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COOLDOWN AND THRUST
NULLING REQUIREMENTS (U)

TRADE STUDY NO. 703/704

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**COOLDOWN AND THRUST
NULLING REQUIREMENTS (U)**

TRADE STUDY NO. 703/704

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ABSTRACT

A trade study performance evaluation of five cooldown and two thrust nulling systems was made on the NERVA NSS hot bleed cycle during the time period of March to November, 1969. Three of the five cooldown systems were tank pressure fed. They are (1) normal cooldown using on/off pulsing of the reactor, (2) independent pulsing of flow to the structural support and reflector paths, and (3) the use of ullage gas with either (1) or (2). Two auxiliary pumped systems were analyzed, one having parallel flow to the structural support-reflector and the other having series flow. The two thrust nulling systems were zero thrust radially directed coolant and thrust balanced axially directed coolant.

A detailed description of the performance calculations on the maxi TNT model, and explanation of the comparison matrix table entries are given. Seven appendixes give details on backup data.

Each cooldown and thrust nulling system combination was compared on each of ten weighted parameters in the matrix. The pressurized systems were shown to lose their performance advantage at power levels below 15-20 MW. As a result, the independent pulsing of flow to the structural support and reflector paths known as Pulsed-on-Separate-Limits, or POSL, was selected in combination with radial thrust nulling or system α .

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1.0 SCOPE

Trade Study 703 is to define NSS hot bleed cycle requirements and evaluate concepts which maximize and/or control the effluent gas temperature during cooldown. This study also defines the criteria for determining the end of cooldown when thermal radiation cooling is sufficient.

Trade Study 704 is to evaluate the capability of various NSS thrust nulling concepts and their influence on the design of the NSS.

These two trade studies are combined into a single report because of the overlapping of analysis and because any system considered for thrust nulling must also be acceptable for cooldown, and vice versa.

Both Trade Study 703 and 704 are input to the ANSC Trade Study 002, Post Shutdown Operations, ⁽¹⁾

This report documents for the record the analysis completed on the hot bleed cycle in the March to November, 1969, period. The majority of this work was presented orally at Trade Study Review Meetings at SNSO-C on June 5, June 20, and August 26, and September 9, 1969. Redirection of the programs to the full flow cycle required additional analysis ^(25,26) on cooldown and thrust nulling.

2.0 IDENTIFICATION OF REQUIREMENTS FOR TRADE-OFF

The proposed post-shutdown schemes must satisfy the NPRD⁽²⁾ and the Requirements Allocation Sheets (RAS), as well as any other constraints imposed by the engine system. These requirements and constraints are:

2.1 Functional Requirements

2.1.1 NERVA Program Requirements Document (NPRD)

2.1.1.1 Part III. B.1. d, Startup and Shutdown

- (i) Impulse lost by operation and less than full I_{sp} is to be minimized.
- (ii) After (tbd) seconds of coolant flow, the system shall provide for either using the available impulse or dissipating the coolant flow with zero thrust, at the option of the user.

2.1.1.2 Part III. B.1. e, Controllability

- (i) Total impulse shall be controlled to within $\pm 20,000$ lb /sec
- (ii) Shutdown time shall be controlled to within ± 15 seconds.
- (iii) Propellant consumption, including cooldown propellant, shall be (tbd).

2.1.1.3 Part III. B.1. j, Reliability

The overall reliability of the NERVA engine shall be 0.995 over 10 operating cycles totaling 50 minutes of operation and rated conditions.

2.1.1.4 Part III.B.1.p, Safety

The engine shall be capable of the following emergency mission:

- (i) Thrust $\geq$ (tbd) lb
- (ii) Specific impulse $\geq$ (tbd) second
- (iii) Total impulse $\geq$ (tbd) lb /sec
- (iv) Probability of success of emergency mode operation shall be 0.990

2.1.1.5 Part III.B.8.a, Man Rating

2.1.2 R A S (Fourth Level, Mission B)

2.1.2.1 Part 1.1.2.3.2 c. Nuclear Subsystem

- (i) NSS structural temperature limits are to be observed.
- (ii) Coolant flow is to be minimized, with coolant exhausted at highest permissible temperature.

2.1.2.2 Part 1.1.8.1.2 c. Nuclear Subsystem

Coolant flow is to be programmed to maintain NSS structural integrity.

2.1.3 Additional System Requirements

- (a) Fuel element corrosion negligible
- (b) Feedback reactivity less than \$ 1.25
- (c) Laminar flow instability limits observed

2.2 Technical Requirements

While a number of the above requirements may be translated directly into quantitative criteria, such as minimum cooldown propellant or thrust nulling initiation at earliest time after shutdown, some of them warrant further discussion.

2.2.1 Impulse Controllability

The requirement that the total impulse shall be controllable within $\pm 20,000$ lb - sec is not directly expressible in terms of quantities that can be used as a basis of comparison of one candidate cooldown/thrust nulling (CD/TN) scheme with another. Before such criteria can be established, it is necessary to define the procedure of controlling the overall impulse within prescribed limits for any post-shutdown operating procedure; it is only after such a definition that individual, detailed schemes can be compared with each other on a quantitative basis.

2.2.2 Reliability

The reliability criterion used in this trade study is a qualification number assigned to each candidate CD/TN scheme, as discussed in Appendix 6.

2.2.3 Safety and Man Rating

The safety and man rating requirements were interpreted as the absence of single failure modes that would result in emergency action. Quantitative emergency mission requirements were not specified in the NPRD. Therefore, no quantitative safety criteria were used.

2.2.4 Coolant Exhaust Temperatures

The RAS specification that the coolant is to be exhausted at the highest temperature compatible with NSS structural integrity was combined with the NPRD specification that the overall coolant flow shall be minimized for comparisons in this study. Several propellant utilization studies were made for the various candidate CD/TN schemes. These are identified explicitly in the comparison matrixes.

2.2.5 Material Temperature Limits

While the NSS material temperature limits are the same for all of the candidate schemes, they enter into the operating procedure associated with each scheme and hence have a direct bearing on the propellant utilization. Two sets of temperature limits were utilized. One set referred to a reactor capable of restart to full power, whereas the other set was for a reactor that will remain intact but will not be capable of restarting.

2.2.6 Fuel Element Corrosion

Incorporation of the fuel element corrosion rate as a criterion in the comparison matrix was not possible, due to lack of corrosion data at the low temperatures and pressures characteristic of cooldown. A discussion of fuel element corrosion as it pertains to cooldown and thrust nulling is presented in Appendix 5. No direct comparison criteria were developed from the corrosion requirements.

2.2.7 Feedback Reactivity

It is necessary that the reactor remains subcritical during the cooldown and thrust nulling periods. At present, the NERVA flight reactor has a predicted shutdown reactivity of \$1.50 composed of the minimum acceptable shutdown reactivity of \$1.25 and a margin of \$.25 for any mode of operation. Hence, there must be not more than + \$1.25 reactivity change relative to cold critical during cooldown and thrust nulling. Feedback reactivity thus is expressed in the comparison matrix as a maximum value of + \$1.25.

2.2.8 Laminar Flow Instability

Present understanding of LFI as it pertains to the NERVA NSS is discussed in Appendix 5. The LFI limitation effectively places a lower bound on the coolant flow rates and thus enters directly into the propellant utilization figures. Since the phenomenon is not completely understood, however, this bound on the flow rate is not precisely defined, and situations arise where it is questionable whether or not LFI is to be expected. Therefore, the comparison matrixes contain a qualitative entry indicating whether or not LFI is expected to influence a particular CD/TN scheme.

3.0 DESCRIPTION OF POSSIBLE DESIGN APPROACHES

The cooldown and thrust nulling trade studies reported here are for the hot bleed cycle engine, shown schematically in Figure 3-1. The cooldown run profile used in these studies is shown in Figure 3-2. Four basic cooldown schemes are considered in this trade study; several thrust nulling arrangements are applied to each scheme, in accordance with the NPRD. An overview of the competitive designs is provided by the outline below.

Unpressurized Systems

Normal cooldown (N C)

Radial thrust nulling (T N) scheme

Axial T N scheme β

Pulsing on separate limits (POSL)

Radial T N scheme

Axial T N scheme β

Modified axial T N scheme β'

Ullage gas cooldown

Pressurized Systems

Parallel flow system 7b with axial TN

Series flow system 12b with radial TN

The basic operating procedures for all of these cooldown/thrust nulling schemes are the same following shutdown initiation; continuous coolant flow is maintained until a decay power level is reached at which it becomes desirable to make the flow intermittent. Cooldown is terminated when the decay power level reaches 10 KW (Appendix 7).

Within this basic framework, a number of variations were considered, as indicated below. A simplified flow schematic of each is given. These show single valves whereas the actual engine would have redundant valves in all instances.

3.1 Unpressurized systems

3.1.1 Normal Cooldown (NC)

This system involves the on/off pulsing of the reactor through the PSOV and is so named because, at the start of this trade study, it was the "normal" method of cooldown. Moreover, it was the only method in which a cooldown had been carried out on past reactor tests. This study represents the first emphasis on high performance cooldown for a flight engine.

The flow schematic for this cooldown scheme differs from Figure 3-1 only in that the turbine circuit is shut off. Following pump tailoff, the TPCV is closed, and continuous coolant flow is maintained by tank pressure until pulsing is initiated. Coolant flow during the pulsed portion of cooldown is determined by material temperature limits and the desirability of having a high nozzle chamber temperature (T_c). A significant aspect of this scheme is that there is no control over the flow split between the nozzle/reflector and support stem circuits; the critical component governs the entire coolant flow.

As indicated above, the coolant flow passes through and is controlled by the on/off action of the PSOV. A variation on this is to install a separate coolant line and control valve (CDCV), so that the coolant does not pass through the main turbopump assembly (TPA). From the NSS viewpoint, this variation is of no consequence. It does permit the possibility of throttling the coolant flow by means of the CDCV, but this variation was not investigated as part of this study.

Figure 3-1. Hot Bleed Cycle Engine Schematic Operation at Rated Conditions

Figure 3-2. Typical Run Profile

According to the NPRD, the candidate cooldown schemes must be compatible with some form of thrust nulling. Two TN schemes are associated with normal cooldown.

3.1.1.1 Radial TN Scheme (System Alpha)

The flow schematic for normal cooldown with TN scheme α is shown in Figure 3-3. The essential modification is that the coolant flow is taken outside the pressure vessel (P V) to the thrust nulling valve (TNCV), where it is either diverted to a set of radial thrust nulling nozzles for zero thrust operation, or returned to the core and exhausted through the main nozzle to produce usable cooldown thrust.

The modification introduced by this thrust nulling concept affects operation at rated conditions in that additional flow impedance is introduced into the propellant circuit by the TNCV and the associated piping. An earlier version of the NC/ System α involved a single TNCV constructed as an integral part of the P V. This scheme did not degrade the rated performance, but it was rejected on the basis of safety (presents a single failure mode).

3.1.1.2 Axial TN System β

The flow schematic for normal cooldown with the axial TN System β , is shown in Figure 3-4. During thrust nulling, the stem flow is diverted by the TNCV to a set of forward facing nozzles. The reflector flow is throttled and passed through the core and main nozzle to counterbalance the backward thrust component.

Figure 3-3. Normal Cooldown with Radial Thrust Nulling Scheme

Figure 3-4. Normal Cooldown with Axial Thrust Nulling Scheme

From an operational viewpoint, System β permits some flexibility in controlling the cooldown impulse. A disadvantage is that an additional complexity is introduced.

3.1.2 Pulsing on Separate Limits (POSL)

A major drawback of the normal cooldown scheme is the lack of control over the flow split between the stem and reflector circuits. This feature of NC is removed by placing an on/off valve in the nozzle/reflector circuit inlet line, as shown in Figure 3-5. With this valve, the reflector flow can be pulsed on different temperature limits than the stem flow; hence the terminology POSL.

A variation of this scheme is to make the CDCV a throttling valve, so that a continuous stem flow can be utilized to maintain a higher exhaust temperature.

Three thrust nulling schemes were considered in conjunction with POSL cooldown. They are:

3.1.2.1 Radial TN Scheme α

The flow schematic for this design is shown in Figure 3-5. The flow configuration is identical to that of the α TN system used with normal cooldown.

3.1.2.2 Axial TN Scheme β

This thrust nulling scheme is also identical to its counterpart in normal cooldown; the flow schematic is shown in Figure 3-6. During thrust nulling, the TNCV diverts the stem flow to forward facing thrust nulling nozzles. The thrust produced by these nozzles is counterbalanced by the throttling reflector flow that passes through the core and the main nozzle.

Figure 3-5. Flow Schematic for POSL with Thrust Nulling System

Figure 3-6. Flow Schematic for POSL with TN System B or B'

3.1.2.3 Modified Axial TN System β'

The flow schematic for POSL cooldown with the β' TN System is the same as for β . The primary difference is that the on/off TNCV is replaced by one that can divert a part of the stem flow to the TN nozzles while the rest passes through the core and main nozzle, to provide in zero net thrust. When the reflector flow is on, system β' is the same as β .

3.1.3 Ullage Gas Cooldown

Cooldown schemes that utilize ullage gas are similar to the two LH_2 systems discussed above in that the flow schematics are essentially the same. The differences arise in coolant management. The use of ullage gas for coolant is more economical than dumping it overboard but the available pressure in the tank depends on previous usage.

3.2 Pressurized Systems

Cooling efficiency during the early, high power period of cooldown is improved by increasing the coolant pressure. Two schemes were considered that utilize an auxiliary turbopump to increase the coolant pressure.

3.2.1 Parallel Flow System 7b

The flow schematic for this cooldown system with its associated thrust nulling scheme is shown in Figure 3-7. During cooldown, the auxiliary turbine exhaust flow is routed to the core and main nozzle; during thrust nulling, a part of this flow is diverted to forward facing TN nozzles to counterbalance the thrust from the main nozzle.

Figure 3-7. Parallel Flow Cooldown and Thrust Nulling System 7b

3.2.2 Series Flow System 13b

The flow schematic for this cooldown system with its associated thrust nulling scheme is shown in Figure 3-8. During cooldown, the entire coolant flow passes through the core and main nozzle to produce thrust. When thrust nulling, the entire flow is diverted to a set of radial TN nozzles where it is dissipated with zero net thrust. The purpose of a series flow system is to obtain a higher core inlet temperature and thus remain in the stable flow region longer.

Figure 3-8. Series Flow Cooldown and Thrust Nulling System 13b

4.0 COMPARISON MATRIX OF DESIGN APPROACHES

		COMPARISON MATRIX OF DESIGN APPROACHES						SELECTION
FUNCTIONAL & TECHNICAL DESIGN REQUIREMENTS		NC/ α 1	POSL/ α 2	NC/ β 3	POSL/ β 4	7b/ β 5	13b/ α 6	
Weighting Factor								
10	NSS Reliability	10	10	8	8	8	9	(1, 2), 6, (3, 4, 5)
10	Safety	10	10	10	10	10	10	(1, 2, 3, 4, 5, 6)
9	Propellant Efficiency	4	8	5	7	8	9	6, (2, 5), 4, 3, 1
8	Impulse Controllability	5	6	4	4	4	6	(2, 6), 1, (3, 4, 5)
7	Early Thrust Nulling Capability	4	4	5	5	7	6	5, 6, (3, 4), (1, 2)
5	LFI	5	5	2	2	2	4	(1, 2), 3 (4, 5, 6)
5	Stress Level	5	5	5	5	3	3	(1, 2, 3, 4), (5, 6)
5	Reactivity Feedback	5	5	5	5	2	5	(1, 2, 3, 4, 6)
4	Control Complexity	4	3	2	2	1	2	1, (2, 6) (3, 4, 5)
3	Configuration Complexity	3	3	2	2	2	1	(1, 2), (3, 4, 5), 6
66	Totals	55	59	48	50	47	55	2, 6, 1 4, 3, 5
* Parentheses Indicate Equality of Ranking								

Figure 4-1. Comparison Matrix of Design Approaches

5.0 SELECTION OF A DESIGN APPROACH

The calculations that have been made to support the entries in the comparison matrixes of the preceding section are summarized in Section 5.1 in accordance with the original studies. Section 5.2 contains the translation of the results of these calculations into the comparison matrix entries.

5.1 Summary of Supporting Performance Calculations

5.1.1 Normal Cooldown

Two sets of calculations were carried out with the normal cooldown configuration, restartable and terminal. Results of the calculations consist of TNT-II computer code input of various NSS parameters as a function of time. The three core channel model (maxi-TNT) was used in all calculations.

5.1.1.1 Restartable Cooldown

These calculations are documented in detail in Reference (3). The primary objective of this study was to examine the system behavior during the early part of cooldown. These calculations are based on a 20 minute full power burn followed by a normal shutdown (See Appendix 1); the material temperature limits used are in accordance with Reference (4) (See Appendix 3).

In the course of these calculations, it was found that if the cooldown is carried out at a fixed chamber temperature of 1400°R , the coolant pulsing frequency becomes excessive. Consequently, the study was addressed in part to the problem of determining a normal cooldown program that would result in an acceptable coolant pulse profile. Tables 5-1 through 5-3 show several NSS temperatures for three

time intervals during the early portion of cooldown. The results are for a constant chamber temperature of 1400°R . These calculations were made using a two second time increment. As can be seen, the pulse duration and the interval between pulses required to maintain the desired chamber temperature range, and satisfy the material temperature limits, become less than the two second time increment of the calculation. To improve the coolant pulse profile, three other chamber temperature profiles were examined - (1) a constant T_c of 1300°R , (2) a constant T_c of 1400°R with the diluent line bleed flow shut off, and (3) a variable T_c program that first terminates the coolant flow at $T_c = 1450^{\circ}\text{R}$, and ends each subsequent pulse when T_c is 75°R less than at the beginning of the pulse until a chamber temperature of 1200°R is reached; thereafter, T_c is held constant until the end of cooldown. (End of cooldown is defined to occur at a decay power level of 10 KW - see Appendix 7). This last cooldown program showed a marked improvement over the coolant pulse profiles of the other programs.

The propellant requirements associated with the above four normal cooldown programs are listed in Table 5-4. In this table, the propellant used during the first 750 seconds following initiation of shutdown is obtained by means of a heat balance based on the instantaneous decay power and average coolant temperature rise computed with the TNT-II Code. The remaining coolant requirement was estimated from decay heat remaining to be removed and an assumed average T_c of 1200°R .

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TABLE 5-1
NORMAL COOLDOWN - RESTARTABLE
CLUSTER RADIAL TEMPERATURE DISTRIBUTION AT THE AVERAGE CORE CHANNEL OF THE TNT-II
MODEL WITH CONSTANT 1400°R T_L LIMIT AT CORE STATION 42" FROM 1598
TO° 1610 SEC

Propellant Flow *	Time, Sec (1)	Fuel Temp.	Unfueled Central Temp.	Pyrosleeve Temp. 3 Nodes			Liner Temp.	Stem Temp.	Core Exit Temp. (2)	Chamber Temp. (3)
On	1598	1517	1518	1480	1392	1291	1229	993	1480	1399
Off	1600	1531	1529	1487	1407	1334	1309	1006	---	---
Off	1602	1545	1542	1503	1432	1375	1360	1018	---	---
On	1604	1537	1537	1503	1433	1357	1307	1028	1484	1380
Off	1606	1551	1549	1514	1449	1390	1371	1040	---	---
On	1608	1544	1544	1512	1445	1369	1317	1043	1486	1391
Off	1610	1558	1555	1522	1458	1400	1381	1055	---	---

NOTE:

- (1) Reactor power at 1600 sec = 11.5 MW., Time of shutdown initiation = 1200 sec., Temperature in °R
- (2) Core fuel channels and LSS exit gas mixed mean temperature
- (3) Core exit and cold bleed mixed mean temperature

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TABLE 5-2
NORMAL COOLDOWN - RESTARTABLE
CLUSTER RADIAL TEMPERATURE DISTRIBUTION AT THE AVERAGE CORE CHANNEL OF THE TNT-II MODEL
WITH CONSTANT 1400°R T_c LIMIT AT CORE STATION 42"
FROM 2038 TO 2048 SEC

Propellent Flow	Time, Sec.	Fuel Temp.	Unfueled Central Temp.	Pyrosleeve Temp. 3 Nodes			Liner Temp.	Stem Temp.	Core Exit ⁽¹⁾ Temp.	Chamber ⁽²⁾ Temp.
On	2038	1466	1468	1448	1402	1343	1296	1038	1389	1293
Off	2040	1475	1473	1451	1407	1364	1347	1044	---	---
Off	2042	1482	1481	1459	1420	1386	1377	1051	---	---
On	2044	1471	1472	1453	1409	1354	1310	1042	1389	1288
Off	2046	1479	1477	1456	1415	1374	1358	1050	---	---
On	2048	1466	1467	1448	1402	1343	1297	1038	1388	1292

NOTES:

- (1) Core fuel channels and LSS exit gas mixed mean temp.
- (2) Core exit and cold bleed mixed mean temp.
- (3) Reactor power at 2040 sec = 6.5 MW., Time of shutdown initiation = 1200 sec., Temperatures in °R

TABLE 5-3

NORMAL COOLDOWN - RESTARTABLE

CLUSTER RADIAL TEMPERATURE DISTRIBUTION AT THE AVERAGE CORE CHANNEL OF THE TNT-II MODEL
WITH CONSTANT 1400°R T_c LIMIT AT CORE STATION 42"
FROM 3580 TO 3596 SEC

Propellant Flow	Time, Sec.	Fuel Temp.	Unfueled Central Temp.	Pyrosleeve Temp. 3 Nodes			Liner Temp.	Stem Temp.	Core Exit ⁽¹⁾ Temp.	Chamber ⁽²⁾ Temp.
Off	3580	1398	1398	1388	1372	1359	1355	1007	---	---
On	3582	1387	1388	1377	1347	1297	1245	993	1242	1152
Off	3584	1389	1389	1374	1343	1307	1291	995	---	---
Off	3586	1391	1390	1377	1349	1323	1314	998	---	---
Off	3588	1393	1393	1380	1356	1337	1332	1000	---	---
Off	3590	1395	1395	1384	1364	1348	1344	1003	---	---
Off	3592	1397	1397	1388	1371	1358	1354	1006	---	---
On	3594	1387	1388	1377	1346	1297	1244	991	1241	1150
Off	3596	1389	1388	1394	1342	1307	1291	994	---	---

NOTES:

(1) Core fuel channels and LSS exit gas mixed mean temp.

(2) Core exit and cold bleed mixed mean temp.

(3) Reactor power at 3590 sec. = 2.5 MW., Time of shutdown initiation = 1200 sec., Temperatures in °R

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TABLE 5-4
PROPELLANT USAGE DURING SHUTDOWN AND COOLDOWN FOR FOUR
DIFFERENT NORMAL COOLDOWN SCHEMES

Time, Sec (1)	Constant 1400° R T _c (lbs)	Constant 1300° R T _c (lbs)	Constant 1400° R T _c Without Bleed Flow	Variable T _c
1200 to 1950 (2)	4160	4216	4115	4248
1950 to 184000 (3)	4970	5354	4970	5748
TOTAL	9130	9570	9085	9996

NOTES:

- (1) Time of Shutdown Initiation = 1200 sec
- (2) Calculated by TNT-II
- (3) Estimated. The power level at end of cooldown is 10 KW

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Another item considered in these calculations is the amount by which the chamber temperature is reduced by mixing with the cold bleed flow. For the normal cooldown flow configuration, this chamber temperature decrement is about 90°R . It is a direct specific impulse penalty, and it also decreases the coolant pulse duration.

5.1.1.2 Terminal Cooldown

The study reported in Reference (5) was carried out to investigate the NSS behavior during normal cooldown with the higher material temperature limits associated with a terminal cooldown, as discussed in Appendix 3. To obtain the maximum decay precursor concentration and thus the propellant integral, the full power run given for these calculations was 50 minutes followed by a reference shut down.

Two cooldown T_c programs were investigated.

(1) a constant T_c of 1900°R and (2) a variable T_c program aimed at improving the coolant pulse profile. Table 5-5 shows several NSS component temperatures during one 20 second time interval of the cooldown corresponding to the $T_c = 1900^{\circ}\text{R}$ program. This chamber temperature program results in an excessive coolant pulse frequency even at 20 minutes after shutdown. The variable T_c program was devised to avoid this difficulty.

TABLE 5-5

CLUSTER RADIAL TEMPERATURE DISTRIBUTION AT THE AVERAGE CORE CHANNEL OF THE TNT-II
MODEL CORE STATION 46", FROM 6164 SEC TO 6184 SEC (AT 3.6 MW),
WITH CONSTANT 1900°R T_c LIMIT

System Flow	Time, Sec	Fuel Temp.	Unfueled Central Temp.	Pyrosleeve 3 Nodes			Liner Temp.	Max. Stem Temp.	Avg. Fuel Channel Exit Temp.	Core Exit Temp. ⁽¹⁾	Chamber Temp. ⁽²⁾
On	6164	2237	2237	2226	2195	2144	2091	1721	2266	1957	1848
Off	6166	2240	2240	2225	2192	2127	2138	1724	---	---	---
Off	6168	2243	2242	2227	2198	2172	2163	1726	---	---	---
Off	6170	2246	2244	2231	2207	2186	2181	1729	---	---	---
Off	6172	2248	2247	2235	2215	2199	2200	1731	---	---	---
On	6174	2237	2238	2225	2191	2139	2085	1719	2266	1956	1848
Off	6176	2240	2239	2223	2189	2150	2133	1722	---	---	---
Off	6178	2212	2241	2225	2196	2168	2158	1724	---	---	---
Off	6180	2245	2243	2229	2204	2183	2177	1727	---	---	---
Off	6182	2247	2246	2234	2213	2196	2200	1729	---	---	---
On	6184	2236	2237	2224	2189	2114	2082	1717	2265	1956	1848

NOTES:

(1) Core fuel channels and LSS exit gas mixed mean temperature

(2) Core exit and cold bleed mixed mean temperature

(3) Reactor power level at 6164 sec = 3.6 MW. Time of Shutdown initiation = 3000 sec, Temperatures in °R

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TABLE 5-6

PROPELLENT USAGE OF TWO TERMINAL SHUTDOWN SCHEMES

	Constant 1900°R Tc Limit, lbs	1900°R Initially, 14 Seconds Pulses, Then Constant 1800°R Tc Limit, lbs
3000 - 4200 sec ⁽¹⁾	4330	4330
4200 - 5600 sec	1303	1267
5600 - End of Cooldown ⁽²⁾ (Estimated)	5517	5844
Total, lbs	11150	11441

NOTES:

(1) Time of terminal shutdown is 3000 sec after 50 minutes of full power operation

(2) End of cooldown is considered to be 37.5 KW for terminal cooldown

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TABLE 5-7

COMPARISON OF RADIAL TEMPERATURE DISTRIBUTION BETWEEN TERMINAL AND NORMAL SHUTDOWNS

	Core Channel Exit Temperatures, °R					Core Exit	Chamber Temp. Tc	Max. Liner Temp.	Max. Stem Temp.	Liner Mat'l Temp. Limit	$\Delta T = (\text{Avg. Ch. Exit Temp.} - T_c)$
	Central Region	Average Region	Peripheral Region	LSS	Cold Bleed						
Terminal (1900°R Tc Limit) at 3.5 MW (6196 sec)	2176	2266	900	685	118	1955	1846	2091	1721	2220	420
Normal (1200°R Tc Limit) at 3.0 MW (1250 sec)	1293	1378	812	605	46	1242	1155	1224	947	1350	223

- (U) The propellant expended during these two terminal cooldown T_c programs is listed in Table 5-6. The T_c decrement due to the cold bleed diluent is approximately the same as in the restartable case.
- (U) One objective of the terminal cooldown study was to determine the increase in chamber temperature that would be possible if the stem liner temperature limit were increased. Table 5-7 shows the pertinent NSS temperatures at a similar power level for the normal restartable and cooldowns. Assuming the hottest channel temperature to be approximately equal to the maximum stem liner temperature, the calculations presented in Table 5-7 show that the T_c and coolant shut-off must be at least 420°R below the liner limit in the terminal case and 223°R below in the restartable case. From these tables it is concluded that increasing the liner limit 870°R (from 1350 to 2220°R) increases the average T_c by 670°R (from 1130 to 1800°R), or 77% of the increase in liner temperature limit.
- (CRD) 5.1.2 Pulsing on Separate Limits (POSL)
Figures 5-1 through 5-5 show some of the results of a group of calculations carried out to investigate the characteristics of the POSL cooldown scheme. The first of these shows temperatures and flow rates in the basic POSL system, with reflector coolant pulses initiated by the liner temperature limit of 1350°R and terminated by a lower T_c limit of 1450°R . This calculation revealed an unexpected characteristic of the POSL cooldown. When the reflector flow is initiated, the support stem liner temperature continues to increase because

(CRD) the increased reflector exit plenum pressures effectively decrease the pressure difference available for maintaining the stem flow. Figures 5-2(a) through 5-2(c) show this same effect when the reflector coolant flow is initiated at maximum liner temperature of 1200°R. Figures 5-3 and 5-4 show samples of cooldown temperature profiles and coolant flow rates that result from increasing the liner temperature limit to 1800°R. The liner temperature again continues to rise following initiation of coolant flow through the reflector. Note that the higher T_c reflector flow shutoff shown in the calculations of Figure 5-4 results in shorter coolant pulses.

(U) 5.1.3 Ullage Gas Cooldown

To scope the effect of gaseous feed conditions on cooldown performance, calculations were made for a POSL system 20 minutes after shutdown. The temperature and flow rate profiles are not significantly affected by the use of gas as shown in Figure 5-5. This conclusion would also be representative of an NC system.

(U) The above calculations do not address themselves to the time dependent tank pressure, but were done for an assumed nominal value of 30 psia. There is some unique tank-pressure/decay-power relationship below which the reactor cannot be cooled. To obtain such a relationship would require numerous parametric studies which were considered beyond the scope of this study. Before any general conclusion can be made regarding the use of ullage gas for all cooldowns, a tank pressure blowdown schedule is required and each potential mission should be analyzed individually. However, it is concluded that maximum use should be made of the high pressure ullage gas in combination with the liquid cooldown system.

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Figure 5-1(a). Chamber Temperature During Early POSL Cooldown

Figure 5-1(b). Structural Support Liner Temperatures During Early POSL Cooldown

Figure 5-1(c). Flow Rates During Early POSL Cooldown

Figure 5-2(a). Chamber Temperature During Early POSL Cooldown

MAXIMUM 1 AVERAGE 2 MINIMUM 3

007T-R18-CD-BLP-060869 POSL COOLDOWN
STRUCTURAL SUPPORT LINER TEMPERATURES

INDEX NO.

001-R1

Figure 5-2(b). Structural Support Liner Temperature During Early POSL Cooldown

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Figure 5-2(c). Flow Rates During Early POSL Cooldown

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Figure 5-3(a). Chamber Temperature During Early POSL 1800°R Liner Limit

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MAXIMUM 1 AVERAGE 2 MINIMUM 3	INDEX NO.
POSL 1800°R LIMIT	391-#1
STRUCTURAL SUPPORT LINER TEMPERATURES	

Figure 5-3(b). Structural Support Liner Temperatures During Early POSL 1800°R Liner Limit

Figure 5-3(c). Flow Rates During Early POSL 1800°R Liner Limit

Figure 5-4(a). Chamber Temperature During Early POSL 1800°R Liner Limit

MAXIMUM 1 AVERAGE 2 MINIMUM 3

POSL TL LIMIT 1800

STRUCTURAL SUPPORT LINER TEMPERATURES

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Figure 5-4(b). Structural Support Liner Temperatures During Early POSL 1800°R Liner Limit

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Figure 5-4(c). Flow Rates During Early POSL 1800°R Liner Limit

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Figure 5-5(a). Chamber Temperature During Early Ullage Gas Cooldown

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Figure 5-5(b). Structural Support Liner Temperatures During Early Ullage Gas Cooldown

Figure 5-5(c). Flow Rates During Early Ullage Gas Cooldown

5.1.4 Parallel Flow System 7b Cooldown

The TNT-II code was used to examine the characteristics of the 7b cooldown scheme. A number of calculations were carried out to determine the flow rates, material and propellant temperatures that result from application of given LH_2 coolant supply pressures. At 94 MW power level and 300 psia LH_2 coolant supply, the steady state chamber temperature is about 3550°R , corresponding to a flow rate of ~ 6.9 lb/sec. Figure 5-6 shows the thermal power versus coolant supply pressure for the 7b system. It is noted from these results that tank pressure can furnish sufficient coolant in the 15 to 20 MW power range; this corresponds to five to ten minutes after shutdown. The major conclusion from this data is that an auxiliary pressurization system can be used to advantage in cooldown only during the initial minutes after shutdown.

Figure 5-7 shows some of the results of a computation carried out subject to a demand chamber temperature of 2500°R until the required supply pressure decreases to 100 psia, then the pressure is held at 100 psia. There the required supply pressure decreases to 100 psia in approximately 60 seconds, indicating the need for a pressurization system is short lived. The flow split between the reflector and support system is approximately 50% for each leg.

Figure 5-9

Figure 5-6. Heat Removable Capability Versus Coolant Supply Pressure for 7b Scheme

CHAMBER 1 FUEL-CENTRAL 2, AVERAGE 3, PERIPHERY 4 LATERAL SUPPORT 5

01A-R10-CD-BLP-000169 SCHEME 7B

CHAMBER TEMPERATURE

INDEX NO.

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Figure 5-7(a). Chamber Temperature During Early 7b Cooldown

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Figure 5-7(b). Structural Support Fluid Temperatures During Early 7b Cooldown

Figure 5-7(c). Structural Support Tube Temperatures During Early 7b Cooldown

MAXIMUM 1 AVERAGE 2 MINIMUM 3

714-R18-CD-BLP-747169 SCHEME 7B

STRUCTURAL SUPPORT LINER TEMPERATURES

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Figure 5-7(d). Structural Support Liner Temperatures During Early 7b Cooldown

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Figure 5-7(e). Flow Rates During Early 7b Cooldown

TOTAL THERMAL 1 FISSION 2 DECAY 3

01R-R18-CD-BLP-000169 SCHEME 7B

POWER

INDEX NO.

001-#1

Figure 5-7(f). Power During Early 7b Cooldown

Figure 5-7(g). Plenum Pressures During Early 7b Cooldown

PUMP 1 REFLECTOR INLET 2 DOME 3 CORE INLET 4

71R-R1B-CD-BLP-700169 SCHEME 7B

PLENUM TEMPERATURES

INDEX NO.

001-R1

Figure 5-7(h). Plenum Temperatures During Early 7b Cooldown

Figure 5-7(i). Pressure Drop During Early 7b Cooldown

Figure 5-7(j). Reactivity During Early 7b Cooldown

Figure 5-7(k). Heat Transfer Coefficients During Early 7b Cooldown

5.1.5 Series Flow System 13b Cooldown

Figure 5-8 shows the results of calculations carried out for the 13b cooldown scheme that utilizes an auxiliary pressurization system to maintain the coolant supply pressure. Here again the required coolant supply pressure decreases quite rapidly (200 to 120 psia in two minutes) so that in five to ten minutes after shutdown the tank pressure would be sufficient to supply the required coolant flow rate.

5.1.6 Thrust Nulling Performance Calculations

The objectives of this study were to determine the earliest time that thrust nulling could be initiated utilizing each of the candidate CD/TN schemes, and to obtain an estimate of the coolant weight penalty associated with each scheme. The flow configurations used in the calculations are shown in Figures 3-3 for system α , 3-4 for β , 3-7 for 7b, and 3-8 for 13b. The TNT-II digital code was used to compute values for the NSS parameters over a selected time interval during cooldown. The coolant supply pressure, thrust nulling nozzle area, and the decay heat profile corresponding to a 20 minute full power burn were provided as input for the calculations. An iteration procedure was introduced for the axial thrust balancing schemes β and 7b to determine the flow rate in each circuit required to produce zero net thrust. The TN initiation was defined as that time at which the heat storage rate of the system is zero, i.e., when the rate of heat removal by the coolant in the thrust nulling configuration at the chosen supply pressure is equal to the nominal decay power.

Figure 5-8(a). Chamber Temperature During Early 13b Cooldown

Figure 5-8(b). Structural Support Liner Temperatures During Early 13b Cooldown

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Figure 5-8(c). Reactivity During Early 13b Cooldown

Figure 5-8(d). Flow Rates During Early 13b Cooldown

Figure 5-8(e). Plenum Pressures During Early 13b Cooldown

- (U) Figure 5-9 shows the variation of the TN initiation time with coolant supply pressure for the auxiliary pump pressure systems. The tank pressure system β is also included in this figure for comparison. The TN initiation time for system β also depends on the thrust nulling nozzle area. This effect is illustrated in Figure 5-10. Figure 5-11 shows typical SSA temperature distributions obtained from these calculations. As shown in these figures, the stem liner temperatures are considerably below their limiting value.
- (C) An earlier study undertaken to examine the thrust nulling capability of the NSS is documented in Reference (6). Two curves on the "quench" method of thrust nulling are repeated here for completeness. Figure 5-12 shows the zero thrust windows that are available between pulses as a function of the average core material temperature drop during a pulse. For example, a 30 minute zero thrust period could be provided at 5-1/2 hours after a 50 minute full power run by allowing the core temperature to decrease 800°R during the preceding coolant pulse.
- (U) There are several points that arise in connection with thrust nulling related to temperature limits that are not treated in the studies mentioned above:
1. Shield and S S Tube Heating
- (U) Figure 5-11 shows the adiabatic temperature rise of the shield because of internal heat generation. This figure shows that shield heating due to internal heat generation is not significant. A similar calculation was made to determine the adiabatic temperature rise rate of the structural support tubes; Figure 5-14 shows that this component may be temperature limiting during periods of no flow within three hours after shutdown.

Figure 5-9. Effect of Supply Pressure on Thrust Null Time

Figure 5-10. Effect of Forward Nozzle Throat Area on Thrust Nulling Time for System Beta
NOTE: Thrust Null Time Includes 1200 Sec Full Furn Time

Figure 5-11(a). Structural Support Temperature Distribution-Thrust Nulling for System Beta

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Figure 5-11(b). Structural Support Temperature Distribution-Thrust Nulling For System Beta - Prime

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Figure 5-11(c). Structural Support Temperature Distribution-Thrust Nulling for System 7b, 300 PSIA, at 1456 Seconds

Figure 5-11(d). Structural Support Temperature Distribution - Thrust Nulling for System 13b, 300 Psia, at 2435 Seconds

Figure 5-11(e). Structural Support Temperature Distribution - Thrust Nulling for System 13b, 200 Psia, at 2500 Seconds

Figure 5-12(a). Time Between Pulses - Operating Time 5 Minutes at Full Power - R-1 Reference Design

612855-4

Figure 5-12(b). Time Between Pulses - Operating Time
50 Minutes at Full Power - R-1 Reference Design

Figure 5-13. Central Shield Average Temperature Rise

Figure 5-14. Midplane Average Support Stem Temperature Rise

2. Support Plate Heating

Figure 5-15 shows the time required for the support plate temperature to rise from an initial condition of 250°R to its limiting value of 710°R due to internal heat generation and radiation from the cold end of the core. The heat fluxes are assumed constant at their values at the time of thrust nulling initiation; these calculations thus represent a limiting case.

3. Nozzle Heating

Another calculation was carried out to estimate the effect of core radiation on an uncooled nozzle assembly temperature. Two sets of results were generated: one corresponding to an average material based on the total nozzle mass, and another based on the assumption that the nozzle coolant tube material retains all of the radiated heat input (no conduction to the nozzle backing). The results are presented in Figure 5-16 showing the time required for the nozzle to reach a limiting temperature of 2240°R as a function of the core hot end temperatures. Consideration of the heat flux and the thermal conductivity of the nozzle cooling tube material (hastelloy-x) suggests that the actual situation may be quite close to the more severe case in Figure 5-16.

4. Support Stem Insulation Effect

In the calculations discussed above, the structural stem liner temperature was frequently found to be the temperature limiting component in thrust nulling resulting in a low exit temperature. A study was therefore undertaken to examine possible stem design alterations.

Figure 5-15. Core Cold End Radiating Surface Temperature Versus Time for Support Plate to go from $T = 250^{\circ}\text{R}$ to $T = 710^{\circ}\text{R}$

Figure 5-16. Time for Average Nozzle Temperature to Reach 2240°R from Thermal Radiation

This study is documented Reference (7).

The objective of this study was to construct a simple analytical model of the structural support assembly that would permit rapid estimation of the maximum liner temperatures and the axial location of this maximum for various SSA designs. The model is used to compare four design variations. Figure 5-17 shows the variations of the individual tube flow rate required to maintain a fixed maximum liner temperature as a function of the decay power for two stem geometries at two temperature limits. This figure indicates that, for a given inlet temperature, the required coolant flow rate may be decreased by either increasing the liner temperature limit or decreasing the effective conductivity of the stem structure by variations of geometry. Figure 5-18 shows the exit temperature increase possible by decreasing the coolant flow rate. The position of the maximum liner temperature for four SSA designs is shown in Figure 5-19. Comparison of the performance gains possible with each of these design variations is listed in Table 5-8. This table shows that increasing the liner temperature limit is comparatively less effective than changing the effective thermal conductivity of the stem structure in increasing the coolant exit temperature. However, this decrease in conductance across the stem is done by increasing the volume of insulation and thus decreasing the design point reactivity swing.

Figure 5-17. Flow Rate Per Tube as a Function of Total Core Decay Power for a Constant Maximum Liner Temperature

Figure 5-18. Tube Exit Temperature Versus Flow Rate Per Tube

Figure 5-19. Position of Maximum Liner Temperature as a Function of Flow

TABLE 5-8
MATRIX OF STEM EXHAUST TEMPERATURES

	P_D T_L	1044				348				174			
		1850	1500	1350	1000	1850	1500	1350	1000	1850	1500	1350	1000
Nominal Design	T_{Ex}	1200	1000	890	660	1070	900	810	625	940	820	730	580
	Φ	0.62	0.73	Ref.	0.66	0.52	0.60	Ref.	0.53	0.42	0.60	Ref.	0.43
Nominal Design plus 0.053" Pyrographite	T_{Ex}^*	1300	1120	1030	750	1130	1010	895	680	1030	890	810	625
	Φ	0.54	0.60	Ref.	0.80	0.47	0.77	Ref.	0.61	0.44	0.53	Ref.	0.53
Nominal Design plus 0.012" Hydrogen	T_{Ex}^*	1360	1200	1050	770	1200	1060	950	745	1090	920	880	675
	Φ	0.62	1.00	Ref.	0.80	0.50	0.73	Ref.	0.59	0.42	0.27	Ref.	0.59
Nominal Design plus 0.027" Hydrogen	T_{Ex}^*	1550	1270	1100	840	1290	1100	975	790	1150	1010	890	720
	Φ	0.90	1.13	Ref.	0.74	0.63	0.83	Ref.	0.53	0.52	0.80	Ref.	0.49

*Stem inlet temperature = 50°R

P_D = Decay power level of core - KW

T_L = Maximum temperature limit of liner - °R

T_{Ex} = Propellant exit temperature from stem - °R

Φ = $(T_{Ex} - T_{Ex, Ref.}) / (T_L - T_{L, Ref.})$

$T_{Ex, Ref.}$ = Propellant exit temperature from stem when $T_L = T_{L, Ref.}$ - °R

$T_{L, Ref.}$ = 1350°R

5.2 Supporting Data for Comparison Matrix

The comparison matrix of Section 4.0 weighs ten characteristics of six CD/TN systems. The ullage gas system is not given consideration as a system in itself because it is not believed to be a feasible system for all possible operating modes. It is a low tank pressure from blow-down rather than the gaseous phase hydrogen which limits operation. A separate recommendation is made, however, that ullage be given consideration for use in combination with the recommended system and used whenever possible.

5.2.1 NSS Reliability

The FARADA ratings of Appendix 6 are the basis of the system weighting factors in the comparison matrix. Generally, α thrust nulling reliability is superior to β . System 13b is slightly degraded because of the extra piping required and the radical change in the NSS internal flow path.

5.2.2 Safety

Early flow schematic representations of the systems did not include multiple redundancy of valves. A safety analysis was performed on these and is reported in Reference 19 in which system α has the best safety rating. A later version of the flow schematic eliminated all single failure points, so that all systems are ranked equal.

5.2.3 Propellant Efficiency

The great majority of the analysis effort of these Trade Studies was to improve the propellant utilization over that of the previous ground test reactors. Section 5.1 described the method of analysis and NSS component performance without dwelling on propellant integral. This propellant integral is used as the figure of merit for system efficiency.

The total post shutdown coolant depends on numerous parameters described in the previous sections for each of the systems. The type of shutdown, the amount of flow throttling, and the material temperature limits have been selected as examples to show their effects on the propellant integral. These results are shown in Tables 5-9 through 5-12 for NC, POSL, 7b and 13b, respectively. The propellant and average chamber temperature are given for the time periods in which different modes of operation exist. Valve throttling is discussed in Reference 1, improved shutdown in Appendix 1 of this report, and the liner limit raised to 1800°R.

Ultimately, all systems degenerate to a low power pulse cooling system because in all cases, laminar flow instability limits must be avoided. The chamber temperatures are an average over the time period. Obviously, as the heat flux approaches zero near the end of cooldown, the reactor and chamber temperature approach an isothermal condition restrained by the lowest material temperature limit within the system.

All the cooldown propellant integrals are based on early TNT-II calculations extrapolated to late cooldown and include the chamber temperature degradation caused by back flow through the hot bleed diluent line from the reflector inlet to the chamber. The amount of degradation in early cooldown is summarized below:

NC	90°R
POSL	75°R
7b	90°R
13b	65°R

TABLE 5-9
COOLDOWN PROPELLANT REQUIREMENTS - SYSTEM NC

TANK PRESSURE 30 PSIA			
<u>TIME - SEC</u>	<u>MODE</u>	<u>PROPELLANT - LB</u>	<u>CHAMBER TEMPERATURE - °R</u>
1200-1300	TAILOFF	2372	
1300-1430	CONTINUOUS	595	1400
1439-1950	PULSE	1281	1400-1200
1950-EOL	PULSE	5748	1200
		<hr/>	
		9996 LB	

IMPROVED SHUTDOWN
9145 LB

IMPROVED SHUTDOWN, INCREASED LINER LIMIT

1200-1250	TAILOFF	1017	
1250-3000	PULSE	3161	1400
3000-EOL	PULSE	3660	1400
		<hr/>	
		7838 LB	

TABLE 5-10

COOLDOWN PROPELLANT REQUIREMENTS - SYSTEM POSL

TANK PRESSURE 30 PSIA		W min = 0.5 LB/SEC CHAMBER TEMPERATURE - °R	
TIME-SEC	MODE	PROPELLANT - LB	
1200-1300	TAILOFF	2372	
1300-1430	CONTINUOUS	595	1400
1430-3700	POSL	2280	1550
3700-EOC	POSL	3788	1220
		<u>9035 LB</u>	
THROTTLE TO Wmin = 0.05 LB/SEC			
1200-1300	TAILOFF	2372	
1300-1430	CONTINUOUS	595	1400
1400-3700	POSL	2280	1550
3700-19000	POSL	2100	1400
19000-EOC	PULSE	1372	1200
		<u>8179 LB</u>	
IMPROVED SHUTDOWN			
		8184 LB	
IMPROVED SHUTDOWN, INCREASED T _L LIMIT			
1200-1250	TAILOFF	1017	
1250-3000	POSL	2200	1900
3000-EOC	PULSE	3660	1400
		<u>6877 LB</u>	

TABLE 5-11
COOLDOWN PROPELLANT REQUIREMENTS - SYSTEM 7b

	MINIMUM PRESSURE	30 PSIA	$W_{min} = 1.2 \text{ LB/SEC}$
1200-1250	1868	TAILOFF	
1250-1900	1440	7b	1900
1900-EOC	5467	PULSE	1300
	<u>8775</u> LB		
THROTTLE TO $W_{min} = 0.12 \text{ LB/SEC}$			
Same as above to	1900		
1900-10000	2890	7b	1400
10000-EOC	2131	PULSE	1200
	<u>8329</u> LB		
IMPROVED SHUTDOWN			
	7924 LB		
IMPROVED SHUTDOWN, INCREASED T_L LIMIT			
1200-1250	1017	TAILOFF	
1250-1900	1073	7b	2200
1900-EOC	4650	PULSE	1400
	<u>6740</u>		

TABLE 5-12
COOLDOWN PROPELLANT REQUIREMENTS - SYSTEM 13b

	MINIMUM PRESSURE	30 PSIA	Wmin = 0.3 LB/SEC
1200-1300	2372	TAILOFF	
1300-2300	1535	13b	1900
2300-2700	290	13b	1710
2700-3000	169	13b	1600
3000-3600	290	13b	1500
3600-5200	593	13b	1400
5200-EOC	3176	PULSE	1200
	<hr/> 8425		

THROTTLE TO Wmin = 0.03 LB/SEC

Same as above to	5200		
5200-27000	1750	13b	1400
27000-EOC	1060	PULSE	1200
	<hr/> 8059		

IMPROVED SHUTDOWN

7574 LB

IMPROVED SHUTDOWN INCREASED T_L LIMIT

1200-1300	1521	TAILOFF	
1300-2300	1285	13b	2200
2300-2700	243	13b	2000
2700-3000	142	13b	1800
3000-3600	243	13b	1700
3600-5200	497	13b	1600
5200-EOC	2760	PULSE	1400
	<hr/> 6691		

TABLE 5-13
COOLDOWN AND THRUST NULLING PERFORMANCE SUMMARY

Name	NC/ α	POSL/ α	NC/ β	POSL/ β	7b	13b
Supply pressure, psia	30	30	30	30	300 ⁽¹⁾	300 ⁽¹⁾
Minimum ⁽²⁾ Cooldown Propellant Requirement - lb	7838	6877	7838	6877	6740	6691
Average Cooldown Temperature- $^{\circ}$ R ⁽³⁾	1445	1642	1445	1642	1675	1715
Earliest Thrust Nulling Initiation Time-sec	8020	8020	6000	6000	1650	2410
Power-MW	0.75	0.75	1.17	1.17	11	4.9
Thrust Nulling Propellant Requirement - Lb.	4810	4810	6130	6130	10360	8520
			493	493	570	705
Initial Thrust Nulling Temperature- $^{\circ}$ R ⁽³⁾	660	660	965	965	1870	
Cooldown plus Thrust Nulling Propellant - lb.	10340	9379	10980	10330	11950	11340
Cooldown and Thrust Nulling Propellant for 350 KW Thrust Nulling Initiation - lb.	9220	8257	9360	8397	8260	8211

(1) Maximum allowable auxillary turbopump pressure, gradually decreased to tank pressure.

(2) Improved shutdown, increased liner limit, minimum flow constrained by LFI.

(3) Flow weighted average

For a liner limit of 1350°R , the liner itself is the limiting component. When this is raised to 1800°R , the stem (limit = 1360°R) and the aluminum support plate (limit = 710°R) become constraining. The effects of a change to high temperature material in the support plate/shields/internal piping area of the reactor is discussed in later paragraphs on high temperature thrust nulling .

The propellant used in thrust nulling may be a significant fraction of the total expended. For this reason it is also included as a measure of propellant efficiency. Thrust nulling may be initiated as a user option at any decay power level within the capability of the system. Two examples are cited, complete thrust nulling at the earliest time possible and thrust nulling from 350 KW to the end of cooldown. A numerical summary of this thrust nulling information and the cooldown of Tables 5-9 to 5-12 based on the minimum propellant integral cases is given in Table 5-13. The rating in the comparison matrix in the propellant efficiency column is directly related to the propellant integrals of this table.

All the previous information was developed for and presented at the August 26, 1969 Status Review Meeting at SNPO-C. Subsequently, the POSL cooldown system was adopted but the choice between α , β and β' was not clear and all have a disappointly low chamber temperature. More analysis was completed in which conduction from the core to the reflector turned out more significant in α than in β , reducing the α temperature from 660°R to 562°R because the necessity of intermittent reflector cooling. Also, study layouts were made of the interval piping for both high and low temperature

material capability. These drawing numbers are References 20 to 23. A substantial weight increase of approximately 2000 lbs. including valve and line weights is required to achieve a 1500°R capability. Table 5-14 contains a summary of this information and was prepared jointly by ANSC and WANL for a September 9, 1969 meeting.

5.2.4 Impulse Controllability

The methods of controlling impulse to ± 20000 lb-sec in the shutdown and cooldown ramp are discussed in Appendix 2. Although fission heating looks superior from a propellant usage viewpoint, the additional engine weight (6000 lb)⁽¹⁾ of gas bottles for the pneumatic actuated drums eliminated the concept. Temperature modulation was recommended as the means of controlling impulse.

The prediction of the final termination of cooldown thrust within ± 15 seconds can only be achieved by thrust nulling. Thus, thrust nulling must also be considered part of impulse controllability.

The comparison matrix ratings are based on the temperatures achievable in both nulling and cooldown. No system is rated highly because each one must sacrifice some performance to meet the requirement.

5.2.5 Early Thrust Nulling Capability

Although the capability of early thrust nulling is a requirement, its use is not recommended except in emergencies because of its high cost in propellant weight. The pressurized systems have superiority in early thrust nulling time. This time, and also the reactor decay power level, is shown on the propellant

TABLE 5-14
COMPARISON OF TANK PRESSURE THRUST NULLING SYSTEMS

SYSTEM	ALPHA	BETA	BETA - PRIME
Description	Stem cooling only, no core flow, reflector flow, placed through vessel head and exhausted radially. Flow rate is reduced approximately proportional to power while maintaining liner at its limiting temperature.	Stem coolant exhausted forward through 2 10/1 nozzles centered 30° from the load path. Reflector-coolant exhausted through core and main nozzle. Stem and core flow are continuously adjusted to balance forward and aft thrust components while maintaining liner at its limiting temperature.	Stem coolant is mixed forward to a flow splitting valve. Port is exhausted forward as in BETA and port is returned to the core and exhausted aft. Flows are adjusted as in BETA. When reflector requires cooling, BETA-PRIME becomes BETA. (75% of time)
Earliest Nulling Initiation Power	0.68 MW	1.05 MW	1.05 MW
Mission A, 2nd Burn, Nulling, from 350 KW	3580 lbs. propellant	3010 lbs. propellant	2950 lbs. propellant
Mission A, 4th Burn, Nulling, from 350 KW	2190 lbs. propellant	1900 lbs. propellant	1860 lbs. propellant
Coolant Exhaust Temperature for 1350°R Liner Limit	562°R	493°R Forward 980°R Aft	495°R Forward 1010°R Aft
Laminar Flow Instability	No core flow. Stems can be guided to avoid any instability in the stem vacuum. Instability not considered a problem.	With continuous 300°R core inlet temperature and >1000°R outlet, minimum stable flow is ~1.3 lb/sec. Operation in unstable flow range is necessary. Depending on location of low flow channel relative to liner, conduction may or may not relieve the situation. For very low flows, (1) channel diameter governs flow distribution, and (2) stable flow may be reestablished.	With continuous ~700°R core inlet temperature and exit < 1800°R, stable operation possible except when reflector flow is on, or when shields are sub-cooled prior to nulling. Conduction same as BETA.
Reactivity Feedback	No problems for low pressure (tank) fed systems.	No problems for low pressure (tank) fed systems.	No problems for low pressure (tank) fed systems.
Two Phase Flow Instability (only with LH₂ Feed)	Stems and reflector can be guided. Because of large volume first pass acting as an accumulator, boiling instability not expected to be a problem in stems.	Nozzle flow could introduce large asymmetries at core inlet and thus outlet.	Core not affected except when reflector flow is on.
Radioactive Effluent from Main Nozzle	None	Yes (how much for long-term core flow is unknown)	Yes (how much for long-term core flow is unknown)
Full Power Pressure Drop	100 psi $\left(\frac{0.02 \text{ sec}}{\text{psi}} \right) \left(\frac{140 \text{ lb}}{\text{sec}} \right)$ = 280 lb. payload	No direct effect on payload. May decrease SSCV reactivity worth.	No effect.
Exhaust Tank Heating	Insignificant.	More than ALPHA but not significant.	More than ALPHA but not significant.
RCS Differences	100 payload penalty over BETA.	BASE (symmetric thrust from low temperature diluent?)	BASE
Maneuverability	None	± ΔV capability	± ΔV capability
Controls	No additional controls	Control of very low flows to balance thrusts	Control of very low flows to balance thrusts
Diluent Effect	Diluent is taken downstream of TNY and thus has no effect.	Diluent taken from dome and degrades aft chamber by 95°R	Diluent taken from dome and degrades aft chamber by 95°R
Low Temperature Nulling Remarks. (T ≤ 700)	No internal plumbing changes. 50 psi pressure drop added to system to take 100% of flow outside and back into vessel at design point. Weight increment, Base	Requires internal manifolding to take stem only out of vessel. Puts ~40 in penetration in shield. Must provide dome mixing at full power. Piping pressure drop could reduce full power flow control. Weight increment, 100 lb.	Requires internal manifolding to take stem only out of vessel. Puts penetration in shield. Must provide dome mixing at full power. Piping pressure drop could reduce full power flow control. Weight increment, 100 lb.
High Temperature Nulling Remarks. (T = 900 - 1200)	Requires extensive manifolding to duct stem flow from high temperature support plate to TNY. Additional valving necessary to join reflector flow in nulling. During coastdown and full power, additional mixing required to duct coolant through central shield. Mixing problem re-introduced. Diluent shut-off valve needed for complete nulling. Pressure vessel - piping interface cooling required. Weight increment >1000 lb.	Requires extensive manifolding to duct stem flow to TNY and back through central shield. Full power and nulling flow mixing problem. Pressure vessel - piping interface cooling required. Weight increment >1000 lb.	Requires extensive manifolding to duct stem flow to TNY and back through central shield. Full power and nulling flow mixing problem. Trick to flow through reflector required to prevent diffusion of high temperature gases to reflector. Pressure vessel - piping interface cooling required. Weight increment >1000 lb.
Valve and Line Weights	840 lb.	900 lb.	900 lb.

efficiency chart, Table 5-13.

5.2.6 Laminar Flow Instability

Continuous flow systems having core flow in thrust nulling cannot avoid operating in the unstable regions (see Appendix) and are therefore ranked lowest. The 13b/ α system has minor penalties associated with early cooldown while the NC/ α and POSL/ α do not, and are ranked accordingly.

5.2.7 Stress Level

After pump tailoff, only the pressurized systems have any significant stresses. The tank pressure systems are virtually unstressed by comparison with design point stresses.

5.2.8 Reactivity Feedback

Only one system was found to have positive feedback; that one is 7b in thrust nulling. The reactivity change relative to cold critical, including both temperature and hydrogen effects, for thrust nulling at the earliest possible time is given below:

System	Reactivity Change - ρ
NC/ α	-0.62
NC/ β	-0.57
POSL/ α	-0.62
POSL/ β	-0.57
7b/ β	+0.03
13b/ α	-0.97

5.2.9 Control Complexity

The on/off control system of NC/ α is by far the simplest to augment. POSL/ α is still relatively simple. Alternates 3, 4 and 6 are equal since 3 and 4 have β nulling and 6 has α nulling but the additional control of an auxiliary turbine. System 7b has the control complexity of each of the systems.

5.2.10 Configuration Complexity

The NC/ α and POSL/ α require the fewest preliminary changes to the NSS and engine. The NC/ β , POSL/ β and 7b/ β require addition engine valves, pressure vessel penetrations, and complex internal piping. The 13b/ α is penalized most because of its multiplicity of valves and flow paths.

6.0 REFERENCES

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APPENDIX 1 SHUTDOWN

All of the analyses performed in support of the cooldown and thrust nulling trade studies were based on a 1200 second full power burn (4640°R , 450 psia) followed by a reference shutdown profile.

The reference shutdown profile shown in Figure A1-1 is based on the calculations reported in Reference (8). It is initiated with a T_c ramp of $150^{\circ}\text{R}/\text{sec}$ to $T_c = 3000^{\circ}\text{R}$ and $P_c = 275$ psia. Drums are then rolled in at $10^{\circ}/\text{sec}$ and a T_c rate of $-75^{\circ}\text{R}/\text{sec}$ to $T_c = 1500^{\circ}\text{R}$, and finally hold at $T_c = 1500^{\circ}\text{R}$ until tank pressure is reached. The SSCV is opened at roll-in time and remains open for the remainder of shutdown and cooldown. Following the 1500°R T_c hold, the various cooldown scheme analyses are initiated (Reference 9). Figure A1-2 shows the variation of chamber temperature during the standard shutdown.

An "improved" shutdown was devised in an effort to reduce propellant consumption. Basically, the scheme involves the use of the support stem liner temperature, which is the limiting structural component, to as a feedback variable for control of the system shutdown. The scheme is described in Reference 10.

Propellant requirements to the two shutdown programs are quite different, Figures A1-3 through A1-5 show the support stem and total system inlet flow rates, weight of propellant used, and the chamber temperature is a function of time following shutdown. As can be seen, the improvements in propellant utilization are significant.

Several other shutdown profiles were examined. Figures A1-7 and A1-8 show the chamber and S S liner temperatures, feedback reactivity, and propellant flow rates for shutdowns at $100^{\circ}\text{R}/\text{sec}$ and $200^{\circ}\text{R}/\text{sec}$ from a 1200 second full power burn. Figures A1-9 and A1-10 show the variation of these same parameters for a shutdown controlled by chamber temperature demand and one by maintaining the maximum SS tube liner temperature within specified range. The $100^{\circ}\text{R}/\text{sec}$ and $200^{\circ}\text{R}/\text{sec}$ calculations provide data bracketing the reference shutdown; the other two TNT runs were carried out in an attempt to investigate other possible shutdown schemes that ultimately resulted in the improved shutdown described previously.

Figure A1-1. Typical Run Profile

CHAMBER 1. FUEL-CENTRAL 2. AVERAGE 3. PERIPHERY 4. LATERAL SUPPORT 5	INDEX NO.
150 R/SEC SHUTDOWN TO 3000 R	001-R1
CHAMBER TEMPERATURE	

Figure A1-2. Chamber Temperature Profile for Reference Shutdown

Figure A1-3. Comparison of Shutdown Support System Flow Rates

Figure A1-4. Comparison of Shutdown System Flow Rates

Figure A1-5. Comparison of Total Propellant Weight During Shutdown

Figure A1-6. Comparison of Shutdown Chamber Temperatures

Figure A1-7a. Chamber Temperature During 100°R/Sec Shutdown

MAXIMUM 1 AVERAGE 2 MINIMUM 3

335-R18-SO-BLP-342259 SHUTDOWN AT 133 R/SEC AFTER 1233 SEC AT FULL
 STRUCTURAL SUPPORT LINER TEMPERATURES

INDEX NO.

331-R1

Figure A1-7b. Structural Support Liner Temperatures During 100°R Sec Shutdown

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FEEDBACK 1 IMPRESSED 2 EXCESS 3

335-R18-S0-BLP-342259 SHUTDOWN AT 133 R/SEC AFTER 1233 SEC AT FULL

REACTIVITY

INDEX NO.

331-R1

Figure A1-7c. Reactivity During 100°R/Sec Shutdown

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Figure A1-7d. Flow Rates During $100^{\circ}\text{R}/\text{Sec}$ Shutdown

CHAMBER 1 FUEL-CENTRAL 2. AVERAGE 3. PERIPHERY 4 LATERAL SUPPORT 5	
334T-R18-SD-BLP-342200 SHUTDOWN AT 200 R/SEC AFTER 1230 SEC AT FULL	INDEX NO.
CHAMBER TEMPERATURE	331-R1

Figure A1-8a. Chamber Temperature During 200°R/Sec Shutdown

MAXIMUM 1 AVERAGE 2 MINIMUM 3

334T-R18-SD-BLF-342269 SHUTDOWN AT 200 R/SEC AFTER 1230 SEC AT FULL	INDEX NO.
STRUCTURAL SUPPORT LINER TEMPERATURES	331-R1

Figure A1-8b. Structural Support Liner Temperatures During 200°R/Sec Shutdown

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FEEDBACK 1 IMPRESSED 2 EXCESS 3

334T-R18-SQ-BLP-342209 SHUTDOWN AT 200 R/SEC AFTER 1230 SEC AT FULL	INDEX NO.
REACTIVITY	331-R1

Figure A1-8c. Reactivity During 200°R/Sec Shutdown

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CHAMBER 1 CORE 2 REACTOR 3 SUPPORT SYSTEM 4 BYPASS 5

334T-R18-SD-BLP-342299 SHUTDOWN AT 233 R/SEC AFTER 1233 SEC AT FULL

FLOW RATES

INDEX NO.

331-R1

Figure A1-8d. Flow Rates During 200°R/Sec Shutdown

Figure A1-9a. Chamber Temperature During Modified Shutdown

Figure A1-9b. Structural Support Liner Temperatures During Modified Shutdown

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Figure A1-9c. Reactivity During Modified Shutdown

Figure A1-9d. Flow Rates During Modified Shutdown

Figure A1-10a. Chamber Temperature During Liner Temperature Controlled Shutdown

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MAXIMUM 1 AVERAGE 2 MINIMUM 3

SHUTDOWN FROM FULL POWER CONTROL ON SS LINER TEMPERATURE
STRUCTURAL SUPPORT LINER TEMPERATURES

INDEX NO.

331-R1

Figure A1-10b. Structural Support Liner Temperatures During
Liner Temperature Controlled Shutdown

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Figure A1-10c. Reactivity During Liner Temperature Controlled Shutdown

Figure A1-10d. Flow Rates During Liner Temperature Controlled Shutdown

APPENDIX 2. IMPULSE CONTROLLABILITY

Consideration of shutdown/cool-down (SD/CD) impulse controllability is a topic distinct from the cool-down (CD) and thrust nulling (TN) trade studies, in that the controllability requirements apply equally to all CD/TN schemes examined. The purpose of the present discussions is to summarize the work done on establishing SD/CD procedures that provide the required impulse control capability and are applicable to any proposed CD/TN scheme. The material summarized here is taken from References (11) and (12). The calculations are based on a run with a total impulse and 9×10^7 lb-sec, with a throttle hold at 60% for 30 seconds before shutdown.

Principal sources of error in the post-shutdown power profile are individual reactor variations and the decay heat uncertainty. At present, this total decay power uncertainty is about $\pm 20\%$; for the flight engine, the uncertainty is projected to be about $\pm 5\%$. Figure A2-1 shows the variation of the total SD/CD impulse as a function of the decay power uncertainty for two average chamber temperatures. Clearly, if the NPRD specification of a $\pm 20,000$ lb-sec uncertainty on the total SD/CD impulse is to be met, some type of impulse control capability must be provided.

There are two aspects to the impulse control problem: (1) reduce the decay heat uncertainty, and (2) devise control schemes that can compensate for the uncertainty that is present.

Reduction of the decay heat uncertainty depends on the information obtained in the ground test program; the only input to the CD/TN trade studies is the maximum amount of heat to be removed, i.e., the total amount of coolant to be loaded including contingency. An uncertainty in the decay heat thus translates directly into a payload penalty.

Three different SD/CD impulse control schemes have been considered: (1) thrust nulling during cool-down, (2) fission heating during cool-down, and (3) modulation of the cool-down temperature. These schemes are reviewed briefly below.

1. Thrust Nulling During Cooldown

The impulse control scheme based on thrust nulling is illustrated in Figure A2-2(a). This figure shows qualitatively the cooldown impulse as a function of time after shutdown and decay heat uncertainty. The total impulse prediction would be based on the 3σ minimum decay power profile, providing for contingencies. The predicted cooldown impulse would thus have some value between the limits shown. Control of the actual impulse consists of choosing the actual thrust nulling time such that the actual impulse is within $\pm 20,000$ lb - sec of the predicted value.

The coolant weight penalty associated with this impulse control scheme consists of two parts. One is the coolant required to remove the decay heat; this quantity depends on the decay heat uncertainty. The other part is the propellant required to generate the impulse voided by thrust nulling; this quantity also depends on the decay heat uncertainty, because the predicted total impulse cannot exceed the 3σ minimum impulse. Both of these quantities depend on the power level at which thrust nulling is initiated. Figure A2-3 shows the fraction of cooldown propellant used as a function of the time after shutdown, while Figure A2-4 shows the propellant required at full power to recover the impulse voided by thrust nulling.

2. Fission Heating During Cooldown

The major disadvantage of thrust nulling to control the SD/CD impulse is that a significant part of the available cooldown thrust is discarded. This deficiency is eliminated by using fission power to control the decay power profile.

Suppose that the SD/CD impulse is always predicted at its three maximum value, as shown in Figure A2-2(b). Then, fission heating between cooldown pulses can be used to maintain the power level at the predicted value.

Details of the technique are discussed in Reference (12). The main advantage of this scheme is that it permits full use of the cooldown impulse for all coolant used. Disadvantages are that the control actuator stores will increase (100-300 lb LH_2 per cooldown for the pneumatic actuators), the increased actuator operation will lead to reliability reduction, and additional sensor and control requirements are introduced.

Pneumatic storage bottles become very weighty. If electric actuators are selected this concept should again be reviewed for its merits.

3. Modulation of Cooldown Temperature

The third scheme proposed to control the cooldown impulse is illustrated in Figure A2-2(c). Briefly, the scheme consists of adjusting the instantaneous coolant flow rate, and thus the chamber temperature, in response to decay heat variation to maintain the predicted SD/CD thrust profile. The coolant required by this control scheme is shown in Figure A2-5 as a function of the decay power uncertainty. This figure also shows the additional coolant required to extend the impulse control capability to correct for full power run termination errors.

Relative merits of the various cooldown impulse control schemes noted above may be expressed in terms of the overall specific impulse decrement associated with each scheme. This comparison is shown in Table A2-1. As can be seen, the fission heating scheme shows a definite advantage. This advantage, however, must be considered in view of the additional control complexity.

TABLE A2-1
IMPULSE CONTROL TECHNIQUES COMPARISON
Mission B

<u>TECHNIQUE</u>	FIRST BURN		ALL BURNS	
	(1) ΔI_{sp} SEC.	(2) WITH 5% UNCERTAINTY	(1) ΔI_{sp} SEC.	(2) WITH 5% UNCERTAINTY
NULL END OF COOLDOWN	-32	-16	-49	-25
VARY T_c DURING COOLDOWN	-13	-7	-20	-10
USE FISSION HEATING	-3	-1.5	-5	-2.5

(1) Reference 13

(2) Projected Uncertainty Level for Flight Engine.

* Impulse during both shutdown and cooldown

Figure A2-1. Preliminary Estimate - Shutdown & Cooldown
Total Impulse Uncertainty
(20 min. equivalent full power run)

(a) THRUST NULLING

Figure A2-2(a). Thrust Nulling Impulse Control Schemes

(b) FISSION HEATING

Figure A2-2(b). Fission Heating Impulse Control Schemes

(c) TEMPERATURE MODULATION

Figure A2-2(c). Temperature Modulation Impulse Control Schemes

Figure A2-3. Preliminary Estimate - NERVA Coolant Usage History - Cooldown

Figure A2-4. Preliminary Estimate - Weight Penalty In Additional Run LH_2^* With Thrust Nulling

Figure A2-5. Preliminary Estimate - Total Shutdown and Cooldown Coolant Required Vs. Modulatable Impulse
(9×10^7 -sec. Total Burn)

APPENDIX 3. MATERIAL TEMPERATURE LIMITS

The material temperature limits used in the cooldown and thrust nulling calculations were obtained from Reference 4. Two sets of temperature limits are considered: (1) shutdown after which the reactor is capable of restarting to full power, and (2) terminal shutdown, after which the reactor will remain intact but will not be capable of restarting. Numerical values of the limiting temperatures are summarized in Table A3-1.

TABLE A3-1

COOLDOWN AND THRUST NULLING MATERIAL TEMPERATURE LIMITS

	RESTARTABLE				Terminal Limit
	Material Capability	Reason	Thermal Uncertainty	Component Limit	
SUPPORT STEM	1460	Ductility (SNPO-C 1)	100	1360	2070
LINER	1500*	Carbiding	150	1350	2220
COLD END HARDWARE	1260	Springs	150	1110	1850
SUPPORT PLATE	710	Overaging	---	710	1030
SHIELD	1010	Loss of H ₂ from TiH ₂ , Overaging of AL	---	1010	1160
REFLECTOR	750	Limited Property Data, Indication of Degradation	---	750	1200
SUPPORT RINGS	1010	Hydriding	---	1010	1200

* RECENT MATERIAL TESTING INDICATES MATERIAL CAPABILITY MAY BE RAISED TO 2000°R. THEREFORE, THE COMPONENT LIMIT WILL BE RAISED TO 1750°R.

APPENDIX 4. FUEL ELEMENT CORROSION

Corrosion of R-1 fuel elements at the low temperatures and pressures characteristic of the cooldown and thrust nulling periods is not well known since only limited experimental data are available at low temperatures and none at low pressure for coated elements. XE fuel elements have been tested in the electrical furnace at an exit gas temperature of $\sim 2500^{\circ}\text{R}$ and an exit gas pressure of ~ 13 atmospheres. The average corrosion weight loss was 0.025 gm/min per element.

At exit gas temperatures below 3000°R the corrosion would tend to take place behind the coating cracks with essentially zero diffusion through the coating. For bare graphite an analytic expression is available which will enable the reaction rates to be computed as a function of temperature and pressure; the forward reaction rate increasing linearly with pressure. The significance of this type of corrosion for R-1 fuel elements depends upon the coating crack distribution and on the effect of subsequent full power operation. It is possible that at low flow rates a back reaction might be present which would tend to retard the corrosion.

Electrical furnace tests on XE fuel elements have indicated that low temperature testing tends to degrade subsequent performance at full power. The most likely explanation for this effect is that the bore enlargement behind the cracks prevents them from closing completely at operating temperatures. No similar data are available for R-1 candidate fuel elements.

Figure A4-1 shows the forward reaction rate for uncoated coolant channels as a function of local surface temperature. This type of corrosion would occur behind each coating crack and represents local channel bore enlargement. At 2000°R it can be seen that the corrosion rate is 0.02 mils/hr and at 2500°R it is 0.3 mils/hr for an exit gas pressure of one atmosphere. Although this degree of corrosion is small, surface regression would be approximately equal to the maximum coating thickness (3 mils) after ten hours at this rate. At the lower power conditions existing during cooldown, it may be assumed that the maximum surface temperature is approximately equal to the exit gas temperature. Under these conditions and for cooldown times

greater than ten hours the maximum fuel element temperature limit is in the vicinity of 2000°R . Experimental data are required to verify this. However, the performance data given in Section 5.0 indicate that none of the systems investigated are capable of sustained operations above 2000°R , so that corrosion ceases to be a cooldown problem.

Figure A4-1. Hydrogen Corrosion Rate for Uncoated Fueled Graphite

APPENDIX 5. LAMINAR FLOW INSTABILITY

At low coolant flow rates, the flow in the core channels may experience what is commonly referred to as laminar flow instability (LFI). The phenomenon is well documented; see for example Reference 15 or 16. Basically, for fixed inlet conditions and heat input, there is a range of pressure drops (ΔP) along a channel for which the flow rate decreases with increasing ΔP . In this region perturbations in pressure or flow rate are amplified with time.

The input of LFI analyses to cooldown and thrust nulling trade studies consists of flow stability limits, i. e. values of coolant flow rate as a function of inlet and exit temperature below which the coolant flow may be expected to be unstable. The stability limits used in the present trade studies were determined in Reference 17. Basically, the procedure involved solving the appropriate steady state conservation equation and matching the solution to the nozzle compatibility equation. The result of this analysis is shown in Figure A5-1. These stability boundaries can be used to set lower limits on the coolant flow rates in the various cooldown and thrust nulling schemes.

It should be noted that the analysis leading to the results shown in Figure A5-1 assumes no coupling between the flow channels; the data represent stability limits derived by applying conservative criteria to single channel calculations.

An estimate of the effect of the LFI boundaries on the cooldown propellant requirements is presented in Figure A5-2. In this figure, the ordinate is the ratio of the total cooldown propellant used assuming all flow is stable, to the amount that would be used if the coolant flow rate is increased to avoid unstable low flows. This minimum stable flow is taken from Figure A5-1. The abscissa is the temperature capability of the system, that is, no material temperature limits are violated at this T_c . It is not the actual operating T_c . For ratios less than 1.0, the actual T_c has been reduced because flow rate has been increased above the minimum in order to remain stable. For continuously throttled stem flow with $T_c < 1450^\circ\text{R}$ there is no associated penalty. This figure does oversimplify the problem because it assumes continuous flow but one can scope the magnitude of the problem with it.

Figure A5-1. Flow Stability Curves for the R-1

Figure A5-2. Estimated Performance Loss From Laminar Flow Instability -

Continuous Support Stem Flow

Continuous Reflector Flow If Total Flow > 0.5 lb/sec

APPENDIX 6. RELIABILITY

Four concepts for thrust nulling and cooldown, α , β , 7b, and 13b were reviewed and reliability ratings were generated. The details of the calculations are given in Reference 18. At the time of this reliability study the basic concepts of each system was known but since that time specific designs have evolved. Since the initial study indicated that the various designs differed only slightly in their reliability ratings, it will be necessary at sometime in the future to refine the previous study by considering the greater design detail now available.

In analyzing and comparing the various thrust nulling and cooldown designs a failure mode analysis was conducted on each design and the potential criticality of each failure was indicated. A collection of the critical failure modes (category A and B) was made for each design and the components involved in these failure modes were listed. Using FARADA tables an average failure rate was established for each of the components. From other FARADA tables the likelihood of a specific type of failure (fails open, closed, stuck, etc.) was calculated. Using the reliability rating technique from reliability guideline 003-A, quantitative numbers were generated for comparison and are listed below:

Design α	2.039
Design β	1.939
Design 7b	2.039
Design 13b	2.036

It appears that design β has a slight advantage over the other designs presented primarily because it is less complex in nature. The difference between the reliability rating figures is not of sufficient magnitude to be conclusive as to which design is best.

The major weakness in the 13b design appears to be the three way valve necessary for thrust nulling. If the valve fails in the closed position the core flow is cut off. During reactor operation this would cause overheating, loss of full power capability, or possible loss of emergency thrust.

APPENDIX 7. DEFINITION OF THE END OF COOLDOWN

Definition of the problem

In order to estimate the time at which cooldown of the NERVA Reactor can be terminated, without subsequently exceeding the temperature limits of any of the nuclear subsystem components, a simple two-dimensional model was used to simulate the mid-axial region of the nuclear subsystem. The TAP-A computer program was used to make a series of steady state and transient heat transfer calculations with this model.

The results indicate that the maximum allowable reflector temperature is the constraint which determines at what decay power level cooldown can be terminated. If the system is to be restartable, the maximum allowable reflector temperature is 750°R . In order that this temperature not be exceeded after cooldown is terminated, the last cooling pulse would have to occur when the decay power level has dropped to approximately ten KW. Relaxing the constraint on the maximum allowable reflector temperature implies that the last cooling pulse can occur at a higher decay power level with the result that cooldown can be terminated earlier. Cooldown can be terminated an hour earlier for every 17°R rise in the maximum allowable reflector temperature for restartable cooldown.

For an "Intact But Not Restartable" type of cooldown the maximum allowable reflector temperature is 1200°R and the decay power level at the time of the last cooldown pulse is approximately 37.5 KW. Figures A7-8 and A7-9 show the maximum reflector temperature reached as a function of decay power level at the time of the last pulse and the savings in cooldown time.

Once the maximum allowable reflector temperature is exceeded the next temperature constraint that must be considered is the 710°R limit imposed upon the aluminum pressure vessel for a restartable type of cooldown. This limit is reached if the decay power level at the time of the last cooling pulse is approximately 12 KW. The temperature limit on the pressure vessel for an "Intact But Not Restartable" cooldown is 1030°R and would be reached if the last cooling pulse occurred at a decay power level of about 42 KW. Figure A7-10 shows the maximum pressure vessel temperature as a function of the decay power level at the time of the last pulse.

The core and lateral support temperature limits for a restartable cooldown are not exceeded for decay power levels at the time of the last cooling pulse up to 45 KW.

The maximum reflector temperature reached after termination of cooldown can be lowered if the resistance to heat flow across the gap separating the pressure vessel and reflector is decreased. At present the only heat transfer mechanism is radiation. Depending upon the decay power level at the time of the last cooling pulse the temperature drop across the gap is 87 to 197°R when the maximum reflector temperature is reached. If the heat flow across the gap could be increased by providing additional heat flow paths, the maximum reflector temperature would be decreased allowing the end of cooldown to occur earlier, that is, when the decay power level is higher.

The heat transfer model is two-dimensional and the temperatures computed should be somewhat higher than would be determined from a three-dimensional model. There is an axial decay power distribution. The highest value occurs at the mid-axial region and is 1.4 times the average value. Axial conduction will tend to lower the temperature at the midpoint region to some extent. Another point to consider is that the model is quite coarse and there will be local temperature variations so that parts of the region assigned to a particular node will exceed the temperature computed for the node. In addition, the view factors for the exterior surface nodes of the pressure vessel were assumed to be unity. Due to the presence of the piping, propellant tank, nozzle, turbo-pump, etc., the pressure vessel does not see only space. The radiative heat transfer from the pressure vessel will be decreased and the temperature levels of the pressure vessel and reactor system will tend to be greater than for an isolated pressure vessel.

The Thermal Model

In order to obtain a preliminary estimate of the allowable decay power level at the time of the last cooling pulse, it was felt that a simple two-dimensional model of the mid-axial region of the pressure vessel and reactor assembly would be sufficient. A thermal model of a 180° sector of this region was made and is shown in Figure A7-1. The core was divided into two radial and eight azimuthal regions.

The first radial region is for $0 \leq R \leq 14"$ and the outer radial region is for $14" \leq R \leq 19.34"$. The latter region includes the filler strips. The azimuthal sectors are the same for all regions and are made up of four 15° and four 30° sectors. The remaining radial regions are composed of the lateral support, the inner and outer reflector and side boral liner, and the pressure vessel.

Two modes of heat transfer were considered, grey-body radiation and conduction. The outer surface of the pressure vessel is assumed to intercept a specified solar heat flux and to radiate to deep space. It is assumed that the grey-body emissivity of the outer surface of the pressure vessel is 0.84 and that the grey-body absorptivity is 0.92. Heat transfer across the gap between the pressure vessel and reflector is assumed to occur by radiation only. The grey-body emissivity of these surfaces is assumed to be 0.84. The lateral support system will transfer heat by radiation and conduction. All other heat transfer between nodes is by conduction only.

To account for the increased heat transfer impedance due to voids and contact resistance, the effective heat conduction path lengths were increased.

It is to be expected that the two-dimensional model will overestimate the temperature somewhat. This is due to the fact that there is an axial decay power distribution which has its largest value at the mid-axial region. Axial conduction will tend to lower the mid-axial temperature, but to obtain any quantitative estimate of the difference, a three-dimensional model would be required.

Assumptions

In order to obtain the temperature histories of the various nodes, the heat input as a function of time and the initial temperature distribution are required. The two sources of heat are the solar flux incident upon the exterior surface of the pressure vessel and the decay heat generation rates in the various regions of the pressure vessel and reactor assembly. The following sections outline the major simplifying assumptions:

1. Solar Heat-Input - To estimate the solar heat input it was assumed that the system is rotating with a rotational period that is small compared to the thermal time constant of the system. The solar flux, q_s , was assumed to be that for near earth ($444 \text{ BTU/FT}^2 - \text{HR}$) and the heat input to the surface, q , was taken to be

$$q = (\alpha / \pi) q_s$$

where α is the grey-body absorptivity of the pressure vessel surface.

2. Decay Heat Generation Rates - The decay heat generation rates were taken from Reference 24. The average heat generation rates given in the reference correspond to a total decay power level of 91 MW at full power operation. It was assumed that the spatial distribution given in this reference was the same at any time after shutdown so that the heat generation rates Q_{AV} , at any power level P KW can be obtained from the rates Q_o at the 91 MW power level by means of the relationship.

$$Q_{AV} = \frac{Q_o P}{9.1 \times 10^4}$$

To obtain the heat generation rates Q_{MAX} at the mid-axial region the following equation was used:

$$Q_{MAX} = 1.4 Q_{AV}$$

(U) When the decay power level has dropped to a few hundred kilowatts, the slope of the curve of power versus time after shutdown on log-log paper is very nearly unity for any full power burn time. As a first approximation the decay power P is proportional to the time after shutdown t , or $P = At^N$ where A is a constant and $N \approx -1$. Since the purpose of this study is to determine the allowable decay power level at the end of the last cooling pulse and the decrease in the total cooldown time as a function of the maximum allowable system temperature, it is not necessary to know the decay power as a function of time after shutdown for each full power burn time. Zero time can be set at some arbitrary decay power level (say 50 KW) since the change in the decay power for a given time increment will be the same in this decay power level range for any full power burn time. Even though the time after shutdown to reach a specified decay power level will be different for two different full power burn times, the time for the decay power to decrease from 50 KW to 40 KW would be approximately the same in both cases. One curve of decay power versus time can thus be used as being representative of any full power burn time. Figure A7-2 shows a typical decay power curve.

(C) 3. Initial Temperatures

The temperature distribution after a cooling pulse was assumed to be independent of the decay power level at which the pulse occurred. The temperatures used in the computations were obtained from TNT computer runs and are listed below:

Inner Core Region	590°R
Outer Core Region and Filler Strips	500°R
Lateral Support	500°R
Inner and Outer Reflector and Side Boral Liner	235°R
Pressure Vessel	370°R

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Method

All calculations were made using the TAP-A thermal analysis program.

In order to estimate the system temperatures for the case of a long stay in a near-earth environment prior to startup, a steady state case was run with no internal heat generation, the solar flux being the only heat input to the system. Several other steady state cases were run at various low power levels to determine the system temperatures under these conditions. Finally a series of transient cases were run assuming the last cooling pulse occurred at power levels from 5 to 35 KW.

Results

The first problem considered assumed that the system was in a near-earth environment prior to startup. The only heat input to the system is that due to the solar flux, internal heat generation being essentially zero. If the system is rotating and the pressure vessel is exposed to the direct rays of the sun, the steady state temperature of the reflector and pressure vessel is approximately 547°R . It can be seen that this temperature is approximately 160 and 200°R less than the maximum allowable temperatures of the reflector and pressure vessel respectively for a restartable cooldown.

Next a series of transient cases were run where the initial temperatures of the various parts of the system were those listed previously and the decay power level at the start of the transient was varied from 5 to 35 KW.

Figures A7-3 through A7-6 show the temperature histories of the different parts of the system for the various power levels considered. It can be seen that the same general trend is followed in most cases for all parts of the system. With the exception of the inner core region and for the two lowest power runs for the outer core region, a relatively sharp initial temperature rise occurs. The rate of increase in temperature gradually decreases; the temperature passes through a maximum and then decreases. Eventually, a steady state value will be approached which should be close to the values computed for the zero heat generation case, assuming the same solar heating. From the figures it is seen that the maximum temperatures are reached somewhere between 60,000 to 90,000 seconds (16.67 to 25 hours) after the start of transient (time of the last

cooling pulse) depending upon the node being considered and the decay power level at the start of the transient. The maximum temperatures are reached sooner for higher initial power levels because the pressure vessel gets hot enough at an earlier time to begin radiating more heat to space than is being generated internally and is being absorbed by the external surface of the pressure vessel.

The temperature of the inner region of the core decreases during the initial part of the transient for most of the decay power levels because the heat generation rate is not large enough to compensate for the heat flowing to the cooler outer regions of the system. As the reflector and pressure vessel heat up the heat flow out of the core decreases and the internal heat generated is then sufficient to cause the core temperature to increase.

For initial heat generation rates up to 45 KW, the temperature limits of the core and lateral support are not exceeded and these regions should not cause any constraints on the time at which cooldown can be terminated.

For restartable type of the cooldown the reflector temperature limit of 750°R is the primary constraint which limits the allowable decay power level at the time of the last cooling pulse to 10 KW. The pressure vessel temperature limit of 710°R requires that the decay power level be about 12 KW at the time of the last cooling pulse.

For an "Intact But Not Restartable" type of cooldown, the reflector and pressure vessel temperature limits are 1200°R and 1030°R respectively. The corresponding decay power levels at the time of the last cooling pulse are 37.5 and 52 KW. Again it can be seen that the reflector temperature limit is the primary constraint.

Since it requires approximately 27 hours for the decay power to decrease from 37.5 KW to 10 KW, then on the average, cooldown can be terminated one hour earlier for every 17°R increase in the maximum allowable reflector temperature. Figure A7-7 shows the decrease in cooldown time as a function of the maximum allowable reflector temperature.

Figures A7-8, A7-10, A7-11 show the maximum temperatures reached by the various parts of the system as a function of the decay power level at the time of the last pulse.

The maximum temperature reached by the reflector after the termination of cooldown could be decreased if the resistance to heat flow across the gap separating the pressure vessel and reflector can be decreased. At present the only heat transfer mechanism across the gap is radiation. Depending upon the decay power at the time of the last pulse, the temperature drop across the gap when the reflector reaches its maximum temperature varies from 87°R to 197°R . If the heat flow across the gap could be increased by providing additional heat flow paths, the maximum reflector temperature could be decreased and it may be possible to terminate cooldown earlier, when the decay power level is higher.

Conclusion

Since a two-dimensional, relatively coarse, thermal model of the pressure vessel and reactor assembly has been utilized to compute system temperatures, and since no attempt has been made to model that portion of the vehicle system exterior to the pressure vessel, only very tentative conclusions can be made as to the constraints imposed upon the decay power level at the end of the last cooling pulse:

1. For a restartable cooldown the maximum allowable temperatures of the reflector and pressure vessel impose the major constraints on the decay power level at the time of the last cooling pulse. These temperature limits are 750°R and 710°R , respectively, and the corresponding decay power levels at the end of cooldown are 10 KW and 12 KW.
2. For an "Intact But Not Restartable" cooldown the temperature limits for the reflector and pressure vessel are 1200°R and 1030°R and the corresponding decay power levels are 37.5 KW and 42.0 KW respectively.

3. The core and lateral support regions will impose no constraints on the power level at the end of cooldown for power level at the end of cooldown up to 45.0 KW.
4. The maximum temperatures occur sometime between 17 and 25 hours after cooldown is terminated depending upon the region considered and the decay power level when cooldown was terminated.
5. If the maximum allowable temperature limits on the reflector and the pressure vessel are increased, cooldown can be terminated sooner. For every 17°R rise in the allowable reflector temperature and 13°R rise in the allowable pressure vessel temperature, cooldown can be terminated one hour earlier.
6. The results of this study are preliminary and depend to a large extent on the simplifying assumptions and thermal model used. The values used for the emissivity and absorptivity of the various heat transfer surfaces, particularly the outer surface of the reflector and inner surface of the pressure vessel, have a very marked effect on the results. Care should be exercised in making any extrapolations beyond the limits of the analysis.

Figure A7-1. R-1 Nuclear Subsystem Space Environmental Model

Figure A7-2. Decay Power as a Function of Time after Startup

Figure A7-3. Reflector Temperature as a Function of Time after Last Cooling Pulse

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Figure A7-4. Pressure Vessel Temperature as a Function of Time After Last Cooling Pulse

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Figure A7-5. Core Temperature as a Function of Time after Last Cooling Pulse - Region (0 r 14")

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Figure A7-6. Core Temperature as a Function of Time After Last Cooling Pulse - Region ($14'' \leq r \leq 19.34''$)

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Figure A7-7. Lateral Support Temperature as a Function of Time after Last Cooling Pulse

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Figure A7-8. Maximum Reflector Temperature as a Function of Decay Power Level at Termination of Cooldown Initial Temperature 235°R.

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Figure A7-9. Maximum Allowable Reflector Temperature as a Function of Decrease In Cooldown Time

Figure A7-10. Maximum Pressure Vessel Temperature as a Function of Decay Power Level at Termination of Cooldown Initial Temperature 370°R

Figure A7-11. Maximum Core and Lateral Support Temperatures as a Function of Decay Power Level at Termination of Cooldown

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ABSTRACT A trade study performance evaluation of five cooldown and two thrust nulling systems were made on the NERVA NSS hot bleed cycle during the time period of March to November, 1969. Three of the five cooldown systems were tank pressure fed. They are (1) normal cooldown using on/off pulsing of the reactor, (2) independent pulsing of flow to the structural support and reflector paths, (3) the use of ullage gas with either (1) or (2). Two auxillary pumped systems were analyzed, one having parallel flow to the structural support-reflector and the other having series flow. The two thrust nulling systems were zero thrust radially directed coolant, and thrust balanced axially directed coolant.			
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