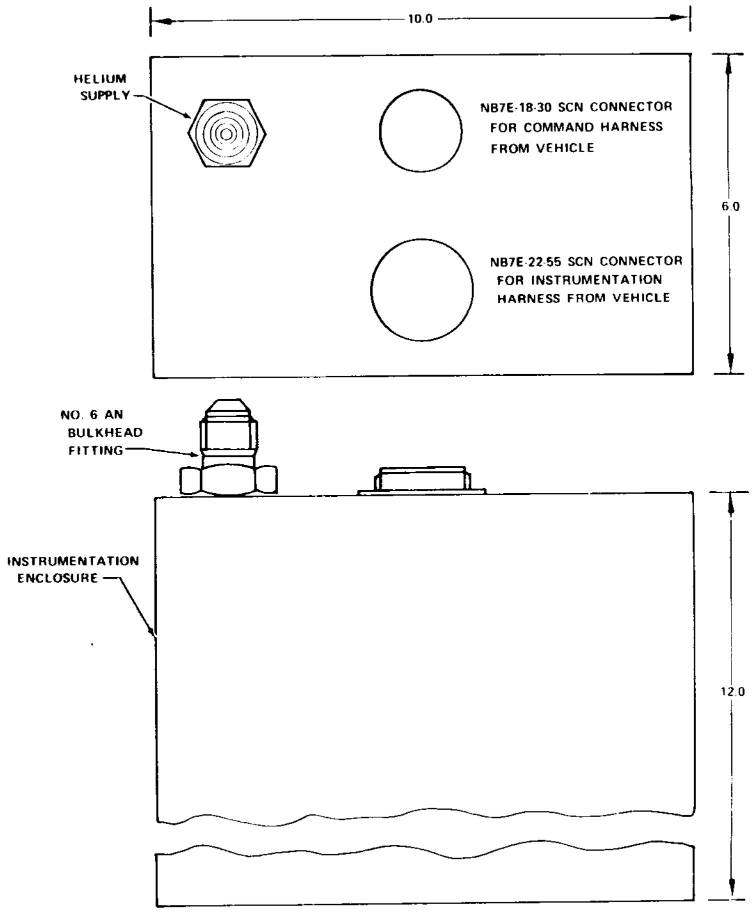

10.0
HELIUM SUPPLY
NB7E-18-30 SCN CONNECTOR FOR COMMAND HARNESS FROM VEHICLE
6.0
NB7E-22-55 SCN CONNECTOR FOR INSTRUMENTATION HARNESS FROM VEHICLE
NO. 6 AN BULKHEAD FITTING
INSTRUMENTATION ENCLOSURE
12.0

Figure C-173. Pneumatic and Electrical Vehicle/ Engine Interfaces FD 76011

4.2.7 Weights, Center of Gravity and Moments of Inertia

The weights shown for the Derivative IIB engine have been obtained from known RL10A-3-3 component weights, from weights calculated from conceptual design layout drawings and from estimates based on similar hardware. Items for which weight was estimated include miscellaneous hardware such as small plumbing, connectors, and brackets, and instrumentation. The weight breakdown of the Derivative IIB engine is shown in table C-34 by detail, subassembly and total engine. Sources for the weight are also listed, i.e., estimated, computed or weighed. The total engine weight shown does not include TVC actuators, propellant utilization actuator, accessory drive equipment, or nonpropulsive vents.

Table C-34. Derivative IIB Weight Breakdown

Component	Estimated, Computed, or Weighed	Engine Weight, lb
Thrust Chamber and Primary Nozzle		
Injector Assembly	Weighed	14.9
Thrust Chamber and Nozzle/Primary	31% Weighed 69% Computed	83.0
Support Ring and Seal	Computed	12.0
Subtotal		109.9
Extendible Nozzle		
Extendible Nozzle	Computed	104.0
Subtotal		104.0
Extendible Nozzle Actuator System		
Extendible Nozzle Actuation System	Computed	43.0
Subtotal		43.0
Turbopumps and Gearbox		
Turbopump	Weighed	75.1
Turbopump Mounts	Weighed	4.0
Subtotal		79.1
GO_2 Heat Exchanger		
GO_2 Heat Exchanger	Computed	13.0
Subtotal		13.0
Ignition System		
Ignition System	Weighed	15.0
Subtotal		15.0
Controls, Valves, Actuators		
Oxidizer Inlet Shutoff Valve	Weighed	5.6
Fuel Inlet Shutoff Valve	Weighed	5.8
Oxidizer Flow Control Valve	Computed	9.0
Gaseous Oxidizer Valve	Computed	2.0
Fuel Vent Valve	Weighed	6.3
Turbine Bypass Valve	Computed	4.2
Thrust Control Valve	Weighed	5.3

Table C-34. Derivative IIB Weight Breakdown (Continued)

Component	Estimated, Computed, or Weighed	Engine Weight, lb
Nozzle Coolant Valve	Computed	2.0
Main Fuel Shutoff Valve	Weighed	3.4
Tank Pressurizing Valves	Computed	4.0
Solenoid Valves	Estimated	10.0
Instrumentation	Estimated	8.8
Subtotal		66.4
Plumbing and Miscellaneous Hardware		
Tube - Oxidizer Control to Injector	Estimated	2.5
Tube - GO_2 Hex Supply and Exit	Estimated	2.0
Tube - Fuel Pump to Vent Valve	Weighed	1.2
Tube - Vent Valve to Thrust Chamber	Weighed	1.6
Tube - Thrust Chamber to Turbine	Estimated	5.5
Tube - Turbine to Main Fuel Shutoff Valve	Estimated	8.4
Tube - Turbine Bypass	Estimated	4.0
Engine Mount	Weighed	8.0
Small Lines	Estimated	2.2
Connecting and Miscellaneous Hardware	Estimated	8.2
Subtotal		43.6
TOTAL ENGINE WEIGHT		474.0

Note 1: Percent Weighed - 36
Percent Computed - 53
Percent Estimated - 11

The estimated maximum dry weight and the estimated wet weight at normal operating conditions are shown below:

Basic Dry Weight, lb	Propellant Weight, lb	Total Wet Weight, lb
474	8	482

Engine center of gravity was determined by the moment method using the weights of table C-34 and the moment arms known, or modified, from the RL10A-3-3. New components had moment arms determined by calculation, or for small parts, estimation.

Axial (X), Nozzle Retracted	Axial (X), Nozzle Extended	Horizontal (Y)	Vertical (Z)
26.1 in.	39.2 in.	1.0 in.	2.2 in.

The gimbaled moment of inertia for the Derivative IIB engine was determined from the summation of the products of the component mass times its cg distance to the gimbal center of rotation squared. While this method ignores the individual component mass moment of inertia, the preliminary nature of the study justifies its use. The estimated gimbaled moments of inertia follow and are in units of in.-lb-sec^2.

I_{xx}	I_{yy}	I_{zz}
480	3540	3520

4.3 Category IV Engine

4.3.1 Category IV Thrust Chamber and Primary Nozzle

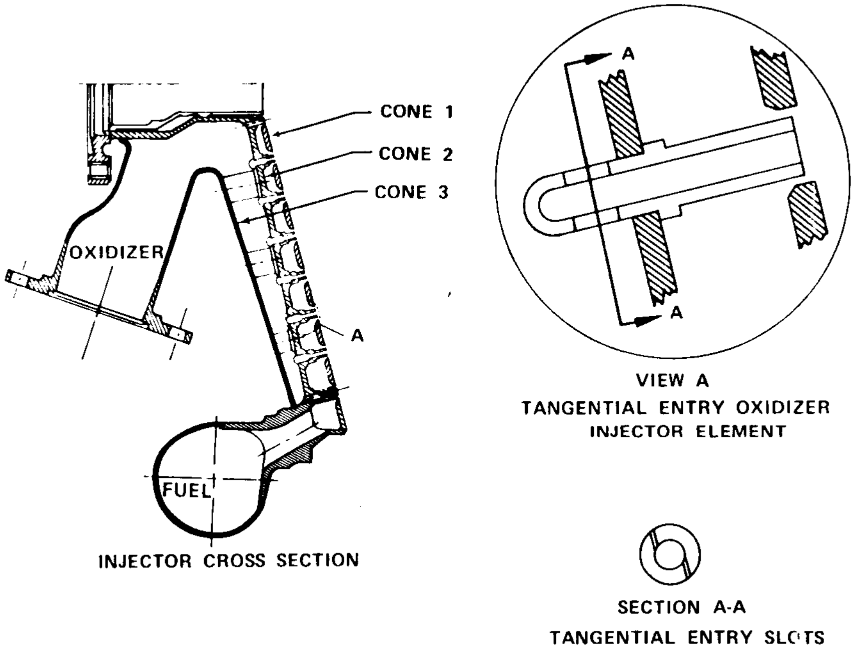
The primary function of the thrust chamber assembly is to mix and burn the propellants and expand them in the thrust nozzle to produce thrust. It also serves as a heat exchanger to supply turbine power for propellant pumps and provides support for the extendible nozzle and actuation system. The Category IV engine thrust chamber assembly consists of three major sections: (1) an injector, (2) a nontubular, regeneratively cooled section from the injector to an area ratio of 6, and (3) a tubular regeneratively cooled nozzle from an area ratio of 6 to 125. The design differs from that of derivative engines in that it requires a nontubular regenerative section to withstand the high heat fluxes at the high chamber pressure level. Requirements for the Category IV engine chamber and primary nozzle are listed in table C-35.

Table C-35. Design Requirements for the Category IV Engine Chamber and Primary Nozzle

Parameter	Tank Head Idle	Maneuver Thrust	Full Thrust
Injector			
Fuel Inlet Pressure, psia	7.0	262.	1001.
Oxidizer Inlet Pressure, psia	15.5	245.	1066.
Fuel ΔP, psi	1.11	27.5	86.4
Oxidizer ΔP, psi	9.7	10.4	151.
Fuel Inlet Temperature, °R	599.	737.	530.
Oxidizer Inlet Temperature, °R	686.	166.	172.
Fuel Flowrate, lb/sec	0.03	1.08	4.26
Oxidizer Flowrate, lb/sec	0.15	7.19	27.4
Combustion Chamber and Primary Nozzle			
Chamber Pressure, psia	5.9	234.	915.
Chamber Mixture Ratio	4.75	6.68	6.42
Coolant Tube Inlet Pressure, psia	15.9	427.	2040.
Coolant Tube Inlet Temperature, °R	37.	47.1	72.8
Coolant Tube ΔP, psi	4.5	76.3	199.
Coolant Tube ΔT, °R	799.	705.	508.
Coolant Flowrate, lb/sec	0.03	1.08	4.26

4.3.1.1 Mechanical Description

The Category IV engine propellant injector is schematically depicted in figure C-174. The function of the propellant injector is to atomize the oxidizer and thoroughly mix fuel and oxidizer to provide the correct conditions for efficient combustion of propellants.



INJECTOR CROSS SECTION

VIEW A
TANGENTIAL ENTRY OXIDIZER
INJECTOR ELEMENT

SECTION A-A
TANGENTIAL ENTRY SLOTS

Figure C-174. Category IV Engine Injector FD 15541

The propellant injector consists of multiple injection elements arranged in equally spaced, concentric circles, each element consisting of an oxidizer tube and a concentric fuel orifice. Liquid oxygen enters the injector through the oxidizer injector manifold, flows into the cavity between cones 2 and 3, and then flows out of the oxidizer orifices and into the combustion chamber. The oxidizer is admitted to the injector element through two tangential slots swirling the oxidizer flow and promoting mixing with hydrogen flow at the end of the element.

Gaseous hydrogen enters the peripheral fuel injector manifold and flows into the cavity between cones 1 and 2. Most of the hydrogen flows out through the annular orifices around the elements, into the combustion chamber where it mixes with the oxidizer. Some hydrogen passes through cone 1, a porous-welded, steel-mesh plate. This flow provides transpiration cooling of the injector face (cone 1).

Immediate contact between oxidizer and fuel is made at each element as oxidizer and fuel leave the face of the injector and enter the combustion chamber. This configuration is designed to provide thorough combustion, high combustion efficiency, and high specific impulse.

The Category IV combustion chamber, throat, and upper nozzle ($\epsilon = 6.0$) are of a one piece, nontubular, regeneratively cooled design shown in figure C-175. The axial cooling passages are milled in a copper thrust chamber. These slots vary in width and depth along their axial length to achieve the desired local coolant flow velocities.

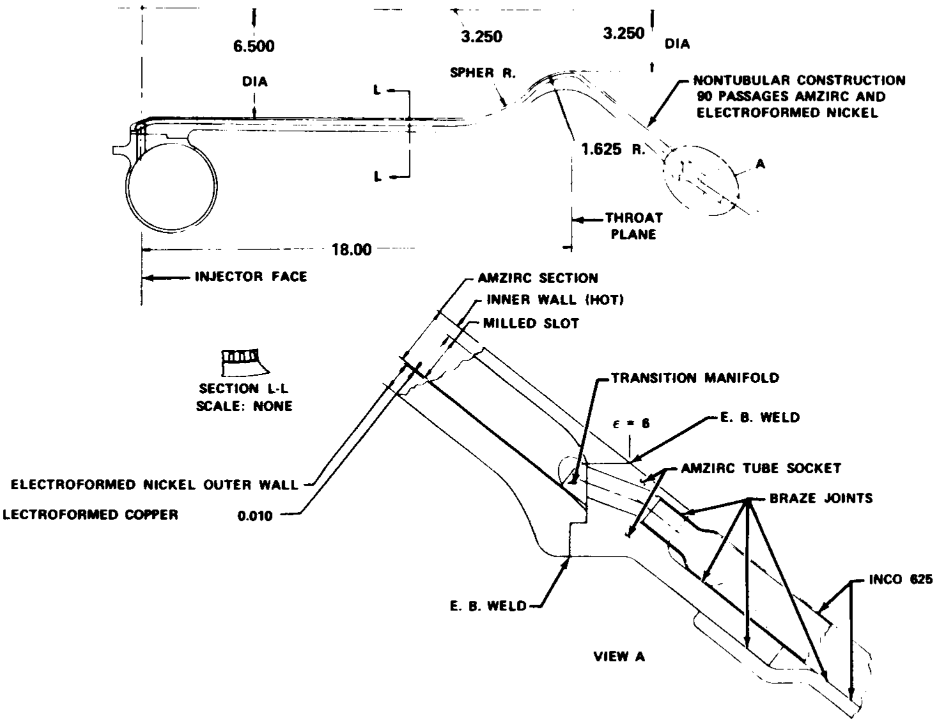

6.500
DIA
L
L
3.250
SPHER R.
3.250
DIA
NONTUBULAR CONSTRUCTION
90 PASSAGES AMZIRC AND
ELECTROFORMED NICKEL
1.625 R.
A
THROAT
PLANE
18.00
INJECTOR FACE
SECTION L-L
SCALE: NONE
AMZIRC SECTION
INNER WALL (HOT)
MILLED SLOT
TRANSITION MANIFOLD
ε = 6
E. B. WELD
AMZIRC TUBE SOCKET
BRAZE JOINTS
ELECTROFORMED NICKEL OUTER WALL
LECTROFORMED COPPER 0.010
INCO 625
E. B. WELD
VIEW A

Figure C-175. Category IV Thrust Chamber FD 68747C

The nontubular chamber is manufactured from a copper alloy containing 0.15% Zirconium. This alloy has improved thermal fatigue and strength properties over pure copper, but at a small loss in thermal conductivity. A nickel outer wall is electroformed over the slotted passages of the copper alloy chamber to provide a structural shell and complete the cooling jacket.

A copper alloy annular manifold, previously shown in figure C-175, serves as a transition piece between the nontubular nozzle with 90 flow passages, and the tubular nozzle with 180 flow passages. Coolant flows from 180 swaged tubes brazed into the transition piece into its annular manifold, where it is distributed through a weld joint into the 90 passages of the nontubular nozzle. This transition occurs at an area ratio of 6.

The tubular section of the primary thrust nozzle shown in figure C-176 is a regeneratively cooled, furnace-brazed assembly consisting of a fuel inlet manifold, 180 short, single-tapered tubes, a turnaround manifold, 180 full length, double-tapered tubes, a fuel exit section, and various stiffeners and component supports.

Hydrogen enters the primary nozzle at the inlet manifold at an area ratio of 30 and immediately flows into 180 single-tapered short tubes that are interleaved between 180 double-tapered, full-length tubes. The full-length tubes form the full periphery of the nozzle down to the junction of the short tubes. The periphery of the remainder of the nozzle is formed by all 360 tubes. The hydrogen flows rearward in the short tubes to the turnaround manifold at an area ratio of 125 where it enters the 180 full-length tubes and travels forward the entire length of the tubular nozzle section to enter the nontubular nozzle section through the transition manifold. This partial two-pass method of chamber construction was adopted to achieve high coolant velocity and heat transfer, and low tube-wall temperature. Both full-length and short tubes are brazed together to form a seal, and are structurally supported by stiffener bands to carry the chamber hoop loads and minimize the effect of any flow-induced vibration.

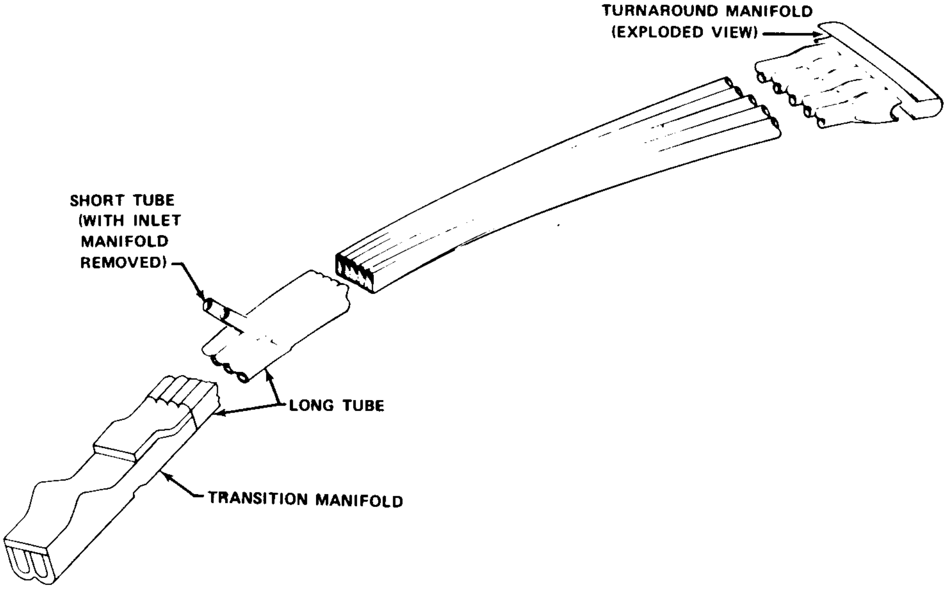

TURNAROUND MANIFOLD (EXPLODED VIEW)
SHORT TUBE (WITH INLET MANIFOLD REMOVED)
LONG TUBE
TRANSITION MANIFOLD

Figure C-176. Thrust Chamber Construction FD 4269A

Thrust chamber ignition is provided by a torch igniter system as shown in figure C-177. A metered flow of hydrogen and oxygen is mixed in an igniter chamber, ignited by a spark, and passed into the combustion chamber to ignite the main propellants. Increased reliability is accomplished by providing dual exciters and spark igniters and by eliminating the need for igniter propellant shutoff valves. The dual spark and exciter configuration provides a fail-safe energy source and designing the igniter to operate at rated thrust with oxidizer and fuel igniter flows eliminates the possibility of igniter damage due to valve leakage.

The fuel and oxidizer is ignited by a spark exciter assembly which provides a minimum of 20 sparks/sec at an energy level of 0.1 joules. The total oxidizer flow is injected into the igniter through a single oxidizer element located in the upper end of the igniter chamber. Fuel flow is split. Part of the flow is delivered to a concentric slot surrounding the oxidizer injector element and the remainder is used for igniter barrel cooling. The burned propellants are discharged into the main chamber through the igniter injector sleeve.

This type of igniter configuration with the exception of the lightweight exciters was standard equipment on the RL10A-3-7 engine and was tested extensively under conditions from tank head idle start to full thrust. Ignition studies of

Derivative engines are reported in Section E and a history of RL10 igniter experience may be found in Appendix I.

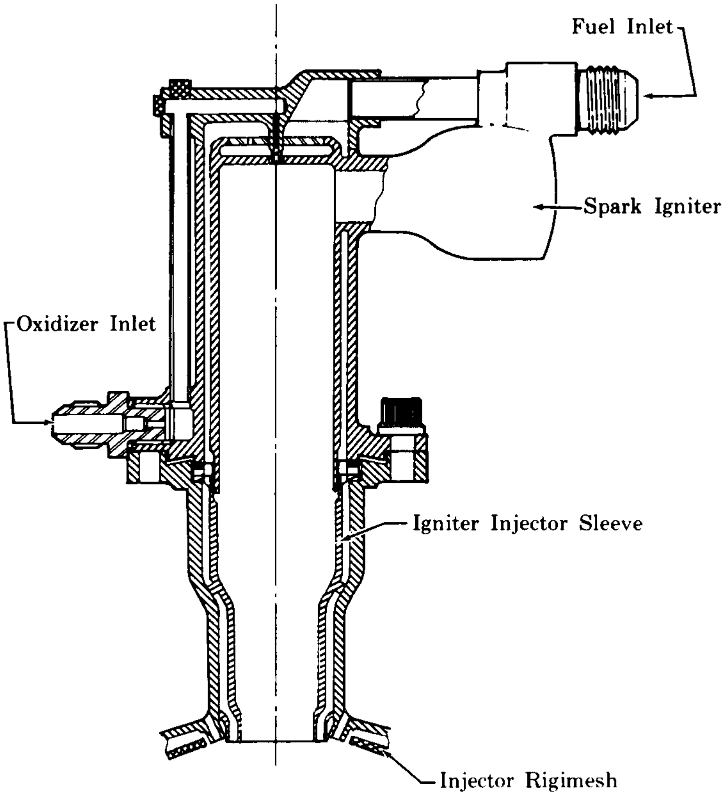


Figure C-177. Category IV Torch Igniter FD 13241

4.3.1.2 Design Studies

High heat flux levels (figure C-178) require high coolant velocities, and therefore, small coolant passages. The nontubular coolant passage concept is used in design of the Category IV engine combustion chamber and nozzle throat region. A high thermal conductivity copper based alloy AMZIRC material is selected to maintain combustion and sidewall temperatures at low levels to provide adequate cycle life. Two-dimensional heat transfer into the material between coolant passages whose fin effect should provide more efficient heat transfer, increase the allowable heat flux and/or reduce the required coolant pressure drop. Hot wall temperature distribution along the chamber wall is estimated in figure C-179 showing that peak temperatures (1425°R) occur close to the injector.

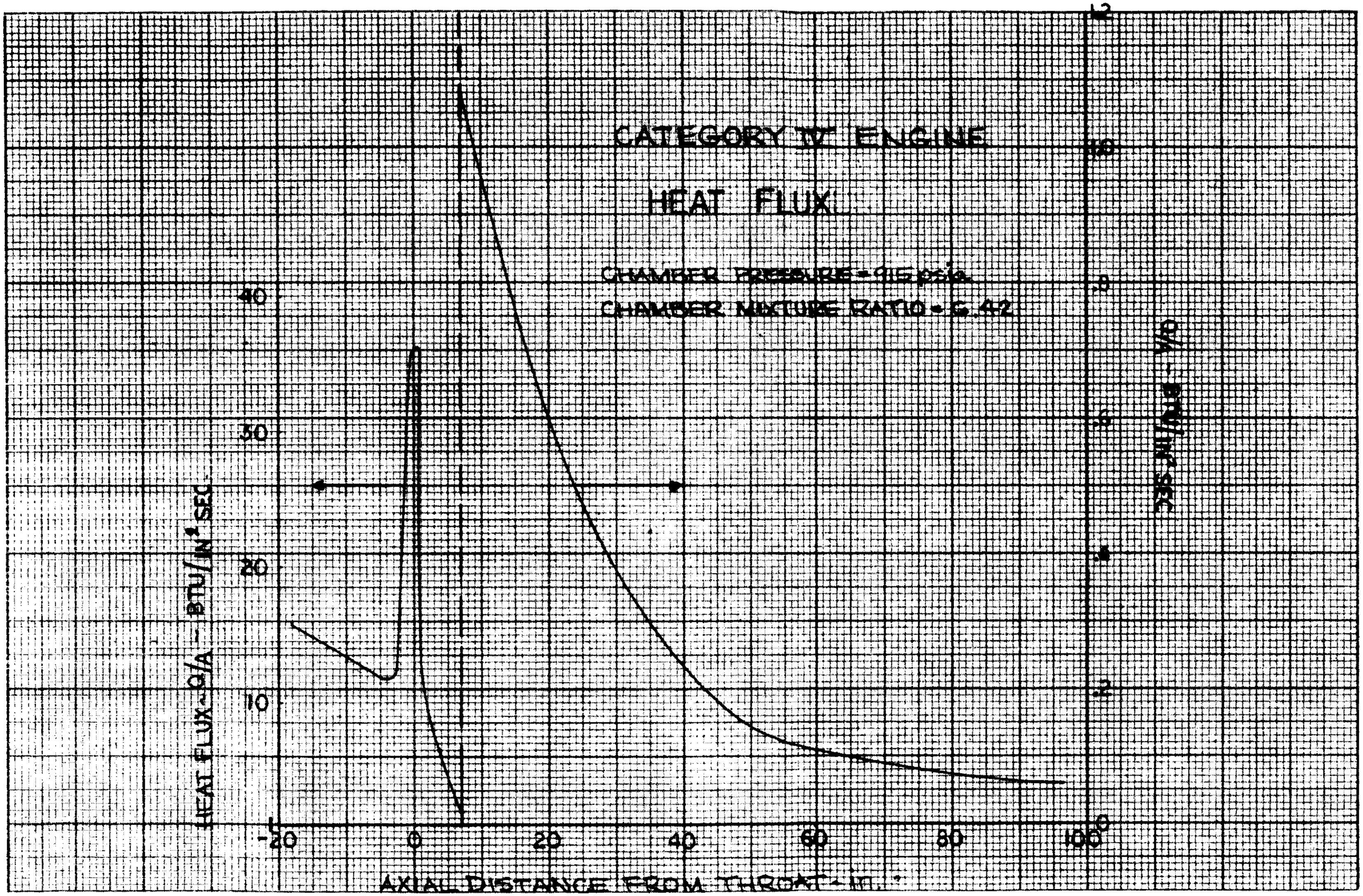


Figure C-178. Category IV Engine Heat Flux

DF 98279

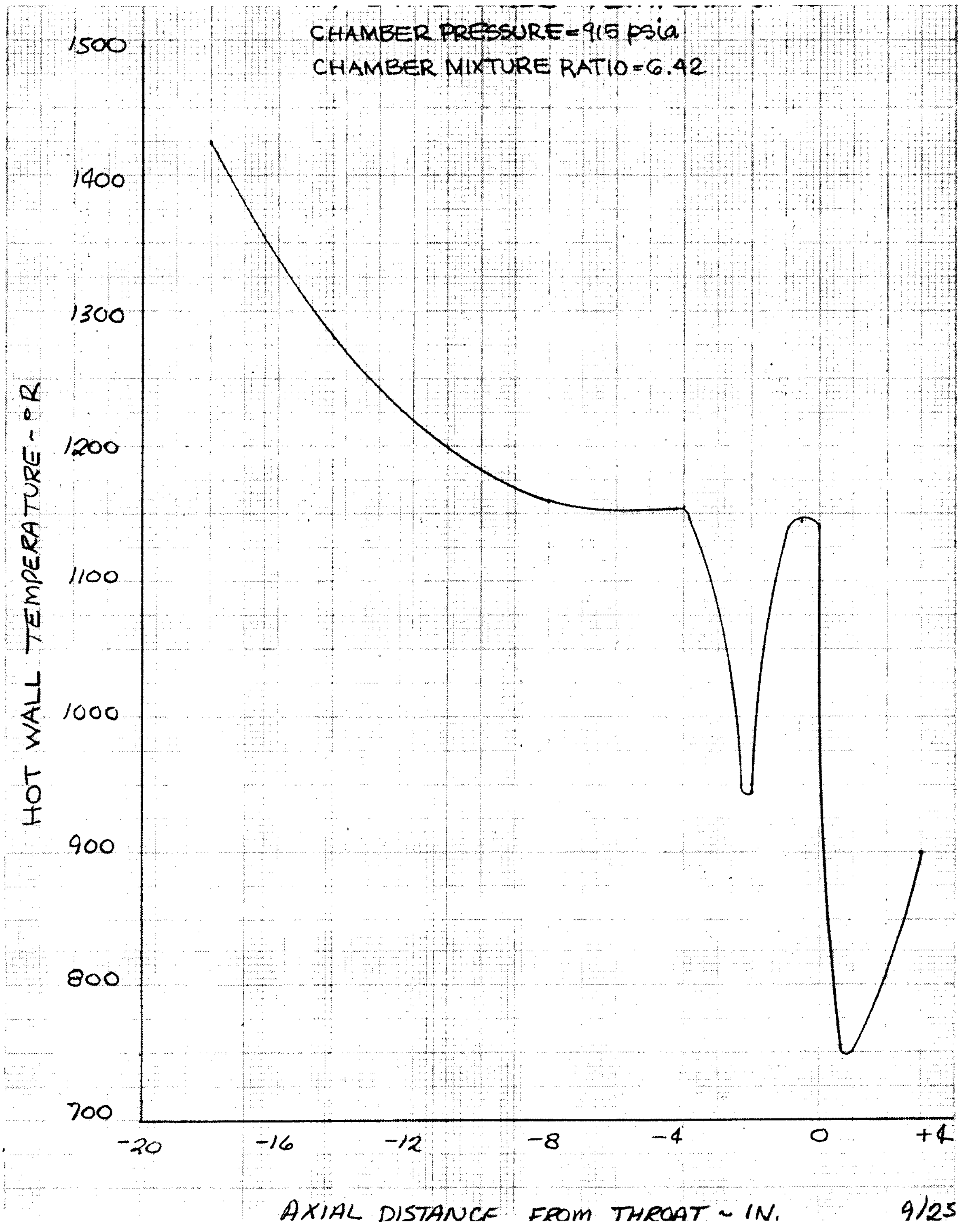


Figure C-179. Category IV Engine Nontubular Chamber Steady-State Wall Temperatures

DF 98280

The coolant side and combustion side convective environments have been determined from a steady-state heat transfer analysis of the entire thrust chamber. Computer formulations were used that allow the determination of the combustion side and coolant side convective environments (temperatures and convective film coefficients) from the input thrust chamber geometries and engine operating conditions (chamber pressure and mixture ratio). Coolant pressure losses and wall temperatures along the chamber walls, based on one-dimensional analyses were predicted. The combustion gas convective environment was determined using the Mayer integral boundary layer analysis (Reference 5) and enthalpy driving potential. The coolant side environment was predicted using empirically determined correlations for hydrogen heat transfer coefficients (Reference 4) modified to account for the effects of passage surface roughness using Dipprey and Sobersky method and passage curvature. A surface roughness of 64μ in. has been used for the heat transfer analysis as a standard value for machined surfaces that should be readily attainable.

A two-dimensional finite element conduction heat transfer analysis was used to determine the passage temperature fields. At each selected location, the passage cross section has been divided into a fixed number of unit depth masses or nodes. The interior and exterior boundary nodes have been subjected to the coolant side and combustion side environments. Heat transfer by radiation is considered to be negligible. A steady-state heat balance has been made for each node simultaneously to determine its operating temperature.

The thermal fatigue properties of pure copper can be improved with only small reduction in conductivity by alloying with small amounts of other metals. The alloy used in the Category IV RL10 chamber design is AMZIRC, an alloy containing 99.85% oxygen-free copper and 0.15% zirconium produced by AMAX, Inc. The AMZIRC is obtained as a forging and cold worked by spinning to rough shape on a mandrel to increase strength. After spinning, the ID surface is final machined and coolant channels are milled to the dimensions shown in figure C-180. The rounded channel shape, shown in figure C-181, is milled on the ID and OD walls allowing the walls to locally bend relieving the compressive stresses existing on the hot wall due to thermal gradients.

A thin copper layer, about 0.010 in. in thickness is provided as the primary passage protective barrier closure material between the hydrogen coolant and a nickel outer shell to alleviate possible property reducing hydrogen embrittlement of the nickel. The structural nickel outer closure is electroformed over the milled liner outer surfaces to complete the cooling passages as shown in figure C-181.

The materials and fabrication techniques described above were used in the construction of a 250,000 lb engine thrust chamber that was subjected to 3000 psia chamber pressure test firings conducted by P&WA during 1971. A photograph of the milled slot liner used in the 250,000 lb thrust engine testing is presented in figure C-182.

The breakpoint between the nontubular section and the tubular section was established at an area ratio of 6 based on the minimum tube wall thickness that can be reasonably fabricated and cooled, and the flow passage cross-sectional area requirements. The tubular regenerative nozzle shown in figure C-183 is identical in concept to the tubular nozzle used on the RL10A-3-3 production engine and requires no new design innovation for this design. The selected tube geometry is listed in figure C-184.

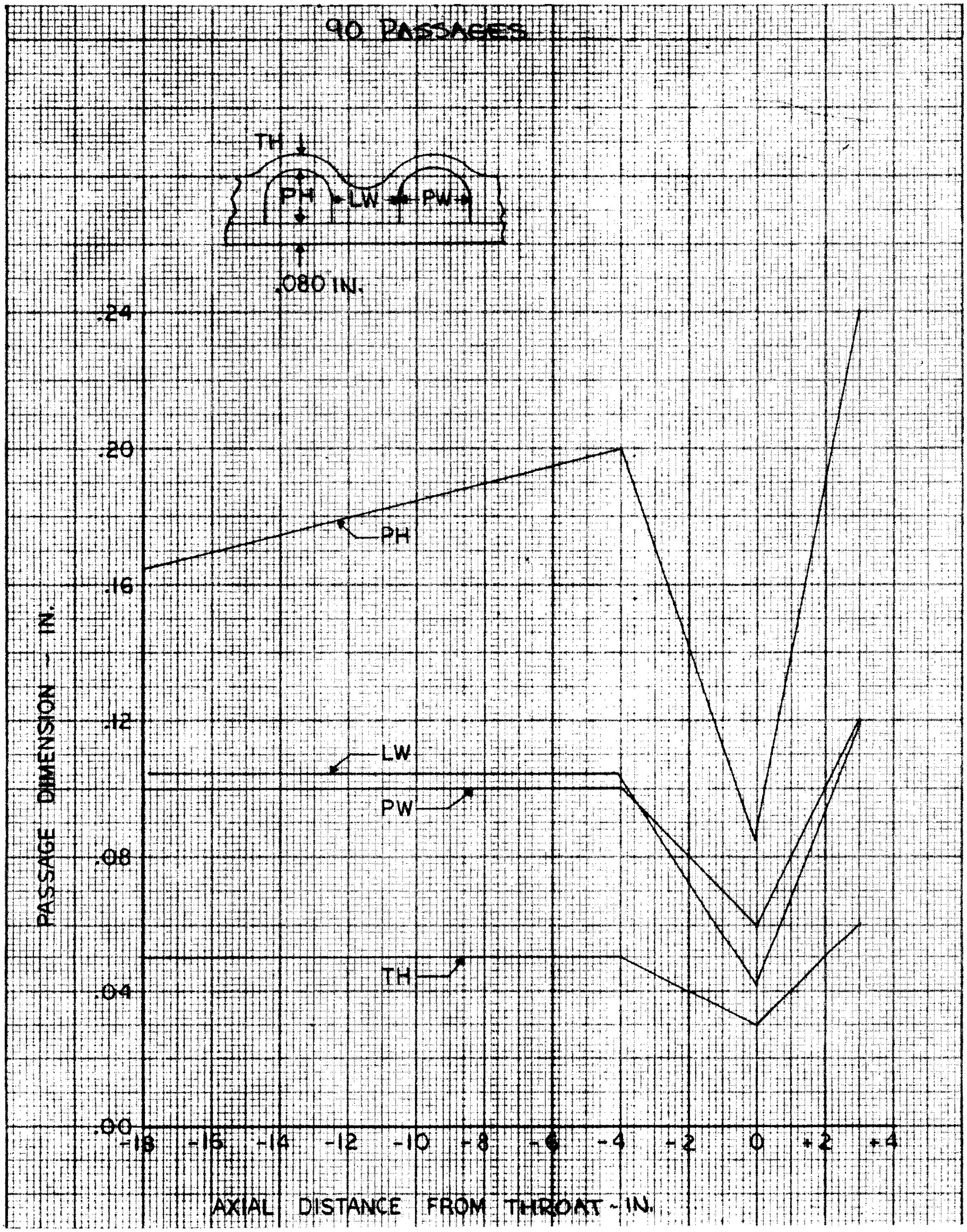


Figure C-180. Nontubular Chamber Passage Dimensions Category IV Engine DF 98281

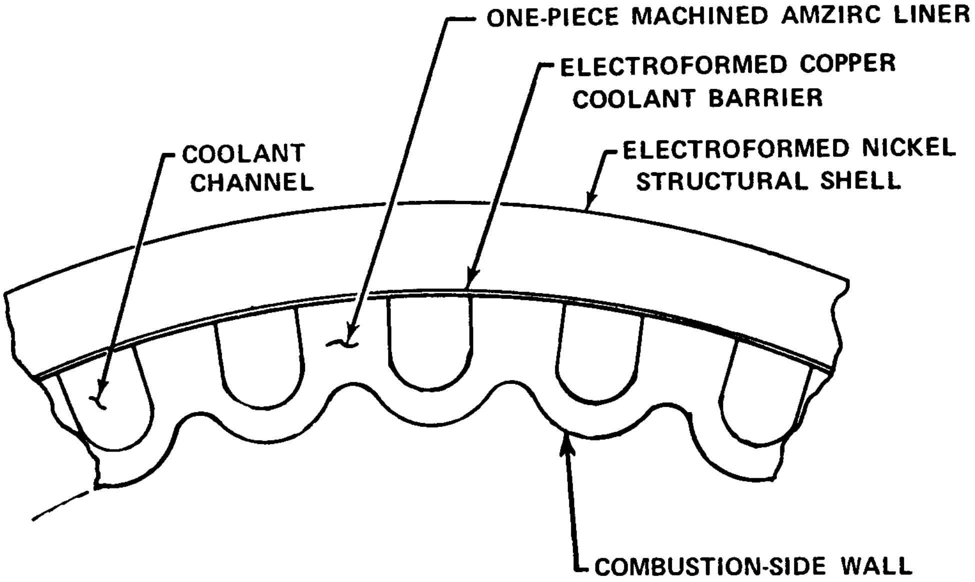

ONE-PIECE MACHINED AMZIRC LINER
ELECTROFORMED COPPER COOLANT BARRIER
COOLANT CHANNEL
ELECTROFORMED NICKEL STRUCTURAL SHELL
COMBUSTION-SIDE WALL

Figure C-181. Structural Nickel Outer Closures FD 68839A

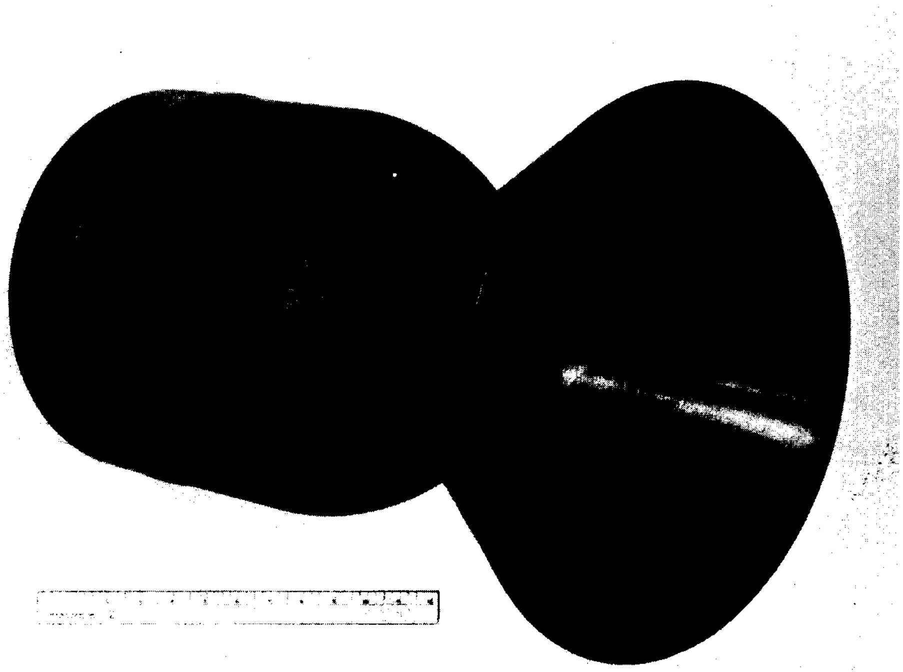


Figure C-182. Milled Slot Liner FAE 105647

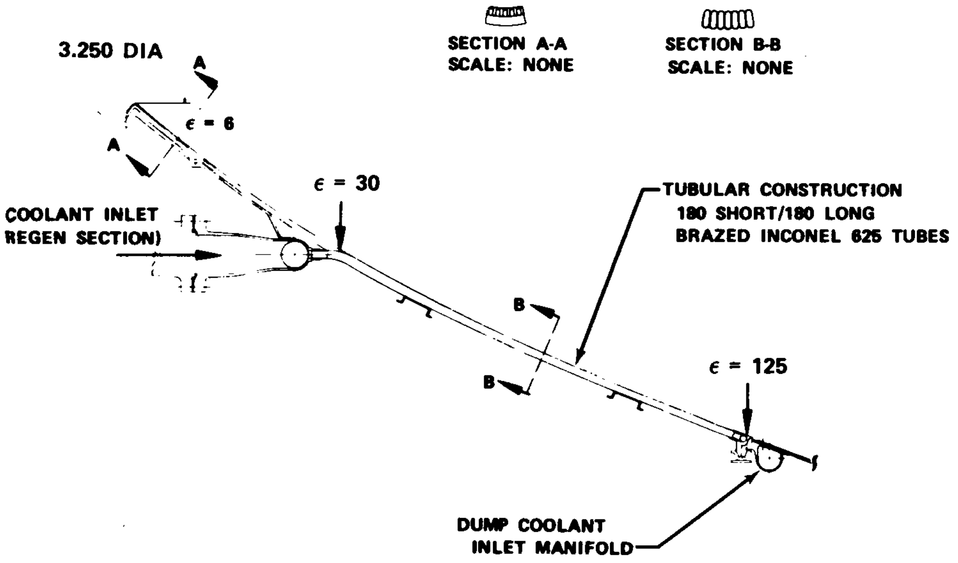
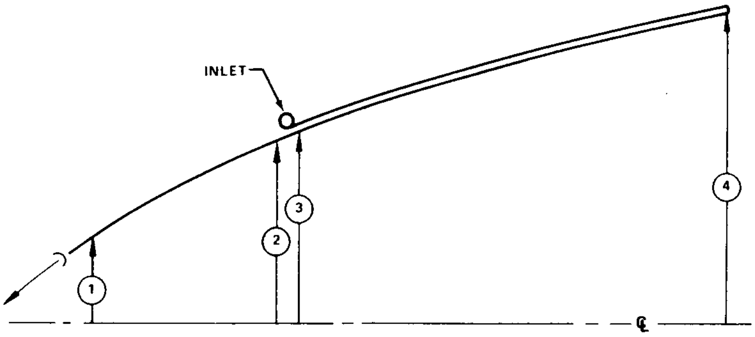

SECTION A-A
SCALE: NONE
SECTION B-B
SCALE: NONE
3.250 DIA
A
ϵ = 6
A
ϵ = 30
COOLANT INLET
REGEN SECTION)
TUBULAR CONSTRUCTION
180 SHORT/180 LONG
BRAZED INCONEL 625 TUBES
B
B
ϵ = 125
DUMP COOLANT
INLET MANIFOLD

Figure C-183. Category IV Thrust Chamber Nozzle FD 66676C



PARAMETER	LOCATION			
	①	②	③	④
ϵ	6.00	30.75	31.25	125.00
R (in.)	3.98	9.01	9.09	18.17
NO. OF TUBES	180	180	360	360
d_i (in.)	0.123	0.303	0.143	0.304
h_i (in.)	0.123	0.305	0.396	0.304
t_w (in.)	0.010	0.010	0.010	0.010

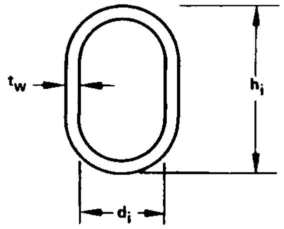


Figure C-184. Category IV Engine Tubular Primary Nozzle FD 71588

Bands are used on the outside of the tubular nozzle to take hoop loads resulting from internal pressure and nozzle longitudinal loads due to thrust, maneuvers and gimbaling acceleration. Bands are placed to limit tube stress to a level below the material yield strength at a safety factor of 1.1. Stiffening bands at the inlet and turnaround manifolds of the tubular skirt section are Inconel 625 to maintain thermal compatibility with the tubes for brazing. The sides of the tubes are "spanked" to give a minimum of 0.030 in. flat braze contact area.

Coolant enters the nontubular nozzle section at its maximum expansion ratio of 6:1 from the tubular section in a counterflow direction. To avoid the weight and pressure loss penalties of two separate manifolds, flanges, and fasteners, direct attachment between the two sections is provided, as previously illustrated in figure C-175. The tubes at the end of the tubular section are brazed into sockets in an AMZIRC ring, which is electron-beam welded to the nontubular AMZIRC inner wall, and to the structural nickel outer wall. This joint, in addition to being lightweight relative to flange type joints, makes use of the high thermal conductivity of the AMZIRC combined with higher coolant velocities compared to coolant velocities in the tubular section, in the transitional flow region to prevent excessive hot wall temperatures.

4.3.1.3 Life

The life limiting component is the nontubular portion of the chamber assembly. Large thermal gradients are generated in regeneratively cooled thrust chambers during operation. These gradients affect the expected cyclic life of the chamber walls, as does the further aggravating condition of high pressure coolant within the wall passages. Pratt & Whitney Aircraft has developed a method for analysis of nontubular thrust chambers that provides a complete evaluation of thermal life, in that thermal gradients and the pressure differential across the hot wall are simultaneously treated. The cycle life of the nontubular portion of the Category IV engine combustion chamber has been estimated and the life satisfies the design requirement level of 1200 cycles (i.e., four times the 300 engine duty cycles). The rounded channel shape, previously shown in figure C-176, is milled on the ID wall allowing local bending and relieving of the compressive stresses existing on the hot wall due to thermal gradients. Semicircular construction of this type is predicted to provide 4400 life cycles compared to 570 cycles for a rectangular channel as shown in figure C-185. Since the required number of design cycles is 1200 the channels need not be semicircular in design but cannot be a simple rectangle. The optimum shape considering manufacturing cost and life will be determined when a final design is initiated.

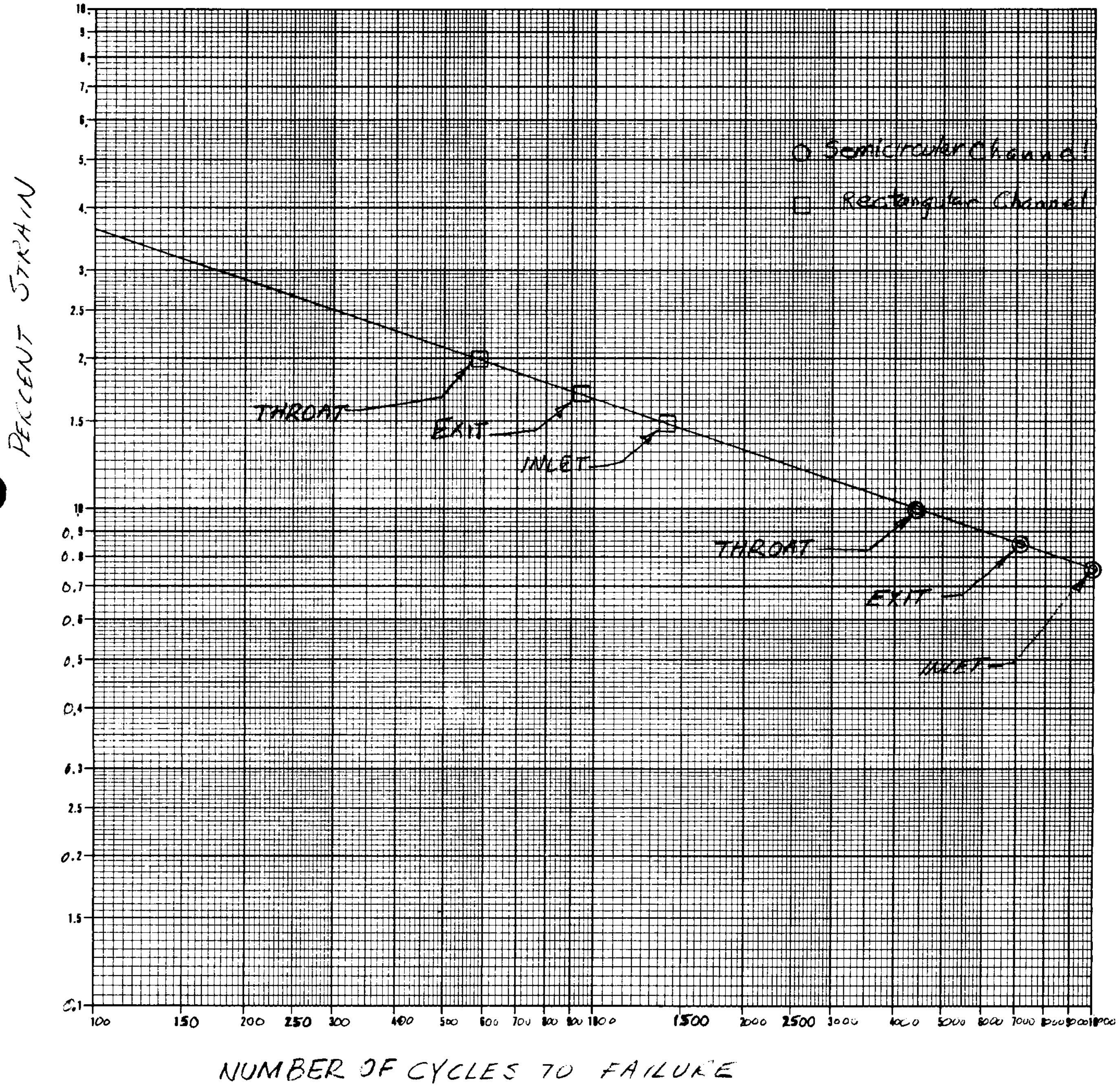


Figure C-185. Category IV Engine Nontubular Chamber Cycle Life

DF 98282

4.3.1.4 Materials

Materials for the Category IV combustion chamber and thrust nozzle were selected based on their successful use on the RL10A-3-3 and high pressure combustion engines developed under AF and NASA contracts. The materials selected are shown below.

Component	Material	Type
Propellant Tubes	Stainless Steel Tubing	AMS 5599
Thrust Chamber Assembly		
Combustion Chamber Inner Wall	Copper AMZIRC	TBD
Combustion Chamber Outer Wall	Electroformed Nickel	TBD
Machined Portion	Inconel 625 Forging	AMS 5666
Formed Portion	Inconel 625 Sheet	AMS 5599
Reinforcing Bands	Inconel 625 Sheet	AMS 5599
Porous Injector Face	Heat-Resistant Alloy Wire	AMS 5794
Gimbal Spider	High-Strength, Stainless Steel Forging	AMS 5735
Gimbal Pedestal and Cone	Aluminum Alloy Forgings	AMS 4139
Brackets	Stainless Steel Sheet	AMS 5599

4.3.1.5 Weight

The weight of the Category IV engine combustion chamber and thrust nozzle assembly is estimated to be 90 lb. The injector is estimated to weigh 10 lb and the internozzle seal and support ring 12 lb with the balance (68 lb) charged to the combustion chamber and thrust nozzle. These weights were calculated from conceptual design layout drawings.

4.3.2 Extendible Nozzle

The primary function of the extendible nozzle is to provide increased engine specific impulse without increasing engine length. It must translate forward over the primary thrust nozzle to provide a short engine package. The extendible nozzle must be a lightweight, dependable design and extend or retract reasonably rapidly and must be cooled by low pressure hydrogen from the gearbox. Design requirements are set by full thrust conditions shown in table C-36.

Table C-36. Extendible Nozzle Cooling Design Requirements

Parameter	Full Thrust
Coolant Inlet Pressure, psia	40
Coolant Inlet Temperature, °R	180
Coolant Tube ΔP, psi	40
Coolant Tube ΔT, °R	<1700°R
Coolant Flowrate, lb/sec	0.25

4.3.2.1 Mechanical Description

The extendible nozzle is translated by means of a jackscrew actuation system. The translating structure consists of three ballscrew jackshafts which are attached on the rear end of the primary nozzle by individual drive gearboxes and bearing assemblies, and are supported at their forward end by an adjustable link, as shown in figures C-186 and C-187. The ballscrew shaft is supported on antifriction bearings at both the front and rear locations. These roller bearings, which take axial and radial loads, are housed in a spherical ball joint that compensates for shaft misalignment as great as 2 deg.

The extendible nozzle drive/synchronization system consists of two electric motors and three interconnecting flexible cables which transmit motor torque to three gear transmissions which drive the ballscrew shafts as shown in figure C-188. A locking mechanism at the base of one ballscrew assembly locks the nozzle in either the extended or retracted position. The lock, shown in figure C-189, is a spring-loaded, normally locked mechanism.

A spline is added to the cable, thus stopping nozzle travel when it is engaged at the lock location. Engagement is ensured by dual helical springs loading the spline locks. When electric power is applied to the drive motor, the electric solenoids within the lock are energized; movement of the solenoid rotates the cam, depressing the cam follower and springs within the lock mechanism, and disengaging the spline lock thus allowing drive shaft rotation. The fixed spline lock is always the first to engage, moving with the shaft and pulling the floating spline into the locked position. Rotation torque is taken out by the spline lock pivot pin. The redundant solenoids are activated during translation only.

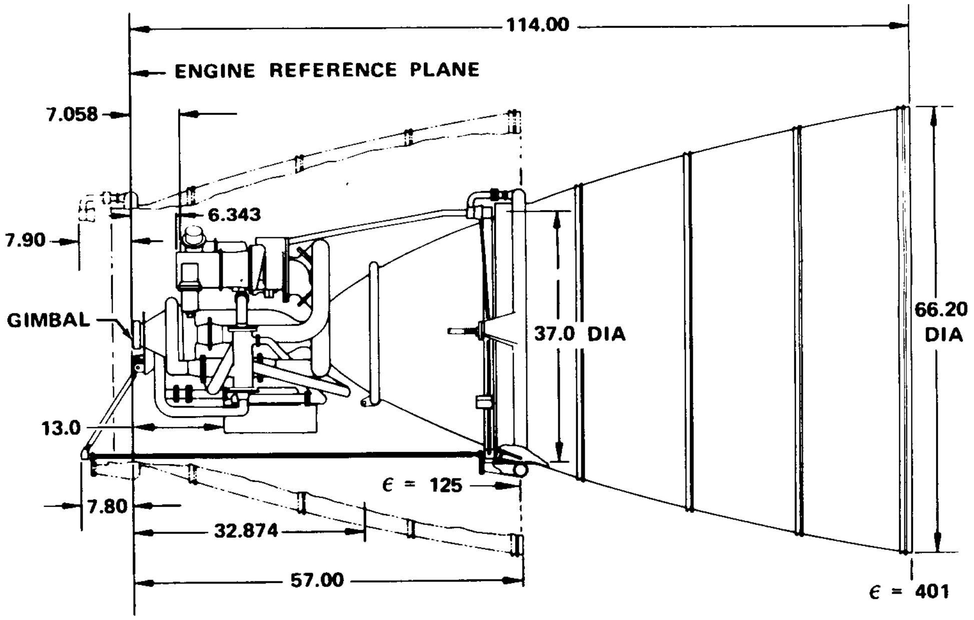


Figure C-186. Category IV Engine Installation Drawing

FD 72042A

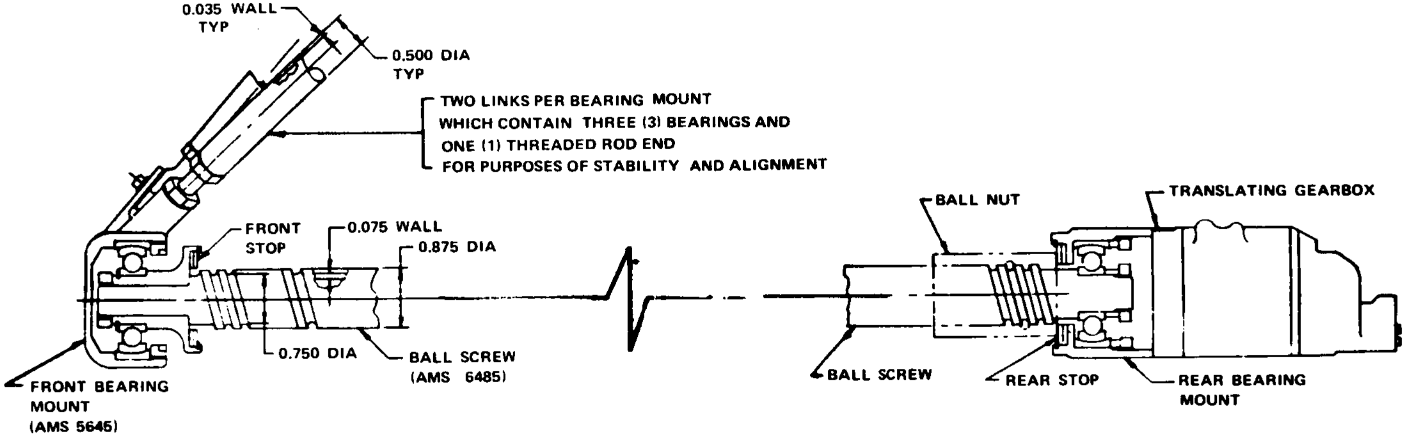

0.035 WALL
TYP
0.500 DIA
TYP
TWO LINKS PER BEARING MOUNT
WHICH CONTAIN THREE (3) BEARINGS AND
ONE (1) THREADED ROD END
FOR PURPOSES OF STABILITY AND ALIGNMENT
FRONT
STOP
0.075 WALL
0.875 DIA
0.750 DIA
BALL SCREW
(AMS 6485)
FRONT BEARING
MOUNT
(AMS 5645)
BALL NUT
TRANSLATING GEARBOX
BALL SCREW
REAR STOP
REAR BEARING
MOUNT

Figure C-187. Jackscrew Mounting Configuration

FD 76008

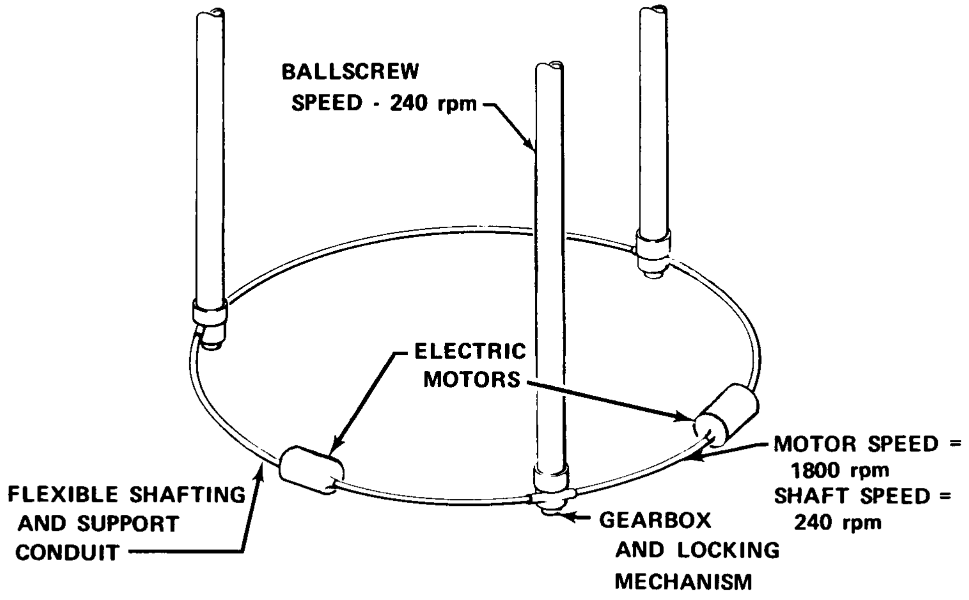

BALLSCREW SPEED - 240 rpm
ELECTRIC MOTORS
MOTOR SPEED = 1800 rpm
SHAFT SPEED = 240 rpm
FLEXIBLE SHAFTING AND SUPPORT CONDUIT
GEARBOX AND LOCKING MECHANISM

Figure C-188. Drive/Synchronization System

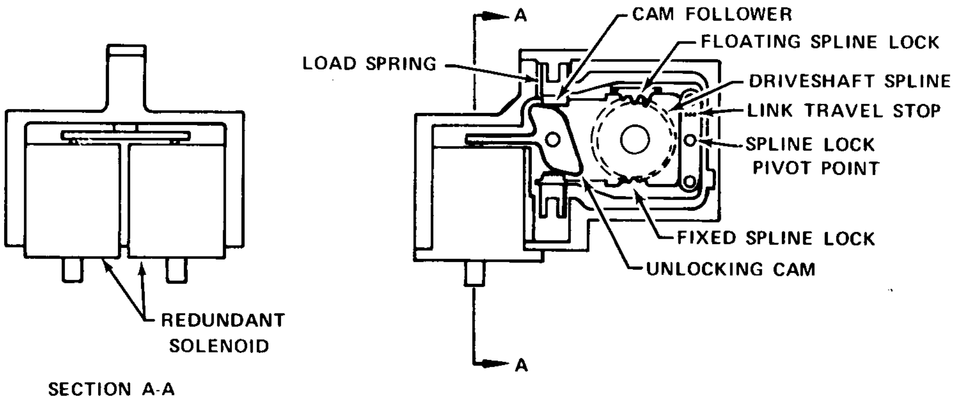

A
CAM FOLLOWER
FLOATING SPLINE LOCK
LOAD SPRING
DRIVESHAFT SPLINE
LINK TRAVEL STOP
SPLINE LOCK PIVOT POINT
FIXED SPLINE LOCK
UNLOCKING CAM
A
REDUNDANT SOLENOID
SECTION A-A

Figure C-189. Solenoid Actuated Brake for Ballscrew Actuators

FD 75864

The nozzle is attached to the translating mechanism at three equally spaced points through a nozzle attachment bracket to the ballscrews. The nozzle attachment bracket consists of a split circular ring and two-piece yoke as shown in figure C-190. The ballnut gimbal attachment provides 2 deg of swivel freedom to prevent transferring bending loads from the nozzle attachment bracket to the ballnut.

The extendible dump-cooled nozzle that extends over an area ratio of 125 to 401 is formed by a smooth outer skin and a corrugated inner skin that are resistance-seam welded longitudinally as shown in figures C-191 and C-192. The corrugations form coolant passages for hydrogen that enters at an inlet manifold located at an area ratio of 125 and discharges to the atmosphere after passing through exit nozzles formed by dimples in the corrugations at an area ratio of 401.

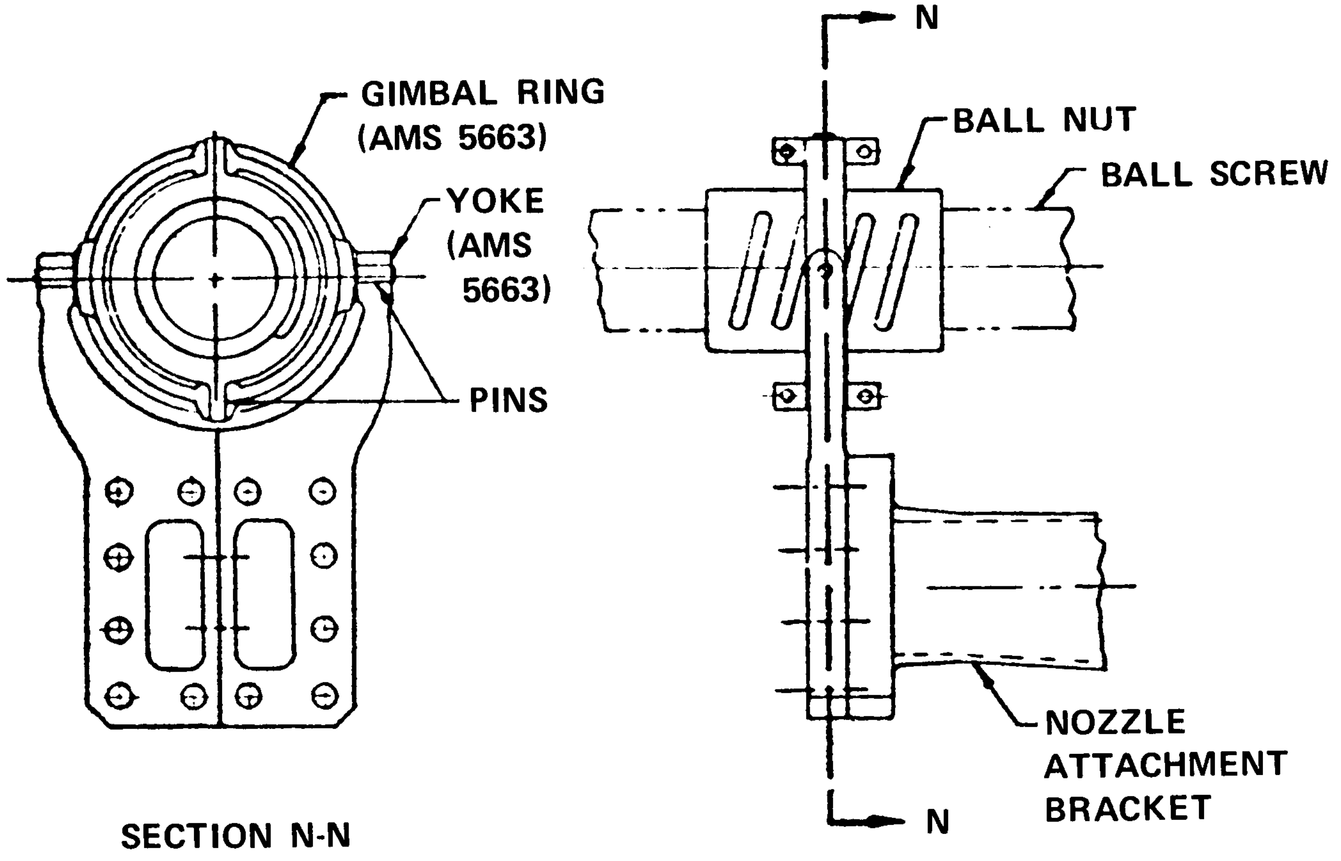

GIMBAL RING
(AMS 5663)
YOKE
(AMS
5663)
PINS
SECTION N-N
N
BALL NUT
BALL SCREW
NOZZLE
ATTACHMENT
BRACKET
N

Figure C-190. Gimbaled Ballnut

FD 76901

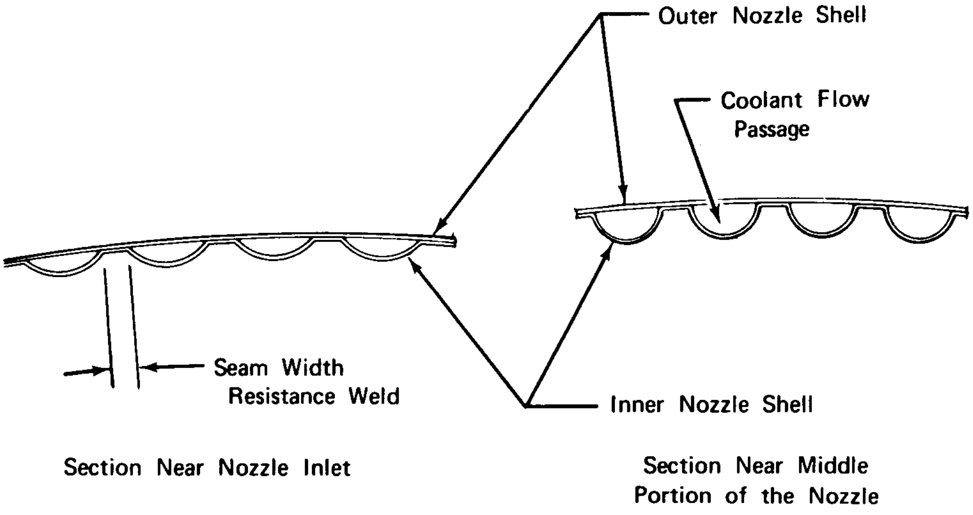

Outer Nozzle Shell
Coolant Flow Passage
Seam Width Resistance Weld
Inner Nozzle Shell
Section Near Nozzle Inlet
Section Near Middle Portion of the Nozzle

Figure C-191. Representative Corrugation Sections for Dump Cooled Nozzle FD 51227A

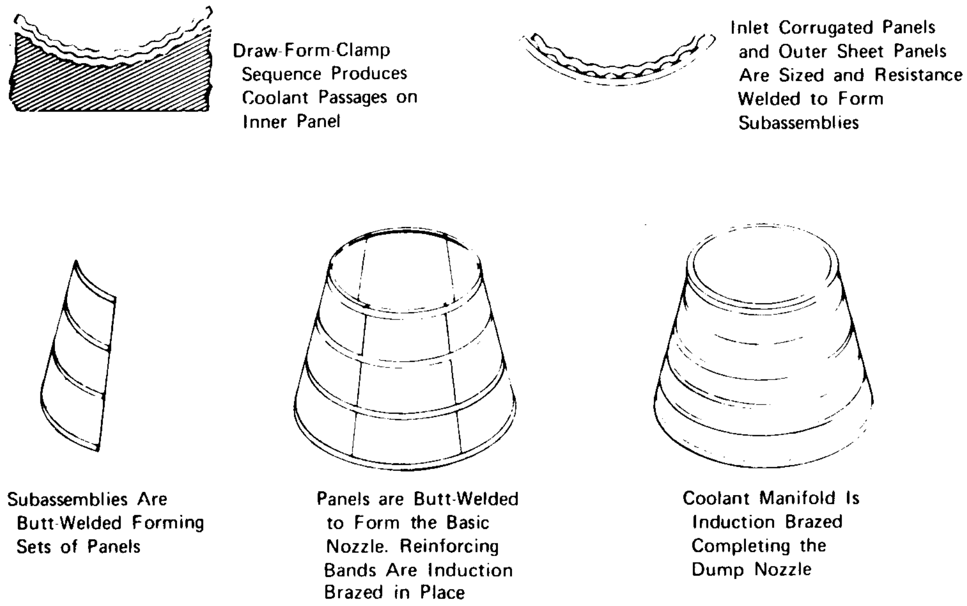

Draw-Form-Clamp Sequence Produces Coolant Passages on Inner Panel
Inlet Corrugated Panels and Outer Sheet Panels Are Sized and Resistance Welded to Form Subassemblies
Subassemblies Are Butt-Welded Forming Sets of Panels
Panels are Butt-Welded to Form the Basic Nozzle. Reinforcing Bands Are Induction Brazed in Place
Coolant Manifold Is Induction Brazed Completing the Dump Nozzle

Figure C-192. Dump Cooled Nozzle Fabrication Sequence FD 51226A

The hydrogen coolant is uniformly distributed to the dump-cooled nozzle by the circular torus inlet manifold shown in figure C-193. Holes are drilled such that the torus supplies hydrogen to the dump-cooled nozzle corrugations and also to a seal land manifold that supplies coolant to the internozzle seals.

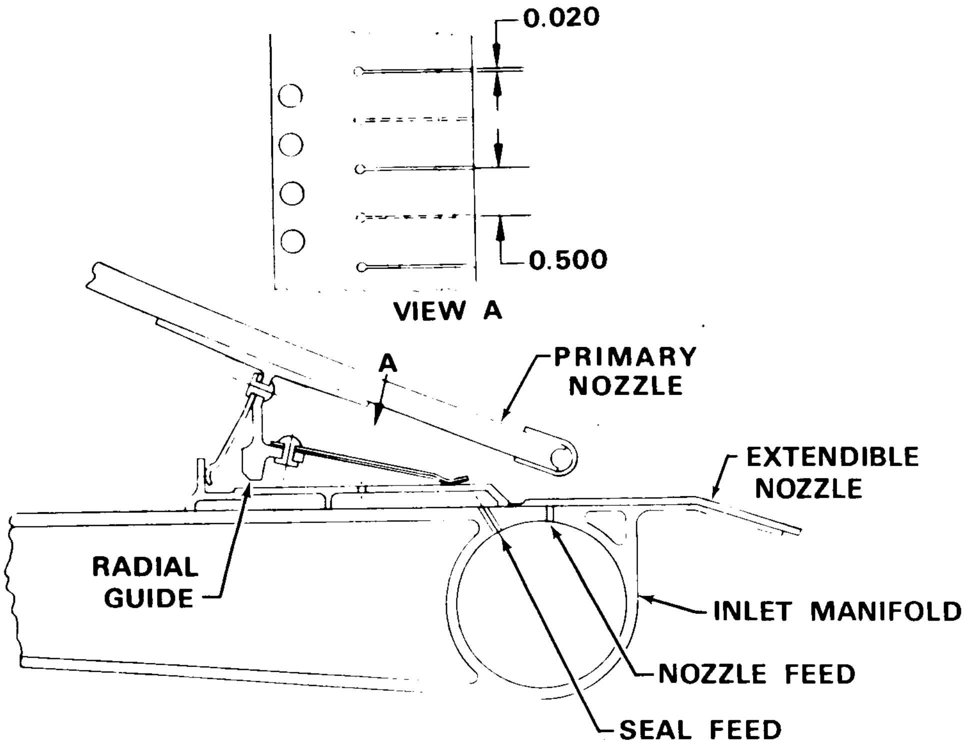


Figure C-193. Double Finger Leaf Nozzle Seal Arrangement FD 72061A

The extendible nozzle coolant is supplied from the turbomachinery gearbox to the inlet manifold of the extendible nozzle by a quick disconnect feed valve depicted in figure C-194. The nozzle is supplied coolant flow only after the nozzle is extended. One portion of the quick disconnect valve is attached to the primary nozzle and the other is attached to the secondary nozzle. Each portion contains a valve which is spring-loaded closed to prevent contamination of the fuel system plumbing when the engine is in the stored position and the nozzle is retracted. The normally closed valves are opened when the nozzle is translated to the extended position and the nose of one valve displaces the other. A spherical seat prevents valve leakage during engine firing.

Reliable engagement of the two parts is ensured by a flared skirt with an internal ramp guide located on the valve portion attached to the retractable nozzle. A bellows section, attached to the extendible nozzle, deflects radially to aid alignment and to axially provide a loading force between the spherical seats. The slow rate of nozzle extension provides adequate time to allow the guides and bellows to compensate for misalignment and dynamic mismatches between the mating parts.

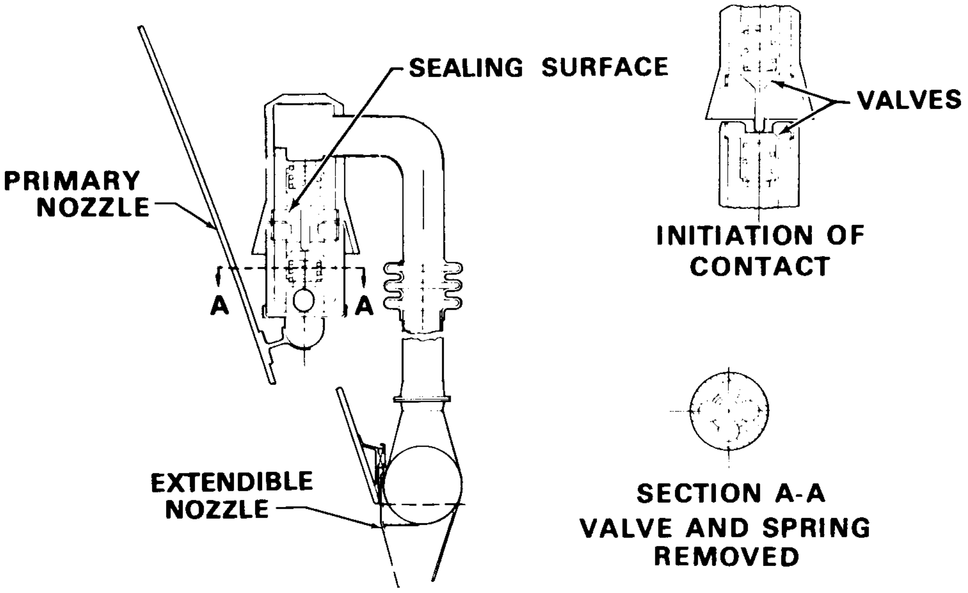


Figure C-194. Extendible Nozzle Coolant Supply FD 72060A

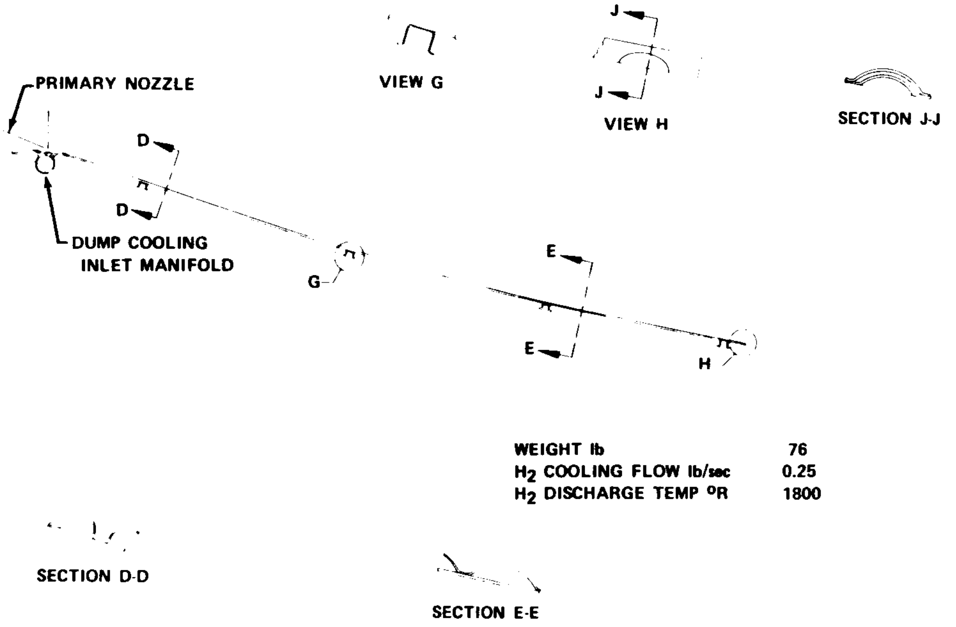
The interface between the primary nozzle and extendible nozzle is sealed by the use of finger leaf seals. Each seal consists of a two-ply sheet metal ring with radial "keyhole" slots in each ply. The slots in each ply are positioned to seal the slots in the mating ply as shown in figure C-193. Both plies are welded to a supporting ring for attachment to the primary nozzle. The assembly seals radially or axially against a land on the extendible nozzle. The seal is cut radially in one place to simplify assembly and replacement, and the two plies overlap at this location to prevent an excessive leak path. The seal assembly is riveted to the primary nozzle so that it can be readily replaced during overhaul.

These seals are supplied coolant flow from a choked orifice on the extendible nozzle inlet manifold. It is sized to supply 0.002 lb/sec of hydrogen should a seal finger get bent or broken. With the seals in their nominal position, leakage between the seal fingers is estimated to be 0.0002 lb/sec.

4.3.2.2 Design Studies

The corrugated dump-cooled design shown in figure C-195 has been used for the large surface area section of the nozzle because it has the following advantages over the tubular type construction: (1) lighter weight, (2) resistance welded flatsheet construction that is less costly than brazed tubular construction, (3) coolant passage cross section that is readily varied to achieve desired coolant velocity profiles, and (4) hoop stresses that are carried by the cooled outer wall. Corrugated sheet construction has been demonstrated in firing tests of a modified RL10A-3-3 thrust chamber conducted in August 1967.

The design of the Category IV engine extendible nozzle system was based upon results of design studies completed for the Derivative II engine. Original design work was limited to a thermodynamic analysis of the coolant passages to define the required geometry and coolant flow. No structural analysis was attempted. The predicted passage temperatures along the nozzle for the selected nozzle coolant flow are given in figure C-196.



WEIGHT lb	76
H_2 COOLING FLOW lb/sec	0.25
H_2 DISCHARGE TEMP °R	1800

Figure C-195. Dump Cooled Extendible Nozzle FD 71305A

4.3.2.3 Life

The dump cooled nozzle should not limit engine life as the practical wall thickness of the materials can be chosen heavy enough to ensure that the material does not yield. Only after temperatures near the end of the nozzle approach 1900°R should the stresses approach the allowable for Inco 625 material, and then wall thicknesses can be increased to keep them below the allowable limits.

The rotating components move so slowly that high wear rates are not anticipated. The ballscrew system must be sized for nondynamic loads such as booster takeoff and landing and thus are oversized for the operational loads encountered while the nozzle is translating.

4.3.2.4 Materials

Materials for use on the Category IV engine extendible nozzle were selected for their compatibility with hot hydrogen and their ease of manufacture. The large thin panels of the nozzle require special attention in regard to ease of forming, welding and compatibility with hydrogen.

Component	Material	Type
Thrust Nozzle	Inconel 625	AMS 5666 AMS 5599
Ballscrew Actuator	Steel	AMS 6485
Support Structure	Aluminum	TBD

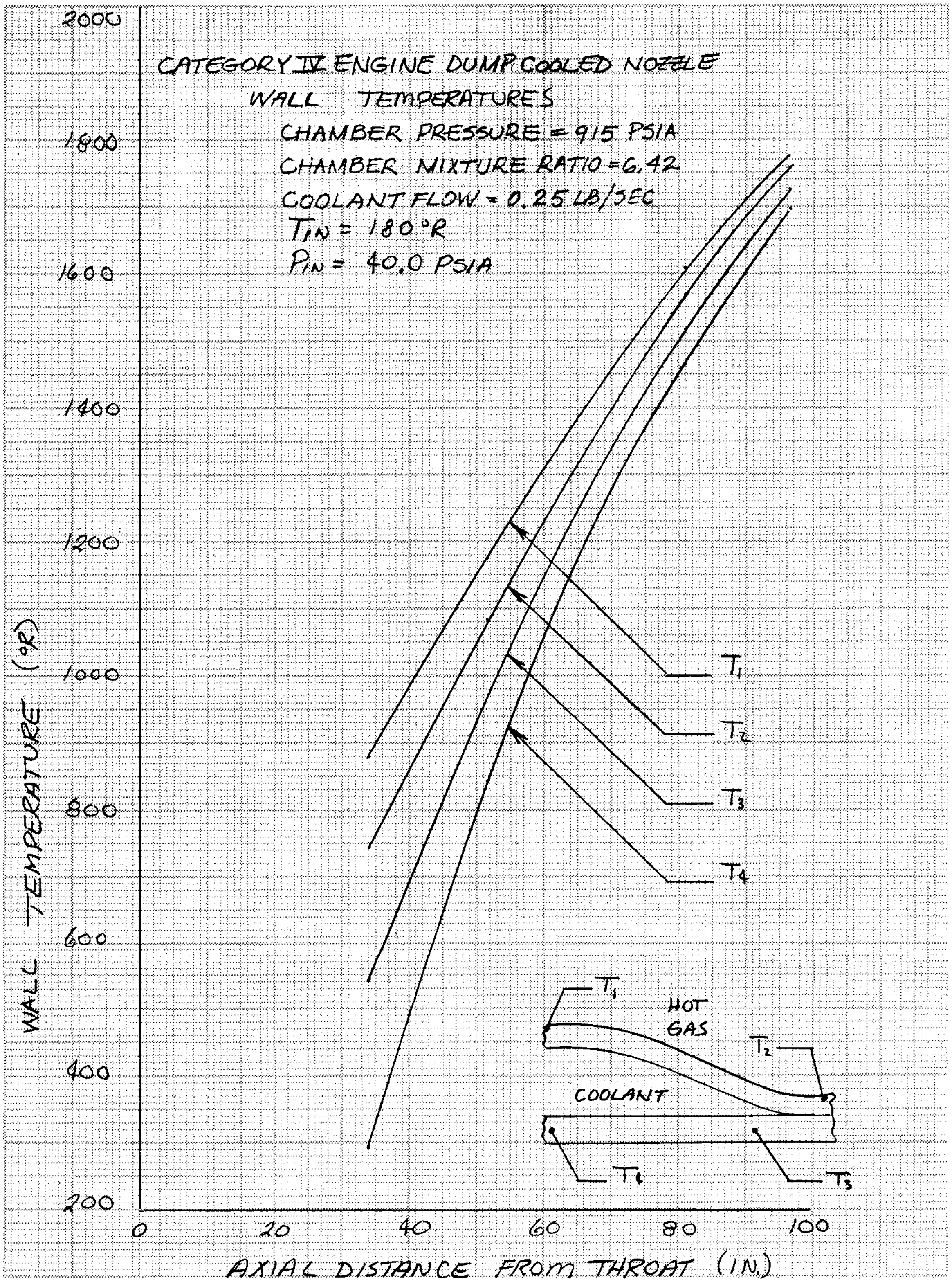


Figure C-196. Category IV Engine Dump Cooled Nozzle Wall Temperatures DF 98283

4.3.2.5 Weight

The weight of this system is approximately 101 lb with the nozzle being 76 lb of the total and the remainder in ballscrew actuator systems and support hardware. The weight was calculated using a layout drawing of the various components.

4.3.3 Category IV Turbomachinery

Category IV Turbomachinery delivers hydrogen and oxygen propellants at the required pressure level and flowrates to maintain a combustion chamber pressure of 915 psia. The Category IV engine turbopump must provide this function with a high vapor fraction at the pump inlets. Since the power required to drive the main pumps is too large for practical gear design, each pump must be driven by a separate turbine. Synchronization of the two turbopump shafts is required to provide a fixed speed relationship. The turbomachinery performance requirements established by cycle analysis at full thrust is shown in tables C-37 and C-38.

Table C-37. Fuel Turbopump Performance at 100% - Full Thrust

Parameter	Value
Fuel LSI	
Inlet pressure, psia	16.0
Inlet temperature, °R	36.9
Rotor speed, rpm	30,450
Flow, lb/sec	4.56
Power required, hp	6.0
Discharge pressure, psia	32.6
Inlet density, lb/ft^3	4.40
Efficiency	0.73
Fuel Pump, 1st Stage	
Rotor speed, rpm	75,050
Power required, hp	469
Discharge pressure, psia	1,050
Discharge temperature, °R	54.5
Inlet density, lb/ft^3	4.43
Discharge density, lb/ft^3	4.31
Efficiency	0.60
Fuel Pump, 2nd Stage	
Power required, hp	484
Inlet pressure, psia	1,040
Discharge pressure, psia	2,050
Discharge temperature, °R	72.8
Discharge density, lb/ft^3	4.25
Efficiency	0.60

Table C-37. Fuel Turbopump Performance at 100% - Full Thrust (Continued)

Parameter	Value
Fuel Pump Turbine	
Power, hp	931
Flow, lb/sec	4.02
Inlet pressure, psia	1,825
Inlet temperature, °R	580
Discharge static pressure, psia	1,193
Turbine pressure ratio	1.53
Efficiency	0.67

Table C-38. Oxidizer Turbopump Performance at Full Thrust

Parameter	Value
Oxidizer LSI	
Inlet pressure, psia	16
Inlet temperature, °R	164
Rotor speed, rpm	3,000
Flow, lb/sec	27.3
Power required, hp	6.1
Discharge pressure, psia	55
Discharge temperature, °R	164
Inlet density, lb/ft^3	70.9
Efficiency	0.65
Oxidizer Pump	
Rotor speed, rpm	39,860
Power required, hp	184
Discharge pressure, psia	1,320
Discharge temperature, °R	171
Inlet density, lb/ft^3	70.9
Discharge density, lb/ft^3	70.9
Efficiency	0.69
Oxidizer Pump Turbine	
Power, hp	218
Flow, lb/sec	4.02
Inlet pressure, psia	1,135
Inlet temperature, °R	537
Discharge static pressure, psia	1,032
Turbine pressure ratio	1.10
Efficiency	0.74

4.3.3.1 Mechanical Description

The turbopump assembly of the Category IV engine shown in figure C-197 consists of: (1) gear driven hydrogen and oxygen low speed inducers to achieve two-phase pumping capability, (2) turbine driven hydrogen and oxygen main pumps geared together for speed synchronization and (3) the gearbox housing.

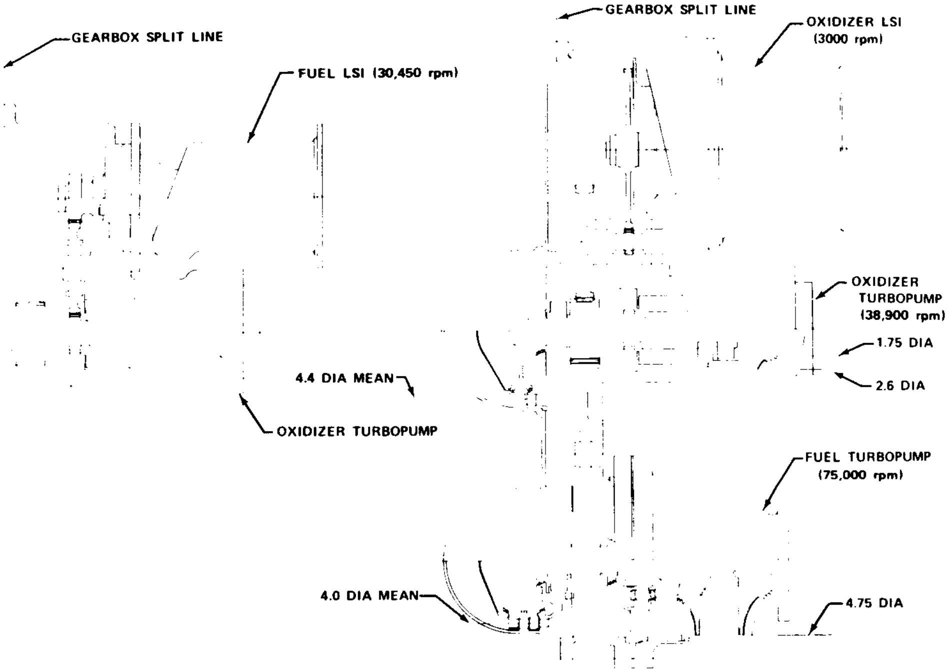

GEARBOX SPLIT LINE
FUEL LSI (30,450 rpm)
GEARBOX SPLIT LINE
OXIDIZER LSI (3000 rpm)
OXIDIZER TURBOPUMP (38,900 rpm)
1.75 DIA
2.6 DIA
4.4 DIA MEAN
OXIDIZER TURBOPUMP
FUEL TURBOPUMP (75,000 rpm)
4.0 DIA MEAN
4.75 DIA

Figure C-197. Category IV Gearbox Arrangement FD 72073A

The fuel low speed inducer is designed to be of a constant diameter helical design with swept leading edge blading for improved suction capability. A "J"-blade angle distribution is used with a helical front section and cambered rear section to provide the required suction performance and head rise. The inducer blading is designed for a 7 deg inlet angle and a 25 deg exit angle and should rotate at approximately 30,000 rpm at full thrust. As shown in figure C-198, mechanical support is provided by hydrogen cooled bearings. The front rotor bearing is cooled by inducer labyrinth seal leakage. The rear ball bearing, cooled by gearbox hydrogen flow, absorbs inducer thrust loads. The pump housing is flanged for easy removal from the gearbox. Gearing is not affected by the LSI removal and alignment is assured when reinstalled due to precision locating pins on the housing flange.

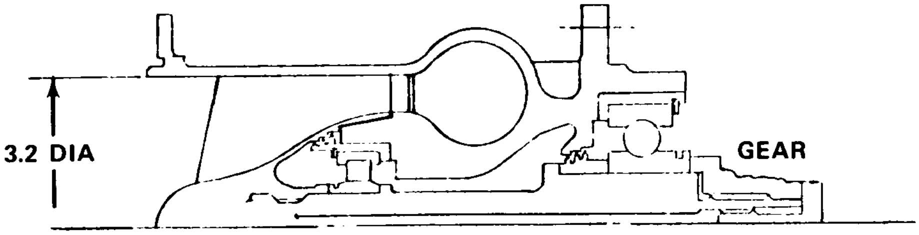


Figure C-198. Category IV Engine Fuel Low Speed Inducer FD 76014

The main hydrogen pump shown in figure C-199 is of a two-stage centrifugal configuration with the impellers mounted back to back. Both impellers operate at 75,000 rpm at full thrust, are integrally shrouded to minimize tip leakage, and incorporate 25 deg blade exit angles. Between the impellers is mounted a preloaded ball bearing, set in a slipper ring which allows axial movement when preload thrust is exceeded. The preload is set to provide bearing axial load for the anticipated small radial loads caused by impeller pressure imbalance inherent in the single collector volute design. The impellers are designed of titanium (AMS 4924) to minimize rotating mass.

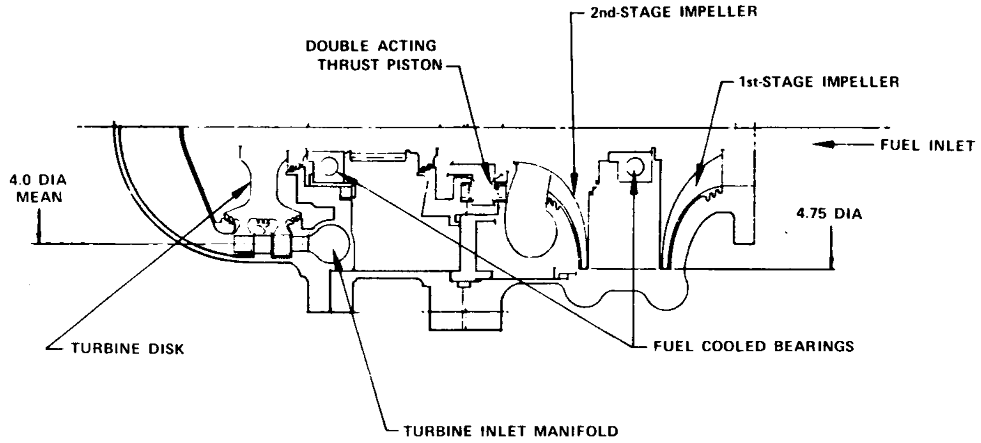


Figure C-199. Fuel Turbopump Category IV Engine FD 76237

A separate thrust balance piston is provided in the turbopump to compensate for axial load unbalance. The thrust piston is double acting in that it absorbs loads in forward and aft directions. A load capability of 463 lb is available to handle an anticipated 244-lb maximum unbalanced load. The hydrogen flow required to supply this piston is 0.35 lb/sec at full thrust. High pressure hydrogen is supplied to cavities on either side of the piston from the pump 2nd-stage discharge through supply orifices. The hydrogen flows through the piston and back to the inlet of the 2nd stage. An axial unbalance in either direction causes the rotor assembly to displace in the direction of the external load. This displacement alters the metering resistances, resulting in cavity pressure changes which generate an axial pressure gradient opposite in direction and equal in magnitude to the externally applied load.

The pump is driven by a two-stage impulse turbine utilizing the energy in the chamber/nozzle hydrogen coolant. The turbine is a partial admission, velocity compounded, shrouded design having a mean turbine diameter of 4.0 in. and a blade height of 0.32 in. Axial chord length of both turbine and stator blades is 0.30 in. Running clearances of the blade tip shroud labyrinth seals is set at 0.010 in. nominal.

The oxidizer LSI conceptual design, shown in figure C-200, is similar to that of the fuel LSI except for the addition of a seal package necessary for the isolation of the oxidizer pumping fluid and the hydrogen within the gearbox. The oxidizer LSI is driven at approximately 3,000 rpm at full thrust by the oxidizer pump turbine through a speed reducing gear train.

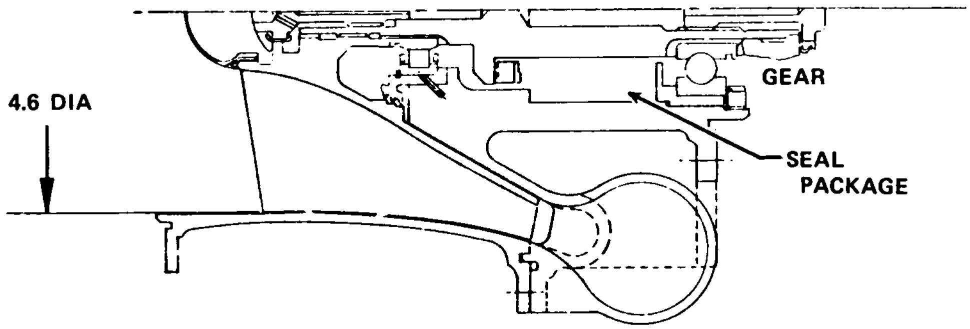


Figure C-200. Category IV Engine Oxidizer Low Speed Inducer FD 76238

The mixed flow inducer of the oxidizer LSI design contains cambered blading with an inlet angle of 9 deg and a mean discharge blade angle of 30 deg. The inducer blade profile consists of a "J"-blade angle distribution with a swept back leading edge, helical front section and a cambered rear section. The inducer is mounted on an oxygen cooled roller bearing and a hydrogen cooled ball bearing. Liquid oxygen from a three lip labyrinth seal is directed through the bearing and then forward through holes provided in the shaft and nose spinner. The ball bearing on the rear of the LSI is cooled by liquid hydrogen in the gearbox and absorbs the axial hydraulic thrust load of the inducer and the radial loads of the gear and collector volute. The collector volute develops a small radial load due to its nonuniform circumferential pressure distribution.

The oxidizer pump shown in figure C-201 is a single-stage, shrouded impeller design which operates at approximately 39,000 rpm at full thrust. The rotor front ball bearing is oxygen cooled from pump discharge flow and the rear ball bearing is cooled by hydrogen within the gearbox. A single acting thrust piston is required in the oxidizer pump as contrasted to the double acting thrust piston of the fuel pump because the single stage oxidizer pump and turbine cause a thrust only in one direction. Both bearings are mounted on slipper carrier rings which allow axial shift of the rotor if bearing preload is exceeded. A seal package is placed between bearings to isolate the high pressure main pump oxygen from the low pressure hydrogen within the gearbox. Bearing preload is provided to set the optimum contact angle and to center the rotor for start and shutdown operations when there is no thrust piston flow or action. Any momentary contact between the thrust piston and its parallel reacting face is absorbed by a leaded bronze material developed for relatively high momentary contact bearing stress.

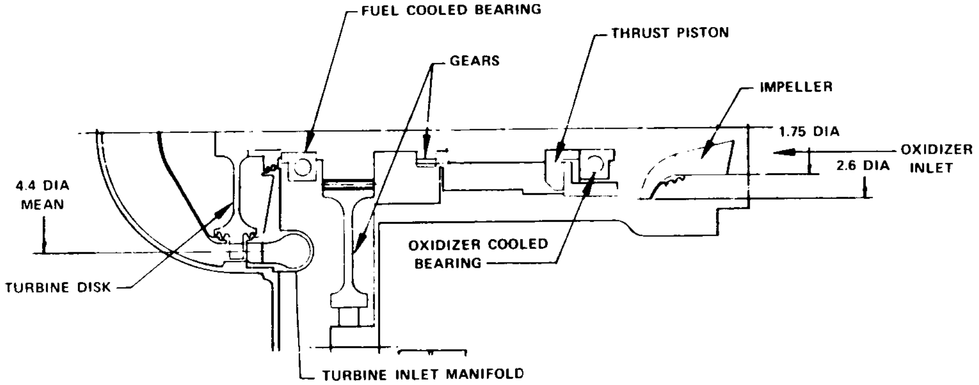


Figure C-201. Oxidizer Turbopump Category IV Engine FD 76239

The oxidizer pump is driven by a single-stage, full admission shrouded impulse turbine design having a mean diameter of 4.4 inches. The turbine is driven by warm hydrogen gas discharged from the hydrogen pump turbine. Design blade height is 0.32 in. and chord length is 0.30 in.

All pumps and turbines of this assembly are mounted on a common gearbox housing previously shown in figure C-197. The bearings located within the gearbox are all liquid hydrogen cooled. Hydrogen bearing coolant is released into the gearbox in gaseous state where it circulates and cools the gears. There are two power gear trains in the gearbox and one synchronizing gear train. The synchronizing gear train couples the fuel pump shaft to the oxidizer pump shaft, keeping them both in a constant speed ratio. This simplifies turbine controls and keeps one pump from "running away" relative to the other. The two power gear trains in the gearbox are coupled to the oxidizer pump shaft and drive the oxidizer and fuel low speed inducers. Both power trains drive through idler gears required for speed reduction and optimum pump packaging. All gears are lubricated by a dry film lubricant sprayed and baked onto the gears. This film is a mixture of molybdenum disulfide and antimony oxide in a silicone binder and a xylene carrier.

4.3.3.2 Design Studies

The two-phase pumping design analysis data generated during the configuration selection of the fuel and oxidizer inducers for the Derivative IIA engine was also used for the selection of Category IV engine pump suction designs. Analysis of the Category IV main fuel turbopump design showed that since it is designed to operate at considerably higher pump speeds than the Derivative IIA engine, a low speed inducer (LSI) configuration was necessary to achieve the required two-phase performance capability. The fuel high speed inducer inlet designs selected for the Derivative IIA engine was considered a desirable low speed configuration for the Category IV design, provided additional head rise could be achieved to supply the higher speed main pump. Design analysis indicated that increasing the head coefficient of the inducer from 0.17 to 0.3 provided the overall head rise necessary to meet the main fuel pump NPSH requirement of 420 ft at full thrust. The speed and inlet geometry of the Derivative IIA fuel inducer design was retained in order to achieve the required two-phase capability while a discharge blade angle of 25 deg was selected to attain the increased head rise requirements.

The oxidizer low speed inducer design selected for the Category IV engine was also based upon the inducer design of the Derivative IIA engine. Again, additional head rise was needed to supply the higher speed main oxidizer pump. Study indicated that an increase in head coefficient from 0.3 to 0.4 and discharge diameter from 4.6 to 5.3 was necessary to meet the main pump NPSH requirement of 38.8 ft at full thrust.

The fuel and oxidizer LSI design parameters are shown in table C-39.

Table C-39. Inducer Design Parameters Category IV

			Fuel Inducer	Oxidizer Inducer
D_{it}	-	inlet tip diameter, in.	3.2	4.6
D_{ih}	-	inlet hub diameter, in.	0.96	1.38
β_{it}*	-	inlet tip blade angle, deg	7	9
2t	-	discharge tip diameter, in.	3.2	5.28
β_{2m}	-	mean discharge blade angle, deg	25	30
$\psi 2_m$	-	overall head coefficient	0.3	0.4
ϕ_{it}	-	inlet tip flow coefficient	0.0629	0.0978
γ	-	vapor fraction - capability	0.4	0.4
γ	-	vapor fraction - required	0.17	0.17
M_n	-	inlet Mach No.	0.38	0.55
i/β *	-	incidence/blade angle	0.5	0.56
k	-	cavitation number	0.024	0.07

The basic design criteria used to establish the Category IV main fuel and oxidizer turbopump configurations were based upon results of optimized hydrodynamic design parameters from several high pressurepumps previously tested for the Air Force XLR129 engine and a NASA 350K engine. The selection of specific diameter, head coefficient, flow coefficient, and discharge blade angle, in proper relation to each other provides a framework within which the desired head rise and flowrates may be attained with optimum efficiency. Selection of the above parameters from previously optimized designs provides assurance of achieving optimum performance in a new design on the basis of hydrodynamic similarity. Figure C-202 presents the basic correlations employed to size the Category IV impeller designs which incorporate the hydrodynamic parameters given in table C-40.

As shown in figure C-202, the oxidizer pump design parameters were selected which closely match those of successful oxidizer pump designs. The hydrogen pump design specific speed was reduced however, relative to other designs, because rotor speed was limited to 75,000 rpm. This speed limitation was imposed to satisfy a gear design criteria which limited pitch line velocity to levels less than 20,000 fpm. With the rotor speed established other pump geometry was selected to optimize efficiency.

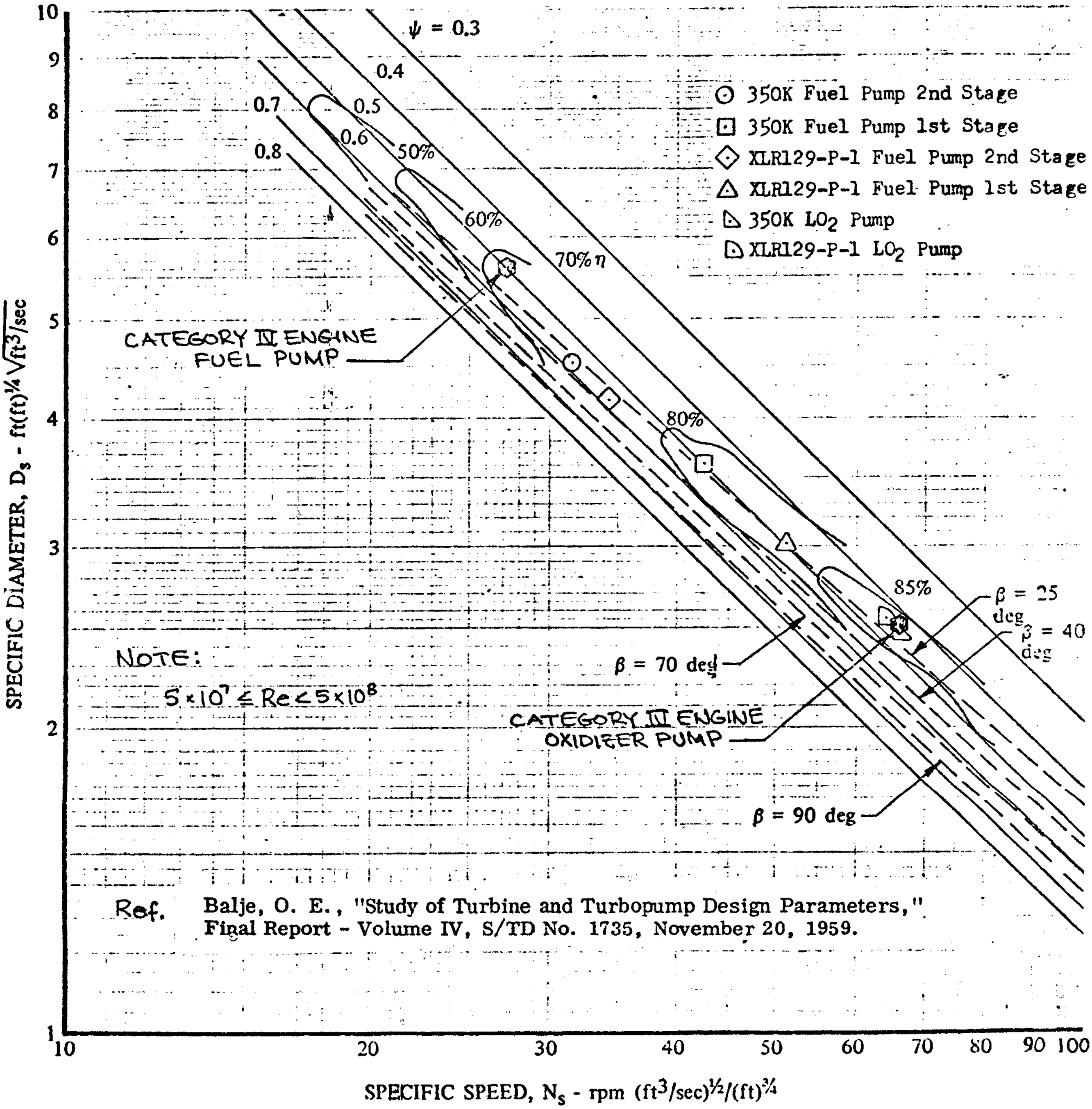


Figure C-202. Design Parameters Optimized from Test Data DF 85915C

Table C-40. Main Pump Design Parameters Category IV

		Fuel Pump Stage 1	Fuel Pump Stage 2	Oxidizer Pump
D_{it} -	inlet tip diameter, in.	2.0	2.14	1.75
D_{2t} -	discharge tip diameter, in.	4.765	4.765	2.61
β_{2m}* -	discharge blade angle, deg	25	25	25
ϕ_{2m} -	discharge flow coefficient	0.08	0.08	0.11
ψ_{2m} -	discharge head coefficient	0.568	0.568	0.445
N_s -	specific speed $\frac{rpm(gpm)^{1/2}}{(ft)^{3/4}}$	553	553	1,406
N_c -	specific speed $\frac{rpm(ft^3/sec)^{1/2}}{(ft)^{3/4}}$	26.1	26.1	66.3
D_s -	specific diameter	5.55	5.55	2.5
N_{SS} -	suction specific speed (capability)	15,000	NA	30,000
NPSH -	Net Positive Suction Head, ft			
	Required	420	NA	38.8
	Available	572	NA	78.1

Shrouded impellers were selected for both the fuel and oxidizer pumps to achieve a high level of volumetric efficiency. The use of unshrouded impellers, with the reduced impeller sizes and low specific speeds required in the Category IV designs, would have imposed undesirable impeller clearance requirements for attainment of high efficiencies. The selected impeller tip speeds of 440 fps and 1560 fps for the oxidizer and fuel pump designs are within the allowable 2200 fps limit criteria assumed for shrouded impeller configurations. Single discharge volute collectors with conical diffusers were used on both the fuel and oxidizer designs to provide a reduced packaging diameter for the pumps. Area ratios selected for the conical diffusers are within the stall limits defined by past configurations.

4.3.3.2.1 Turbopump Packaging

Three turbopump assemblies were studied, one a baseline design that matched the derivative engine centerline spacing for ease of engine interchangeability, and two others which provided reduced powerhead installation envelope dimensions for decreased engine length. (See Section D2.) Each configuration was required to meet the following ground rules:

1. Main pump speed synchronization by means of an interconnecting idler gear
2. Gear driven fuel and oxidizer low speed inducer powered by the main oxidizer pump turbine
3. Low speed inducers and main pumps mounted on a common gearbox.

a. Derivative Engine Interfaced Configuration (Baseline Design)

The considerations used to generate the turbopump and low speed inducer envelopes are basically the same. In each case, the turbine and/or impeller elevations as defined by analytical studies were combined with best approximations for bearing sizes, thrust piston sizes and seal package sizes to establish a reasonable representation of the pump rotors. For the low speed inducers, the rotors were then enclosed by a suitable housing which was configured with regard for type of collector volute, pump to gearbox attachment, and gear drive. For the main pumps the housings were configured as above but in addition, other factors unique to the two main pumps, were also considered. For example, the realization that the gearbox cavity must be isolated from the high pressure thrust piston and turbine supplies necessitated the dual labyrinth shaft seals for the fuel pump. Similarly, the need to prevent mixing of oxygen and hydrogen in the oxidizer pump necessitated the use of a hydrodynamic seal package, while a shaft mounted labyrinth seal was required similar to that in the fuel pump. In addition the need for shaft mounted gears affected the housing length of all pumps.

Three gear trains were used to provide main pump speed synchronization and to drive the oxidizer and fuel low speed inducers. The synchronizing train consists of a fuel pump mounted pinion gear, a single bearing mounted idler gear and an oxidizer pump mounted driven gear. This gear train transmits 22 horsepower from the oxidizer pump turbine to help drive and synchronize the fuel turbopump. The speed reduction is approximately 2:1. The oxidizer pump synchronizing gear is also the first pinion of the gear train which drives the oxidizer LSI. The speed reduction through this train is 12.9:1 through two steps, 2.58:1 and 5:1. The gear train transmits 6.1 horsepower. The remaining gear train drives the fuel LSI and consists of an oxidizer pump mounted pinion gear, a single bearing mounted idler gear and fuel LSI drive gear. This gear train transmits 6.0 horsepower and has a speed reduction of 1.27:1. The second pinion on the oxidizer pump shaft is required to position the fuel LSI with respect to the oxidizer LSI such that the inlet radial dimensions are met. The top view of the gear train is shown in figure C-203. The powerhead minimum diameter of 32.4 in. is set by the oxidizer LSI housing depicted in figure C-204 (top view) and figure C-205 (side view).

b. Minimum Envelope Configurations

Two additional configurations were studied which provided reductions in installation diameter. The first, shown in figure C-206, repositions the baseline gearbox housing established to meet derivative engine interfaces such that its maximum envelope projection is minimized. The resulting maximum powerhead diameter is 28 in.

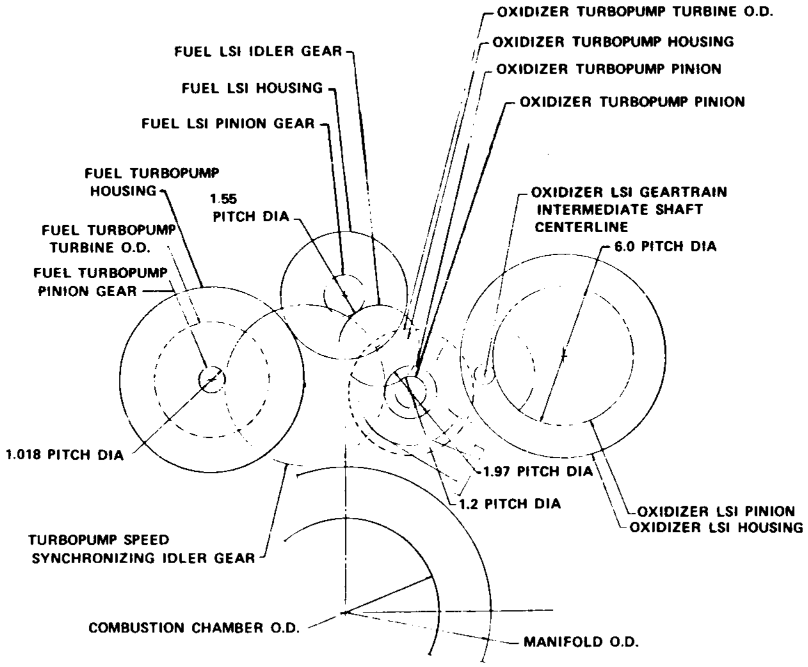

OXIDIZER TURBOPUMP TURBINE O.D.
OXIDIZER TURBOPUMP HOUSING
OXIDIZER TURBOPUMP PINION
OXIDIZER TURBOPUMP PINION
FUEL LSI IDLER GEAR
FUEL LSI HOUSING
FUEL LSI PINION GEAR
FUEL TURBOPUMP HOUSING
1.55 PITCH DIA
FUEL TURBOPUMP TURBINE O.D.
FUEL TURBOPUMP PINION GEAR
OXIDIZER LSI GEARTRAIN INTERMEDIATE SHAFT CENTERLINE
6.0 PITCH DIA
1.018 PITCH DIA
1.97 PITCH DIA
1.2 PITCH DIA
OXIDIZER LSI PINION
OXIDIZER LSI HOUSING
TURBOPUMP SPEED SYNCHRONIZING IDLER GEAR
COMBUSTION CHAMBER O.D.
MANIFOLD O.D.

Figure C-203. Category IV Engine Geartrains and Pump Housing for Gearbox FD 76240

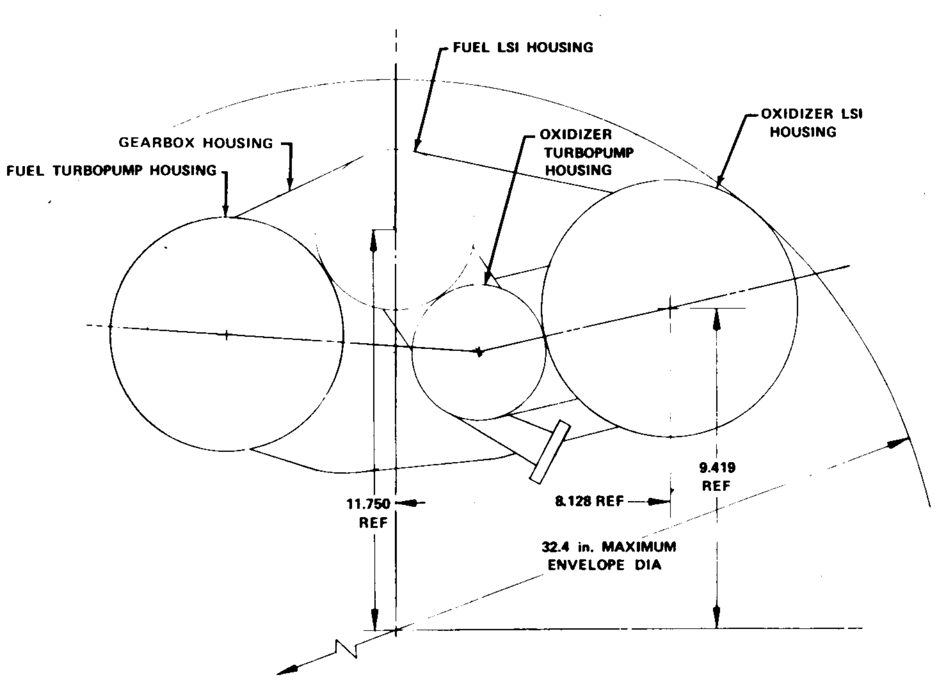

FUEL LSI HOUSING
OXIDIZER LSI HOUSING
GEARBOX HOUSING
FUEL TURBOPUMP HOUSING
OXIDIZER TURBOPUMP HOUSING
9.419 REF
11.750 REF
8.128 REF
32.4 in. MAXIMUM ENVELOPE DIA

Figure C-204. Category IV Gearbox Arrangement [RL10 Inlet Alignment] FD 72072A

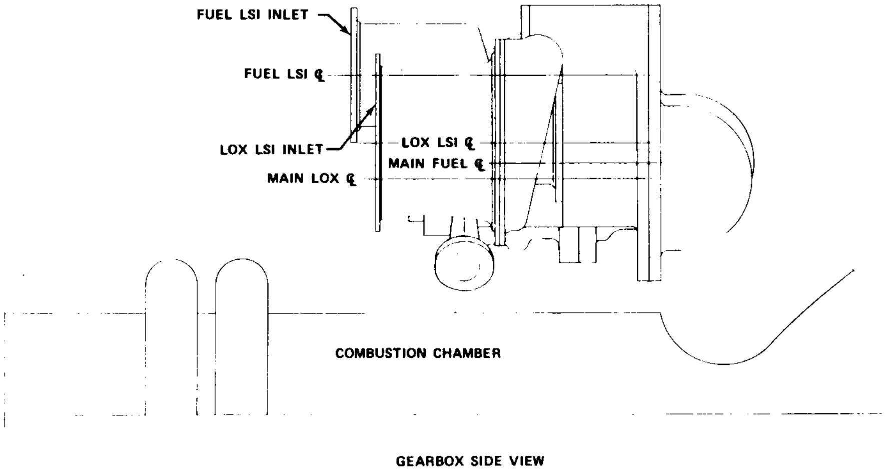

FUEL LSI INLET
FUEL LSI ℄
LOX LSI INLET
MAIN LOX ℄
LOX LSI ℄
MAIN FUEL ℄
COMBUSTION CHAMBER
GEARBOX SIDE VIEW

Figure C-205. Category IV Gearbox Arrangement [RL10 Inlet Alignment] FD 72071

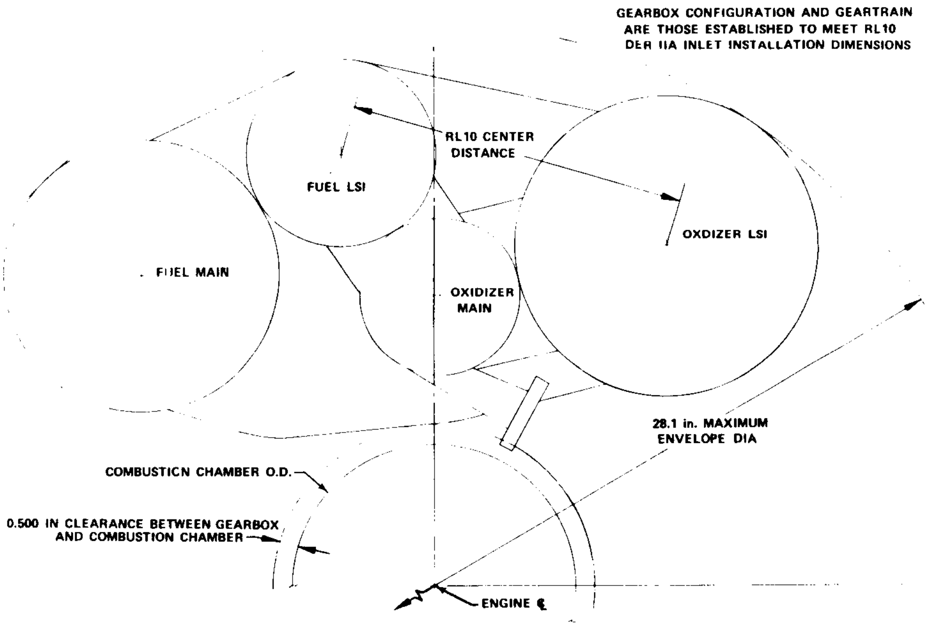

GEARBOX CONFIGURATION AND GEARTRAIN ARE THOSE ESTABLISHED TO MEET RL10 DER IIA INLET INSTALLATION DIMENSIONS
RL10 CENTER DISTANCE
FUEL LSI
OXDIZER LSI
FUEL MAIN
OXIDIZER MAIN
28.1 in. MAXIMUM ENVELOPE DIA
COMBUSTICN CHAMBER O.D.
0.500 IN CLEARANCE BETWEEN GEARBOX AND COMBUSTION CHAMBER
ENGINE ℄

Figure C-206. Category IV Gearbox from RL10 Inlet Arrangement - Minimum Envelope FD 72066

The other configuration studied is depicted by figure C-207. This gearbox generates a minimum powerhead package because the four pumps are positioned circumferentially adjacent to one another at radial locations selected to minimize clearance between the combustion chamber and the respective pump housing. The resultant gearbox housing is "kidney" shaped, and the maximum powerhead diameter for this wraparound configuration is 26 in.

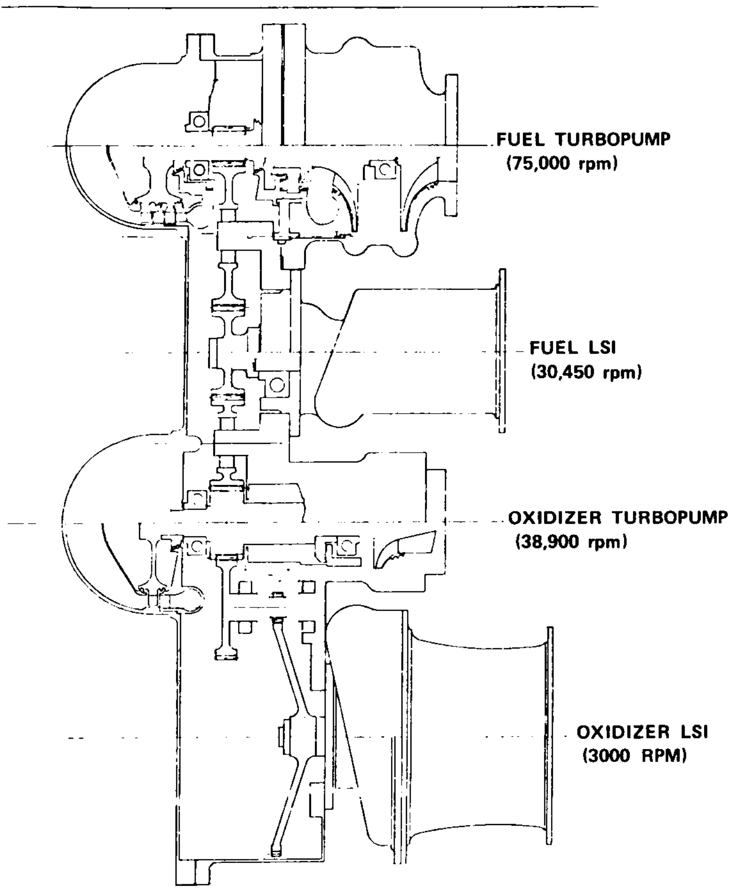


Figure C-207. Category IV Engine Wraparound Gearbox Laid Out Flat to Show Geartrain FD 76241

The oxidizer turbopump supplies some power to the fuel turbopump and all power to the fuel LSI through a geartrain transmitting 28 horsepower, 22 horsepower for driving the fuel turbopump and 6.0 horsepower for driving the fuel LSI. The oxidizer LSI geartrain transmits 6.1 horsepower through a speed reduction of 12.9:1. The geartrains are depicted in figure C-208.

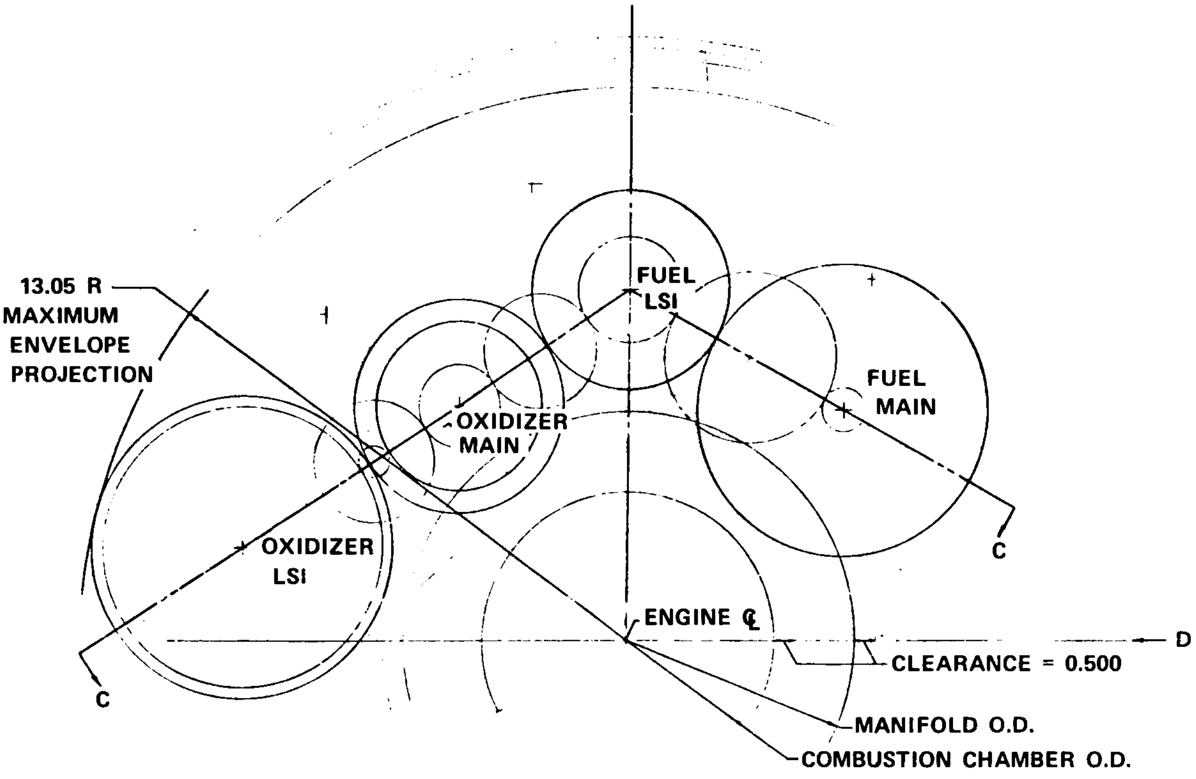

13.05 R
MAXIMUM
ENVELOPE
PROJECTION
FUEL
LSI
FUEL
MAIN
OXIDIZER
MAIN
OXIDIZER
LSI
C
C
ENGINE ℄
D
CLEARANCE = 0.500
MANIFOLD O.D.
COMBUSTION CHAMBER O.D.

Figure C-208. Category IV Engine Wraparound Gearbox Top View FD 76245

4.3.3.3 Life

The Category IV engine turbopump was designed for 10 hr effective life. Seals are of the floating ring seal and hydrostatic type and exhibit little wear. Gears were designed to provide over 10-hr life based on the gear design criteria and a new dry film lubricant PWA™ 550. All bearings were selected for 100 hr B_{10} life and are projected to provide over 10-hr life in their cryogenically cooled environment.

4.3.3.4 Materials

The materials listed below were selected for use on the Category IV engine based on their use in similarly designed turbomachinery.

Component	Material	Specification
Fuel Pump Gearbox Housing	Stainless Steel	PWA 1052
Housings	Stainless Steel	PWA 1052
Impeller (Hydrogen)	Titanium	AMS 4924
Impeller (Oxygen)	Nickel Alloy	AMS 5663
Shafts	Stainless Steel	AMS 5735
Gears	Carburizing Steel	AMS 6260
Bearings	Stainless Steel Bar and Forging	AMS 5630
Bearing Liners	Stainless Steel	AMS 5646
Turbine Disks	Nickel Alloy	PWA 1058

4.3.3.5 Weight

The weight of the Category IV turbopump is 87 lb. The weight was calculated based upon turbopump layout drawings.

4.3.4 GO2 Heat Exchanger

The function of the oxidizer heat exchanger is to gasify oxygen before entry into the injector during tank head idle operation and to provide gaseous oxygen for tank pressurization during full thrust operation. The heat exchanger requirements at these engine operating points were defined by engine cycle and vehicle tank system studies and are shown in table C-41 below:

Table C-41. Oxidizer Heat Exchanger Requirements

	Full Thrust	Tank Idle
Hydrogen Flowrate, lb_m/sec	0.24	0.03
Hydrogen Inlet Temperature, °R	580	836
Hydrogen Exit Temperature, °R	502	599
Hydrogen Inlet Pressure, psia	1840	11.5
Hydrogen Pressure Loss, psid	11	0.8
Oxygen Flowrate, lb_m/sec	0.50	0.15
Oxygen Inlet Temperature, °R	171	164
Oxygen Exit Temperature, °R	520	569
Oxygen Inlet Pressure, psia	1320	15.9
Oxygen Pressure Loss, psid	<1	0.3
Heat Flux, Btu/sec	76	27.3

4.3.4.1 Mechanical Description

The heat exchanger is a compact, laminated assembly with alternating layers of hydrogen and oxygen flow passages as shown in figure C-209. Each passage is formed by two parallel plates closed on two sides and joined by a series of copper fins oriented in the direction of flow. Special provisions are made to ensure complete isolation of the working fluids.

Warm hydrogen enters the heat exchanger through a flanged inlet, and through a semicircular distribution manifold directing the incoming hydrogen into 7 parallel finned passageways. Hydrogen convects heat into the fins and facesheets as it flows through the passageways. The hydrogen flow is then collected in a manifold and discharged through a flanged exit.

The oxidizer flow makes two passes through the heat exchanger. The flow enters the oxidizer inlet distribution manifold through an end mounted flange. The flow is routed through three flow passages, collected in a turnaround manifold and rerouted through three additional passages before exiting the heat exchanger. Heat is added to the oxygen as it flows through the fins and between the facesheets of each passageway.

Flow passages as shown in figure C-210 for the hydrogen and oxygen differ only slightly in length and width. Both are constructed with thin AISI 347 stainless steel facesheets and copper fins. All passageways are 0.100 in. high with

0.043 in. between fins. They are constructed by welding two facesheets into a leakproof assembly and brazing in the performed corrugated copper fins. The core is created by stacking alternate layers of hydrogen and oxygen passageways. A furnace braze is used to assemble the core and form a thermal bond between the outer surfaces of adjacent facesheets.

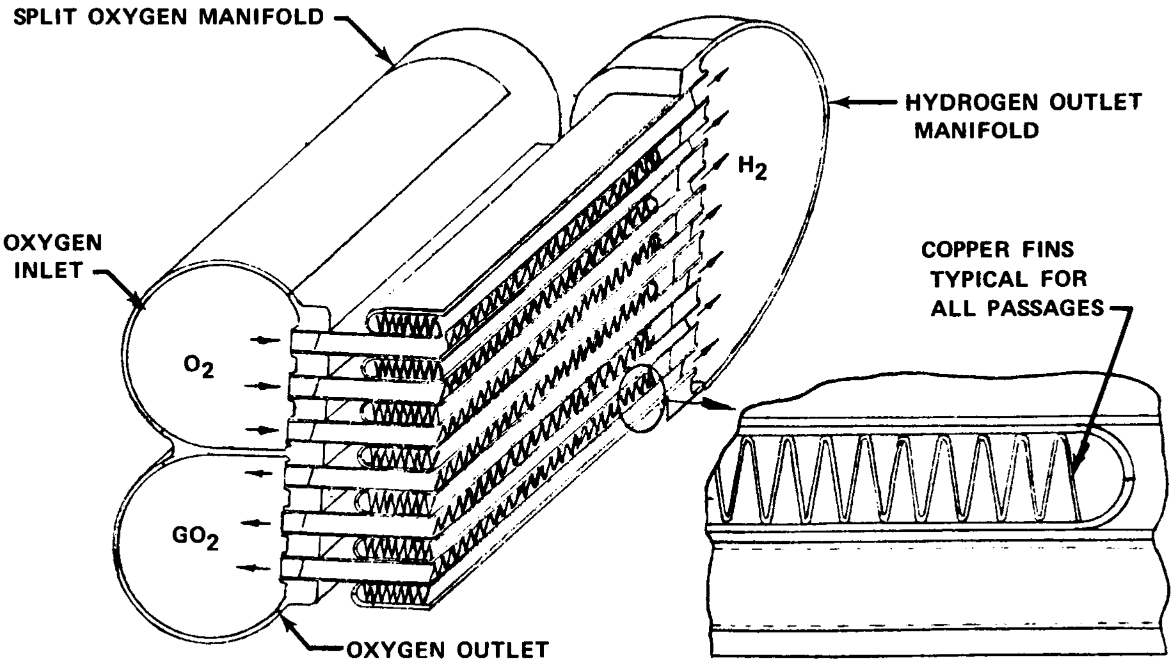

SPLIT OXYGEN MANIFOLD
HYDROGEN OUTLET MANIFOLD
H_2
OXYGEN INLET
O_2
GO_2
COPPER FINS TYPICAL FOR ALL PASSAGES
OXYGEN OUTLET

Figure C-209. Gaseous Oxidizer Heat Exchanger Design Derivative IIA Engine FD 76010

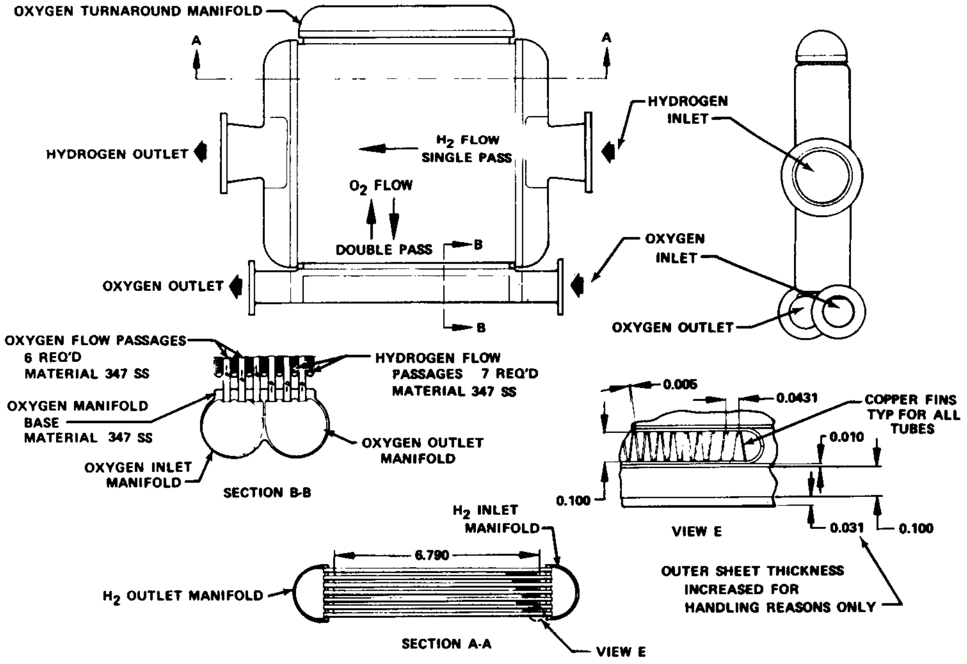

OXYGEN TURNAROUND MANIFOLD
A
A
HYDROGEN OUTLET
H_2 FLOW SINGLE PASS
O_2 FLOW
DOUBLE PASS
B
B
HYDROGEN INLET
OXYGEN INLET
OXYGEN OUTLET
OXYGEN OUTLET
OXYGEN FLOW PASSAGES 6 REQ'D MATERIAL 347 SS
HYDROGEN FLOW PASSAGES 7 REQ'D MATERIAL 347 SS
OXYGEN MANIFOLD BASE MATERIAL 347 SS
OXYGEN OUTLET MANIFOLD
OXYGEN INLET MANIFOLD
SECTION B-B
0.005
0.0431
COPPER FINS TYP FOR ALL TUBES
0.010
0.100
VIEW E
0.031
0.100
OUTER SHEET THICKNESS INCREASED FOR HANDLING REASONS ONLY
H_2 INLET MANIFOLD
6.790
H_2 OUTLET MANIFOLD
SECTION A-A
VIEW E

Figure C-210. GO_2 Heat Exchanger FD 72057

Isolation of the working fluids is provided by assembling each hydrogen and oxygen passage separately and then braze-assembling the passages along their flat sides. The hydrogen and oxygen manifold are also completely independent to prevent internal leaks. All leak paths are to vacuum environment.

4.3.4.2 Design Studies

The Category IV engine cycle heat exchanger requirements were reviewed and it was recognized that an oxidizer heat exchanger similar in concept and design to that of the Derivative II engines could meet these requirements. No thermodynamic or structural design studies were attempted during this study.

4.3.5 Controls and Instrumentation

The RL10 Category IV propellant control and instrumentation system consists of the following components: fuel inlet shutoff valve, oxidizer inlet shutoff valve, oxidizer flow control, main fuel control, main fuel shutoff valve, fuel tank pressurization valve, oxidizer tank pressurization valve, nozzle coolant valve, 4 solenoid valves, and engine instrumentation and connecting cables. Conceptual design layouts were not generated for the Category IV engine control systems. However, valve design would be based on a resizing or modification of existing RL10A-3-3 or Derivative engine component design. A schematic of the propellant system is shown in figure C-211 in which the control components are identified and the location in the engine system defined. The effective area schedules for the valves have been previously presented in Section 3.2.2. A description of the control/instrumentation system is provided in the following paragraphs.

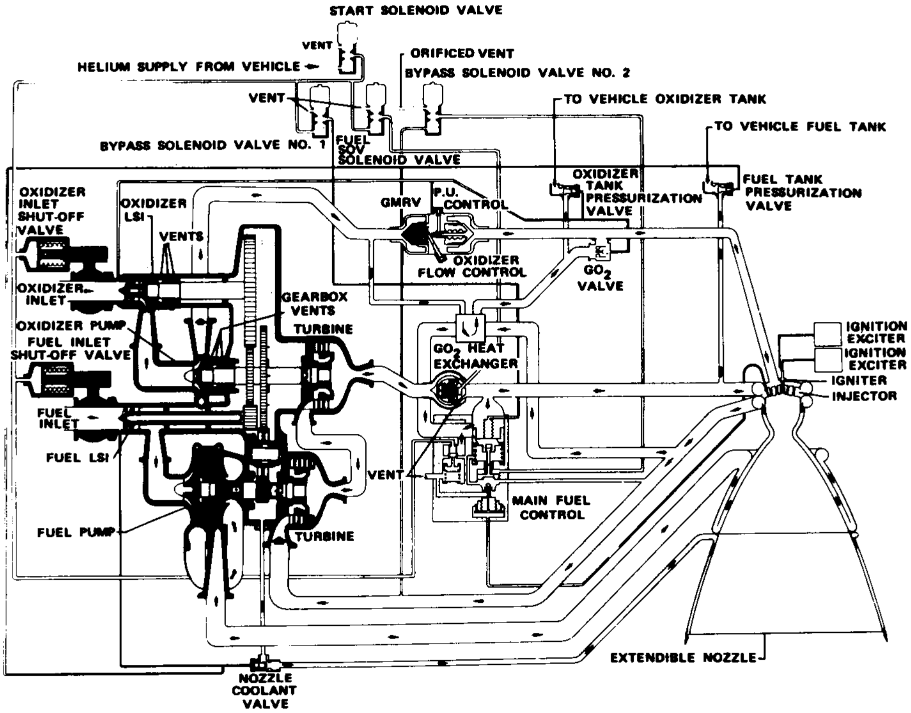

START SOLENOID VALVE
VENT
HELIUM SUPPLY FROM VEHICLE
ORIFICED VENT
BYPASS SOLENOID VALVE NO. 2
VENT
TO VEHICLE OXIDIZER TANK
TO VEHICLE FUEL TANK
BYPASS SOLENOID VALVE NO. 1
FUEL SOV SOLENOID VALVE
OXIDIZER INLET SHUT-OFF VALVE
OXIDIZER LSI
VENTS
GMRV
P.U. CONTROL
OXIDIZER TANK PRESSURIZATION VALVE
FUEL TANK PRESSURIZATION VALVE
OXIDIZER FLOW CONTROL
GO_2 VALVE
OXIDIZER INLET
GEARBOX VENTS
TURBINE
OXIDIZER PUMP
FUEL INLET SHUT-OFF VALVE
GO_2 HEAT EXCHANGER
IGNITION EXCITER
IGNITION EXCITER
IGNITER
INJECTOR
FUEL INLET
FUEL LSI
VENT
MAIN FUEL CONTROL
FUEL PUMP
TURBINE
EXTENDIBLE NOZZLE
NOZZLE COOLANT VALVE

Figure C-211. RL10 Category IV Propellant Flow Schematic FD 74113A

4.3.5.1 Propellant Control

a. Propellant Inlet Shutoff Valves

These valves are spring loaded, normally closed, helium-operated, two-position ball valves that provide a seal between the vehicle propellant tank and the engine pumps when inoperative. Both valves are located just upstream of their respective pump inlets and are of the same respective diameter as the fuel and oxidizer pump inlets, i.e., 3.2 in. and 4.6 in. in diameter. The valves are actuated by helium operating on a piston/bellows assembly. The linear motion of the actuator is translated by rack and pinion into rotary motion at the ball valve. Figure C-212 shows both valves and a cross section of the actuator.

Ball valve sealing is accomplished with dual pressure-loaded fluorocarbon rub seals. The valves incorporate a vented cavity between dual seals such that any leakage past the closed valve is vented overboard.

The fuel inlet shutoff valve estimated weight is 13 lb and the oxidizer inlet shutoff valve estimated weight is 15 lb. The weight of each valve is minimized by use of aluminum for the valve housing, ball and actuation housing. The rack and pinion, return spring, and other valve hardware are designed of stainless steel.

b. Oxidizer Flow Control Valve

The oxidizer flow control valve shown in figure C-213 is a spring-loaded, normally closed, line pressure actuated valve located in the main oxidizer line between the oxidizer pump and the injector. The valve meters liquid oxidizer flow at all operating levels at the above maneuver thrust. It is configured to provide ground trim of propellant mixture ratio, and provide inflight oxidizer propellant utilization control during full thrust operation.

The oxidizer flow control valve is comprised of two functional sections. The forward section contains two spring-loaded poppet valves used to meter oxidizer flow during maneuver and full thrust and regulate flow during the engine transient to full thrust. The aft section contains a propellant utilization control pintle valve. The two poppet valves in the forward section are normally spring-loaded closed and open as a function of the pressure differential between valve inlet pressure and a pressure within the valve cavity which has been vented to pump inlet pressure. During tank head idle operation, the poppets are closed and liquid oxidizer is not allowed to enter the injector. As oxidizer line pressure increases during the acceleration from tank head idle to maneuver thrust, the bypass poppet valve fully opens to the metering area required for maneuver thrust operation. When the engine accelerates from maneuver thrust to full thrust operation, the main poppet valve is also opened as a function of the differential pressure between valve inlet and pump inlet pressure. The bypass and main poppet valves both remain open during full thrust operation and the combined areas meter the required oxidizer flow. The full open position of the main poppet valve can be ground-trimmed by a threaded mechanical stop to ground adjust engine mixture ratio.

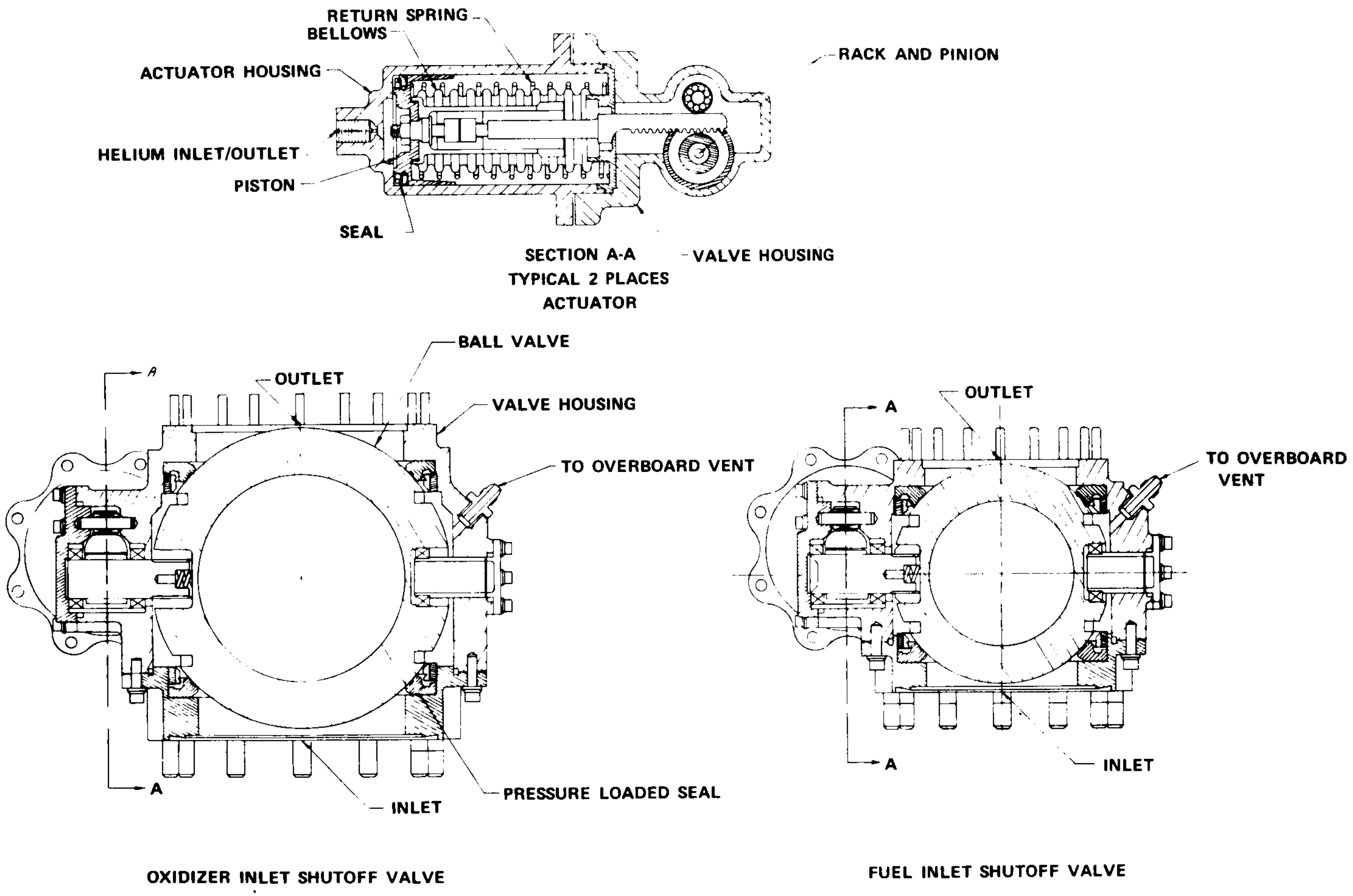

RETURN SPRING
BELLOWS
ACTUATOR HOUSING
HELIUM INLET/OUTLET
PISTON
SEAL
RACK AND PINION
SECTION A-A
TYPICAL 2 PLACES
ACTUATOR
VALVE HOUSING
BALL VALVE
OUTLET
VALVE HOUSING
TO OVERBOARD VENT
A
A
PRESSURE LOADED SEAL
INLET
OXIDIZER INLET SHUTOFF VALVE
OUTLET
A
TO OVERBOARD VENT
A
INLET
FUEL INLET SHUTOFF VALVE

Figure C-212. Inlet Shutoff Valve

FD 76003

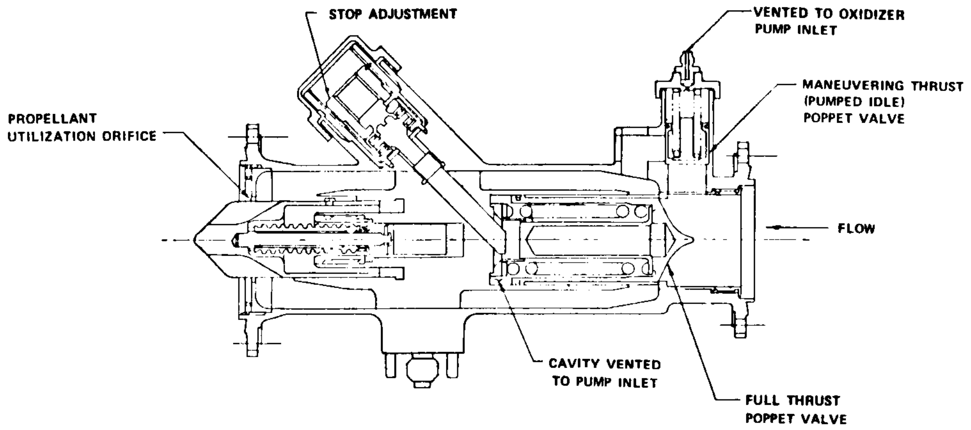

STOP ADJUSTMENT
VENTED TO OXIDIZER PUMP INLET
MANEUVERING THRUST (PUMPED IDLE) POPPET VALVE
PROPELLANT UTILIZATION ORIFICE
FLOW
CAVITY VENTED TO PUMP INLET
FULL THRUST POPPET VALVE

Figure C-213. Oxidizer Flow Control Valve FD 76005

The total valve oxidizer flow exits through the propellant utilization orifice and pintle at the aft end of the valve. Engine mixture ratio can be varied by applying torque to an actuation shaft rack and pinion mechanism that positions the discharge pintle. The actuation shaft is sealed by a bellows assembly. The pinion shaft incorporates stops to limit shaft rotation and, therefore, engine mixture ratio range within allowable limits.

The valve is designed to be manufactured to aluminum with stainless steel hardware. The estimated weight is 9 lb.

c. Main Fuel Control

The main fuel control shown in figure C-214, as its name implies, provides the main fuel control functions, i.e., control of turbine bypass flow for thrust regulation and ventage of fuel at shutdown.

The thrust control portion of the main fuel control valve, shown in figure C-214, is a normally closed, servo-operated, variable-position bypass valve used to control engine thrust by the regulation of the turbine power. Control of engine thrust is provided at full thrust by combustion chamber pressure acting through a servo pressure supplied from engine turbine upstream pressure. The bypass valve position feedback signal is mechanically transmitted. As combustion chamber pressure varies from the desired value, the action of the control allows the turbine bypass section to vary fuel flow through the turbine. This regulates turbine power and speed which in turn regulates combustion chamber pressure. The thrust control valve position is monitored by a LVDT device and the data are interpreted as a degree of engine health.

The valve is also pressure actuated to allow the settling of two discrete areas for metering turbine bypass hydrogen flow during tank head idle and maneuver thrust operation. During tank head idle operation the valve is activated to the full open position by helium operating on a bellows actuator. During maneuver thrust operation, the valve is actuated to an intermediate area by gaseous hydrogen operating on a concentric secondary bellows actuator.

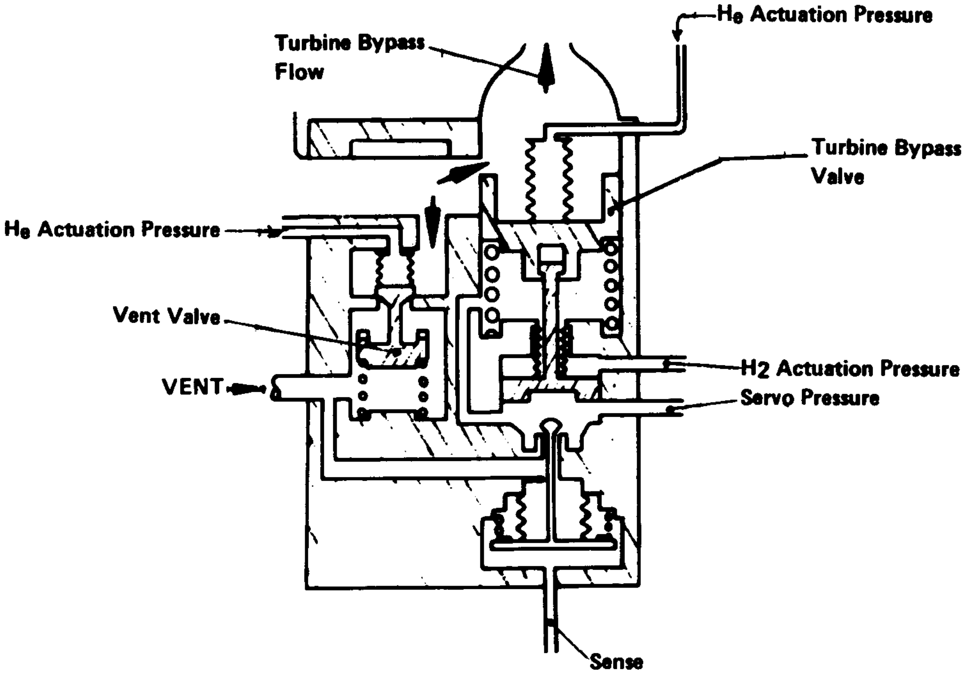


Figure C-214. Main Fuel Control FD 76902

The fuel vent valve portion of the main fuel control is a pressure operated, two-position, poppet valve that is spring loaded open to provide pressure relief of the fuel system lines during the engine shutdown. The valve is maintained in the closed position during all three active modes of engine operation. At the start signal, helium pressure actuates the valve assembly, moving the valve to close the overboard vent port. At shutdown, when helium pressure is removed, the vent port opens fully, relieving fuel pressure in the fuel system lines.

The estimated weight of the main fuel control is 10 lb.

d. Gaseous Oxidizer Valve

The gaseous oxidizer valve shown in figure C-215 is a spring-loaded, normally open, pressure activated sleeve valve located between the GO_2 heat exchanger and the injector. This valve meters gaseous oxygen flow during tank head idle and regulates oxygen flow during the transient to maneuver thrust. The pressure differential used for valve actuation is maintained on the sleeve piston by valve inlet pressure on the high side and by venting a low pressure valve cavity to the oxidizer pump inlet. As line pressure increases during the acceleration to maneuver thrust, the valve closes against the preloaded spring. The valve is designed with an aluminimum housing and valve sleeve. The estimated valve weight is 2.0 lb.

e. Tank Pressurization Valves

Propellant tank pressurization valves shown in figure C-216 are spring-loaded, normally closed, line pressure actuated, two-position poppet valves that supply propellants for fuel and oxidizer tank pressurization. These valves limit but do not regulate the tank pressurant flowrate. Flow regulation is provided within the vehicle tank pressurization system. When the differential between line pressure and an internal cavity vented to pump inlet pressure increases to a predetermined value, the total force acting on the poppet overcomes the spring load and the valve opens fully. The propellant tank pressurization valves are designed of aluminum with stainless steel fittings with a mylar poppet seal. The estimated weight of each valve is 2 lb.

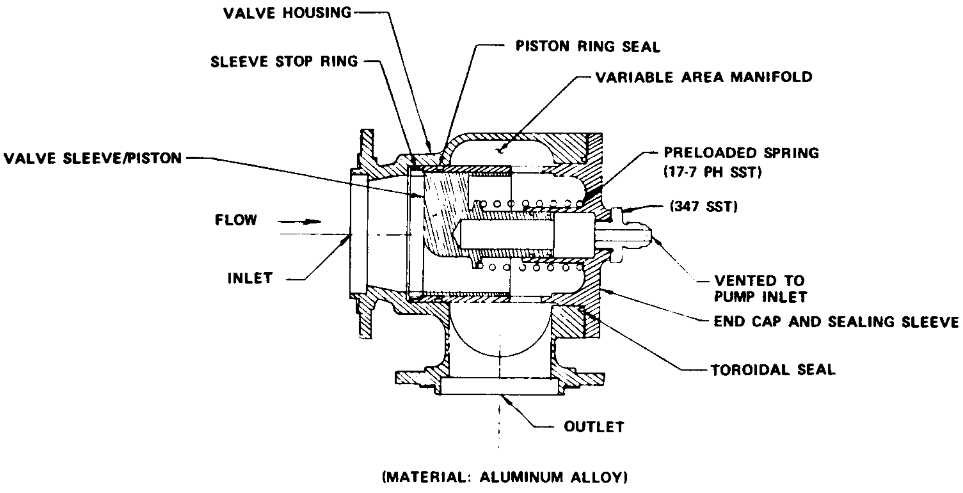

VALVE HOUSING
SLEEVE STOP RING
PISTON RING SEAL
VARIABLE AREA MANIFOLD
VALVE SLEEVE/PISTON
PRELOADED SPRING (17-7 PH SST)
(347 SST)
FLOW
INLET
VENTED TO PUMP INLET
END CAP AND SEALING SLEEVE
TOROIDAL SEAL
OUTLET
(MATERIAL: ALUMINUM ALLOY)

Figure C-215. Gaseous Oxidizer Valve FD 76002A

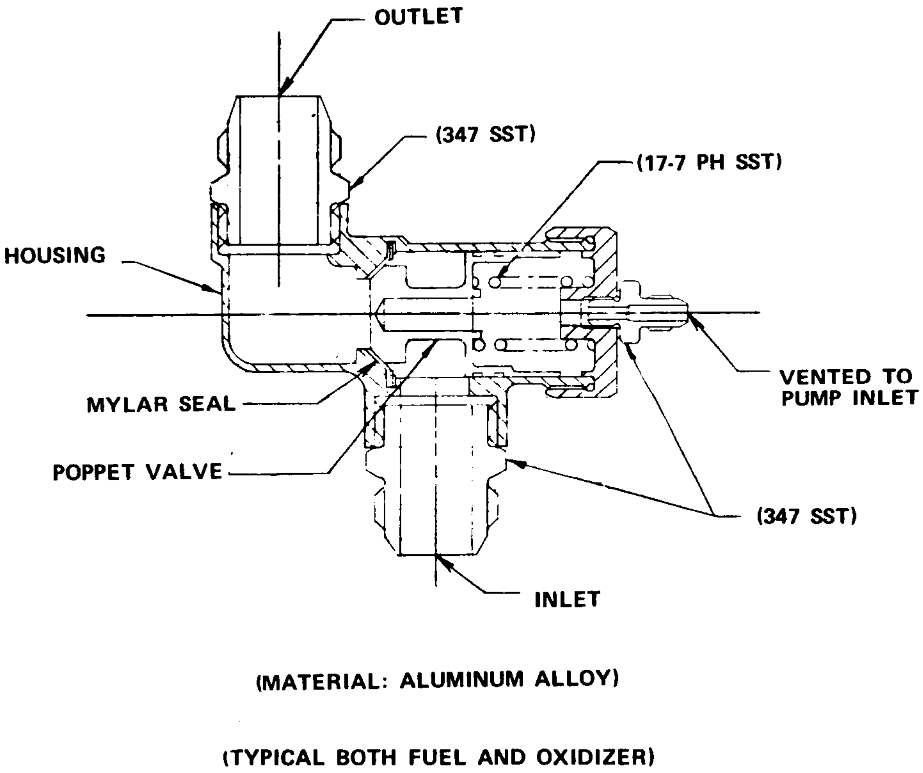
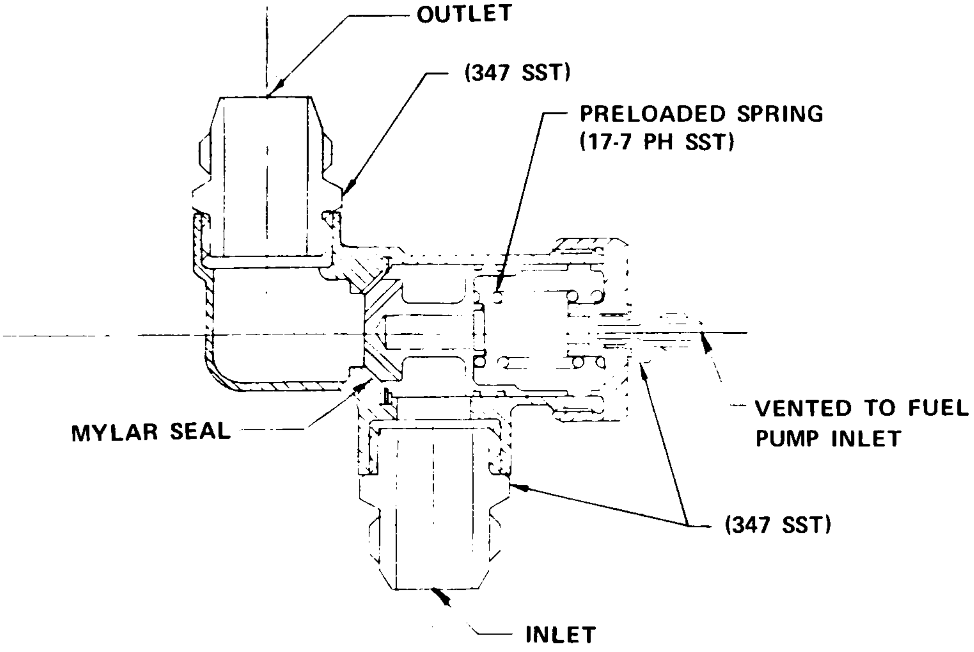

OUTLET
(347 SST)
(17-7 PH SST)
HOUSING
VENTED TO PUMP INLET
MYLAR SEAL
POPPET VALVE
(347 SST)
INLET
(MATERIAL: ALUMINUM ALLOY)
(TYPICAL BOTH FUEL AND OXIDIZER)

Figure C-216. Tank Pressurization Valve FD 76007A

f. Nozzle Coolant Valve

The nozzle coolant valve shown in figure C-217 is a spring-loaded, normally closed, line pressure actuated, two-position, poppet valve which utilizes the same housing and valve design as the tank pressurization valves. The valve poppet is orificed to provide a small area when closed to meter coolant flow to the dump-cooled nozzle during tank head idle operation. As line pressure increases to a level greater than 20 psia the total force acting on the poppet overcomes the spring preload and the valve fully opens for maneuver and full thrust operation. The nozzle coolant valve is estimated to weigh 2 lb.



(MATERIAL: ALUMINUM ALLOY)

Figure C-217. Nozzle Coolant Valve FD 76006A

g. Main Fuel Shutoff Valve

The main fuel shutoff valve, shown in figure C-218, is a helium operated, two-position, normally closed annular gate valve. The valve serves to prevent the flow of fuel through the fuel pump turbine during tank head idle operation and provides a rapid cutoff of fuel flow to the combustion chamber at engine shutdown.

The shutoff valve gate is opened by helium pressurization of the bellows assembly to allow the flow of fuel through the turbine at all operating modes above tank head idle. The compressed shutoff valve spring returns the gate to its normally closed position when helium pressure is vented at engine shutdown. Sealing is accomplished by the sealing of the spherical surface of the gate against a conical surface on the valve housing and by the gate seal ring.

The valve is constructed primarily of steel and is estimated to weigh 3 lb.

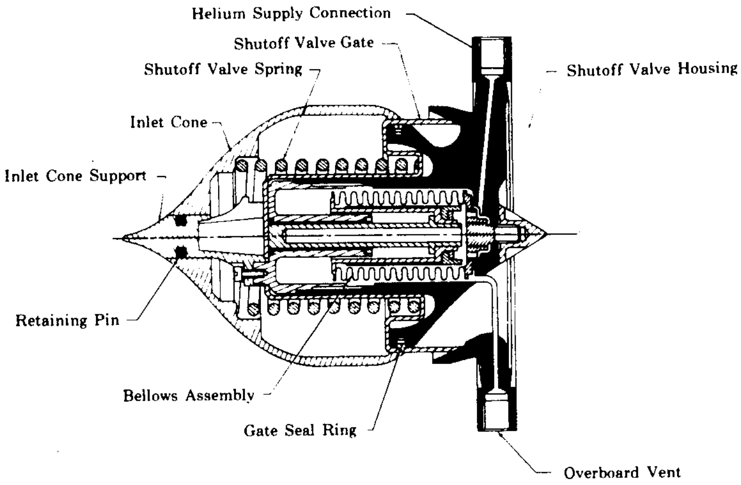


Figure C-218. Main Fuel Shutoff Valve Schematic FD 1551D

h. Solenoid Valves

The solenoid valves are solenoid actuated, direct acting, 3-way valves with double-ended poppets that supply helium or hydrogen actuation fluid to the various propellant valves.

The four solenoid valves used in the Category IV engine are identical in design and function. The start solenoid valve controls the actuator helium supply to the propellant inlet shutoff valves and fuel vent valve. The fuel shutoff solenoid valve controls the actuator helium supply to the fuel shutoff valve. The bypass solenoid valve No. 1 controls the actuator helium supply to the turbine bypass valve. The bypass solenoid valve No. 2 controls the actuator hydrogen supply to the turbine bypass valve. Each solenoid is estimated to weigh 2.5 lb.

4.3.5.2 Instrumentation and Harness System

The instrumentation and command system on the RL10 Category IV engine consists of an instrumentation enclosure, command harness, instrumentation harness, and various sensing units which monitor engine parameters listed in table C-42. The engine electrical wiring layout is shown in figure C-219. The instrumentation enclosure contains the solenoids, pressure transducers, completion networks, and other signal conditioning units for the temperature and vibration sensors. Since these instruments require a controlled temperature environment for operation, the instrumentation enclosure also contains an electrical heater and a thermocouple to monitor the internal temperature. Mounted on the instrumentation enclosure are 2 interface connectors which mate with the vehicle cables. One connector provides command signals from the vehicle computer to the engine solenoids, exciters, and nozzle motors. The other connector provides 28 vdc excitation to the engine instrumentation and returns the instrumentation output signals to the vehicle. Also mounted on the instrumentation enclosure are 2 connectors which mate with the engine command and engine instrumentation harnesses.

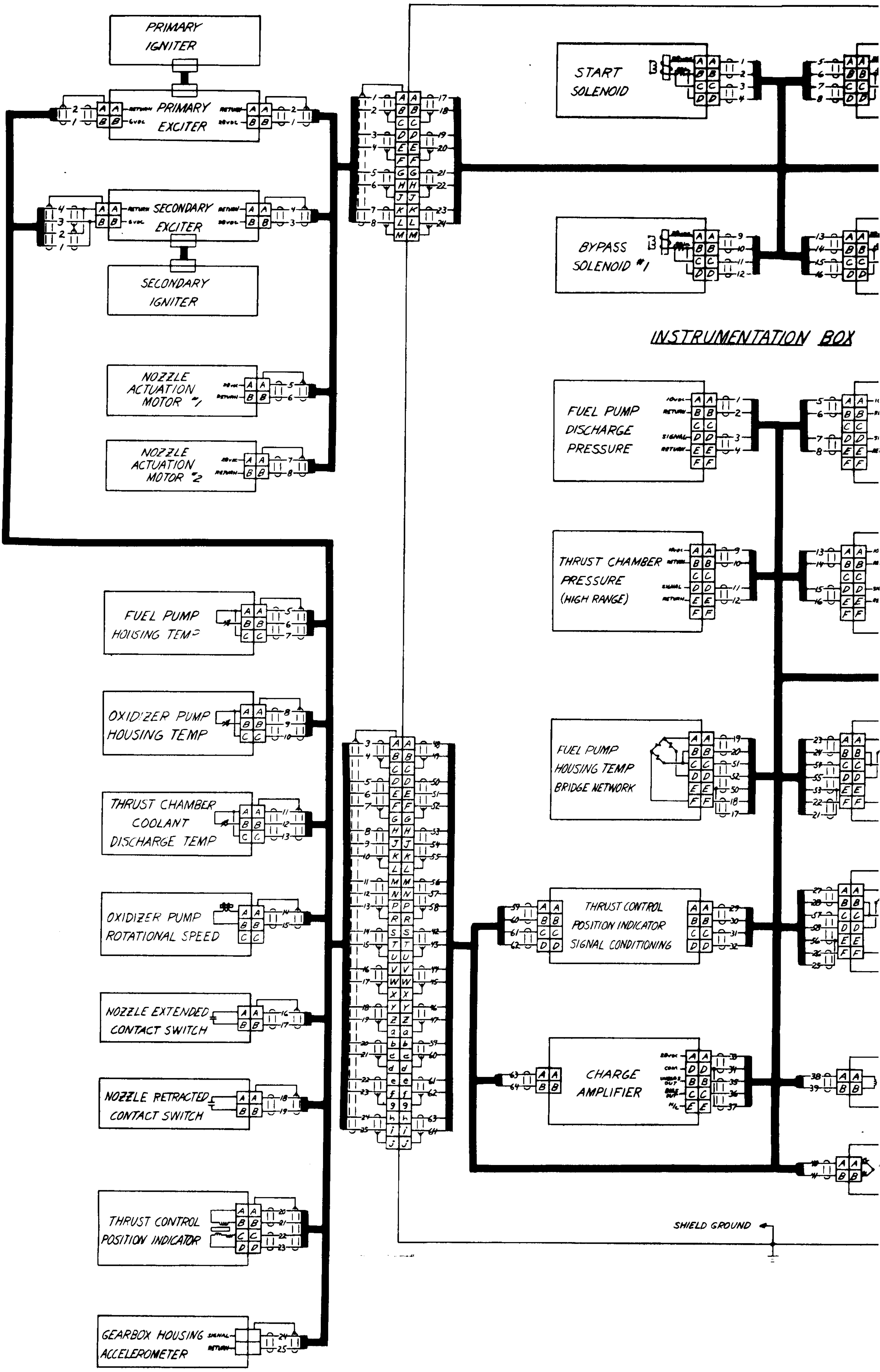
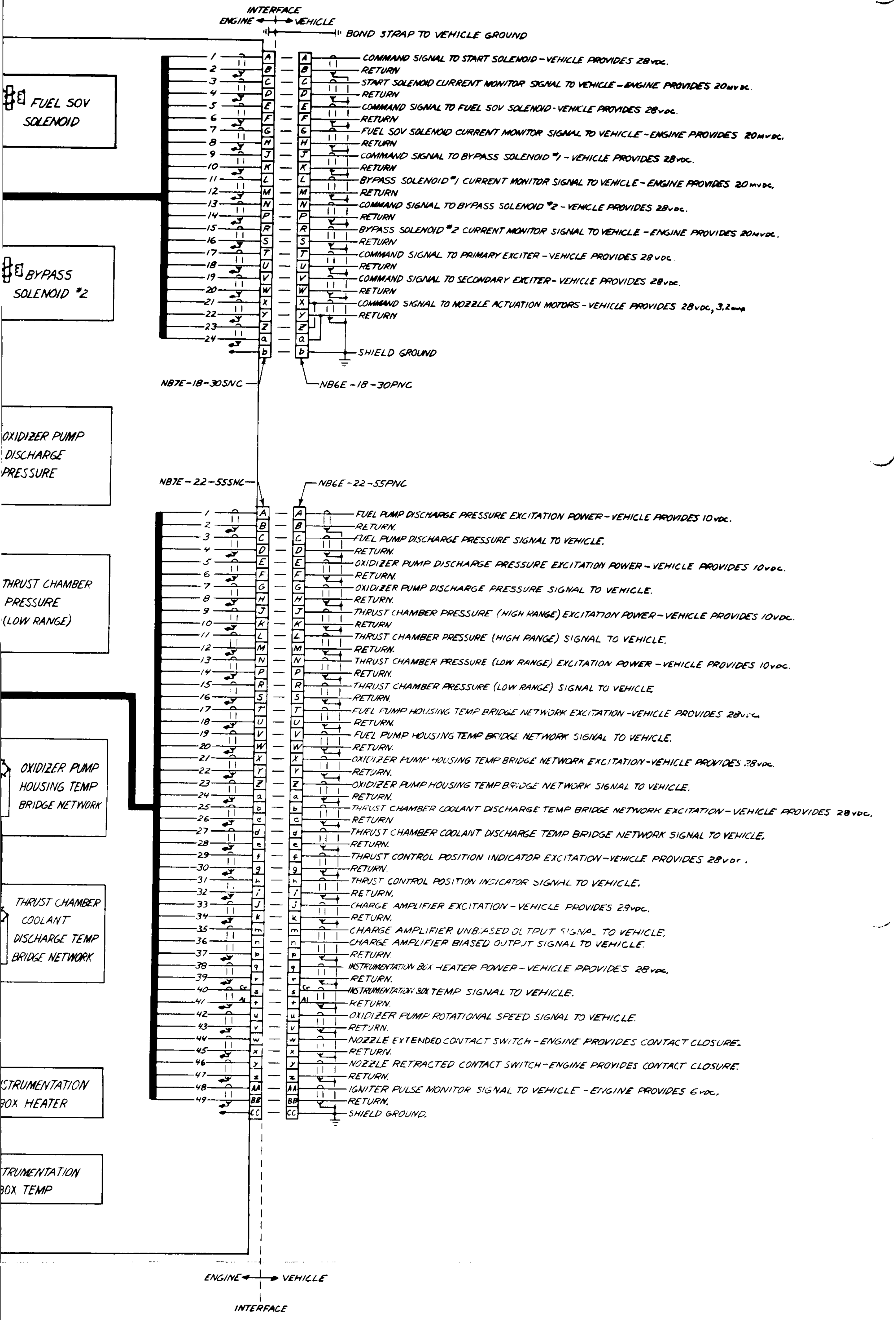

PRIMARY IGNITER
PRIMARY EXCITER
RETURN
6VDC
28VDC
SECONDARY EXCITER
SECONDARY IGNITER
NOZZLE ACTUATION MOTOR #1
NOZZLE ACTUATION MOTOR #2
FUEL PUMP HOUSING TEMP
OXIDIZER PUMP HOUSING TEMP
THRUST CHAMBER COOLANT DISCHARGE TEMP
OXIDIZER PUMP ROTATIONAL SPEED
NOZZLE EXTENDED CONTACT SWITCH
NOZZLE RETRACTED CONTACT SWITCH
THRUST CONTROL POSITION INDICATOR
GEARBOX HOUSING ACCELEROMETER
SIGNAL
START SOLENOID
BYPASS SOLENOID #1
INSTRUMENTATION BOX
FUEL PUMP DISCHARGE PRESSURE
10VDC
THRUST CHAMBER PRESSURE (HIGH RANGE)
FUEL PUMP HOUSING TEMP BRIDGE NETWORK
THRUST CONTROL POSITION INDICATOR SIGNAL CONDITIONING
CHARGE AMPLIFIER
SHIELD GROUND

Figure C-219. Wiring and Instrumentation Schematic


INTERFACE
ENGINE ←→ VEHICLE
BOND STRAP TO VEHICLE GROUND
FUEL SOV SOLENOID
BYPASS SOLENOID #2
COMMAND SIGNAL TO START SOLENOID - VEHICLE PROVIDES 28VDC.
RETURN
START SOLENOID CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20MVDC.
RETURN
COMMAND SIGNAL TO FUEL SOV SOLENOID - VEHICLE PROVIDES 28VDC.
RETURN
FUEL SOV SOLENOID CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20MVDC.
RETURN
COMMAND SIGNAL TO BYPASS SOLENOID #1 - VEHICLE PROVIDES 28VDC.
RETURN
BYPASS SOLENOID #1 CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20MVDC.
RETURN
COMMAND SIGNAL TO BYPASS SOLENOID #2 - VEHICLE PROVIDES 28VDC.
RETURN
BYPASS SOLENOID #2 CURRENT MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 20MVDC.
RETURN
COMMAND SIGNAL TO PRIMARY EXCITER - VEHICLE PROVIDES 28VDC.
RETURN
COMMAND SIGNAL TO SECONDARY EXCITER - VEHICLE PROVIDES 28VDC.
RETURN
COMMAND SIGNAL TO NOZZLE ACTUATION MOTORS - VEHICLE PROVIDES 28VDC, 3.2AMP
RETURN
SHIELD GROUND
NB7E-18-30SNC
NB6E-18-30PNC
OXIDIZER PUMP DISCHARGE PRESSURE
THRUST CHAMBER PRESSURE (LOW RANGE)
OXIDIZER PUMP HOUSING TEMP BRIDGE NETWORK
THRUST CHAMBER COOLANT DISCHARGE TEMP BRIDGE NETWORK
STRUMENTATION BOX HEATER
TRUMENTATION BOX TEMP
NB7E-22-55SNC
NB6E-22-55PNC
FUEL PUMP DISCHARGE PRESSURE EXCITATION POWER - VEHICLE PROVIDES 10VDC.
RETURN.
FUEL PUMP DISCHARGE PRESSURE SIGNAL TO VEHICLE.
RETURN.
OXIDIZER PUMP DISCHARGE PRESSURE EXCITATION POWER - VEHICLE PROVIDES 10VDC.
RETURN.
OXIDIZER PUMP DISCHARGE PRESSURE SIGNAL TO VEHICLE.
RETURN.
THRUST CHAMBER PRESSURE (HIGH RANGE) EXCITATION POWER - VEHICLE PROVIDES 10VDC.
RETURN
THRUST CHAMBER PRESSURE (HIGH RANGE) SIGNAL TO VEHICLE.
RETURN.
THRUST CHAMBER PRESSURE (LOW RANGE) EXCITATION POWER - VEHICLE PROVIDES 10VDC.
RETURN.
THRUST CHAMBER PRESSURE (LOW RANGE) SIGNAL TO VEHICLE
RETURN.
FUEL PUMP HOUSING TEMP BRIDGE NETWORK EXCITATION - VEHICLE PROVIDES 28VDC.
RETURN.
FUEL PUMP HOUSING TEMP BRIDGE NETWORK SIGNAL TO VEHICLE.
RETURN.
OXIDIZER PUMP HOUSING TEMP BRIDGE NETWORK EXCITATION - VEHICLE PROVIDES 28VDC.
RETURN.
OXIDIZER PUMP HOUSING TEMP BRIDGE NETWORK SIGNAL TO VEHICLE.
RETURN.
THRUST CHAMBER COOLANT DISCHARGE TEMP BRIDGE NETWORK EXCITATION - VEHICLE PROVIDES 28VDC.
RETURN
THRUST CHAMBER COOLANT DISCHARGE TEMP BRIDGE NETWORK SIGNAL TO VEHICLE.
RETURN.
THRUST CONTROL POSITION INDICATOR EXCITATION - VEHICLE PROVIDES 28VDC.
RETURN.
THRUST CONTROL POSITION INDICATOR SIGNAL TO VEHICLE.
RETURN.
CHARGE AMPLIFIER EXCITATION - VEHICLE PROVIDES 28VDC.
RETURN.
CHARGE AMPLIFIER UNBIASED OUTPUT SIGNAL TO VEHICLE.
CHARGE AMPLIFIER BIASED OUTPUT SIGNAL TO VEHICLE.
RETURN.
INSTRUMENTATION BOX HEATER POWER - VEHICLE PROVIDES 28VDC.
RETURN.
INSTRUMENTATION BOX TEMP SIGNAL TO VEHICLE.
RETURN.
OXIDIZER PUMP ROTATIONAL SPEED SIGNAL TO VEHICLE.
RETURN.
NOZZLE EXTENDED CONTACT SWITCH - ENGINE PROVIDES CONTACT CLOSURE.
RETURN.
NOZZLE RETRACTED CONTACT SWITCH - ENGINE PROVIDES CONTACT CLOSURE.
RETURN.
IGNITER PULSE MONITOR SIGNAL TO VEHICLE - ENGINE PROVIDES 6VDC.
RETURN.
SHIELD GROUND.
ENGINE ←→ VEHICLE
INTERFACE

FD 76713

Table C-42. Category IV Instrumentation and Monitored Parameters

Parameter	Range and Units	Uncertainty[1]
Fuel Pump		
Housing Temperature	35-660°R	±6°R
Discharge Pressure	0-2500 psia	±2%
Oxidizer Pump		
Housing Temperature	150-564°R	±6°R
Discharge Pressure	0-1600 psia	±2%
Rotational Speed	0-45,000 rpm	±4%
Thrust Chamber		
Chamber Pressure	0-1000 psia	±2%
Low Range Chamber Pressure	0-15 psia	±2%
Chamber Coolant Discharge Temperature	200-1200°R	±15°R
Miscellaneous		
Nozzle Extension Contact	Switch	N/A
Thrust Control Position	0-100%	±5%
Gearbox Vibration	0-5 g	±10%
Exciter Monitor	0-10 vdc	±5%
Solenoid Monitor	0-20 mvdc	±5%

---

[1] (Bias + $t_{.95}S$) in % of full-scale.

The engine command harness provides 28 vdc command signals from the vehicle via the instrumentation enclosure, to the nozzle motors and ignition exciters on the engine.

The engine instrumentation harness provides 28 vdc excitation to and output signals from the various temperature, speed, and position sensors on the engine. This harness is also connected to the vehicle cables through the instrumentation enclosure.

4.3.6 Engine Installation

The Category IV engine is designed for maximum interchangeability with the RL10 and its other derivative engines for ease of vehicle system integration. The mechanical connections between the engine and vehicle consist of the gimbal thrust mount and actuator attachment points, propellant inlets, pneumatic and electrical supply connections, and tank pressurization flow supply points. Mechanical interfaces for this engine are shown in the installation drawing, figure C-220. Installation dimensions are shown in table C-43. (Also see figure C-221 for dimensional references.)

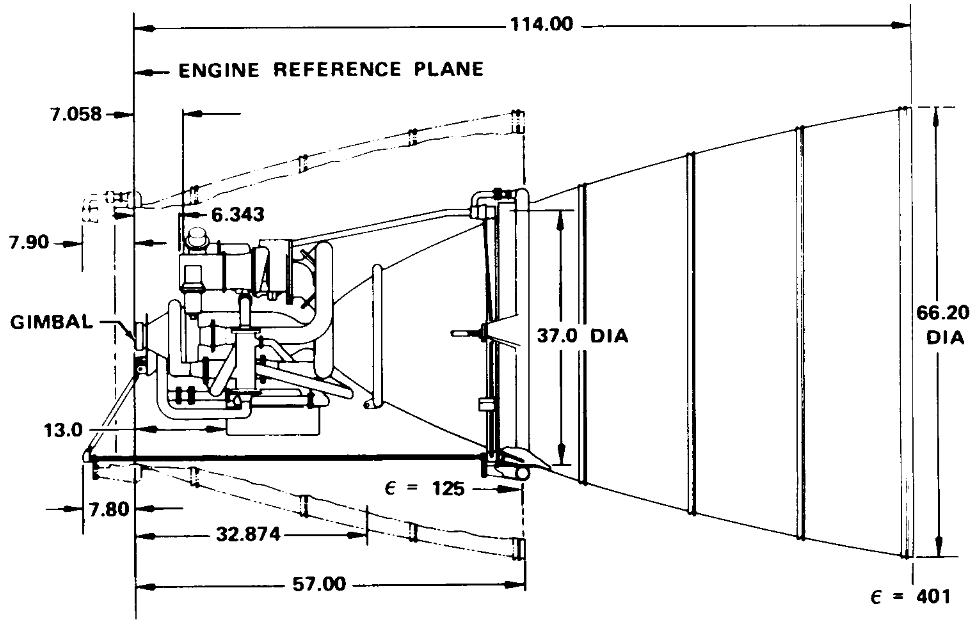

114.00
ENGINE REFERENCE PLANE
7.058
6.343
7.90
GIMBAL
13.0
7.80
32.874
57.00
ε = 125
37.0 DIA
66.20 DIA
ε = 401

Figure C-220(1). Category IV Engine Installation Drawing FD 72042A

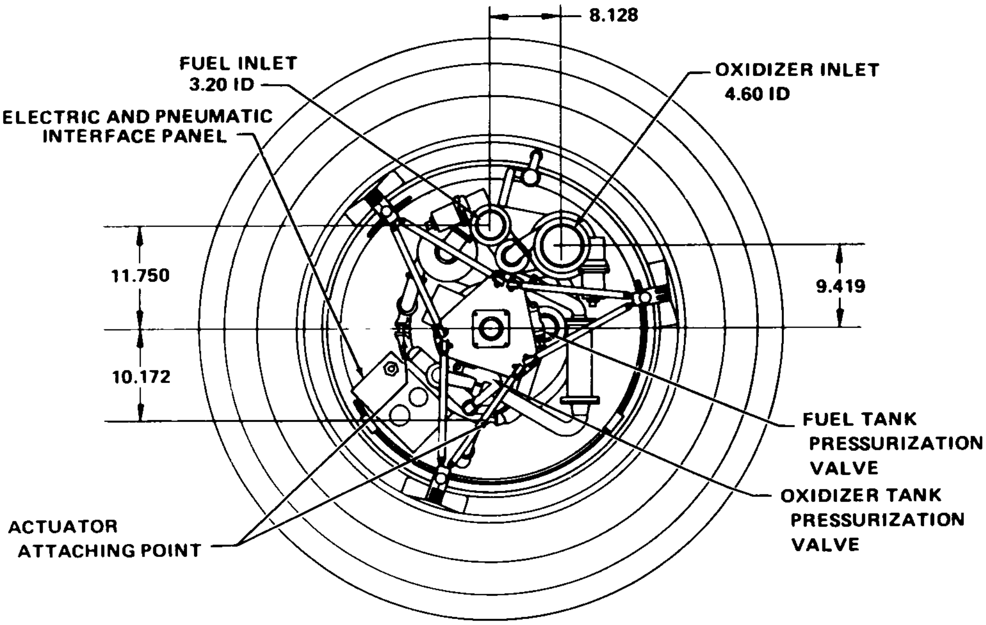

8.128
FUEL INLET 3.20 ID
ELECTRIC AND PNEUMATIC INTERFACE PANEL
OXIDIZER INLET 4.60 ID
11.750
10.172
9.419
FUEL TANK PRESSURIZATION VALVE
OXIDIZER TANK PRESSURIZATION VALVE
ACTUATOR ATTACHING POINT

Figure C-220(2). Category IV Engine Installation Drawing FD 72044A

Table C-43. Dimensions of Category IV Engine

Dimension	Description	Dimension
A	Y Axis To Fuel Inlet	11.750
B	Radius To Actuator Attach	10.172
C	Z Axis To Oxidizer Inlet	8.128
D	Y Axis To Oxidizer Inlet	9.419
E	ERP To Oxidizer Inlet	6.343
F	ERP To Fuel Inlet	7.058
G	Interface Diameter	2.876
H	Interface Height	0.240
J	ERP To Gimbal Plane	1.500
K	ERP To Actuator Attachment Point	32.874
L	Engine Length (Ext)	114
	(RTC)	57
M	Exit Diameter	66.2

ALL DIMENSIONS IN INCHES

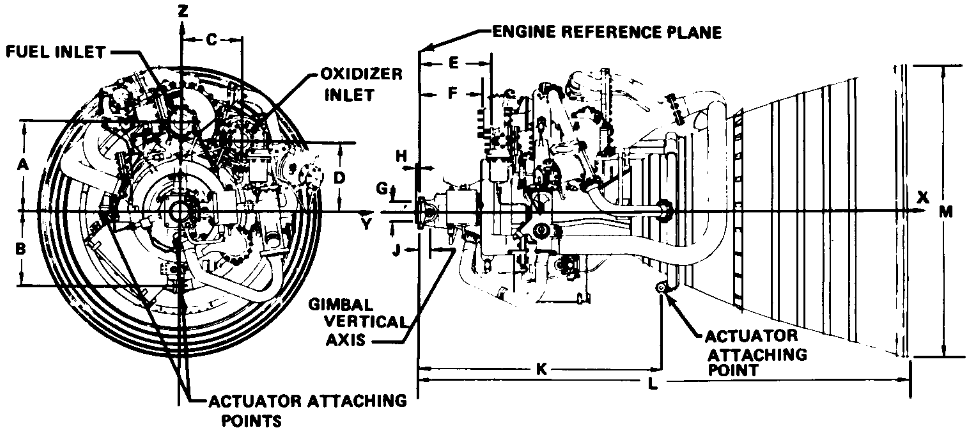


Figure C-221. RL10 Outline Installation Drawing FD 68853A

4.3.6.1 Mechanical Design

In order to clear the retractable nozzle, the powerhead for the Category IV must fit within an 18.5-in. radius of the engine centerline. The installation drawing shows an arrangement of components and plumbing which allows retractable nozzle translation past the powerhead while meeting the necessary interface restraints. The envelope of this engine extends forward of the engine/vehicle thrust mount interface 8 inches. This space is required for the retractable nozzle jackshafts and their support structure and the retractable nozzle portion of the quick disconnect nozzle feed.

a. Gimbal Thrust Mount and Actuator Attachment

The gimbal thrust mount is a mechanical connection at the forward end of the engine gimbal which provides a universal bearing system to allow gimbaling of the engine for thrust vectoring. This gimbal is similar in concept to that of the other derivative engines, and features an identical but smaller and lighter vehicle interface.

The two actuator clevis type attachments are located on the primary nozzle coolant inlet manifold positioned 90 deg apart. A bolt through the clevis and actuator provides rotation for gimbaling and absorbs inplane thrust loads. The position of these two clevises is the same as that of the RL10A-3-3 engine for interchangeability convenience.

b. Propellant Inlets

The propellant inlet connections are made at the fuel and oxidizer inlet shutoff valves. These interfaces are located below the gimbal interface at the same radial and angular locations as their RL10A-3-3 equivalents. The inlet diameters have been increased to allow two-phase pumping capability and their location is slightly closer to the engine reference plane than those of the RL10A-3-3. The fuel and oxidizer inlet interfaces are shown in figures C-222 and C-223.

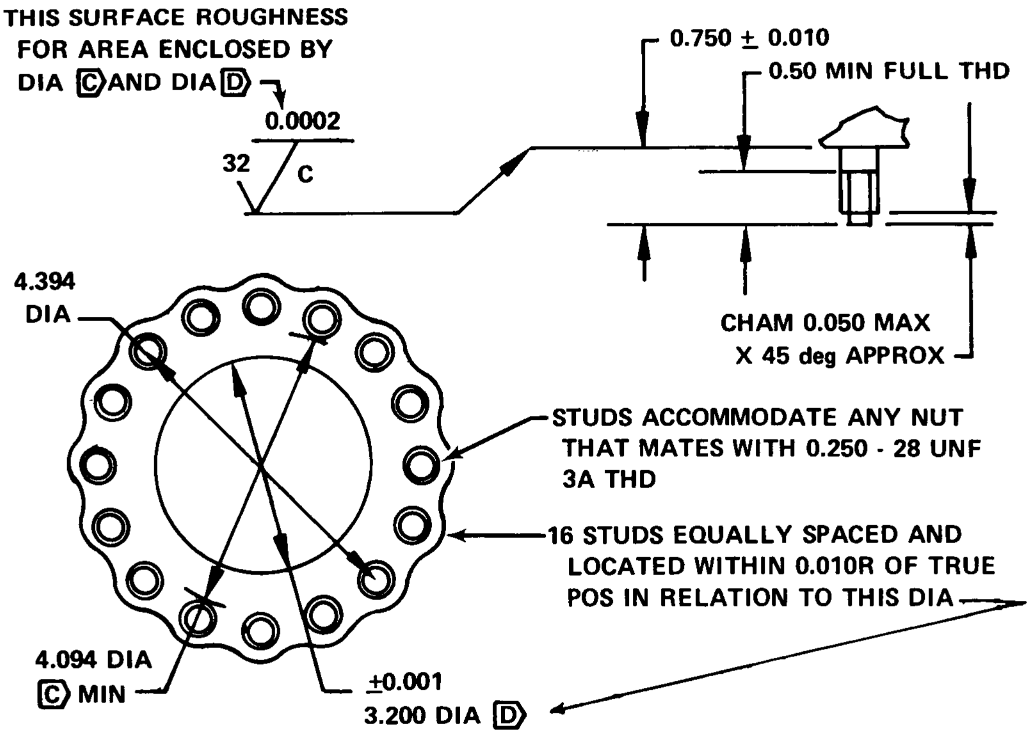

THIS SURFACE ROUGHNESS FOR AREA ENCLOSED BY DIA C AND DIA D
0.0002
32
C
0.750 ± 0.010
0.50 MIN FULL THD
CHAM 0.050 MAX X 45 deg APPROX
4.394 DIA
STUDS ACCOMMODATE ANY NUT THAT MATES WITH 0.250 - 28 UNF 3A THD
16 STUDS EQUALLY SPACED AND LOCATED WITHIN 0.010R OF TRUE POS IN RELATION TO THIS DIA
4.094 DIA C MIN
±0.001
3.200 DIA D

Figure C-222. Engine Fuel Inlet, Category IV Engine FD 75258

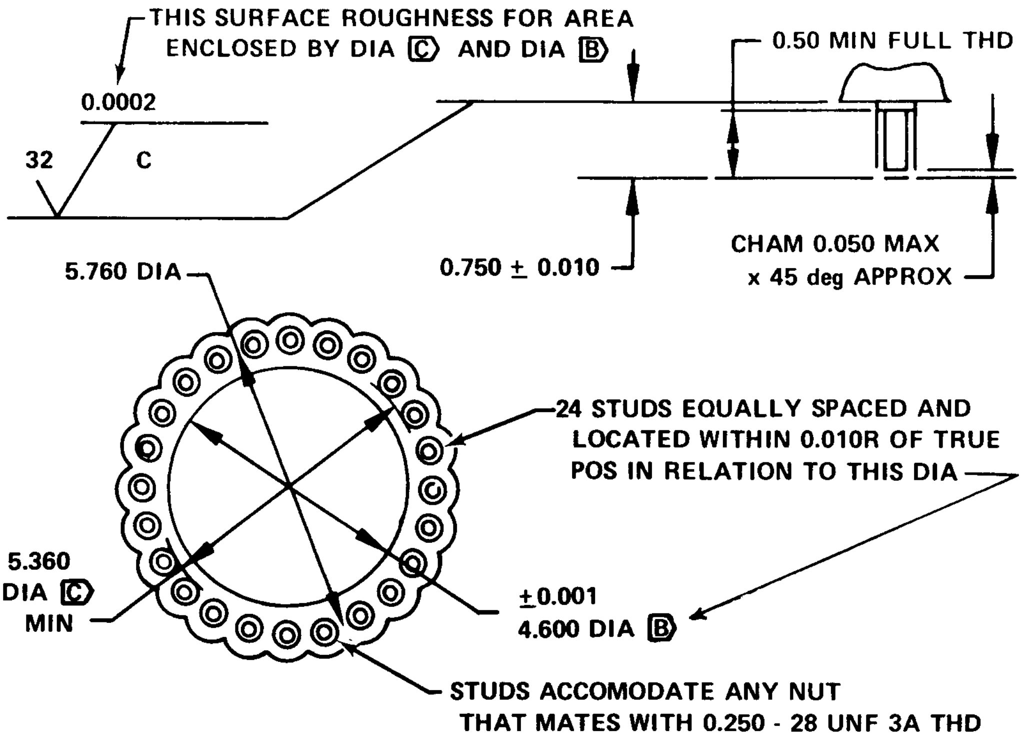

THIS SURFACE ROUGHNESS FOR AREA ENCLOSED BY DIA C AND DIA B
0.0002
32
C
0.50 MIN FULL THD
5.760 DIA
0.750 ± 0.010
CHAM 0.050 MAX x 45 deg APPROX
24 STUDS EQUALLY SPACED AND LOCATED WITHIN 0.010R OF TRUE POS IN RELATION TO THIS DIA
5.360 DIA C MIN
±0.001
4.600 DIA B
STUDS ACCOMODATE ANY NUT THAT MATES WITH 0.250 - 28 UNF 3A THD

Figure C-223. Engine Oxidizer Inlet, Category IV Engine FD 75259

c. Tank Pressurization Connections

The hydrogen and oxygen tank pressurant supply connections are located at the forward end of the engine near the engine centerline as previously shown in figure C-220. The connections are made directly to the respective tank pressurization valves by use of male AN-16 fittings.

d. Electrical-Pneumatic Interfaces

These interfaces are provided on a single interface panel on the instrumentation enclosure shown in figure C-224. The location of this panel is common to all derivative engines. There is one pneumatic (helium) connection and two electric connectors. The two electric connectors interface the vehicle with the engine command harness, and instrumentation harness.

4.3.7 Weight

The weights shown for the Category IV engine have been obtained from calculations based on conceptual designs, and from estimates based on experience with similar hardware. The weights of the following components were based on calculations from conceptual designs: thrust chamber and primary nozzle, extendible nozzle actuation system, turbopumps and gearbox, GO_2 heat exchanger, inlet shutoff valves and tank pressurizing valves. Weights of the remaining components, plumbing, and instrumentation were estimated. A complete weight breakdown is shown in table C-44 by detail, subassembly and total engine. Sources for the weight are also listed, whether estimated or computed. The total engine weight shown does not include TVC actuators, propellant utilization actuator, accessory drive equipment, or nonpropulsive vents.

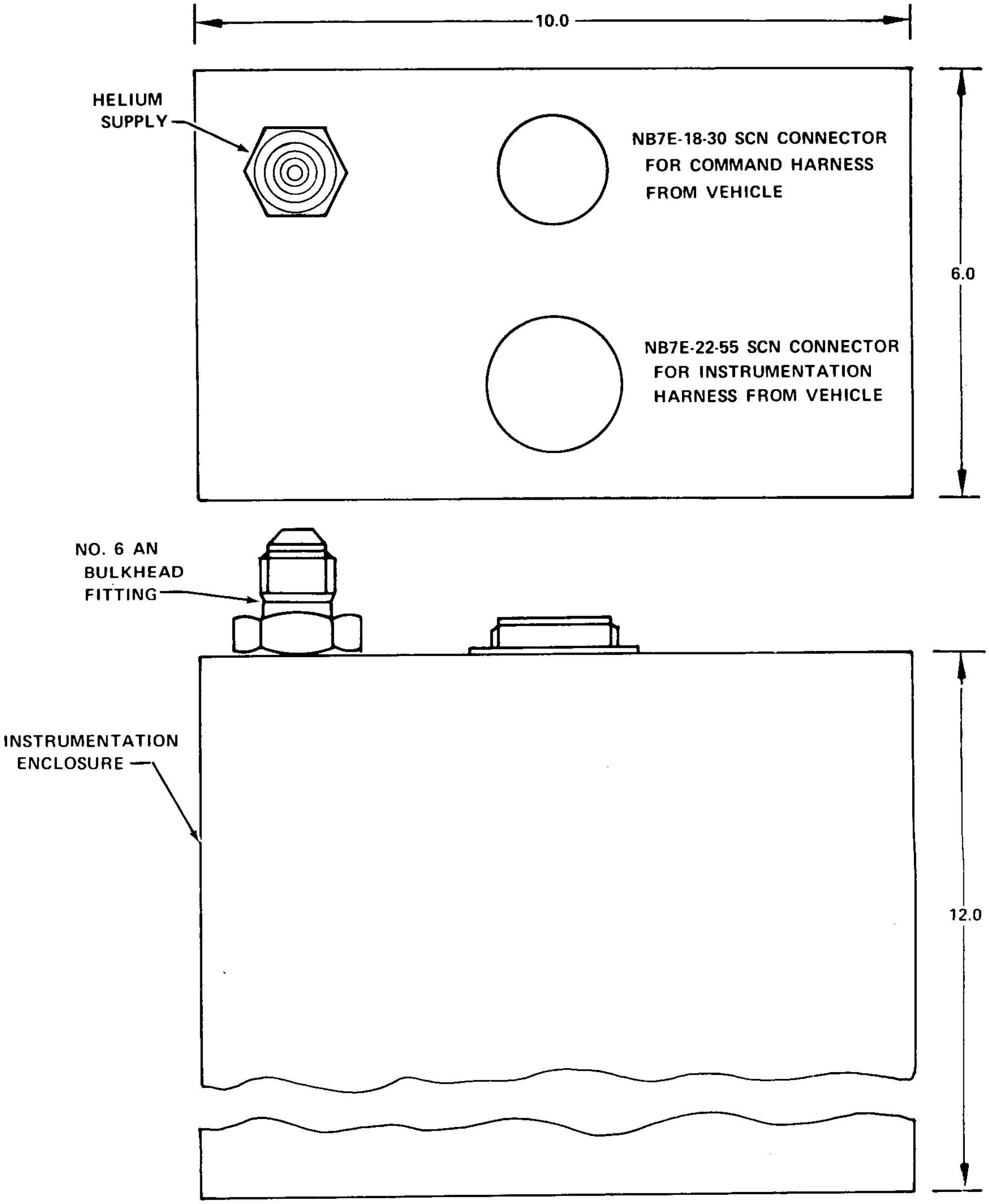

10.0
HELIUM SUPPLY
NB7E-18-30 SCN CONNECTOR FOR COMMAND HARNESS FROM VEHICLE
6.0
NB7E-22-55 SCN CONNECTOR FOR INSTRUMENTATION HARNESS FROM VEHICLE
NO. 6 AN BULKHEAD FITTING
INSTRUMENTATION ENCLOSURE
12.0

Figure C-224. Pneumatic and Electrical Vehicle/ Engine Interfaces

FD 76011

Table C-44. Category IV Weight Breakdown

Component	Estimated or Computed	Engine Weight, lb
Thrust Chamber and Primary Nozzle		
Injector Assembly	Computed	10.0
Thrust Chamber and Nozzle/Primary	Computed	68.0
Support Ring and Seal	Computed	12.0
Subtotal		90.0
Extendible Nozzle		
Extendible Nozzle	Estimated	76.0
Subtotal		76.0
Extendible Nozzle Actuator System		
Extendible Nozzle Actuation System	Computed	35.0
Subtotal		35.0
Turbopumps and Gearbox		
Turbopump	Computed	87.0
Turbopump Mounts	Computed	4.0
Subtotal		91.0
GO_2 Heat Exchanger		
GO_2 Heat Exchanger	Computed	13.0
Subtotal		13.0
Ignition System		
Ignition System	Estimated	7.0
Subtotal		7.0
Controls, Valves, Actuators		
Oxidizer Inlet Shutoff Valve	Computed	15.0
Fuel Inlet Shutoff Valve	Computed	13.0
Oxidizer Flow Control Valve	Estimated	9.0
Gaseous Oxidizer Valve	Estimated	2.0
Fuel Control	Estimated	10.0
Nozzle Coolant Valve	Estimated	2.0
Tank Pressurizing Valves	Computed	4.0
Solenoid Valves	Estimated	10.0
Instrumentation	Estimated	8.8
Subtotal		73.8
Plumbing and Miscellaneous Hardware		
Tube - Oxidizer Control to Injector	Estimated	2.3
Tube - Main Oxidizer Pump Inlet	Estimated	1.8
Tube - GO_2 Hex Supply and Exit	Estimated	1.8
Tube - Fuel Pump to Vent Valve Tube - Vent Valve to Thrust Chamber	Estimated	2.5
Tube - Thrust Chamber to Turbine	Estimated	5.0
Tube - Turbine to Main Fuel Shutoff Valve	Estimated	7.6
Tube - Turbine Bypass	Estimated	3.6
Engine Mount	Estimated	4.0
Small Lines	Estimated	2.0
Connecting and Miscellaneous Hardware	Estimated	7.4
Subtotal		38.0
TOTAL ENGINE WEIGHT		423.8

Note 1: Percent Computed - 61
Percent Estimated - 39

The estimated maximum dry weight and the estimated wet weight at normal operating conditions are shown below:

Basic Dry Weight, lb	Propellant Weight, lb	Total Wet Weight, lb
424	12	436

Engine center of gravity was determined by the moment method using the weights of table C-44 and the moment arms known, or modified, from the RL10A-3-3. New components had moment arms determined by calculation, or for small parts, estimation.

Axial (X), Nozzle Retracted	Axial (X), Nozzle Extended	Horizontal (Y)	Vertical (Z)
20.9	30.7	0	1.7

The gimbaled moment of inertia for the Category IV engine was determined from the summation of the products of the component mass times its cg distance to the gimbal center of rotation squared. While this method ignores the individual component mass moment of inertia, preliminary nature of the study justifies its use. The estimated gimbaled moments of inertia follow and are in units of in.-lb-sec^2.

I_{xx}	I_{yy}	I_{zz}
325	2275	2245

SECTION D
PARAMETRIC DATA

1. INTRODUCTION

Three baseline engines were defined under this contract, one of them a new design (RL10 Category IV) and the other two derivatives of the existing RL10 engine (the RL10 Derivative IIA and IIB). All the baseline engines are designed to fixed requirements. Since these requirements are not optimum for all Space Tug vehicle applications, a parametric analysis was conducted during this study. The following table D-1 summarizes the independent and dependent parameters which were perturbated in this analysis:

Each parametric engine design was optimized to give maximum nozzle performance for a given length (minimum length contour). Two-dimensional equilibrium analyses were used to define parametrically minimum length nozzle contours, and the contours having been defined, performance was calculated using JANNAF methodology. The JANNAF method used is described in Appendix II of this report.

The nozzles were of the two-position variety in which the primary (fixed) portion of the nozzle was regeneratively cooled and the secondary (translating) portion of the nozzle was dump cooled. The dump cooling flow was redefined for each design to provide adequate cooling flow for each parametric design point. The propellant injector was considered to be optimized for each design point condition and was assumed to give an energy release efficiency of 0.9941 and a striation loss of less than 0.1 sec of impulse.

Engine weight was generated using the turbopump, nozzle actuator, heat exchanger, control, plumbing and miscellaneous weights from the 15,000 lb thrust baseline engine design. The remaining engine weight, i.e., chamber/nozzle weight, was estimated using a fixed weight/ft^2 of nozzle surface area of 1.12, 2.46 and 6.0 for dump cooled, regeneratively cooled tubular and regeneratively cooled nontubular sections, respectively.

2. DERIVATIVE II ENGINE PARAMETRIC DATA

The following ground rules were used in the parametric study of Derivative II engines:

a. Constant Design thrust of 15,000 lb

b. Constant chamber pressure of 400 psia for design point analysis

c. Engine mixture ratio $4.5 \leq O/F \leq 7.0$

d. Retracted engine length varied between powerhead limit and 70 in.

e. Primary nozzle exit diameter ≥ 40 in.

2.1 Retracted Engine Length Effects

The available range of retracted engine length of the Derivative II engine is limited on the low end by engine powerhead diameter and on the high end by the 70-in. length constraint inherent in the engine definition. Minimum retracted engine length is essentially made up of gimbal length, combustion chamber length, and primary nozzle length. The turbopumps are packaged around the combustion chamber and do not add to the overall length.

Table D-1. Parametric Study Variables

Baseline Engine	Independent Variable	Dependent Variable	Constant
RL10 Derivative IIA & IIB	Installed Length (70 to 55 in.)	Specific Impulse Engine Weight Engine Geometry	Thrust (15,000 lb) Mixture Ratio (6.0) Chamber pressure (400 psia)
	Mixture Ratio (4.5 to 7)	Specific Impulse Engine Weight Engine Geometry	Thrust (15,000 lb) Installed Length (55 and 70 in.) Chamber Pressure (400 psia)
	Chamber Pressure (200 to 550 psia)	Engine Life Engine Thrust Specific Impulse	Engine Geometry Mixture Ratio (6.0)
RL10 Category IV	Installed Length With RL10 Derivative IIA Interfaces (50 to 70 in.) Installed Length With No Interface Constraint (30 to 58 in.)	Specific Impulse Engine Weight Engine Geometry Chamber Pressure	Thrust (15,000 lb and 20,000 lb) Mixture Ratio (6.0) Life (10 hr/300 firings)
	Mixture Ratio (4.5 to 7)	Specific Impulse Engine Weight Engine Geometry Chamber Pressure	Thrust (15,000 lb) Installed Length (57 in.) Life (10 hr/300 firing)
	Allowable Cycle Life (50 to 650 firings)	Specific Impulse Chamber Pressure Engine Weight	Thrust (15,000 lb) Mixture Ratio (6.0) Installed Length (57 in.)

Since the gimbal and combustion chamber lengths remain essentially constant, engine length is primarily a function of primary nozzle area ratio; and since engine thrust, chamber pressure and nozzle contour are almost constant, minimum engine length is primarily a function of primary nozzle exit diameter. The smaller the exit diameter the shorter the engine length. The primary nozzle exit diameter must be slightly larger than the engine powerhead diameter, however, to allow the secondary nozzle to fully translate.

The shortest allowable Derivative II engine has a retracted length of 55 in. The engine was configured with a secondary nozzle length equal to the length of the primary engine. An "equal length" engine shorter than this would require the primary nozzle exit diameter to be less than 40 in. violating ground rule "e" given above. The 40-in. minimum diameter constraint is determined by the Derivative II engine powerhead diameter of 39.5 in.

For engines larger than 55 in. the best performance is obtained by "equal length" configurations which require the primary nozzle exit diameter to be greater than 40 in. For example, a 70-in. engine with total engine length (secondary nozzle extended) of 140 in. would have a primary exit diameter of approximately 57.5 in. Testing of such an engine would require a modification to the FRDC test facilities, however, since the E-6 stand will currently accept nozzle diameters only slightly greater than 40 in. and the E-7 stand will currently accept nozzle diameters up to approximately 47.5 in. Figure D-1 presents design parametric performance data at a mixture ratio of 6.0 for retracted length variations between 55 and 70 in. with no constraint on the maximum primary nozzle exit diameter. Figure D-2 presents design parametric data at a mixture ratio of 6.0 for retracted length variations between 55 and 70 in. with the primary nozzle exit diameter held at 40 in. throughout to allow testing on FRDC stands.

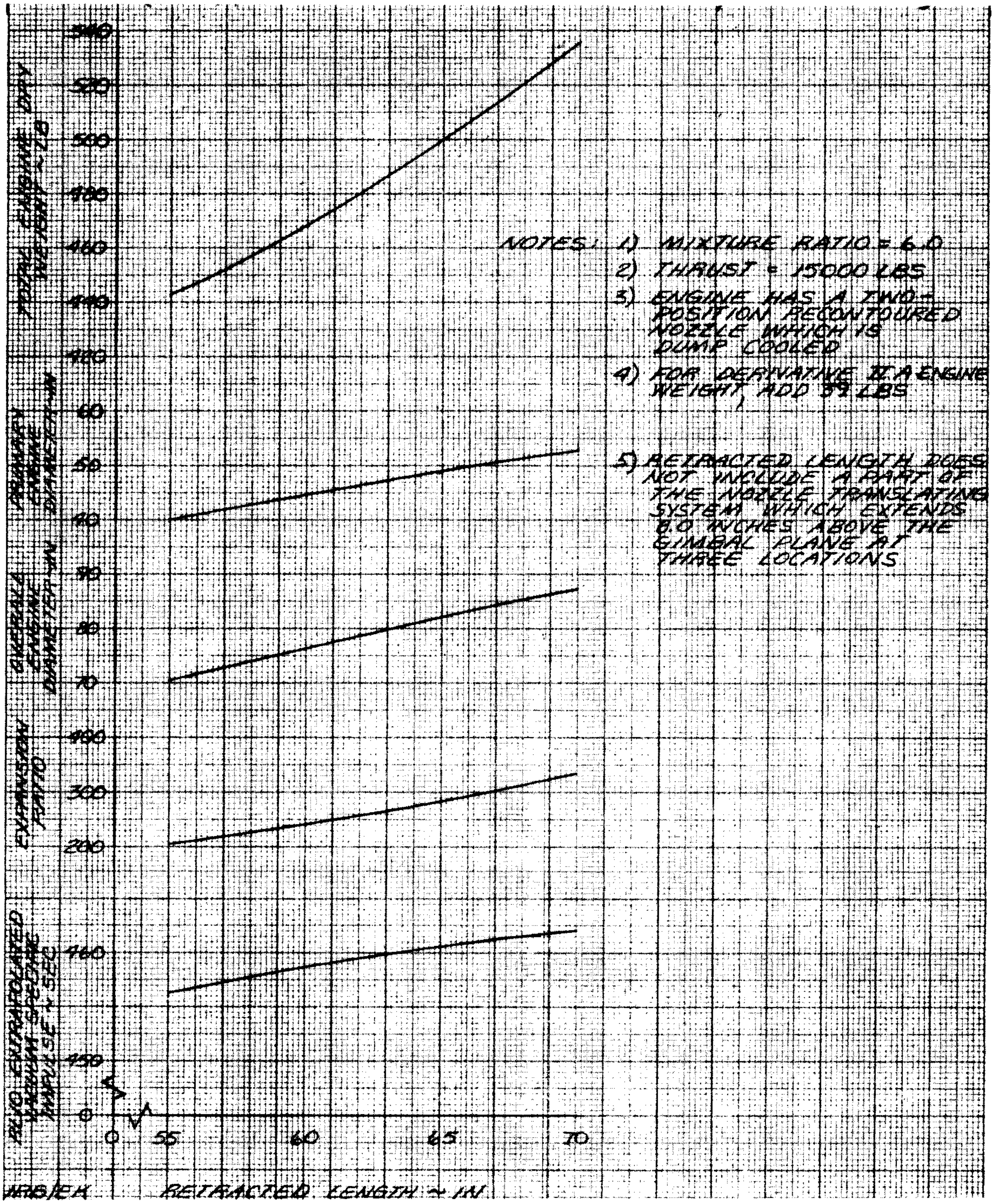


Figure D-1. Effect of Retracted Length on RL10 Derivative IIA and IIB Design Point Performance (Primary Nozzle Diameter Not Constrained by Test Facility) DF 95917

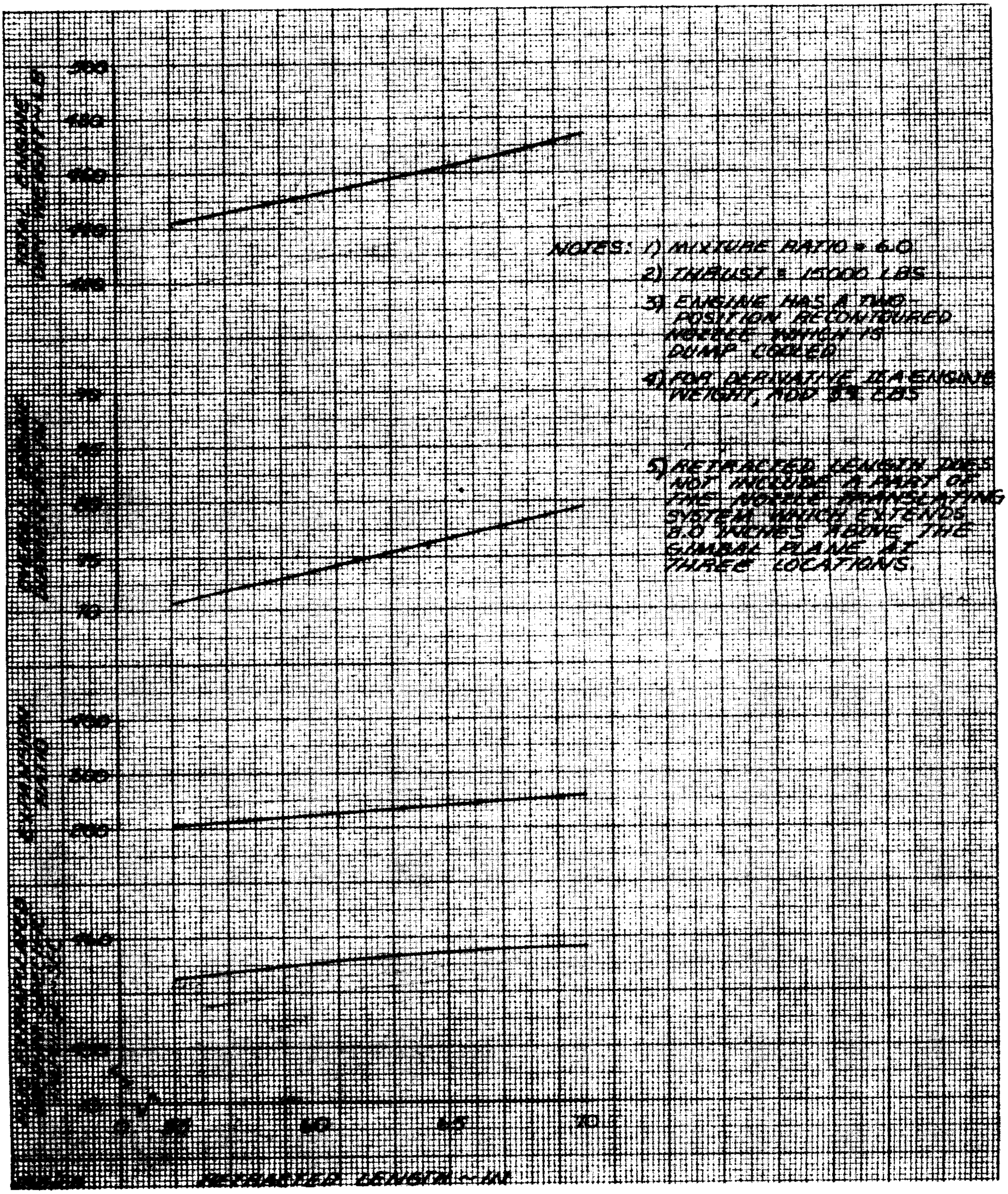


Figure D-2. Effect of Retracted Length on RL10 Derivative IIA and IIB Design Point Performance (Primary Nozzle Exit Diameter Limited to 40 in.)

DF 95918

2.2 Engine Mixture Ratio Effects

Design parametric performance data were generated over the range of mixture ratio for engines designed at the minimum and maximum allowable retracted engine lengths. The engine designs presented are capable of providing an additional off-design mixture ratio excursion of plus or minus one-half mixture ratio unit. Figures D-3 and D-4 present parametric design point data over a mixture ratio range of 4.5 to 7.0, respectively, for 55 and 70 in. retracted length engines.

2.3 Chamber Pressure Effects

Parametric life data were generated for the Derivative II engines as a function of chamber pressure with a constant engine geometry. Derivative II engine life is limited in both firing duration and number of firings. Firing duration is limited by gear wear while chamber thermal low cycle fatigue (LCF) limits the number of firings. The chamber LCF life was evaluated from a correlation of hot wall temperature and strain as a function of chamber pressure. The hot wall temperature was determined by running the cycle deck off-design. Figure D-5 presents cycle limited life, thrust and impulse as a function of chamber pressure varied between 200 psia (50% of nominal) and 560 psia (maximum power available in the baseline cycle). Results show that while engine cycle life can be increased from the baseline level of 190 cycles to 300 cycles by reducing thrust to 12,000 lb, a loss of over 3 sec specific impulse would result.

Turbopump speed and horsepower transmitted to the oxidizer pump through the gears were also estimated by off-design running of the cycle deck. The resultant change in speed and transferred horsepower was used to calculate the change in Hertz stress relative to the baseline engine design. This change was then used to calculate the change in engine wear life as shown in figure D-6. Results show that engine wear life can be increased from the baseline level of 5 hr to 10 hr by reducing thrust to 13,500 lb. A loss on specific impulse of two seconds is predicted.

3. CATEGORY IV

The ground rules used in the generation of the Category IV parametric data are as follows:

a. Optimum chamber pressure for each design point

b. Optimum chamber length for each design point; however, chamber length should not be shorter than 12 in. to ensure good combustion performance nor greater than 30 in.

c. Evaluate thrust levels of 15,000 and 20,000 lb

d. Engine mixture ratio $4.5 \leq O/F \leq 7.0$

e. Retracted engine length between 70 in. (RL10A-3-3 length) and the minimum set by the powerhead diameter

f. For engine interchangeable with RL10 Derivative IIA, powerhead diameter limits the primary nozzle exit diameter to a level greater than 37 in.

g. For engines not interchangeable with the RL10 Derivative engines the minimum exit diameter is 26 in.

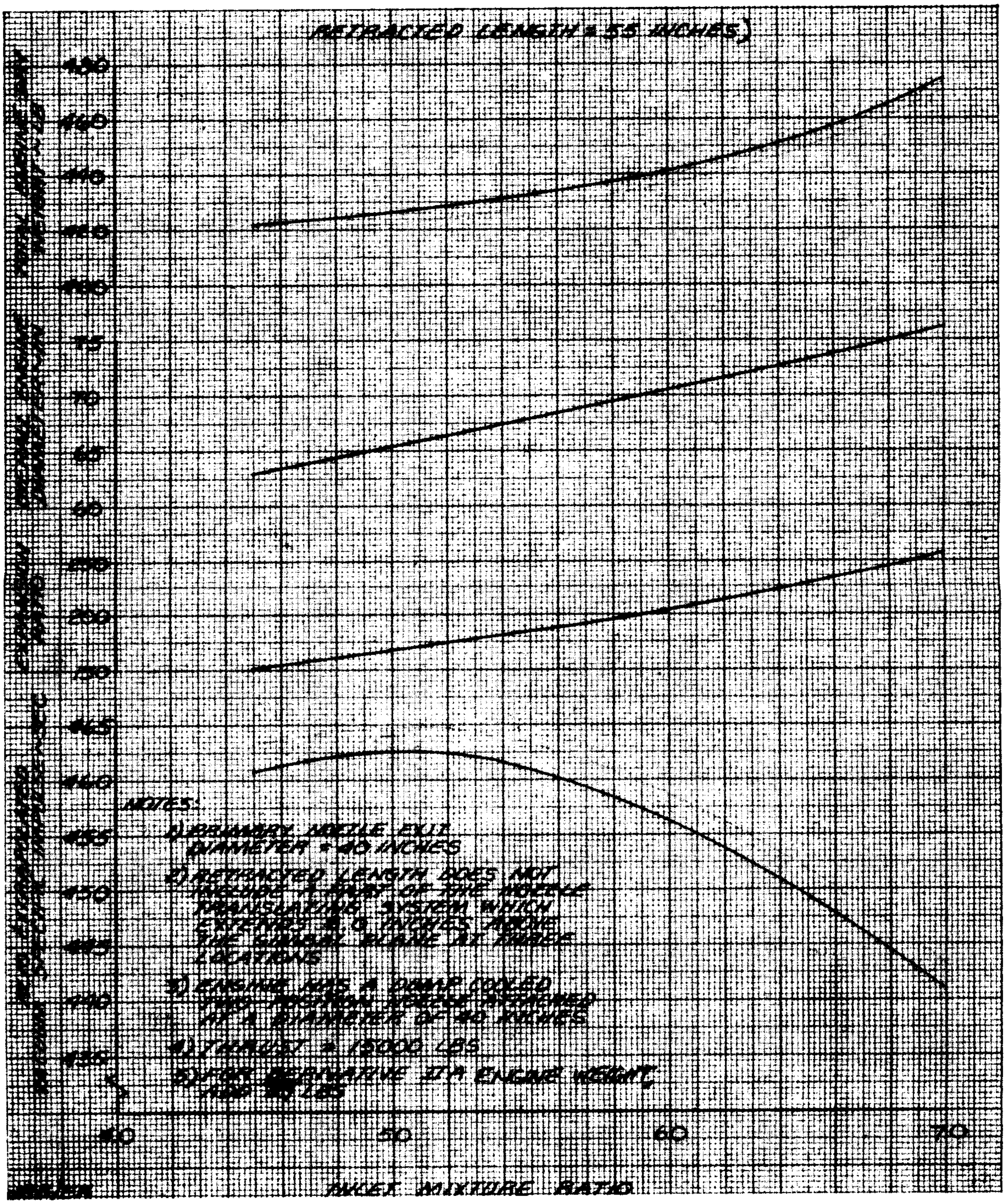


Figure D-3. Effect of Mixture Ratio on RL10 Derivative IIA and IIB Engine Design Point Performance

DF 95916

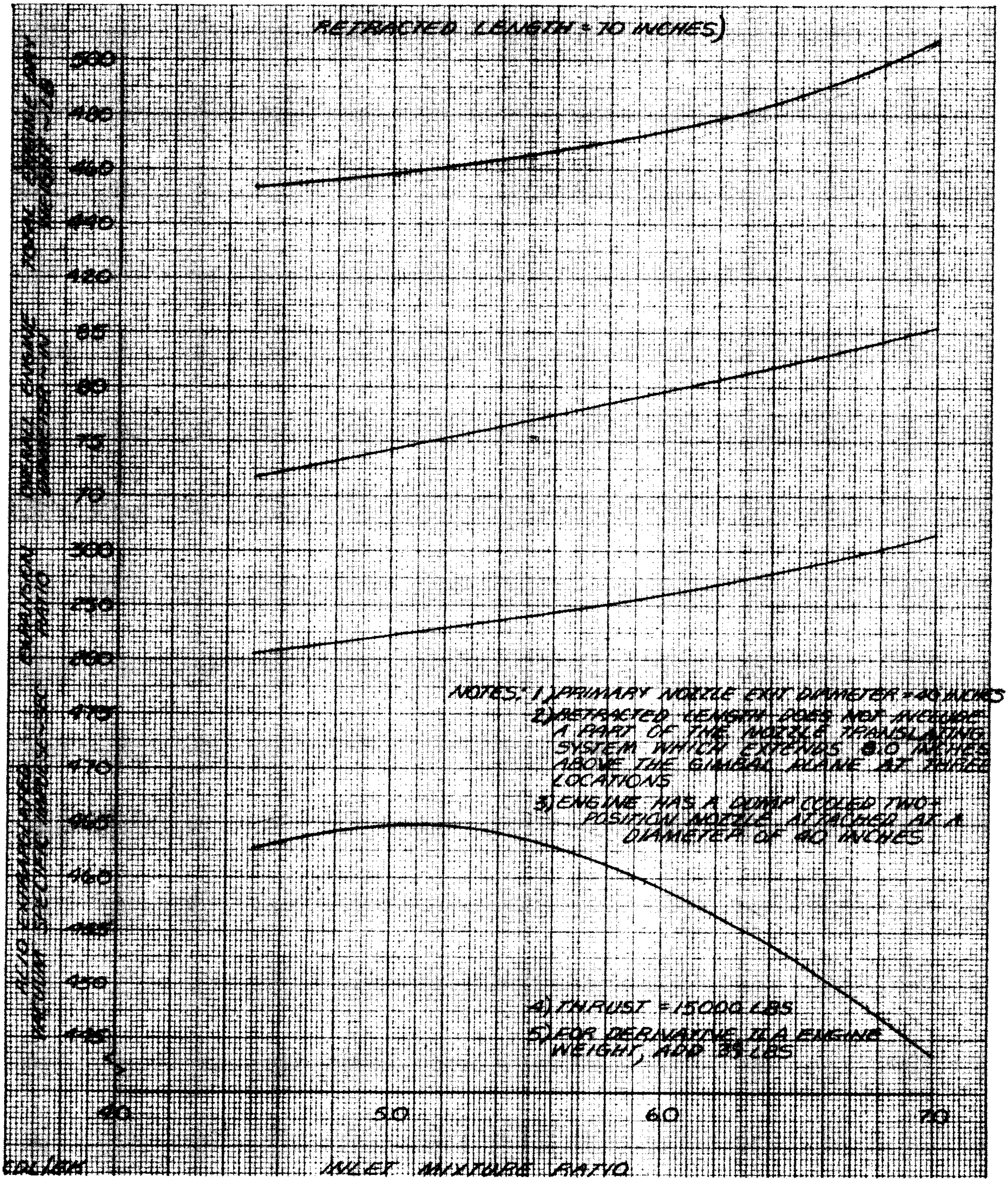


Figure D-4. Effect of Mixture Ratio on RL10 Derivative IIA and IIB Engine Design Point Performance

DF 95915

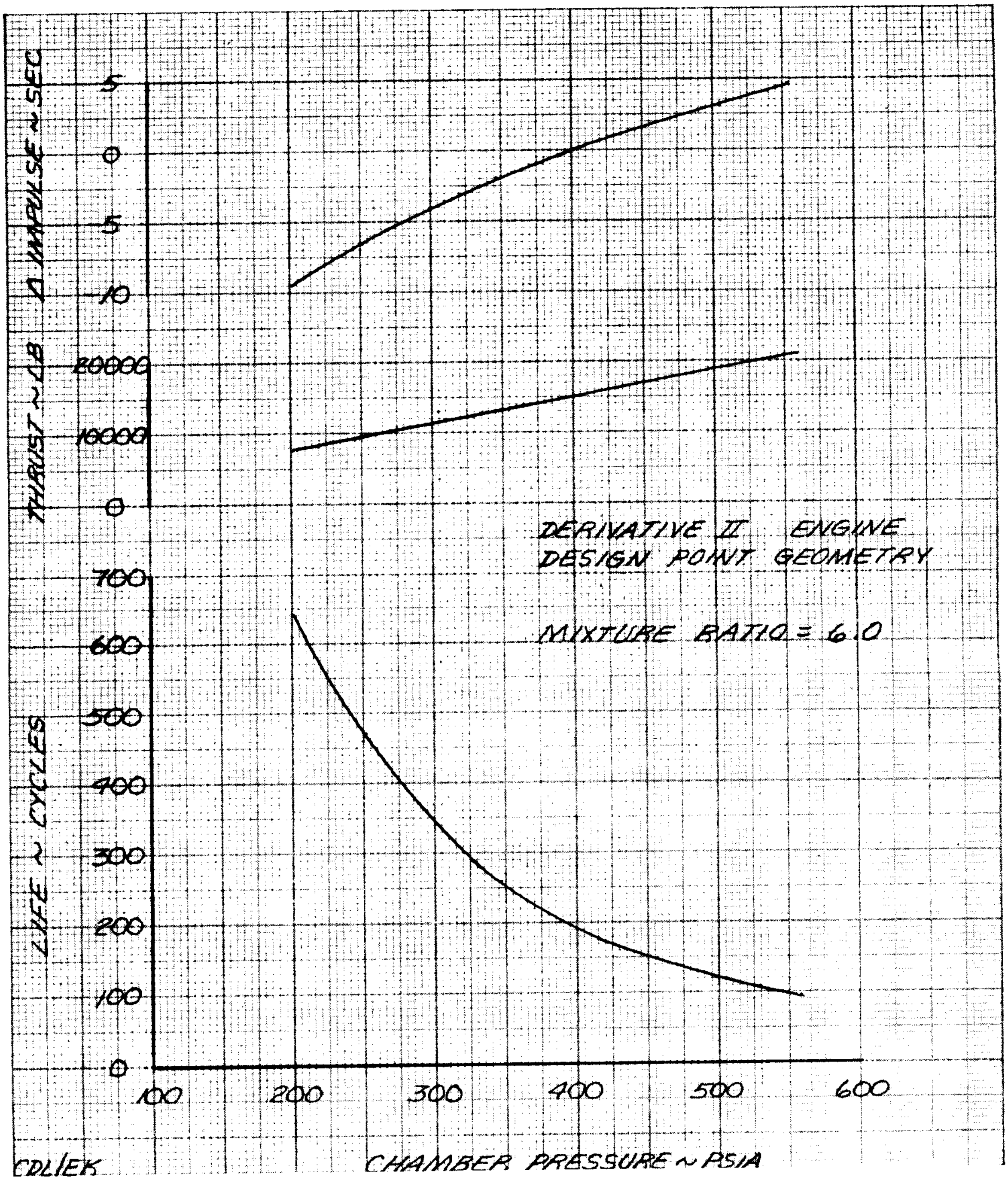


Figure D-5. Effect of Chamber Pressure on Cycle Life, Thrust and Specific Impulse Derivative II Engines

DF 97080

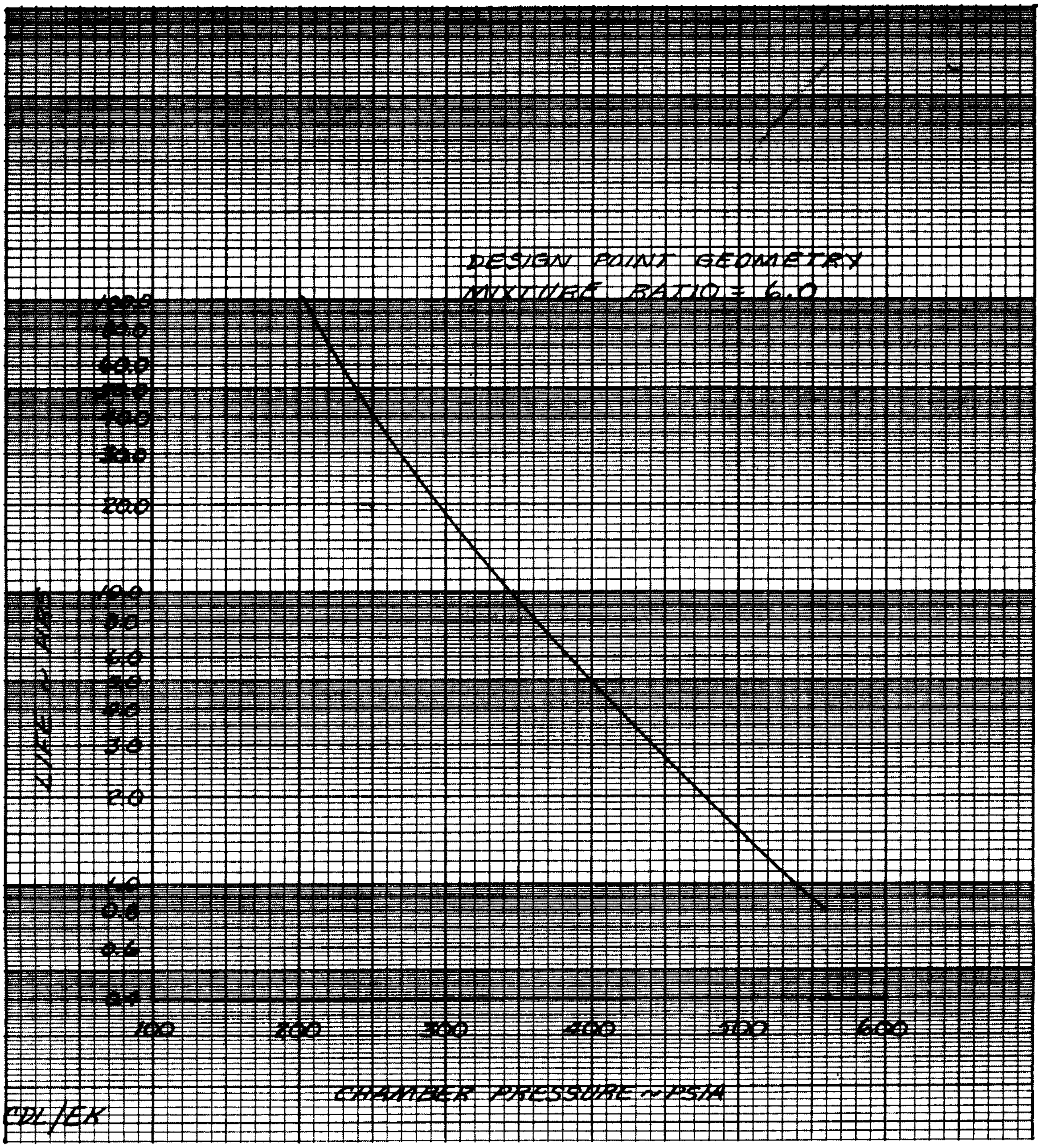


Figure D-6. Effect of Chamber Pressure On Derivative II Engine Wear Life

DF 96775

Regenerative nozzle parameters which affect engine power level (ΔP and ΔT) were estimates relative to the baseline design point and were used to determine the optimum chamber pressure and chamber length for each design evaluated. The optimization technique was the same as that used to optimize the Category IV baseline design as described in Section C3 of this report.

3.1 Engine Length Effects

3.1.1 Engines Interchangeable with RL10 Derivative Engines

The shortest permissible engine design had retracted length of 50.5 in. and 50.0 in. for respective thrust levels of 15,000 and 20,000 lb. These engines had chamber lengths of 12 in. Any engine design shorter than this would require either a chamber length shorter than 12 in. or a primary nozzle exit diameter less than 37 in., violating either ground rule "b" or "f". Figure D-7 presents design parametric data at a mixture ratio of 6.0 for retracted length variations between 50 and 70 in. for thrust levels of 15,000 and 20,000 lb.

3.1.2 Engines Not Interchangeable with RL10 Derivative Engines.

The shortest permissible engine design had a retracted engine length of 38.5 in. Any engine length shorter than these would require a chamber length shorter than 12 in. or a primary nozzle exit diameter less than 26 in. The longest permissible engine design had a retracted engine length of 57.5 in. and was limited by the 30 in. chamber length ground rule. Figure D-8 represents design parametric data at a mixture ratio of 6.0 for retracted lengths between 38.5 and 57.5 in.

3.2 Mixture Ratio Effects

Figure D-9 represents parametric performance data over an engine mixture range of 4.5 to 7.0 for a 15,000 lb thrust Category IV engine having a retracted length of 57 in.

3.3 Engine Life Effects

The life of the Category IV engine is set by the chamber LCF limit. Changes in the chamber passage geometry were evaluated to determine the effect on strain and thereby, the effect on LCF. Figure D-10 presents the results of this study and shows that while increased performance may be obtained, the reduction in LCF life is significant.

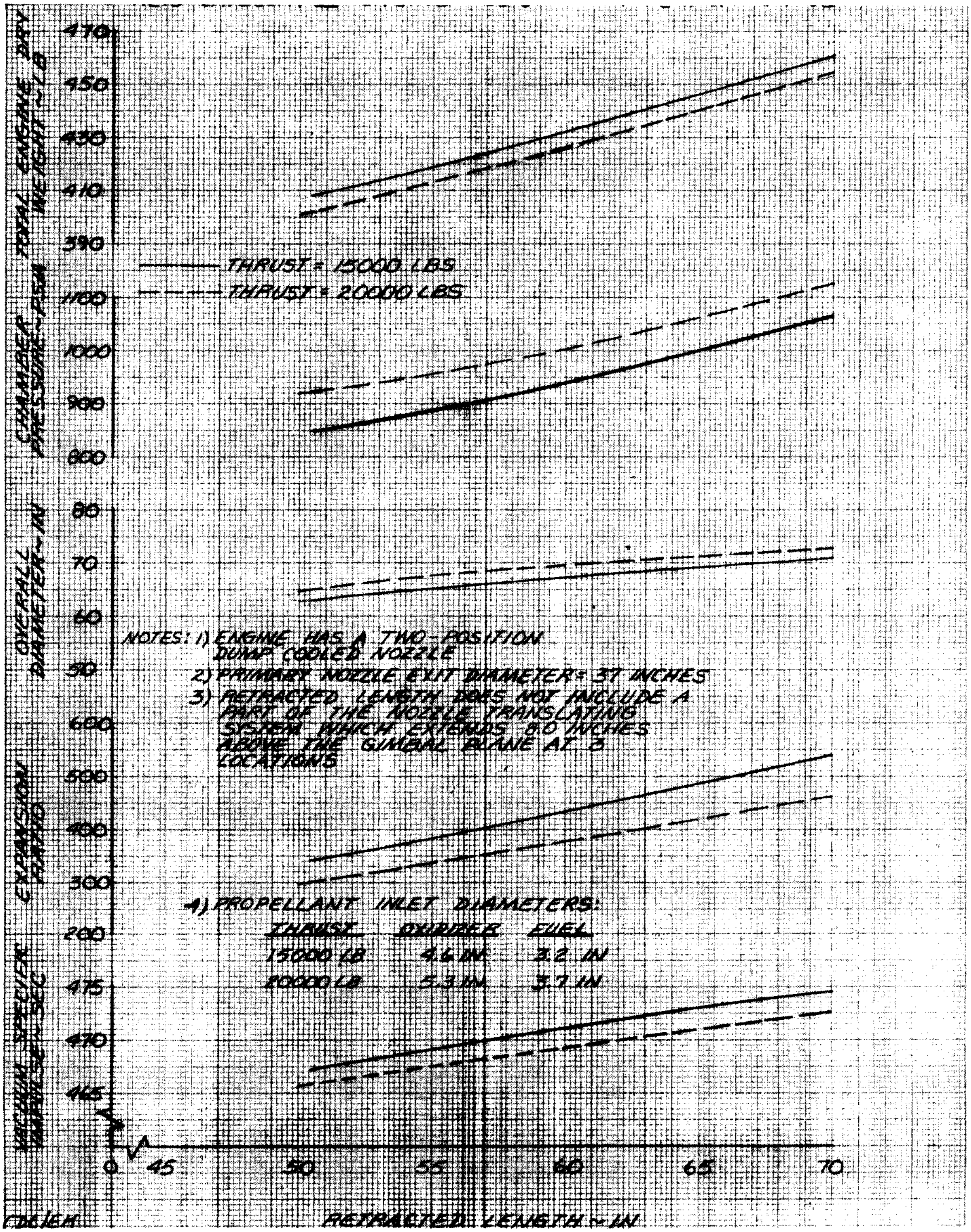


Figure D-7. Effect of Retracted Length on RL10 Category IV Engine Design Point Performance

DF 96022

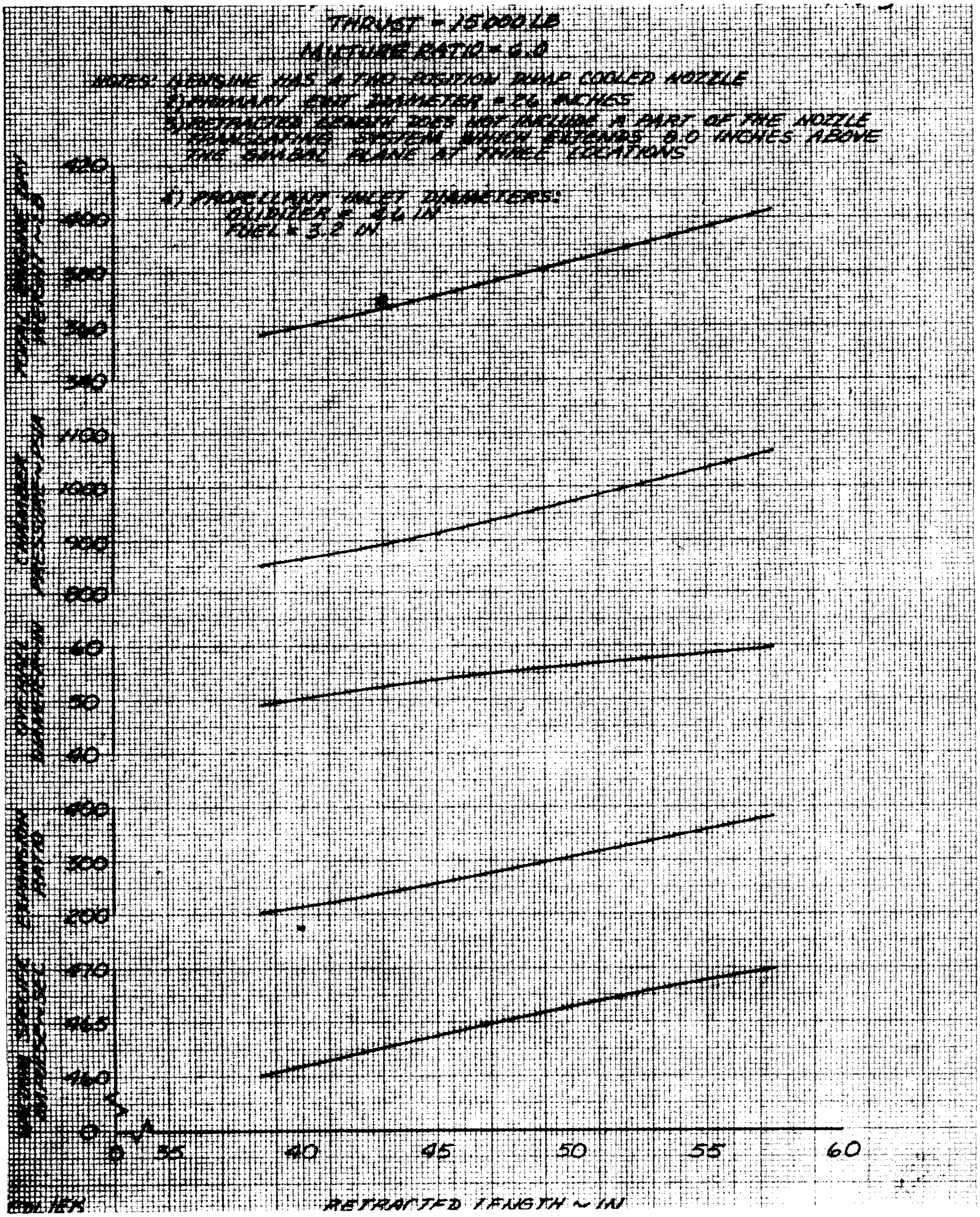
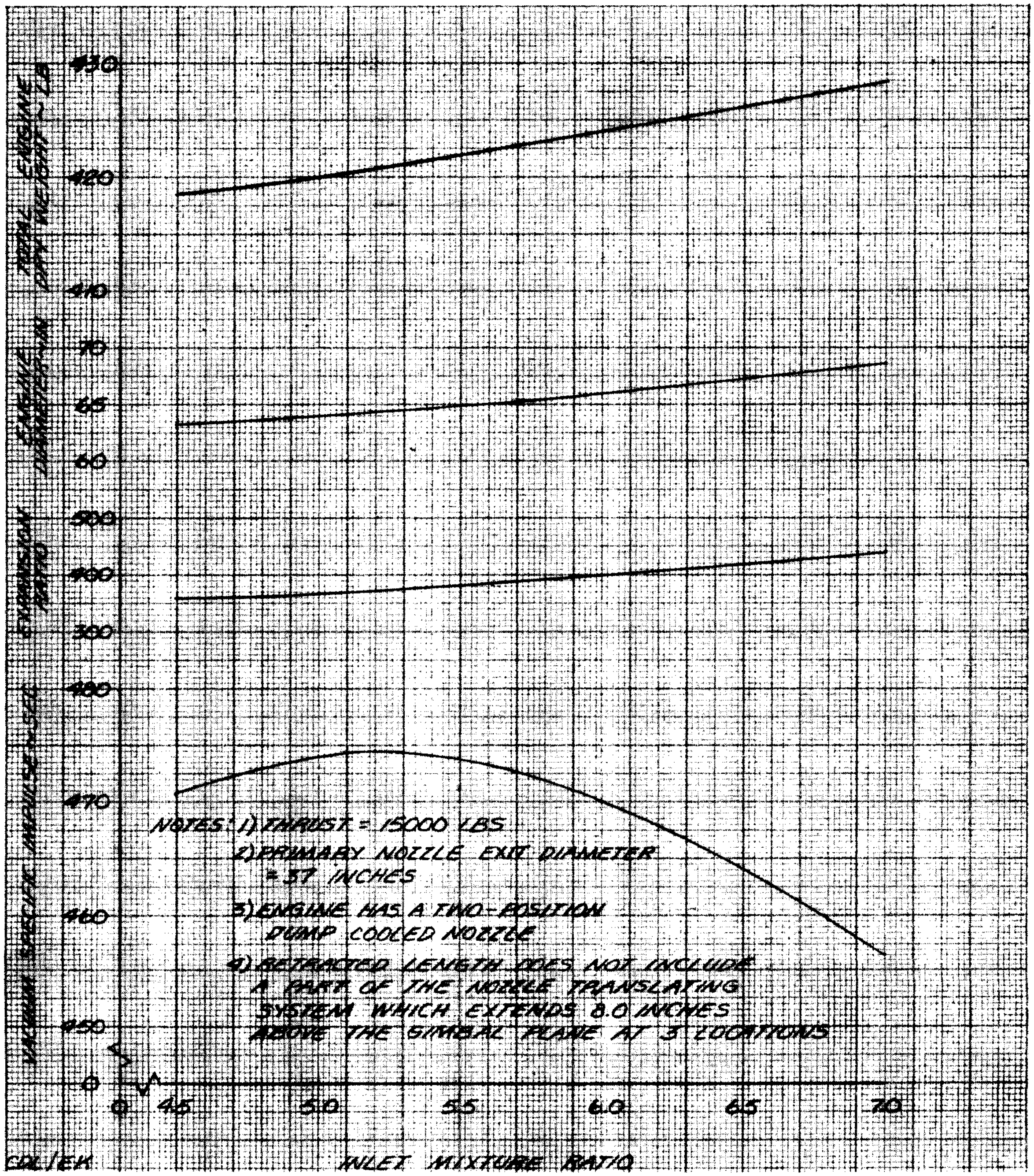


Figure D-8. Effect of Retracted Length on RL10 Category IV Engine Design Point Performance (Engine Not Interchangeable With Derivative Engines)

DF 96982



NOTES: 1) THRUST = 15000 LBS
2) PRIMARY NOZZLE EXIT DIAMETER = 57 INCHES
3) ENGINE HAS A TWO-POSITION DUMP COOLED NOZZLE
4) RETRACTED LENGTH DOES NOT INCLUDE A PART OF THE NOZZLE TRANSLATING SYSTEM WHICH EXTENDS 8.0 INCHES ABOVE THE GIMBAL PLANE AT 3 LOCATIONS

Figure D-9. Effect of Mixture Ratio on RL10 Category IV Engine Design Point Performance (Retracted Length = 57 in.)   DF 96023

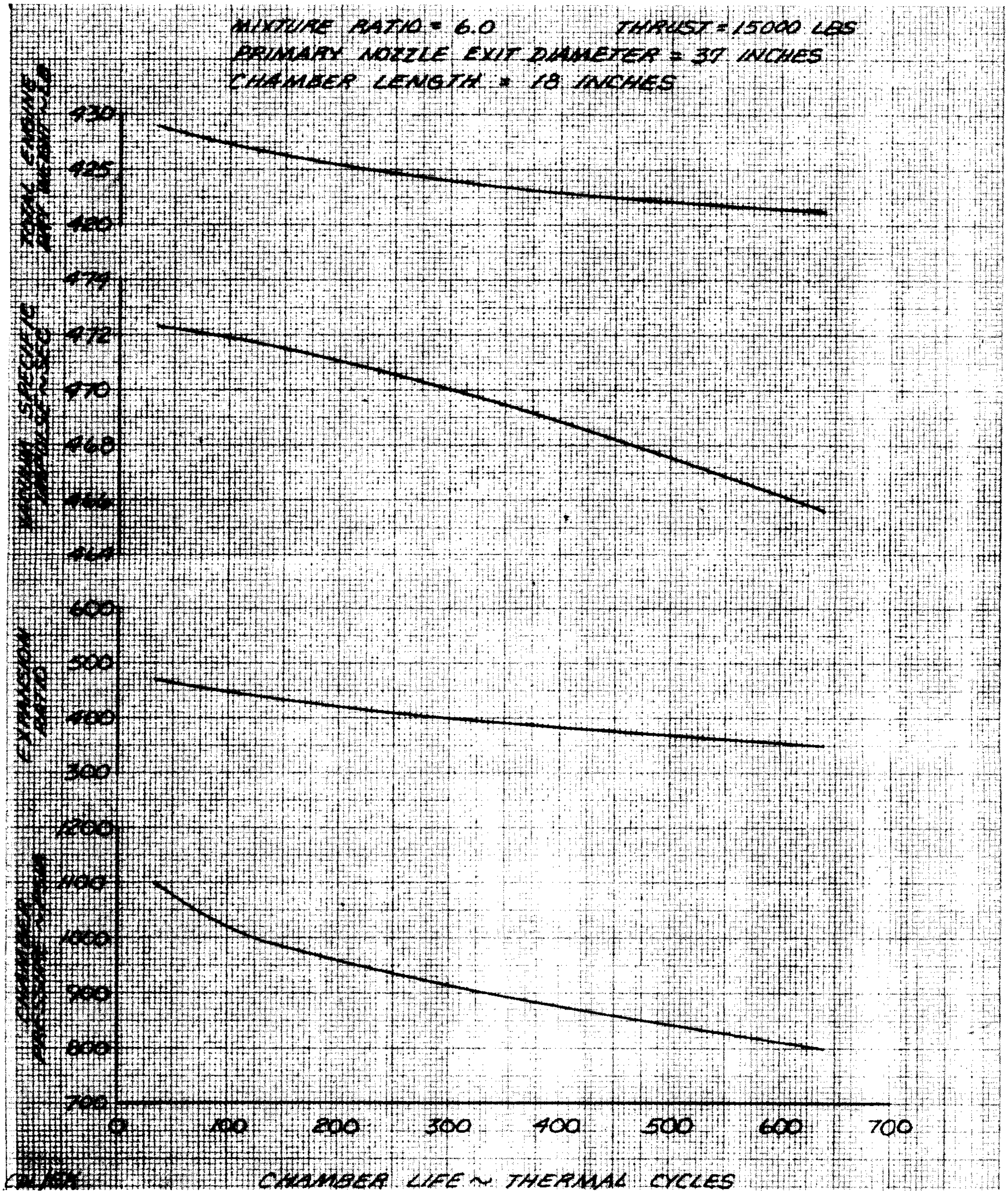


Figure D-10. Effect of Chamber Life on Category IV Engine Design Point Performance (Retracted Length = 57 in.)

DF 96986

SECTION E
SPECIAL STUDIES

1. IMPACT OF SPACE TUG ENVIRONMENT ON ENGINE DESIGN

The baseline engine designs were reviewed relative to the environment that will exist while on orbit, in the Orbiter payload bay and on the ground. The review showed that the most severe environmental conditions occur while the Space Tug is being carried to orbit or returned to earth in the Orbiter payload bay. These environmental conditions are covered in the "Space Shuttle System Payload Accommodations Document," JSC 07700, Volume XIV, dated April 1972. NASA TM X-64713 specified the on-orbit environmental conditions.

Since the Derivative II engines use many existing RL10 components, the Space Tug environmental factors were also compared with the current RL10-A-3-3 Model Specification requirements. While there could be a significant impact on the Derivative II engines if the existing components could not meet the Space Tug requirements a comparison showed only minor differences. It was assumed that the Category IV engine could be designed to meet these environmental and load requirements.

Table E-1 summarizes the comparison between the Space Tug environment and the RL10A-3-3 Specification requirements. In addition, the individual factors that were evaluated are briefly discussed below.

1.1 Dynamic Limit Load Acceleration

The basic RL10A-3-3 engine meets the acceleration load requirements of the Space Tug as shown by figure E-1. Of the new components designed for the Derivative II engine the two-position-nozzle was the only assembly significantly impacted by the dynamic load acceleration level. The structural design of this assembly was set by the 3g load encountered during the Orbiter reentry.

1.2 Pressure

The basic RL10A-3-3 engine was designed to operate at altitudes greater than 200,000 ft. which is similar to the Space Tug operating environment. In addition, the current engine is carried to its operational altitude in a pressure environment similar to that of the Orbiter. None of the new components were impacted by the Space Tug environmental criteria.

1.3 Sound Pressure

As shown in table E-1, the RL10A-3-3 meets the Space Tug sound pressure requirements for a single cycle. The structural limits of the RL10A-3-3 engine under repeated cycles are not known and testing would be required to establish the limits. The limits for the Derivative II engines would be expected to be the same as for the RL10A-3-3 with the exception of the extendible nozzle.

Table E-1. Space Tug Environment Evaluation

	Item	Tug Environment (1)	RL10A-3-3 Specification (2)
1.	Dynamic Limit Load Acceleration	(See figure E-1)	
2.	Pressure	Vacuum	Altitude greater than 200,000 ft
3.	Sound Pressure, db	145 (1.8 min)	147 (2 min)
4.	Contamination	TBD	
5.	Vibration	TBD	
6.	Shock	TBD	
7.	Temperature, °R	360 to 660	250 to 620
8.	Humidity	KSC Ground environment	Not covered in specification. Engine is presently desiccated or purged with helium

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Notes:

(1)Reference: "Space Shuttle System Payload Accommodations Document," JSC 0770, Volume XIV, 13 April 1973.

(2)Reference: RL10A-3-3 Model Specification No. 2265A, Revision No. 2, 15 March, 1967.

1.4 Temperature

The maximum allowable temperature for the RL10A-3-3 engine is 40 deg lower than the indicated Space Tug requirement of 660°R. However, the RL10A-3-3 (or the Derivative II engine) is not expected to be adversely effected by the higher temperature level. The RL10A-3-3 will operate successfully at temperatures 110 deg lower than the minimum requirements indicated for the Space Tug. To ensure reliable engine solenoid operation and repeatable engine transducer measurement data, these components will be thermally conditioned by the engine.

1.5 Humidity

While the Space Tug vehicle is to be surrounded by a dry N_2 atmosphere in the payload bay prior to propellant loading and a dry He atmosphere until launch, the engine will be subjected to a high humidity when the Orbiter reenters the atmosphere. To protect against water contamination a gaseous helium purge is required prior to reentry. The requirements for this purge are discussed in the ICD (Volume III). No purges are required during launch operations due to the dry environment of the payload bay.

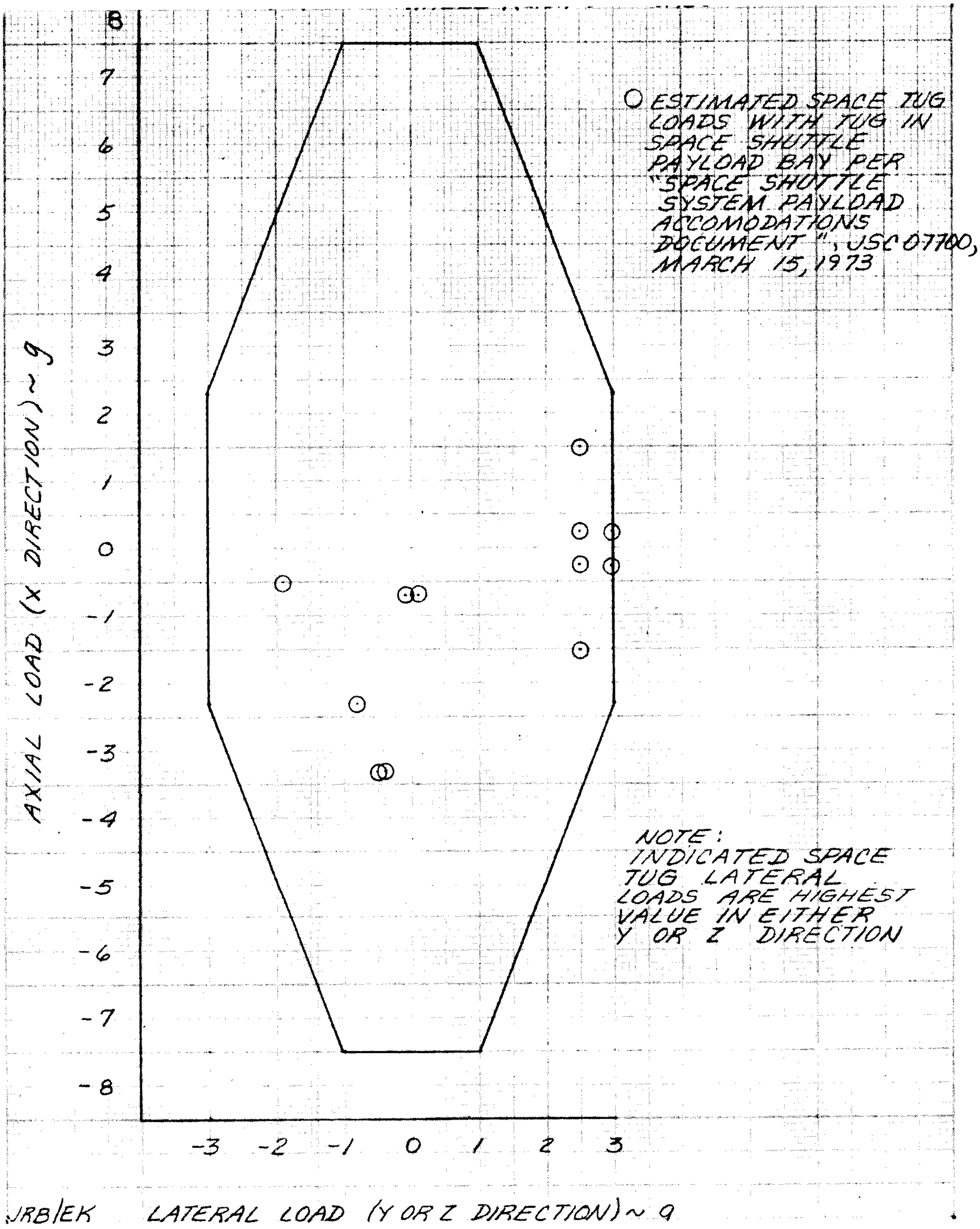


Figure E-1. Comparison of Space Tug Acceleration Loads With Allowable RL10A-3-3 Loads DF 97090

1.6 Contamination, Vibration and Shock

Space Tug contamination, vibration and shock levels have not been defined at this time. Therefore, no determination could be made of their effect on the baseline engine designs.

2. IMPACT OF SPACE TUG SYSTEM SAFETY CRITERIA ON ENGINE DESIGN

The safety requirements specified in the Space Tug System Safety Criteria were reviewed to ensure compliance of the baseline engine designs. This review showed that the safety criteria could be met without any significant impact on the engine designs.

The Space Tug System Safety Criteria specifies the following requirements related to the propulsion system.

- No single failure shall result in unprogrammed motion of the Tug during on-orbit deployment or while on orbit and within TBD distance of the Orbiter
- The propulsion system shall be designed to be fail safe.

The RL10 derivative engines can easily meet the first requirement. The inlet valves cannot be opened until both electrical power and helium pressure are supplied to the engine. As long as these are withheld from the engine by the vehicle until the Space Tug is a safe distance from the Orbiter, the engine inlet valves cannot open and no engine operation of any kind can occur. Present plans are for the vehicle to withhold both of these.

A failure analysis was conducted for all of the valves and solenoids on the Derivative IIB engine to determine the engine failsafe capabilities. The results of this analysis are presented in table E-2. It showed that none of the assumed failure modes will result in unsafe operation of the engine. Since all of the engines use the same type of valves and controls, the results are applicable to all of the RL10 derivative engines.

Propellant leakage through the engine inlet valves while the tug is in the Orbiter could present a potential hazard. To guard against it the vehicle manufacturers are considering the use of prevalves in series with the engine inlet valves. In addition double seal engine inlet valves like the valve shown in figure E-2 were designed to eliminate this leakage. They were baselined for the Derivative IIA and Category IV engines since those engines required new inlet valves and could be added to Derivative IIB engines. They should provide added insurance against leaking prevalves or could possible eliminate the need for vehicle prevalves.

3. IMPACT OF INTERCHANGEABILITY ON ENGINE DESIGNS

To facilitate the replacement of engines in a phased vehicle development program the RL10 Derivative engines were designed for maximum interchangeability. To accomplish this, mechanical vehicle/engine interface connections such as the gimbal actuator attachment points, the electrical and helium pressure supply connections, instrumentation signal connections, and the centerline of the propellant supply ducts, were placed at the same physical locations relative to the engine centerline and gimbal attachment plane. Special studies described below were conducted during the Baseline Engine Design effort to determine if vehicle/engine performance was compromised by requiring the engines to be interchangeable.

3.1 Category IV Engine Inlet Conditions

The Category IV baseline engine is designed to be interchangeable with the Derivative IIA engine; that is, it was designed to operate with the same two-phase inlet propellant conditions at full thrust. This required the use of large diameter fuel and oxidizer low speed inducers. The impact of designing the Category IV engine to replace a Category I or Derivative IIB engine in a phased development program was examined. Two cases were studied. In the first case it was assumed that the early vehicle propellant supply configuration would not change and propellants would be supplied at the NPSP levels previously required for Category I and Derivative IIB engine operation. With these NPSP levels the Category IV low speed inducer designs could be changed to reduce inducer diameter and weight. In the second case, it was assumed that the early vehicle provided inlet NPSP through the use of vehicle boost pumps that could be modified to provide the NPSP required of the Category IV main pumps. The low speed inducers were removed entirely from the Category IV engine, and the main pumps were redesigned to operate at the lowest practical NPSP. This further reduced engine weight and inlet diameters. The results of these studies are summarized in table E-3. While both cases showed significant engine weight savings, the true potential gain for these phased development programs will probably come from reduced vehicle development costs.

3.2 Engine Installed Length

The Category IV baseline engine was designed with installed length of 57 in. to be completely interchangeable with the minimum length Derivative II engine. At this length the Category IV baseline engine can replace any Derivative II engine without impacting the Space Tug installation length.

The Derivative II baseline engine was designed with an installed length of 70 in. to be completely interchangeable with the Category I engine. If the engine was designed for minimum installed length of 57 in. area ratio would be reduced to 210 and specific impulse would be reduced 3 sec.

Chamber pressure for the Category IV engine is higher and as a result chamber diameter is reduced relative to the other RL10 derivative engines permitting the powerhead diameter to be significantly smaller. The Category IV engine was designed, however, to place the centerline of the propellant inlet ducts at the same locations as for the other RL10 derived engines. With the turbopumps so located, the minimum powerhead diameter is 37 in. If the centerline locations of the inlet ducts were placed at optimum locations for that engine, the powerhead diameter could be reduced to 26 in. and the minimum engine length could be reduced from 51 in. to 26 in. Section D parametrically shows the effect on engine installed length and performance of being able to reduce the powerhead diameter of the Category IV engine to 26 in.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
Start Solenoid Valve (Normally Closed)	Controls the start cycle by porting helium to other solenoids, inlet valves, and fuel vent valve.	1. Fails to open a. Burned out solenoid coil b. Shaft binding on guides	1. Start	1. Inlet valves will not open - engine will not operate.
		2. Fails to remain open due to burned out solenoid coil	2. THI, PI, or rated thrust	2. Engine will shut down since inlet valves will close.
		3. Fails to close due to shaft binding on guides	3. Shut down	3. Inlet valves will remain open - engine will not shut down completely. Oxidizer will be lost overboard - fuel will be lost overboard through turbine bypass valve (in closed position) - high O/F.
			4. Internal leakage	4. Helium will be lost overboard.
Fuel Shutoff Valve Solenoid (Normally Closed)	Ports helium to open turbine discharge. Shut off valve for rotating idle and rated thrust operation.	1. Fails to open a. Burned out solenoid coil b. Shaft binding on guides	1. Start of pumped idle	1. Turbine discharge valve will not open. Allows no flow through the turbine. Bleed flow through bypass valve is only fuel flow reaching chamber. High O/F.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
Fuel Shutoff Valve Solenoid (Normally Closed) (Continued)		2. Fails to remain open due to burned out solenoid coil	2. a. Pumped idle	2. a. Engine will decelerate - to same condition as (1) above. High O/F.
			b. Rated thrust	b. Engine will decelerate - to same condition as (1) above. Fuel pump housing and inlet line will experience a high pressure surge.
		3. Fails to close due to shaft binding on guides	3. Shut down	3. Extended shutdown. Fuel side propellants will bleed down through turbine.
Turbine Bypass Valve Solenoid No. 1 (Normally Closed)	Ports helium to turbine bypass valve for THI operation.	1. Fails to open a. Burned out solenoid coil b. Shaft binding on guides	1. Start of THI	1. Turbine bypass valve will not open to THI position. Insufficient fuel flow will reach the chamber. The engine will not operate at THI.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
Turbine Bypass Valve Solenoid No. 1 (Normally Closed) (Continued)		2. Fails to remain open due to burned out solenoid coil	2. THI	2. Same as (1) above.
		3. Fails to close due to shaft binding on guides	3. Start of pumped idle	3. Turbine bypass valve will not close - allows engine operation in THI only.
Turbine Bypass Valve Solenoid No. 2 (Normally Closed)	Ports hydrogen from turbine inlet to turbine bypass valve for pumped idle operation.	1. Fails to open a. Burned out solenoid coil b. Shaft binding on guides	1. Pumped idle start	1. Turbine bypass valve remains closed. Engine will try to accelerate to rated thrust but pumps will cavitate, because tanks have not been pressurized. No operation in pumped idle.
		2. Fails to remain open due to burned out solenoid coil	2. Pumped idle	2. Turbine bypass valve closes - engine will try to accelerate to rated thrust.
		3. Fails to close due to shaft binding on guides	3. End of pumped idle	3. Turbine bypass valve remains open. Engine will not accelerate to rated thrust. No operation at rated thrust.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
Oxidizer Inlet Valve (Normally Closed)	Seals off oxidizer supply from engine.	1. Fails to open a. Jammed gear-train bearing or piston b. Ruptured bellows	1. THI start	1. Engine will not operate due to lack of oxidizer. Fuel will be lost overboard.
		2. Fails to remain open due to ruptured bellows	2. THI, PI, and rated thrust	2. Engine will shut down due to loss of oxidizer supply. Fuel will be lost overboard.
		3. Fails to close a. Jammed gear-train bearing or piston b. Inability to remove helium signal	3. Shut down	3. Engine will shut down because of loss of fuel supply. Oxidizer will be lost overboard.
Main Fuel (Normally Closed)	Seals off fuel supply from engine.	1. Fails to open a. Jammed gear-train bearing or piston b. Ruptured bellows	1. THI start	1. Engine will not operated due to lack of fuel. Oxidizer will be lost overboard.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Conditions at Time of Failure	Failure Effect on Engine Performance
Main Fuel (Normally Closed) (Continued)		2. Fails to remain open due to ruptured bellows	2. THI, PI, and rated thrust	2. Engine will shut down due to loss of fuel supply. Oxidizer will be lost overboard.
		3. Fails to close a. Jammed geartrain bearing or piston b. Inability to remove helium signal	3. Shut down	3. Engine will shut down normally. Fuel will be lost overboard through the fuel vent valve.
Fuel Vent Valve (Normally Open)	Vents trapped fuel overboard at shutdown to prevent high pressures in fuel side of engine.	1. Fails to close due to jammed piston	1. THI start	1. Possible ignition problem due to lack of fuel. If ignition occurs engine will operate in THI at very high mixture ratio. Fuel will be lost overboard.
		2. Fails to remain closed.	2. THI, PI, and rated thrust	2. Engine will lose thrust. Fuel will be lost overboard. Rated thrust and pumped idle operation will be at reduced thrust levels with excessive amounts of fuel being dumped overboard.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
Fuel Vent Valve (Normally Open) (Continued)		3. Fails to open due to jammed piston	3. Shut down	3. Fuel pump housing and fuel inlet line will experience a high pressure surge.
Nozzle Coolant Control Valve (Normally Closed With Continuous Bleed Open)	Prevents excessive fuel flow through dump cooled nozzle at tank head idle and regulates flow during pumped idle and full thrust operation.	1. Fails to open due to jammed piston	1. Pumped idle and rated thrust	1. Dump cooled nozzle will not receive proper cooling.
		2. Fails to close due to jammed piston	2. Deceleration from pumped idle	2. Fuel leakage would be higher at tank head idle resulting in lower impulse.
Turbine Bypass Valve (Normally Closed Continuous With Bleed Open)	3-position valve - helium actuated to tank head idle position - hydrogen actuated to pumped idle position - closed with 1% bleed at rated thrust.	1. Fails to open due to jammed piston	1. a. THI start	1. a. Insufficient fuel flow will reach the chamber. Ignition will not occur. Engine cannot operate. Oxidizer will be lost overboard.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effects on Engine Performance
Turbine Bypass Valve (Normally Closed Continuous With Bleed Open) (Continued)			b. Pumped idle start	b. Engine will try to accelerate to rated thrust but pump will cavitate because tanks have not been pressurized. No operation in pumped idle.
		2. Fails to close due to jammed piston	2. a. Pumped idle start	2. a. Engine will only operate at tank head idle.
			b. Rated thrust start	b. Engine will only operate at pumped idle.
Thrust Control Valve (Normally Closed)	Controls turbine bypass flow by changing the position of the bypass valve in response to combustion chamber pressure at rated thrust, limits overshoot during acceleration to rated thrust.	1. Fails closed a. Broken pneumatic reset capillary tubes b. Jammed vent valve lever c. Ruptured reference bellows d. Ruptured motor bellows e. Motor bellows binding in housing or binding reference system bearing.	1. Acceleration to rated thrust	1. Engine will accelerate to higher than rated thrust with high thrust overshoot.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
Thrust Control Valve (Normally Closed) (Continued)		f. Bypass piston or piston rod jammed g. Excessive leakage past piston		
		2. Fails open a. Plugged upstream venturi pressure orifice b. Jammed vent valve lever c. Binding motor bellows d. Bypass piston or piston rod jammed e. Clogged pneumatic reset capillary tube f. Plugged servo supply orifice	2. Acceleration to rated thrust	2. Engine will not accelerate to rated thrust level
Main Fuel Shutoff Valve (Normally Closed)	Prevents flow through the turbine during THI and provides a rapid shutdown by cutting off most of the flow to the chamber.	1. Fails to open a. Binding piston or actuating rod b. Ruptured bellows	1. Pumped idle start	1. Allows no flow through the turbine. Bleed flow through bypass valve is only fuel flow reaching chamber. High O/F.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
Main Fuel Shutoff Valve (Normally Closed) (Continued)		2. Fails to remain open	2.	2.
		a. Ruptured bellows	a. Pumped idle	a. Engine will decelerate to same condition as (1) above. High O/F.
		b. Excessive helium leakage	b. Rated thrust	b. Engine will decelerate to same condition as (1) above. Fuel pump housing and inlet line will experience a high pressure surge.
		3. Fails to close a. Jammed piston or actuating rod	3. Shut down	3. Extended shutdown. Fuel side propellants will bleed down through turbine.
Fuel Tank Pressurization Valve (Normally Closed)	Routes high pressure fuel to the fuel tank for pressurization.	1. Fails to open	1. Pumped idle	1. No tank pressurization. Engine cannot accelerate to rated thrust.
		2. Fails to close	2. THI	2. May allow fuel leakage from tank to fuel injector. May operate at lower O/F than normal.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
Oxidizer Control Valve (Normally Closed)	3-position valve - closed for THI - intermediate position for pumped idle - full open for rated thrust operation - oxidizer pump discharge pressure actuated.	1. Fails to open due to binding of valve	1. a. Acceleration to pumped idle	1. a. Engine will not accelerate to the pumped idle thrust level because of insufficient Lox flow. Low O/F.
			b. Acceleration to rated thrust	b. Engine will not accelerate to rated thrust because of insufficent Lox flow. Low O/F.
		2. Fails to close due to binding of valve	2. a. Deceleration to pumped idle	2. a. Engine would not decelerate to pumped idle thrust level because of excessive Lox flow. Very high O/F. No effect on shutdown. THI operation would be (a) above.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
Oxidizer Control Valve (Normally Closed) (Continued)			b. Shut down	b. No effect on shutdown, but tank head idle operation during next burn would be at very high O/F. Possibility of chugging instability.
Mixture Ratio Control Valve (Open) (Part of Oxidizer Control Valve)	Provides propellant utilization capability at rated thrust.	1. Sticks in nominal position	1. Rated thrust	1. No propellant utilization capability.
		2. Sticks in high or low O/F position	2. Rated thrust	2. No propellant utilization capability.
GO_2 Control Valve (Normally Open)	Controls oxidizer flow in THI mode.	1. Fails to close due to binding of valve	1. Pumped idle acceleration	1. Engine would not accelerate to pumped idle thrust level because of reduced oxidizer flow through injector. Low O/F. Same results at rated thrust.

Table E-2. Failure Mode and Effects Analysis for Derivative IIB Valves and Solenoids (Continued)

Component	Function	Failure Type	Engine Condition at Time of Failure	Failure Effect on Engine Performance
GOX Control Valve (Normally Open) (Continued)		2. Fails to open due to binding of valve	2. Shut down	2. Engine would shut down. No oxidizer flow to chamber during next THI start and engine will not operate in THI mode. Fuel flow dumped overboard.
Oxidizer Tank Pressurization Valve (Normally Closed)	Provides high pressure oxidizer flow for oxidizer tank pressurization.	1. Fails to open due to binding of valve	1. Pumped idle	1. Oxidizer tank cannot be pressurized. Engine cannot accelerate to rated thrust, because oxidizer pump will cavitate.
		2. Fails to close due to binding of valve	2. THI	2. May allow oxidizer leakage from tank to oxidizer injector. May operate at higher O/F than normal.

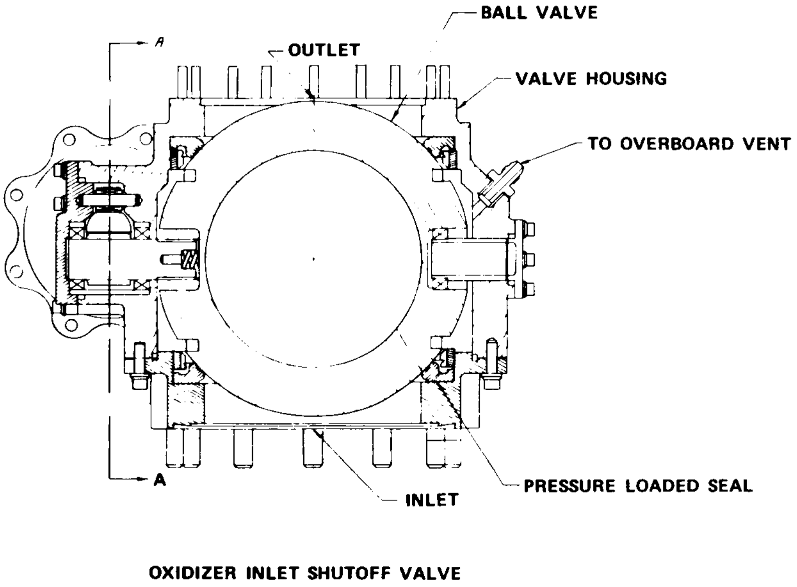

BALL VALVE
A
OUTLET
VALVE HOUSING
TO OVERBOARD VENT
A
INLET
PRESSURE LOADED SEAL
OXIDIZER INLET SHUTOFF VALVE

Figure E-2. Oxidizer Inlet Shutoff Valve FD 76376

Table E-3. Category IV Engine as a Replacement for Category I or Derivative IIB Engines

	Current Baseline Cat IV	Cat IV With Low Speed Inducers(2)	Cat IV Without Low Speed Inducers(3)
Engine NPSP Required, psi			
Oxidizer	0 (2 phase)	3.7	23.6
Fuel	0 (2 phase)	0.4	4.8
Engine Inlet Diameter, in.			
Oxidizer	4.6	1.9	1.9
Fuel	3.2	2.2	2.1
Engine Weight Change (Relative to Baseline), lb_m		-25	-42
Estimated Horsepower Required If Boost Pumps are Used(1)			
Oxidizer	None	0.6	3.7
Fuel	None	0.2	1.8

Notes:

(1) To raise saturated propellants to required NPSP levels.

(2) Assumes vehicle boost pumps or tank pressurization system unchanged from Category I configuration.

(3) Assumes vehicle boost pumps are used to supply main pump NPSP.

4. IMPACT OF MAINTAINABILITY ON ENGINE DESIGN

In order to ensure that the basic maintainability concepts were considered early in the design phase the designers were provided with a maintainability checklist. The checklist provides the designers with specific "Do's and Don'ts" to be incorporated into the engine design. Each engineering design layout was reviewed during the preliminary design phase to ensure that qualitative and quantitative maintainability requirements are part of the engine design. Comments relative to the extent of incorporation of maintainability into the design were documented on the Maintainability Engineering Layout Review (MELR) form. Other disciplines such as weight, cost, and performance are considered when maintainability changes to a design are recommended.

Comments provided on the MELR consist of:

a. Maintainability problem areas

b. Assessment relative to the design maintainability checklist

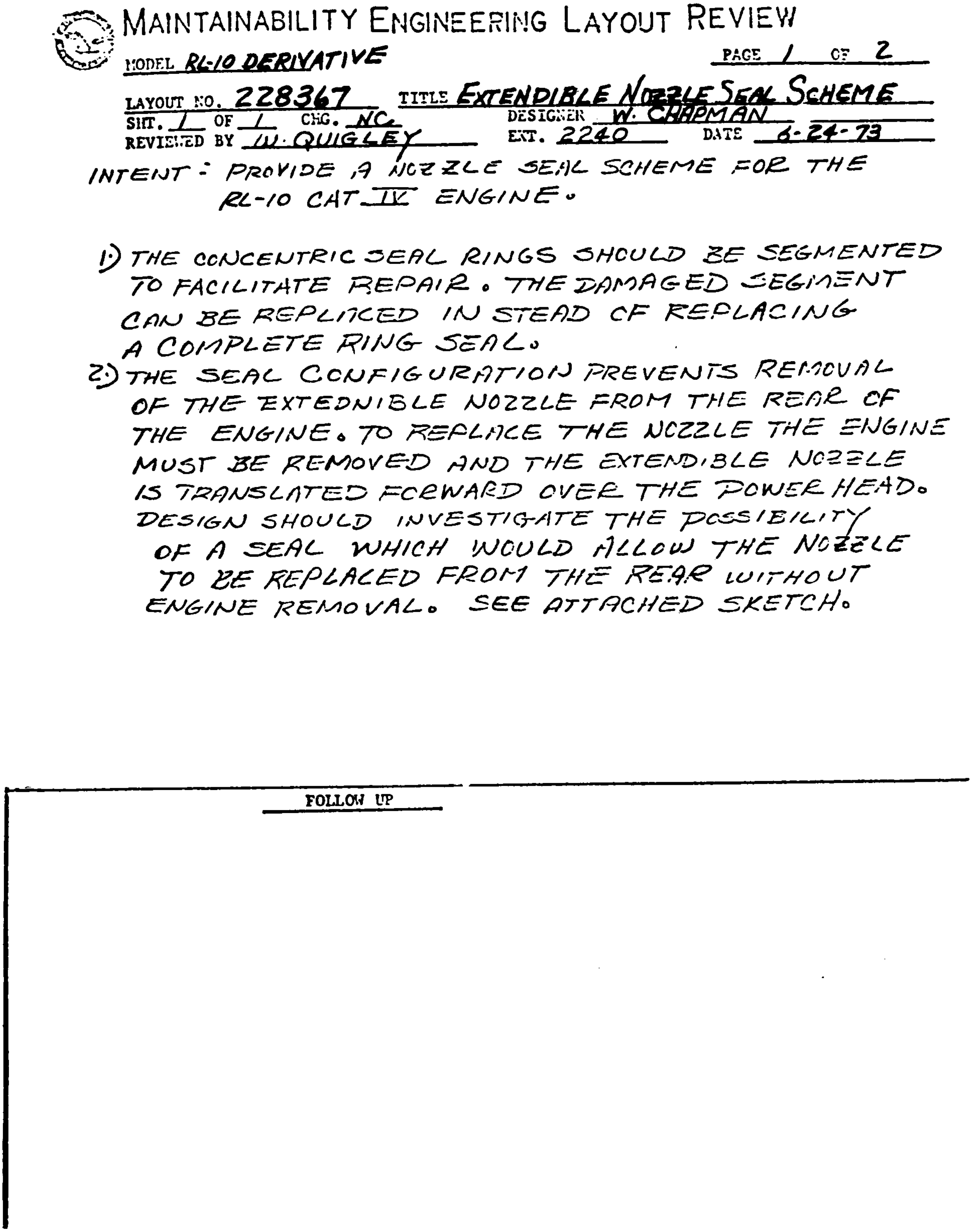
c. Extent of incorporation of maintainability recommendations

d. Comments and statements of good maintainability features incorporated.

Copies of the MELR were sent to the Design Engineer, Design Project Engineer and the cognizant Project Engineers. The original is retained in the Design Maintainability Group file for followup. Copies of the followup MELR's are sent to the same distribution as the initial copies.

During the Critical Elements Analysis and Baseline Engine Design phases of the study 20 MELR's and 5 supplementary MELR's were issued by the Design Maintainability group. Of the original 20 issued, 17 MELR's apply to the three engine configurations selected for baseline design. The MELR suggestions will be incorporated at the time of any future RL10 derivative engine design. An example of a typical MELR is shown in figure E-3 on which it is recommended that a part be changed to facilitate fabrication and part repair. It also identifies a potential assembly problem and a possible solution to the problem.

Copies of all MELR's are contained in Appendix V; the appendix is in two sections. The first section contains copies of MELR's for the Derivative IIA, IIB and Category IV engine configurations, and the second section contains copies of the MELR's for rejected or preliminary design concepts.



MAINTAINABILITY ENGINEERING LAYOUT REVIEW

MODEL RL-10 DERIVATIVE
PAGE 1 OF 2
LAYOUT NO. 228367
TITLE EXTENDIBLE NOZZLE SEAL SCHEME
SHT. 1 OF 1
CHG. NC
DESIGNER W. CHAPMAN
REVIEWED BY W. QUIGLEY
EXT. 2240
DATE 6-24-73

INTENT: PROVIDE A NOZZLE SEAL SCHEME FOR THE RL-10 CAT IV ENGINE.

1) THE CONCENTRIC SEAL RINGS SHOULD BE SEGMENTED TO FACILITATE REPAIR. THE DAMAGED SEGMENT CAN BE REPLACED IN STEAD OF REPLACING A COMPLETE RING SEAL.

2) THE SEAL CONFIGURATION PREVENTS REMOVAL OF THE EXTEDNIBLE NOZZLE FROM THE REAR OF THE ENGINE. TO REPLACE THE NOZZLE THE ENGINE MUST BE REMOVED AND THE EXTENDIBLE NOZZLE IS TRANSLATED FORWARD OVER THE POWER HEAD. DESIGN SHOULD INVESTIGATE THE POSSIBILITY OF A SEAL WHICH WOULD ALLOW THE NOZZLE TO BE REPLACED FROM THE REAR WITHOUT ENGINE REMOVAL. SEE ATTACHED SKETCH.

FOLLOW UP

Figure E-3(1). MELR Example

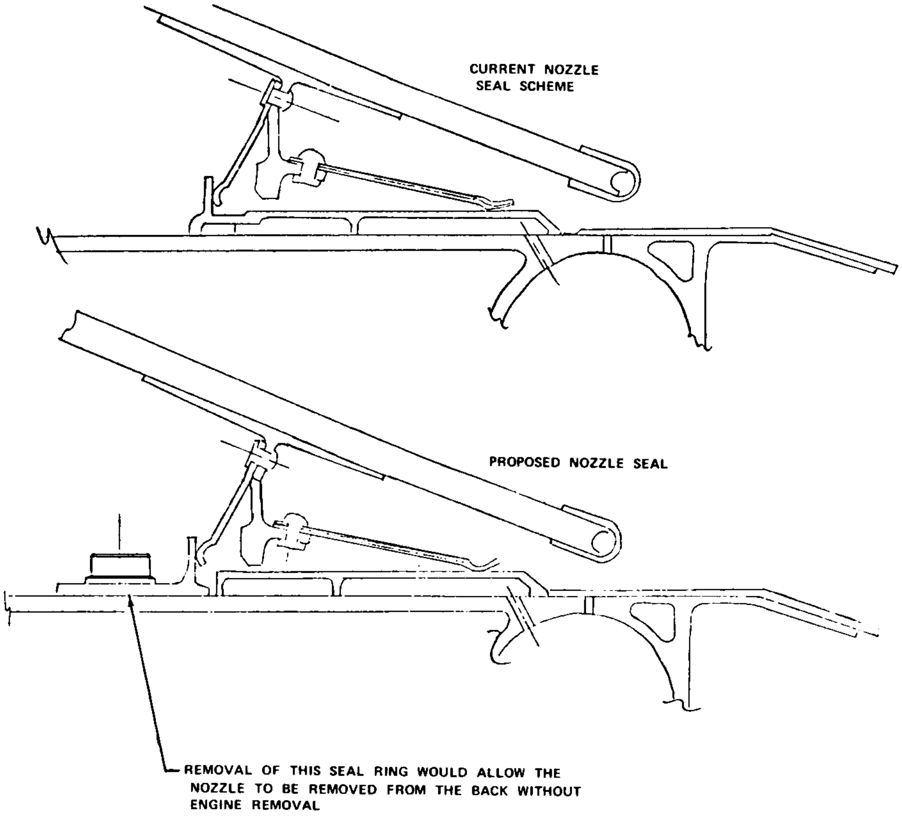

CURRENT NOZZLE SEAL SCHEME
PROPOSED NOZZLE SEAL
REMOVAL OF THIS SEAL RING WOULD ALLOW THE NOZZLE TO BE REMOVED FROM THE BACK WITHOUT ENGINE REMOVAL

Figure E-3(2). Example of MELR Second Sheet Usage

5. SELECTION OF ALLOWABLE RANGE OF INLET CONDITIONS

Space Tug vehicle studies have shown a large potential gain in delivered payload through the use of nonpressurized propellant tank systems. These systems contain either boost pumps capable of pumping propellants at saturated conditions to supply the main engines or main engines capable of pumping two phase propellants. In either case propellant tank pressure is allowed to drop during the engine firing as the liquid is removed from the tank and the ullage gases expand to fill the increased volume. Most Space Tug missions schedule a long hold time between firings. During this coast period heat leaks into the tank and increases ullage pressure thereby compensating for the pressure reduction which occurred during the previous mainstage firing. There are, however, a limited number of Space Tug missions which require both long firing durations and short hold times. These missions, if conducted without special procedures, could require engine starts and operation at engine inlet pressures as low as 10 psia. Studies were conducted to determine the allowable ranges of inlet conditions for the baseline engines. Because of its importance particular emphasis was placed on defining the minimum pressure levels allowable.

Figures E-4 through E-8 show the allowable ranges of inlet conditions for operation of the baseline engines at tank head idle, maneuvering thrust (pumped idle) and full thrust. The criteria and rationale used to establish the limits on these curves are discussed below.

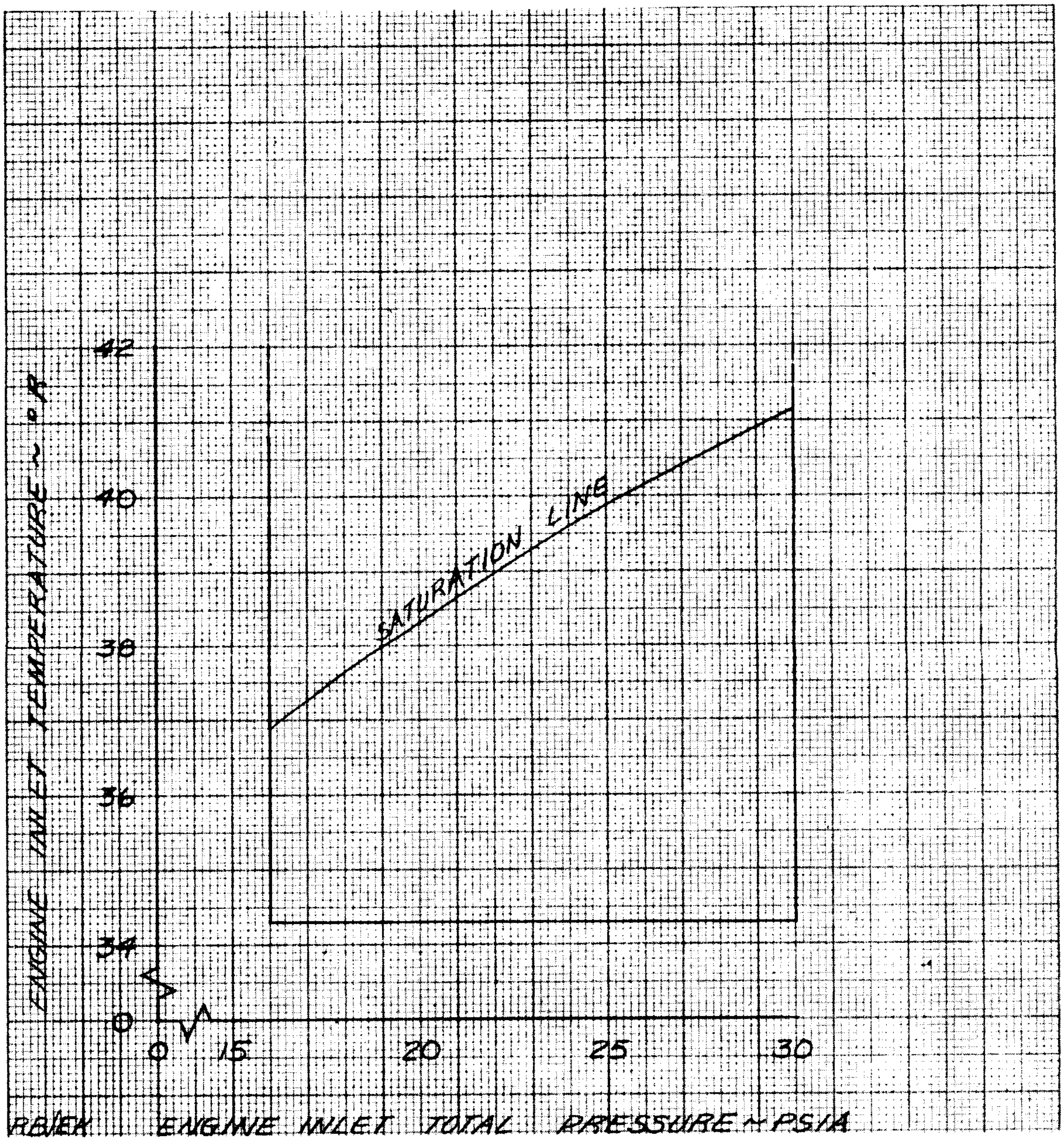


Figure E-4. Allowable Fuel Conditions for Tank Head Idle Start Derivative IIA, IIB and Category IV Engines DF 96914

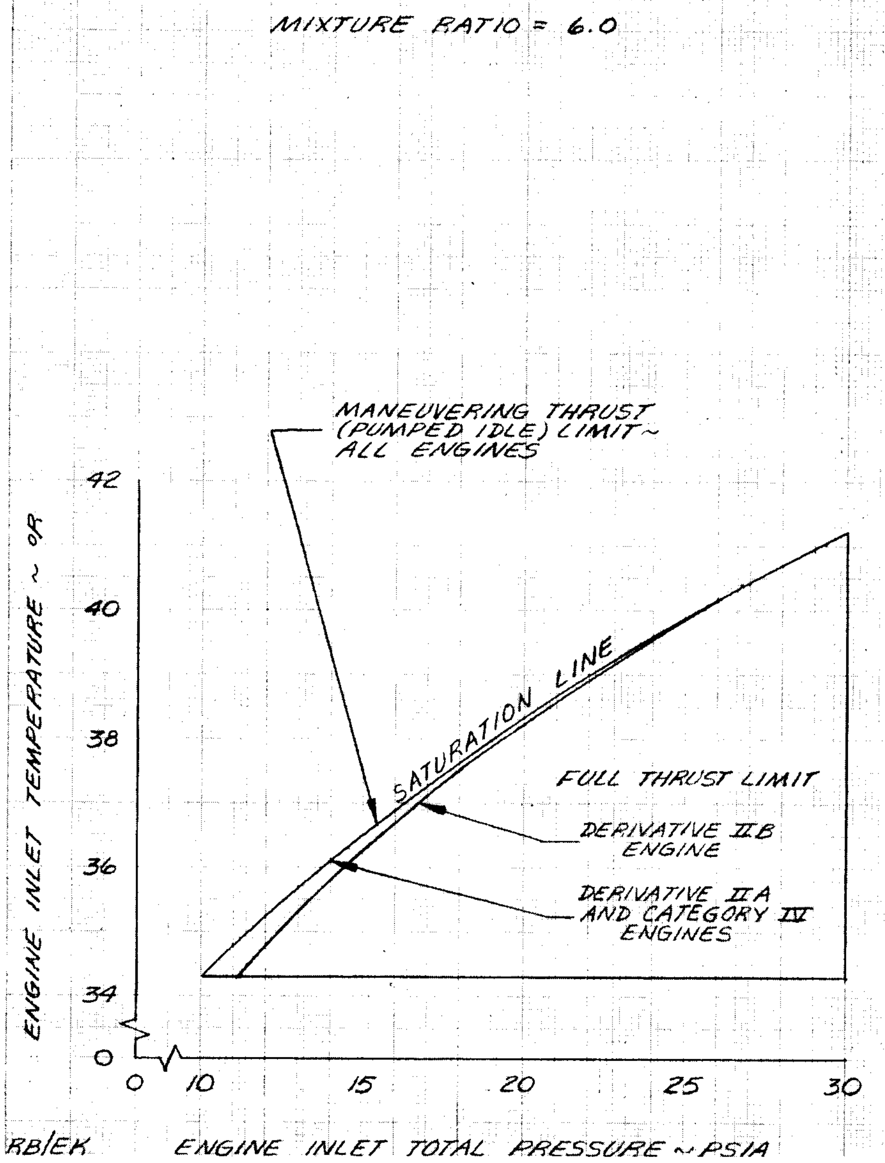


Figure E-5. Required Fuel Conditions at Engine Inlet Derivatives IIA and IIB and Category IV Engines Maneuver and Full Thrust DF 96907

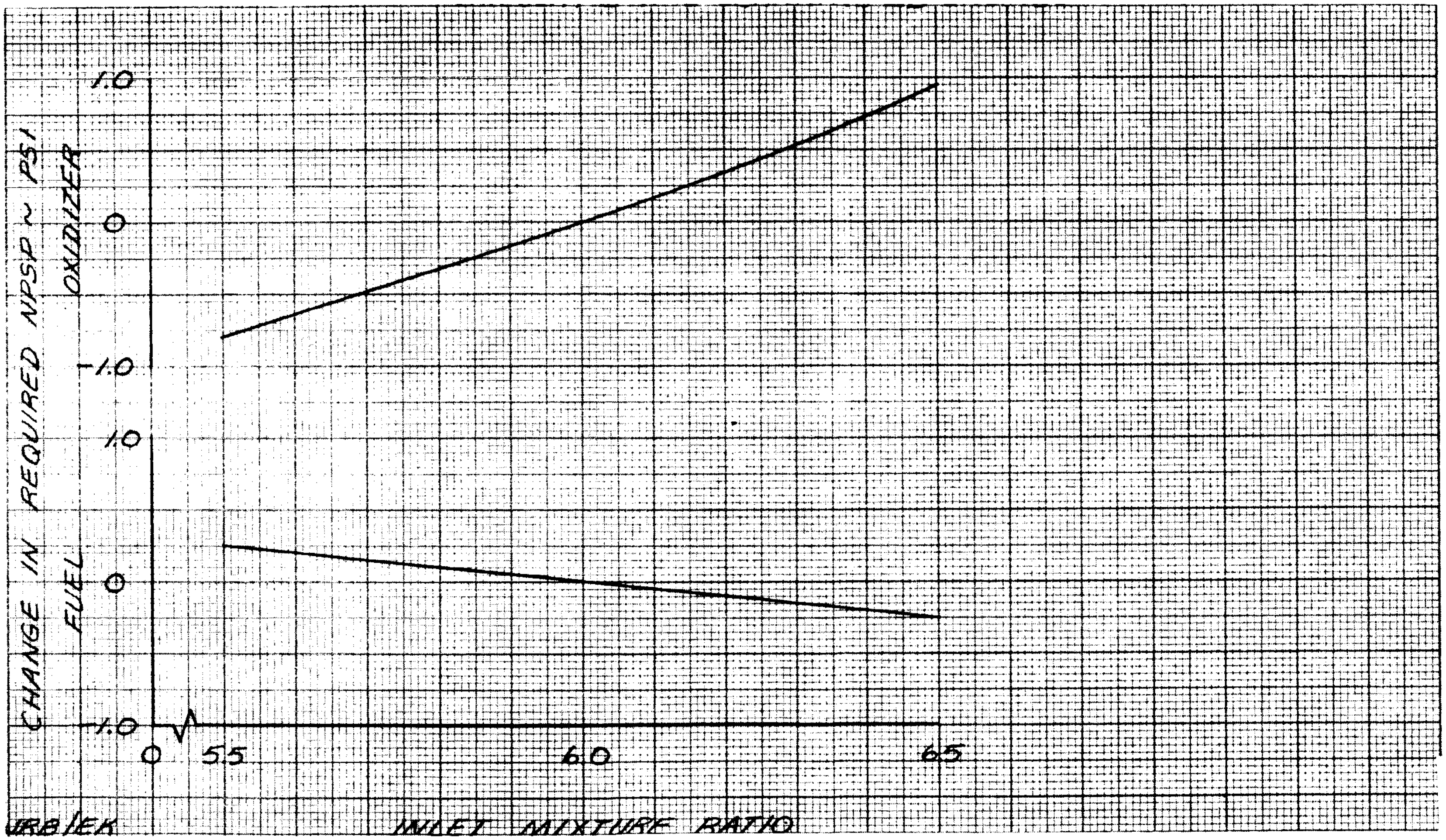


Figure E-6. Effect of Mixture Ratio on Required Engine Inlet NPSP Category I and Derivative IIB Engines DF 96972

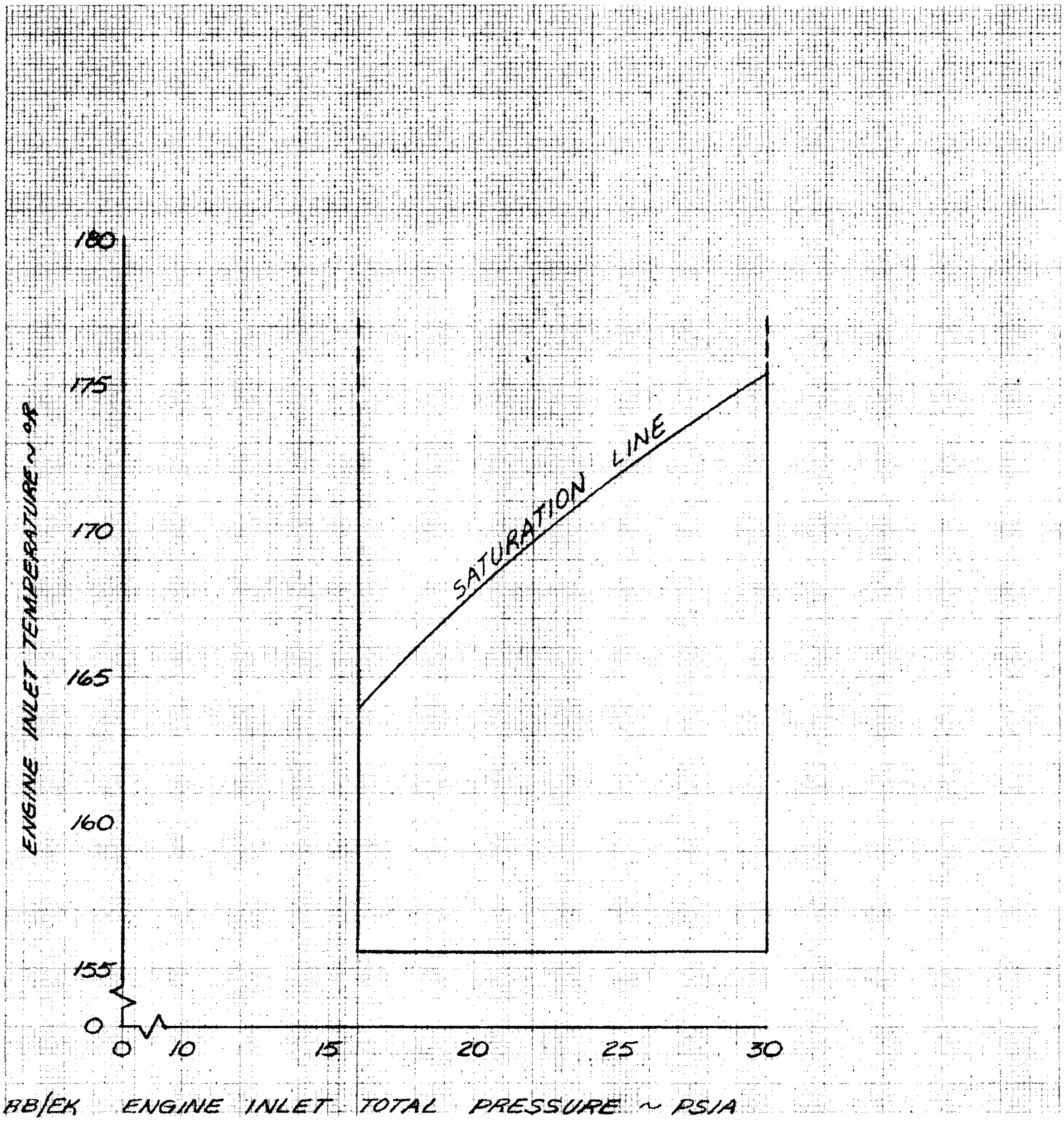


Figure E-7. Allowable Oxidizer Conditions for Tank Head Idle Start Derivative IIA, IIB and Category IV Engines

DF 96913

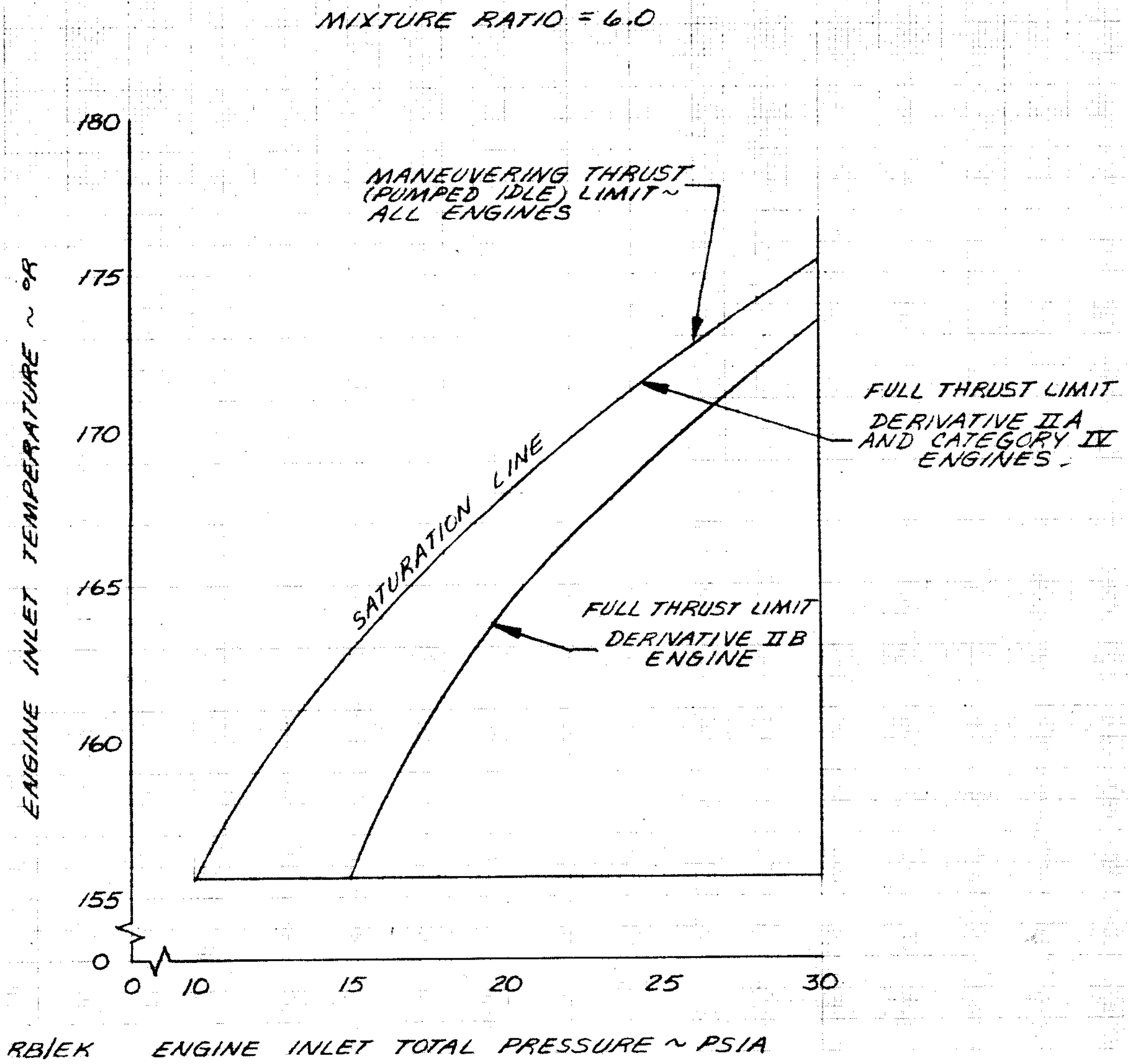


Figure E-8. Required Oxidizer Conditions at Engine Inlet Derivatives IIA and IIB and Category IV Engines Maneuver and Full Thrust DF 96908

5.1 Maximum Pressures

Maximum recommended propellant inlet pressure is 30 psia. This is not an engine limit, but it was selected as the upper pressure limit because pressures higher than this were not believed to be of interest for the Space Tug. It is desirable to keep the range of inlet conditions small so that a fewer number of development tests would be required to verify satisfactory engine operation over the specified range.

5.2 Minimum Pressures

The minimum pressure limits were determined by examining the impact of low inlet pressures on engine ignition, turbopump breakaway torque, and turbopump suction requirements. The results of these analyses are discussed below.

5.2.1 Analysis of Ignition Limits

The ignition characteristics of a burner are a function of the level of energy provided to initiate combustion, the velocity or turbulence level of the combustibles, the homogeneity of the combustible mixture, the proximity of burner surfaces which may dissipate the heat of reaction, the ratio of propellant mass relative to the ideal mixture required for combustion (mixture ratio) and the proximity of the combustible molecules to each other (chamber pressure). Mixture ratio and chamber pressure are independent variables for any fixed set of hardware and are often used as a means of correlating ignition performance. The ignition envelope generally takes on a bucket shape as shown by the solid line in figures E-9 and E-10 because of the quenching effects of excess combustibles present at very high and low mixture ratios.

The chamber mixture ratio and chamber pressure present in the combustor prior to ignition is determined to a great extent by the fuel and oxidizer inlet pressure which exist at engine start. If the inlet pressures and the resulting flowrates are too low, the chamber will never achieve the condition necessary for ignition. An analysis was undertaken to define this lower limit of inlet pressures.

A review was made of previous RL10A-3-7 engine tests to determine the minimum level of inlet pressures that provided satisfactory starts to tank head idle. The review showed that satisfactory ignition had been achieved at fuel and oxidizer inlet pressures as low as 16 psia. Testing was limited to pressures above sea level ambient pressure because of the added cost of evacuating the propellant tanks and protecting against the possibility of air leaks into the tanks and inlet lines. Since these RL10A-3-7 engine tests were conducted with the turbopump, igniter, injector and main chamber being considered for use on the Derivative II engines the results are directly applicable and therefore confirm the ability to light at engine inlet pressures down to 16 psia. A summary of these tests is included in Appendix I.

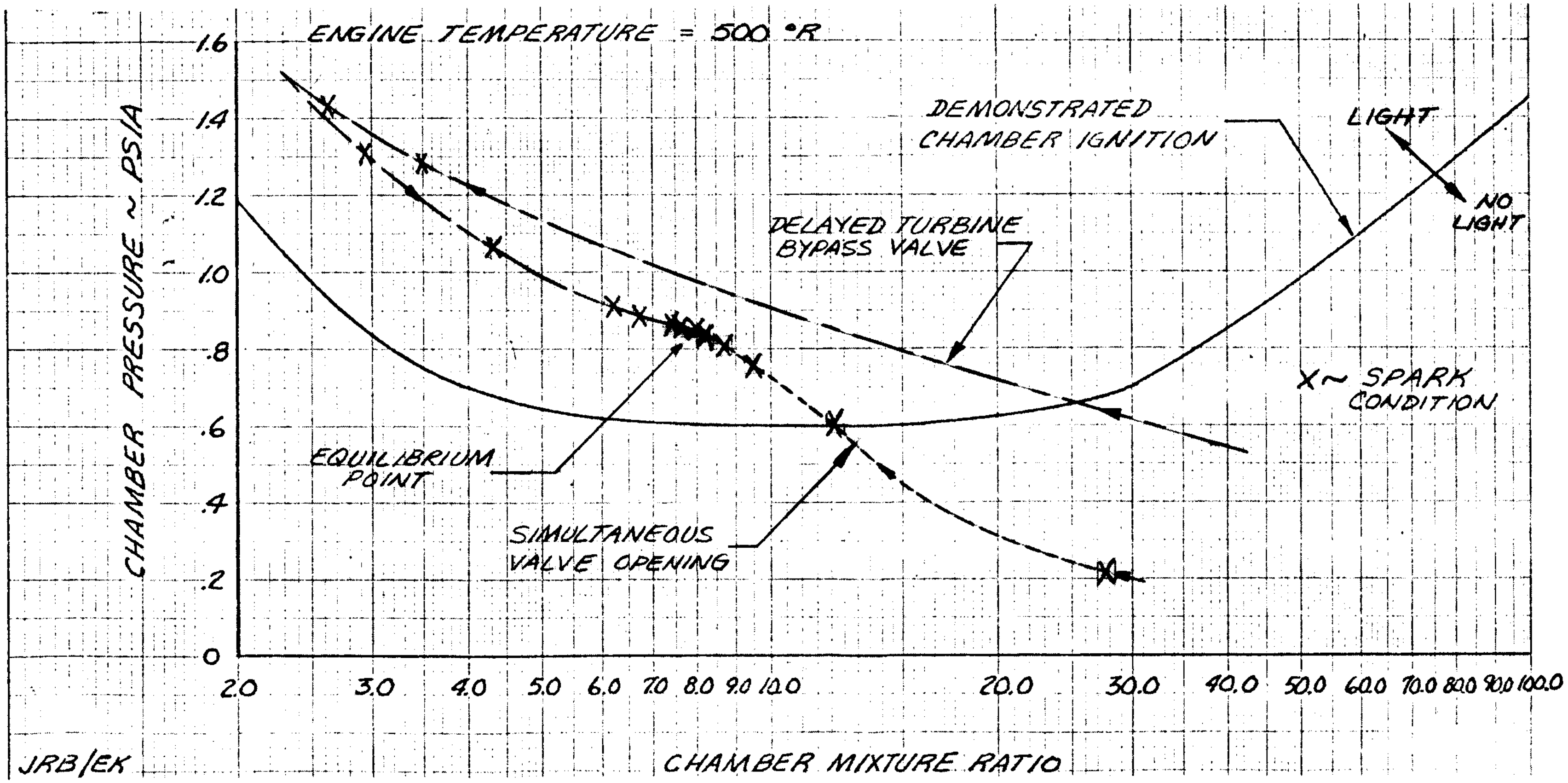


Figure E-9. RL10 Derivative II Unlit Chamber Flow Characteristics at Start

DF 96611

DF 96612

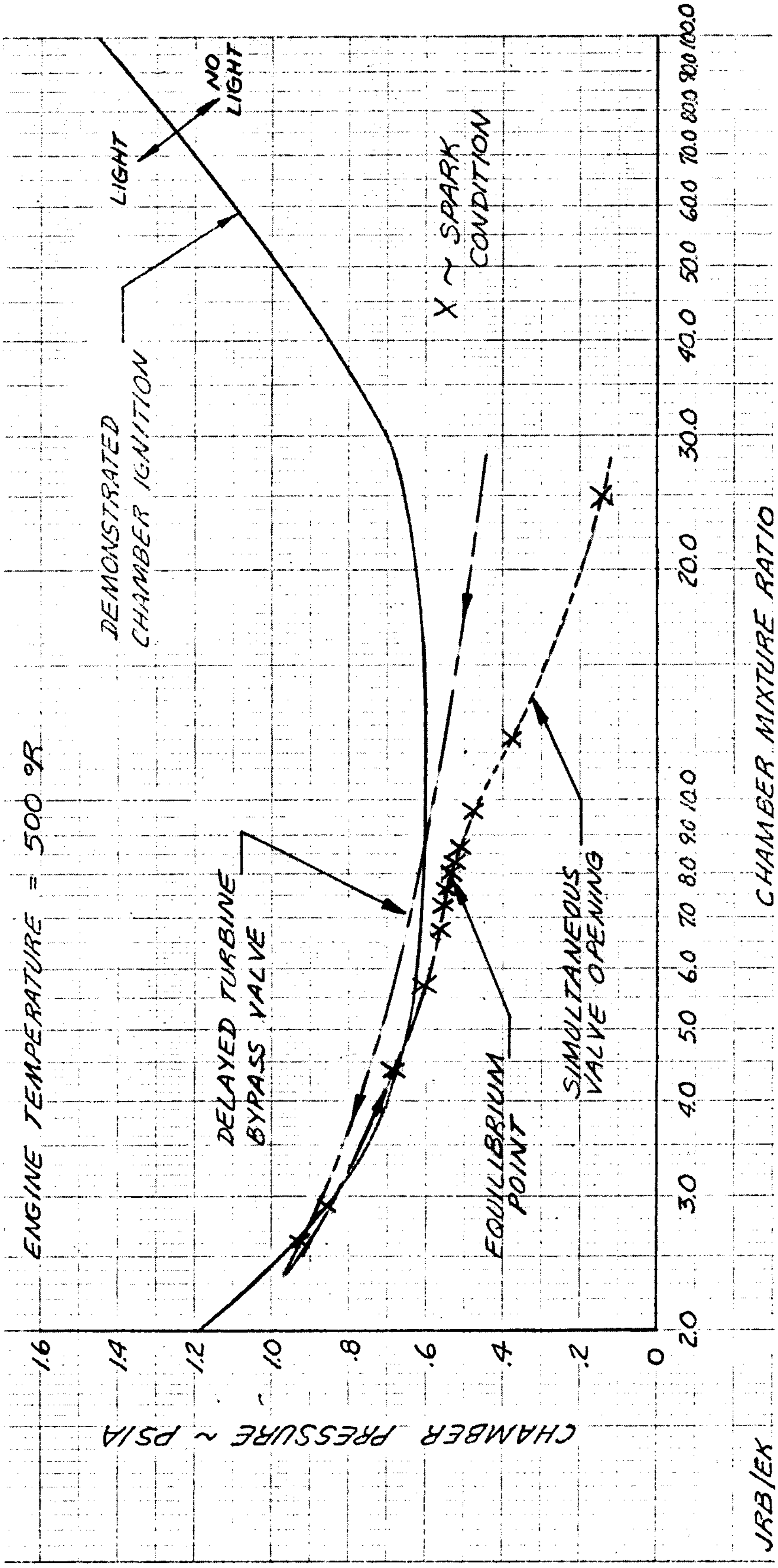


Figure E-10. Derivative II Unlit Chamber Flow Characteristics at Start

While the engine ignition testing was limited to supply pressures greater than 16 psia, it was possible to simulate the process of attempted ignition at lower inlet pressures through the use of a computer program. The computer program used for this purpose simulates the engine internal resistance, heat transfer and volumetric dynamics and provides the parameters necessary to predict main chamber ignition capability. These parameters, unlit chamber pressure and chamber mixture ratio, were calculated as a function of time after engine start signal and were compared to the chamber ignition characteristic defined from test results of the RL10A-3-7 engine. Ignition characteristics were predicted for both 16 psia and 10 psia fuel and oxidizer inlet pressures.

As demonstrated in engine tests, the predicted mixture ratio/chamber pressure characteristic for a Derivative II engine with a 16 psia inlet pressure was well within the demonstrated chamber ignition "bucket" for two assumed start sequences as previously shown in figure E-9. In one sequence the engine inlet valves and the turbine bypass valve are opened simultaneously. As the fuel and oxidizer systems fill, the flowrates into the chamber increase thus increasing unlit chamber pressure until an equilibrium point is reached. In the other sequence the fuel and oxidizer inlet valves are opened simultaneously but the turbine bypass valve opening is delayed 0.200 sec. Delaying the opening of the turbine bypass valve allows the fuel system volume to become charged to a level of 16 psia prior to attempting ignition. When the turbine bypass valve is opened to allow the fuel to enter the chamber the initial fuel flowrate is large thus providing a chamber pressure/mixture ratio path well within the ignition "bucket". This is the sequence used by all operational RL10 engines. The equilibrium point is, of course, common to both cases.

The predicted mixture ratio/chamber pressure characteristic for a Derivative II engine with an assumed fuel and oxidizer inlet pressure of 10 psia is shown in figure E-10. While ignition could conceivably take place prior to reaching equilibrium for the case with a delayed opening of the turbine valve, the equilibrium point is not within the demonstrated "bucket". Ignition is not predicted for the case with simultaneous valve opening. It is unlikely that ignition could be obtained consistently with the Derivative II engine configuration with 10 psia inlet pressures. While engine modifications could improve the ignition characteristics e.g., pump bypass lines, enlarged valve area, etc., by increasing the initial flowrates they would complicate engine operation.

Analysis of the Category IV engine indicated that satisfactory ignition could also be achieved with that engine with inlet pressures below 16 psia. Even though its starting flowrates are lower than for the Derivative II engines its chamber throat is considerably smaller making its unlit chamber pressure levels higher. As a result it has more ignition margin than the Derivative II engines.

The following conclusions were made from this analysis and lead to the selection of 16 psia as the minimum inlet pressure limit for tank head idle start.

a. Satisfactory ignition characteristics have been demonstrated with inlet pressures as low as 16 psia.

b. An ignition margin (P_c minus minimum allowable P_c) of 42% is predicted for the Derivative II engine with inlet pressures of 16 psia.

c. Ignition is unlikely for starts with an inlet pressure of 10 psia for the present Derivative II engine configuration.

d. While ignition is likely at inlet pressures as low as 13 psia with the Derivative II engine, start tests would require major modifications to facility tanks and feed lines to allow operation under evacuated conditions.

e. Delaying the opening of the turbine bypass valve provides additional margin for engine ignition.

5.2.2 Turbopump Breakaway Torque

Analytical studies were also conducted using the engine transient computer programs to determine if sufficient turbine torque would be developed to breakaway the turbopumps with inlet pressures as low as 10 psia. Turbine torque levels were estimated for various fuel inlet pressures and these values were compared with the torque required to break away the turbopump. Breakaway torque for the Derivative II engines was determined from statistical analysis of data from 28 RL10 production engines. A comparison of available and required torques for these engines is presented in figure E-11. This comparison shows that sufficient torque is developed at 10 psia to break away almost all of the Derivative II engines. The percentage of these engines that would require rework to reduce breakaway torque is shown in figure E-12 as a function of fuel inlet pressure. It indicates that less than 1% of the Derivative II engines would require rework with inlet pressures as low as 10 psia. Low friction seals were incorporated in the design of the Category IV engine, resulting in an extremely low required turbopump breakaway torque. Analysis indicated that the Category IV engine could break away with inlet pressure as low as 5 psia. It was concluded from this study that turbopump breakaway would not be a problem for accelerations from tank head idle to pumped idle (maneuvering thrust) with fuel inlet pressures as low as 10 psia.

5.2.3 Turbopump Suction Requirements

Analytical studies were conducted to determine the effect of low inlet pressure on the suction capabilities of the baseline engines at full thrust. The effects of low inlet pressures on choking velocity, cavitation number, and vapor volume capability were examined for the Derivative IIA and Category IV engines. It was determined that no significant reduction in two phase pumping capability should result for these engines from decreasing inlet pressures to 10 psia. Therefore, 10 psia was selected as the minimum allowable inlet pressure for full thrust operation of the Derivative IIA and Category IV engines. Since all three engines also have two-phase pumping capability at maneuvering thrust (pumped idle) 10 psia was also selected as the minimum allowable level for this operating mode.

DF 97600

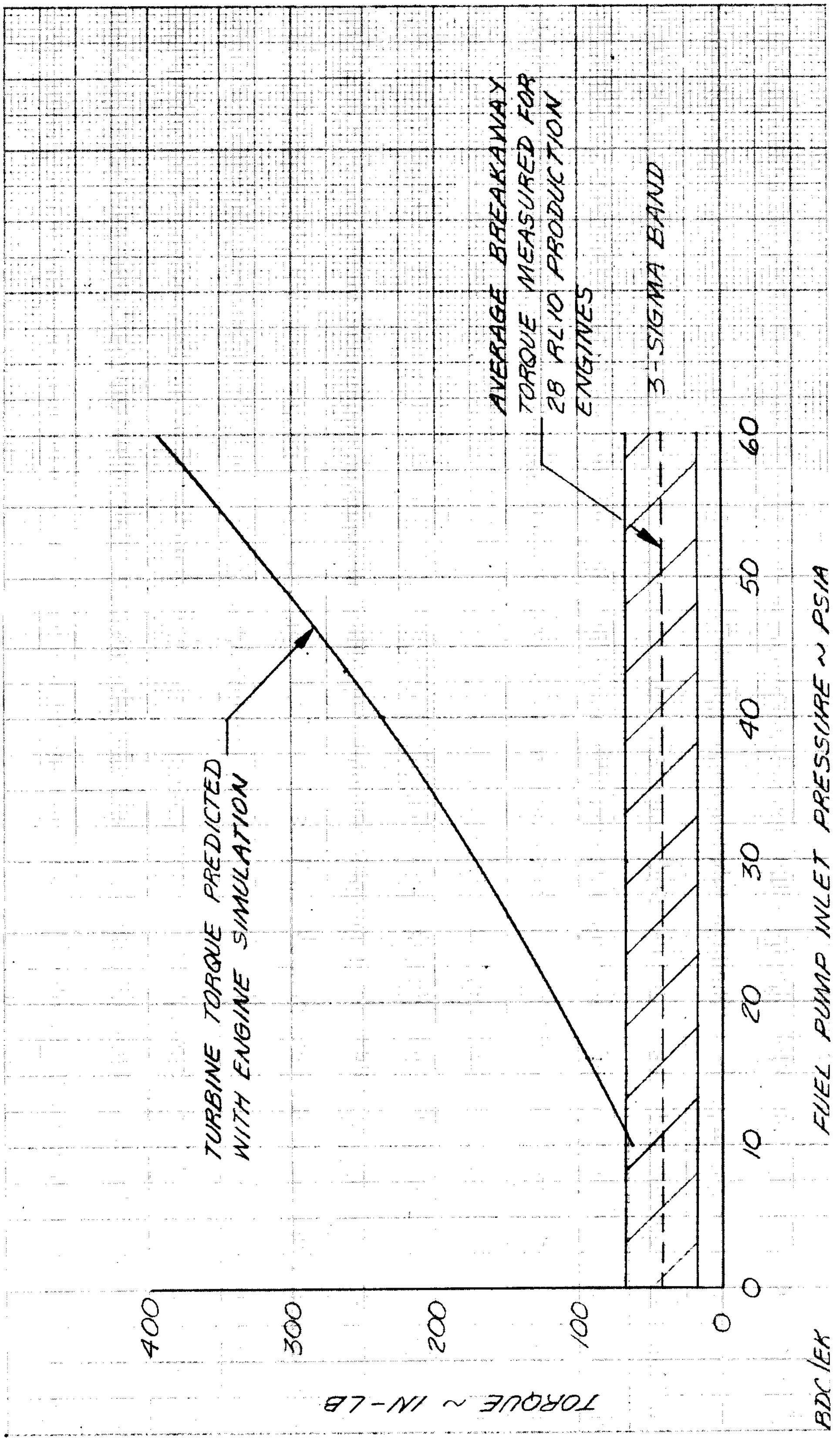


Figure E-11. Estimated Derivative IIA Breakaway Torque Margin

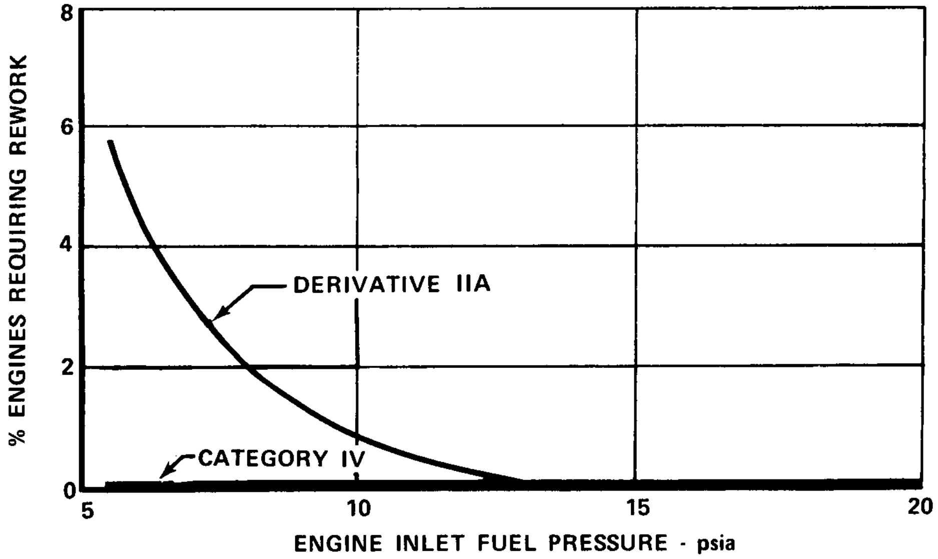


Figure E-12. Percent Engines Requiring Rework for Breakaway Torque FD 74140

Since the Derivative IIB engine requires a positive pump inlet NPSH, a study was conducted for the Derivative IIB engine using the NPSH and TSH curves defined in Section C.1.1.1, to determine the effect of tank saturation pressures on engine NPSH requirements. As saturation pressures are reduced there is an accompanying decrease in inlet temperature, causing the available TSH of the propellants to decrease. Since the NPSH required by the pump is a direct function of the available TSH, pressure reductions which reduce TSH cause an increase in NPSH required by the engine. The results of this study are shown in figure E-13. These effects were used to set the minimum allowable NPSP levels for full thrust operation of the Derivative IIB engine previously shown in figures E-5 and E-8. The effects of changes in the operating point of the engine due to mixture ratio changes on NPSH requirements were also defined for the Derivative IIB engine. These effects are as previously shown in figure E-6 and they indicate that as mixture ratio is increased fuel NPSH requirements are decreased and oxidizer NPSH requirements are increased.

6. CATEGORY I ENGINE STUDIES

While the basic definition of the Category I engine was accomplished during the previous study (Reference 1), an expanded definition of engine cooldown requirements and a redefinition of engine inlet requirements were established in this study. In addition, the engine characteristics have been included along with other RL10 Derivative engines in the Interface Control Document, Volume III. A brief description of the Category I engine and the results of the trickle cooldown and inlet condition studies are given below.

6.1 Engine Description

The Category I engine is basically an RL10A-3-3 engine that has been modified and requalified to operate under Space Tug conditions. The changes in operating conditions and operating characteristics from that of the RL10A-3-3 engine are as follows:

a. The design point mixture ratio and mixture ratio range are changed from 5.0 ± 0.6 to 6.0 ± 0.5.

b. The propellant mass expended overboard for turbopump cooldown is reduced by reducing the cooldown flowrate and increasing the cooldown time. This is known as trickle cooldown.

c. Provisions are incorporated for providing gaseous hydrogen during full thrust operation for autogenous pressurization of the vehicle fuel tank.

d. The nominal operating engine inlet pressures are reduced to levels more consistent with the Space Tug requirements.

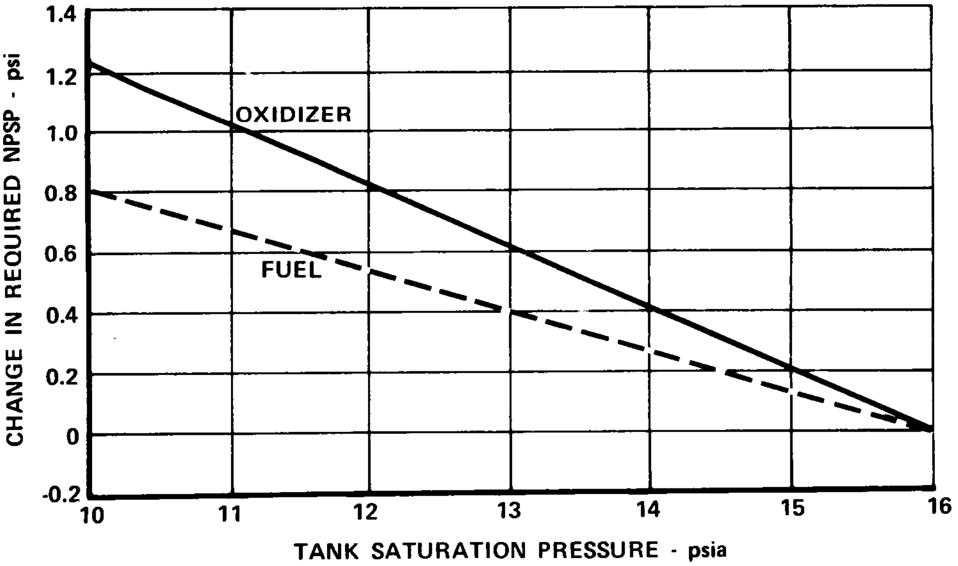


Figure E-13. Change in Required NPSP FD 74139

Table E-4 presents the full thrust design point characteristics, and a summary of engine geometry for the Category I engine.

Detailed engine cycle sheets for the Category I engine showing full thrust operating characteristics at mixture ratios of 5.5, 6.0 and 6.5 are presented in Appendix IV. Transient and other operational characteristics are included in the Interface Control Document (Volume III) of this report.

Table E-4. Baseline Category I Engine

Full Thrust Design Point Characteristics	
Thrust, lb vac	15,000
Mixture Ratio	6.0
Chamber Pressure, psia	400
Expansion Ratio	57
Length, in.	70.1
Diameter, in.	39.5
Dry Weight, lb	292.5
Specific Impulse, sec	438.0
Fuel Pump Speed, rpm	29,000
Required NPSH, ft	
Fuel	14
Oxidizer	7.5
Life	
Minimum Number of Firings	≥190
Minimum Operating Time, hr	5.0

6.2 Hardware Modifications

Several modifications to the basic RL10A-3-3 engine hardware are required to produce a Category I engine. The modifications are as follows:

a. The oxidizer flow control valve is modified to provide one oxidizer flowrate during turbopump chilldown and another flowrate to the combustion chamber at start for engine ignition. A two step, helium actuated bypass system similar to the one successfully used during development testing of the RL10A-3-3A engine is envisioned to provide this function.

b. The port areas and sequencing of the interstage bleed and pressure relief valve and the discharge cooldown and pressure relief valve are modified to enable the engine to operate in the trickle mode for turbopump cooldown and in the same mode as the current RL10A-3-3 engine during the remainder of the engine duty cycle.

c. A third electrical solenoid is added to accomplish the new sequencing of the interstage bleed and pressure relief valve in the trickle cooldown mode and the opening of the oxidizer flow control bypass prior to engine start.

d. A fuel tank pressurization valve is added to provide gaseous hydrogen for autogenous pressurization of the fuel tank during steady-state engine operation. A valve of this type was successfully used on the RL10A-3 engines for pressurization of the Saturn S-IV fuel tank and it was also tested during the development of the RL10A-3-3A engine.

e. The following changes in helium line routing are required to achieve the trickle cooldown mode:

(1) A helium line is required from the prestart solenoid to the fuel pump Discharge Cooldown and Pressure Relief Valve rather than from start solenoid as on the existing engine.

(2) Helium lines are required to the new solenoid valve, and from this valve to the Oxidizer Flow Control valve and to the fuel pump Interstage Bleed and Pressure Relief Valve.

6.3 Category I Trickle Cooldown Data

Cooldown of the Category I engine is accomplished by flowing liquid propellants through the engine pumps and discharging them overboard. While this cooldown method is currently used for the RL10A-3-3 engine in the Centaur vehicle, the cooldown duration must be kept low because of the "g" forces acting on the vehicle over that time period. More efficient use can be made of the propellant, if the cooldown is accomplished at a low flowrate over an extended period of time.

Flow through the oxidizer pump is set by the flow area of the oxidizer flow control valve. After leaving the valve the oxidizer discharges overboard through the injector. Fuel flow through the fuel pump is set by the cooldown area of the fuel pump discharge cooldown and pressure relief valve. The interstage bleed and pressure relief valve is closed at this time so that all of the fuel cooldown flow goes through both stages of the pump. The fuel is discharged overboard through the fuel pump discharge cooldown and pressure relief valve.

Parametric cooldown data were generated for the Category I engine during this study to more fully define propellant consumption and cooldown time requirements than those presented in the previous study (Reference 1). A computer program which simulated the transient cooldown characteristics of the Category I engine was used to establish the cooldown requirements. This simulation had originally been built to predict RL10A-3-3 cooldown characteristics and its predictions have been checked out against RL10A-3-3 cooldown data obtained during testing at the NASA-LeRC Plum Brook facility.

Cooldown characteristics were evaluated for the Category I engine over the following ranges of conditions:

	Oxidizer	Fuel
• Engine Housing Temperature, °R	$200 \leq T \leq 500$	$100 \leq T \leq 500$
• Engine Inlet Pressure, psia	$16 \leq P \leq 28$	$16 \leq P \leq 24$
• Engine Inlet NPSP, psi	$0 \leq NPSP \leq 8$	$0 \leq NPSP \leq 4$

Cooldown data were generated for constant housing temperatures, inlet pressures and NPSP's with varying discharge cooldown and pressure relief valve and oxidizer flow control valve areas. For any given set of conditions, engine temperature characteristics were obtained as a function of time during the cooldown transient. The cooldown was considered to be complete when the pump housing temperature reached 2 deg above the fluid saturation temperature. The parametric cooldown curves generated are included in the Interface Control Document (Volume III) of this report.

6.4 Category I Inlet Requirements

Category I engine propellant inlet condition requirements were defined in the previous study (Reference I). During the current study those requirements were re-evaluated using the same criteria as were used for the Derivative IIB, since both engines use the same RL10A-3-3 turbomachinery. The redefined propellant inlet condition curves are presented in the Interface Control Document (Volume III) of this report as figures E-1 (fuel conditions) and E-5 (oxidizer conditions).

The rationale used to establish the limits shown on these curves were as follows:

a. The fuel and oxidizer maximum inlet pressures were reduced from 40 to 30 psia since (while not an engine limit) pressures higher than this were not believed to be of interest for the Space Tug.

b. The steady-state minimum operating pressure and temperature limits were reduced to reflect the capability of the pumps to operate with tank saturation pressures as low as 10 psia. In the previous study, tank saturation pressures lower than 16 psia were not considered.

c. The pumps required NPSH characteristics were redefined in this study in the same manner as described for the Derivative IIB in Section C3.1.1 and included consideration of TSH variations with propellant conditions which was not done in the previous study (Reference 1).

d. The minimum allowable inlet pressures at start were not changed from the previous study.

7. IMPACT ON ENGINE DESIGN OF ELIMINATING THE MANEUVERING THRUST (PUMPED IDLE) MODE FROM THE DERIVATIVE II ENGINES

The Derivative IIA and IIB engines are both designed to operate at three thrust levels (i.e., Tank Head Idle, Maneuvering Thrust (Pumped Idle) and Full Thrust) and to go through these steps sequentially during the transient to full thrust. A special study was accomplished to determine the impact on the Derivative II engine designs of eliminating the maneuvering thrust (pumped idle) requirement from these engines.

If the requirement for maneuvering thrust (pumped idle) were eliminated, some engine components would differ from those of the baseline Derivative II to accomplish a direct start transient from tank head idle to full thrust. These differences are minor and they would in most cases result in a simplification of the components. The specific components affected and the changes required are discussed below:

a. Turbine Bypass Valve - This valve could be simplified in that it would no longer have to reopen after the start of rotation to a setting for maneuvering thrust. In particular, the bellows system which is actuated by turbine inlet pressure could be eliminated since it would no longer be required.

b. Bypass Solenoid No. 2 - This solenoid could be eliminated. Its present function is to port hydrogen, upon command, from the turbine inlet to the turbine bypass valve to actuate the valve for the maneuvering thrust operation.

c. Small Lines - Two small hydrogen lines would not be required. They are used in the baseline engine design to carry hydrogen from the turbine inlet to the Bypass Solenoid No. 2 and from the solenoid to the Turbine Bypass Valve.

d. Thrust Control - The thrust control would require modifications to its preload and lag volume in order to regulate the transient rise rate to prevent excessive inlet line fluid acceleration losses. An acceleration from tank head idle to full thrust without stopping at the maneuvering thrust level would have transient characteristics similar to those for the Category I engine. The Category I engine accelerates from 10 to 90% thrust in approximately 0.3 sec.

e. Oxidizer Control Valve - This valve could also be simplified. Since it would not have to open to an intermediate area for control of oxidizer flow at maneuvering thrust, the bypass pintle and its actuation system could be eliminated.

Figure E-14 is a propellant flow schematic for the Derivative IIB engine. The locations of the components which differ from the baseline design are indicated on this figure. The components are representative of those on the Derivative IIA engine as well.

8. GO_2 HEAT EXCHANGER STABILITY EVALUATION

The possibility of oxidizer instability occurring in the GO_2 heat exchanger was investigated for the RL10 derivative engines. The evaluation was specifically conducted for the Derivative IIB engine; however, the results should be applicable to all of the baseline engines.

Stability was evaluated using criteria established by R. S. Thurston, J. D. Rogers, and V. J. Skoglund during an investigation of pressure oscillations occurring during convective heating of hydrogen. The criteria had also successfully been applied to liquid oxygen heat exchanger data obtained during tests made in support of the Saturn turbine exhaust gas/liquid oxygen heat exchanger.

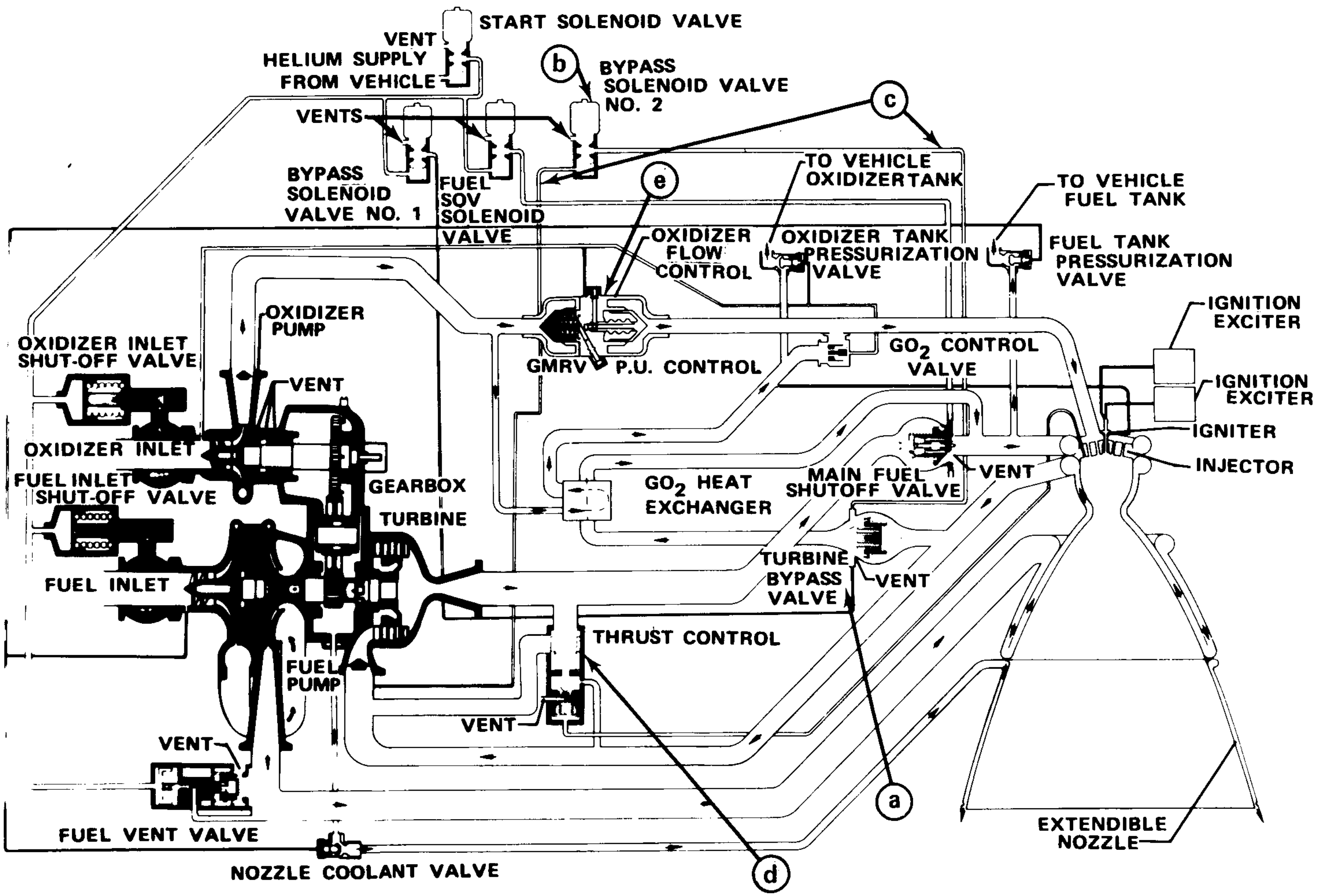

START SOLENOID VALVE
VENT
HELIUM SUPPLY FROM VEHICLE
(b)
BYPASS SOLENOID VALVE NO. 2
(c)
VENTS
BYPASS SOLENOID VALVE NO. 1
FUEL SOV SOLENOID VALVE
(e)
TO VEHICLE OXIDIZERTANK
TO VEHICLE FUEL TANK
OXIDIZER FLOW CONTROL
OXIDIZER TANK PRESSURIZATION VALVE
FUEL TANK PRESSURIZATION VALVE
IGNITION EXCITER
OXIDIZER PUMP
OXIDIZER INLET SHUT-OFF VALVE
GMRV
P.U. CONTROL
GO_2 CONTROL VALVE
IGNITION EXCITER
VENT
IGNITER
OXIDIZER INLET
MAIN FUEL SHUTOFF VALVE
VENT
INJECTOR
FUEL INLET SHUT-OFF VALVE
GEARBOX
GO_2 HEAT EXCHANGER
TURBINE
TURBINE BYPASS VALVE
VENT
FUEL INLET
THRUST CONTROL
FUEL PUMP
VENT
VENT
(a)
FUEL VENT VALVE
EXTENDIBLE NOZZLE
NOZZLE COOLANT VALVE
(d)

Figure E-14. RL10 Derivative IIB Propellant Flow Schematic

FD 74111B

These criteria relate the inception of pressure oscillations in the heat exchanger to the specific volume characteristics of the fluid being heated and the heat transfer characteristics of the heat exchanger. The specific volume characteristics are described by a specific volume number:

$$N_{SV} = V^*_{FG}/V^*_F$$

where:

N_{SV} is the specific volume number
V^*_{FG} is the change in specific volume going from a liquid to gas
V^*_F is the specific volume of the liquid

The heat exchanger characteristics are described by a boiling number:

$$N_{BO} = q/G\lambda^*$$

where:

N_{BO} is the boiling number
q is the heat flux at the point where film boiling occurs
G is the mass flux (flowrate per unit of flow area) at the point where film boiling occurs
λ^* is the heat of vaporization

For a given specific volume number there is a minimum boiling number above which oscillations occur. The equation for the boiling number at the inception of oscillation is

$$N_{BO} = 0.0045\ (N_{SV})^{-0.75}$$

Oxidizer stability characteristics were investigated by determining a boiling number and specific volume number for the Derivative IIB engine operating conditions and GO_2 heat exchanger configuration, comparing the N_{BO} value to one calculated by this equation. The analysis showed that some instability would exist at all three operating levels but tank head idle was the only operating condition at which oxidizer heat exchanger instability could adversely affect engine operation.

While the analysis indicated that oxidizer instability may occur with the baseline GO_2 heat exchanger design during tank head idle operation there was insufficient confidence in the stability criteria to change the design. Some very preliminary design analysis was accomplished, however, on an alternate design to confirm that a heat exchanger could be made which would meet the stability criteria. The alternate heat exchanger design would be heavier and cause higher pressure loss.

Since it was felt that the stability question could not be resolved without heat exchanger tests, the baseline design was retained.

(A)

REFERENCES

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