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TRADE STUDY REPORTS

Data Item Identification Number
S054-SS090205-F1

NUCLEAR ROCKET OPERATIONS

September 1968

CLASSIFICATION CATEGORY

UNCLASSIFIED

[Signature]
CLASSIFYING OFFICER

3 Oct. 1968
DATE

AEROJET-GENERAL CORPORATION
A SUBSIDIARY OF THE GENERAL TIRE & RUBBER COMPANY

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TRADE STUDY REPORTS

Data Item Identification Number
S054-SS090205-F1

September 1968

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I. NERVA Mass Properties
Status Report

AEROJET-GENERAL CORPORATION
NUCLEAR ROCKET OPERATIONS
SACRAMENTO, CALIFORNIA

TO: J J Beereboom

DATE: 19 September 1968
KET:lm:7730:M839

FROM: I L Odgers

SUBJECT: NERVA Data Item S-47-43.1

DISTRIBUTION: E Arbtin, F B Damerval, R V Evleth, G S Kaveney, J L Smith,
W D Stinnett, K E Talbott

REFERENCE: (a) Memo 7810:0550M, S A V... to Distribution, dtd 28 August 1968
Subj: "NERVA System Documentation and Assignments"

ATTACHMENT: (1) AFSCM 310-1/AFLCM 310-1, Number S-47-43.1 (NRO)
(2) Report Number RN-S-0483 "NERVA 75K Flight Engine Estimated
Mass Properties Status Report"

Attachments (1) and (2) are hereby submitted in response to Reference (a).

Attachment (1) is a rewrite of the AFSCM 310-1 document by the cognizant Section 7730 Structures Engineer and is considered more applicable than the original document. Both the Minuteman and Titan Weights Groups were contacted, and it was found that both were required by contract to follow MIL-M-38310A. Inspection of this document revealed it to be very complete in the coverage of reports of this nature, and for this reason it has been incorporated in the AFSCM 310-1 document. Perhaps the most outstanding change is in the title. It is felt that "MASS PROPERTIES" is a much more definitive term than "WEIGHT AND BALANCE" and for this reason it is felt that this change should be adopted.

Attachment (2) is the report itself, and although it does not follow S-47-43.1 precisely, the information contained is adequate at this time and can be easily expanded as required.

CONCURRED WITH:

G. S. Kaveney
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CLASSIFICATION CATEGORY	
<i>Unclassified</i>	
<i>G. S. Kaveney</i>	<i>9/15/68</i>
CLASSIFYING OFFICER	DATE

30 September 1968

AFSCM 310-1/AFICM 310-1

TITLE	NUMBER
MASS PROPERTIES STATUS REPORT	S-47-43.1 (NRO)
USE	DATE
The purpose of this data is to provide reasonably detailed information for engineering checking and reference purposes.	OFFICE OF PRIMARY RESPONSIBILITY
	DDC REQUIRED
	REFERENCES AFSCM 80-8 MIL-M-38310A
INTERRELATIONSHIP	
PREPARATION INFORMATION	
<p>The Contractor shall prepare a Mass Properties Status Report which shall be estimated, calculated, or actual dependent on the status of the material within the report.</p> <p>The scope of the individual document shall determine the extent to which the information contained within that document shall be carried. When practical, MIL-M-38310 should be adhered to for the purpose of formulating the contents of a document, but for the sake of brevity in cases where not all the information specified by MIL-M-38310 is yet decided, the Contractor shall omit that which he deems unnecessary.</p> <p>A typical report could contain the following data in the order shown:</p> <ul style="list-style-type: none">(a) Title Page, containing the report number, type of submittal, step stage or module, serial numbers, date of issuance, contractor's name, mission identification.(b) Table of Contents.(c) Introduction with weight, current status, changes from last current to current, current mass property which has reached or exceeded its control parameter, highlights of newly encountered problems.(d) Mass properties summary including the following for first, second, and third generation components (Ref. Figure 1):	

- (1) Code
 - (2) Description
 - (3) Current weight
 - (4) Changes, last to current
 - (5) Percent estimated, calculated, or actual
 - (6) Center of gravity
 - (7) Moment of inertia
 - (8) Note number, which shall refer to remarks or change analysis
- (e) Detail mass properties up to fourth generation:
- (1) Code
 - (2) Description
 - (3) Estimated, calculated, or actual
 - (4) Current mass properties.
- (f) Mass properties change analysis which shall explain significant changes in current mass properties.
- (g) Trade data. This is applicable only when trade studies are being conducted.
- (h) Diagrams showing the following:
- (1) Principal structural stations and interfaces
 - (2) Reference datum for the 3-axes moment arms
 - (3) Significant lengths, heights, widths, and diameters
 - (4) Principal components identified.
- (i) Government Furnished Equipment - A tabulation of all GFE showing the drawing number and description, serial number where applicable, number required, and the unit, total dry and total wet specified mass properties.
- (j) Mass distribution in graphical form giving weight per unit of longitudinal length representing distributed loads and concentrated loads and related reaction points.

(k) Mass properties limits which are applicable only if limits are established by contract.

Figures 2 and 3 are samples of reporting forms that conform to items (d) and (e) of the outline given above. Reporting of the data within the report by machine is an approved method.

FUNCTIONAL CODE

FIRST GENERATION		5. MAIN PROPULSION	
SECOND GENERATION		THIRD GENERATION	
5.1	BASIC NUCLEAR POWERPLANT AND ACCESSORIES	5.1.1	Vehicle Mating Thrust Vector System Thrust Structure Thrust Chamber Nozzle Assembly (incl. extensions) Nuclear Subsystem Propellant Feed System Pneumatic System Electric and Instrumentation
5.2	VEHICLE INTEGRATION	5.2.1	Engine Mating Propellant Control Engine Control Nuclear Control

Ref. MIL-M-38310, Appendix B, Page 15

Figure 1

MASS PROPERTIES SUMMARY

CODE	DESCRIPTION	CURRENT WEIGHT	CHANGES LAST TO CURRENT	PERCENTAGE BREAKDOWN OF CURRENT WEIGHT			CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				EST	CALC	ACT	X	Y	Z	ROLL	PITCH	YAW	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	

REPORT NUMBER

RN-S-0483

TO

AEC-NASA SPACE NUCLEAR PROPULSION OFFICE

NERVA 75K FLIGHT ENGINE

ESTIMATED MASS PROPERTIES STATUS REPORT

SEPTEMBER 1968

NERVA PROGRAM

CONTRACT SNP-1

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INTRODUCTION

This report describes the mass properties data for the 75K thrust level NERVA flight engine as of September 1968.

The NERVA Engine features a thrust chamber constructed of 7039 aluminum with a Hastelloy X U-tube nozzle and Cres 347 skirt.

The total engine dry weight of 19,093 pounds includes all components that Aerojet-General Corporation is responsible for plus the Nuclear Subsystem and Internal Shielding as provided by Westinghouse Astronuclear Laboratories.

Approximately 15,000 pounds would be added to the weight of this engine with the inclusion of a man-rated nuclear shield.

MASS PROPERTIES SUMMARIES

All weight values given in the following summaries are in pounds. The center of gravity values are measured in inches from the X, Y, and Z reference datum as described on Page 24 of this report. The moment of inertia values are slug-feet squared and are moments about the gimbal point unless otherwise specified by a Note.

Numerical entries in the Note column are used to delineate mass properties changes or pertinent remarks which are explained in the mass properties change analysis on Page 23 of this report.

The Gimbaleed Components Summary on page 7 is included to separate the mass properties of the gimbaleed components only from the Basic Nuclear Powerplant and is therefore not a subtotal of the Main Propulsion System.

MASS PROPERTIES SUMMARY

CODE	DESCRIPTION	CURRENT WEIGHT	CHANGES LAST TO CURRENT	PERCENTAGE BREAKDOWN OF CURRENT WEIGHT			CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				EST	CALC	ACT	X	Y	Z	ROLL	PITCH	YAW	
5.0	MAIN PROPULSION SYSTEM	19093		86	14		128.9	1.0	0.3	1687.74	21092.83	21335.46	*

* Inertia About Main Propulsion System C.G.

MASS PROPERTIES SUMMARY

CODE	DESCRIPTION	CURRENT WEIGHT	CHANGES LAST TO CURRENT	PERCENTAGE BREAKDOWN OF CURRENT WEIGHT			CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				EST	CALC	ACT	X	-Y	Z	ROLL	PITCH	YAW	
5.1	BASIC NUCLEAR POWER PLANT	17618		85	15		128.9	1.0	0.4	1476.79	50369.85	50623.82	
5.1.1	Vehicle Mating	415		100			14.5	0.6	0.6	0	0	0	
5.1.2	Thrust Vector System	510		100			30.5	8.8	9.4	0	0	0	
5.1.3	Thrust Structure	760		100			64.4	0.3	0.3	125.20	353.73	361.51	
5.1.4	Thrust Chamber	858			100		139.9	0	0	104.05	2404.08	2404.08	
5.1.5	Nozzle Assembly	2160		20	80		244.3	-0.2	0	355.21	22416.77	22417.99	
5.1.6	Nuclear Subsystem	10115		100			130.7	0	0	493.32	21932.43	21932.43	
5.1.7	Propellant Feed System	2055		100			89.8	4.7	0.8	272.41	2625.31	2855.09	
5.1.8	Pneumatic System	405		100			44.1	1.5	4.2	78.96	98.93	117.92	
5.1.9	Elec. and Instr.	340		100			81.1	9.7	-6.6	47.64	538.61	534.81	

MASS PROPERTIES SUMMARY

CODE	DESCRIPTION	CURRENT WEIGHT	CHANGES LAST TO CURRENT	PERCENTAGE BREAKDOWN OF CURRENT WEIGHT			CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				EST	CALC	ACT	X	Y	Z	ROLL	PITCH	YAW	
5.2	VEHICLE INTEGRATION	1475		100			129.7	0	0	75.0	6987.0	6987.0	**
5.2.1	Engine Mating	250		100			4.0	0	0	0	0	0	
5.2.2	Propellant Control	195		100			0.3	0	0	0	0	0	
5.2.3	Engine Control	300		100			25.0	0	0	0	0	0	
5.2.4,	Nuclear Control	730		100			270.9	0	0	0	0	0	

** Inertia About Vehicle Integration C.G.

MASS PROPERTIES SUMMARY

CODE	DESCRIPTION	CURRENT WEIGHT	CHANGES LAST TO CURRENT	PERCENTAGE BREAKDOWN OF CURRENT WEIGHT			CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				EST	CALC	ACT	X	Y	Z	ROLL	PITCH	YAW	
	GIMBALED COMPONENTS	16619		84	16		135.3	0.8	0.1	1476.79	50369.85	50623.82	
5.1.3	Thrust Structure	760		100			64.4	0.3	0.3	125.20	353.73	361.51	
5.1.4	Thrust Chamber	858			100		139.9	0	0	104.05	2404.08	2404.08	
5.1.5	Nozzle Assembly	2160		20	80		244.3	-0.2	0	355.21	22416.77	22417.99	
5.1.6,	Nuclear Subsystem	10115		100			130.7	0	0	493.32	21932.43	21932.43	
5.1.7	Propellant Feed System	2055		100			89.8	4.7	0.8	272.41	2625.31	2855.09	
5.1.8	Pneumatic System	405		100			44.1	1.5	4.2	78.96	98.93	117.92	
5.1.9	Elec. and Instr.	266		100			103.6	12.5	-8.5	47.64	538.61	534.81	

DETAIL MASS PROPERTIES

All weight values given in the following summaries are in pounds. The center of gravity values are measured in inches from the X, Y, and Z datum as described on Page 24 of this report. The moment of inertia values are slug-feet squared and are moments about each individual unit's own center of gravity unless otherwise specified by a Note.

Numerical entries in the Note column are used to delineate mass properties changes or pertinent remarks which are explained in the mass properties change analysis on Page 23 of this report.

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.1	VEHICLE MATING	415.0		14.5	0.6	0.6	73.30	49.20	41.40	
5.1.1.1	Outer Ring - Upper	74.3	Est.	4.0	0	0	21.97	11.01	11.01	
5.1.1.2	Inner Ring - Upper	12.6	Est.	3.8	0	0	1.01	0.51	0.51	
5.1.1.3	Upper Stiffener	24.9	Est.	6.5	0	0	7.01	3.52	3.52	
5.1.1.4	Lower Stiffener	19.4	Est.	21.0	0	0	3.26	1.64	1.64	
5.1.1.5	Outer Ring - Lower	14.9	Est.	23.3	0	0	2.23	1.12	1.12	
5.1.1.6	Inner Ring - Lower	9.6	Est.	23.5	0	0	0.75	0.38	0.38	
5.1.1.7	Upper Web	23.4	Est.	3.3	0	0	4.41	2.20	2.20	
5.1.1.8	Lower Web	9.0	Est.	23.8	0	0	1.01	0.50	0.50	
5.1.1.9	Outer Skin	31.9	Est.	13.3	0	0	7.21	3.83	3.83	
5.1.1.10	Inner Skin	14.7	Est.	13.6	0	0	1.11	0.66	0.66	
5.1.1.11	Ribs	63.6	Est.	12.1	0	0	9.43	5.20	5.20	
5.1.1.12	Gimbal Pivots	100.0	Est.	27.0	0	0	8.15	8.23	0.44	
5.1.1.13	TVS Pivots	11.0	Est.	4.8	23.0	23.0	2.51	1.26	1.26	
5.1.1.14	Misc. Brackets	5.7	Est.	14.9	0	0	0.80	0.42	0.42	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.2	THRUST VECTOR SYSTEM	510.0	Est.	30.5	8.8	9.4	48.48	25.02	26.28	
5.1.2.1	Gimbal Ring	138.0	Est.	31.0	0	0	5.19	2.90	2.90	
5.1.2.2	Pins and Bearings	56.0	Est.	31.0	0	0	1.60	0.82	0.82	
5.1.2.3	TVS Actuators	316.0	Est.	30.2	14.2	15.2	30.46	15.30	17.31	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.3	THRUST STRUCTURE	760.0		64.4	0.3	0.3	125.28	170.73	178.52	
5.1.3.1	Ring	18.8	Est.	43.3	0	0	5.71	2.85	2.85	
5.1.3.2	Ring	18.2	Est.	51.2	0	0	5.15	2.58	2.58	
5.1.3.3	Ring	18.9	Est.	51.2	0	0	5.77	2.88	2.88	
5.1.3.4	Ring	8.8	Est.	51.1	0	0	0.59	0.29	0.29	
5.1.3.5	Ring	7.9	Est.	38.7	0	0	0.42	0.21	0.21	
5.1.3.6	Ring	7.8	Est.	47.1	0	0	0.41	0.21	0.21	
5.1.3.7	Ring	4.6	Est.	23.8	0	0	0.08	0.04	0.04	
5.1.3.8	Angle	47.2	Est.	74.3	0	0	4.68	6.69	6.69	
5.1.3.9	Angle	44.0	Est.	76.9	0	0	10.57	8.86	8.86	
5.1.3.10	PV Joint	105.7	Est.	106.9	0	0	17.28	8.73	8.73	
5.1.3.11	Beam Web	76.8	Est.	60.8	0	0	10.77	11.22	11.22	
5.1.3.12	Beam Inner Skin	52.0	Est.	69.0	0	0	5.15	6.66	6.66	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.3.13	Inner Skin	58.8	Est.	39.6	0	0	10.41	5.20	5.20	
5.1.3.14	TPA Web	11.0	Est.	51.0	0	0	0.41	0.21	0.21	
5.1.3.15	Skin	111.7	Est.	77.8	0	0	26.66	21.11	21.11	
5.1.3.16	Bulkhead	46.9	Est.	51.7	0	0	8.24	4.12	4.12	
5.1.3.17	TVS Pivots	11.0	Est.	39.0	18.5	18.5	1.63	0.83	0.83	
5.1.3.18	Gimbal Pivots	100.0	Est.	35.0	0	0	8.15	0.44	8.23	
5.1.3.19	Misc. Brackets	9.9	Est.	64.4	0	0	1.61	1.45	1.45	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.4	THRUST CHAMBER	858.0		139.9	0	0	104.14	211.20	211.20	
5.1.4.1	Closure	175.4	Calc.	104.2	0	0	10.09	6.31	6.31	
5.1.4.2	Cylinder	642.6	Calc.	151.5	0	0	88.08	126.96	126.96	
5.1.4.3	Bolts	40.0	Calc.	109.4	0	0	5.98	2.99	2.99	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.5	NOZZLE ASSEMBLY	2160.0		244.3	-0.2	0	355.50	1219.66	1220.87	
5.1.5.1	Nozzle - 6 to 1	915.0	Calc.	203.5	0.1	0	67.66	66.41	66.42	
5.1.5.2	Hot Gas Port	20.0	Est.	204.7	16.0	0	0.04	0.03	0.03	
5.1.5.3	Nozzle Bolts	30.0	Est.	196.0	0	0	3.02	1.51	1.51	
5.1.5.4	Skirt - 6 to 24	373.7	Calc.	237.6	-0.4	0	34.07	29.64	29.73	
5.1.5.5	Skirt Bolts	6.3	Est.	226.8	0	0	0.34	0.17	0.17	
5.1.5.6	Skirt Extension - 24 to 70	811.3	Calc.	296.3	0	0	248.58	293.24	293.24	
5.1.5.7	Ext. Attach Bolts	3.7	Est.	258.4	0	0	0.67	0.34	0.34	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.6	NUCLEAR SUBSYSTEM	10115.0		130.7	0	0	493.74	249.66	249.66	
5.1.6.1	Reactor	8565.0	Est.	130.7	0	0	439.54	219.77	219.77	
5.1.6.2	Internal Shield	1550.0	Est.	130.7	0	0	54.20	29.89	29.89	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.7	PROPELLANT FEED SYSTEM	2055.0		89.8	4.7	0.8	262.49	1093.27	1313.67	
5.1.7.1	Turbopump	800.0	Est.	59.0	0	0	5.01	20.07	20.07	
5.1.7.2	Pump Inlet Bellows	50.0	Est.	32.3	0	0	0.23	0.46	0.46	
5.1.7.3	Pump Discharge Line	350.9	Est.	101.1	38.8	2.3	6.09	250.81	254.45	
5.1.7.4	Turbine Inlet Line	450.4	Est.	113.1	16.7	-1.2	35.10	218.33	240.80	
5.1.7.5	Turbine Exhaust Lines	222.4	Est.	140.4	0	0	58.93	187.05	230.58	
5.1.7.6	Structural Coolant Line	170.4	Est.	93.6	23.1	8.2	1.17	1.16	1.41	
5.1.7.7	Diluent Line	10.9	Est.	197.3	31.3	0	0.04	0.11	0.14	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.8	PNEUMATIC SYSTEM	405.0		44.1	1.5	4.2	77.30	82.41	102.76	
5.1.8.1	Reservoirs	204.0	Est.	34.2	0	0	42.40	8.29	42.71	
5.1.8.2	Filters	15.0	Est.	71.8	-5.5	15.2	1.57	1.97	0.81	
5.1.8.3	Pressure Regulators	20.0	Est.	68.7	-3.7	10.2	2.72	3.39	1.36	
5.1.8.4	Valves	45.0	Est.	71.1	-5.1	14.1	5.08	6.36	2.59	
5.1.8.5	Plumbing	66.0	Est.	72.8	-6.1	16.9	6.03	7.61	3.17	
5.1.8.6	Disconnect Panel 1	35.0	Est.	8.0	23.3	-23.3	0.11	0.09	0.03	
5.1.8.7	Disconnect Panel 2	20.0	Est.	8.0	28.6	16.5	0.06	0.05	0.01	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.1.9	ELEC. AND INSTR.	340.0		81.1	9.7	-6.6	37.99	367.55	359.70	
5.1.9.1	Disconnects	74.0	Est.	0	0	0	0.51	0.45	0.16	
5.1.9.2	Wiring	142.0	Est.	94.6	14.0	-10.8	18.55	110.64	105.71	
5.1.9.3	Transducers	124.0	Est.	114.0	10.7	-5.8	15.58	115.60	112.13	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.2.1	ENGINE MATING	250.0	Est.	4.0	0	0	38.42	19.50	19.50	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.2.2	PROPELLANT CONTROL - PSV	195.0	Est.	0.3	0	0	0.94	10.59	10.59	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.2.3	ENGINE CONTROL - EPIC	300.0		-25.0	0	0	4.32	4.32	4.32	
5.2.3.1	ECS	150.0	Est.	-25.0	0	0	2.16	2.16	2.16	
5.2.3.2	PWR Supply and Sig. Cond.	150.0	Est.	-25.0	0	0	2.16	2.16	2.16	

DETAIL MASS PROPERTIES

CODE	DESCRIPTION	CURRENT WEIGHT	EST CALC ACT	CENTER OF GRAVITY			MOMENT OF INERTIA			NOTE
				X	Y	Z	ROLL	PITCH	YAW	
5.2.4	NUCLEAR CONTROL	730.0		270.9	0	0	31.00	708.49	708.49	
5.2.4.1	Poison Wires	100.0	Est.	135.0	0	0	11.42	14.52	14.52	
5.2.4.2	Poison Wire Removal	630.0	Est.	292.5	0	0	19.58	231.89	231.89	

MASS PROPERTIES CHANGE ANALYSIS

CHANGE IDENTIFICATION OR NOTE NUMBER	CODE	TOTAL WEIGHT CHANGE POUNDS	REMARKS

NERVA ENGINE REFERENCE DATUM

75K NERVA COMPONENTS

II. 75K NERVA Engine Steady State
Values at 100%, 57.7%, and 20% Thrust

M E M O R A N D U M

18 September 1968
7830:M3854:DLP:rja

TO: S. A. Varga

FROM: D. L. Petite

SUBJECT: 75K NERVA Engine Steady-State Values (100%, 57.7% and 20% Thrust)

COPIES TO: J. H. Altseimer, J. J. Beereboom, D. Buden, W. W. Madsen,
B. Mandell, R. J. Pickering,
NERVA Engine Analysis Group personnel

REFERENCE: (a) NERVA Program Requirements Document, dated 5 June 1968,
page 18, subparagraph (2)

ENCLOSURE: (1) 100% Thrust Design Steady-State Values
(2) 57.7% Thrust Design Steady-State Values
(3) 20% Thrust Design Steady-State Values

Enclosure (1) contains the design steady-state point values at 100% thrust with $P_c = 450$ psia and $T_c = 4500^\circ R$. Enclosure (2) contains the steady-state point values at $P_c = 260$ psia and $T_c = 4500^\circ R$; this point lies on the tie tube temperature limit line. Enclosure (3) contains the engine state points at $P_c = 90$ psia and $T_c = 1500^\circ R$; this point lies on the tie tube temperature limit line. It is also at the 20% engine thrust limit as specified in Reference (a) as a minimum required engine operating thrust for 10 minutes. At this lower value of P_c and T_c the turbine U/C_o value ~ 0.3936 exceeds the range of the turbine efficiency curve. The efficiency value used was extrapolated to give an efficiency of 30%. Please supply Systems Analysis with an extended curve of η_T vs. U/C_o for values of U/C_o at least to 0.40.

D. L. Petite

Approved:

D. Buden, Manager
Systems Analysis Section

CLASSIFICATION CATEGORY

CLASSIFYING OFFICER	DATE
	9/19/68

M E M O R A N D U M

26 September 1968
7830:M3854A
DLP:rja

TO: S. A. Varga

FROM: D. L. Petite

SUBJECT: 75K NERVA Engine Steady-state Values (100%, 57.7% and 20% Thrust)

COPIES TO: J. H. Altseimer, J. J. Beereboom, D. Buden, W. W. Madsen,
B. Mandell, R. J. Pickering, NERVA Engine Analysis Group

REFERENCE: (a) Memo 7830:M3854, D. L. Petite to S. A. Varga, dated
18 September 1968, same subject

ENCLOSURE: (1) 100% Thrust Design Steady-State Values (amended)
(2) 57.7% Thrust Design Steady-State Values (amended)
(3) 20% Thrust Design Steady-State Values (amended)

The three enclosures transmitted herewith supersede the three enclosures in Reference (a).

D. L. Petite
NERVA Engine Analysis Group

Approved:

D. Buden, Manager
Systems Analysis Section

CLASSIFICATION CATEGORY	
<i>Unclassified</i>	
CLASSIFYING OFFICER	DATE
<i>[Signature]</i>	<i>7/25/88</i>

DATE 9-18-68

MVA ENGINE

Engine: $P_c = 450$; $T_c = 4500$; Thrust = 75,529 lbf; Impulse = 824.55 sec;

Power Level = 1554 Mw; TPCV = 44.75 degrees

Enclosure (1)
7830:M3854-A

Turbomachinery:

	Pump	Turbine
Shaft Speed, rpm	18,759	18,759
Power, Mw	5.63	5.63
Efficiency, percent	68.89	49.9
NPSP, psi	4.5	-

State Point Description	Flow lb/sec	(PS) Pressure (PO) Static psia	Total psia	Temp. °R	Heat Flux Btu/hr
Dewar Outlet	91.6	30	30	40	
Pump Inlet	91.6	29.5	29.5	40	
Pump Leakage	0.574	--	937.9	53.4	
Pump Outlet	91.04	937.9	937.9	53.4	
Structure Support Inlet	5.3	--	908	53.4	
Structure Bypass Inlet	15.7	--	908	53.4	
Bolt Coolant	2.99	--	701	53.4	
Skirt Inlet	66.8	903	903	53.4	
Skirt Outlet	66.8	--	891	83.2	7.76
Nozzle Inlet	66.8	--	891	83.2	32.91
Nozzle Outlet	66.8	701	701	205	
Reflector Inlet	66.9	701	701	199	27.34
Reflector Outlet	66.9	649	649	298	
Periph. Shield Outlet	66.9	638	638	307	
Structure Outlet	5.3	638	638	975	
Support Plate Inlet	88.1	638	638	301	
Central Shield Inlet	88.1	629	629	307	
Core Inlet	88.1	617	617	312	1499
Core Outlet	88.1	450	450	4500	
Nozzle Chamber	86.8	238.5	450	4446	
Diluent Bleed Inlet	2.9	--	701	199	
Diluent Bleed Outlet	2.9	--	449	--	
Hot Bleed	1.33	--	450	4446	
Mixing Ring Outlet	4.24	--	431.5	--	
TPCV Inlet	4.24	--	421	1660	
Turbine Inlet	4.24	169	317	1660	
Turbine Outlet	4.81	--	42.4	1306	
Skirt Extension Inlet	4.81	--	42.0	1306	
Skirt Extension Outlet	4.81	18.5	34.9	1577	

Engine: $P_c = 260$; $T_c = 4500$; Thrust = 43,568 lbs; Impulse = 827.9 sec;

Power Level = 893 Mw; TPCV = 36.84 degrees

 Enclosure (2)
 7830:M3854A

Turbopump:

	<u>Pump</u>	<u>Turbine</u>
Shaft Speed, rpm	<u>13,892</u>	<u>13,892</u>
Power, Mw	<u>1.85</u>	<u>1.85</u>
Efficiency, percent	<u>66.3</u>	<u>44.3</u>
MRSP, psi	<u>4.8</u>	<u>--</u>

State Point Description	Flow lb/sec	(PS) Pressure Static psia	(PO) Total psia	Temp. °R	Heat Flux Btu
Deaer. Outlet	52.62	30	30	40	
Pump Inlet	52.62	29.8	29.8	39.2	
Pump Leakage	0.37		532	47.8	
Pump Outlet	52.25		532	47.8	
Structure Support Inlet	3.56		518	47.8	
Structure Bypass Inlet	10.54		518	47.8	
Bolt Coolant	1.75		412.3	47.8	
Skirt Inlet	36.4		518	47.8	4.71
Skirt Outlet	36.4		514.6	75.4	
Nozzle Inlet	36.4		514.6	75.4	21.92
Nozzle Outlet	36.4		412.3	227.4	
Reflector Inlet	36.46		412.3	220.7	15.77
Reflector Outlet	36.46		383.1	325.7	
Periph. Shield Outlet	36.46		377	334.6	
Structure Outlet	3.56		377	954.4	
Support Plate Inlet	50.57		377	318.3	
Central Shield Inlet	50.57		371.6	324.3	
Core Inlet	50.57		364.5	329.3	
Core Outlet	50.57	260	260	4500	
Nozzle Chamber	50.0	137.8	260	4446	
Diluent Bleed Inlet	1.65	412	412.3	220.7	
Diluent Bleed Outlet	1.65	259	259	---	
Hot Bleed	0.48	260	260	4500	
Mixing Ring Outlet	2.13	---	254	1272	
TPCV Inlet	2.13	250	250.5	1274	
Turbine Inlet	2.13	74	140	1274	
Turbine Outlet	2.5	20	20	1038	
Skirt Extension Inlet	2.5	19	19	1038	
Skirt Extension Outlet	2.5	5	10	1338	

DATE 9-18-68

NERVA ENGINE
(20% Thrust)Enclosure (3)
7830;M3854AOperating: $P_c = 90$; $T_c = 1500$; $\dot{m}_{\text{fuel}} = 13,704$ lbs; Impulse = 420.2 sec;Inlet Level = 170 ft; $T_{\text{inlet}} = 38$ degrees

Turbopump:

	Pump	Turbine
Shaft Speed, rpm	8,400	8,400
Power, Mw	0.29	0.29
Efficiency, percent	67	30*
NPSP, psi	4.9	---

State Point Description	Flow lb/sec	(PS) Static psia	(PO) Total psia	Temp. °R	Heat Flux Mw
Nozzle Outlet	32.61	30	30	40	
Pump Inlet	32.61	29.9	29.9	40	
Pump Leakage	0.178		163.6	43	
Pump Outlet	32.43		163.6	43	
Structure Support Inlet	1.48		157.7	43	
Structure Bypass Inlet	4.39		157.7	43	
Bolt Coolant	1.03		131.3	43	
Skirt Inlet	26.53		153.6	43	
Skirt Outlet	25.53		151.4	51	0.774
Nozzle Inlet	25.53		151.4	51	
Nozzle Outlet	25.53		131.3	59	3.04
Reflector Inlet	25.31		131.3	58	
Reflector Outlet	25.31		126.1	73	
Periph. Shield Outlet	31.19		124.6	75	
Structure Outlet	1.48	124	124.6	434.2	
Support Plate Inlet	31.19	124	124.6	81	
Central Shield Inlet	31.19	123	123.2	84	
Core Inlet	31.19	120	120.9	85	
Core Outlet	31.19	90	90	1500	163.69
Nozzle Chamber	30.92	47.5	90	1480	
Diluent Bleed Inlet	1.41		131.3	58	
Diluent Bleed Outlet	1.41	--	89	--	
Hot Bleed	0.11	--	90	1500	
Mining Ring Outlet	1.56	--	88.2	239	
TECV Inlet	1.56	87.2	87.2	240	
Turbine Inlet	1.56	26.5	50	240	
Turbine Outlet	1.74	1.16	1.16	161	
Skirt Extension Inlet	1.74	1.0	1.0	161	
Skirt Extension Outlet	1.74	0.42	0.8	361	

* Extrapolated value for $U/C_o = 0.3936$

III. Mass Quality of LH_2 at NERVA
Turbopump Inlet

M E M O R A N D U M

12 September 1968
7830:M3844:CJN:rja

TO: S. A. Varga

FROM: C. J. Naretto

SUBJECT: Mass Quality of LH_2 at NERVA Turbopump Inlet

COPIES TO: J. J. Beereboom, D. Buden, W. W. Madsen, B. Mandell, W. E. Campbell,
R. J. Pickering, NERVA Engine Analysis Group personnel, I. L. Odgers

REFERENCE: (a) Memo 7810:0538M, S. A. Varga to D. Buden, dated
21 August 1968, subject: Operating Line with
15% Vapor Pump Inlet

ENCLOSURE: (1) Steady-State Summary at Design Point
(2) Steady-State Summary at Gate-Valve-Wide-Open Point

Steady-state operating points were run on the D31201 NETAP incorporating the turbopump characteristics of Reference (a). Thus, the NERVA engine operation with these data was determined. Pertinent system parameters at the design operating point are summarized in Enclosure (1). Enclosure (2) summarizes the steady-state conditions when both TPCV's are opened to 80°.

A calculation was made to determine the mass quality of the LH_2 at the pump inlet at the steady-state design point conditions, assuming 15% volumetric quality at a pressure of 30 psia. The mass quality was approximately 0.07% at this point. Operation at the gate-valve-wide-open point would not increase this value appreciably as the pump inlet pressure decreased only 0.28 psia. This calculation was made because the TPA curves supplied were for only 15% vapor and if, at the TPCV's open position, this vapor percent increased significantly, they would no longer be valid. However, as noted above, the vapor percent did not change enough to make the supplied TPA curves invalid.

C. J. Naretto
NERVA Engine Analysis Group
Systems Analysis Section

Approved:

D. Buden, Manager
Systems Analysis Section

CLASSIFICATION CATEGORY	
<i>Confidential</i>	
CLASSIFYING OFFICER	DATE
<i>[Signature]</i>	<i>9/12/68</i>

11 Sept. 1968

Enclosure (1)
7830:M3844

Region: $P_c = 450$; $T_c = 4500$; $\dot{m}_{\text{core}} = 75,563$ lbs; Impulse = 825.0 sec;

Power Level 1555 Mw; $T_{\text{cool}} = 46.58$ degrees

Turbopump:

	Pump	Turbine
Shaft Speed, rpm	19,278	19,278
Power, Mw	5.925	5.925
Efficiency, percent	65.60	50.99
NPSP, psi		

State Point Description	Flow lb/sec	(PS) Pressure (PO) Static psia	Total psia	Temp. °R	Heat Flux Mw
Dewar Outlet	91.63		30.0	40.8	
Pump Inlet	91.63		29.47	39.27	
Pump Leakage	0.49		29.47	39.27	
Pump Outlet	91.14		941.19	54.47	
Structure Support Inlet	21.24		910.75	54.73	
Structure Bypass Inlet			910.75	54.73	
Bolt Coolant	2.99		893.52	84.08	
Skirt Inlet	66.91		905.58	54.73	
Skirt Outlet	66.91		893.52	84.08	
Nozzle Inlet	66.91		893.52	84.08	
Nozzle Outlet	66.91		701.22	206.12	
Reflector Inlet	66.91		701.22	200.78	
Reflector Outlet	66.91		649.69	299.61	
Periph. Shield Outlet	66.91		638.69	307.98	
Structure Outlet	21.24		638.70	287.10	
Support Plate Inlet	88.15		638.70	302.75	
Central Shield Inlet	88.15		629.24	308.63	
Core Inlet	88.15		617.11	313.52	
Core Outlet	88.15		463.57	4476.5	
Nozzle Chamber	86.78		450.16	4501.5	
Diluent Bleed Inlet	2.99		701.22	200.78	
Diluent Bleed Outlet	2.99				
Hot Bleed	1.37				
Mixing Ring Outlet					
TPCV Inlet					
Turbine Inlet	4.36		326.22	1660.0	
Turbine Outlet (mix)	4.85		43.26	1166.4	
Skirt Extension Inlet	86.78		449.14	4124.9	
Skirt Extension Outlet	86.78		449.14	4117.4	

DATE 11 Sept. 1968

Enclosure (2)
7830;M3844

Average

Engine: $P_c = 553$; $T_c = 4500$; Thrust = 92,907 lb; Impulse = 823.1 sec;

Power Level = 1909 kW; TPOV = 80.0 degrees (wide-open)

Probopid:

Shaft Speed, rpm	21,861	21,861
Power, kW	9.424	9.424
Efficiency, percent	64.56	55.82
WSP, psi		

State Point Description	Flow lb/sec	(PS) Pressure (PS) Static Total psia psia	Temp. °F	Heat Flux Btu/hr
Dewar Outlet	113.94	30.0	40.0	
Pump Inlet	113.94	29.19	39.27	
Pump Leakage	0.55	29.19	39.27	
Pump Outlet	113.39	1181.4	58.45	
Structure Support Inlet	24.90	1135.6	58.90	
Structure Bypass Inlet		1135.6	58.90	
Bolt Coolant	3.70	1109.0	88.30	
Skirt Inlet	84.24	1127.6	58.90	
Skirt Outlet	84.24	1109.0	88.30	
Nozzle Inlet	84.24	1109.0	88.30	
Nozzle Outlet	84.24	857.04	201.12	
Reflector Inlet	83.67	857.04	196.32	
Reflector Outlet	83.67	792.74	292.53	
Periph. Shield Outlet	83.67	778.89	300.75	
Structure Outlet	24.90	778.90	293.14	
Support Plate Inlet	108.57	778.89	298.84	
Central Shield Inlet	108.57	767.23	304.68	
Core Inlet	108.57	752.37	309.53	
Core Outlet	108.57	568.72	4473.1	
Nozzle Chamber	106.59	552.45	4498.6	
Diluent Bleed Inlet	4.27	857.04	196.32	
Diluent Bleed Outlet	4.27			
Hot Bleed	1.98			
Mixing Ring Outlet				
TPOV Inlet				
Turbine Inlet	6.25	467.40	1659.7	
Turbine Outlet (mix)	6.80	59.68	1156.7	
Skirt Extension Inlet	106.59	551.24	4124.5	
Skirt Extension Outlet	106.59	551.24	4117.2	

IV. Propellant Tank Pressurization
Requirements for One-Way
Synchronous Orbit Mission

2815

TO: K. Sato 9 September 1968
FROM: S. E. Colucci SEC:res 0735:2815
Ext. 7172:Bldg. 2015
SUBJECT: Propellant Tank Pressurization Requirements for One Way
Synchronous Orbit (W.O. 1186-21-110)
COPIES: J. J. Beereboom, O. J. Demuth, M. J. Ditore, R. S. Fairall,
G. D. Hart, I. L. Odgers, T. Pasternak, R. J. Pickering,
R. L. Rishel, D. W. Wittlesey, S. A. Varga, File
REFERENCES: a. Memo S. A. Varga - K. Sato, Thermal Analysis Support
in Definition of NERVA Program Requirements, 7810:0534M,
21 August 1968.
b. Properties of Principal Cryogenics, AGC public PRA-SA-DSR
8-20-64
ENCLOSURES: Table I - Engine Operational Period Flow Rates to Maintain
Propellant Tank Pressure of 30 psia.
Table II - Coast Period Propellant Tank Flow Rates to Maintain
a Pressure of 30 psia.
Figure 1 - Propellant Tank Heating Rates for the One Way
Synchronous Orbit Mission
Appendix A - Derivation of Equations for Tank Pressurization
or Venting for the Synchronous Orbit Mission

The purpose of this memorandum is to present the results of a study performed to determine propellant tank pressurization gas requirements for the one way synchronous orbit mission.

A simplified thermal analysis was performed to determine the liquid hydrogen tank pressurization gas requirements, as requested in Reference a, to maintain the tank at a constant pressure of 30 psia. The results are summarized on Table I for the steady state engine operational periods when liquid extraction and heating rates are constant. These were: suborbit start, 0.695 lb/sec, depart from earth orbit, 0.676 lb/sec, and enter synchronous orbit, 0.642 lb/sec. During the coast periods propellant heating rates are variable. Initially the volume of propellant gas evaporated by heating exceeds the volume of liquid extracted for reactor cooling and the tank must be vented to prevent overpressurization. When the heating rates fall below a certain level the propellant tank venting must cease and pressurizing with bleed gas commence. This occurs at 4300 seconds for the earth orbit coast and at 660 seconds for the synchronous coast periods. These results are tabulated versus time on Table II, the equations for both conditions are derived in the appendix.

Assumptions used in these calculations were:

1. Propellant tank contains liquid para-hydrogen with properties identical to those shown on Reference b.
2. Liquid extraction rates from the tank are constant and averaged for the durations shown on Table I.

3. Hydrogen Bleed gas for tank pressurization available at 640 psia and 270°R.

4. Propellant tank contains a pressure relief valve (30 psia) to prevent overpressurization during early part of coast periods due to continual heat input.

5. Propellant nuclear heating rate of 200 Btu/sec, constant during engine operational periods and variable during coast periods. Constant solar heating rates of 0.265 Btu/sec were used. Coast period total heating rates used for this study are shown on Figure 1.

6. Thermal energy lost to tank walls by bleed gas is negligible and may be ignored for this study.

S. E. Colucci

S. E. COLUCCI, Sr. Engr. Spec.
Aerophysics - Dept. 0735

TABLE I

ENGINE OPERATIONAL PERIOD FLOW RATES TO
MAINTAIN PROPELLANT TANK PRESSURE OF 30 PSIA

	Lbs Prop. At Start <u>lb H₂</u>	Total Propellant Used <u>lb H₂</u>	Time θ <u>sec</u>	Liquid H ₂ $\dot{m}_L$ <u>lb/Sec</u>	Bleed Gas $\dot{m}_g$ <u>lb/Sec</u>
1. Suborbit Start	207,908	112,302	1227	91.525	.695
2. Coast in Earth Orbit	95,606	3,490	21,600	.16157	Variable see Table II
3. Depart from Earth Orbit	92,116	37,103	414	89.621	.676
4. Coast to Synchronous Orbit	55,013	5,208	18,000	.28933	Variable see Table II
5. Enter Synchronous Orbit	49,805	47,797	554	86.276	.642
6. Disposal	2,008	-	-		

TABLE II

COAST PERIOD PROPELLANT TANK FLOW RATES TO
MAINTAIN A PRESSURE OF 30 PSIA

<u>Coast to Earth Orbit</u>				<u>Coast to Synchronous Orbit</u>		
<u>Time</u> <u>sec</u>	<u>q_T</u> <u>Btu/sec</u>	<u>$\dot{m}_L^*$</u> <u>lb/sec</u>	<u>$\dot{m}_g^*$</u> <u>lb/sec</u>	<u>q_T</u> <u>Btu/sec</u>	<u>$\dot{m}_L$</u> <u>lb/sec</u>	<u>$\dot{m}_g^*$</u> <u>lb/sec</u>
1	35	.16157	-.1475	27	.28933	-.1073
10	28	△	-.1167	20	△	-.0765
20	24		-.0991	15.5		-.0567
50	17		-.0683	11		-.0369
100	13		-.0507	7.6		-.0220
500	6.2		-.0208	3.2		-.0026
660	5.5		-.0178	2.61		0.0000
800	4.7		-.0143	2.22		.000431
1000	4.1		-.0116	1.92		.000765
2000	3.0		-.0068	1.20		.001563
3000	1.8		-.0015	.88		.001919
4300	1.46		0.0000	.69		.002133
5000	1.24		.000246	.60		.002226
7000	.95		.000366	.50		.002341
10,000	.70		.000844	.39		.002462
18,000	.49		.001080	.30	▽	.002564
21,600	.45	▽	.001122			

* Negative sign - Saturated H₂ vapor vent rates

Positive sign - Bleed gas (640 psia and 270°R) pressurization rates

PROPELLANT TANK HEATING RATES FOR ONE WAY SYNCHRONOUS ORBIT MISSION

APPENDIX A

DERIVATION OF EQUATIONS FOR TANK PRESSURIZATION OR VENTING FOR THE SYNCHRONOUS ORBIT MISSION

During those periods of steady state engine operation, the propellant tank energy change due to liquid extraction exceeds the energy addition by heating and bleed gas is required to maintain the tank pressure at 30 psia. However, during the coast periods a different condition exists. Initially, the volume of gas generated by heat addition exceeds the volume of liquid extracted for reactor cooling. To prevent the tank pressure from increasing, the excess vapor in the ullage space is vented to maintain a constant pressure of 30 psia. The venting continues until the volume of liquid extracted equals the vapor volume generated by both nuclear and solar heating. Thereafter hot bleed gas must again be added to maintain the tank pressurization. The equations used for the two conditions mentioned, pressurization with bleed gas (equation 1) and tank venting (equation 2) are derived below.

APPENDIX A

1. Steady State Nuclear Heating Tank Pressurization with Bleed Gas

LIQUID VOLUME:

$$V_L = V_L^0 - \frac{\dot{m}_L d\theta}{\rho_L} - \frac{q d\theta}{H_v \rho_L} - \frac{(h_g - h_v) \dot{m}_g d\theta}{H_v \rho_L} + \frac{p \dot{m}_L d\theta}{J H_v \rho_L \rho_L}$$

ULLAGE VOLUME

$$V_u = V_u^0 + \underbrace{\frac{\dot{m}_g d\theta}{\rho_v}}_{\text{VOLUME ADDED BY BLEED}} + \underbrace{\frac{q d\theta}{H_v \rho_v}}_{\text{VOLUME VAPOR ADDED BY NUCLEAR HEAT EVAP LIQUID}} + \underbrace{\frac{(h_g - h_v) \dot{m}_g d\theta}{H_v \rho_v}}_{\text{ULLAGE VAPOR GENERATED FROM HIGH TEMP BLEED GAS BY LIQUID EVAP.}} - \underbrace{\frac{p \dot{m}_L d\theta}{J H_v \rho_L \rho_L}}_{\text{REPRESENTS WORK DONE BY ULLAGE TO EJECT LIQ.}}$$

$$V_T = V_L + V_u = V_L^0 + V_u^0 + \frac{\dot{m}_g d\theta}{\rho_L} + \frac{\dot{m}_g (h_g - h_v) d\theta}{H_v (\rho_v - \rho_L)} - \frac{\dot{m}_L d\theta}{\rho_L} + \frac{q d\theta}{H_v (\rho_v - \rho_L)} - \frac{p \dot{m}_L d\theta}{J H_v \rho_L (\rho_v - \rho_L)}$$

SINCE $V_T = V_L^0 + V_u^0$:
(SYSTEM VOLUME UNCHANGED)

$$\frac{\dot{m}_g}{\rho_v} + \frac{\dot{m}_g (h_g - h_v)}{H_v (\rho_v - \rho_L)} - \frac{\dot{m}_L}{\rho_L} + \frac{q}{H_v (\rho_v - \rho_L)} - \frac{p \dot{m}_L}{J H_v \rho_L (\rho_v - \rho_L)} = 0$$

$$\frac{\dot{m}_g}{\rho_v} \left[1 + \frac{(h_g - h_v) (\rho_L - \rho_v)}{H_v \rho_L} \right] = \frac{1}{\rho_L} \left[\dot{m}_L + \frac{p \dot{m}_L (\rho_L - \rho_v)}{J H_v \rho_v \rho_L} - \frac{q (\rho_L - \rho_v)}{H_v \rho_v} \right]$$

$$\dot{m}_g \frac{\left[\dot{m}_L \left[H_v + \frac{p}{J \rho_L} \left(\frac{\rho_L}{\rho_v} - 1 \right) \right] - q \left(\frac{\rho_L}{\rho_v} - 1 \right) \right]}{\left[\frac{H_v \rho_L}{\rho_v} + (h_g - h_v) \left(\frac{\rho_L}{\rho_v} - 1 \right) \right]} \quad \text{EQ (1)}$$

2. Coast Period Tank Pressurization by Venting Excess Vaporization

LIQUID VOLUME:

$$V_L = V_L^0 - \frac{\dot{m}_L d\theta}{\rho_L} - \frac{q d\theta}{H_v \rho_L} + \frac{p \dot{m}_L d\theta}{J H_v \rho_L \rho_v} + \frac{p \dot{m}_g d\theta}{\rho_v \rho_L J H_v}$$

ULLAGE VOLUME:

$$V_u = V_u^0 - \frac{\dot{m}_g d\theta}{\rho_v} + \frac{q d\theta}{H_v \rho_v} - \frac{p \dot{m}_L d\theta}{J H_v \rho_L \rho_v} - \frac{p \dot{m}_g d\theta}{J H_v \rho_v \rho_v}$$

VOLUME GAS VAPOR VENTED
 ULLAGE VOL. ADDED BY HEATING & EVAP. LIQ
 REPRESENTS WORK DONE BY ULLAGE TO EJECT LIQUID
 REPRESENTS WORK DONE BY ULLAGE TO VENT VAPOR

$$V_T = V_L + V_u = V_L^0 + V_u^0 + \frac{q d\theta}{H_v (\rho_v - \rho_L)} - \frac{p \dot{m}_L d\theta}{J H_v \rho_L (\rho_v - \rho_L)} - \frac{p \dot{m}_g d\theta}{J H_v \rho_v (\rho_v - \rho_L)} - \frac{\dot{m}_g d\theta}{\rho_v} - \frac{\dot{m}_L d\theta}{\rho_L}$$

SINCE $V_T = V_L^0 + V_u^0$
TANK VOLUME UNCHANGED,

$$\frac{\dot{m}_g}{\rho_v} \left[1 + \frac{p}{J H_v \rho_L} \left(\frac{\rho_L}{\rho_v} - 1 \right) \right] = \frac{q}{H_v \rho_v} \left(\frac{\rho_L}{\rho_v} - 1 \right) - \frac{\dot{m}_L}{\rho_L} \left[1 + \frac{p}{J H_v \rho_L} \left(\frac{\rho_L}{\rho_v} - 1 \right) \right]$$

$$\dot{m}_g = \frac{\left[\frac{q \left(\frac{\rho_L}{\rho_v} - 1 \right)}{H_v \rho_v} - \frac{\dot{m}_L}{\rho_L} \left\{ \frac{H_v \rho_L + \frac{p}{J} \left(\frac{\rho_L}{\rho_v} - 1 \right)}{H_v \rho_v} \right\} \right]}{\frac{1}{\rho_v} \left[\frac{H_v \rho_L + \frac{p}{J} \left(\frac{\rho_L}{\rho_v} - 1 \right)}{H_v \rho_v} \right]}$$

$$\dot{m}_g = \frac{\left[\frac{q \left(\frac{\rho_L}{\rho_v} - 1 \right)}{H_v \rho_v} - \frac{\dot{m}_L}{\rho_L} \left\{ H_v + \frac{p}{J \rho_L} \left(\frac{\rho_L}{\rho_v} - 1 \right) \right\} \right]}{\left[\frac{H_v \rho_L}{\rho_v} + \frac{p}{J \rho_v} \left(\frac{\rho_L}{\rho_v} - 1 \right) \right]}$$

EQ (2).

NOMENCLATURE:

V_T	- total tank volume, ft^3
V_L^o	- volume of liquid in tank, superscript denotes previous time
V_u^o	- volume of vapor in ullage space, superscript denotes previous time
$\dot{m}_L$	- liquid flow rate out of tank, lb/sec
$\dot{m}_g$	- gas flow rate into or out of tank, lb/sec
H_v	- heat of vaporization of liquid hydrogen at 30 psia, 184 Btu/lb
h_g	- enthalpy of bleed gas at 640 psia and 270°R, 792 Btu/lb
h_v	- enthalpy of saturated hydrogen vapor in ullage space at 30 psia, 85 Btu/lb
θ	- time, seconds
ρ_v	- saturated vapor density in tank ullage space, 4.13 lb/ft^3
ρ_L	- saturated liquid density, 0.165 lb/ft^3
ρ_L/ρ_v	- ratio of liquid to vapor density at 30 psia, 25:1
q_{NR}	- nuclear heating rate into propellant, Btu/sec
q_{SR}	- solar heating rate into propellant, Btu/sec
q	- $q_{NR} + q_{SR}$ - total propellant heating rate, Btu/sec
p	- tank pressure maintained at 30 psia

V. Thrust Nulling Report

THRUST NULLING REPORT

Prepared by:

G. D. Hart

Approved By:

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I. INTRODUCTION

The requirements for thrust nulling capability following operation of the NERVA Engine at steady-state full-power thrust is stated in the PRD, Section III.B.1.d., as follows:

"During the shutdown transient, impulse lost by using propellant at less than full Isp shall be minimized. However, after (tbd) seconds of coolant flow, the system should provide for either using the available impulse or for dissipating the coolant flow with zero thrust. The user shall have the option of either using the available impulse or counteracting the thrust."

A study has been performed to identify a number of systems which could potentially satisfy this requirement. The results of this study are presented in this report. See Table I for a comparison of the different systems which were considered. Appendices A and B present the supporting studies performed by the Systems Analysis Section and the Thrust Chamber Section, respectively.

Figures 1 and 2 present parametric curves of integrated decay heat and quantities of coolant required versus time after SCRAM. Table II gives a summary of the impulse generated during cooldown periods of the reference missions. This is shown separately for normal cooldown when thrust nulling may be required and for terminal cooldown when thrust nulling may not be required.

II. ENGINE COOLING SCHEMES

A. NORMAL FLOW PATH

This is the same cooling path that is used during steady-state engine operation. Parallel flows of coolant from the structural support system and the nozzle tube - reflector are mixed and the total flow goes through the core.

B. STEM COOLING

An alternate scheme for cooling the engine is shown in Figure 1 of Appendix A. The flow to the nozzle tubes is blocked off by a valve and all of

II. B. Stem Cooling (Continued)

the coolant goes to the structural support system. However, coolant may be required intermittently through the blocked off path to prevent overheating. All of the reactor coolant is radially exhausted through auxiliary nozzles.

C. SEPARATE STRUCTURAL SUPPORT COOLING

This scheme is shown in Figure 2 of Appendix A. As in the normal flow path, the coolant flows parallel through the structural support system and the nozzle tube-reflector path. However, in this scheme the structural support exit flow is discharged overboard rather than passing through the core as it does during steady-state operation.

III. CONCEPTS FOR THRUST NULLING

A. RADIAL DISCHARGE SYSTEMS

One method of obtaining zero thrust is the balanced radial discharge of reactor coolant flow. The six schemes that were considered are discussed below.

1. Stem Coolant Flow

In this scheme, the reactor is cooled through the structural support system, and through the nozzle tube-reflector path intermittently as required to prevent overheating. The total flow is exhausted radially as shown in Figure 1 of Appendix A.

2. Mechanical Choked Throat

This concept is shown in Figure 10 of Appendix B. The nozzle throat is plugged and the core discharge coolant exits through radial ports in the plenum chamber.

III.A. Radial Discharge Systems (continued)

3. Throat Choke and Front Ports

In this scheme the nozzle throat is plugged. The coolant which exits from the core in the normal path travels back through a center hole in the core. Then it is exhausted radially as shown in Figure 11 of Appendix B.

4. Exit Cone Closure

This scheme is shown in Figure 12 of Appendix B. The main nozzle discharge is plugged off and the coolant from the core is exhausted radially through ports in the plenum chamber.

5. Exit Plane Radial Bleed

In this concept, shown in Figure 16 of Appendix B, the main nozzle discharge is partially blocked. As the coolant gases escape they are deflected in a radial direction.

6. Full Flow Cycle

This configuration is shown in Figure 11 of Appendix A. Coolant flows through the structural support system, and intermittently through the nozzle tube-reflector path if required. Then the total coolant can flow to the turbine if discharge pressures greater than tank pressure are required or it can be bypassed around the turbine if tank driving pressure is sufficient. The dome inlet flow is shut off by a two-way valve and flow is discharged radially. The collection system for radial discharge is an inherent part of this system, whereas the stem coolant concept for the bleed cycle will require reactor design changes.

B. ENGINE COUNTERBALANCE SYSTEMS

Another method of obtaining zero thrust is to counterbalance the thrust from the main nozzle with an equal and opposite thrust. Five concepts are discussed below:

III. B. Engine Counterbalance Systems (continued)

1. Separate Structural Support Cooling

This concept is shown in Figure 2 of Appendix A. The structural support coolant is exhausted at the dome end of the reactor to counterbalance the thrust from the core discharge coolant.

2. Split Flow

In this concept the subsonic flow from the core discharge is split so that a sufficient amount is exhausted through auxiliary nozzles in the plenum chamber to counterbalance the thrust from the main nozzle. This concept is illustrated in Figure 7 of Appendix B.

3. Vortex Choked Flow

This concept is shown in Figure 9 of Appendix B. It is the same as No. 2 above except that an additional gas flow is injected into the plenum chamber to reduce the effective nozzle throat area. This reduces the requirements on the counterbalance system.

4. Balanced Thrust End Plate

This concept is shown in Figure 17 of Appendix B. Part of the coolant discharges through the center of a plate at the main nozzle exit. The plate also deflects enough of the exhaust gas to give a counterbalance thrust.

5. Intra-Skirt Radial Bleed

This concept is shown in Figure 18 of Appendix B. A portion of the main nozzle flow is diverted at an expansion ratio of 25:1 by rotating plates. This diverted flow produces a counterbalance thrust to the main nozzle thrust.

C. NON-ENGINE COUNTERBALANCE SYSTEMS

Other systems which are part of the stage but not of the nuclear engine may be used to produce counterbalance thrust. The thrust nulling requirements

III. C. Non-Engine Counterbalance Systems (continued)

for Mission B, shown in Table III are used as the basis for typical weight estimates in the following two systems:

1. Auxiliary Chemical Propulsion Unit

The weight estimate for the auxiliary chemical propulsion unit considered is shown in Table IV. A less optimistic chemical system is also discussed in Enclosure (2) of Appendix B.

2. Tank Venting

The weight estimated for tank venting fluid is shown in Table V. Weights are shown for three (3) different fluid conditions. If propellant tank heating conditions require venting of the tank, then it could possibly be scheduled so that the thrust could be utilized for counterbalancing. This could reduce the penalty of the thrust nulling system.

IV. CONCLUSIONS

The objective of this study was to conceptually define a number of potential systems to satisfy the thrust nulling requirement. Twelve concepts have been presented in this report that will potentially meet this requirement the reader is referred to the appendices for an analytical discussion of these concepts. None of the eight mechanical concepts in Appendix B present an attractive solution to this problem. In view of this and the large weight penalties associated with auxiliary chemical propulsion units or tank venting systems, the following recommendations are made:

(1) Mission oriented studies should be performed to define methods to maximize the utilization of the thrust during the shutdown/cooldown period with coolant flowing through the normal path. This will eliminate or reduce the thrust nulling requirement and its associated penalties.

IV. Conclusions (Continued)

(2) The maximum capabilities of the stem cooling concept have been defined by this study, when the reactor constraints of these concepts are defined in detail the limitation of this approach will be determined. WANL has been requested to review the stem-cooling results presented in this report and to define in detail the reactor constraints, however, to date this action has not been completed. Based upon analysis performed to date, if tank pressure alone is used to supply coolant, the null thrust option cannot be used with stem cooling until several minutes after scram. However, nuclear subsystem constraints may extend the period significantly. The required supply pressure and flow rate have been defined as a function of time after SCRAM in Appendix A. A system study should be performed to define methods of meeting these coolant pressure and flow rate requirements.

Figure 1

D. 3-CR LS10 DIETZGEN GRAPH PAPER
SEMI LOGARITHMIC
3 CYCLES X 10 DIVISIONS PER INCH

EUGENE DIETZGEN CO
MADE IN U.S.A.

(16,623 lb.
at 100,000 Sec)

Figure 2

TABLE I

COMPARISON OF CONCEPTS FOR THRUST NULLING	TIME AFTER SCRAM AT WHICH THRUST NULLING MAY BE INITIATED (FOLLOWING FIRST BURN OF 1227 SEC. IN MISSION B)	MECHANICAL WEIGHT OF SYSTEM	PROPELLANT REQUIRED DURING THRUST NULLING (MISSION B)	SUM
A. RADIAL DISCHARGE SYSTEMS				
1. Stem coolant flow (30 psia tank pressure)	500 sec.	550	6,900	7,450
2. Mechanical choked throat	0	734	8,150	8,884
3. Choke throat and front ports	0	736	8,150	8,886
4. Exit core closure	0	4,400	8,150	12,550
5. Exit plane radial bleed	0	3,865	8,150	12,015
6. Full flow cycle	0	150	12,225	12,375
B. ENGINE COUNTER-BALANCE SYSTEMS				
1. Separate structural support cooling (30 psia tank pressure)	300 sec.	600	6,200	6,800
2. Split flow	0	1,233	8,150	9,383
3. Vortex choked flow	0	4,110*	8,150	12,260
4. Balanced thrust end plate	0	1,850	8,150	10,000
5. Intra-skirt radial bleed	0	1,230	8,150	9,380
C. NON-ENGINE COUNTER-BALANCE SYSTEMS				
1. Auxiliary chemical propulsion unit	0	14,576	8,150	22,726
2. Tank venting				
(200°R vapor)	0	22,968	8,150	31,118
(37°R saturated vapor)	0	72,880	8,150	81,030
(37°R saturated liquid)	0	315,813	8,150	323,963

*Includes 3,000 pounds of injection fluid.

TABLE I (Cont'd)

ASSUM

1. Thrust nulling required for normal cooldown but not for terminal cooldown.
2. Cooldown through the normal flow path is at $1,500^{\circ}\text{R } T_c$.
3. Cooldown through the structural support path is at $1,000^{\circ}\text{R}$ exit temperature.

TABLE II

COOLDOWN DATA FOR REFERENCE MISSIONS

	A	B	CI	CII	D
	Manned Synchronous Orbit Ferry Mission	Manned Synchronous Orbit One-Way Trip	Jupiter Probe With Parking Orbit	Jupiter Probe, Direct Transfer	Manned Mars* Mission (Leave Earth Stage)
<u>Impulse During Normal Cooldown (lb-sec.)</u>					
Continuous flow period	860,250	906,750	418,500	0	0
Pulse flow period	4,533,750	2,883,000	1,813,500	0	0
Sum	5,394,000	3,789,750	2,232,000	0	0
<u>Impulse During Terminal Cooldown (lb-sec.)</u>					
Continuous flow period	0	237,600	415,800	594,000	831,600
Pulse flow period	0	297,000	2,286,900	3,682,800	2,482,900
Sum	0	534,600	2,702,700	4,276,800	3,314,500
<u>Coolant Requirements During Normal Cooldown (lbs. LH₂)</u>					
Continuous flow period	1,850	1,950	900	0	0
Pulse flow period	9,750	6,200	3,900	0	0
Sum	11,600	8,150	4,800	0	0
<u>Coolant Requirements During Terminal Cooldown (lbs. LH₂)</u>					
Continuous flow period	0	400	700	1,000	1,400
Pulse flow period	0	500	3,850	6,200	4,180
Sum	0	900	4,550	7,200	5,580

Assumptions

Normal cooldown at 1500°R T_c , engine to be reused.

Terminal cooldown at 2500°R T_c , no engine reuse.

Minimum flow rate of 4 lbs/sec.

Cooldown terminated at 0.025 Mw decay power.

*Number approximately the same for Mars arrive and Mars depart stages.

TABLE III

THRUST NULLING

Mission B - Manned One-Way Trip To Synchronous Orbit

<u>Phase</u>	<u>Delta Time</u>	<u>Lbs LH₂</u>	<u>Isp (sec)</u>	<u>Impulse (lb-sec)</u>
Suborbital start burn	1227 sec	111,657	825	92,117,025
Coast in elliptical orbit	150 min	4,700	465	2,185,500*
Depart elliptical orbit burn	414 sec	37,674	825	31,081,050
Coast to Synchro orbit	5 hr	2,900	465	1,348,500*
Enter Synchro orbit burn	554 sec	50,414	825	41,591,550
Coast for payload separation	3 min	550	465	255,750*
Final burn to higher orbit	150 sec	13,650	825	11,261,250
Terminal Cooldown	3 hrs.	900	594	534,600
		<u>222,445</u>		

	<u>Impulse</u>
* Thrust Nulling periods:	2,185,500
	1,348,500
	255,750

$$(8150 \text{ lb.LH}_2) (465 \text{ sec}) = 3,789,750 \text{ lb-sec}$$

TABLE IV

COUNTERBALANCE OF COOLDOWN THRUST BY
USE OF AUXILIARY CHEMICAL PROPULSION UNIT

Assume auxiliary chemical propulsion unit (storable liquid rocket) with specific impulse of 325 seconds and propellant mass fraction of 0.8.

$$\text{Weight System} = \frac{3,789,750 \text{ lb-sec}}{(325 \text{ sec}) (0.8)} = 14,576 \text{ lbs.} \quad \text{Mission B}$$

TABLE V
COUNTERBLANCE OF COOLDOWN THRUST BY TANK VENTING

For tank venting nozzles, assumed $\epsilon = 6:1$, $P_c = 30$. The fluid to be vented was considered for the following conditions:

<u>State Of Fluid To Be Vented</u>	<u>Temp (°R)</u>	<u>Isp (Sec)</u>	<u>Amount Of Fluid Required For Thrust Nulling (lbs.)</u>
Vapor	200	165	22,968
Saturated Vapor	37	52	72,880
Saturated Liquid	37	12	315,813

(3,789,750 lb/sec impulse to be nulled for Mission B)

APPENDIX

APPENDIX A

MEMORANDUM

TO: S. A. Varga
FROM: L. R. Lederer

DATE: 27 September 1968
LRL:vl:7830:M3853 (amended)

SUBJECT: Preliminary Analysis of NERVA Engine Null Thrust Techniques

COPIES TO: E. Arbtin, J. J. Beereboom, J. H. Beveridge, R. V. Evleth,
G. D. Hart, B. Mandell, W. W. Madsen, D. L. Petite,
R. J. Pickering, J. H. Ramsthaller, R. L. Rishel, J. L. Smith,
C. M. Rice, W. D. Stinnett

A study was performed on two potential engine null thrust techniques to determine the required engine flow and driving pressure to effect null thrust while maintaining the engine system within structural and temperature constraints for both hot bleed and full flow engine cycles. Propellant usage weight penalties inherent to thrust nulling for potential missions were estimated.

The two null thrust techniques investigated were (1) radially exhausted flow scheme and (2) thrust balanced, axially exhausted flow scheme. They are shown schematically in Figures (1) and (2) for hot bleed cycle engine configurations.

In the first approach, most of the engine cooldown flow is passed through the structural coolant loop and exhausted outboard radially through small nozzles. The radial exhaust of these gasses produce no forces normal to the engine and thereby null thrust. This series flow technique, however, will require a small bleed through the reflector by introducing flow downstream of the turbopump. In order to remain within the constraint limits on all components, this flow will be added to the exhaust from the structural support system.

The second engine null thrust scheme is a thrust balancing technique in which a portion of the flow would be passed through the normal engine flow route exhausting out the main nozzle and another portion would be passed through the structural support cooling system and exhausted through the thrust nozzle in the direction opposite that of the main thrust nozzle. This flow scheme utilizes parallel flow circuits.

Required hot bleed cycle engine system hardware modifications for these thrust nulling schemes would consist of a control valve in the pump discharge line, a structural coolant outlet collection manifold, blocking valve in the normal structural coolant outlet to the dome, and the lines, valves, injectors, and nozzles associated with the auxilliary thruster.

Both techniques were evaluated for supply liquid hydrogen flow driving pressure with either a boost pump, engine tank pressure or a combination of both. The nature of this analysis was preliminary. Hand heat balances were

CLASSIFICATION CATEGORY
<i>Unclassified</i>
<i>W. J. Madsen 6/11/78</i>
CLASSIFYING OFFICE

performed as opposed to node to node computerized heat transfer analysis. However, values of temperature, pressure and density used for the hand heat balances were extracted from the Fortran IV NERVA engine transient analysis program at the appropriate performance map point. Structural coolant loop pressure drops were scaled from WANL-TME-1798 (Figures 4-3 and 4-4). Maximum structural coolant loop flow of 5.3 pounds per second and maximum exit gas temperature as 1000°R were assumed. Choked flow and convective heat transfer were also assumed and confirmed by hand calculations throughout the study. Nuclear after heats were taken from the latest NRO decay heat curves. Continuous flow rates as opposed to pulsed flow was considered for this investigation.

Radially Exhausted Flow

Thrust implications of this scheme were neglected for the exhausting gasses were assumed to be vented radially and in equal amounts in opposite direction, causing no side force misalignment. A parametric flow-pressure drop relation was established to initially determine required auxiliary chamber pressure for various LH₂ flows. This relationship is shown in Figure (3). Three typical choked throat areas and 20, 40, and 60 square inches were selected to be carried through the study to show the sensitivity effects of this parameter. After choked flow pressures were determined, main structural support pressure drops at the various flows as indicated by WANL-TME-1798 were scaled. An additional 25% pressure drop was added to the core structural coolant pressure drop to account for miscellaneous line, valve, and nozzle injector losses. These pressure drops were summed to determine the required driving pressure for this radial exhaust scheme. This driving pressure is shown in Figure (4), plotted versus flow rate for the 20, 40, 60 square inch auxiliary thruster throat areas. Since tank pressure would probably be 30 psia, tank pressure driven LH₂ could only operate below the 2 pound a second flow regime. Between the 2 pound a second value and the 5.3 pound a second maximum structural coolant flow constraint, boost pump driving pressure would be required to supply flow. At the maximum flow rate, pump discharge pressure of approaching 200 psia are required.

To determine the time phased, flow requirements for thrust nulling, reactor full power operating were examined. From mission information for cooldown and thrust spoiling analysis supplied by S. A. Varga to D. Buden, dtd 15 July 1968, all four potential mission profiles bracketed full power reactor operation time from 5 to 30 minutes. ~~It should be noted that the cumulative fission product effect of three nearly-30 minute burns for the Mars Landing Mission could not be accurately accessed.~~ Flows required for cooldown of the reactor after missions were calculated and are presented versus time from scram for a variety of reactor operating times in Figure (5). This plot has been modified in Figure 6 to indicate the 5.3 pound a second maximum flow limit constraint for structural coolant flow and the flow regimes best handle by boost pumps and tank pressure propellant feed for this radially exhaust flow case. This plot indicates flows less than 2 lbs/sec requires boost pumps supply coolant until the 200 seconds to 750 seconds bandwidth on the cooling profile is attained. An optional approach to this technique would periodically flow a fraction of the total required flow through the normal engine flow path to cool components such as the reflector to constraint limits and then recombine this flow fraction

with the structural coolant flow in its outlet manifold.

Thrust Balanced, Axially Exhausted Flow

This null thrust scheme, Figure (2), must meet two major requirements. First, axial thrust must be continuously balanced. Simultaneously total engine flow rate, which is split between parallel circuits must remove all the reactor afterheat while maintaining system temperatures within temperature and structural constraints. Thrust is a function of flow rate and specific impulse. If flow were split equally between the structural support flow circuit and the normal engine flow circuit, the specific impulse of the exiting gasses would have to be identical to balance axial thrust. These specific impulses are not equal and therefore the flow split must be adjusted to equalize thrust. Specific impulse is a function of exit gas temperature, T_c , and nozzle area ratio, C . Since the structural support coolant circuits exit gas constraint is 1000°R and its probable area ratio less than 10, its I_{sp} must be traded off against the probable 1200 - 1400°R main nozzle exit temperatures and its fixed area ratio of 100. This relationship is portrayed graphically in Figure (7), where nozzle area ratio is plotted versus theoretical specific impulse for exit gas temperature of 1400 , 1200 and 1000°R . The exchange ratio band for representative auxiliary thrust nozzle area ratios of 2, 6 and 10 range from 1.4 to 1.15 with 1.25 appearing to be most representative. This flow split ratio resulting in more flow to the structural coolant loop to adjust for its cooler exit gas and smaller area ratio also maintained required thermal limits for the system.

After thrust balancing and energy removal requirements were satisfied, required driving pressures were determined. Driving pressures were determined by again summing nozzle chamber pressure with total system flow resistance for various flows of each circuit. Required driving pressures are shown plotted against system flow rate for the selected three study throat areas in Figure (8). The required driving for this balanced thrust, axial exhausted flow techniques, are less than 100 psia or about half the values of radial exhaust technique. This is an inherent characteristic of parallel flow circuits when compared to series flow circuits of identical flowrates and comparable resistances. With this scheme, tank pressure can supply the required system driving pressure at approximately 3 pounds/second. Translated to mission requirements as shown in Figure (9), where required heat removal flow rate is shown versus time from scram for various reactor full power operating times. Tank pressure can be used to provide driving pressure from 120 to 400 seconds.

A summary of advantages and disadvantages of both techniques are summarized in Table (1).

The propellant weight usage required for null thrust operation is shown in Figure (10) where accumulative propellant usage in pounds is plotted versus time from scram for reactors operating for 5, 15, and 30 minutes at full power. For example, approximately 10,000 pounds of LH_2 would be required to null thrust for 20,000 seconds (5.55 hours) from scram.

Thrust Null Technique - Full Flow Cycle

An adaption of the afore described thrust nulling techniques to the full engine is shown schematically in Figure (11). This configuration does not require a separate boost pump for it utilizes the existing engine turbo-machinery operating at low flow and head conditions to provide the required system flow. Besides the null thrusters with their associated lines, only a two-way valve in the core inlet line is required to modify the hardware for null thrust operation.

In this configuration, liquid hydrogen is flowed from the tank through the pump and structural coolant loop. At this point, it can flow either to the turbine if discharge pressures greater than tank pressure are required or can be bypassed around the turbine if tank pressure is sufficient to meet system flow requirements. The core inlet flow is shut off by a two way valve and the flow is directed outboard through the null thrusters.

L. R. Lederer
Data Analysis Group

Approved:

D. Buden, Manager
Systems Analysis Section

RADIALLY EXHAUSTED FLOW

(SCHEMATIC)

THRUST BALANCED, AXIALLY EXHAUSTED FLOW

FIGURE 2

FIGURE 3

AUXILIARY THRUSTER CHAMBER PRESSURE
VERSUS
NOZZLE CHOKED FLOW AREA
FOR
DIFFERENT STRUCTURAL COOLANT FLOWS

EUGENE DIETZGEN CO.
MADE IN U. S. A.

NO. 34DR-20 DIETZGEN GRAPH PAPER
20 X 20 PER INCH

AUXILIARY THRUSTER CHAMBER PRESSURE, PSIA

170
160
150
140
130
120
110
100
90
80
70
60
50
40
30
20
10
0

STRUCTURAL COOLANT FLOW RATE

5.3 LB/SEC
5.0 LB/SEC
4.0 LB/SEC
3.0 LB/SEC
2.0 LB/SEC
1.0 LB/SEC

NOZZLE CHOKED FLOW AREA, SQUARE INCHES

REQUIRED DRIVING PRESSURE, PSIA [RADIALLY EXHAUST FLOW]

FIGURE 4

REQUIRED DRIVING PRESSURE
VERSUS
SYSTEM LH_2 FLOW RATE
FOR
VARIOUS NOZZLE THROAT AREAS

NOZZLE THROAT
AREA, IN^2

20

40

60

TANK DRIVING
PRESSURE

" 1500 FT. SEC.
 " 1000 FT. SEC.
 " 500 FT. SEC.
 " 300 FT. SEC.

REQUIRED DRIVING PRESSURE, PSIA [Thrust Balanced, Axially Exhausted]

FIGURE 8

FIGURE 4.

CUMULATIVE PROPORTION USED, $10^3 L$

FULL FLOW ENGINE CONFIGURATION

FIGURE 11

(SCHEMATIC)

SUMMARY

RADIALLY EXHAUSTED FLOW SCHEME (SERIES CIRCUIT)

ADVANTAGES

- (1) ~~NO~~ AXIAL THRUST CONTROL REQUIRED
- (2) NO FINE FLOW CONTROL SYSTEM REQUIRED

DISADVANTAGES

- (1) HIGHER DRIVING PRESSURES REQUIRED
- (2) POTENTIAL SIDE FORCE GENERATED FROM THRUST MISALIGNMENT

THRUST BALANCED AXIALLY EXHAUSTED FLOW SCHEME (PARALLEL CIRCUIT)

ADVANTAGES

- (1) LOWER DRIVING PRESSURES REQUIRED
- (2) CONTINUOUS COOLANT PROVIDED TO NOZZLE TUBES AND REFLECTOR

DISADVANTAGES

- (1) A FINE FLOW CONTROL SYSTEM REQUIRED TO ADJUST FLOWS FOR THRUST NEUTRALIZATION
- (2) POTENTIAL TANK HEATING FROM REVERSE FLOW, HIGH TEMPERATURE GASES

APPENDIX B

INTER-OFFICE MEMO

AEROJET-GENERAL CORPORATION®

TO: S. A. Varga
FROM: I. L. Odgers
SUBJECT: Thrust Spoiler Studies

DATE: 20 September 1968
EBC:nla
7750:M0373

DISTRIBUTION: J. J. Beereboom, H. J. Bronner, D. Buden, E. B. Cleveland,
R. V. Evleth, W. W. Madsen, T. Pasternak, R. J. Pickering,
C. M. Rice, L. A. Shurley, W. D. Stinnett, W. O. Wetmore

Reference: (a) Memorandum 7750:M0333 dated 3 July 1968, I. L. Odgers
to S. A. Varga, subj: Same as above
(b) Memorandum 7810:0497M dated 11 July 1968, S. A. Varga
to L. A. Shurley, subj: Same as above
(c) Memorandum 7810:0519M dated 5 August 1968, S. A. Varga
to R. V. Evleth, subj: Same as above
(d) Memorandum 7810:0530M dated 21 August 1968, S. A. Varga
to Distribution, subj: Plan for Resolution of TBDs in
Program Requirements Document, Paragraph 6 Thrust Nulling

Enclosure: (1) Thrust Nullification Study
(2) Liquid Retro Rocket Evaluation

As agreed in Reference (a), several thrust nullification concepts have been defined and evaluated according to the requirements of References (b), (c), and (d). Up to six concepts were promised by Reference (a) and eight have actually been evaluated. These concepts and the evaluations are transmitted as Enclosure (1) of this memorandum.

A recommendation will not be made for any one of these systems because it is expected that appropriate trade studies will be conducted by your organization, which will include other thrust nulling methods, to evaluate overall engine performance and mission requirements. All of the concepts presented in the enclosures can conceivably be developed but will impair engine or component performance and reliability. A generalized development schedule is included to illustrate the type of minimum program and time required to develop one of these concepts.

A brief evaluation of a liquid retro rocket system was also made for comparison purposes and this is presented as Enclosure (2).

I. L. Odgers, Manager
Engineering Department
Nuclear Rocket Operations

THRUST NULLIFICATION STUDY

I. INTRODUCTION

The purpose of this study is to identify potential means of thrust nullification and to determine their relative merits or negative characteristics in terms of weight, complexity and effectiveness, inclusive of problems such devices may generate.

Several concepts to nullify thrust of the NERVA Engine during the reactor cooling cycles are discussed and compared. The concepts presented here deal only with nullification of axial thrust by deflection of some or all of the gases that have flowed through the reactor assembly. Other techniques for counteraction of engine thrust are worthy of consideration, but are beyond the assigned scope of this report. Furthermore, this report will not attempt to make a recommendation for selection of a thrust nullification system for the NERVA Engine, but will provide sufficient information for comparison of these general concepts with other techniques of balancing forces acting upon the vehicle.

II. SUMMARY

Three concepts for achieving balanced lateral thrust and five balanced longitudinal thrust concepts were evaluated. A selection was not made from among these eight concepts because each must be further evaluated relative to engine system and mission requirements.

The evaluation of the eight concepts showed that nullification devices of this type can probably be developed but all will reduce engine performance and reliability. Estimate of concept weights range from 734 lb. to 4400 lb. excluding actuation and gimbaling power requirements. All of the concepts will increase secondary radiation.

It was determined that a 28 month program would be required to develop any one of these concepts exclusive of engine gimbaling and radiation problems.

III. DISCUSSION

A. REQUIREMENTS

A system or device must deflect the coolant gases (Figure 1) in such a manner so as to achieve zero net axial thrust during any or all of the cooling periods planned for a motor mission.

Devices must be suitable up to 47 on-off cooling periods following each full power operation in addition to an initial 180 second period after each shutdown. Full power operations may number up to 10 with the most probable number being four.

The system must be compatible with 180 second initial cooling period during which the thrust will be 8,000 lbf; the mass flow 12.6 lbm/sec; and pressure 40 psia. During subsequent cooling periods the values will be 2,000 lbf; 4.2 lbm/sec; and 10 psia. The system must be compatible with the temperature environment. The average temperature of the coolant gases is 1600°R and the maximum expected is 2000°R. Nuclear heating rates are expected to be within 0.05 and 0.50 Btu/cu-in, sec.

Analyses of each concept are to include the following:

1. Rough conceptual outline of mechanical arrangement.
2. Estimates of weight, reliability, gas flow patterns, and forces.
3. Estimates of heating problem.

B. GENERAL ANALYSIS

Nullification of coolant thrust by deflection of the exhaust gases can be achieved by two methods: (1) All of the exhaust gases can be deflected 90° to the engine axis and divided to equalize side forces (Figure 3). This is termed "Balanced Lateral Thrust". (2) Part of the exhaust gases can be deflected more than 90° to produce equal and opposite longitudinal thrust forces and some balanced lateral flow (Figure 4). This is termed "Balanced Longitudinal Thrust". The basic operation, major components, advantages and disadvantages of both principles are outlined in Figures 5 and 6.

III.B. (Continued)

Principle ways to actually deflect the gases include diverting a portion of the flow from within the nozzle or skirt either by bleed lines or plates, or by a combination of these.

Eight concepts based on these principles have been analyzed in terms of the stated requirements. These eight concepts are:

1. Split Flow
2. Vortex Choked Flow
3. Mechanical Throat Choke
4. Throat Choke Front Port
5. Exit Cone Closure
6. Exit Plane Radial Bleed
7. Balanced Thrust End Plate
8. Intra Skirt Radial Bleed

The power required to operate the valves and actuators was not determined but is not considered significant in comparing the concepts. Flow analyses were made for only three of the more complex concepts. The loads were based on the initial cooling period condition of 40 psia pressure.

Formal heat transfer and stress analyses were not performed, and the weight estimates are based on conservative but comparable structures that can be developed within current technology.

The effects of nuclear heating and secondary radiation have only been considered to the extent that materials with low nuclear cross-sections are used wherever possible. Effects on engine gimbaling because of increased engine weight were not evaluated because this requires an independent evaluation in light of the stipulated weight of each of the systems.

III. (Continued)

C. CONCEPT ANALYSIS

1. Split Flow

Perhaps the simplest method to achieve thrust nullification is to split the subsonic flow so that approximately half flows aft out of the primary nozzle and the other half flows in a forward direction through secondary nozzles. This concept is illustrated in Figure 7.

Because the forward facing secondary nozzles must be angled approximately 30° to the engine center line to direct the exhaust away from the engine system, fuel tank, and crew module, a greater amount of the flow must be directed forward. Using secondary nozzles with expansion ratios of 10:1, the relationship between the primary and secondary nozzle throat areas to achieve equal axial thrust is:

$$\begin{aligned}\text{Secondary Throat Area, } A_t^1 &= \text{Main Throat Area, } A_t \left(\frac{1}{\cos 30^\circ} \right) \left(\frac{C_f}{C_{f1}} \right) \\ A_t^1 &= \frac{1}{0.866} \frac{1.7145}{1.5160} A_t \\ &= 1.306 A_t\end{aligned}$$

Assuming that two secondary nozzles will be used and that the main nozzle throat area is 100 sq. in., the minimum inside diameters of these bleed lines will be:

$$d_t^1 = \sqrt{(1/2) (1.306) A_t \left(\frac{4}{\pi} \right)} = 9.118 \text{ in. dia.}$$

A preliminary layout of the primary nozzle showed that two diametrically opposed ports of this size can be incorporated in the existing design without interfering with the hot gas bleed port. The ports impose a design problem, however, because a large number of coolant tubes must be interrupted and their flow diverted through manifolds around the ports. Extra material will also have to be added to the nozzle forging to accommodate the higher discontinuity stresses imposed by the bosses and lines. This extra material will aggravate the nuclear heating problem and may require cooling of the outside of the nozzle, as well as the lines.

III.C.1.(Continued)

The weight tabulation in Figure 7 includes an estimate of the added weight required to strengthen the primary nozzle and provide the two large bosses, but does not include provision for external cooling. The added nozzle weight shown is 15% of the present total nozzle weight.

During engine operation these bleed lines must be closed off so that all flow is aft through the primary nozzle. This requires a hot gas, high pressure valve in each line. The assumption is made here that such a valve can be made and that the weight of the valve and actuator will be similar to that of other high pressure valves currently designed for the engine. Figure 8 presents a graph of current engine valve/actuator weights as a function of line diameter. Based on nominal line size of 9-in.-dia., each of these bleed valves would be expected to weigh 200 lb.

Because recirculation of the hot exhaust gases may occur in the large diameter line between the nozzle and the closed valve during engine operation, this portion of each line is shown jacketed and cooled by circulation of nozzle coolant. The additional coolant that might be required to cool these lines and valves was not determined.

It is recognized, however, that there is a potential serious problem related to material selection and design complexity associated with cooling the valves. Without cooling, it could be expected that some parts of the valve assembly would attain temperatures approaching 4500°R which would make this scheme impractical.

Weights of the lines are based on a preliminary layout that uses standard attachment joints and bend radii. The calculated weight of each line is 283 lb. If the lines were made from a graphite composite material such as AGCarb-101 and metal fittings were used, each line would weight 225 lb. This lower weight was not used in the comparison of concepts because graphite lines are considered beyond current technology.

III.C.1. (Continued)

The secondary nozzles were, however, assumed to be made from a combination of AGCarb-101 and AISI-347 Steel. The calculated weight of each is 67.5 lb.

Using standard bend radii and fittings results in a line/nozzle/valve assembly that extends approximately 7 feet from the engine center line. This may cause a problem with the design of a engine shroud and may affect the gimbaling of the engine. Investigation of these considerations was considered beyond the scope of this trade study. Also a structure to support these lines was not included, but would be required.

Total weight of this concept is estimated to be 1233 lb. and the major functional problem will be sealing the ports during engine operation and developing a high temperature valve.

2. Vertex Choked Flow

The required throat area of the secondary nozzle of the Split Flow concept and, consequently, the total weight could be reduced if the primary nozzle throat area could be made smaller during the cooling periods.

One concept investigated to choke down the flow through the primary nozzle would utilize injection of additional gas flow into the subsonic section of the nozzle to produce a vortex swirl. This principle is used in control valves to regulate flow without moving mechanical parts.

A swirling flow analysis of the concept shown in Figure 9 was made and indicated that theoretically a choking of the main throat could be achieved with a vortex with an injectant mass flow of 25% of the coolant flow and at 1.5 times the pressure. A line diameter of 7-in. can be used resulting in a 517 lb. weight reduction as compared to the Split Flow concept. When the additional hydrogen and the injectant lines and valve weights are added, however, this concept becomes unattractive as shown by the tabulation in Figure 9.

III.C.2. (Continued)

If the injectant fluid could be taken from the coolant flow at the aft end of the reactor it could be pumped back in farther downstream to create the vortex. This would add the weight of a pump, drive motor and electrical power, but eliminate the 3000 lb. of additional hydrogen. This approach was not evaluated.

3. Mechanical Throat Choke

Following the approach of choking the primary nozzle throat, the concept of a mechanical plug controlled from the front end of the engine was investigated (Figure 10). Since no coolant will flow through the primary nozzle, all of the diverted flow can be ejected laterally. The bleed ports can be a minimum diameter and secondary nozzles are not required. For the purpose of consistent comparisons, the lines are extended out to 7 ft. though they could probably be terminated at the valves.

This approach would reduce the bleed system weight by almost 700 lb. as compared to the Split Flow concept if the plug and associated weights are ignored.

Disregarding the problems of reactor redesign and efficiency reductions caused by incorporating a center hole, it would be convenient to actuate the plug from the front end of the engine. The plug and rod could be made from high temperature materials such as graphite and be retracted against the aft end of the reactor during engine operation. Including a token weight increase of 100 lb. for reactor changes, this portion of the concept would add 200 lb. and the total concept weight would be 734 lb.

The major problems apparent with this concept are the reactor redesign necessary and bleed port sealing which entails intrinsic design complexities and thermal aspects affecting materials of construction.

III.C. (Continued)

4. Throat Choke and Front Ports

Consideration was given to a mechanical throat plug and bleed ports at the front end of the engine as shown in Figure 11. In this case, the primary nozzle is not affected and the shutoff valves can be combined into one axial valve built into the plug control rod.

A hole up through the center of the reactor equal in area to the main throat would be required to carry the coolant gases to the forward ports. This, of course, increases the reactor weight and redesign problems, and would necessitate an increase in pressure vessel diameter.

The estimated weight of this concept of 736 lbs. is very close to that of the previous concept (Figure 10).

5. Exit Cone Closure

Another way to prevent axial flow through the primary nozzle is to plug the end of the exit cone with a closure that can be retracted during engine operation. Such a concept is presented in Figure 12. The bleed port diameter is a minimum and forward facing secondary nozzles are not required. Closure plate and skirt loads are substantial, however.

It is assumed that insulated closure plates can be made to withstand the maximum gas temperature of 2000°R . Weight estimates for the plates assume an average temperature of 1000°R since the gas flows intermittently, and the skirt and plates will radiate to a space environment.

To withstand a pressure as high as 40 psia, the skirt and skirt extension will have to be reinforced.

The ejection load on the plate at 40 psia will be:

$$L_e = 70 A_t 40 \text{ psia} = 280,000 \text{ lbf.}$$

Assuming no tube coolant flow during the cooling periods and an average skirt temperature of 1000°R , approximately 150,000 lbf can be imposed before the skirt starts to yield. Skirt reinforcing is, therefore, required if

III.C.5. (Continued)

the plates are supported from the skirt. The estimated increase in skirt weight shown in Figure 12 includes both hoop and longitudinal strengthening of the skirt and skirt extensions.

Several mechanisms were considered to move the closure plates in and out of position and these are illustrated in Figures 13 - 15. The estimated mechanism weight is based on the method shown in Figure 13 because this requires the least amount of space to move the plates from the stowed to the operating position.

The weight of this concept is 4400 lb. and the major problem is still sealing the bleed ports. Additional detrimental effects introduced are those resulting from a significant increase in secondary radiation which if proportional to the added mass may represent a four-fold increase in radiation dose generated by the skirt extension and an increase in gimbaling mass compounded by a shift in center of gravity.

6. Exit Plane Radial Bleed

In the concept shown in Figure 16, the bleed lines and valves were eliminated by allowing the gases to escape radially between the plate and the end of the skirt. The edges of the plates are angled to direct the flow in a radial direction.

Some difficulty may be encountered in actually turning the flow 90 degrees and in achieving equal radial forces. A detailed flow analysis of this concept was not made but a preliminary investigation of the flow turning problem revealed that the hydrogen would have to be shocked down to subsonic flow, turned the 90 degrees and then vented radially.

Elimination of the bleed ports results in a reduction in weight but the total concept weight of 3860 lb. is high. The high weight indicated by this preliminary analysis and associated increase in radiation scatter made a more detailed study unwarranted. An increase in gimbaling requirements is another factor.

III.C. (Continued)

7. Balanced Thrust End Plate

One way to reduce the end plate load and thus the weight is to put a hole in the center and allow some of the gases to escape aft. The thrust of these gases must then be balanced by diverting the remainder of the flow forward. This concept is illustrated in Figure 17 and the mechanism to move the plate sections would be as shown in Figure 13.

A flow analysis of this configuration was made and the results, presented in the appendix, show that a minimum plate load is achieved when the plate is 112 in.-dia; the hole is 25.5 in; and the gap between the plate and exit plane is 3 in. The estimated concept weight is 1850 lb. as shown in Figure 17.

Since the plate load is 46,800 lb. which is 17% of the Exit Plane Radial Bleed (Figure 16) plate load, the skirt will not have to be reinforced and the plate, actuators and mechanism can be lighter. Some attachment bosses will have to be added to the skirt to connect the plates to the skirt and the weights of these are included in the mechanism weights. The total weight of this concept is then about 50% of the previous concept weight. However, effects on gimbaling requirements and increase in radiation emission caused by mass increase are still a matter of concern.

8. Intra Skirt Radial Bleed

To obtain a further decrease in weight, the principle of the previous concept was applied at an expansion ratio of 25:1, just aft of the skirt/skirt extension joint. This concept, Figure 18, features six pie-shaped plates that can be rotated into the skirt to divert some of the flow out through upstream openings. When the plates are rotated out of the skirt, a flange on one side of each plate closes the opening. A portion of the plate extending outside of the skirt turns the gases to develop equal forward thrust.

III.C.8. (Continued)

The pressure on the plates is assumed to be twice that predicted for the end plate concept because it is considered impractical to open as large a vent area as is possible at the exit plane. A vent area $1/2$ that of the End Plate Concept results in a 3 in. high vent slot at each plate.

Cooling the plates and supporting structure during engine operation is a major problem, as is sealing around each hinge. Selection of optimum materials and contouring of the external part of each plate to improve flow turning could further reduce the weight of this concept.

IV. RELIABILITY

A quantitative assessment of the reliability of these concepts is not possible; however, after consideration of the major problem identified for each concept, it appears that No. 7, the Balanced Longitudinal Thrust End Plate, would be the most reliable.

V. DEVELOPMENT CONSIDERATIONS

In the event that one of the concepts presented here is selected for development, the full and subscale tests and the evaluations listed below will be necessary to demonstrate design acceptability.

<u>TYPE TEST</u>	<u>PURPOSE</u>
Material and Structure Evaluation	Thermal tests of sample composite structures to optimize weight, strength, and heat transfer.
Subscale Flow	Warm gas flow tests in space simulator to establish thrust nulling errors, pressures, flow paths, loads, losses and stabilities.

V. (continued)

<u>TYPE TEST</u>	<u>PURPOSE</u>
Full Scale Functional	Operate full scale components to establish operating power requirements, rates and actuator loads.
Full Scale Structural	Test full scale components to establish ultimate loads capability and to locate critical strength points.

As shown in Figure 20, a 28 month program will be required to complete the evaluations, tests, analyses and drawings. This includes 8 months for initial design, analysis and subscale tests. Long lead time full scale hardware will be ordered near the end of this 8 months. The full scale tests including hardware procurement will require 20 months based on past experience.

REACTOR COOLING

FIGURE 1

THRUST NULLIFICATION CONCEPTS - COOLING CYCLE

Sequence of N Cycles	ON, Sec	Off, Sec
1	180	20
2	50	20
15	20	100
30	10	1500

Figure 2

11 PRO

BALANCED LATERAL THRUST

REPORT
COM.
GENERAL

COOLANT GASES ARE DIVIDED
TO PROVIDE EQUAL FWD & AFT
THRUST

111 WRO

BALANCED LATERAL THRUST PRINCIPLE OF THRUST NULLIFICATION

OPERATION

COOLANT FLOW IS PREVENTED FROM FLOWING
THROUGH NOZZLE, IS SPLIT AND EJECTED RADIALY.

COMPONENTS

PLUG OR COVER TO PREVENT FLOW OUT OF NOZZLE.
RADIAL EXHAUST PORTS
VALVES TO CLOSE PORTS DURING ENGINE OPERATION
POWER SOURCE TO MOVE PLUG OR COVER AND OPERATE VALVES

ADVANTAGE

ASSURES ZERO AXIAL THRUST.

DISADVANTAGES

EXHAUST VALVES MAY BE EXPOSED TO 4500°R.
PORT AREA MUST BE LARGE OR COOLANT PRESSURE HIGH

11100

BALANCED LONGITUDINAL THRUST PRINCIPLE OF THRUST NULLIFICATION

OPERATION

DEVICE DEFLECTS A PORTION OF THE COOLANT EXHAUST
TO GIVE AN EQUAL FORWARD COMPONENT OF THRUST.

COMPONENTS

DEFLECTOR TO CHANGE DIRECTION OF FLOW
RESTRICTOR TO REDUCE REARWARD FLOW
POWER SOURCE TO MOVE DEFLECTOR AND RESTRICTOR

ADVANTAGES

PRINCIPLE IS PROVEN IN JET ENGINES
MODULATED LOW LEVEL FORWARD
OR REVERSE THRUST POSSIBLE

DISADVANTAGES

CAN ONLY BE TESTED IN A SPACE ENVIRONMENT

ADDED MOTOR WEIGHT, LB	
PRIMARY NOZZLE	137
LINES	566
VALVES & ACTUATORS	400
SECONDARY NOZZLES	130
TOTAL	1233

MAJOR PROBLEMS:
SEALING BLEED PORTS &
HIGH TEMPERATURE VALVE

VALVE-ACTUATOR WEIGHT

BASED UPON ACTUAL
WEIGHTS OF NERVA
HIGH PRESSURE SHUT
OFF VALVES

Figure 8

THRUST NULLIFICATION CONCEPT - VORTEX CHOKED FLOW

ADDED MOTOR WEIGHT, LB	
PRIMARY NOZZLE	151
LINES	300
VALVES & ACTUATORS	160
SECONDARY NOZZLES	104
SUB TOTAL	
	715
INJECTOR ASSEMBLY	35
VALVE & ACTUATOR	40
LINE	320
HYDROGEN	3000 *
SUB TOTAL	
	3395
TOTAL	
	4110

MAJOR PROBLEMS:
 SEALING BLEED PORTS &
 HIGH TEMPERATURE VALVE

* BASED ON 4 ENGINE RESTARTS

11 NRO

THRUST NULLIFICATION CONCEPT - MECHANICAL THROAT CHOKE

$$A_t' = A_t$$

$$d_t' = 5.6 \text{ IN. DIA EA.}$$

ADDED MOTOR WEIGHT, LB	
NOZZLE	74
LINES	300
VALVES & ACTUATORS	160
SUB TOTAL	534
FWD HEAD	10
PLUG & ROD	40
ACTUATOR & HOUSING	50
REACTOR	100
SUB TOTAL	200
TOTAL	734

MAJOR PROBLEM:
REACTOR REDESIGN

Figure 10

MEMO

THRUST NULLIFICATION CONCEPT - THROAT CHOKE & FRONT PORTS

ADDED MOTOR WEIGHT, LB	
LINES	340
PRESSURE VESSEL	86
PLUG & ROD	40
ACTUATOR & HOUSING	50
VALVE	20
REACTOR	280
TOTAL	736

MAJOR PROBLEM:
COMPLETE REDESIGN OF REACTOR
AND PRESSURE VESSEL

[N] NRO

THRUST NULLIFICATION CONCEPT - EXIT CONE CLOSURE

$$A_t' = A_t$$

$$d_t' = 5.6 \text{ IN. DIA EA.}$$

ADDED MOTOR WEIGHT, LB	
SKIRT	1005
NOZZLE	75
LINES	300
VALVES & ACTUATORS	160
CLOSURE	2500
ACTUATOR (4)	60
MECHANISM	300
TOTAL	4400

MAJOR PROBLEMS:
SEALING BLEED PORTS &
HIGH TEMPERATURE VALVE

72123

THRUST NULLIFICATION CONCEPT - CLOSURE MECHANISM

Figure 13

Figure 14

NR0

THRUST NULLIFICATION CONCEPT - CLOSURE MECHANISM

Figure 15

ADDED MOTOR WEIGHT, LB	
SKIRT	1000
CLOSURE	2000
ACTUATOR	60
MECHANISM	300
TOTAL	3860

MAJOR PROBLEMS:
HIGH SKIRT LOADS &
TURNING FLOW 90°

1110

THRUST NULLIFICATION CONCEPT - BALANCED THRUST END PLATE

c = 3 IN.
d = 25.5 IN.
D = 112 IN.

ADDED MOTOR WEIGHT, LB	
SKIRT	200
CLOSURE	1500
ACTUATOR	50
MECHANISM	100
TOTAL 1850	

MAJOR PROBLEM:
ATTACHMENT TO SKIRT

Figure 17

LAURO

THRUST NULLIFICATION CONCEPT - INTRA SKIRT RADIAL BLEED

ADDED MOTOR WEIGHT, LB	
PLATES	930
ACTUATOR	20
MECHANISM	50
STRUCTURE	235
TOTAL	1235

MAJOR PROBLEMS:
SEALING HINGE POINTS &
COOLING THE PLATES

Figure 18

SUMMARY - THRUST NULLIFICATION CONCEPTS

	1	2	3	4	5	6	7	8
	<u>Split Flow</u>	<u>Vortex Flow</u>	<u>Throat Choke</u>	<u>T. C. Front Ports</u>	<u>E. C. Closure</u>	<u>F. P. Rad. Bleed</u>	<u>B. T. End Plate</u>	<u>I. S. Rad. Plate</u>
<u>WEIGHTS</u>								
Primary Nozzle	137	151	74	--	75			
Lines	566	300	300	340	300			
Valves & Actuators	400	160	160	70	160			
Secondary Nozzles	130	104	--	--	--			
Injector Assembly		35						
Valve & Actuator		40						
Line		320						
Hydrogen		*3000						
Fwd Head & P.V.			10	86				
Plug & Rod			40	40				
Actuator			50	--				
Reactor			100	200				
Closure or Plates					2500	2500	1500	930
Actuators					60	60	50	20
Mechanism					300	300	100	50
Skirt Reinf.					1005	1005	200	230
Added Motor Weight	1233	4110	734	736	4400	3865	1850	1230
<u>** MAJOR PROBLEMS</u>								
	Sealing Ports & High Temp Valve	Sealing Ports & High Temp Valve	Reactor Redesign	Reactor & P. V. Redesign	Sealing Ports & High Temp Valve	High Skirt Loads & Turning Flow 90°	Attachment to Skirt	Sealing Hinge & Cooling Plates
<u>RELIABILITY RATING</u>	Low	Low	Low	Avg.	Low	High	Highest	Avg.

*Based on 4 Restarts.

**All concepts increase gimbaling loads, engine weight and secondary radiation effects.

Figure 19

DEVELOPMENT SCHEDULE

MONTHS FROM START	2	4	6	8	10	12	14	16	18	20	22	24	26	28
Analyses														
Preliminary Design and Drawings														
Materials & Structure Evaluations														
Subscale Flow Tests														
Full Scale Functional Tests														
Full Scale Structural Tests														
Update Design and Drawings														
Final Report														

Figure 20

LIQUID RETRO-ROCKET EVALUATION

Two liquid retro-rocket systems were investigated as possible thrust nullification methods. The results are summarized below. The weight estimates are based on 10 engine restarts.

	<u>System Wt., Lb.</u>	
	<u>LO₂/LH₂</u>	<u>H₂O₂</u>
H ₂ Propellant	50,000	123,000
Tanks and Lines	18,500	11,000
Thrust Chamber & Supt	1,000	1,500
TOTAL	69,500	135,500

The lightest system, using LO₂/LH₂, would add 69,500 lb. to the engine based on 4 engine restarts.

Reference Memo S. A. Varga to L. A. Shurley, subject: Operational Criteria for Thrust Reversing Devices, dated 11 July 1968.

The required impulse time is as follows:

<u>Cycles</u>	<u>Time</u>
2	50
15	20
30	10

It is estimated that the reaction required to nullify the transient and cooldown thrust will be one-180 sec. cycle beginning at 8000 lb. and ending at 2000 lb. Total impulse for the transient would be:

$$(8000 - 2000) \times 180 \times \frac{1}{2} = 540,000 \text{ lb-sec.}$$

The impulse per engine cycle is tabulated below:

<u>Cycles</u>	<u>Time</u>	<u>Thrust</u>	<u>Impulse</u>
1	180	8000-2000	540,000 lb.sec
2	50	2000	200,000
15	20	2000	600,000
30	10	2000	<u>600,000</u>
			1,940,000 lb.sec

Through 10 engine re-starts and associated cooldown cycles, the possible total impulse would be:

$$10 \times 1,940,000 = 19,400,000 \text{ lb-sec.}$$

Assume a hydrogen-peroxide reactor system. The specific impulse for 90% H_2O_2 is approximately 168 sec. at 300 psi using a 30:1 nozzle. A realistic efficiency for I_s using variable thrust and through many transients is 93%, thus giving an actual I_s of 156 sec.

Therefore, the required H_2O_2 to complete the maximum reaction would be:

$$1.94 \times 10^7 / 1.56 \times 10^2 = 1.23 \times 10^5 \text{ lbs.}$$

The density of H_2O_2 is 86.6 lb/ft³.

Volume of tankage required would be:

$$1.23 \times 10^5 / .866 \times 10^2 = 1420 \text{ ft}^3.$$

A spherical tank to contain this volume, neglecting spare propellant and ullage, would be sized as follows:

$$V = \frac{4}{3} \pi r^3$$

$$r = \left(\frac{3}{4} \frac{V}{\pi} \right)^{1/3}$$

$$= \left(\frac{3 \times 1420}{4 \times 3.1416} \right)^{1/3} = (339)^{1/3} = \underline{\underline{6.20 \text{ ft.}}}$$

The surface area of such a tank

$$A = 12.57 r^2$$

$$A = 12.57 (6.20)^2 = 483 \text{ ft}^2$$

Assuming a .500 wall thickness, the tank would weigh approximately

$$.3 \times 483 \times 144 \times .500 = 10,500 \text{ lbs.}$$

Supporting structure, lines, controls, and insulation would more than double the tank weight.

Other problem areas with the H_2O_2 reaction system are decomposition during storage, high freezing point, large number of restarts, unknowns of radiation effect on H_2O_2 , heating systems required to keep combustion products from freezing on catalyst screens, heating system for controls, lines, and tank.

Properties of 90% H_2O_2

Freezing Temp.	11.3°F
Boiling Temp.	287°F
Density	86.6 lb/ft ³ at 77°F

References :

1. Hydrogen Peroxide Handbook, AFRPL-TR-67-144, July 1967 by Rocketdyne (AGC Library 7U6116)
2. Liquid Propellants for Naval Missiles, TA-NOTS-19-Re2d-456-6, Feb. 1950 by US NOTS (AGC Library 6U7161)
3. Investigation of Catalyst Beds, NASA-TN-D-1808, June 1963 (AGC Library 3U6212)
4. Advanced Catalyst Pack for H_2O_2 , Research Suggestion, Oct. 1967 by AGC (AGC Library 7U7702)

Compare the H_2O_2 reaction system with one utilizing LO_2/LH_2 .

The total impulse would be the same, i.e., 19,400,000 lb-sec. Specific impulse for this system would be approximately 450 sec. as opposed to 156 for the H_2O_2 system.

Total propellant required is:

$$1.94 \times 10^7 / .450 \times 10^3 = 43,200 \text{ lbs.}$$

Assume a mixture ratio of 5.5 and find weight of LO_2 and LH_2 .

$$W_{H_2} = \frac{W_T}{MR + 1} = \frac{43,200}{6.5} = 6,650 \text{ lbs.}$$

$$W_{O_2} = W_T - W_{H_2} = 43,200 - 6,650 = 36,550 \text{ lbs.}$$

LH_2 could be tapped off the main fuel tank, but a pump would be required to produce sufficient chamber pressure to keep the thrust chamber size within reasonable limits. For this study, a pressurized supply of fuel separate from the main tank is considered. The volume of fuel required is roughly equivalent to the propellant for the H_2O_2 system, so the tank weight for that system, 10,500 lb., is used here.

Density of LO_2 is approximately 71 lb/ft³, therefore the tank volume would be:

$$36,550 / 71 = 514 \text{ ft}^3$$

neglecting spare propellant and ullage.

A spherical tank to contain the oxidizer would be approximately

$$r = \left(\frac{3V}{4\pi} \right)^{1/3} = \left(\frac{3 \times 514}{4 \times 3.14} \right)^{1/3} = 4.96 \text{ ft.}$$

Such a tank suitable for pressurization equipped with insulation would weight approximately 15,000 lbs.

Total weight of the LO_2/LH_2 system including tankage, lines, controls, and thrust chambers would be approximately as follows:

Propellant	50,000 lbs.
Tanks & Lines	18,500
Thrust Chambers & Supports	<u>1,000</u>
	69,500 lbs.

Compare this to the H_2O_2 system:

Propellant	123,000 lbs.
Tanks & Lines	11,000
Thrust Chambers & Supports	<u>1,500</u>
	135,500 lbs.

The above ROM weights do not consider added weight of interstage, structures, tank supports, pressurization systems, and spare propellant.

Development costs of the two systems would not be significantly different. There would be problems in combustion stability and cooling with the LO_2/LH_2 system while catalyst development would probably be the largest problem with H_2O_2 .

Thrust Chamber Sizes

Throat area of the H_2O_2 thrust chamber.

First consider a single barrel.

$$F = 8000 \text{ lbs.}$$

$$P_c = 100, 300 \text{ psia}$$

$$\epsilon = 16, 30, 100$$

$$F = P_c A_t C_F$$

$$A_t = \frac{F}{P_c C_F}$$

P_c	ϵ	P_c/P_e	I_s	$C_F \text{ Theo}$	$.96 C_F$	A_T	D_T	D_e	L
100	16	220	159	1.65	1.58	50.6	8.0	32.0	60.0
	30	520	168	1.71	1.64	48.8	7.9	43.3	81.0
	100	*2650	*172	*1.75	1.68	47.6	7.8	78.0	146.0
300	16	220	159	1.65	1.58	16.9	4.8	19.2	35.8
	30	520	168	1.71	1.64	16.3	4.6	25.2	47.0
	100	*2650	172	*1.75	1.68	15.9	4.5	45.0	84.0

*Extrapolated.

Examples

$$A_T = \frac{8000}{100 \times 1.58} = 50.6 \text{ in}^2$$

$$D_e = D_T \sqrt{\epsilon} = 8.0 \sqrt{16} = 32.0 \text{ in.}$$

Assume a 15° half angle conic nozzle

$$L = D_e/2 \cot 15^\circ = 1.866 D_e$$

The reaction thrust would probably be generated by two thrust chambers, therefore, the sizes would be as follows: $F = 4000 \text{ lbs.}$

P_c	ϵ	P_c/P_e	I_s	$C_F \text{ Theo}$	$.96 C_F$	A_T	D_T	D_e	L
	16	220	159	1.65	1.58	25.3	5.7	22.8	42.5
100	30	520	168	1.71	1.64	24.4	5.6	30.8	57.4
	100	*2650	*172	*1.75	1.68	23.8	5.5	55.0	102.0
	16	220	159	1.65	1.58	8.5	3.3	13.2	24.6
300	30	520	168	1.71	1.64	8.2	3.2	17.5	32.6
	100	*2650	*172	*1.75	1.68	7.9	3.1	31.0	57.8

*Extrapolated.

Since the propellant tank would be pressurized, the chamber pressure would probably be no more than 300 psia. A higher pressure would necessitate a heavier wall on the tank and since there is no significant increase in performance with pressure, the two limiting parameters would be tank weight and nozzle exit diameter. The 300 P_c nozzle with a 30:1 exit area ratio would be approximately 32.6 inches and have a diameter of 17.5 inches.

The retro thrust chambers would have to be suspended to the NERVA nozzle near its exit. This would increase the NERVA envelop diameter by more than 35 inches.

A factor not considered is that of direction of thrust. It has been assumed that the retro would be 180° from the NERVA nozzle. However, this is not feasible due to impingement of exhaust upon NERVA tankage and engine components. The retro thrust chambers must be angled outward and therefore will lose effective retro thrust. The effective thrust will be the thrust chamber thrust times the cosine of the divergence angle.

Another factor of serious consequence is radiation reflection. We are going to extremes to eliminate lines and protrubances to minimize sources of radiation reflection. It will be hard to justify hanging two 18 inch diameter thrust chambers onto the nozzle.

VI. NERVA Engine Controllability
During Startup

Propellant Consumption During
Shutdown/Cooldown

M E M O R A N D U M

1 October 1968
7830:M3866
DB:rja

TO: S. A. Varga

FROM: D. Buden

SUBJECT: NERVA Engine Controllability During Startup

COPIES TO: E. Arbtin, J. J. Beereboom, R. V. Evleth, R. D. Huffman,
W. W. Madsen, B. Mandell, F. L. Openshaw, R. J. Pickering,
W. D. Stinnett, J. L. Smith, W. O. Wetmore, E. A. Sheridan

REFERENCE: (a) Memo 7810:0533M, S. A. Varga to E. A. Sheridan, dated
21 August 1968, subject: Controllability Characteristics
of NERVA Engine Control System

ENCLOSURE: (1) Trade Study on Engine Controllability During Startup

Enclosed is the trade study made on the controllability characteristics of the NERVA engine control system during startup.

D. Buden, Manager
Systems Analysis Section

CLASSIFICATION CATEGORY	
<i>Unclassified</i>	
<i>D. Buden</i>	<i>10/1/68</i>
CLASSIFYING OFFICER	DATE

TRADE STUDY ON ENGINE CONTROLLABILITY DURING STARTUP

1. Scope

This trade study presents the factors used to determine the "to be determined" items in the NERVA Program Requirements Document dated 5 June 1968, page 6 on controllability for startup. Items specifically investigated are: (a) impulse, (b) time, and (c) propellant consumption including chilldown propellant for both startup modes.

2. Functional and Technical Design Requirements

2.1 The NERVA Program requirements Document dated 5 June 1968, starting on page 3 under startup, states:

"Optional modes of bootstrap startup shall be provided. One mode shall provide highest possible integrated specific impulse within the (thermal) stress limitations of the reactor structures, during the starting transient the temperature rates shall be optimized. This mode shall follow the reactor structural(temperature)limits up to the rated temperature and maintain this temperature to rated thrust. Another mode shall provide the highest possible integrated thrust during the transient from flow initiation to rated operating conditions. This can be accomplished by increasing the engine chamber temperature at an optimum rate with chamber pressure increasing at an optimum rate. Notwithstanding, during the major portion of the transient ramps, the engine shall have the capability and the controllability of 150°R/sec chamber temperature and 50 psia/sec chamber pressure rate."

3. Design Approaches and Significant Design Characteristics

3.1 In order to establish the magnitude of the quantities required for controllability of the 75K NERVA engine during startup, various time profiles of chamber pressures and temperatures were used. The analysis

determined the flow rate, total flow, thrust, impulse, reactor power, decay power, and specific impulse for the inputted pressures and temperatures. Use was made of the nuclear afterheat removal program documented in Report RN-S-0487, Systems Analysis Digital Computer Programs. The startup profiles considered for this comparison are 40%, 60%, 100% and high thrust. The percentage thrust startup is defined as the percentage of full thrust that is achieved when chamber temperature first reaches full temperature. These paths are plotted as temperature vs. pressure on Figure 1. The relationships with respect to time are plotted on Figures 2 through 5. The initial chilldown of the engine for preconditioning before bootstrap is an extrapolation of XECF data to NERVA conditions. The temperature rate of increase above 1000°R is equal to 150°R/sec. Once the design chamber temperature is reached, chamber pressure is increased at 50 psi/sec to design conditions. The high thrust start assumes that the pressure will be **increased as rapidly as possible** along the open turbine power control valve line until design chamber pressure is reached.

Characteristics used for comparison were time, total flow, impulse, and average specific impulse, Table 1 presents a comparison of these parameters. The first block, Item A, presents a comparison up to the point where the chamber temperature first reaches 4500°R. It would indicate that the high thrust start also has the highest impulse up to this point. However, a much larger quantity of hydrogen flow rate is used so that the overall system performance will be poor. Item B in Table 1 presents a comparison of the performance for the first 3000 lbs of fluid used. It shows a clear advantage to the high specific impulse starts--the lower the thrust point achieved when design

COMPARISON OF THRUST STARTUP
TEMPERATURE RISE = 150°R/SEC

	Thrust			
	<u>40%</u>	<u>60%</u>	<u>100%</u>	<u>High</u>
A. Temperature = 4500°R				
Time, sec	103.5	103.5	103.5	103.5
Total flow, lb	1,913	2,176	2,698	3,234
Impulse, lb/sec	783K	962K	1,316K	1,631K
Avg I_{sp} $\frac{\text{lbf-sec}}{\text{lbm}}$	409	442	488	504
B. Total Flow = 3,000 lb				
Time	117	113	107	101
Total flow	3,000	3,000	3,000	3,000
Impulse	1,680K	1,642K	1,566K	1,444K
Avg I_{sp}	558	546	520	481
C. Time = 300 sec				
Time	300	300	300	300
Total flow	19,941	20,321	20,906	21,443
Impulse	15,657K	15,931K	16,339K	16,653K
Avg I_{sp}	785.1	784.0	781.5	776.6
D. Time = 3,000 sec				
Time	3,000	3,000	3,000	3,000
Total flow	270,142	270,521	271,107	271,643
Impulse	222,071K	222,346K	222,753K	223,068K
Avg I_{sp}	822.06	821.92	821.64	821.18
E. Temperature = 4500°R, Pressure = 450 psia				
Time	109	107	103	103
Total flow	2,242	2,436	2,698	3,234
Impulse	1,055K	1,176K	1,316K	1,631K
Avg I_{sp}	470.4	482.9	487.9	504.1

chamber temperature is reached, the better the overall performance that can be achieved. The average specific impulse for the first 3000 lbs, comparing the 40% thrust start and the high thrust start, is 77 sec better. In Items C and D, comparison is made for a short and a long run duration in order to demonstrate the significance of the startup transient on overall engine performance. For the short duration run, 8.5 sec gain in overall specific impulse is achieved. For the long duration run, less than one second of overall specific impulse is obtained. Item E gives the performance up to the point where design conditions are first achieved. It is seen that a significant increase in overall performance occurs from using the high specific impulse start compared to a high thrust start. Figure 6 is a plot of the average specific impulse vs. time for the various startups, while Figure 7 presents the total flow vs. time.

3.2 In order to determine the tolerances during the startup, (1) a review was made of the predictability of engine startup during NRX/EST; (2) variations of chamber temperature profiles during startup were run for the 60% thrust case; and (3) an uncertainty band of ± 5 sec was used for the settling time for the startup transient to reach full power.

3.2.1 Predictability of Engine Startup from NRX/EST

Analysis was made to determine the predictability of the engine startup based on the NRX/EST full-power runs. Predictability of the engine startup involves accurate predictions of the various engine-performance parameters, such as the time to reach full-power hold, total flow, total impulse, and average specific impulse. The NRX/EST EP-III and

EP-IV full-power runs were therefore analyzed to determine how close these parameters were in the actual startup from the planned startup.

Nozzle chamber temperature and pressure (T_c and P_c) profiles for the planned ramp and actual (as-run) ramp are shown in Figures 8 and 9 for the NRX/EST EP-III and EP-IV full-power runs respectively. Since durations of the intermediate power holds were not specified for the planned ramp, the time to reach full-power hold from start of the engine flow in the planned ramp was assumed to be identical to that of the actual ramp. T_c and P_c profiles for the planned startup were based on the test prediction from RN-S-0232* during the bootstrap phase and on the temperature-pressure demand schedule (P_c slaved to T_c) during the programmed ramp phase. The planned profiles were used at higher power because the tests were not run in the same manner as the predictions were made.

Various assumptions were made to simplify the calculations:

(1) total flow and impulse and average I_{sp} are for the flows through the nozzle only; (2) specific heat ratio, k , for the nozzle flow is constant at 1.32; (3) flow through the nozzle with the area ratio 10:1 discharges into a vacuum so that the thrust coefficient, C_F , is constant at 1.68. These assumptions, however, will not have any significant effect on the validity of the engine startup predictability in terms of the percent deviations of the actual startup to the planned startup.

The nozzle flow rates and thrusts were calculated at a sufficient number of time points. For isentropic flow through the

*RN-S-0232, NRX/EST Test Prediction Report, Revisions 5 and 6,
28 February 1966 and 14 March 1966

nozzle:

$$\dot{w} = P_c A_E Z / (T_c)^{0.5} = 7.9757 P_c / (T_c)^{0.5}$$

where P_c is the nozzle chamber pressure in psia, T_c is the nozzle chamber temperature in °R, A_E is the effective nozzle throat area (58.05 in.² for the NRX/EST nozzle), and Z equals $[(kg/R) (2/k+1)^{k+1/k-1}]^{0.5}$. With a constant k value of 1.32, Z equals 0.1374. Thrust can be calculated from:

$$F = C_F A_t P_c = 99.82 P_c$$

where C_F is 1.68 for the k of 1.32 and the 10:1 area ratio nozzle, with the flow discharging into a vacuum and throat area A_t equals 59.4 in.².

The calculated flow rate and thrust were then plotted as a function of time to obtain the total flow and impulse by graphical integrations. Figure 10 presents the flow and thrust profiles for the planned and actual ramps during the NRX/EST EP-IV startup to the full-power hold. Results of these calculations are summarized in Table 2. The average I_{sp} is obtained from dividing the total impulse through the nozzle by the total nozzle flow and represents, therefore, the average nozzle I_{sp} during the startup to reach full-power hold. Deviation of the average I_{sp} of the actual ramp from the planned ramp is less than one percent, although the deviations of the total flow and impulse are as much as 3.5 percent. This is because the deviations in the total flow and impulse are about the same magnitude and tend to cancel each other when the average I_{sp} is calculated.

The NRX/EST startups indicate that the actual (as-run) ramp closely follows the scheduled ramp with the predictable delay and overshoot characteristics and that the intermediate power holds can be held at the planned hold points. It is, therefore, concluded that the

TABLE 2

COMPARISON OF PLANNED AND ACTUAL ENGINE
STARTUPS DURING NRX/EST FULL POWER RUNS

	EP-III			EP-IV		
	<u>Planned Ramp</u>	<u>Actual Ramp</u>	<u>(Actual/Planned)</u>	<u>Planned Ramp</u>	<u>Actual Ramp</u>	<u>(Actual/Planned)</u>
Time to Reach Full Power Hold, Sec.*(1)	248.5	248.5	1.0	252	252	1.0
Total Nozzle Flow, lb	10,691	11,024	1.031	7,106	7,010	0.986
Total Impulse Through Nozzle, Lb-sec.	7.242×10^6	7.495×10^6	1.035	4.264×10^6	4.219×10^6	0.990
Average I_{sp} , Sec.	677.4	679.9	1.004	600.1	601.9	1.003

*(1) Since deviations of the intermediate power holds were not specified for the planned ramp, the time to reach full power hold from start of the engine flow in the planned ramp was assumed to be identical to that of the actual ramp.

predictability of the engine startup with less than one percent error seems possible with the anticipated further improvements in the engine control and in the computer programs simulating the engine system.

3.2.2 Predictability for 60% Thrust Case

Figure 11 shows the profiles used in these cases. Various combinations of temperature overshooting and undershooting with and without time lags during the ramp were analyzed. Results are presented in Table 3. Looking at Item E, the largest deviation in flow is four percent.

3.2.3 Settling Time

It has been estimated by the controls design personnel that the settling time during the startup is ± 5 sec. A summary of the tolerances shows that this becomes the controlling factor during the startup transient.

3.3 Effect of Ramp Rate During Startup

Analysis was made comparing $100^{\circ}\text{R}/\text{sec}$ startup vs. a $150^{\circ}\text{R}/\text{sec}$ startup on a 60% thrust start. The profiles are shown in Figure 12. Results are shown in Table 4. From Item E, it is seen that the higher ramp increases the overall average specific impulse 26 sec during startup.

TABLE 3

SUMMARY OF THRUST STARTUP FOR 60% THRUST WITH TEMPERATURE DEVIATIONS

	<u>Time Lag of 2-sec Temp. to 4600°R</u>	<u>Temp. to 4600°R(no time lag)</u>	<u>Temp. Lag 100°R to 4500°R</u>	<u>Temp. Lag 100°R to Temp. 4600°R</u>	<u>Temp. Lag 100°R during Precon- ditioning and Start to 4500°R</u>	<u>Temp. Lag 100°R during Precon- ditioning and Start to 4400°R</u>
A. Temperature = 4500°R						
Time, sec	105	103	104	104	104	103
Total Flow, lb	2,116	1,983	2,029	2,029	2,142	2,121
Impulse, lb-sec	882K	830K	847K	847K	850K	833K
Avg I _{sp} , $\frac{\text{lb-sec}}{\text{lbm}}$	416.8	418.8	417.4	417.4	396.9	392.6
B. Total Flow = 3000 lb						
Time, sec	119	120	120	120	119	119
Total Flow, lb	3,000	3,000	3,000	3,000	3,000	3,000
Impulse, lb-sec	1,622K	1,683K	1,649K	1,660K	1,559K	1,549K
Avg I _{sp} , $\frac{\text{lb-sec}}{\text{lbm}}$	540.7	560.7	549.7	553.3	519.4	516.0
C. Time = 300 sec						
Time, sec	300	300	300	300	300	300
Total Flow, lb	19,527	19,438	19,683	19,464	19,796	20,022
Impulse, lb-sec	15,446K	15,438K	15,412K	15,431K	15,415K	15,399K
Avg I _{sp} , $\frac{\text{lb-sec}}{\text{lbm}}$	791.0	793.9	783.0	792.8	778.7	769.1
D. Time = 3000 sec						
Time, sec	3,000	3,000	3,000	3,000	3,000	3,000
Total Flow, lb	266,620	266,531	269,883	266,557	269,996	273,429
Impulse, lb-sec	222,132K	222,117K	221,827K	222,117K	221,830K	221,582K
Avg I _{sp} , $\frac{\text{lb-sec}}{\text{lbm}}$	833.2	833.4	821.9	833.3	821.6	810.4
E. Chamber Tc = 4600°R Pc = 450 psia						
	4600°R	4600°R	4500°R	4600°R	4500°R	4400°R
	450 psia	450 psia	450 psia	450 psia	450 psia	450 psia
Time, sec	118	118	118	118	118	118
Total Flow, lb	2,871	2,782	2,817	2,807	2,931	2,941
Impulse, lb-sec	1,514K	1,500K	1,498K	1,499K	1,502K	1,501K
Avg I _{sp} , $\frac{\text{lb-sec}}{\text{lbm}}$	527.5	539.2	531.9	534.0	512.4	510.3

TABLE 4

EFFECT OF RAMP RATE DURING STARTUP

60% Thrust

	Temp. Rise <u>150°R/sec</u>	Temp. Rise <u>100°R/sec</u>
A. Temperature = 4500°R		
Time, sec	103.5	114
Total Flow, lb	1,983	2,624
Impulse, lb-sec	830K	1,249K
Avg I _{sp} , $\frac{\text{lb-f-sec}}{\text{lbm}}$	419.0	475.6
B. Total Flow = 3000 lb		
Time, sec	120	119
Total Flow, lb	3,000	3,000
Impulse, lb-sec	1,671K	1,558K
Avg I _{sp} , $\frac{\text{lb-f-sec}}{\text{lbm}}$	557.0	519.2
C. Time = 300 sec		
Time, sec	300	300
Total Flow, lb	19,657	19,787
Impulse, lb-sec	15,413K	15,408K
Avg I _{sp} , $\frac{\text{lb-f-sec}}{\text{lbm}}$	784.1	778.7
D. Time = 3000 sec		
Time, sec	3,000	3,000
Total Flow, lb	269,857	269,987
Impulse, lb-sec	221,828K	221,823K
Avg I _{sp} , $\frac{\text{lb-f-sec}}{\text{lbm}}$	822.1	821.6
E. Temperature = 4500°R, Pressure = 450 psia		
Time, sec	109	118
Total Flow, lb	2,792	2,922
Impulse, lb-sec	1,499K	1,494K
Avg I _{sp} , $\frac{\text{lb-f-sec}}{\text{lbm}}$	537.0	511.3

4. Results and Conclusions

Table 5 shows the tolerance limits during startup and restart, based on a 30-psia tank pressure. The 60% thrust case was selected for the high specific impulse start because it approximates the current structural support system limit in the nuclear subsystem. The tolerances during the preconditioning cycle where the PSV is opened but prior to the opening of the turbine power control valve, is based on a 5-sec uncertainty on the positive side, and a negative 65-sec uncertainty. The large negative uncertainty is a result of the conditions of the engine from being in space for an undefined length of time. A long duration of time in space prior to initial firing of the engine could result in the turbopump and nozzle being virtually prechilled prior to the initial startup. The longer startup time would be associated with a warm-day start from suborbital conditions. The thrust buildup during the initial start is dominated by the ± 5 sec tolerance associated with the control system settling time. The restart case is based on initially being at 1500°R with the turbopump prechilled by the pulse cooldown.

TABLE 5
CALCULATED TOLERANCE LIMITS

	Initial Start				Restart	
	Preconditioning		Thrust Buildup		Thrust Buildup	
	Nominal	Tolerance	Nominal	Tolerance	Nominal	Tolerance
Impulse, thousand-lb-sec						
60% thrust	216	+ 35 -216	960	+340	805	+340
High thrust	216	+ 35 -216	1415	+385	1225	+385
Time, sec						
60% thrust	65	+ 5 sec -65 sec	42	+ 5 sec	24	+ 5 sec
High thrust	65	+ 5 sec -65 sec	39	+ 5 sec	20	+ 5 sec
Total flow, lb						
60% thrust	835	+125 -835	1600	+410	1145	+410
High thrust	835	+125 -835	2395	+475	1865	+475

FIG. 1

Fig. 2

75-K NERVA ENGINE STARTUP PROFILE
60% THRUST

FIG. 3

FIG. 4

FIG. 5

FIG. 6

75-K NERVA ENGINE STARTUP
TOTAL FLOW CURVE
100:1 Area Ratio

FIG. 7

NOZZLE CHAMBER TEMPERATURE AND PRESSURE INITIAL RUN VERSUS PLANNED RAMP (WORK TEST, FC-III, FULL POWER RUN)

7830: 113866

Page 20 of 24

FIG. 8

NOZZLE CHAMBER TEMPERATURE AND PRESSURE

7830: M3866

Pg. 21 of 24

ACTUAL RAMP VERSUS PLANNED RAMP
(INRS) ST. 60-11 FIXED DEVM. START)

FIG. 9

NOZZLE FLOW RATE AND THRUST

PERCENT FLOW VERSUS PERCENT FLOW
(HYP/ESI, FI-IX, FIXED, ERS, START)

7832, 113866

PO 22, H 24

FIG. 10

FIG. 11

FIG. 12

M E M O R A N D U M

To: S. A. Varga ✓ 1 October 1968
From: G. D. Hart 7810:1002M
GDH:se
Subject: Propellant Consumption During Shutdown/Cooldown
Distribution: T. Pasternak, R. J. Pickering, R. L. Rishel
Enclosure: (1) Propellant Consumption During Shutdown/Cooldown

Enclosure (1) is a tabulation of the preliminary values for propellant consumption during shutdown and cooldown currently being used in the Mission Analysis Program for the reference missions.

The values represent the best current estimate of available control ability.

G. D. Hart

G. D. Hart
NERVA Operational Requirements Section
Nuclear Rocket Operations

PROPELLANT CONSUMPTION DURING SHUTDOWN/COOLDOWN

	Burn Time (Sec)	Cooldown Time (Min)	Shutdown Lbs Lh2	Cooldown* Lbs LH ₂	Total Lbs LH ₂	Tolerance
<u>Mission A</u>						
Phase 1	761	300	967	4540	5507	+25%
2	428	600	967	4257	5223	"
3	332	300	967	3701	4668	"
4	344	420	967	3868	4835	"
Sum					20,233	
<u>Mission B</u>						
1	1227	150	967	4472	5439	+25%
2	414	300	967	3904	4871	"
3	554	3	967	1188	2155	"
4	150	180	848	1791	2639	"
Sum					15,104	
<u>Mission CI</u>						
1	1200	150	967	4452	5418	+25%
2	1560	1900	848	5046	5894	"
Sum					11,312	
<u>Mission CII</u>						
1	2760	2840	848	7996	8844	+25%
<u>Mission D</u>						
1	1896	2040	848	5743	6592	+25%
2	1693	1980	848	5337	6185	"
3	1707	1980	848	5363	6211	"
Sum					18,988	

* Cooldown is here defined as "time after SCRAM until 0.025 Mw decay power"
Chamber temperature is 1500°R during normal cooldown,
2500°R during terminal cooldown.

VII. Components Performance/Design
Characteristics

INTER-OFFICE MEMO

AEROJET-GENERAL CORPORATION®

TO: S. A. Varga

FROM: L. A. Shurley

SUBJECT: Components Performance/Design Characteristics

DATE 27 August 1968
 LAS:nla
 7750:M0367

DISTRIBUTION: E. Arbtin, J. J. Beereboom, H. J. Bronner, D. Buden, R. V. Evleth, C. M. Kawashige, I. L. Odgers, D. L. Petite, R. J. Pickering, J. L. Smith, W. D. Stinnett, W. O. Wetmore

Reference: (a) Memorandum 7810:0539M dated 21 August 1968, S. A. Varga to I. L. Odgers, subj: Component Performance Input for Systems Analysis

(b) Memorandum 7810:0479M dated 29 May 1968, T. Pasternak to S. A. Varga, subj: Weight to I_{sp} Trade-Off Factors

Enclosure: (1) Memorandum 7750:M0293 dated 23 April 1968, L. A. Shurley to S. A. Varga, subj: Component Parameter Input for System Analyses

(2) Memorandum 7750:M0299 dated 3 May 1968, L. A. Shurley to S. A. Varga, subj: Correction to Memorandum 7750:M0293

(3) Memorandum 7750:M0331 dated 1 July 1968, H. J. Bronner to L. A. Shurley, subj: Skirt Extension System Operational Characteristics

(4) Figures 1, 2, 3, and 4 - Nozzle Area Ratio Optimization and Performance

The material depicted in Enclosures (1) through (4) was compiled by Section 7750 and represents partial fulfillment of your request per Reference (a), for component performance input in general.

You may notice that with the exception of work related to area ratio optimization, Enclosure (4), information pertaining to other thrust chamber components, lines included, has been in your possession for some time. Enclosures (1), (2), and (3) containing the information of interest to you are resubmitted for your convenience.

Work on nozzle area ratio optimization study for 75K thrust engine was not undertaken until August 15, 1968 (the date a copy of Reference (b) was received), because the undersigned was not placed on the original distribution list.

CLASSIFICATION CATEGORY
 UNCLASSIFIED

8-27-68

27 August 1968
7750:M0367

The area ratio optimization studies were based on a set of trade-off parameters, as defined for four different missions, May 29, 1968, Reference (b), and again as redefined August 21, 1968, Reference (a). The skirt-skirt extension interface was held constant at $A_e/A_t = 24:1$. You will notice that a distinction between metallic, tubular construction and graphite skirt extension has been made, and that approximately 800 lbs of useful allowable engine weight gain favoring the graphite construction has been identified.

96.5% nozzle efficiency has been assumed throughout these analyses. Studies related to nozzle performance losses attributable to thermal and viscous losses are now in progress and are scheduled for completion September 24, 1968. Not included in these studies are effects of vibration which are known to downgrade the performance but are very difficult to assess.

It may also be of significance for you to know that we were unable to find a 70:1 nozzle contour compatible with 825 sec. engine I_s (hot bleed) requirement. Best estimates indicate that 4-6 second performance deficiency can be expected.

L. A. Shurley, Manager
Thrust Chamber Section
Engineering Department
Nuclear Rocket Operations

Approved:

I. L. Odgers, Manager
Engineering Department
Nuclear Rocket Operations

Enclosure (1)
7750:M0367
27 August 1968

INTER-OFFICE MEMO

AEROJET-GENERAL CORPORATION®

TO S. A. Varga
FROM L. A. Shurley
SUBJECT Component Parameter Input for System Analyses

DATE 23 April 1968
REW:nla
7750:M0293

DISTRIBUTION E. Arbtin, J. J. Beereboom, H. J. Bronner, R. V. Evleth, G. D. Hart,
C. M. Kawashige, L. H. Larson, W. W. Madsen, I. L. Odgers, D. L. Petite,
R. J. Pickering, L. M. Smalec, J. L. Smith, W. D. Stinnett, J. L. Watkins,
R. B. Wright, W. O. Wetmore, D. Buden, D. W. Whittlesey

Enclosure: (1) Computer Printout No. A8Y054JOS for Main Nozzle at 75K Conditions
(2 Copies to R. J. Pickering only)
(2) Material W_T and Cg for Nozzle, Skirt and Skirt Extension
(3) 75K Nozzle Jacket Layout
(4 Copies to R. J. Pickering only)

In answer to your request for component performance input, current data on various thrust chamber components are shown below:

A. Main Nozzle and E Skirt

Nozzle performance based on conditions from RN-PD-P-1110K are shown in Enclosure (1) (Computer Printout No. A8Y054JOS). It should be noted that this computer program does not include a pressure drop of approximately 57 psi for orificing of tube outlets to compensate for additional pressure drop of tubes intersected by the hot bleed port manifold.

Figure 1 shows the loss coefficients for the nozzle and nozzle E Skirt.

Current nozzle E Skirt performance from Computer Job 8047 dated 21 March 1968, reference A8AA039TJI, is based on 219 tubes and the following conditions:

	Flow (Lb/sec)	Pressure (psia)	Temperature (°R)
Skirt Inlet	63.5	920	55
Skirt Outlet	63.5	903	83
Nozzle Chamber	87.1	450	4500

Loss coefficients for the nozzle E Skirt as shown in Figure 1 are:

- Inlet manifolding to the first station in the coolant tube 2.14
- Turning loss at the lower manifold 1.6
- Loss of skirt outlet manifolding into nozzle inlet manifold 2.22

CLASSIFICATION CATEGORY

23 April 1968

Nozzle E Skirt performance from Computer Printout A8AA039TJI:

<u>AXIAL DISTANCE</u>	<u>FLOW PATH LENGTH</u>	<u>CUMULATIVE FLOW PATH LENGTH</u>	<u>COOLANT FLOW AREA</u>	<u>WETTED PERIMETER</u>	<u>HOT SIDE DIAMETER</u>	<u>AREA RATIO</u>
41.14	0	0	.354	2.507	27.28	6
43.14	2.28	2.28	.373	2.501	29.46	7
45.11	2.25	4.53	.389	2.494	31.50	8
48.91	4.24	8.77	.414	2.482	35.22	10
52.54	4.00	12.77	.434	2.472	38.58	12
56.10	3.89	16.66	.446	2.460	41.66	14
59.56	3.75	20.41	.456	2.450	44.54	16
63.02	3.75	24.16	.460	2.435	47.24	18
66.51	3.75	27.91	.463	2.428	49.80	20
70.06	3.71	31.62	.463	2.416	52.24	22
73.27	3.54	35.16	.460	2.403	54.56	24

B. Nozzle Skirt Extension

Current skirt extension performance from Computer Job 8047 dated 18 March 1968, reference A8U040JOS, is based on 486 tubes and the following conditions:

	<u>Flow (lb/sec)</u>	<u>Pressure (psia)</u>	<u>Temperature (°R)</u>
Skirt Extension Inlet	4.6	30	1130
Skirt Extension Outlet	4.6	17.4	1500
Nozzle Chamber	87.1	450	4500

The loss coefficient for entrance manifolding into the skirt extension coolant tubes as shown in Figure 2 is 2.905.

Skirt Extension performance from Computer Printout A8U040JOS:

<u>AXIAL</u> <u>DISTANCE</u>	<u>FLOW PATH</u> <u>LENGTH</u>	<u>CUMULATIVE</u>		<u>COOLANT</u> <u>FLOW AREA</u>	<u>WETTED</u> <u>PERIMETER</u>	<u>HOT SIDE</u> <u>DIAMETER</u>	<u>AREA</u> <u>RATIO</u>
		<u>FLOW PATH</u> <u>LENGTH</u>	<u>FLOW PATH</u> <u>LENGTH</u>				
73.27	0	0	0	.126	1.282	54.56	24
74.87	1.70	1.70	1.70	.130	1.301	55.68	25
83.57	10.40	12.10	12.10	.154	1.411	61.00	30
92.40	10.40	22.50	22.50	.178	1.514	65.89	35
96.71	4.46	26.96	26.96	.189	1.558	68.14	37.44
103.31	6.78	33.74	33.74	.205	1.621	71.34	41.03
110.19	7.05	40.79	40.79	.223	1.690	74.45	44.69
117.36	7.35	48.14	48.14	.239	1.750	77.46	48.38
124.79	7.57	55.71	55.71	.256	1.810	80.35	52.05
132.52	7.86	63.57	63.57	.273	1.869	83.15	55.75
140.53	8.13	71.70	71.70	.289	1.919	85.82	59.38
148.84	8.42	80.12	80.12	.304	1.970	88.38	62.98
157.43	8.67	88.79	88.79	.320	2.020	90.81	66.49
166.17	8.80	97.59	97.59	.337	2.070	93.18	70.00

C. Lines

	<u>PDL</u>	<u>TTL</u>	<u>TDL</u>
Pressure (psia)	936	450	39.8
Temperature ($^{\circ}$ R)	55	1660	1330
Flow Area (in^2)	16.62	12.03	33.09
Line Length (in)	171.73	151.4	183.6
Line Thickness (in)	0.20*	0.0478	0.067
Loss Coefficients (Excluding valves)	1.06**	1.99	1.20

*Thickness based on 1200 psia, 560 $^{\circ}$ R emergency cooldown conditions.
For 936 psia, 55.6 $^{\circ}$ R, the thickness required is 0.1495 in.

**This loss coefficient can be broken down as 0.3182 to SSC discharge point. See Figure 2 for location of line loss coefficients.

23 April 1968

The hot bleed port insert has not been designed as yet but it is expected that it will be similar to the Technology Bleed Port with approximately 40 psi hot gas pressure drop and approximately 90 psi diluent pressure drop.

It should be noted that nozzle and skirt values are still subject to minor changes due to tube smoothing.

Nozzle jacket thickness values versus developed length have not been tabulated but a 75K nozzle jacket layout is being given to Mr. R.J.Pickering.

Nozzle, skirt and skirt extension weights are shown in Enclosure (2).

L. A. Shurley
L. A. Shurley, Manager
Thrust Chamber Section
Engineering Department
Nuclear Rocket Operations

Approved:

I. L. Odgers
I. L. Odgers, Manager
Engineering Department
Nuclear Rocket Operations

NOZZLE & SKIRT FLOW DIAGRAM

LOSS COEFFICIENTS:

Pump Discharge Line:
(Excluding valves)

$$I \text{ to } J = .3182$$

$$J \text{ to } A = .7437$$

$$I \text{ to } A = 1.06$$

Turbine Inlet Line:

$$K \text{ to } L = 1.99^*$$

Turbine Discharge Line:

$$M \text{ to } N = 1.20$$

Skirt Extension Inlet Manifold:

$$N \text{ to } O = 2.905$$

*Does not include 40 psi drop
thru turbulence ring in hot
bleed port or ΔP of TPCV & TBV.

MAT'L : CRES 347

Item	Weight	X	Y	Z
1. FORWARD RING (incl 3.7 lbs bolts)	97.1	258.4	0	0
2. TORUS	82.6	258.8	0	0
3. TUBE BUNDLE	534.1	303.2	0	0
4. NOZZLE SECTION	51.2	347.9	0	0
TOTAL	815.0	296.2	0	0

Item	Weight	X	Y	Z
1. FORMED RING (Incl Bolts @ 6.3 lb.)	131.3	226.3	0	0
2. Torus	41.5	224.6	-3.4	0
3. TUBE BUNDLE	170.8	244.3	0	0
4. APT RING	36.2	258.0	0	0
TOTAL	379.8	237.4	- .4	0

75 K: NERVA

NOZZLE

Material: Hastelloy X

Item	WT	X	Y	Z
1. Core Suppt Ring	201.3	189.8	0	0
2. Flange $\frac{1}{2}$ Bolts	372.7	196.1	0	0
3. Convergent Cone	286.6	205.1	0.5	0
4. Divergent Cone	129.8	219.5	0	0
5. Aft Flange	100.9	235.1	0	0
6. Bleed Port	15.0	201.9	-16.1	0

TOTAL

1106.3 207.2 -1.1 0

Enclosure (2)
Page 3

Enclosure (2)
7750:M0367
27 August 1968

INTER-OFFICE MEMO

AEROJET-GENERAL CORPORATION®

TO: S. A. Varga
FROM: L. A. Shurley
SUBJECT: Correction to Memorandum 7750:M0293

DATE 3 May 1968
REM:nla
7750:M0299

DISTRIBUTION: E. Arbtin, J. J. Beersboom, H. J. Bronner, R. V. Evleth, G. D. Hart, C. M. Kawashige, L. H. Larson, W. W. Madsen, I. L. Odgers, D. L. Petite, R. J. Pickering, L. M. Srelec, J. L. Smith, W. D. Stinnett, J. L. Watkins, R. B. Wright, W. O. Wetmore, D. Duden, D. W. Whittlesey

Reference: (a) Memorandum 7750:M0293 dated 23 April 1968, L. A. Shurley to S. A. Varga, subj: Component Parameter Input for System Analyses

Enclosure (2) to Reference (a) was in error due to nozzle weight being based on the 106K nozzle jacket instead of 75K jacket thickness.

Page 3 of Enclosure (2) should show the total weight of the 75K NERVA nozzle as 963 lbs. Item 2 of the tabulation on this page (Flange and Bolts Weight) should be reduced to 326 lbs, and Item 3 (convergent cone) should be reduced to 190 lbs.

It should also be noted that the 1.1 dimension shown on Page 3 of Enclosure (2) was for the 106K nozzle and should be reduced to .85 for the 75K nozzle.

L. A. Shurley, Manager
Thrust Chamber Section
Engineering Department
Nuclear Rocket Operations

Approved:

I. L. Odgers, Manager
Engineering Department
Nuclear Rocket Operations

Enclosure (3)
7750:M0367
27 August 1968

INTER-OFFICE MEMO

AEROJET-GENERAL CORPORATION®

TO: L. A. Shurley
FROM: H. J. Bronner
SUBJECT: Skirt Extension System Operational Characteristics

DATE: 1 July 1968
EMT:nla
7750:M0331

DISTRIBUTION: J. J. Beereboom, W. E. Campbell, O. I. Ford, I. L. Odgers,
R. J. Pickering, E. M. Takemori, S. A. Varga, J. L. Watkins,
D. W. Whittlesey

Reference: (a) RN-PD-P-1110K dated 12 February 1968, Enclosure (5), Part I,
Rev. B., Page 7 of 7, NERVA Engine Steady State Values - 75K
NERVA

Enclosure: (1) Figure 1, 75K Skirt Extension - Minimum Turbine Exhaust Gas
Coolant Requirement Study
(2) Figure 2, 75K Skirt Extension - Coolant Tube Exhaust Nozzle
Performance
(3) Table I, 75K Skirt Extension - Optimum Coolant Tube Design
Information

A system study was conducted on the turbine exhaust gas cooled skirt extension to establish the following:

1. Minimum turbine exhaust gas coolant requirement, i.e., pressure, temperature, and flow rate to meet design limit (maximum hot gas wall temperature " T_{wg} " of 1500° F and maximum coolant velocity Mach number $(M) = 0.4$).
2. Optimum coolant tube design for design requirement of Reference (a).
3. Coolant tube exhaust nozzle performance for design requirement of Reference (a).

The results of the study are presented herein:

1. The minimum turbine exhaust conditions to operate the skirt extension without exceeding design limits is presented in Figure 1, 75K Skirt Extension - Minimum Turbine Exhaust Gas Coolant Requirement Study.

CLASSIFICATION CATEGORY	
CLASSIFICATION SYMBOL	DATE

1 July 1968
7750:M0331

2. The optimum coolant tube design was established from heat transfer studies using the design point operating conditions per Reference (a) and design limitations of maximum $T_{wg} = 1400^{\circ}\text{F}$ and maximum $M = 0.4$. The resultant optimized coolant tube design is tabulated in Table I, 75K Skirt Extension Optimum Coolant Tube Design Information.

3. Coolant tube exhaust nozzle performance was conducted and presented in Figure 2, 75K Skirt Extension - Coolant Tube Exhaust Nozzle Performance. The two lower curves, "RAO Type", and "15° Conical Type" represent nozzles where weight calculations for the divergence section past an area ratio of 4 to 1 employed material density of molybdenum. The two upper curves represent nozzles made entirely from CRES 347 where the inside wall exposed to the engine exhaust is eliminated past the 4 to 1 area ratio.

H. J. Bronner, Supervisor
Thrust Chamber Section
Engineering Department
Nuclear Rocket Operations

Approved for distribution:

L. A. Shurley
L. A. Shurley, Manager
Thrust Chamber Section
Engineering Department
Nuclear Rocket Operations

I. L. Odgers
for I. L. Odgers, Manager
Engineering Department
Nuclear Rocket Operations

75K SKIRT EXTENSION
MINIMUM TURBINE EXHAUST
CAS COOLANT REQUIREMENT
STUDY

TURBINE EXHAUST FLOW RATE, LB/SEC

4.8
4.4
4.0
3.6
3.2
2.8

SAFE
OPERATING
RANGE

$P_{T8} = 50$ PSIA

10

30

20

MAXIMUM $T_{wg} = 1500^{\circ}\text{F}$
MAXIMUM MACH NO. = 0.4
SKIRT/SKIRT EXTENSION INTERFACE $C = 24:1$

1000

1100

1200

1300

1400

TURBINE EXHAUST TOTAL TEMPERATURE, $^{\circ}\text{R}$

FIGURE #2.

75% SPIRT EXTENSION
COOLANT TUBE EXHAUST NOZZLE
PERFORMANCE

REF. 1 SECOND Isp = 136.163

TABLE I
OPTIMUM COOLANT TUBE DESIGN INFORMATION
(REF. RN-PD-P-1110K dated 12 February 1968)

Enclosure (3)
7750:MO331
28 June 1968

RAO NOZZLE CONTOUR				OVAL COOLANT TUBE GEOMETRY							
STA	$\mathcal{E}$	AXIAL DISTANCE "X" in.	HOT GAS DIAMETER "D-C" in.	HOT GAS SIDE RADIUS "Rg" in.	AMBIENT SIDE RADIUS "Ra" in.	OVAL FLAT SIDE HEIGHT "LTU" in.	DEVELOPED LENGTH "L _{Dev} " in.	FLOW AREA "A _F " in ²	WETTED PERIMETER "W _P " in.	EQUIVALENT HYDRAULIC DIAMETER "D _e " $(D_e = \frac{4A_F}{W_P})$	TUBE THICKNESS "t _m "
1	24	73.27	54.56	0.177	0.178	0.120	0.00	0.126	1.286	0.392	0.012
2	25	74.87	55.68	0.181	0.182	0.120	1.70	0.130	1.305	0.393	0.012
3	30	83.57	61.00	0.198	0.199	0.120	10.40	0.154	1.412	0.436	0.012
4	35	92.40	65.89	0.214	0.215	0.120	10.40	0.178	1.512	0.471	0.012
5	37.44	96.71	68.14	0.222	0.222	0.120	4.46	0.189	1.559	0.485	0.012
6	41.03	103.31	71.34	0.232	0.233	0.120	6.78	0.205	1.625	0.505	0.012
7	44.69	110.19	74.45	0.242	0.243	0.120	7.05	0.223	1.685	0.529	0.012
8	48.38	117.36	77.46	0.252	0.253	0.120	7.35	0.239	1.751	0.546	0.012
9	52.05	124.79	80.35	0.261	0.262	0.120	7.57	0.256	1.808	0.566	0.012
10	55.75	132.52	83.15	0.270	0.271	0.120	7.86	0.273	1.864	0.586	0.012
11	59.38	140.53	85.82	0.279	0.280	0.120	8.13	0.289	1.921	0.602	0.012
12	62.98	148.84	88.38	0.288	0.288	0.120	8.42	0.304	1.974	0.616	0.012
13	66.49	157.43	90.81	0.295	0.296	0.120	8.67	0.320	2.021	0.633	0.012
14	70	166.17	93.18	0.303	0.304	0.120	8.80	0.337	2.072	0.651	0.012

RAO NOZZLE PERFORMANCE VS. AREA RATIO

HYDROGEN
 $P_c = 450 \text{ PSIA}$
 $T_c = 4500^\circ \text{R}$

Enclosure (4)
 2/20/68
 2/20/68
 2/20/68
 2/20/68

2/7/68

RAO NOZZLE PERFORMANCE vs. AREA RATIO

HYDROGEN
 $P_c = 450 \text{ PSIA}$
 $T_c = 4500^\circ \text{R}$

SPECIFIC IMPULSE " I_s " (SECONDS)

THEORETICAL (INCLUSIVE OF DISSOCIATION)

EXPECTED ($\eta = 0.965$)

AREA RATIO " E "

2/7/68

Enclosure (4)
 Figure 1
 7:00:236
 21 August 1968

REF MEMO 7310:0370A 29A29/1918

75K NERVA
OPTIMUM RAO NOZZLE
PERFORMANCE UTILIZING
CRES 347 SKIRT EXTENSION

MISSION	A	B	C	D
ENGINE WEIGHT PENALTY (LBS/1 SEC I_{sp})	154	150	152	208
INTERSTAGE LENGTH PENALTY (INCH/1 SEC I_{sp})	31	16	34	32.5

Enclosure (1)
Figure 2
750:10367
27 August 1968

8/21/68

REF. MEMO 7810:0479M 29 MAY 1968

75K NERVA
 OPTIMUM RAO NOZZLE
 PERFORMANCE UTILIZING
 GRAPHITE SKIRT EXTENSION

MISSION	A	B	C	D
ENGINE WEIGHT PENALTY (LBS/1 SEC Isp)	154	150	152	208
INTERSTAGE LENGTH PENALTY (INCH/1 SEC Isp)	31	16	34	32.5

Enclosure (4)
 Figure 3
 750:00:00
 27 August 1968

8/24/68

REF. MEMO 7810.0539M 21 AUGUST 1968

75K NERVA
OPTIMUM RAD NOZZLE
PERFORMANCE UTILIZING
GRAPHITE OR CRES 347
SKIRT EXTENSION

MISSION	COMBINATION
ENGINE WEIGHT PENALTY (LBS/1 SEC I_{sp})	158
INTER STAGE LENGTH PENALTY INCH/1 SEC I_{sp}	27.5

8/23/68

Enclosure (4)
Figure 4
7750.10367
27 Aug 68

VIII. Mission Input for NERVA
Environmental Definition

MEMORANDUM

To: S. A. Varga
From: G. D. Hart
Subject: Mission Input For NERVA Environmental Definition
Distribution: E. Arbtin, J. J. Beereboom, R. V. Evleth, G. D. Hart,
T. Pasternak, R. J. Pickering, R. L. Rishel, J. L. Smith,
W. D. Stinnett
Enclosure: (1) Mission Altitude Histories

30 August 1968
GDH:se:7810:0556M
Ext.7480:Bldg.2019A2

Enclosure (1) is preliminary mission data to be used in defining the NERVA engine environments for the four reference missions. The altitudes and time intervals for each phase of Mission B are presented. These preliminary data together with the additional information required for the definition of the missions A, C and D can be used to represent all missions.

Detailed altitude histories are being generated for the preparation of Requirement Allocation Sheets.

G. D. Hart

G. D. Hart
Systems Engineering
NERVA System Section

Approved By:

S. A. Varga

S. A. Varga, Manager
NERVA System Section
Nuclear Rocket Operations

CLASSIFICATION CATEGORY	
<i>Unclassified</i>	
CLASSIFYING OFFICE	DATE
<i>Pickering</i>	<i>8/30/68</i>

MISSION ALTITUDE HISTORIES

Mission B Altitude History

<u>Time Delta</u>	<u>Altitude (NM)</u>	
470 Sec.	0-92	Launch to suborbit
1227 Sec.	92-205	Suborbit burn to elliptical (118 x 3667 nm) earth orbit
74 Min.	205-3667	Coast to apogee
76 Min.	3667-478	Coast to restart
414 Sec.	478-650	Depart elliptical orbit burn
5 hr.	650-19,000	Coast to synchronous orbit
554 Sec.	19000-19350	Enter synchronous orbit burn
3 Min.	19,350	Coast for payload separation
150 Sec.	19350-19500	Final burn to higher orbit

Exceptions For Missions A, C, and D

1. 30 days in Earth-synchronous orbit (Synchronous-Orbit Ferry)
2. 90 days in circular-earth rendezvous orbit at 485 Km (leave earth stage of Mars Vehicle).
3. No. 2 plus 200 days in transit from Earth orbit to Mars orbit (arrive Mars Stage of Mars Vehicle).
4. No. 3 plus 30 days in circular Mars rendezvous orbit at 500 km altitude (leave Mars Stage of Mars Vehicle).

IX. 75K NERVA Engine Design Point
Tolerance Investigation

M E M O R A N D U M

16 September 1968
7830:M3846:DLP:rja

TO: S. A. Varga

FROM: D. L. Petite

SUBJECT: 75K NERVA Engine Design Point Tolerance Investigation

COPIES TO: J. H. Altseimer, J. J. Beereboom, D. Buden, W. W. Madsen,
B. Mandell, R. J. Pickering,
NERVA Engine Analysis Group personnel

REFERENCE: (a) NERVA Program Requirements Document, dated 5 June 1968,
page 2 paragraph a, pages 5-6 paragraph e

ENCLOSURE: (1) Tolerance Study Flow Diagram for the 75K NERVA Engine

(2) Tolerance Study for 75K NERVA Engine

(3) NERVA Engine Steady-State Value Tolerance Study (Run 1)

(4) " " " " " " (Run 3)

(5) " " " " " " (Run 7)

(6) " " " " " " (Run 9)

(7) " " " " " " (Run 40)

(8) " " " " " " (Run 41)

(9) " " " " " " (Run 42)

(10) " " " " " " (Run 43)

(11) 75K NERVA Engine Tolerance Study Thrust Chamber
Temperature vs. Thrust Chamber Pressure

(12) Nominal NERVA Engine Steady-State Values, 75K (Run 20)
 $I_{sp} = 825$ sec

CLASSIFICATION CATEGORY	
<i>[Signature]</i>	
CLASSIFYING OFFICER	DATE
<i>[Signature]</i>	9/16/68

DETERMINATION OF DESIGN POINT TOLERANCE

1. Scope

This study presents the evaluation of the design point tolerance based on the NERVA Program Requirements Document.

2. Functional and Technical Design Requirements

a. Per NERVA Program Requirements Document, dated 5 June 1968, page 2, Performance Requirements at Rated Conditions:

"Rated conditions shall be based on MSFC Spec. 356A (liquid) hydrogen, delivered at the tank outlet (upstream of the main propellant shutoff valve (PSOV)) at 30 psia, and saturation temperature, containing zero % vapor; with the engine at zero ambient pressure; zero gimbal angle; and single module (engine) operation, utilizing a hot bleed cycle to drive the turbopump and assuming a contoured nozzle expansion ratio of 100:1.

(1) Thrust, F	75,000 $\pm$ 2000 lb
(2) Min. Mixed Mean Chamber Inlet Temp., T_c	4500°R
(3) Nominal Chamber Pressure, P_c	450 psia
(4) Engine Weight Max.	(tbd) lb
(5) Shield Weight Max.	(tbd) lb."

b. Per NERVA Program Requirements Document, dated 5 June 1968, page 6, Controllability, Steady State:

"(a) Specific Impulse	$\pm$ 0.75%
(b) Thrust	$\pm$ 2%"

3. Approaches and Significant Characteristics

a. A flow diagram was prepared in order to interpret the functional design requirements given above [Enclosure (1)]. The specific impulse defines the maximum, nominal, and minimum range acceptable and the thrust branch gives the maximum, nominal, and minimum acceptable thrust values.

b. Utilizing the thrust levels of $75K \pm 2K$ (to account for as-built engine variations) and the nominal chamber pressure of 450 psia and minimum specific impulse of 825 sec, nozzle throat areas were determined. These areas provide the throat area variations allowable, assuming all other components meet the nominal values. The results obtained were:

Area No. 1 = 98.339 in.² (77K thrust)

Area No. 2 = 95.935 in.² (75K thrust)

Area No. 3 = 93.400 in.² (73K thrust).

The runs made used the preliminary engine system design program with a constant pump efficiency of 68% and turbine efficiency of 52%. The nominal NERVA engine design point 75K, $I_{sp} = 825$, is included for reference as Enclosure (12).

c. Control tolerances were then applied to thrust and specific impulse for the engine operation (e.g., nozzle areas) obtained. For example, the thrust of 77K was increased by 2% to give 78.54K and engine operating values were analyzed at engine specific impulses of 825 sec minimum, 831.2 sec nominal, and 837.4 sec maximum. Engine chamber pressure and chamber temperature values were calculated using the various combinations of specific impulse and thrust for each of the throat areas determined.

4. Comparison of Results

Using the requirements listed above, the following are the possible variations in design conditions. However, all parameters cannot be varied independently and still meet the PRD tolerances at design.

a.	I_{sp}	825 to 837.4 sec
b.	Thrust	71,540 to 78,540 lb
c.	Flow rate (total)	85.4 to 95.2 lb/sec
d.	Power level	1480 to 1650 Mw
e.	Pump ΔP	873 to 960 psid
f.	Turbine flow	3.70 to 4.41 lb/sec
g.	Core exit temperature	4500 to 4604°R
h.	Nozzle throat area	93.40 to 98.34 in. ²
i.	Chamber pressure	442 to 460 psia

The outlines defined by the maximum and minimum areas, as shown in Enclosure (11), indicate that the area defined by the specifications is virtually the same. With the maximum $I_{sp} = 837.4$ sec, the maximum envelope thrust chamber temperature of 4600°R is obtained. At the maximum thrust 78.54K and at the lower $I_{sp} = 825$ sec, the thrust chamber temperature drops below the 4500°R minimum at the lower thrust = 71.54K. The 4500°R lower temperature limit, per NERVA Program Requirements Document, forms the bottom line of the envelope. Two envelopes are shown in Enclosure (11): one for the minimum nozzle throat area and the other for the maximum nozzle throat area. The higher chamber pressure, 460 psia on the envelope, is defined from the smallest area with its associated highest thrust = 74.46K. The lowest chamber pressure, 442 psia on the envelope, is defined from the largest nozzle area with its associated lowest thrust = 75.46K.

Enclosures (3) through (10) define the corner points for the envelopes. Point 9 dips below the 4500°R minimum by about 20°R at the lower thrust chamber pressure because less proportional flow is required to drive the turbine, thus increasing the I_{sp} . The acceptable engine operating area on the P_c, T_c map on Enclosure (11) is crosshatched. Enclosure (2) is a summary of runs made, including the effect of reducing the tank pressure to 24 psia, its lower limit, in Runs 31, 32 and 33.

D. L. Petite
NERVA Engine Analysis Group

Approved:

D. Buden, Manager
Systems Analysis Section

TOLERANCE STUDY FLOW DIAGRAM FOR THE 75-K NERVA ENGINE

TOLERANCE STUDY FOR 75K NERVA ENGINE

Run	Option	Dewar		Thrust F (K-lb)	I _{sp} (sec)	P _c (psia)	T _c (°R)	Nozzle Throat Area	
		P _T (psia)	T _T (°R)					No.	(in. ²)
1	(P _c , T _c)	30	41	78.54	837.4	459.0	4603.6	1	98.339
2		30	41	78.54	831.2	460.19	4549.2	1	98.339
3		30	41	78.54	825.0	460.3	4493.4	1	98.339
4		30	41	75.0	837.4	450.9	4594.2	2	95.935
5		30	41	75.0	831.2	451.1	4538.4	2	95.935
6		30	41	75.0	825.0	450.0	4498.0	2	95.935
7		30	41	71.54	837.4	441.99	4591.8	3	93.400
8		30	41	71.54	831.2	442.23	4536.8	3	93.400
9		30	41	71.54	825.0	442.39	4481.47	3	93.400
19	(T _c , A _N)	30	41	77	825.0	450.0	4507.0	(1)	98.339
20		30	41	75	825.0	450.0	4497.7	(2)	95.935
21		30	41	73	825.0	450.0	4496.0	(3)	93.400
25	(P _c F)	30	41	78.54	825.79	460.35	4500.0	1	98.339
26		30	41	71.54	827.33	430.7	4500.0	2	95.935
27		30	41	75.0	826.59	463.7	4500.0	3	93.400
31		24	39.5	77	823.1	449.5	4500.0	1	98.339
32		24	39.5	75	824.17	449.5	4500.0	2	95.935
33		24	39.5	73	824.29	449.53	4500.0	3	93.400
37	(P _c , T _c)	30	41	75	837.4	463.0	4596.5	3	93.400
38		30	41	75	837.4	439.96	4596.72	1	98.339
39		30	41	75	825.0	440.39	4481.58	1	98.339
40	(P _c , T _c)	30	41	74.46	837.4	459.75	4596.7	3	93.400
41		30	41	74.46	825.0	461.5	4485.2	3	93.400
42		30	41	75.46	837.4	442.68	4592.0	1	98.339
43		30	41	75.46	825.0	443.16	4481.6	1	98.339

NERVA ENGINE STEADY STATE VALUES TOLERANCE STUDY(1) NERVA, A1=98.339 (1) THRUST=78540 LBS

ENGINE PC= 459.9 TC= 4551. THRUST 78,540 LBS, IMPULSE 837.4 SEC
 POWER LEVEL= 1650.5 IN TURBO P= 14500.000

TURBOPUMP OVERALL
 SHAFT POWER, HP 6.0671
 TURBINE EFFICIENCY 0.5200
 PUMP EFFICIENCY 0.6730

STATE POINT DESCRIPTION	FLOW LBS/SEC	TOTAL PRESSURE	TEMPERATURE	ENTHALPY BTU/LB
DEWAR OUTLET	93.76	20.4	39.2	-101.1
PUMP OUTLET	93.26	988.8	54.1	-39.8
TPA BEARING COOLANT	0.50	988.8	54.1	-39.8
STRUCTURE SUPPORT INLET	24.00	988.8	54.1	-39.8
STRUCTURE BYPASS INLET	6.00	988.8	54.1	-39.8
NOZZLE MANIFOLD INLET	63.26	954.8	54.1	-39.8
NOZZLE OUTLET	60.26	715.0	201.5	522.0
SKIRT OUTLET	0.20	715.0	201.5	522.0
BOLT COOLANT OUTLET	3.00	715.0	54.1	-39.8
REFLECTOR INLET	60.60	715.0	194.6	495.3
REFLECTOR OUTLET	60.60	657.6	309.6	946.8
PERIPHERAL SHIELD OUTLET	60.60	656.7	319.5	936.0
SUPPORT PLATE INLET	90.60	656.7	290.9	872.8
STRUCTURE SUPPORT OUTLET	24.00	656.7	276.4	814.9
CENTRAL SHIELD INLET	90.60	646.8	297.0	896.7
CORE INLET	90.60	634.3	302.0	916.7
CORE OUTLET	90.60	459.0	4603.6	17588.7
NOZZLE CHAMBER	89.40	459.0	4551.0	17326.1
MAIN NOZZLE EXIT	89.40	459.00	4512.9	17147.1
DILUENT BLEED INLET	2.65	715.0	194.6	495.3
HOT BLEED INLET	1.20	459.9	4551.0	17326.1
TURBINE INLET	3.85	300.6	1660.0	5729.6
MAIN TURBINE EXIT	3.85	39.2	1238.7	4239.0
TURBINE EXIT (AT COLLECTOR)	4.35	39.2	1097.8	3748.1
SKIRT EXTEN. EXIT (NOZZ. IN.)	4.35	7.2	1467.8	5037.5

NOZZLE TUBE Q=33857. SKIRT TUBE Q= 0. MAIN NOZZLE THROAT AREA 98.339 IN2
 MAIN NOZZLE IDEAL ISP 882.41, TPCV K/A2 VALUE 0.369159E 00, PUMP EXIT DENSITY 4.301
 MAIN NOZZLE ACTUAL ISP 857.88 AND TURBINE EXHAUST NOZZLE ACTUAL ISP 420.61

Enclosure (3)
 7830:M3846

NERVA ENGINE STEADY STATE VALUES TOLERANCE STUDY(3) NERVA, A=93.339 (1) THRUST=75540 L

ENGINE PC= 460.3 TC= 4439. THRUST 74528. LBS, IMPULSE 525.07 SEC
PC EX LEVEL= 1624.3 TURBOCOMP= 14600. RPM

TURBOPUMP OVERALL
SHAFT POWER, MW 6.1624
TURBINE EFFICIENCY 0.5200
PUMP EFFICIENCY 0.6730

STATE POINT DESCRIPTION	FLO	LBS/SEC	TOTAL PRESSURE	TEMPERATURE	ENTHALPY BTU/LB
DE AR OUTLET		95.19	29.4	39.2	-101.1
PUMP OUTLET		94.69	989.3	54.1	-39.7
TPA BEARING COOLANT		0.50	989.3	54.1	-39.7
STRUCTURE SUPPORT INLET		24.00	989.3	54.1	-39.7
STRUCTURE BYPASS INLET		6.00	989.3	54.1	-39.7
NOZZLE MANIFOLD INLET		64.69	955.3	54.1	-39.7
NOZZLE OUTLET		61.64	715.5	201.5	522.0
SKIRT OUTLET		0.00	715.5	201.5	522.0
BOLT COOLANT OUTLET		3.00	715.5	54.1	-39.7
REFLECTOR INLET		62.02	715.5	194.8	495.9
REFLECTOR OUTLET		62.02	668.1	305.4	930.0
PERIPHERAL SHIELD OUTLET		62.02	657.2	314.9	967.7
SUPPORT PLATE INLET		92.02	657.2	287.4	559.6
STRUCTURE SUPPORT OUTLET		24.00	657.2	273.0	801.4
CENTRAL SHIELD INLET		92.02	647.3	293.2	881.8
CORE INLET		92.02	634.7	298.1	901.2
CORE OUTLET		92.02	460.3	4493.4	17054.3
NOZZLE CHAMFER		90.77	460.3	4439.4	16799.6
MAIN NOZZLE EXIT		90.77	460.3	4399.0	16609.7
DILUENT BLEED INLET		2.66	715.5	194.8	495.9
HOT BLEED INLET		1.25	460.3	4439.4	16799.6
TURBINE INLET		3.91	305.3	1860.0	5729.8
MAIN TURBINE EXIT		3.91	39.7	1238.7	4239.0
TURBINE EXIT (AT COLLECTOR)		4.41	35.8	1099.8	3754.8
SKIRT EXTEN. EXIT (NOZZ. I.)		4.41	7.8	1469.8	5044.3

NOZZLE TUBE C=34657. SKIRT TUBE C= 0. AIN NOZZLE T-POAT AREA 98.339 I.2
MAIN NOZZLE IDEAL ISP 863.90, TPCV K/A2 VALUE 0.348751E 00, PUMP EXIT DENSITY 4.301
MAIN NOZZLE ACTUAL ISP 847.75 AND TURBINE EXHAUST NOZZLE ACTUAL ISP 420.88

NERVA ENGINE STEADY STATE VALUES TOLERANCE STUDY(7) NERVA, AN=93.405 (3) THRUST=71340 LB

ENGINE PC= 441.9 TC= 4537. THRUST 71325. LBS, IMPULSE 837.39 SEC
POWER LEVEL= 1503.5 MW TURBOPUMP= 18500. RPM

TURBOPUMP	OVERALL
SHAFT POWER, MW	5.0344
TURBINE EFFICIENCY	0.5200
PUMP EFFICIENCY	0.6730

STATE POINT DESCRIPTION	FLOW LBS/SEC	TOTAL PRESSURE	TEMPERATURE	ENTHALPY BTU/LB
DEWAR OUTLET	85.41	29.4	39.2	-101.1
PUMP OUTLET	84.91	902.0	52.8	-45.2
TPA BEARING COOLANT	0.50	902.0	52.8	-45.2
STRUCTURE SUPPORT INLET	24.00	902.0	52.8	-45.2
STRUCTURE BYPASS INLET	6.00	902.0	52.8	-45.2
NOZZLE MANIFOLD INLET	54.91	867.5	52.8	-45.2
NOZZLE OUTLET	51.91	683.7	200.2	517.9
SKIRT OUTLET	0.00	683.7	200.2	517.9
BOLT COOLANT OUTLET	3.00	683.7	52.8	-45.2
REFLECTOR INLET	52.71	683.7	192.2	487.1
REFLECTOR OUTLET	52.71	639.2	312.9	960.0
PERIPHERAL SHIELD OUTLET	52.71	629.0	323.4	1001.0
SUPPORT PLATE INLET	82.71	629.0	284.5	847.5
STRUCTURE SUPPORT OUTLET	24.00	629.0	255.8	733.3
CENTRAL SHIELD INLET	82.71	619.6	290.6	871.4
CORE INLET	82.71	607.6	295.6	891.3
CORE OUTLET	82.71	441.9	4591.8	17527.8
NOZZLE CHAMBER	81.71	441.9	4537.8	17270.0
MAIN NOZZLE EXIT	81.71	441.9	4504.2	17111.5
DILUENT BLEED INLET	2.20	683.7	192.2	487.1
HOT BLEED INLET	0.99	441.9	4537.8	17270.0
TURBINE INLET	3.20	249.4	1660.0	5728.3
MAIN TURBINE EXIT	3.20	32.5	1238.4	4237.6
TURBINE EXIT (AT COLLECTOR)	3.70	32.5	1072.3	3659.1
SKIRT EXTEN. EXIT (NOZZ. IN.)	3.70	0.5	1442.3	4949.2

NOZZLE TUBE Q=29239. SKIRT TUBE Q= 0. MAIN NOZZLE THROAT AREA 93.400 IN2
MAIN NOZZLE IDEAL ISP 880.92, TPCV K/A2 VALUE 0.614169E 00, PUMP EXIT DENSITY 4.294
MAIN NOZZLE ACTUAL ISP 856.44 AND TURBINE EXHAUST NOZZLE ACTUAL ISP 417.14

Enclosure (5)
7830:M3846

NERVA ENGINE STEADY STATE VALUES TOLERANCE STUDY(9) NERVA, AN=93.405 (3) THRUST=71540 LB

ENGINE PC= 442.3 TC= 4427. THRUST 71525. LBS, IMPULSE 824.97 SEC
POWER LEVEL= 1480.0 MATUPBCPUMP= 16500. RPM

TURBOPUMP	OVERALL
SHAFT POWER, MW	5.1127
TURBINE EFFICIENCY	0.5200
PUMP EFFICIENCY	0.6730

STATE POINT DESCRIPTION	FLOW LBS/SEC	TOTAL PRESSURE	TEMPERATURE	ENTHALPY BTU/LB
DEWAR OUTLET	86.70	29.4	39.2	-101.1
PUMP OUTLET	86.20	902.4	52.8	-45.2
TPA BEARING COOLANT	0.50	902.4	52.8	-45.2
STRUCTURE SUPPORT INLET	24.00	902.4	52.8	-45.2
STRUCTURE BYPASS INLET	6.00	902.4	52.8	-45.2
NOZZLE MANIFOLD INLET	56.20	867.9	52.8	-45.2
NOZZLE OUTLET	53.20	684.2	200.2	517.9
SKIRT OUTLET	0.00	684.2	200.2	517.9
BOLT COOLANT OUTLET	3.00	684.2	52.8	-45.2
REFLECTOR INLET	53.99	684.2	192.4	487.8
REFLECTOR OUTLET	53.99	639.7	308.4	942.2
PERIPHERAL SHIELD OUTLET	53.99	629.4	318.4	981.7
SUPPORT PLATE INLET	83.99	629.4	281.1	833.9
STRUCTURE SUPPORT OUTLET	24.00	629.4	252.8	721.2
CENTRAL SHIELD INLET	83.99	620.1	287.0	857.1
CORE INLET	83.99	608.0	291.8	876.4
CORE OUTLET	83.99	442.3	4481.2	17002.2
NOZZLE CHAMBER	82.94	442.3	4427.2	16746.9
MAIN NOZZLE EXIT	82.94	442.3	4392.4	16582.6
DILUENT BLEED INLET	2.20	684.2	192.4	487.8
HOT BLEED INLET	1.04	442.3	4427.2	16746.9
TURBINE INLET	3.25	233.3	1660.0	5728.4
MAIN TURBINE EXIT	3.25	33.0	1238.4	4237.6
TURBINE EXIT (AT COLLECTOR)	3.75	33.0	1074.5	3666.7
SKIRT EXTN. EXIT (NOZZ. IN.)	3.75	1.0	1444.5	4956.9

NOZZLE TUBE O=29962. SKIRT TUBE O= 0. MAIN NOZZLE THROAT AREA 93.400 IN2
MAIN NOZZLE IDEAL ISP 867.52, TPCV K/A2 VALUE 0.565880E 00, PUMP EXIT DENSITY 4.294
MAIN NOZZLE ACTUAL ISP 843.41 AND TURBINE EXHAUST NOZZLE ACTUAL ISP 417.45

Enclosure (6)
7830:M3846

NERVA ENGINE STEADY STATE VALUES TOLERANCE STUDY(40) NERVA, AN=93.4 (3) THRUST=744460

ENGINE PC= 460.0 TC= 4542. THRUST 74,460 LBS, IMPULSE 837.47 SEC
 POWER LEVEL= 1565.7 MW TURBOPUMP= 18500. RPM

TURBOPUMP	OVERALL
SHAFT POWER, MW	5.4175
TURBINE EFFICIENCY	0.5200
PUMP EFFICIENCY	0.6730

STATE POINT DESCRIPTION	FLOW LBS/SEC	TOTAL PRESSURE	TEMPERATURE	ENTHALPY BTU/LB
DEWAP OUTLET	88.96	29.4	39.2	-101.1
PUMP OUTLET	88.46	931.1	53.2	-43.4
TPA REAPING COOLANT	0.50	931.1	53.2	-43.4
STRUCTURE SUPPORT INLET	24.00	931.1	53.2	-43.4
STRUCTURE BYPASS INLET	6.00	931.1	53.2	-43.4
NOZZLE MANIFOLD INLET	58.46	896.9	53.2	-43.4
NOZZLE OUTLET	55.46	703.1	200.6	519.0
SKIRT OUTLET	0.00	703.1	200.6	519.0
BOLT COOLANT OUTLET	3.00	703.1	53.2	-43.4
REFLECTOR INLET	56.09	703.1	193.2	490.2
REFLECTOR OUTLET	56.09	658.6	311.1	952.9
PERIPHERAL SHIELD OUTLET	56.09	648.4	321.3	993.0
SUPPORT PLATE INLET	86.09	648.4	287.2	857.9
STRUCTURE SUPPORT OUTLET	24.00	648.4	264.4	767.4
CENTRAL SHIELD INLET	86.09	638.9	293.2	881.8
CORE INLET	86.09	626.8	298.2	901.8
CORE OUTLET	86.09	459.7	4596.7	17545.7
NOZZLE CHAMBER	85.02	459.7	4542.7	17287.0
MAIN NOZZLE EXIT	85.02	459.7	4507.0	17119.0
DILUENT BLEED INLET	2.36	703.1	193.2	490.2
HOT BLEED INLET	1.07	460.0	4542.7	17287.0
TURBINE INLET	3.44	268.2	1660.0	5728.8
MAIN TURBINE EXIT	3.44	35.0	1238.1	4236.8
TURBINE EXIT (AT COLLECTOR)	3.94	35.0	1082.3	3693.9
SKIRT EXTEN. EXIT (NOZZ. IN.)	3.94	3.0	1452.3	4984.2

NOZZLE TUBE Q=31197. SKIRT TUBE Q= 0. MAIN NOZZLE THROAT AREA 93.400 IN2
 MAIN NOZZLE IDEAL ISP 881.40, TPCV K/A2 VALUE 0.553848E 00, PUMP EXIT DENSITY 4.295
 MAIN NOZZLE ACTUAL ISP 856.90 AND TURBINE EXHAUST NOZZLE ACTUAL ISP 418.54

Enclosure (7)
 7830:M3846

NERVA ENGINE STEADY STATE VALUES TOLERANCE STUDY(41) NERVA, A₁=93.4 (3) THRUST=74446 LBS

ENGINE PC= 460.0 TC= 4455. THRUST 74,460 LBS, IMPULSE 825.0 SEC
 POWER LEVEL= 1545.0 TURBOPUMP = 18500. RPM

TURBOPUMP	OVERALL
SHAFT POWER, VW	5.4775
TURBINE EFFICIENCY	0.5200
PUMP EFFICIENCY	0.6730

STATE POINT DESCRIPTION	FLOW LBS/SEC	TOTAL PRESSURE	TEMPERATURE	ENTHALPY BTU/LB
DEWAR OUTLET	89.94	29.4	39.2	-101.1
PUMP OUTLET	89.44	931.1	53.2	-43.4
TPA BEARING COOLANT	0.50	931.1	53.2	-43.4
STRUCTURE SUPPORT INLET	24.00	931.1	53.2	-43.4
STRUCTURE BYPASS INLET	6.00	931.1	53.2	-43.4
NOZZLE MANIFOLD INLET	59.44	895.9	53.2	-43.4
NOZZLE OUTLET	56.44	703.1	200.6	519.0
SKIRT OUTLET	0.00	703.1	200.6	519.0
BOLT COOLANT OUTLET	3.00	703.1	53.2	-43.4
REFLECTOR INLET	57.08	703.1	193.3	490.6
REFLECTOR OUTLET	57.08	658.6	307.7	939.4
PERIPHERAL SHIELD OUTLET	57.08	648.4	317.6	978.3
SUPPORT PLATE INLET	87.08	648.4	284.4	846.6
STRUCTURE SUPPORT OUTLET	24.00	648.4	261.7	756.7
CENTRAL SHIELD INLET	87.08	638.9	290.3	870.2
CORE INLET	87.08	626.8	295.2	889.6
CORE OUTLET	87.08	461.54	4485.2	17128.0
NOZZLE CHAMBER	85.96	461.54	4431.2	16873.3
MAIN NOZZLE EXIT	85.96	461.54	4417.9	16698.9
DILUENT BLEED INLET	2.36	703.1	193.3	490.6
HOT BLEED INLET	1.11	460.0	4455.0	16873.3
TURBINE INLET	3.48	271.1	1660.0	5728.9
MAIN TURBINE EXIT	3.48	35.4	1238.1	4236.8
TURBINE EXIT (AT COLLECTOR)	3.98	35.4	1083.8	3699.1
SKIRT EXTEN. EXIT (NOZZ. IN.)	3.98	3.4	1453.8	4989.4

NOZZLE TUBE Q=31751. SKIRT TUBE Q= 0. MAIN NOZZLE THROAT AREA 93.400 IN²
 MAIN NOZZLE IDEAL ISP 870.78, TPCV K/A2 VALUE 0.534280E 00, PUMP EXIT DENSITY 4.295
 MAIN NOZZLE ACTUAL ISP 846.58 AND TURBINE EXHAUST NOZZLE ACTUAL ISP 418.75

Enclosure (8)
 7830:M3846

NEVA ENGINE STEADY STATE VALUES TOLERANCE STUDY(42) NEVA, AN=98.339 (1) THRUST=75460

ENGINE PC= 442.8 TC= 4542. THRUST 75,460 LBS, IMPULSE 837.4 SEC
POWER LEVEL= 1586.7 MWTUPROPEL= 15500. RPM

TURBOPUMP	OVERALL
SHAFT POWER, MW	5.3815
TURBINE EFFICIENCY	0.5200
PUMP EFFICIENCY	0.6730

STATE POINT DESCRIPTION	FLOW LBS/SEC	TOTAL PRESSURE	TEMPERATURE	ENTHALPY BTU/LB
DEWAR OUTLET	90.07	29.4	39.2	-101.1
PUMP OUTLET	89.57	913.9	53.0	-44.5
TPA BEARING COOLANT	0.50	913.9	53.0	-44.5
STRUCTURE SUPPORT INLET	24.00	913.9	53.0	-44.5
STRUCTURE BYPASS INLET	6.00	913.9	53.0	-44.5
NOZZLE MANIFOLD INLET	59.57	879.7	53.0	-44.5
NOZZLE OUTLET	56.57	685.9	200.4	518.5
SKIRT OUTLET	-0.00	685.9	200.4	518.5
BOLT COOLANT OUTLET	3.00	685.9	53.0	-44.5
REFLECTOR INLET	57.22	685.9	193.0	490.0
REFLECTOR OUTLET	57.22	641.4	310.3	949.7
PERIPHERAL SHIELD OUTLET	57.22	631.2	320.5	939.6
SUPPORT PLATE INLET	87.22	631.2	287.7	860.0
STRUCTURE SUPPORT OUTLET	24.00	631.2	266.9	777.2
CENTRAL SHIELD INLET	87.22	621.7	293.7	883.9
CORE INLET	87.22	609.6	298.7	903.9
CORE OUTLET	87.22	442.7	4592.0	17553.0
NOZZLE CHAMBER	86.15	442.7	4542.7	17293.0
MAIN NOZZLE EXIT	86.15	442.7	4507.0	17124.5
DILUENT BLEED INLET	2.35	685.9	193.0	490.0
HOT BLEED INLET	1.06	442.8	4542.7	17293.0
TURBINE INLET	3.42	266.0	1560.0	5728.8
MAIN TURBINE EXIT	3.42	34.8	1238.5	4238.0
TURBINE EXIT (AT COLLECTOR)	3.92	34.8	1081.8	3692.1
SKIRT EXHAUST EXIT (NOZZ. I..)	3.92	2.8	1451.8	4982.4

NOZZLE TUBE Q=31856. SKIRT TUBE Q= -0. MAIN NOZZLE THROAT AREA 98.339 IN2
MAIN NOZZLE IDEAL ISP 881.52, TPCV K/A2 VALUE 0.497378E 00, PUMP EXIT DENSITY 4.294
MAIN NOZZLE ACTUAL ISP 857.02 AND TURBINE EXHAUST NOZZLE ACTUAL ISP 418.46

Enclosure (9)
7830:M3846

THE VA ENGINE STADY STATE VALUES TURBINE STUDY(43) 15, H.F.90.339 (1) T-OUT-7540

ENGINE PC= 440.4 TC= 4427. THROUST 75,460 LBS, I FUEL 100.5 SEC
POWER LEVEL= 1550.0 W TURBINE = 1000.0 W

TURBOPUMP OVERALL
SHAFT POWER, A 5.153
TURBINE EFFICIENCY 0.520
PUMP EFFICIENCY 0.6730

STATE POINT DESCRIPTION	FLO	LBS/SEC	TOTAL PRESSURE	TEMPERATURE	ENTHALPY BTU/LB
DEAR OUTLET	90.39	29.4	39.2	-101.1	
PUMP OUTLET	90.39	211.5	52.0	-44.0	
TPA BEATING COOLANT	0.50	211.5	52.9	-44.6	
STRUCTURE SUPPORT INLET	24.00	911.5	52.9	-44.0	
STRUCTURE BYPASS INLET	6.00	911.5	52.9	-44.0	
NOZZLE MANIFOLD INLET	60.39	677.3	52.9	-44.6	
NOZZLE OUTLET	57.39	633.5	200.4	518.4	
SKIRT OUTLET	0.00	633.5	200.4	518.4	
ROLT COOLANT OUTLET	3.00	633.5	52.0	-44.6	
REFLECTOR INLET	58.05	633.5	193.0	490.0	
REFLECTOR OUTLET	58.05	639.0	300.0	932.0	
PERIPHERAL SHIELD OUTLET	58.05	628.0	310.5	971.2	
SUPPORT PLATE INLET	88.05	628.0	283.7	644.0	
STRUCTURE SUPPORT OUTLET	24.00	628.0	262.1	758.4	
CENTRAL SHIELD INLET	88.05	619.3	289.5	667.2	
COPE INLET	88.05	607.2	294.4	686.5	
COPE OUTLET	88.05	443.1	4481.6	17004.9	
NOZZLE CHAMFER	86.94	443.1	4427.6	16749.5	
NOZZLE EXIT	86.94	443.1	4390.6	16574.7	
DILUTANT BLEED INLET	2.33	619.3	193.0	490.0	
ROT BLEED INLET	1.10	440.4	4427.6	16749.5	
TURBINE INLET	3.44	208.3	166.0	5720.5	
MAIN TURBINE EXIT	3.44	35.0	1239.5	4138.2	
TURBINE EXIT (AT COLLECTOR)	3.44	35.0	1082.7	3095.0	
SKIRT EXTENS. EXIT (NOZZ. I.)	3.44	35.0	1452.7	4985.6	

NOZZLE TYPE Q=22312. SKIRT TYPE Q= 0.0. NOZZLE INLET AREA 98.341 IN²
AI NOZZLE IDEAL ISP 967.50, TPCV K/2 VALUE 0.77722 DC, PLP EXIT VELOCITY 4.29+
AI NOZZLE ACT L IS 843.40 AND TURBINE EXHAUST NOZZLE CTCL IS 418.50

75-K NERVA ENGINE TOLERANCE STUDY

THRUST CHAMBER TEMPERATURE VS. THRUST CHAMBER PRESSURE

NERVA ENGINE STEADY STATE VALUES TOLERANCE STUDY (20)NERVA, FIND AN, TC, PC=450 & THRUST=75000

ENGINE PC= 450.0 TC= 4443. THRUST 75000. LBS, IMPULSE 824.99 SEC
POWER LEVEL= 1552.2 MWTURBOPUMP= 18500. RPM

TURBOPUMP	OVERALL
SHAFT POWER, MW	5.4754
TURBINE EFFICIENCY	0.5200
PUMP EFFICIENCY	0.6730

STATE POINT DESCRIPTION	FLOW LBS/SEC	TOTAL PRESSURE	TEMPERATURE	ENTHALPY BTU/LB
DEWAR OUTLET	90.91	29.4	39.2	-101.1
PUMP OUTLET	90.41	921.1	53.1	-44.0
TPA BEARING COOLANT	0.50	921.1	53.1	-44.0
STRUCTURE SUPPORT INLET	24.00	921.1	53.1	-44.0
STRUCTURE BYPASS INLET	6.00	921.1	53.1	-44.0
NOZZLE MANIFOLD INLET	60.40	886.9	53.1	-44.0
NOZZLE OUTLET	57.40	693.1	200.5	518.7
SKIRT OUTLET	0.00	693.1	200.5	518.7
BOLT COOLANT OUTLET	3.00	693.1	53.1	-44.0
REFLECTOR INLET	57.75	693.1	193.2	490.8
REFLECTOR OUTLET	57.75	648.6	306.9	936.3
PERIPHERAL SHIELD OUTLET	57.75	638.4	316.7	975.0
SUPPORT PLATE INLET	87.75	638.4	284.3	846.5
STRUCTURE SUPPORT OUTLET	24.00	638.4	262.5	759.8
CENTRAL SHIELD INLET	87.75	628.9	290.2	869.7
CORE INLET	87.75	616.8	295.0	889.1
CORE OUTLET	87.75	450.0	4497.7	17078.1
NOZZLE CHAMBER	86.49	450.0	4443.7	16823.1
MAIN NOZZLE EXIT	86.49	450.0	4404.6	16638.6
DILUENT BLEED INLET	2.65	693.1	193.2	490.8
HOT BLEED INLET	1.25	450.0	4443.7	16823.1
TURBINE INLET	3.91	304.8	1660.0	5729.7
MAIN TURBINE EXIT	3.91	39.8	1285.6	4402.8
TURBINE EXIT (AT COLLECTOR)	4.41	39.8	1141.2	3898.9
SKIRT EXTEN. EXIT (NOZZ. IN.)	4.41	7.8	1511.2	5191.9

NOZZLE TUBE Q=32310. SKIRT TUBE Q= 0. MAIN NOZZLE THROAT AREA 95.935 IN2
MAIN NOZZLE IDEAL ISP 869.48, TPCV K/A2 VALUE 0.319869E 00, PUMP EXIT DENSITY 4.294
MAIN NOZZLE ACTUAL ISP 845.31 AND TURBINE EXHAUST NOZZLE ACTUAL ISP 426.81

X. Report on NERVA Engine/Tank
Close-Coupling in ETS-1

Report on NERVA Engine/Tank Close-Coupling

In ETS-1

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I. ABSTRACT

This study of close-coupling and non-close-coupling for NERVA engine development testing in ETS-1 resulted in a recommendation of a non-close-coupled configuration featuring minimum costs.

The major reasons for this recommendation are:

1. No serious technical problems were uncovered concerning engine testing in a non-close-coupled configuration in ETS-1.
2. The test results from such a configuration would be meaningful and applicable to evaluate most of the important engine performance parameters. In addition, the test data provided would enable improving the engine system and nuclear computer models to improve the accuracy of prediction of flight environment.
3. Engine/stage testing that follows engine development testing will provide a close-coupled configuration closely simulating a flight environment.
4. Optimum use of available funds to arrive at the best engine possible under existing constraints, is to apply these funds to component design, development, and test efforts and perform non-close-coupled engine development tests.

This recommendation does pose some program risk; however, this risk appears to be small.

II. INTRODUCTION

This report documents a study requested by the Engine Committee of the Engine System Task Force related to the advisability of close-coupling of the E-Engine and the ETS-1 run tank during NERVA Engine ground testing.

The purpose of the study was to consider both close-coupled and non-close-coupled engine tank configurations during engine development testing in ETS-1 and recommend an approach for the Alternate NERVA Engine Program.

The approach used in the study included consideration of technical,

programmatic and cost aspects as follows:

1. Compare the general advantages and disadvantages of close-coupled and non-close-coupled configurations assuming that optimum configurations could be employed.
2. Consider the entire NERVA Program - including engine component development, engine development, stage development and vehicle flight test phases to determine the need for close-coupling during the engine development phase.
3. Consider various ETS-1 configurations providing different degrees of attainment of the general advantages of close-coupling previously described.
4. Estimate the ETS-1 modification costs for the configurations selected.
5. Compare the advantages and disadvantages of the configurations selected.
6. Select the recommended configuration.

The study was directed by the NERVA System Department with assistance from component designers, nuclear analysis and test facility designers.

Quantitative data from previous related studies were used in this study; however, this is essentially a qualitative study, except for the cost estimates provided. The intent of the study is to collect and evaluate all available data related to the subject and recommend a configuration based on this available data.

The practical limitations regarding ground test simulation of the flight gravity field, space vacuum and nuclear radiation environments must be recognized.

The study is really an evaluation of technical, programmatic and cost tradeoffs regarding simulation of the following flight environments during NERVA engine development tests:

1. Nuclear radiation.

2. Hydrogen conditions (pressure, temperature, quality, etc.) at the inlet port of the PSOV.

3. Structural loads at the engine/flight tank (MPT) interface.

The general philosophy of providing maximum simulation of flight environment during the overall ground test development program involving engine component, engine and integrated engine/stage phases within existing budget constraints was considered in this study.

III. GENERAL COMPARISON - CLOSE-COUPLED AND NON-CLOSE-COUPLED

The first step in the study was to consider the relative advantages and disadvantages of close-coupled and non-close-coupled engine/tank configurations for use during the engine development testing in a general sense.

The major advantages of a close-coupled configuration are:

1. Better simulation of flight nuclear radiation environment.
2. Better simulation of fluid (H_2) inlet conditions to the PSOV inlet during flight.
3. Improved simulation of flight structural loads at the engine/module propellant tank (MPT) interface and between engine components.
4. The flight engine/MPT interface operational adequacy can be evaluated earlier in the total development program.
5. Provides improved capability for testing of nuclear radiation shielding systems for both unmanned and manned vehicles during the engine development test phase.

The major disadvantages of a close-coupled configuration are:

1. Test facility costs are greater for the engine development program.
2. The stage tank (MPT) design and development may have to be accelerated to enable incorporation into the engine development program. This may increase total program costs during the engine development phase.

IV. CLOSE-COUPLING CONFIGURATIONS FOR E-ENGINE TESTS IN ETS-1

The previous section describes close-coupling advantages based on optimum design of the ground test facilities to provide the best possible simulation of flight environments. The E/STS-2/3 test facility design activity was based on similar design criteria and objectives for testing a 200K thrust NERVA Engine.

The current Alternate NERVA Engine Program is based on conducting all engine development tests in ETS-1. The next step in the study was to consider various close-coupling configurations in ETS-1 that would provide a lesser degree of attainment of the close-coupling advantages described in Section III.

Configuration No. 1

A non-close-coupled configuration was studied to serve as a basis for comparison with the close-coupled configurations. Figure 1 shows this configuration, which requires the following modifications to the current ETS-1 configuration employed during Technology Engine testing:

1. A new propellant feed line from the run tank to the test stand adapter (TSA).
2. Engine Test Compartment (ETC) top shield modifications.
3. Interface system plumbing and electrical modifications.
4. New TSA
5. New ETC bottom shield.
6. New NERVA Exhaust System (NES) duct extension.
7. Relocation of existing facility intermediate shield.

Configuration No. 2

The first close-coupled configuration, shown in Figure 2, represents minimum ETS-1 modification costs with the lowest degree of attainment of close-coupling advantages. The following ETS-1 modifications are required:

1. Removal of the top shield of the ETC.

FIG. 1
CONFIGURATION NO.1
NON-CLOSE-COUPLED

FIG. 2
CONFIGURATION NO. 2
CLOSE-COUPLED CONCEPT A

2. Extensive modifications to the Interface System.
3. Lowering the existing run tank to close-couple with the engine PSOV.
4. Design and provisioning of a thrust structure connecting the tank and the engine.
5. New NES duct extension.
6. Relocated facility intermediate shield.
7. Modification of the run tank carrying truss.
8. New ETC bottom shield.
9. New structure between run tank and side shields to provide vacuum capability within the ETC.

Configuration No. 3

The second close-coupled configuration, shown in Figure 3, represents more extensive ETS-1 modifications than the first configuration and also provides a greater degree of attainment of close-coupling advantages. This configuration requires the following modifications to ETS-1:

1. Removal of the ETC top shield.
2. Extensive modifications to the Interface System.
3. Modification of the existing run tank adding a section beneath the cylindrical portion that close-couples with the engine PSOV without modification of the ETC side shields. This added section shall be 33 feet in diameter and shall have flight tank bottom configuration.

4. Design and provisioning of a thrust structure connecting the tank and the engine.

5. New NES duct extension.
6. Relocated facility intermediate shield.
7. New run tank carrying truss.
8. New ETC bottom shield.
9. New structure between run tank and side shields.

Configuration No. 4

The third close-coupled configuration, shown in Figure 4, represents the

FIG. 3
CONFIGURATION NO. 3
CLOSE-COUPLED
CONCEPT B

FIG. 4

CONFIGURATION NO.

CLOSE-COUPLE
CONCEPT C

maximum ETS-1 modifications providing maximum attainment of close-coupling advantages in ETS-1. This configuration involves the following modifications to ETS-1:

1. Removal of the ETC top shield.
2. Extensive modifications to the Interface System.
3. Replacement of the present run tank with a 33-foot diameter tank.

of flight configuration that is close-coupled to the engine. The tank thrust structure (flight configuration) shall be provided to connect the tank and engine.

4. The ETC side shields shall be replaced with shields designed to complement the engine internal shield to simulate the flight nuclear radiation environment.

5. New NES duct extension.
6. New facility intermediate shield.
7. New run tank carrying truss.
8. New ETC bottom shield.

V. COMPARISON OF ALTERNATE CONFIGURATIONS

The cost estimates for the four configurations considered are presented in Appendices A, B, C, and D. These estimates differ somewhat from the ETS-1 Modification Study Summary data issued on 25 October 1967 because of some changes in ground rules and better definition of requirements. Significant cost changes occurred in the following scope areas:

1. Fluid storage dewars and pressurization bottles were provided by relocating equipment presently at Test Cell A rather than procurement of new items.
2. No data acquisition additions were necessary as previously described.
3. A NES duct extension was added.

The total costs for ETS-1 modifications to meet all the requirements of the NERVA Program Requirements Document from SNPO dated 10 October 1967 are presented to provide a reference point. Figure 5 presents a summary cost

Figure 5

ETS-1 MODIFICATION COST SUMMATION

<u>ITEM</u>	<u>ENGINE/TANK CONFIGURATION NO.</u>			
	1 <u>Non-Close-Coupled</u>	2 <u>Close-Coupled Concept No. A</u>	3 <u>Close-Coupled Concept No. B</u>	4 <u>Close-Coupled Concept No. C</u>
Basic ETS-1 Modification Costs Common to All Configurations	18,342K\$	18,342K\$	18,342K\$	18,342K\$
Additional Modification Costs Unique to Each Configuration Considered	1,363K\$	3,181K\$	4,033K\$	9,796K\$
TOTALS:	19,705K\$	21,523K\$	22,375K\$	28,138K\$
Close-Coupled Delta Cost (as compared to Non-close-coupled baseline costs)	- - -	+ 1,818K\$	+ 2,670K\$	+ 8,433K\$

comparison of the data in the appendices.

The total basic ETS-1 modifications required for all four configurations considered are as follows:

1. New NES (duct) including I&C and any required duct extension.
2. New steam supply system including I&C.
3. New bottom shield (increased size for close-coupled concept No. 4).
4. Increased propellant storage capability including I&C, transfer and pressurization systems.
5. NES and FEP test systems.
6. Increased water supply and purification systems and borated water system.
7. Cooldown and interface systems (off stand).
8. Increased topping capability.
9. Increased I&C and power capability.
10. TSCS/ECS and environmental system modifications.
11. NES duct cover shield modifications.
12. Fill station modifications.
13. Steam generator and duct water supply piping.
14. Steam line (at test stand).
15. Miscellaneous piping and structural modifications.
16. Miscellaneous NES modifications.

To assist in selecting a recommended configuration, the advantages and disadvantages of Configuration No. 1 are described herein and the other three configurations are compared to Configuration No. 1.

The advantages of Configuration No. 1 are:

1. Minimum test facility cost. The total ETS-1 modification costs are 18.34 million dollars and the costs unique to this configuration are 1,363 thousand dollars.

2. Partial simulation of flight engine component nuclear radiation environment is provided.

3. Partial simulation of fluid inlet conditions to the PSOV during flight.

4. Partial simulation of flight engine startup and shutdown conditions.

The disadvantages of Configuration No. 1 are:

1. No capability for flight propellant heating data regarding fluid conditions within the MPT.

2. Less adequate simulation of:

a. Flight nuclear radiation environment within the MPT and engine.

b. Flight structural loads at the engine/MPT interface.

c. Engine startup and shutdown conditions.

3. Demonstration of zero NPSP capability is less meaningful.

4. Evaluation of the flight engine/MPT interface operational adequacy is delayed until the engine/stage ground test development program.

5. Less capability for development tests of nuclear radiation shielding systems.

The other three configurations provide progressively better simulation of flight environmental conditions, capability for demonstration of zero NPSP operation, simulation of flight propellant heating effects within the MPT, capability for nuclear shielding system development tests and capability for evaluating the flight engine/MPT interface operational adequacy. Configuration No. 4 features the best simulation of flight conditions of all the configurations considered.

The relative disadvantages of the other configurations are:

1. The degree of simulation of flight conditions.

2. The relative cost.

The cost comparison is shown in Figure 5, which indicates the following cost comparisons with the non-close-coupled configuration as a reference:

<u>Configuration No.</u>	<u>Delta Cost (K\$)</u>
2	+ 1,818
3	+ 2,670
4	+ 8,433

VI. DISCUSSION

The first step in selecting a configuration is to evaluate the requirement for close-coupling during the engine development phase of the overall program.

The general philosophy regarding maximum simulation of flight environments during ground testing mentioned previously applies to the consideration of the need for close-coupling tests.

Evaluation of the need for close-coupling tests during engine development testing in ETS-1 involves a basic question: Are close-coupling tests necessary prior to engine/stage development tests?

The situation is that this question cannot be evaluated on strictly an analytical or quantitative basis to enable a clear cut decision. A general guideline is that simulation of flight environments early in the program is advisable. A problem that this presents is that the inaccuracies in predicting flight environment, simulation of such environments and test data from simulation tests decrease the value of such an approach. Another problem is that component and environmental interactions are extremely difficult to predict analytically, thus system development tests are an important phase of every program.

The answer to the question posed involves economic and technical tradeoffs, which were conducted during this study primarily based on data currently available.

The current turbopump assembly (TPA) Component Development Program includes tests to demonstrate operation at low values of NPSP. Current

predictions indicate operation as low as 0.6 psi NPSP will be accomplished. Tests with hydrogen tank outlet conditions from 38 to 42°R and 24 to 32 psia are planned. There are no TPA tests planned to demonstrate capability to operate the TPA with fluid - vapor conditions at the tank outlet.

Figure 6 presents the current prediction of tank outlet (engine inlet) fluid conditions for flight operation of a single 75,000 pound thrust NERVA engine based on the definition of attaining zero NPSP conditions at the engine inlet just at the end of engine operation. TPA component testing will demonstrate operation at conditions representative of the start and completion of flight operation. Current test facility constraints preclude demonstration or simulation of the entire operating regime as shown on Figure 6.

The inherent capability of the TPA to operate under zero NPSP conditions minimizes the interdependency of tank and engine; consequently the need to conduct close-coupled engine development tests for TPA purposes diminishes.

The component development programs currently include some evaluation of nuclear environment by tests at the General Dynamics and Plumbrook reactors, as well as during the NERVA reactor testing at NRDS. Current evaluation of potential engine component problems caused by nuclear radiation environment indicates the following situation:

1. The polymers used in the TPA bearings can withstand five times the maximum predicted nuclear environment regarding gamma damage ionization mechanism. All other engine component materials have greater resistance to gamma damage.
2. The component materials can withstand two decades greater than the predicted shielded environment within the engine thrust structure subassembly regarding fast neutron dislocation mechanism effects.
3. The predicted thermal neutron (n, α) transmutation damage is just equal to the current estimate of shielded TPA turbine environment without consideration of extra low boron content or operation below 900°F.
4. The heating of engine components caused by gamma radiation appears

FIGURE 6

PROPELLANT TANK OUTLET
TEMPERATURE & PRESSURE

VS

ENGINE RUN TIME

FOR 75K ENGINE FLIGHT OPERATION

NOTE: BASED ON SINGLE ENGINE MODULE
AND ATTAINMENT OF ZERO NESP
JUST AT END OF ENGINE OPERATION

to be a problem that can be solved by existing design methods, including possible component cooling with hydrogen propellant.

The foregoing predictions suggest that there is no positive requirement to conduct close-coupled engine development tests in ETS-1 for purposes of evaluating nuclear radiation effects on engine components.

VII. RECOMMENDED CONFIGURATION

The previous discussion of comparative merits of various configurations of close-coupling and a non-close-coupled configuration as well as a general comparison of close-coupling and non-close-coupling during engine development tests portrays the many factors to be considered prior to deciding on a particular configuration and approach.

The relative value of the technical, programmatic and economic factors related to selecting a recommended configuration was considered.

This study has been based on the current program as described in the NERVA Program Requirements Document from SNPO dated 10 October 1967 and the current NERVA funding allocations. The recommended configuration selection is based on these requirements and all qualitative and quantitative data currently available.

The recommended configuration is the non-close-coupled concept described in Configuration No. 1 and shown in Figure 1. The reasons for this recommendation are:

1. No serious technical problems are apparent concerning engine testing in a non-close-coupled configuration in ETS-1.

2. The test results from such a configuration would be meaningful and applicable to evaluate most of the important engine performance parameters. In addition, the test data provided would enable improving the engine system and nuclear computer models to improve the accuracy of prediction of flight environment.

3. Engine/stage testing that follows engine development testing will provide a close-coupled configuration closely simulating a flight environment.

4. The various close-coupled configurations require additional funds ranging from approximately two million to eight and one half million dollars over the cost of the non-close-coupled configuration. As a result of these expenditures, additional engine system test information (in varying degree) will be obtained as compared to the information from the non-close-coupled tests. The worth of this additional information was evaluated in terms of decreasing the component design, development, and test funds in order to perform these close-coupled tests. The results of this evaluation indicated that optimum use of the available funds to arrive at the best engine possible, would be to apply these funds to the component design, development, and test efforts and perform non-close-coupled engine development tests.

This recommended configuration does pose some program risk in case problems occur during the integrated engine/stage testing that were not predicted analytically or observed during non-close-coupled engine development tests. However, these problems, if they occur, will probably be in either the hydrogen inlet conditions to the TPA or in component radiation levels. In both cases, solutions will most likely not involve component changes but rather such things as propellant tank internal changes (baffling, stand-pipe, operating pressure, etc.), local shielding, or minor lines and valves relocations. It is noted that the capability of the TPA to accept significant vapor ingestion without serious loss of performance is additional assurance that problems in the hydrogen inlet conditions area will not be encountered in the integrated engine/stage testing.

Nuclear analyses conducted during preparation of this report indicate that the following additional scope requirements may apply with resultant changes in estimates of ETS-1 modification costs and engine system costs:

1. An engine intermediate shield mating with the test facility intermediate shield may be necessary to prevent an excessive nuclear radiation

environment within the engine thrust structure subassembly during ground tests in ETS-1.

2. A new test facility intermediate shield may be necessary for all close-coupled and non-close-coupled concepts considered in this study.

Further nuclear analysis will be required to enable evaluation of these possible additional requirements. The program cost estimates may also need revision pending the results of the nuclear analysis activity.

CONFIGURATION NO. 1(NON-CLOSE-COUPLED CONFIGURATION)

1.	Propellant Feed Line	160K\$
2.	Top Shield	311
3.	Interface & Cooldown System	
	a. Cooldown manifold	330K\$
	b. Interface Systems	242
	Subtotal	572K\$ 572
4.	Thrust structure	100
5.	Relocate existing intermediate shield	10
6.	Test Stand Adapter (TSA)	210
	TOTAL	1,363K\$

CONFIGURATION NO. 2(CLOSE-COUPLED CONCEPT NO. A)

1.	Remove top shield	20K\$
2.	Interface & Cooldown Systems	
a.	Cooldown manifold	1,600K\$
b.	Gas supply lines	600
c.	Removal and rework of existing piping	920
		<hr/>
		2,520K\$
	Subtotal	2,520
3.	Lower Run Tank	237
4.	Thrust structure modification	355
5.	Relocate intermediate shield	10
6.	Run tank carrying truss*	-
7.	Run tank-to-side shield structure	39
		<hr/>
	TOTAL:	3,181K\$
	- Increase to Non-Close-Coupled Concept	1,918K\$

* Included in Item 4.

CONFIGURATION NO. 3
(CLOSE-COUPLED CONCEPT NO. B)

	<u>Item</u>	<u>Cost</u>
1.	Removal of top shield	20K\$
2.	Cooldown & Interface Systems	2,520*
3.	Modify Run Tank - 33 ft. section	831
4.	Thrust structure modification	355*
5.	Relocate intermediate shield	10*
6.	Run tank truss **	-
7.	Structure between run tank & shield	297
		<hr/>
	TOTAL:	4,033K\$
	- Increase to Non-Close Coupled Concept	2,670K\$

* Same as Close-Coupled Concept No. 1

** Included in Item 4

CONFIGURATION NO. 4
(CLOSE-COUPLED CONCEPT NO. C)

<u>Item</u>	<u>Cost</u>
1. Removal of top shield	20K\$*
2. Cooldown & Interface Systems	2,520 *
3. Replacement of run tank	1,000 ***
4. Thrust structure modifications	355 *
5. New ETC side shields	4,500
6. New intermediate shield	700
7. Run tank truss **	--
8. Cost for bottom shield	550
9. Test Stand Structure	151
<hr/>	
TOTAL:	9,796K\$
- Increase to Non-Close-Coupled Concept	8,433K\$

* Same as Close-Coupled Concept Numbers 1 & 2

** Included in Item 4

*** Tank fabrication cost included. Cost of item will decrease if tank is available as GFE item from the Saturn program.

XI. Back Pressure Requirements for NERVA
Engine Test Facilities

MEMORANDUM

23 October 1967
DB:rja:7830:13468

TO: R. V. Evleth

FROM: D. Buden

SUBJECT: Back Pressure Requirements for NERVA Engine Test Facilities

COPIES TO: J. J. Beereboom, W. W. Madsen, B. Mandell, R. J. Pickering,
S. A. Varga, W. L. Vinegar

REFERENCE: (a) Memo from R. V. Evleth to Distribution dtd 16 October 1967,
subject "Meeting Report NERVA Program Discussion at
SNPO-C 12-13 October 1967"

ENCLOSURE: (1) Back Pressure Requirements for NERVA Engine Test Facilities

A review has been made of the back pressure requirements for the test facility for the NERVA engine. The summary of the information is enclosed.

D. Buden, Manager
Systems Analysis Department

CLASSIFICATION CATEGORY	
<i>Unclassified</i>	
<i>D. Buden</i>	<i>10/27/67</i>
CLASSIFYING OFFICER	DATE

BACK PRESSURE REQUIREMENTS FOR NERVA ENGINE TEST FACILITIES

The back pressure requirements for the engine test facility must be adequate to demonstrate engine performance for orbital and suborbital operation, and prevent the introduction of destructive vibrations that would not be experienced in a flight engine. A review has been made of the technology engine and studies for the large NERVA engine to be used as a guide for the selection of the facility back pressure design. The following information has been reviewed to establish the back pressure requirements:

1. CFDTs performance data showing the effects of variation of back pressure and tank pressure on the initial bootstrap.
2. The NERVA engine bootstrap analysis.
3. Facility pull-in at the minimum thrust and maximum specific impulse operating point.
4. Criteria used for E/STS 2-3.
5. Engine stability for NRX/EST.
6. Flight condition requirements.

After reviewing this information, it appears that startup is the major criteria for establishing the back pressure requirements in the facility: To do a thorough investigation of the requirements requires a detailed analysis of the engine system performance during startup or the use of a NERVA cold flow engine system. Information including low-speed turbopump performance data is required for the analysis to be meaningful. Since the engine size has not been defined as yet, the detailed information is not available nor is a cold flow engine available.

1. CFDTS Performance Data

A review of CFDTS data indicates that on a typical run when "bootstrap" is achieved that initially the flow is dependent mainly on the tank pressure but a point is reached in time where the turbopump provides the main driving pressure to the system. In runs where "bootstrap" did not occur, the tank pressure was the main driving force throughout the run. The starting characteristics of the engine are quite different in tests where the turbopump becomes the main driving pressure. As back pressures are lowered, lower tank pressures result in successful engine bootstraps. Figure 1 shows the regions of successful and unsuccessful bootstraps. There is a band of uncertainty in which no data was obtained. Thus, for a tank pressure of 25 psia with a back pressure of 0.25 psia, the engine will bootstrap. However, the engine will not bootstrap with the same tank pressure but the back pressure increased to 7.5 psia.

Tank pressures between 23 and 35 psia have been discussed in the past during stage studies. For tank pressures of 23 psia, the data is uncertain as to what back pressure is required. At 30 psia, the back pressure could go to 8 psia and still bootstrap.

A second consideration is the timing for the engine to bootstrap. It affects the whole timing of the control system. Figure 2 shows the effect of back pressure on the time to reach the peak speed for different tank pressures. Assuming that a satisfactory facility would match times within 25%, the cross-hatched area shows that a 30 psia tank pressure requires a facility with 1 psia back pressure, a 35 psia tank pressure requires 3 psia back pressure, and a 40 psia tank pressure requires 7.5 psia back pressure.

2. NERVA Engine Bootstrap Analysis

The analysis of the 200,000-pound thrust NERVA engine indicates that the system is more difficult to bootstrap than the technology engine.

Analyses were made at a tank pressure of 35 psia and back pressures of 0.1, 1.0 and 7.6 psia. The results (see Figure 3) show that for back pressures of 0.1 and 1.0 psia the engine bootstrapped, reaching their peak chamber pressures in 4 and 5 seconds, respectively. However, for the 7.6 psia back pressure, the engine would not bootstrap. For the technology engine, bootstrap occurs with a 35 psia tank pressure and 7.6 psia back pressure. Thus, the analysis indicates that the NERVA engine is more difficult to bootstrap and that the facilities must therefore closer simulate flight conditions.

The NERVA engine startup characteristics are different because:

- (1) The turbine exit is unchoked, thus making it much more sensitive to back pressure. The exit nozzle for a 1 psia back pressure would choke at 2 psia. For 2 psia at the nozzle, the engine needs 9 psia at the turbine exit or 64 psia at the chamber. Two psia back pressure raises this to 75 psia chamber pressure.
- (2) The diluent extraction point occurs in the engine system at a point where colder fluid temperatures are manifest; thus, the fluid delivered to the turbine is colder at any particular time during the bootstrap than would be expected during XE.

One factor tending to help the NERVA engine bootstrap is the 0 NPSP inducer. This guarantees a positive NPSP to the turbopump.

3. Minimum Thrust at Maximum Specific Impulse

The pull-in at the chamber pressure where the structural limit (or pump stall) intersects with the chamber temperature limit was investigated. The nozzle should flow full at this point. This chamber pressure is 240 psia for XE-1. The use of a different structural design would lower it to say 200 psia. Using the start pressure ratio of 33 requires a cell pressure of $200/33$, or 6 psia.

4. Criteria for E/STS 2-3

The criteria for E/STS 2-3 facility is based upon scale-model test data and nozzle considerations. Scale-model tests indicated that a back pressure of 2 psia was the lowest reasonably obtainable back pressure. Since the facility was being designed to simulate as close as possible the flight conditions, the 2 psia back pressure was used as a facility design criteria.

5. Engine Stability

During NRX/EST, flow oscillations occurred around chamber pressure of 140 psia. These oscillations are associated with the fluid conditions in the nozzle at that point. Conditions in the NERVA engine are such that we would expect similar oscillations. It is desirable that the pull-in point for the facility occur before we reach this chamber pressure. Dividing 140 by 33, indicates a cell pressure of 4 psi or less is required.

6. Flight Conditions

Reviewing the conditions for flight, possibilities of starting the engine under a variety of back pressures which depend on the mission and vehicle configuration exist. In an orbital start, the engine will be started under conditions of essentially zero back pressure (vacuum). However, in a suborbital start, the shroud surrounding the engine could result in a build-up of back pressure. This results in the desirability to test the engine under various back pressure conditions. At this point, insufficient mission and vehicle data is available to define the upper limits for flight conditions.

SUMMARY

The tank pressure must be established as an essential step in establishing the facility design criteria for back pressure. Considering engine bootstrap performance and timing, from CFDTS data, for a 30 psia tank pressure a 1 psia back pressure is required, while for a 35 psia tank pressure a 3 psia facility back pressure would suffice. NERVA analysis on the 200,000-pound thrust engine indicated that this engine would be more difficult to bootstrap so that the technology numbers are probably upper limits. Other conditions, such as having the facility pulled in at maximum temperature, engine oscillations, and the criteria for E/STS 2-3, are not as restrictive in the selection of the facility design criteria.

Fig. 1

TANK PRESSURE VS CELL PRESSURE FOR CFDLS

O Burst
X No Burst

Fig. 2

Enclosure (1)
7830:M3468
Page 7 of 8

Time to Peak Speed vs. Peak Pressure

Based on CESE Data

Deylor Moore

FIGURE 3

XII. Systems Analysis of NERVA LH₂ Topping
Modification to ETS-1

MEMORANDUM

TO: B. L. Nauslar DATE: 6 August 1968
FROM: R. J. Williams RJW:vl:7830:M3807
SUBJECT: Systems Analysis of NERVA LH₂ Topping Modification
to ETS-1
COPIES TO: Sacto: J. H. Altseimer, J. J. Beereboom, D. Buden,
L. K. Hampson, B. Mandell, A. L. Ottaviani,
R. A. Uffer
NTO: R. Lovci
REFERENCE: (a) Memo 7400:2213M, dtd 23 July 1968, B. L. Nauslar,
Subj: NERVA LH₂ Topping Modifications to ETS-1 -
Work Assignments
ENCLOSURE: (1) Systems Analysis of NERVA LH₂ Topping Modification
to ETS-1

Enclosure (1) is submitted in fulfillment of the systems analysis work assignment contained in Reference (a). A rough draft of Enclosure (1) was submitted for review on 30 July 1968.

The results of a systems analysis investigation of the modifications to the propellant transfer line described in RD-DR-0150 show that it will provide a system adequate for the 75K engine tests.

R. J. Williams

R. J. Williams
Technology Engine Analysis Group

Approved:

R. J. Williams

for D. Buden, Manager
Systems Analysis Section

CLASSIFICATION CATEGORY	
<i>Unclassified</i>	
CLASSIFYING OFFICER	DATE
<i>W. J. Williams</i>	<i>2-6-68</i>

SYSTEMS ANALYSIS
OF
NERVA LH₂ TOPPING MODIFICATION
TO ETS-1

August 1968

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I. INTRODUCTION

The purpose of this report is to provide analytical verification that the LH₂ topping system described in Report No. RD-DR-0150, ETS-1 Modifications for 75K NERVA Conceptual Study, 8 April 1968 (U) is adequate to accommodate testing of the 75K NERVA engine system in ETS-1 and to provide sufficient preliminary analysis to establish an operational mode for the proposed system. The present propellant supply system consists of the 250,688-gallon main storage dewar (V-3801), the 77,000-gallon propellant run tank (V-5001), and the propellant transfer line and valves connecting these tanks. Each of the tanks is equipped with a pressurization and vent system capable of being operated in either a manual valve position mode, or in an automatic pressure control mode. In order to satisfy the engine testing requirements outlined in the Criteria for Conceptual Study, ETS-1 Modification for 75K NERVA dated 8 March 1968, it will be necessary to transfer propellant from the V-3801 storage tank into the V-5001 run tank while the engine is operating. The modification to the present system proposed in RD-DR-0150 would provide a greater topping flow rate capability than presently exists by increasing the diameter of most of the propellant transfer line and increasing the size of the remote shutoff valves installed in the line.

The approach used for this investigation was to identify the potential problem areas associated with various means of controlling the topping process and to verify that the proposed system is adequate to maintain satisfactory control of the propellant supply system under the most adverse conditions that would exist during testing of the 75K NERVA engine. Topping of the V-5001 tank is an established technique.

Considerable operational data was obtained on the present propellant transfer system during the Facility Experimental Plans in which topping tests were conducted using an engine simulator. During FEP-III-2 flow tests were conducted with and without topping at high (70% to 90%) and low (15% to 40%) V-5001 liquid levels; and at pressures of 10, 25 and 45 psig. This test data was used as the primary basis for evaluating the proposed system and method of operation.

The technical discussion presents a proposed mode of operation of the topping system, an evaluation of steady-state topping capability, a discussion of the effects of topping on the engine system, and a discussion of the system characteristics during possible transient conditions, including an evaluation of fluid hammer in the propellant transfer line.

II. SUMMARY

The results of this investigation show that the present propellant transfer system must be considered marginal for the requirements of the 75K NERVA engine tests. Incorporation of the modifications to the propellant transfer line described in RD-DR-0150 will provide a system more than adequate for the 75K engine tests. It is recommended that propellant topping be manually controlled by adjustment of V-3801 pressure and by shutoff valves in the propellant transfer line. Flow tests conducted with the present system have demonstrated that the run tank pressurization system is adequate to allow initiation and termination of topping with less than 1 psi variation in tank pressure. The most rapid response time required for the suggested mode of operation is that in event of an emergency shutdown at maximum V-5001 liquid level and 100 lb/sec topping flow, the propellant line shutoff valves must be closed in less than 123 sec to avoid overflow of V-5001. This is considered as more than adequate time for operator action.

The most significant effect of topping on engine performance is increase in propellant feed temperature. This effect may be minimized by obtaining a minimum V-3801 liquid temperature at test initiation, and by maintaining a high vacuum in the propellant transfer line jacket.

III. TECHNICAL DISCUSSION

A. Description of the Propellant Supply System

1. Low Pressure Liquid Hydrogen System

The main storage tank, V-3801, a spherical, double-wall, perlite-insulated dewar is shown in Figure 1. Maximum volume is 250,688 gallons. However, with an allowable 10% ullage volume (the instrumented 100% level, or full mark), the dewar capacity is 245,934 gallons (143,014 lbs). At other dewar levels below full-mark, the fluid levels are measured with an instrumented probe inserted into the dewar. The instrumentation data (which are presented in Figure 1) provide the instrument number, gallon-amount, and other pertinent information at the various dewar levels.

The low-pressure liquid hydrogen system provides liquid hydrogen to fill the engine run tank (V-5001) and the high pressure emergency cooldown dewar (V-5002). The system also supplies liquid hydrogen to the vaporizer for conversion to gaseous hydrogen. The low-pressure liquid hydrogen system consists primarily of the main storage dewar (V-3801) and transfer piping.

The present 3-LH-6LV low-pressure liquid hydrogen transfer line, consisting of the piping line lengths, valves, and other pertinent components from the storage dewar (V-3801) to the run tank (V-5001) and to the high-pressure dewar (V-5002), is shown in Figure 2. Figure 3 is an isometric view of the proposed 75K modification transfer line configuration. This modified line will utilize part of the present ETS-1 facility transfer line system, 3-LH-6LV, and part of a new plus an existing ETS-1 system, 7-LH-12HV. The new system has been established to meet the 75K thrust

NERVA engine requirement for topping. It will provide 91 lb/sec of flow at a pressure drop from V-3801 to V-5001 of 36.5 psi.

2. Propellant Run Tank

The liquid hydrogen run tank (V-5001) is the directly connected source of propellant for engine testing. The run tank is a double-wall, vacuum-insulated vessel, as shown in Figure 4. Nominal, maximum tank volume is 77,000 gallons. With a 10% ullage volume (the instrumented 100% level or full mark), it contains 69,534 gallons (40,900 lbs) of liquid hydrogen). At other run-tank levels below the full mark, the fluid levels are measured with an instrumented probe inserted into the tank. The instrumentation data presented in Figure 4 provides the instrument number, gallon amount, and other pertinent information at the various tank levels.

3. V-5001 and V-3801 Pressurization and Venting Systems

The gaseous hydrogen system provides hydrogen for the pressurization of three liquid hydrogen dewars: (1) the main storage dewar (V-3801), to facilitate the transfer of liquid hydrogen to the propellant run-tank (V-5001) and the high-pressure liquid hydrogen dewar (V-5002); (2) the propellant run-tank (V-5001) to provide the desired engine tank pressure; and (3) the high-pressure liquid hydrogen dewar (V-5002) for emergency engine cooldown. The gaseous hydrogen system shown in Figure 5 consists primarily of three high-pressure vessels (V-3207, V-3208 and V-3209) and the transfer piping.

The pressurization of V-5001 is controlled by two valves, PCV-165 to pressurize the tank and PCV-180 to vent the tank. Two modes of control of these valves are available to the operator.

One mode of control is a manual valve position mode, while the other mode available is an automatic V-5001 pressure control mode. In both the manual and pressure control modes, valves 180 and 165 are controlled with a common analog signal which ranges over both positive and negative voltages. This signal is the voltage on the arm of a pot when operating in the manual mode while it is the output voltage of the pressure controller when operating in the pressure control mode. One polarity of this signal causes the valve 165 to open, pressurizing the tank, while demanding 180 to close. The other polarity causes the valve 180 to open, venting the tank, while demanding the valve 165 to close.

The V-3801 LH_2 storage tank is pressurized and vented in similar fashion and is controlled by the two valves, PCV-248 to pressurize the tank and PCV-179 to vent the tank. Figure 6 is a schematic showing the inlet line from the high-pressure GH_2 vessels and the pressurization and venting system for V-3801 and V-5001.

50 PCV-165

PCV-165 is a two-inch globe valve manufactured by Annin Valve Company having a maximum C_v of 13.

50 PCV-180

PCV-180 is a six-inch globe valve manufactured by Annin Valve Company having a maximum C_v of 400.

38 PCV-248

PCV-248 is a two-inch pressure control valve manufactured by Annin Valve Company having a maximum C_v of 13.

38 PCV-179

PCV-179 is a 12-inch pressure control valve manufactured by Annin Valve Company having a maximum C_v of 1500.

B. System Requirements

The requirements outlined in the Criteria for Conceptual Study, ETS-1 Modification for 75K NERVA, dated 8 March 1968, are as follows:

1. Functional

The functional requirement for the topping system is to maintain a range of liquid hydrogen levels in run tank V-5001 within a range of tank pressures.

The maximum requirements for the 75K thrust NERVA engine have been established at a flow rate of 100 lb/sec, run tank pressure of 65 psia, run duration of seventeen minutes, and a total propellant quantity of 90,200 lbs for the run.

2. Operational

The topping systems to be considered shall have the capability of being operated manually from the test operations consoles located in the control point. To allow for controlled bootstrap starts, topping flow will not be initiated until three minutes of steady-state engine operations have been attained. The topping system must be capable of maintaining specified run tank liquid levels through scram, flow, and programmed shutdowns and their associated cooldown periods.

The run tank V-5001 will be filled from the storage tank V-3801 immediately prior to the run without subsequent refilling of V-3801.

3. Constraints

Equipment and Operational Constraints

The maximum design pressure for V-3801 is 115 psia and for V-5001 is 65 psia. These are operational limits and, when exceeded, will result in termination of the test.

To provide reasonable operating margins, the design operating pressure of V-3801 shall be considered at 100 psia yielding a ΔP of 35 psi at the maximum specified run tank pressure. The system design shall permit the minimum required topping rate at this ΔP . The minimum liquid level in V-5001 shall not be permitted to drop below the 10% level (or 7000 gallons) and the topping rate shall be such as to keep the level in V-5001 above this minimum limit at a ΔP of 35 psi.

4. Engine Constraints

The current design criteria for the 75K NERVA engine system is based on a zero minimum NPSP turbopump. This imposes a constraint on the propellant supply system in that LH_2 flow must be supplied to the pump inlet at a pressure equal to or greater than the vapor pressure of the propellant at the existing local propellant temperature.

C. Mode of Operation

In keeping with the basic approach of providing only minimum changes required to the present system it is recommended that propellant topping be accomplished in manual control with the topping flow controlled by adjustment of the pressure in the V-3801 LH₂ storage tank and by opening and closing the valves in the propellant transfer line.

Review of data pertinent to topping system characteristics obtained during the test conducted in FEP-III-2 together with consideration of the test requirements for the 75K NERVA engine and the flow impedance of the proposed modified propellant transfer line shows that manual control will be quite adequate based on the following considerations:

1. The V-5001 run tank volume is large in comparison to the required maximum engine flow rate, hence, a very slow response time is required with respect to initiation or termination of topping. For example, at the maximum flow rate of 100 lb/sec only 10% of the tank volume is consumed in 41 seconds with no topping, or about 328 seconds are required to deplete the tank from the 90% liquid level to the 10% liquid level. Since the depletion rate of V-5001 is the net effect between engine flow and topping flow the time available for operator action becomes greater if topping is in process.

2. Analysis of the modified transfer line shows that for engine flow rates of 100 lb/sec or less and V-5001 pressures of 55 psia or less the amount of LH₂ in the run tank can be increased by topping at a flow rate greater than the engine flow being consumed.

3. Operating room procedures established prior to each test conducted will provide the test operator in charge of propellant supply management with an anticipated schedule of engine usage and allow him to

establish a schedule for performing the amount of topping required for the test. Deviations from the planned engine consumption schedule can readily be accomodated for the reasons listed above.

4. Inspection of the data from topping tests shows that initiation or termination of topping results in less than 1 psi temporary deviation in V-5001 pressure when the tank pressurization is in automatic control.

5. Calculations show that the liquid head effects on pump NPSP due to changes in the liquid level in V-5001 are negligible in comparison to the controlable variation of propellant temperature and pressure.

The only operational constraints that appear reasonable for this mode of operation is that it is recommended that topping should not be initiated or should be suspended when the liquid level in V-5001 is greater than 80%, and a maximum operating run tank pressure of 60 psia should be established. These recommendations are suggested in order to ensure that the V-500 tank could not accidentally be flooded or pressurized in excess of 65 psia. A further explanation of the reasons for these restraints is presented in Section III.E.5.

Control of the propellant supply system, including topping can adequately be maintained by a single test operator, who would be responsible for control of the V-3801 pressurization and vent system, the V-5001 pressurization and vent system and the propellant transfer line shutoff valves and vent valve. As feedback information this operator would have on display at his console the following measurements:

1. Pressure in V-3801 and V-5001
2. Feed pressure to V-3801 and V-5001
3. Valve positions of V-3801 and V-5001 pressurization and vent valves, and transfer line security and vent valves.

4. On line calculated flow of the propellant supply line (FE-014)
5. On line calculated topping flow rate. (Based on the pressure differential between V-3801 and V-5001)
6. Liquid level measurement of V-3801 and V-5001
7. On line calculated NPSP at the pump inlet.

The test operator in charge of propellant management would establish a schedule for propellant topping prior to a test based on the predicted engine flow data for the test planned, and control the necessary tank pressures and transfer line valves during the test. For tests conducted at a constant engine flow rate this would consist of bleeding in the transfer line, establishing the V-3801 pressure to provide a topping flow rate equal to engine flow, initiating topping when the liquid level in V-5001 reaches a predetermined level and terminating topping when sufficient propellant has been transferred to ensure completion of the test.

For mapping tests in which the engine flow rate is frequently varied, it may be necessary to vary the V-3801 pressure or to suspend and re-initiate topping. However, since for this type of test the average engine flow is less than for full power the topping requirements can easily be fulfilled.

In the event of an emergency shutdown of the engine during a test, topping would immediately be terminated, and if necessary the tanks and transfer line vented.

D. Steady State Operation

1. Topping Capability

The flow impedance of the proposed modified LH₂ transfer line between V-3801 and V-5001 was calculated based on test data obtained on the present transfer line.

Data was analyzed from FEP-III-2 performed on March 24, 1967, to determine the LH₂ topping flow rate in the transfer line between V-3801 and V-5001 with V-5001 at pressures of 10, 25 and 45 psig. The topping flow rates were determined by the V-3801 level change and by the V-5001 level change and FE-14. Figure 7 shows the pressure differential between V-3801 and V-5001 versus the topping flow rate. There is good agreement between the methods of topping flow rate determination. Also shown is the predicted curve from Appendix IV of the Test Summary Report (Memo NTO-R-0092). There is generally good agreement between the predicted and test results. All topping tests performed during FEP-III-2 are shown in Figure 7. The topping flow rate varies from about 60 to 75 pps at V-3801/V-5001 differential pressures of 50 psi to 83 psi.

The friction factor for the existing transfer line was calculated based on the curve shown in Figure 7 and manufacturers data for the valves utilized. This friction factor together with manufacturers data for the proposed 12 inch security valves was used to determine the flow impedance for the proposed modified transfer line. The results of this analysis is shown in Figure 8 and the detailed calculations are presented in Appendix A. The flow resistance of the line ($\frac{K}{A^2}$) was found to be 0.00875 in⁻⁴.

Based on the flow impedance values shown in Figures 7 and 8 the capability of both the present system and the proposed modified system to fulfill the requirements established by the Criteria for Conceptual Study, ETS-1 Modification for 75K NERVA, dated 8 March 1968, was evaluated. In

addition, the capability of either system to perform a steady state run with the 75K engine in which all of the propellant stored in V-3801 and V-5001 is consumed was evaluated. This criteria was believed to be of interest because it represents a maximum duration test in which all available propellant is consumed.

This evaluation was based on the following ground rules:

1. A maximum operating pressure in V-3801 of 100 psia.
2. A minimum liquid level in V-5001 of 10%.
3. Steady state flow to the engine from V-5001.
4. Normal operating procedure for loading propellant.

V-3801 would be filled to the 90% level prior to propellant fill and chill of V-5001 and V-5002. V-5001 and V-5002 and the associated lines would then be chilled and filled from V-3801 and the test initiated without re-filling V-3801. It is estimated from facility test data that V-3801 would contain a maximum of 94,414 lbs of propellant at the start of a test and V-5001 would contain 40,900 lbs.

5. Topping would be initiated when the liquid level in V-5001 reached 80%. Based on the predicted start transient shown in Criteria for Conceptual Study, ETS-1 Modification for 75K NERVA 1562 lbs of LH_2 would be consumed in reaching a steady state flow of 91 lb/sec, providing approximately 72 seconds of time to ensure normal operation of the engine and feed system prior to reaching the 80% liquid level in V-5001.

The capability of the present and the modified system to supply LH_2 for a steady state run consuming 90,200 lbs of propellant is shown in Figure 9. It may be seen that with the modified system the V-5001 tank pressure is limited only by the maximum operating pressure limit of the tank, and the topping system is adequate even at an engine flow rate of 110 lb/sec.

This figure also illustrates that the present system is very marginal to accommodate 75K engine testing. For example, at the engine design flow rate of 91 lb/sec the V-5001 run tank pressure must be less than 49 psia to obtain the required topping, or at 100 lb/sec flow rate the run tank pressure must be less than 38 psia.

The capability of the present and the modified system to perform a maximum duration steady state test is shown in Figure 10. Again the modified system is capable of operation at any run tank pressure up to the operating pressure limit and any engine flow rate up to 110 lb/sec. By contrast, at 91 lb/sec the present system would be limited to a run tank pressure of less than 29.5 psia.

Calculations for the data shown in Figure 9 and 10 is supplied in Appendix B.

2. NPSP vs Temperature, Pressure and Liquid Level Without and With Topping

Plots of NPSP versus pump inlet flow rate based on the LH_2 vapor pressure data of Figure 11 for an 80% and 20% V-5001 propellant tank level were constructed for various LH_2 tank pressures (24, 26, 30, 35 and 40 psia) and pump inlet temperatures (35° , 37° , 38° , 39° , 40° and 41°R). The curves, shown in Figure 12 are based on the latest calculated flow resistance of the propellant supply line from the LH_2 run tank to the pump inlet which includes the modified PSV inlet adapter for the 75K NERVA engine system. The curves show the effect of temperature and tank pressure on NPSP for steady-state engine operation without topping. For example, it may be seen that if the pump inlet flow rate is 90 lb/sec and the fluid temperature at the pump inlet is 38°R , the run tank pressure must be 24 psia or greater to provide a positive NPSP at the pump.

Figure 13 presents curves which show the effect of temperature and pressure with tank topping. The curves have been based on FEP-III-2 topping test data and include the effect of transfer line to pump inlet temperature rise on NPSP during the transfer of LH_2 from V-3801 to engine pump inlet.

FEP-III-2 test data showed that the total temperature rise from V-3801 to V-5001 with the existing 3-LH-6 transfer line to PCV-750 to be 2.4°R for a series of seven topping tests. With the LH_2 transfer line change for the 75K mod system, the temperature rise at the pump inlet is predicted to be 1.95°R (see Section III-D-3.

It can be seen from Figure 13 that for a flow rate of 91 lb/sec and with an NPSP of zero, the predicted pump inlet temperature and tank pressure are 38.05°R and 24 psia respectively. The V-3801, 10% tank level, however, should be set at 36°R and held constant. This can be

accomplished by venting the V-3801 LH₂ storage tank to atmospheric conditions until the 10% level temperatures remains stable. From Figure 13, venting the 3801 tank to atmospheric conditions (12.8 psia) would result in a temperature of 35.7°R.

3. Temperature Rise in Transfer Line

During FEP-III-2 the propellant feed system was operated for approximately 4960 seconds and the maximum measured fluid temperature in the PSL was 41.3°R. The initial unpressurized liquid temperature in V-3801 was 38.25°R and seven periods of topping or V-5001 refill were experienced with a total accumulated time of about 895 seconds. Based on this test data, the fluid temperature at the pump inlet can be kept sufficiently cold during run tank topping to satisfy the 75K engine test requirements.

The predicted LH₂ transfer line steady-state temperature rise between V-3801 (TT-402) and V-5001 (TT-044) as a function of LH₂ flow rate is shown in Figure 14.

The curve is based on FEP-III-2 V-3801 to V-5001 tank topping test data. The temperature rise in the transfer line during the FEP-III-2 tests resulted in a ΔT of 1.05°R. The topping flowrate and system (3-LH-6 transfer line) pressure drop was 75 lb/sec and 80 psi respectively. Utilizing the test data, an equivalent heat gain due to friction and piping system heat leak was obtained for the 75K modified system. This heat gain was utilized to construct the curve shown in Figure 14.

Figure 14 shows that for high flow rates the temperature rise is due mainly to friction. For the lower flow rates the inverse is true. For flow rates between 30 to 60 lb/sec there is relatively little temperature rise.

Details of the calculations concerned with heat transfer and NPSP are presented in Appendices C, D and E.

4. Effect of Rate of Change in Liquid Level on NPSP

The effect of V-5001 liquid level on NPSP was investigated to determine if a realistic requirement for closely matching topping flow to engine flow might exist in the event that a requirement for engine tests at constant NPSP developed.

Based on a V-5001 tank internal diameter of 257 inches and a propellant density of 4.4 lb/ft^3 or $.002546 \text{ lb/in}^3$, the nomograph presented in Figure 15 was constructed from the data listed below, showing the rate of change in liquid head (or NPSP) contributable to the mass flow into or out of V-5001.

<u>Lb/sec</u>	<u>In³/sec</u>	<u>-V-5001 Liquid Level Rate of Change In/sec</u>	<u>Rate of Change In V-5001 Liquid Head Psi/sec</u>
0	0	0	0
25	9819	.1893	.00048
50	19638	.3786	.00096
75	29458	.5679	.000144
100	39276	.7572	.00192

The results shown in Figure 15 indicate that even at gross difference between engine flow and topping flow the rate of change due to liquid head in V-5001 is negligibly small in comparison to possible changes in V-5001 pressure ($\pm .5 \text{ psi}$), or pump inlet LH_2 temperature insofar as control of pump NPSP is concerned. For example, at an engine flow of 80 lb/sec with topping at 10 lb/sec the liquid head change rate is only $-.0012 \text{ psi/sec}$. It was therefore concluded that no practical requirement exists for maintaining the liquid level in V-5001 constant during engine testing.

5. Determination of Steady State Conditions During Topping

During the topping, the attainment of steady-state in the modified topping line will be established by monitoring the following ETS-1

facility parameters. These are the same channels which were monitored during a series of seven topping tests conducted during the FEP-III-2 tests. With exceptions of TT-044, which is assumed to be located in the modified system as it is now in the present 3-LH-6 system.

TT402 - 10% V-3801 LH₂ storage tank temperature, °R.

TT044 - LH₂ transfer line temperature, upstream of V-5001 run tank fill entrance, °R (to be added in new system).

PT-68 - V-3801 LH₂ storage tank ullage pressure, psia,

PT-99 - V-5001 LH₂ run tank ullage pressure, psia.

TT-402 is the measurement of fluid temperature into the 7-LH-12HV and 3-LH-6LV LH₂ transfer line from V-3801 to V-5001. TT-044 is the temperature at the downstream end of the above line. These two measurements will be used to determine any heat leak into the transfer line. Steady-state temperature conditions will exist when the temperature at the entrance to V-5001 run tank (TT-044) remains constant.

The V-3801 and V-5001 tank pressures, PT-068 and PT-099 will be monitored to determine that the ullage tank pressures are constant prior to and during topping. The V-3801 to V-5001 flow resistance has been calculated for the new transfer line configuration based on FEP-III-2 test data so flow rate can be planned for and achieved by selection of the tank pressures. The pressures in both tanks are regulated by the tank ullage pressurization systems. No flow meter is needed in the transfer line for monitoring during the tests. Flow rates will be determined by the V-3801 to V-5001 pressure differential.

Additional parameters will be monitored to determine engine inlet conditions prior to and during the topping. These parameters,

the pump inlet temperatures and pressures. Also, NPSP will be computed on-line and monitored in the Test Diagnostic Center.

E. Transient Operation

1. Response Characteristics of V-5001 Pressurization System

The adequacy of the V-5001 pressurization system to maintain pressure control during rapid transients of engine flow and topping flow was demonstrated during FEP-III-2 conducted 24 March 1967. Data from selected portions of this test are shown in Figures 16 through 19 to illustrate the controlling capability of the system.

Figure 16 shows the action of the PCV-165 control valve and the run tank pressure during initiation and termination of topping with a small ullage in the V-5001 tank. During this time period the outflow from V-5001 was 83 lb/sec. It may be seen from this figure that the V-5001 pressure was maintained within 0.5 psia during both initiation and termination of topping.

Figure 17 presents data for initiation of topping with a large ullage value (i.e., low liquid level) present in V-5001. Again the V-5001 pressure is constant within 1 psia. For this time period the outflow from V-5001 was approximately 77 lb/sec when a topping flow rate of about 67 lb/sec was established. The large motions of PCV-165 shown prior to the range time 50825 were caused by increases in tank pressure demand.

Figure 18 shows the V-5001 pressurization characteristics during termination of topping, followed by termination of V-5001 outflow and subsequent venting of the run tank. It may be seen how termination of the topping flow caused PCV-165 to open from about 50% to

about 70% in order to maintain the tank pressure and then close fully after the V-5001 outflow was terminated. Subsequent full opening of the vent valve PCV-180 caused the tank pressure to decrease at approximately 1.5 psi/sec.

Figure 19 presents the case in which the V-5001 outflow is rapidly terminated while topping flow continues with subsequent increase in the V-5001 liquid level. It is interesting to note that even though the topping flow rate was high (approximately 77 lb/sec) PCV-180 moved only to the 22% open position in order to maintain V-5001 pressure within 1 psi. This shows that the V-5001 automatic vent valve PCV-180 is capable of preventing overpressurization of the tank in the event of an emergency shutdown of the engine while run tank topping is in process.

2. Engine Startup

Run tank topping will not be initiated when the V-5001 liquid level is greater than 80% or following engine startup until the engine system has operated at a steady-state condition for sufficient time to verify that the run tank pressurization system is operating in a normal manner.

3. Engine Steady-State Operation and Flow Ramps

The data presented in Figures 16 through 19 show that run tank topping may be initiated and terminated at any time during engine operation without affecting engine operation. Evaluation of past and planned nuclear engine tests shows that, in general, two types of tests can be anticipated for the 75K engine:

- a. Steady-state tests in which the engine is held at constant conditions for a prolonged period of time;
- b. Mapping tests in which the chamber temperature and pressure (and consequently engine flow) are frequently varied.

Run tank topping for the first of these types was adequately discussed in Section III.D.1. For mapping tests, it would be possible to vary the topping rate or to intermittently top the tank; however, it appears unlikely that such a procedure would be required. A review of the mapping tests planned for XE-1 tests shows that, due to the large volume of the V-5001 tank, it is likely that any mapping tests conducted on the 75K engine could be accommodated by a single period of topping at a constant topping flow rate. To illustrate this capability, an example of a mapping test for a 75K engine is shown in Figure 20. This is a rather extreme example since the test includes a period of high flow rate both at start and shutdown with a total propellant consumption of 82,000 lbs of LH_2 in 1600 sec. It was further decided to see if the topping requirements could be met at the topping flow rate of 50 lb/sec, corresponding to minimum temperature increase in the transfer line (see Figure 14). The results of this example are shown in Figure 20. The topping requirements can easily be satisfied by a single period of topping at a constant flow rate of 50 lb/sec and still maintain the liquid level in V-5001 between 36% and 73% with a 22% shutdown margin.

4. Normal Engine Shutdown

In general, topping will be terminated prior to initiation of a scheduled normal engine shutdown since the propellant consumed during a shutdown is small. In any case, the demands on the

propellant feed control system would be less stringent during a normal engine shutdown than during the emergency shutdown, discussed below, because the rate of change in propellant outflow from V-5001 is lower.

5. Emergency Shutdown

The worst case for an emergency engine shutdown, with respect to the propellant feed system, is for the shutdown to occur when topping is at the maximum flow rate with the V-5001 tank pressure at the maximum operating limit and minimum ullage present in V-5001.

Under these conditions, two requirements must be satisfied:

- a. The V-5001 tank must not be overpressurized.
- b. Topping flow must be terminated prior to overflowing the V-5001 tank with LH_2 .

For the operating conditions established in Section III.D these worst case conditions correspond to a V-5001 liquid level of 80% and a V-5001 tank pressure of 60 psia. The maximum topping flow rate for these conditions, from Figure 8, is 95.5 lb/sec.

The ullage volume of V-5001, corresponding to a liquid level of 80%, is 2892 ft^3 . Assuming a LH_2 density of 4.4 lb/ft^3 the volumetric topping flow rate is $21.7 \text{ ft}^3/\text{sec}$. For the V-5001 pressure to reach the operational limit of 65 psia, the ullage must be reduced to $\frac{60}{65} (2892) = 2669 \text{ ft}^3$ with no venting of the pressurizing gas. This is a reduction in ullage volume of $2892 - 2669 = 223 \text{ ft}^3$. Hence, at the topping flow rate of $21.7 \text{ ft}^3/\text{sec}$ it would require 10.3 sec for the V-5001 tank pressure to reach 65 psia if the outflow from V-5001

were stopped instantaneously. Extrapolation of the pressure control valve action, shown in Figure 19, shows that the vent valve PCV-180 would have opened to 100% within 10.4 sec of first closing motion of PCV-165 had it continued to open at its maximum slew rate. Therefore, it is apparent that the control system is more than adequate to prevent over-pressurization of V-5001 for this worst case. In addition, V-5001 was proof-tested to 82 psig and is further protected by relief valve 50-RV-66 set to relieve at 55 psig with a burst disc in parallel designed to rupture at 65 psig. In addition, rapid venting may be accomplished by opening RSV-390, a 12-inch valve with $C_v = 2900$. Since it would require 48 sec for V-5001 pressure to reach 94 psia at constant topping flow and no venting, sufficient time for manually opening RSV-390 is available if needed.

For an ullage volume of 2892 ft^3 and a topping flow of $21.7 \text{ ft}^3/\text{sec}$, there is $\frac{2892}{21.7} = 133.3 \text{ sec}$ available for closing the security valves in the propellant topping line, to prevent overflowing V-5001 with LH_2 .

F. Analysis of Water Hammer Effects

A simplistic ROM analysis was made of the maximum pressure spike conditions for the proposed 75K LH₂ topping line due to water hammer.

Calculations and assumptions are shown in Appendix F. The analysis showed that if the valve is closed in .28 sec., or less, a pressure rise of 425 psid will occur. Due to the simplifying assumptions outlined in Appendix F, the accuracy of this number is undetermined. The value may not necessarily be conservative. In addition to maximum pressure considerations, the probability of large pressure gradients at bends must be acknowledged.

To analyze longer closing times would require the use of a large computer program. Such analysis would, however, not account for two-dimensional effects in bends (17 total) or reflections in area reducers (2 total). It is not believed that such analysis would give adequate information to relate closing time and structural design. There is no program available at AGC Sacramento to perform such analysis. To do this analysis on such a program (if one is available elsewhere) would cost \$5,000 - 10,000 combined computer and manpower time.

A simple analogy was made to a water hammer recorded in XECF-EP-II in an LN₂ line. On this test a 50 psid over pressure was noted for a valve closing time of four (4) seconds. Through analogous comparisons of variables, it is estimated that during 100 lb/sec LH₂ flow in the 75K mods line a condition 50% to 75% as severe would result for the same closing time. As with the other calculations, the accuracy is indeterminable and values may not be conservative.

As 120 seconds is the minimum time required for valve closing, it is

recommended that the valve be dampened so the maximum accidental closing rate is 10 seconds. In addition, as a part of normal facility testing, a determination should be made of the quickest closing time in which water hammer is noticable. A proposed plan is to close the transfer line valves in 30 seconds the first time topping is required and to reduce the closing time by about 5 seconds during each succeeding test until fluid hammer is first noticable.

INSTRUMENTATION DATA					
INSTRUMENT NUMBER	SENSOR TYPE	LOCATION (FULL TO EMPTY)	LH ₂ TANKAGE BASED ON		
			%	GALLONS	LB (AT 4.35 LB/FT ³)
LT492	RESISTOR	440.8"	100	245,934	143,014
LT490	RESISTOR	384.4"	90.5	222,763	129,540
LT489	RESISTOR	358.9"	85.7	210,846	122,609
LT486	RESISTOR	304.7"	71	174,785	101,640
LT484	RESISTOR	272.4"	61.1	150,548	87,546
LT482	RESISTOR	241.1"	51.1	126,167	73,368
LT480	RESISTOR	209.6"	41.1	101,619	59,093
LT478	RESISTOR	176.8"	31	76,980	44,765
LT476	RESISTOR	141.0"	20.9	52,184	30,345
LT474	RESISTOR	98.1"	10.7	27,111	15,765
TT 35 TT401 TT402	RTT	58.4"			
LT472	RESISTOR	17.5"	0	975	567

LIQUID HYDROGEN STORAGE VESSEL DATA		
INNER VESSEL		OUTER VESSEL
1.47	SHELL THICK., IN.	.8125
143.7 (AIR)	TEST PRESS., PSIG	18.75 (AIR)
0	CORR. ALLOW., IN.	0
100 PSI	MAX. OPER. PRESS.	FULL VACUUM
115 PSI	MAX. A.W. PRESS.	0 PSIG
INNER VESSEL 115 PSIG @ -423°F		
TEMP RANGE -423°F TO 100°F		
MANUFACTURED BY CHICAGO BRIDGE AND IRON		
DWG NO. AETRON 3338-M1 & 2		

V3801 - LH₂ Storage Tank

FIGURE 1

NOTE:
DIMENSIONS ARE IN
INCHES, MEASURED
JULY, 1967

Liquid Hydrogen Fill Line (Existing)

FIGURE 2.

NO. 558 VOLUME MAP & DRAWING INDEX

NOT FOR CONSTRUCTION

TITLE NERVA TEST OPERATIONS E ENGINE MOD. L _H TOPPING LINE INSTRUMENTATION & CONTROL MONETRIC	
DATE 20790	DRAWING NO. F101343
SCALE 1/4" = 1'-0"	

JOB No. 947

INSTRUMENTATION DATA							
INSTRUMENT NUMBER	SENSOR TYPE	INSTRUMENT NUMBER	SENSOR TYPE	LOCATION (FULL TO EMPTY)	LH ₂ TANKAGE BASED ON 69,534 GAL = 100%		
					%	GALLONS	LBS (AT 4.4 LB/FT ³)
TT518	RTT	LT452	RESISTOR	360.68"	100	69,534	40,900
		LT451	RESISTOR	329.12"	90	62,500	36,800
TT517	RTT	LT450	RESISTOR	297.82"	80	55,500	32,600
		LT445	RESISTOR	266.53"	70	48,600	28,600
TT516	RTT	LT444	RESISTOR	235.23"	60	41,700	24,500
		LT443	RESISTOR	203.83"	50	34,700	20,400
TT515	RTT	LT442	RESISTOR	172.63"	40	27,800	16,400
		LT441	RESISTOR	141.34"	30	20,800	12,200
TT514	RTT	LT440	RESISTOR	110.04"	20	13,900	9,700
TT513	RTT	LT438	RESISTOR	77.60"	10	7,000	4,100
TT032	RTT	LT437	RESISTOR	69.54"	8	5,600	3,400
		LT436	RESISTOR	60.08"	6	4,200	2,500
		LT435	RESISTOR	48.37"	4	2,800	1,600
		LT434	RESISTOR	32.28"	2	1,400	800
		LT433	RESISTOR	20.46"	1	700	400
		LT432	RESISTOR	0"	0	0	0

LIQUID HYDROGEN RUN VESSEL DATA		
INNER VESSEL		OUTER VESSEL
0.8125	SHELL THICKNESS, IN.	0.6875
0.8270	HEAD THICKNESS, IN.	0.6875
0	CORRUGATE ALLOW.	0
50 PSIG	MAX OPER. PRESS, PSI	FULL VAC
50 PSIG	MAX ALLOW. W.P., PSI	FULL VAC

DWG. NO. AETRON 3350-M1 & 2
INNER VESSEL 65 PSI @ 70°F
TEMP RANGE -423° TO 70°F
CAPACITY 77,000 GAL. MAX
HORTON PRESSURE VESSEL
MANUFACTURED BY CHICAGO
BRIDGE AND IRON

V5001 - LH₂ Run Tank

FIGURE 4.

NOTES:
 DIMENSIONS ARE IN INCHES,
 MEASURED JULY, 1967

GH₂ PRESSURIZATION LINE

FIGURE 5

PRESSURIZATION & VENTING SYSTEM
V3801 & V5001

PRESSURE DIFFERENTIAL
PSID

100

90

80

70

60

50

40

30

20

10

0

0 10 20 30 40 50 60 70 80

LH₂ TOPPING FLOW RATE, PPS

△ Determined by change
in V-3801 Level.

○ Determined by change
in V-5001 Level.

Figure 7

Topping Flow Rate vs Differential Pressure

— Predicted

Between V-3801 and V-5001

TOPPING FLOW RATE
VS
DIFFERENTIAL
PRESSURE

Figure 8

7/10/68

(16)

CAPABILITY OF TAPPING SYSTEM FOR
A STEADY STATE RUN CONSUMING 90,000 LB
OF H_2

CAPABILITY OF TOPPING SYSTEM TO
UTILIZE ALL AVAILABLE PROPELLANT
DURING A STEADY STATE RUN

FIGURE 10

Figure 11
Hydrogen Vapor Pressure - vs. - Temperature

TOPPING
NDSP VS PUMP INLET TEMPERATURE

FIGURE 13.

7/25/68
FIGURE 1F

FIGURE 15

INITIATION AND TERMINATION OF TOPPING

REP-III-2 24 MAR 67

INITIATION OF TOPPING

FEP-III-2 24 MAR 67

TOPPING FLOW RATE

63.3 $\mu\text{f/sec}$ (CALC FROM V-5001 LEVEL)

66.7 $\mu\text{f/sec}$ (CALC FROM V-5001 LEVEL)

VALVE POSITION, % & PRESSURE, PSIA
& FLOW RATE, $\mu\text{f/sec}$ & LEVEL

FIGURE 17

TERMINATION OF TOPPING AND V-5001 CUTOFF

FEP-III-2-24 VIA 67

V-5001 LIQUID LEVEL = 15%

TERMINATION OF V-5001 OUTFLOW WITH TOPPING

FCP-TIT-2 24 MAR 67

TOPPING FLOW RATE -

68.3 #/SEC (CALC FROM V-5001 LEVEL CHANNEL)

66.7 #/SEC (CALC. FROM V-5001 LEVEL CHANNEL)

FIGURE 19

Figure 20

75K 002

AEROJET-GENERAL CORPORATION
SACRAMENTO • CALIFORNIA

APPENDIX A

AGCS-0800-11

REPORT NO

PAGE 1 OF

SUBJECT

Flow Resistance and Friction factor
Determination 6" and 12" Pipe system

DATE

WORK ORDER

BY

ALO

CHK BY

DATE

7/18/68

≡ Pipe Length - 7-LH-12HV

77'	0"	x Valves = 2
7'	10"	x 90° L's = 5
10'	6"	x T's = 3
2'	4"	x 45° L's = 5
2'	10"	
10'	2"	x Reducer 1 = 1 12"X6"-90° Red ELL
24'	11"	x filter = 1
93'	3"	420' 0"
51'	5"	21 0
90'	3"	9 6
33'	0"	<u>450' 6"</u>
415'	54"	
		<u>415</u>
		<u>5</u>
		420'

≡ Pipe Length - 3-LH-6LV

10'		
21'	11"	x 90° L's = 5
13	5	
7	0	x T's = 2
1	6	
70	0	x Reducer = 1 6"X12"
6	7	x 45° L's = 2
128	29	

130'

Per drawing EDSK-0236 - Isometric View - 75K
Modis - LH₂ Topping, ...

SUBJECT

DATE

WORK ORDER

BY

CHK BY

DATE

ALO

75K MOD

7/18/68

From FER-III-2 test Data and existing 3-LH-6LV line configuration (3 April 1967). For topping flowrate of 75 lb/sec the pressure differential between V3801 and V5001 (PT68 and PT99) is 80 psi. Pressure drop through valves:

$$50 \text{ RSV } 252 - C_v = 800$$

$$50 \text{ RSV } 129 - C_v = 800$$

$$38 \text{ RSV } 83 - C_v = 800$$

From CRANE, Flow of Fluids

$$Q = \frac{\dot{W}}{\rho} (7.48)(60) ; Q = 7.90 C_v \sqrt{\frac{\Delta P}{\rho}}$$

$$\frac{\dot{W}}{\rho} (7.48)(60) = 7.90 C_v \sqrt{\frac{\Delta P}{\rho}}$$

$$\Delta P = \frac{\dot{W}^2 3228.9}{C_v^2 \rho}$$

$$T = 38$$

$$P_g = 100 \text{ psia}$$

$$\rho = 4.44$$

1)

3 Valves

$$\Delta P = \frac{(75)^2 3228.9}{(800)^2 4.44} \times 3 = 19.175 \text{ psi}$$

APPENDIX A

AEROJET-GENERAL CORPORATION
SACRAMENTO • CALIFORNIA

AGCS-0800-11

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75K MOD

BY

ALD

CHK BY

DATE

7/18/68

2) Pressure drop through 50LF-6 filter

Spec test data for filter per NTO
@ 100 lb/sec flowrate; $\Delta P = 5 \text{ psi}$

$$\frac{\Delta P_1}{\Delta P_2} = \frac{\dot{W}_1^2}{\dot{W}_2^2}$$

$$\Delta P = \frac{5 \times 75^2}{100^2} = 2.813 \text{ psi}$$

3) To determine friction factor for the 3-LH-6 line per test data FEP III-2, it is assumed that the line from V3801 to V5001 is 6".

$$\Delta P_{\text{pipe}} = \Delta P_{\text{Total}} - \Delta P_{\text{valve}} - \Delta P_{\text{filter}}$$

$$\Delta P_p = 80 - 19.175 - 2.813$$

$$\Delta P_p = 58.012 \text{ psi}$$

Marks Hd8
4th Ed pg 267

$$\Delta P = \frac{f L \dot{W}^2}{1858 \times 10^4 \times D^5 \times S}$$

$$f = \frac{\Delta P (1858) (10^4) (D)^5 S}{L \dot{W}^2}$$

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75K A102

$$f = \frac{(58.012)(1858 \times 10^4)(6.35)^5(.071)}{(638)(75)^2(3600)^2}$$

$$\frac{6644''}{12} = 553.6'$$

$$\frac{\text{fittings } - 36}{518}$$

$$\frac{C'/\text{fitting } 120}{638'}$$

$$f = .01699$$

6" x 12" pipe System

3-LH-6LV & 7-LH-12HV

- 1) Pressure drop through 2 security
12" valves

$$C_v = 2000$$

$$\Delta P = 2 \left(\frac{56.8^2 W^2}{C_v^2 \rho} \right)$$

$$= 3.015 \text{ psi}$$

- 2) Pressure drop through filter

$$\Delta P = \frac{5 \times 91^2}{100^2} = 4.14 \text{ psi}$$

* Per isometric XE-1 Prediction Report
Date 11/12

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3) Pressure drop through 5 - 90° L's
12"

$$\frac{v}{d} = \frac{18}{12.39} = 1.453$$

$$\frac{L}{D} = 12.5$$

$$K = f \times \frac{L}{D} \times N = .01699 (12.5) (5)$$

$$= 1.062$$

$$A = 120.5$$

$$\Delta P = \frac{K}{A^2} \frac{w^2}{2g\rho} = \frac{(1.062)(91)^2(144)}{(120.5)^2(64.4)(4.44)}$$

$$= .305 \text{ psi}$$

Pressure drop through 5 - 90° L's
6"

$$\frac{v}{d} = \frac{9}{6.35} = 1.42 \quad \frac{L}{D} = 12.3$$

$$K = .01699 (12.3) (5) = 1.045$$

$$\Delta P = \frac{(1.045)(91)^2(144)}{(31.65)^2(64.4)(4.44)} = 4.35 \text{ psi}$$

$$A = 31.65$$

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7/11/64

75K MID

4) Pressure drop through 3 T's
12"

Per
Louis Dodge
Product Engineering

$$K = 0.1 \times 3 = 0.3$$

$$\Delta P = \frac{(0.3)(91)^2(144)}{(120.5)^2(64.4)(4.44)} = 0.0862$$

Pressure drop through 2 T's
6"

$$K = 0.1 \times 2 = 0.2$$

$$\Delta P = \frac{(0.2)(91)^2(144)}{(31.65)^2(64.4)(4.44)} = 0.8326 \text{ psi}$$

5) Pressure drop through 5-45° EL's
12"

Per
Louis Dodge

$$K = .17 \times 5 = 0.850$$

$$\Delta P = \frac{(.85)(91)^2(144)}{(120.5)^2(64.4)(4.44)} = .2441$$

Pressure drop through 2-45° EL's
6"

$$K = .17 \times 2 = .34$$

$$\Delta P = (.34)(4.1645) = 1.4157 \text{ psi}$$

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6) Pressure drop through Reducer
12"x6" (2)

$$L = 8''$$

$$d_1 = 6.35''$$

$$\alpha = 40^\circ$$

$$d_2 = 12.39''$$

$$\frac{L}{d} = \frac{8}{6.35} = 1.259$$

$$\frac{d_2}{d_1} = 1.95'$$

$$K = 0.6$$

$$\Delta P_{6''} = (0.6)(4.1645) = 2.498 \text{ psi}$$

$$\Delta P_{12''} = (0.6)(.28721) = .1723 \text{ psi}$$

7) Pressure drop in 12" Line

$$L = 450' \text{ Per Dwg EDSK 0236}$$

$$\Delta P = \frac{f L W^2}{1858 \times 10^4 D^{5.5}}$$

$$\Delta P = \frac{(.01699)(450)(91)^2(3600)^2}{1858 \times 10^4 (12.39)^{5.5} (.071)}$$

Marks 4th Ed
P 207

$$= 2.130 \text{ psi}$$

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8) Pressure drop in 6" Line

$$L = 130'$$

$$D = 6.35"$$

$$f = .01699$$

$$S = .071$$

$$W = 71 \times 3600$$

$$\Delta P = \frac{(.01699)(130)(71)^2(3600)^2}{1858 \times 10^4 \times 6.35^5 \times .071} = 17.403$$

Total pressure drop through system

$$\text{Valves} = 3.015$$

$$\text{Filter} = 4.146$$

$$12''-90^\circ \text{EL} = 0.305$$

$$6''-90^\circ \text{EL} = 4.350$$

$$12''-T's = 0.096$$

$$6''-T's = 0.833$$

$$12''-45^\circ = 0.244$$

$$6''-45^\circ = 1.416$$

$$\text{Reducer} = 2.670$$

$$12'' \text{ line} = 2.130$$

$$6'' \text{ line} = \underline{17.403}$$

$$\Delta P \text{ Total} = 36.492 \text{ psi}$$

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System K_w

$$K_w = \frac{\dot{w}}{\sqrt{\Delta P_s}} = \frac{(91)}{\sqrt{(36.492)(.071)}}$$

$$= 56.535$$

ΔP	$\dot{w}$
75 psi	130.5 lb/sec
65	121.5
55	111.7
50	106.5
45	101.1
35	89.1

$$\left(\frac{K}{A_2}\right) = \frac{(36.5)(64.4)(4.44)}{(91)^2 144}$$

$$= 0.00875 \text{ in}^{-4}$$

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STEADY STATE TOPPING CAPABILITY

BY

RJW

CHK BY

DATE

A. For test consuming 90,200 LB of LH₂

Propellant in V-5001 at test start = 40,900 LB

" " " " " termination = 4100

Propellant Supplied by V-5001 = 36,800 LB

Topping Required = 90,200 - 36,800 = 53,400 LB

If topping is started when V-5001 reaches 80% level
then 53,200 LB must be transferred from V-3801 to
V-5001 in the time period that 36,800 - 8,800 = 28,000 LB
are used from V-5001.

V-5001 supply flow / Topping flow = $\frac{28000}{53400} = 0.524$

Engine flow = Topping flow + V-5001 supplied flow

or Topping flow = Engine flow / 1.524

Engine flow LB/sec	Topping flow LB/sec	ΔP req'd. Modified System psi	ΔP req'd. Present System
60	39.4	6.8	22
70	45.9	9.3	30
80	52.5	12.2	40
90	59.0	15.5	50
100	65.6	19.0	62
110	72.2	22.8	75

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STEADY STATE TOPPING CAPABILITY

BY

RJW

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B. For test using total V-5001 Capacity

wt of LH₂ in V-3801 at start of test = 94,414 LB

$$V-5001 \text{ flow} / \text{Topping flow} = \frac{28000}{94,414} = 0.296 \text{ L.B.}$$

$$\text{Topping flow} = \text{Engine flow} / 1.296$$

Engine flow LB/sec	Topping flow LB/sec	ΔP Req'd Modified System Psi	ΔP Req'd Present System Psi
60	46.3	9.5	30.3
70	54.0	12.9	41.3
80	61.7	16.8	54.5
90	69.5	21.1	69
100	77.2	26.0	84.5
110	85.0	31.8	102.5

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Predicted Pressure Loss and NPSP vs $\dot{w}$ -
LH₂ V5001 Run Tank To Pump Inlet Line -
No Topping

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A.L.O.

CHK BY

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7/19/68

Per page 9 to 14

$$\frac{K}{A^2} = .0015074$$

$$\Delta P = \frac{K}{A^2} \frac{\dot{w}^2 144}{2g\rho} - \Delta P_H$$

Elevation head at pump

80% LH₂ Tank Level $\approx$ 1.76 psi

20% LH₂ Tank Level $\approx$ 1.30 psi

$$T_p = 38^\circ R$$

$$\rho = 4.33 \text{ lb/ft}^3$$

lb/sec lb²/sec² in⁻⁴

$\dot{w}$	$\dot{w}^2$	K/A^2	$144/2g\rho$	Line Loss PSI	80% P_H	20% P_H	ΔP_{80}	ΔP_{20}
120	14400	.0015074	0.5164	11.21	-1.76	-1.30	9.45	9.91
110	12100			9.48			7.72	8.18
100	10000			7.78			6.02	6.48
90	8100			6.31			4.55	5.01
80	6400			4.98			3.12	3.58
70	4900			3.81			2.05	2.51
60	3600			2.80			1.04	1.50
50	2500			1.95			0.19	0.65
40	1600			1.25			-0.51	-0.05
30	900			.70			-1.06	-0.60
20	400			.31			-1.45	-0.99
10	100			.08			-1.68	-1.22
0	0	0	0	0	1.76	1.30	-1.76	-1.30

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$P = 16/43$

36°R	37°R	38°R	39°R	40°R	41°R	42°R
$P = 4.46$	$P = 4.39$	$P = 4.33$	$P = 4.31$	$P = 4.28$	$P = 4.20$	—
V.P. = 13.5	V.P. = 16	V.P. = 19	V.P. = 21.5	V.P. = 25	V.P. = 28	V.P. = 32.5

$P_{TANK} = 30 \text{ psia}$ $T_p = 38^\circ R$
 $VP = \text{Vapor Pressure, psia}$
 $NPSP = \text{Tank Pressure} - \text{Vapor Pressure} - (\text{Line Loss} + \text{Elevation Head})$

W	P _{TANK}	V.P.	Line Loss + Head		NPSP	
			80%	20%	80%	20%
lb/sec	psia	psia	psi	psi	psi	psi
120	30	19	-9.45	-9.91	1.55	1.09
110	↓	↓	-7.72	-8.18	3.28	2.82
100			-6.02	-6.48	4.98	4.52
90			-4.55	-5.01	6.45	5.99
80			-3.12	-3.58	7.88	7.42
70			-2.05	-2.51	8.95	8.47
60			-1.04	-1.50	9.96	9.50
50			-0.19	-0.65	10.81	10.35
40			+0.51	+0.05	11.51	11.05
30			+1.06	+0.60	12.06	11.60
20			+1.45	+0.99	12.45	11.99
10	+1.68	+1.22	12.68	12.22		
0	+1.76	+1.30	12.76	12.30		

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$P_{tank} = 35 \text{ psia}$

$T_p = 38^\circ R$

$VP = \text{Vapor Pressure}$

$NPSP = \text{Tank Pressure} - \text{Vapor Pressure} - (\text{Line Loss} + \text{Elevation Head})$

Q	P_{tank}	VP	Line Loss Plus Head		NPSP	
			80%	20%	80%	20%
lb/sec	psia	psia	psi	psi	psi	psi
120	35	19	-9.45	-9.91	6.55	6.09
110			-7.72	-8.18	8.28	7.82
100			-6.02	-6.48	9.98	9.52
90			-4.55	-5.01	11.45	10.99
80			-3.12	-3.58	12.88	12.42
70			-2.05	-2.51	13.95	13.49
60			-1.04	-1.50	14.96	14.50
50			-0.19	-0.65	15.81	15.35
40			+0.51	+0.05	16.51	16.05
30			+1.06	+0.60	17.06	16.60
20			+1.45	+0.99	17.45	16.99
10			+1.68	+1.22	17.68	17.22
0			+1.76	+1.30	17.76	17.30

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$$T_p = 36^\circ R$$

$$\rho = 4.1616 / ft^3$$

$$\frac{1.4}{2.8} = 0.5$$

lb/sec lb²/sec² in⁻⁴

Line

$\dot{w}$	$\dot{w}^2$	K/A^2	$144/2g\rho$	Loss PSI	$80\%P_H$	$20\%P_H$	ΔP_{80}	ΔP_{20}
120	14400	1.0015074	501.4	10.88	-1.76	-1.30	9.12	9.58
110	12100			9.15			7.39	7.85
100	10000			7.56			5.80	6.26
90	8100			6.12			4.36	4.82
80	6400			4.84			3.08	3.54
70	4900			3.70			1.94	2.40
60	3600			2.72			.96	1.42
50	2500			1.89			.13	0.59
40	1600			1.21			-.45	-.09
30	900			0.68			-1.08	-.62
20	400			0.30			-1.46	-1.00
10	100			0.07			-1.69	-1.23
0	0			0			-1.76	-1.30

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$$\Delta P = \frac{\omega^2 K 149}{A^2 2g} \times \frac{1}{\rho}$$

$$T_p = 37^\circ R$$

$$\rho = 4.39 \text{ lb/ft}^3$$

ω			Line Loss PSI	80% P _H	20% P _H	ΔP_{80}	ΔP_{20}
120	48.535	1.2278	11.06	-1.76	-1.30	9.30	9.76
110	40.783		9.29			7.43	7.89
100	33.705		7.68			5.92	6.38
90	27.301		6.22			4.46	4.92
80	21.571		4.91			3.15	3.61
70	16.516		3.76			2.00	2.46
60	12.134		2.76			1.00	1.46
50	8.426		1.92			0.16	0.62
40	5.393		1.23			-0.53	-0.07
30	3.033		0.69			-1.07	-0.62
20	1.348		0.31			-1.45	-0.99
10	0.337		0.08			-1.68	-1.22
0			0	-1.76	-1.30	-1.76	-1.30

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$$T_p = 39^\circ R$$

$$\rho = 4.31$$

$$\Delta P = \frac{w^2 K}{11^2 28} \times \frac{1}{\rho}$$

w			Line Loss PSI	80% P _H	20% P _H	ΔP_{80}	ΔP_{20}
120	48.535	2320	11.26	-1.76	-1.30	9.50	9.96
110	40.783		9.46			7.70	8.16
100	33.705		7.82			6.06	6.52
90	27.341		6.33			4.57	5.03
80	21.571		5.00			3.24	3.70
70	16.516		3.83			2.07	2.53
60	12.134		2.82			1.06	1.52
50	8.426		1.95			0.19	0.65
40	5.313		1.25			-0.51	-0.05
30	3.033		0.704			-1.06	-0.60
20	1.348		0.31			-1.45	-0.99
10	0.537		0.08			-1.68	-1.22
0						-1.76	-1.30

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$$T_p = 40^\circ R$$

$$\rho = 4.28 \text{ lb/ft}^3$$

$$\frac{\omega^2 R}{A^2} \times \frac{1}{\rho}$$

↓ ↓

ω			Line Loss PSI	80% P _H	90% P _H	ΔP_{80}	ΔP_{90}
120	47.535	.0336	11.34	-1.76	-1.30	9.58	10.04
110	40.783		9.53			7.77	8.23
100	33.705		7.87			6.11	6.57
90	27.301		6.38			4.62	5.08
80	21.571		5.03			3.27	3.73
70	16.516		3.86			2.10	2.56
60	12.134		2.83			1.07	1.53
50	8.426		1.97			0.21	0.67
40	5.393		1.26			-0.50	-0.04
30	3.033		0.709			-1.05	-0.59
20	1.346		0.31			-1.45	-0.99
10	0.337		.08			-1.68	-1.22
0						-1.76	-1.30

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$T_p = 410^\circ R$

$P = 4.20 \text{ lb/gal}$

$\dot{W}^2 K \frac{199}{A^2 28} \times$

$\frac{1}{P}$

$\dot{W}$	$\downarrow$	$\downarrow$	Line Loss PSI	80% P_H	20% P_H	ΔP_{80}	ΔP_{20}
120	48.535	.2381	11.55	-1.76	-1.30	9.77	10.25
110	40.783		9.71			7.95	8.41
100	33.705		8.03			6.27	6.73
90	27.301		6.50			4.74	5.20
80	21.571		5.14			3.38	3.84
70	16.516		3.93			2.17	2.63
60	12.134		2.89			1.13	1.59
50	8.426		2.01			0.25	0.71
40	5.393		1.28			-0.48	-0.02
30	3.033		0.72			-1.04	-0.58
20	1.348		0.32			-1.44	-0.98
10	0.337		0.08			-1.68	-1.22
0				-1.76	-1.30	-1.76	-1.30

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Pump Inlet Line -

BY

ALD

CHK BY

Component Data

1. Potter flow meter, Catalog No 5945
8" by 7"
2. 8 in Hadley Valve; $C_v = 3473$
3. Strainer, 4 mesh by .041 in dia
wire cone 10 in high by 8" in
diameter

General Data

1. ID = 8.329" (ID varies see RNS-0289)
2. OD = 8.125"
3. material: stainless steel
4. Dewar Outlet temperature = $38^\circ R$
5. Max heat gain at full flow
= $1.95^\circ R$

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Pump Inlet Line - K/A^2 Loss
Coefficient

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1. Line Loss coefficient from V5001 Dewar to
50 FT-14 Potter flow meter (obtained from
test data FEP-III-2)

$$\frac{K}{A^2} = 0.0006681 \text{ in}^{-4}$$

2. 50 FT 14 Flow meter

Test data FEP-III-2; $\Delta P \approx 1.51 \text{ psi}$
for $\dot{w} = 78 \text{ lb/sec}$

$$\frac{\Delta P_1}{\Delta P_2} = \frac{\dot{w}_1^2}{\dot{w}_2^2}$$

$$\Delta P_1 = \frac{(1.51)(91)^2}{(78)^2} = 2.055 \text{ psi}$$

$$K = \frac{\Delta P \times 2g \times \rho \times A^2}{\dot{w}^2 \times 144}$$

$$= \frac{(2.055)(64.4)(4.33)(54.5)^2}{(91)^2 \times 144} = 1.4273$$

$$\frac{K}{A^2} = .0004805$$

3. Straight Line below Potter meter
per Design Data Book RN-S-0289

$$\frac{K}{A^2} = 0.000292 \text{ in}^{-4}$$

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PUMP INLET LINE

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4. Strainer

Per Design Data Book RNS-0289

$$\frac{K}{A^2} = .0000005 \text{ in}^{-4}$$

5. straight line below strainer

Per Design Data Book RNS-0289

$$\frac{K}{A^2} = .0000323 \text{ in}^{-4}$$

6. Expansion section

a) Ref P-12 Louis Dodge
(Product Engineering)

b) New adapter - per RD-DR-050

14" Flange
New

$\gamma = 8^\circ$ assumed,

$$\frac{d_2}{d_1} = \frac{13.25}{8.329} = 1.591$$

$$K = 0.06$$

$$\frac{K}{A^2} = .0000202 \text{ in}^{-4}$$

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7. PSV

Assuming similar pressure loss through PSV per test Data 31 March 1966, AGC Test Area;

$$\dot{W} = 80 \text{ lb/sec} \quad \Delta P = 0.745 \text{ psi}$$

$$\frac{\Delta P_1}{\Delta P_2} = \frac{\dot{W}_1^2}{\dot{W}_2^2}$$

$$\Delta P_1 = \frac{(0.745)(91)^2}{(80)^2} = 0.964 \text{ psi}$$

$$\frac{K}{A^2} = \frac{\Delta P \cdot 2g \cdot \rho}{\dot{W}^2 \cdot 144} = .000225 \text{ in}^{-4}$$

8. Straight line, PSV to Bellows

Per Design Data Book RN-S-0289

$$\frac{K}{A^2} = .0000191 \text{ in}^{-4}$$

9. Bellows

Per Design Data Book RN-S-0289

$$\frac{K}{A^2} = .0000037 \text{ in}^{-4}$$

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10. straight line to pump

$$\frac{K}{A^2} = .0000284 \text{ in}^{-4}$$

Total Loss Coefficient.

	$\frac{K}{A^2}$
Dewar to 50 FT 1/4 Potter Flowmeter	.0006681
50 FT 1/4 Pottermeter	.0004805
st. line below Pottermeter	.0000292
strainer	.0000005
straight line below strainer	.0000323
Expansion section	.0000202
PSV	.0002254
st line, PSV to Bellows	.0000191
Bellows (open)	.0000037
st line to Pump	.0000284

$$\Sigma \left(\frac{K}{A^2} \right) = .0015074 \text{ in}^{-4}$$

$$\Delta P = \left(\frac{K}{A^2} \right) \frac{\dot{W}^2_{144}}{2g\rho} - \Delta P_H$$

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where:

ΔP = pressure drop, psid

$\dot{w}$ = flow rate, lb/sec

g = gravity constant, 32.17 ft/sec²

ρ = density, lb/ft³

$\frac{K}{A^2}$ = total loss coefficient, in⁻⁴

ΔP_H = pressure difference due to liquid elevation

$$\overline{1K} = \left(\frac{K}{A^2} \right) \frac{144}{2g} = \frac{1.5074 \times 10^{-3} \times 144}{64.34}$$

$$= 3.374 \times 10^{-3} \frac{\text{sec}^2}{\text{ft}^3 \text{in}^2}$$

$$\Delta P = \frac{\overline{1K} \dot{w}^2}{\rho} = 3.374 \times 10^{-3} \frac{\dot{w}^2}{\rho} - \Delta P_H$$

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Temperature increase from V3801
(TT432) to V5001 (TT044)

BY

JLO

CHK. BY

Conditions at V3801

$$T_{402} = 37^{\circ}R \quad \rho = 4.44$$

$$P_{98} = 100 \text{ psia}$$

$$C_{pA} = 2.293$$

System K_v factor = 56.535
(LH_2 Transfer line V3801 to V5001)

Assume C_p, ρ remain constant

$$K = \frac{\Delta P \times 2g \times \rho \times A^2}{\dot{w}^2 \quad 144}$$

$$\Delta P = \frac{K \dot{w}^2 (144)}{2g \rho A^2}$$

$$\Delta H \text{ of } \text{LH}_2 = \frac{\Delta P (144)}{\rho}$$

$$Q = \frac{\Delta P (144)}{\rho} \approx \text{Btu/lb}$$

OR -
$$\Delta T_{\text{rise}} = \frac{\Delta P (144)}{\rho \cdot 778 C_p} = 0.185 \frac{\Delta P}{\rho C_p}$$

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$$\frac{\Delta T_1}{\Delta T_2} = \frac{\Delta P_1}{\Delta P_2} = \frac{\left(\frac{K}{\rho^2}\right)_1 \dot{\omega}_1^2}{\left(\frac{K}{\rho^2}\right)_2 \dot{\omega}_2^2}$$

$$= \frac{80}{36.5} = 2.19$$

From FEP-III-2 Test Data,
see pg 2 of Flow Resistance calculations
 ΔT rise during topping at a
flow topping rate of 75 lb/sec
was equal to 1.05 °R

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Therefore $\Delta T_1 = 1.05^\circ R$ from
test Data for the 3-LH-6 piping
configuration

$$\Delta T_2 = \frac{1.05}{2.19}$$

$$\Delta T_2 = 0.479^\circ R$$

ΔT_2 is the increase in
temperature predicted for the
3-LH-6 + 7-LH-12 transfer LH₂ system
of the 75K Mod. from TT402 to TT049.
due to frictional heat. plus line heat leak.

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Temperature Increase from TT049
(V5001) to Pump Inlet.

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From FEP-III-2 data the temperature increase from TT049 to PCV750 was 1.19°R. PCV 750 was substituted in place of the pump for the facility checkout and topping flow test. The inlet temperature to PCV 750 was monitored just downstream of the valve, at a similar position to that for pump inlet conditions.

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The flow resistance for this system:

$$\left(\frac{K}{A^2}\right) = 0.00155 \text{ in}^{-4}$$

The LH_2 out flow through PCV750 for a topping flow rate of 75 lb/sec was:

$$\dot{w} = 82 \text{ lb/sec}$$

For the 75K mod system:

$$\left(\frac{K}{A^2}\right) = 0.0015074$$

$$\dot{w} = 91 \text{ lb/sec}$$

From page 2 this set:

$$\frac{\Delta T_1}{\Delta T_2} = \frac{\left(\frac{K}{A^2}\right)_1 \dot{w}_1^2}{\left(\frac{K}{A^2}\right)_2 \dot{w}_2^2} = \frac{.00155 (82)^2}{.00151 (91)^2} = .832$$

$$\Delta T_2 = \frac{1.19}{.833} = 1.43^\circ \text{R}$$

@ pump inlet

SUBJECT

Predicted Temperature Rise - V3801 to V5001
Transfer Ltr Line with flow rate in.

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Pipe length 7-LH-12 HV $\approx 450'$

90° L's = 5

45° L's = 5

Reducer = 1

filter = 1

T's = 3

$$\frac{450'}{45' / jt} \approx 10 \text{ welded joints}$$

$$\frac{19 \text{ welded joints per dwg EDSK0236}}{29}$$

From Linde Co. Design Manual

Joints $29(3.5)(11.9) = 1207.85$ 90° L's $5(3.5)(3) = 52.50$ 45° L's $5(3.5)(2) = 35.00$ Reducer $1(3.5)(4.5) = 7.00$ filter $1(3.5)(15) = 52.50$ T's $3(3.5)(4.3) = 45.15$ Valves $2(3.5)(55) = 385.00$ 1785.0 Btu/hr. $\frac{1785}{3600} = 0.496 \text{ Btu/sec}$

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Per Linde Manual - Design Manual
for Vacuum SI Insulated Piping pg 81. For a
10" hq system

$$d_1 = 1" \approx q_1 = 14.2 \text{ Btu/hr/ft}$$

$$d_2 = 2"$$

$$q_2 = 26 \text{ Btu/hr/ft}$$

$$\text{Linde Manual } q \approx 28$$

$$d_1 = 6" \approx q_1 = 72 \text{ Btu/hr/ft}$$

$$d_2 = 8"$$

$$q_2 = \frac{72 \times 8}{6} = 96 \text{ Btu/hr/ft}$$

$$\text{Linde Manual } q \approx 93$$

Since the manual pg 81 curves only provide
information up to an 8" pipe

Then:

$$d_2 = 8" \approx q_2 = 93 \text{ Btu/hr/ft}$$

$$d_3 = 12"$$

$$q_3 = \frac{93 \times 12}{8} = 139 \text{ Btu/hr/ft}$$

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$$q_c = \frac{(139)(43)}{3600} = 17.37 \text{ Btu/sec}$$

For 7-LH-12HV line

$$q_a = 17.37 + .496 = 17.87$$

Pipe length 3-LH-6LV = 130'

$$90^\circ \text{L's} = 5$$

$$T's = 2$$

$$\text{Reducer} = 1$$

$$45^\circ \text{L's} = 2$$

$$\frac{130'}{45'} \approx 3 \text{ welded joints}$$

$$\approx 19 \text{ welded joints per dwg}$$

22

EDSK0236

$$\text{Joints} = 22(1.85)(13) = 529.1$$

$$90^\circ \text{L's} = 5(1.85)(3.2) = 29.6$$

$$T's = 2(1.85)(4.9) = 18.1$$

$$\text{Reducer} = 1(1.85)(4.5) = 8.3$$

$$45^\circ \text{L's} = 2(1.85)(2) = 7.4$$

592.5 Btu/hr

.165 Btu/sec

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$$q = \frac{(72)(130)}{3600} = 2.6 \text{ Btu/sec}$$

For 3-LH-6LV line

$$q_b = 2.6 + .17 = 2.77$$

$$q_T = 2.77 + 17.87 = 20.64 \text{ Btu/sec}$$

"Heat Leak"

$$\Delta T_{HL} = \frac{q_T}{\dot{w} c_p} = \frac{20.64}{\dot{w} 2.293} = \frac{9.00}{\dot{w}}$$

$$\Delta T_{HL} = \frac{9.00}{\dot{w}} = .09^\circ R; \Delta T_f + \Delta T_{HL} = 0.479 - .09 \text{ page 3 this set}$$

$$\Delta T_f = .389^\circ R$$

$$\Delta T_f = 0.185 \frac{\Delta P}{\rho c_p}$$

$$= (0.185) \frac{K}{\pi^2} \frac{\dot{w}^2 144}{2g \rho^2 c_p}$$

Assume $(0.185) \frac{K}{\pi^2} \frac{144}{2g \rho^2 c_p}$
equal K

$$0.389 = K \dot{w}^2$$

$$K = \frac{0.389}{(9)^2} = .0000470$$

$$\Delta T_f = .0000470 \dot{w}^2$$

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OR Rise From
V3801 to V5001

$\dot{w}$ lb/sec	$\dot{w}^2$ lb ² /sec ²	$\Delta T_f - OR$.0000472 $\dot{w}^2$	$\Delta T_{HL} - OR$ $\frac{9.00}{\dot{w}}$	$\Delta T_f + \Delta T_{HL}$ OR
100	10000	0.470	0.09	0.560
90	8100	0.381	0.10	0.481
80	6400	0.301	0.11	0.411
70	4900	0.230	0.13	0.360
60	3600	0.169	0.15	0.319
50	2500	0.118	0.18	0.298
40	1600	0.075	0.23	0.305
30	900	0.042	0.30	0.342
20	400	0.019	0.45	0.469
10	100	0.005	0.90	0.905
0	0	—	—	—

Appendix E

AEROJET-GENERAL CORPORATION
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SUBJECT <i>NPSP at Pump - During Topping with temperature increase in fluid temperature</i>		REPORT NO. _____	PAGE <i>1</i> OF _____
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Assume:

1. Temperature rise from V3801 to Pump inlet of 1.95°R ; at pump see pgt #4 calculations;

"Temperature increase V3801 to V5001" is Temperature increase to pump inlet."

2. From curves NPSP vs $\dot{w}$ - No Topping
NP vs NPSP vs $\dot{w}$

$P_T = 29 \text{ psia}$

Tempr.	36°	37°	38°	39°	40°	41°
NPSP	6.05	3.35	0.25	-2.3	-5.95	-9.15

$P_T = 26 \text{ psia}$

36°	37°	38°	39°	40°	41°
8.1	5.4	2.3	-0.3	-3.90	-7.10

$P_T = 30 \text{ psia}$

36°	37°	38°	39°	40°	41°
12.2	9.5	6.4	3.8	0.2	-3.0

$P_T = 35 \text{ psia}$

36°	37°	38°	39°	40°	41°
17.2	14.2	11.4	8.8	5.2	2.0

$P_T = 40 \text{ psia}$

36°	37°	38°	39°	40°	41°
22.2	19.5	16.4	13.8	10.2	7

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Tank Pressures ↓ P _T	T _p =37.75'	T _p =38.75'	T _p =39.75'	T _p =40.75'	→ Conditions at pump
	NPSP	NPSP	NPSP	NPSP	
24	.3	-2.6	-5.7	-9.0	
26	2.4	-0.4	-3.5	-6.8	
30	6.7	3.7	0.6	-2.7	
35	11.8	8.75	5.55	2.25	
40	16.8	13.70	10.50	7.1	

APPENDIX F

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WATER HAMMER IN 75 K. M.I.D. S
LH₂ TAPPING LINE

DATE

30 JULY 68

WORK ORDER

1186

BY

W. R. L.

CHK BY

DATE

A. CALCULATE WAVE RESONANCE TIME

LENGTH OF LINE = 6×10^3 IN - REF. EDSK - 0326

C* LH₂ 0.8876 60714 = 4.3×10^4 IN/SEC

$$t = \frac{2L}{C^*} = \frac{2 \times 6 \times 10^3 \text{ IN}}{4.3 \times 10^4 \text{ IN/SEC}} = .28 \text{ SEC}$$

B. CALCULATE MAXIMUM HEAD RISE

1. ASSUME - a. CONSTANT DIA. LINE

b. NO LINE BENDS

c. VALVE CLOSING TIME $\leq .28$ SEC.

$$2. h_{\text{underrun}} = \frac{V_{\text{LIN}}}{\sqrt{e_{\text{LIN}} g_c \left(\frac{1}{K_m} + \frac{D}{E_e} \right)}}$$

Ref. Mach of
INCOMPRESSIBLE FLUIDS
P. 6-57

$$\text{but } \frac{D}{E_e} \ll \frac{1}{K_m} = \frac{5.76}{2.8 \times 10^6 \times 43} \ll \frac{1}{1.2 \times 10^4}$$

Modulus of Elasticity Bulk Mod

$$= .8 \times 10^{-6} \ll .82 \times 10^{-4}$$

$$3. h_{\text{max}} = \frac{V_{\text{LIN}} \times C^*}{g}$$

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$$4. \quad v = \frac{W}{\rho A} = \frac{W \text{ S.U.}}{A} = \frac{1.2 \times 10^2 \text{ sec} \cdot 3.95 \times 10^2 \text{ IN}^3/\text{ft}}{26.1 \text{ IN}^2}$$

6" schedule 80 pipe

$$= 1.5 \times 10^3 \text{ IN/sec.}$$

$$5. \quad h = \frac{1.5 \times 10^3 \text{ IN/sec} \cdot 4.3 \times 10^4 \text{ IN/sec} \cdot (6")}{3.84 \times 10^2 \text{ IN/sec} \cdot 80} = 1.68 \times 10^5 \text{ IN}$$

$$6. \quad \text{PRESSURE} = \frac{h}{\text{S.U.}} = \frac{1.68 \times 10^5 \text{ IN.}}{3.95 \times 10^2 \text{ IN}^3/\text{ft}} = \underline{\underline{42.5 \text{ psi}}}$$

C. INACCURACIES OF ASSUMPTIONS:

1. MUCH LINE IS 12" RATHER THAN 6" PROVIDING GREATER MASS TO BE STOPPED, BUT LESS KINETIC ENERGY.
2. LINE HAS MANY BENDS (14 @ 90°, 3 @ 45°) THE REFLECTIONS AND ECHOS OF THE PRESSURE WAVE COULD BE CANCELLING OR REINFORCING IN DIFFERENT LOCATIONS.
3. - THIS SIMPLISTIC METHOD CAN NOT BE EXTENDED BEYOND .28 SEC closing time, HOWEVER THE LONGER CLOSING TIMES WOULD REPRESENT LOWER PRESSURES.
4. - DUE TO C1 & C2 THE PEAK PRESSURE RISE (425 PSIA) MAY NOT NECESSARILY BE CONSERVATIVE. BUT SHOULD BE THE PROPER ORDER OF MAGNITUDE.
5. IT MUST BE NOTED THAT LARGE PRESSURE DIFFERENTIALS COULD EXIST IN WELD AREAS.