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WANL-TME-1516

30 September 1966

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NR-1 THERMAL, FLUID FLOW, NUCLEAR AND
RADIATION DESIGN
STATUS REPORT

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1.0 INTRODUCTION

This report defines the status of the NR-1 reactor thermal, fluid flow, nuclear, and radiation design and analysis programs existing on September 30, 1966.

The main features of the present NR-1 reactor design are as follows:

- 1) 5000 MW thermal power, reflector controlled core
- 2) High void fuel (37 percent void fraction, with the capability of using the 29 percent void NRX fuel)
- 3) Counterflow tie tube cluster support with variable tie tube flow capability
- 4) Beryllium-ring combined reflector system
- 5) Flight-type shield located between the support plate and core
- 6) Flight-type actuator system with ganged drum control drive
- 7) Phoebus-2 nozzle interface

This report is a compilation of pertinent thermal, fluid flow, nuclear, and radiation studies of the reactor components and analyses made subsequent to the work reported in Reference 1 on the reflector system, lateral support system, the core, the tie tube system, and the shield. Also included are discussions of the component test program and some reactor transient behavior. Reference 2 is a companion report covering the mechanical design status.

The purpose of this study is to more fully define the reactor and requirements of the reactor components and determine the best design commensurate with a simple lightweight reactor that will provide acceptable control characteristics and design flexibility. This concept is an outgrowth of the effort previously reported in References 1, 3, and 4 as well as NRX-A6 reactor design evaluations and application of design principles developed since issuing Reference 1.

The previous studies mentioned above were concerned with the design feasibility of the NR-1, the basic configuration incorporating a combined reflector system similar to the NRX-A6, and a control capability in the tie tube support system. Parametric studies of the design were reported in Reference 4 and established some desirable changes which have been incorporated, including the relocated flight shield and resized tie tubes.

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The conceptual design presented in Reference 1 includes a tie tube support system, the combined reflector design, and a preliminary evaluation of the capability of the NR-1 reactor. These studies were not comprehensive but limited to areas which affected the major design component concepts and system capabilities. The design at that time was also based on the Phoebus-2 nozzle characteristics. Previous NERVA reactor studies were primarily oriented toward use of the highest probable fuel void fraction obtainable (40 percent). However, this maximum void fuel presented development problems. Consequently, WANL selected a 37 percent void fuel as the reference design to permit maximum utilization of Phoebus technology.

The conceptual design presented in this report is based on constraints of the Phoebus-2 nozzle, a 5000 MWT level, and 4500°R chamber temperature. The core overall geometry and reflector system configuration have been held as developed in the previous studies.

The design criteria used in this study are presented in Reference 2, Section 2.0, and are in accordance with the NR-1 model specification.

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2.0 SUMMARY

The status of the NR-1 reactor subsystem thermal and fluid flow, nuclear, and radiation design analysis as of September 30, 1966 is presented in this report. A description of the basic configuration of the reactor and its components is included.

Thermal and fluid analyses made during the period covered by this report are primarily in the areas noted below:

- 1) A reactor designed to provide 5000 MW thermal power with a 4500°R chamber temperature and a life capability of 60 minutes of operation at full power.
- 2) A tie tube core support system modified to require a constant flow of 50 lb/sec regardless of tie tube coolant flow demand during reactor lifetime at full power operation, resulting in minimum perturbation in system performance.
- 3) A lifetime reactivity control capability in the tie tube support system to provide a minimum power and temperature shift with core life.
- 4) A 107-mil-diameter fuel channel with a 37 percent fuel void fraction (similar to the Phoebus-2A design) to permit increased application of Phoebus technology development.
- 5) Relocation of the shield below the support plate which provides a substantial weight savings.
- 6) A revised beryllium ring coolant distribution to increase thermal margins and reduce design sensitivity.

Nuclear analysis of NR-1 was completed to the extent that no nuclear design problems appear to exist with the NR reactor that cannot be successfully resolved. Sufficient reactivity was provided to allow for 3.0% material shimming at the design cold critical drum position. The redesign of the fuel elements to include 0.110-inch reamed diameter flow channels resulted in a 20 percent reduction in the uranium inventory relative to the original design concept, i.e., from 300 kilograms to 235 kilograms. Should other considerations such as corrosion indicate that a smaller channel diameter is desirable, further substantial savings in the inventory could be realized.

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The present reactor includes a mechanical control system capable of meeting all of the requirements for all-mechanical control. The design of such a system has indicated that an enriched boron-10 bearing control plate is preferred over the natural B_4C -Cu composition used in Phoebus-2.

The present fuel zoning scheme results in an acceptable radial power distribution throughout the reactor lifetime, using drums to compensate for a corrosion loss of 3.0% or more. Final fuel zoning and specifications will be based on further analysis and experimental results.

A significant potential for a gain in performance exists by use of the tube flow control to minimize the variation in radial power distribution with reactor life and eventually reduce the mechanical span requirement.

A technical basis exists, primarily through the NR Optimization Study of Reference 4, to consider extensive changes in the present design if mission and systems studies make these design changes attractive.

Main study areas covered in the detailed shielding and radiation analysis are as follows:

1) The weight savings gained by the use of the Inconel-718 pressure vessel are partially offset by weight penalties of 720 and 450 pounds to achieve the same off-axis tank heating as for a 1.095-inch and 0.86-inch aluminum pressure vessel, respectively. These penalties are based on the reactor configuration which does not include the flight shield.

2) A flight shield design was selected which consists of a TiH_2 -Al- B_4C shield located between the core and the core support plate.

3) A one-dimensional evaluation of the internal environment was performed which includes radial and axial gamma dose rates, fast and thermal fluxes, and gamma and neutron heating rates.

4) A two-dimensional evaluation of the external environment was performed at the control drum actuator and selected engine component locations in the vicinity of the reactor. Heating rates in the control drum actuator are higher than in any of the engine components considered except the turbopump. Therefore, special consideration will be given to assure safe operation at this component by using shielding to reduce incident radiation.

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5) The effectiveness of the shield in terms of reduction in tank heating was investigated analytically for both single and clustered module designs.

6) The effectiveness of the side shield extension in reducing propellant heating for the single module and especially for the clustered module configuration was clearly demonstrated.

7) Gross and detailed tank heating for clustered-module propellant tanks was determined for the shielded configuration.

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3.0 REACTOR DESCRIPTION

3.1 BASIC CONFIGURATION

This section describes the NERVA NR-1 reactor concept as it existed on September 30, 1966. The overall reactor configuration is shown in Figures 3-1 and 3-2. Preliminary estimates of pertinent reactor geometry and design parameters are summarized in Table 3-1. A detailed design description of the NR-1 reactor is given in Reference 2.

3.2 FLOW SYSTEM

The basic NR-1 flow system and thermal performance are shown in Figures 3-3 and 3-4. The hot bleed engine cycle is shown with estimated reactor system fluid conditions at various points of interest. The basic flow system is similar to the NERVA flow system reported in detail in Reference 1. However, one significant deviation exists in regard to the counter flow tie tube cooling scheme. This involves the flow split which permits the use of variable tie tube flow for reactivity control. Description and performance comparison of these systems is presented in Section 4.0. A preliminary steady-state flow distribution at design conditions is presented in Figure 3-5, which shows the nominal tie tube flow rate used in the performance calculations of Reference 1.

3.3 COMPONENTS

The major components of the NR-1 reactor studied in this period are the reflector system, the lateral support system, the core, and the tie tube system. The basic configurations used in the analysis of these components are shown in Figures 3-1, 3-6, and 3-7.

3.4 REACTOR INTERFACE

Pertinent reactor interface data include the reactor reflector coolant inlet (nozzle first-pass exit) conditions, requirements for the tie tube feed system, core exit conditions, and the turbine bleed-off fluid conditions. These data are shown in Figure 3-8 for the NR-1 at the 100 percent and at the 60 percent power and flow levels for a preferred system which maintains a constant support system flowrate. This system has the advantage that reactor conditions are not perturbed from beginning to end-of-life of the core. The variation in state point parameters throughout reactor lifetime that results from a varying tie tube system flow is shown in Figure 3-9.

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The preferred design uses a control valve system that maintains a fixed tie tube system flowrate. At the control valve which is outside the pressure vessel, the tie tube supply line branches off into two lines. One line is routed to the tie tubes and carries the flow required for structural cooling and reactivity compensation. The other line carries the flow difference between the total tie tube system flow and the flow going into the first line and feeds it into the reflector output plenum. Here, this flow is combined with the tie tube second pass outlet and reflector outlet coolant and is then routed to the reactor dome. With this flow system there is no noticeable effect on the reactor interface, as shown in Figure 3-8.

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TABLE 3-1

SUMMARY OF NR-1 REACTOR DATA

CORE

Power (Thermal)	5000 MW
Power Density	68 MW/ft ³
Fueled Diameter	55.8 inches
Core Diameter	57.5 inches
Length (Overall)	54.25 inches
Fuel Length	52.0 inches
Peak Loading	243 mg/cc
Uranium Inventory	235 kg
Core ΔP	155 psi
Radial Peak to Average Power	1.2
Fuel Exit Temperature	4550°R
Mixed Chamber Temperature	4500°R
Exit Gas Mass Rate	285 lb/sec
Exit Pressure	625 psia
Thrust	233,415 lb
I_{sp} Vacuum	819 sec
Lateral Support Face	7.0 psi (at assembly)

SUPPORT PLATE

Thickness	8.5 inches
Diameter	59.1 inches

SHIELD

Diameter	58.08 inches
Length	6.00 inches

FUEL

No. of Fueled Elements Full Size	4260
Partial Elements	0
Void Fraction	37 percent
Fueled Graphite Volume Fraction	63 percent
No. of Unfueled Elements	715
Geometry	19 Hole Hex.
Fuel Size	0.754 ⁺⁰ _{-.002}
Hole Size Reamed	0.110 inch
Coated	0.107 inch
Inner Web	0.0586 inch
Outer Web	0.030 inch

REFLECTOR

Control Drum Diameter	4.90 inches
Control System	24 Drums
Control Span	9.2\$
Configuration	3 Stacked Be Rings
Inside Diameter	60.16 inches
Length	52.0 inches
Be Radial Thickness	6.75 inches

OVERALL DIMENSIONS

Component Diameter	73.91 inches
Component Length	98.00 inches

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4.0 THERMAL AND FLUID FLOW ANALYSES

This section presents the NR-1 thermal and fluid flow analyses and performance calculations at full and partial power conditions. Most of the results reported are based on the full power operating condition of 5000 MW. The reactor performance was also calculated for the throttled down condition where the reactor is operated at 60 percent power and 60 percent flow at full temperature. Evaluation of the tie tube system to provide a design with desirable reactivity control has been a major effort. Some preliminary startup and shutdown analyses were performed. Thermal analyses are reported briefly for each major component. The reactor subsystem performance is based on Phoebus-2A nozzle performance and is, therefore, approximate.

4.1 REFLECTOR SYSTEM

The beryllium reflector was redesigned to provide increased thermal margins over the design as presented in Reference 1. The thermal performance of the NR-1 beryllium reflector system was determined for steady-state as well as for startup and shutdown transient conditions. A view of a typical sector of the NR-1 redesigned reflector showing hole size and location is presented in Figure 4-1. The inner diameter of the NR-1 beryllium reflector is bounded by a lateral support system which has the capability to accommodate either a minimum leakage conduction cooled seal or a fully cooled NRX or Phoebus-2 type seal configuration. However, the thermal performance reported herein applies primarily to the NRX fully cooled seal configuration.

The core bypass flow that results from a fully cooled lateral support system results in a lower mixed mean core exit temperature. It is interesting to note that the fully cooled design costs the NR-1 approximately 5 seconds in specific impulse. Every effort must be made to increase the outlet temperature and reduce flow to improve performance.

The flow distribution and bulk coolant axial temperature profiles in the beryllium reflector are shown in Figure 4-2. The reflector pressure drop is estimated at 75 psi and the coolant mixed outlet temperature is 225°R with this configuration. The heating rates used in these analyses are given in Section 6.0. A factor of 1.2 above the calculated nominal values has been used in the analysis.

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There are two primary contributors to the local heat load on the beryllium reflector in the peak heat flux location: internal heat generation within the material, and the heat conducted from the seals into the beryllium ID. Convective heat load between seals is nominally significant off-seal but results in lower local heat flux rates than for conduction on-seal. Except for flow conditions where the coolant has good convective capability and cavity leakage flow temperature levels are high, the off-seal temperature maximums are lower than the on-seal temperature maximums. Flow conditions presenting this problem can be adjusted if required.

Figure 4-3 shows the on-seal axial heat flux at the OD of the pyrographite insulation. Depending on the coolant flow rate in the seal cavity, this heat flux varies from 0.27 to 0.49 Btu/sec-in². Corresponding heat fluxes at the beryllium ID are shown in Figure 4-4 and indicate a maximum local heat flux of 0.67 Btu/sec-in² for a cooled periphery with an increase to 1.7 Btu/sec-in² maximum flux for a minimum leakage, hot periphery configuration. The difference between these two curves represents the internal heat generation of the seals.

Gross axial temperature distributions in the beryllium reflector, shown in Figure 4-5, indicate the maximum average temperature is 340°R, with a peak temperature of about 30°R above this average occurring near the inner tie bolt. The region of interest is at core midplane where the maximum average and maximum differential temperatures occur, as shown in Figure 4-6. Resulting stress analysis has shown maximum stress levels in this region for steady-state operating conditions. It is noted that the differential scale is exaggerated on the temperature difference curve. The $T_{\max} - T_{\text{avg}}$ corresponds to a compressive loading condition, whereas the $T_{\text{avg}} - T_{\min}$ represents a tensile loading condition. Detailed beryllium sector temperature distributions at Stations 0, 12, 38, and 52 inches are shown in Figures 4-7, 4-8, 4-9, and 4-10, respectively. The maximum steady-state stress occurs at Station 26; the detailed temperature distribution of the beryllium sector in this region is shown in Figure 4-11 for the on-seal condition and Figure 4-12 for the off-seal condition. A worse mechanical stress condition occurs at the on-seal station since the variations from the average temperature are greater than for the off-seal condition. In the local region of maximum stress (at the inner tie bolt), a fine-detail model was made for accurate temperature determination; these results, shown in

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Figure 4-13, were used for the calculation of the maximum steady-state stress condition reported in Reference 2. This local condition could be alleviated by some coolant distribution tailoring or use of center-cooled tie bolts.

The above calculations were performed for a seal cavity flow rate of 5.5 lb/sec. In addition, calculations were performed with other peripheral flow rates and some of these results are presented below. The beryllium coolant flow distribution and hole patterns were held constant for all of these studies. This permitted evaluation of the capability of the beryllium to accommodate any periphery without design changes or orificing of individual coolant holes within the sector. For a minimum periphery flow rate of 1.0 lb/sec, the beryllium temperature distribution is shown in Figure 4-14. The maximum-from-average temperature differential is increased by a factor of three, while the average temperature increases by 10°R to 336°R. However, these results indicate that a reduction of approximately 25 percent occurs in the minimum-to-average temperature variation from the corresponding cooled periphery case.

These conditions result from the maintenance of a fixed beryllium flow distribution. Lower thermal stress conditions can be obtained for the cooled case if the capability of minimum leakage is sacrificed.

4.1.1 Sensitivity Studies

An indication of design margin for thermal stress considerations of the NR-1 beryllium reflector is the sensitivity to three prime parameters: heating rate, coolant flow rate, and beryllium ID heat flux. That is, if low sensitivity exists over large variations in these basic parameters, good design margins are inherent. The beryllium maximum temperature as a function of heating rate is shown in Figure 4-15 and indicates a low sensitivity where a 20 percent variation from the nominal heating rate results in a 34 degree temperature variation. Corresponding variations in differential temperatures are below 10°R for the same variation in the heating rates, as shown in Figure 4-16. It is noted that the differential scale is exaggerated to provide a readable curve. The sensitivity of the maximum beryllium temperature to the seal cavity coolant flow is shown in Figure 4-17 and indicates that even for the limiting condition

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without any seal cavity flow, the beryllium temperature level is acceptable and the maximum temperature at approximately 490°R is essentially the same as for the minimum leakage case. The beryllium average and minimum temperatures are insensitive to variations in beryllium ID heat flux, as shown in Figure 4-18, although the maximum temperature level varies by 23°R for a 20 percent variation in heat flux from the design case. The beryllium temperatures are insensitive to variations in control drum annulus flow, as shown in Figure 4-19. The average and minimum temperatures are insensitive to beryllium ID heat flux as shown in Figure 4-20, whereas the maximum temperature varies by approximately 22°R for a 20 percent variation in heat flux. The significance of the effects of temperature and temperature difference variations from the cooled periphery design conditions on the calculated stresses in the beryllium reflector are reported in Reference 2.

On the basis of the sensitivity studies presented herein, the configuration of the NR-1 beryllium reflector as presently designed is considered to be acceptable from consideration of reasonable variations of heat generation, coolant flow rate, and boundary heat flux. It is noted that these variations were determined about a design condition calculated with a 1.2 heating rate factor over best estimates.

4.1.2 Transient Studies

Transient studies were performed to determine the NR-1 beryllium reflector performance during a startup and shutdown ramp of $150^{\circ}\text{R}/\text{sec}$. This is the worst transient condition except for a loss-of-flow emergency shutdown condition. Beryllium reflector temperature patterns during the $150^{\circ}\text{R}/\text{sec}$ startup shown in Figure 4-21 indicate that the worst time for tensile loading is at approximately 14 seconds. During this transient the axial location of maximum temperature differential in the beryllium, shown in Figure 4-22, indicates that the limiting core station is at the hot end. These two curves serve to localize the critical time and station during startup. However, calculated stresses actually peak just prior to this peak temperature difference time at 12 seconds. Corresponding temperature distributions are shown in Figure 4-23. The beryllium temperature at this time and location is 328°R , with minimum and maximum temperatures varying by 101°R and 135°R , respectively, from this average. The stresses corresponding to this temperature distribution, reported in Reference 2, are acceptable.

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A prime off-design operating point is the throttled-down condition of 60 percent power and 60 percent flow. The corresponding detailed temperature distribution for this condition, shown in Figure 4-24, indicates an 18°R reduction in average temperature from the corresponding full power condition. Together with the resulting reduction in maximum and minimum variations from this average, the result is a satisfactory stress condition as reported in Reference 2.

4.2 LATERAL SUPPORT SYSTEM

The NR-1 periphery design is flexible to permit installation of either a fully cooled (NRX or Phoebus-2 type) or a minimum leakage periphery. The lateral support has been analyzed with a hot buffer (NRX-A6 type) core periphery boundary condition. The gross axial temperature distribution shown in Figure 4-25 indicates a maximum fuel temperature of 4780°R . The hot buffer filler will be designed and orificed to operate at essentially fuel temperatures. The maximum seal temperature is 1550°R with a filler stave coolant outlet temperature of 1250°R and a seal cavity coolant outlet temperature of 900°R . The total periphery coolant flow was 5.5 lb/sec. The corresponding radial profile at Station 28 is shown in Figure 4-26 and the detailed temperature distribution in the midplane seal segments is shown in Figure 4-27. The stress distribution corresponding to this seal temperature distribution was reported in Reference 2 as adequate. Other seal segment distributions at Stations 0, 17, 34, and 52 are shown in Figures 4-28, 4-29, 4-30, and 4-31, respectively; these curves confirm that the maximum temperatures and temperature differentials and, consequently, the stresses occur near Station 34.

The axial temperature distribution and the radial temperature profile at Station 28 are shown for the minimum leakage periphery in Figures 4-32 and 4-33. The seal temperatures run considerably higher than for the cooled periphery, as shown in Figure 4-34, where the midplane seal has a maximum temperature of 3454°R . There is no insulating tile between the seal and the fillers for approximately the first 30 inches of core length, where seal temperatures exceed filler temperatures. Beyond this axial station, however, insulation will be placed inboard of the seals to isolate them from the hotter core and reduce the heat leakage from the core. Seal temperatures could be tailored as desired by placing insulation at the beryllium ring ID.

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4.3 CORE

The core axial temperature profiles at full power design conditions for the maximum power fuel channels are shown in Figure 4-35. These temperature profiles are for design condition operation at the orifice drum angle. The nominal fuel channel coolant outlet temperature is shown as 4550°R consistent with the Phoebus-2 nozzle chamber temperature of 4500°R. The 50-degree difference allows for the diluent effect of the peripheral core bypass flow. The corresponding pressure profiles shown in Figure 4-36 indicate a 155 psi core pressure drop and 625 psi chamber pressure. Pressure profiles are shown for three power factors to represent the orificing requirement as a function of the spread in NR-1 heat generation. Depending on the core loading, power factors as low as 0.6 may be experienced. A preliminary estimate of the interstitial pressure distribution in the core central region is also indicated on this curve, based on tapered gaps varying a factor of 4 from the inlet to the outlet (0.2 mils at the inlet to 0.8 mils at the outlet end of the core). The temperature profiles shown in Figure 4-37 for the throttled down condition at 60 percent power and 60 percent flow are similar to those shown for the full power case. The pressure distribution shown in Figure 4-38 indicates a core pressure drop of 100 psi with the profiles shown for three different power factor effects. The variation in the nominal maximum fuel temperature at full power and flow shown in Figure 4-39 indicates a 57°R variation for power factors from 0.8 to 1.2 with a gas exit temperature of 4550°R (as-orificed nominal value). This temperature variation could only be maintained by use of some flow control system, etc., which would yield no change in power factor with drum angle or reactivity loss.

There are no meaningful bore weight loss corrosion calculations at this time for the NR-1 fuel. WANL is in the process of developing high void fuel elements and these will be tested during the forthcoming year to determine the corrosion characteristics of the NR-1 fuel.

Preliminary estimates have been made of the external surface corrosion of the fuel elements. These calculations consider only interstitial flow without any pinholing (i.e., no communication between bore and interstices). It should be recognized that these calculations were based on NRX technology and empirical correlations. The results shown in Figure 4-40 indicate a nominal interstitial corrosion rate of 0.035 mils per minute which would result in about 2 mils corrosion for a 1-hour run at NR-1 chamber temperature.

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4.4 TIE TUBES

Studies have been performed to determine the tie tube configuration effects on reactivity and to provide some indication of the reactivity range obtainable with reasonable flow rate ranges. A tie tube and sleeve geometry offering the optimum reactivity control capability has not been determined. A flow control capability has been considered desirable since it permits a significant reduction in radial temperature variation with core life. The potential for temperature flattening is shown in Figure 4-41 for an NR-1 reactor with flow control and an NR-1 reactor with drum control. This curve indicates that, instead of the rise of approximately 1300°R in peripheral fuel temperatures that accompanies outward drum movement, a decrease of approximately 200°R would occur with tie tube flow reactivity compensation. Theoretically, a combination of drum movement and flow control could result in a completely flat radial temperature profile throughout the core operating lifetime.

To maximize the tie tube hydrogen reactivity worth, it is necessary to maximize the first pass of the tie tube volume. Studies have been made considering that overall liner size is constrained by structural considerations of the unfueled element and the support block, and that the second-pass tie tube pressure drop must be compatible with the reflector/support system. The resulting tie tube design is compared in Figure 4-42 with the Reference 1 design which was selected on the basis of a minimum flow impedance geometry.

The characteristics of the selected tie tube configurations with various amounts of exit end orificing are shown in Figure 4-43 for full-power conditions. The orificing is identified on the curves by an equivalent loss coefficient representing the velocity head loss. On this curve, the Reference 1 design represents a variation in tie tube hydrogen reactivity of approximately 1.0\$ for tie tube flow variation between 12 and 50 lb/sec. As noted, considerably larger reactivity variations exist with the revised geometry. The lower limit on tie tube flow is established by allowable structural material temperatures, while the upper limit is based on nozzle tube temperature limitations as established by the amount of bypass flow permitted. It is noted that as outlet orifice size is decreased (i. e., the loss coefficient is increased), the pressure in the tie tube system increases with a corresponding increase in hydrogen density, resulting in greater reactivity worth of the hydrogen for a given flow rate. Preliminary

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calculations indicate that the tie tube coolant temperature rises 30 degrees through the plenums and piping prior to arriving at Core Station 0 because of a counterflow heat exchange effect. Design studies are in progress to isolate the counterflow effect of the second pass on the first pass in the tubing immediately above the core. This would further increase the reactivity, but would reduce the sensitivity to flow rate variation somewhat. The reference tie tube design provides a tie tube reactivity variation or control capability of approximately 4.0\$ at the full power condition. The mechanical design implications of this configuration of the tie tube are discussed in Reference 2.

The characteristics of this selected tie tube configuration at the throttled condition of 60 percent power and 60 percent flow are shown in Figure 4-44. It is noted that at this condition the tie tube reactivity range is decreased by approximately 0.5\$ from the full power condition for the same flow range of 10 to 50 lb/sec. However, at the throttled condition the available pressure drop will not provide the same tie tube flow range unless further design changes are made (such as insertion of a control valve in the main propellant line downstream of the tie tube system bleedoff). With the configuration shown it is estimated that the maximum attainable tie tube flow at the 60 percent power and flow condition is approximately 30 lb/sec.

The implications of the tie tube flow variation at the full power design condition on the steady-state point data are shown in Figure 3-8. These data are based on the constant tie tube flow system feed of 50 lb/sec, with the difference between this flow rate and the tie tube reactivity control coolant being bypassed into the reflector outlet plenum. It is noted that a minimum effect on the reactor system results in this case as compared to that of the alternate bypass system shown in Figure 3-9. This latter system has the disadvantage that nozzle flow rate varies at any operating point as a function of core reactivity life remaining.

An analog computer study was made of the dynamics characteristics of a support system with and without the bypass and included an analysis of controllability and engine system performance during startup, maneuvering, and shutdown.* Although limited in scope, this parametric study revealed that dynamics differences between the systems with and without a bypass were of secondary importance and hence the addition of a bypass would not create dynamics problems.

* WANL-TME-1542, Preliminary NERVA System Analysis, to be issued.

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4.5 REACTOR SUBSYSTEM PERFORMANCE

A limited study of the overall reactor subsystem performance has been made using the redesigned tie tube system, and is restricted to determination of steady-state control and operating limits. Reactor system fluid and thermal performance with a wide open (maximum) tie tube control valve position has been evaluated at several steady-state operating conditions with the current configuration. The maximum open tie tube control valve produces the maximum tie tube flow and feedback reactivity for a given chamber temperature and pressure condition. The data for these steady-state conditions are presented in the form of a general reactor operating map for some of the major reactor parameters in Figure 4-45.

One of the prime reactor operating limits which bound the operating map is shutdown reactivity. The 2.0\$ minimum shutdown reactivity is shown on the map for the wide open tie tube control valve position for maximum tie tube flow capability (70 lb/sec). This represents the maximum reactivity condition. Reactivity shutdown at the design point with a minimum of 13 lb/sec tie tube flow would be 7.0\$ for this operating condition. Considerable additional operating envelope is available within the 2.0\$ shutdown limit as the tie tube flow is reduced. This curve is based on start-of-life reactor conditions (no corrosion) and the assumption that the control drum reactivity worth is 9.0\$. Corrosion effects would increase the shutdown capability at any operating point.

Since this analysis has not been conducted for the area where the tie tube flow falls below the minimum required flow for full power and temperature conditions, the tie tube limiting temperature was not reached at any of the steady-state points analyzed.

The tie tube flow rate and tie tube reactivity feedback are shown in Figure 4-46. These flow rates and tie tube feedback data are based on a wide open tie tube control valve; therefore, the data represent maximum tie tube feedback and tie tube flow rates.

The TNT analytical model used for these calculations has been refined to include the effect of all major NR-1 tie tube system effects. Studies have been initiated in this area.

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4.6 NR-1 COMPONENT TEST PROGRAM

The component test program supporting the development of the NR-1 reactor has been defined on the basis of the conceptual configuration. This effort was coordinated within the Reactor Engineering Department and is reported in Reference 5.

The data requirements necessary to design and predict the performance of the reactor covered such areas as coolant leakage rates, thermal junction impedances, flow impedance, temperature and pressure gradients, and thermo-physical and chemical effects.

Component test information needs were identified as follows:

- 1) Design of flow impedances in the tie tube system, shield, fuel element orifices, lateral support seal system, plunger pin capsules, control drum annulus, and reflector orifices.
- 2) Seal leakages in the lateral support spring caps, seal segments, and plunger pin capsules.
- 3) Performance of the reflector ring; the seals and thermal baffle; the nozzle interface; the shield, reflector, and support plate seals; and the tie tube plena system.
- 4) Thermal junction impedances between the various lateral support materials as well as for the thermal baffle.
- 5) Effects of temperature gradients within the lateral support system.
- 6) Corrosion testing of all graphitic materials subject to hydrogen attack which affect the life of the reactor as well as development of tests which will lead to a more complete understanding of the corrosion phenomenon.

Reference 5 includes a description of these as well as other Reactor Engineering Department tests, specific test definitions, and the estimated period of time for completion of the tests.

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4.7 REACTOR TRANSIENT BEHAVIOR

The characteristics of the NR-1 reactor during transient periods of startup and shutdown were investigated, using a typical $150^{\circ}\text{R}/\text{sec}$ startup ramp and $-150^{\circ}\text{R}/\text{sec}$ shutdown ramp. This consists of a $150^{\circ}\text{R}/\text{sec}$ temperature ramp to 60 percent design flow and full temperature and then continuing with a $4.5 \text{ lb}/\text{sec}^2$ fixed flow ramp to full power. The shutdown ramp consisted of ramping the flow down at $-4.5 \text{ lb}/\text{sec}^2$ from the design conditions. When the flow dropped to 60 percent of design flow, the chamber temperature was ramped at $-150^{\circ}\text{R}/\text{sec}$ down to 2500°R . At this point the reactor was scrammed. The startup and shutdown ramps provided information on the characteristics of the NR-1 during programmed transient operation. These conditions do not reveal the essential characteristics to be expected during emergency operation and their effect upon the reactor system.

The analytical model used in this analysis was based on the NR-1 geometry and heating rates as presented in Reference 1 and includes the tie tube system. This geometry did not include the relocated shield or the increased tie tube diameter which resulted from consideration of additional full control capability. However, this model does provide a reasonable estimate of the characteristics of the NR-1 during transient operation. Results of these studies are shown in Figure 4-47 through 4-52.

The flow rate, power, and chamber temperature profiles for the startup are shown in Figure 4-47 and for the shutdown in Figure 4-50. The resulting tie tube temperature and flow characteristics are shown in Figures 4-48 and 4-51, and the control drum motion and reactivity feedback are shown in Figures 4-49 and 4-52. These calculations utilized a fixed tie tube valve position. The tie tube temperature and flow characteristics are shown for two tie tube valve positions which correspond to 13 and 32 lb/sec tie tube flow at design conditions. Control drum motion and reactivity feedback are only shown for a tie tube valve position which corresponds to 13 lb/sec at design conditions.

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5.0 NUCLEAR ANALYSIS

This section summarizes the nuclear design and analysis of the NR-1 reactor. The nuclear and pertinent physical characteristics are presented and discussed.

5.1 COMPONENT GEOMETRY AND COMPOSITION

The present NR-1 reactor includes three areas of redesign (relative to the conceptual design presented in Reference 1) which have major implications in the neutronics analysis.

The presently defined fuel element includes 0.110-inch diameter reamed coolant channels. The increase in fueled graphite volume fraction permits a net decrease in total uranium inventory (relative to the 0.115-inch design) and a corresponding reduction in zone loadings. Partial fuel elements (with fewer than 19 channels) have been eliminated in the current design.

The present counterflow tie tubes permit the stored hydrogen mass in the core (and hence reactivity) to vary over a range wide enough to compensate for the estimated corrosion loss and allow start-of-life shimming.

The control vane mean diameter is now 4.90 inches; this reduction from the original conceptual design value of 5.31 inches has led to a reevaluation of the desired control plate material.

These areas are discussed in detail in the following sections. A summary description of important nuclear design parameters is shown in Table 5-1.

5.2 METHOD OF ANALYSIS

The method used in the present analysis of the NR-1 reactor is essentially the revised Nuclear Standard Design Method, NSDM-II. A comprehensive description of the method is given in Reference 6. Primary features of the method are an accurate forty-group determination of the reactor spectrum, a reweighting scheme which reduces the group representation to nine, and a nine-group two-dimensional code, EXTERMINATOR, which utilizes a full up and down scattering matrix.

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TABLE 5-1

SUMMARY DESCRIPTION OF NR-1 REACTOR

Mean Fuel Diameter 55.8 inches
Reamed Fuel Channel Diameter 0.110 inch
Uranium Inventory 235.00 kg

<u>Fuel Zone</u>	<u>Number of Fuel Elements</u>	<u>Uranium Loading (mg/cc of Fueled Graph)</u>
1	1038	178
2	2532	243
3	222	176
4	228	125
5	240	77

Approximate Cold Critical
Drum Position 115 degrees
Outer Reflector Thickness 6.75 inches
Number of Drums 24
Control Vane Mean Diameter 4.90 inches
Control Drum Span 9.25

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Basically, three computer codes are used in implementing NSDM-II. Forty-group cross sections (20 fast, greater than 1.86 ev, and 20 thermal) are calculated with the combined GAM-TNS cross-section generating code. The one-dimensional transport theory code DTK is run, using the GAM-TNS cross sections, and the resulting forty-group spatially dependent fluxes stored on tape. The R, θ or R, Z diffusion theory code, EXTERMINATOR, as well as the radial and axial DTK's, use nine-group cross sections (to conserve computer cost) weighted over the forty-group spectrum available on tape.

Extensive use was made of both the DTK and EXTERMINATOR codes. EXTERMINATOR was used to determine the basic room temperature reactivity, control drum span, and R, θ power distributions, including azimuthal variations and power distribution changes with control drum movement. Radial DTK calculations were performed to determine the cold-to-hot reactivity swing which includes hydrogen coolant worth, the Doppler and thermal base effects, and the radial component of expansion. Axial DTK calculations provided the axial component of the core expansion effect as well as the coolant worth profile and axial power distribution.

In performing the preliminary nuclear design calculations, it was not considered necessary to generate forty-group cross sections of the several core and lateral support and reflector regions. Rather, the NRX spectra were used as weighting functions to generate NR-1 cross sections in conjunction with the proper NR-1 atom densities. An examination of Figures 5-2 and 5-3 in Reference 6 reveals that the broad group cross sections are relatively constant over most of the core and rise slightly near the reflector. It was therefore assumed that the difference in energy spectrum between NR-1 and NRX at a given relative distance^{*} from the reflector would be less than the variation across the NRX core, and thus the NRX spectra would yield adequate nine-group cross sections for this phase of the NR-1 analysis.

In order to verify the assumed similarity of NR-1 and NRX spectra, the specific procedure outlined in NSDM-II has recently been applied to the NR-1 reactor. Forty-group

* Radial position divided by the core radius.

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cross sections were generated for the core, filler strips, and lateral support and reflector regions. The cross sections were then used in a radial DTK, and the resulting forty-group fluxes stored on tape. The same atom densities were again used to generate cross sections in a nine-group scheme, using the stored fluxes as weighting functions. The resulting eigenvalue calculation was compared in detail to one using NRX spectrally weighted cross sections. The excellent agreement of all quantities compared validates the assumptions used in the NR-1 calculations as outlined above. In particular, eigenvalues, absorption rates, median energies of fission, and radial power distributions were compared. The calculated reactivity with the NR-1 spectrum was 0.30\$ higher than the preliminary result.

Figure 5-1 shows radial fission density traverses for the two calculations. In both calculations a boron ring (ring cross sections were identical) was used to simulate the control drums at 90 degrees.

Figure 5-2 shows the integrated fraction of fissions as a function of energy in the nine-group scheme. The results of the two calculations are compared in the central and outermost fuel regions, and the agreement is good over the entire core.

5.3 REACTIVITY

5.3.1 Room Temperature

Room temperature reactivity is primarily a problem of specification of the uranium inventory. Sufficient reactivity must be provided to attain criticality at the desired cold critical drum position and to allow for material shimming of the reactor. In the NERVA reactors the shimming procedure makes use of tantalum carbide loaded unfueled elements which place a specific amount of negative reactivity in the core. In the NR-1 design, a value of -3.0\$ was selected for the shim content. This allows for a reactivity increase of up to 3.0\$ by replacement of part or all of the poisoned central elements with regular graphite elements, thus providing flexibility for minor design changes. Negative reactivity is added simply by increasing the initial number of poisoned unfueled elements.

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LASL measurements of tantalum in the Phoebus Honeycomb experiment give a worth of $-0.40\$/\text{kg}$ for 0.002-inch thick foils. If this worth is assumed together with a tantalum loading of 70 grams per element, approximately 100 elements are required to provide the -3.0% . This is a small fraction of the core complement of 715 unfueled elements.

The other factors which affect the selection of the cold critical drum angle are the desired full power start-of-life drum position (hot critical), the cold-to-hot reactivity swing, and the differential control drum span. For the purposes of analysis a hot critical drum angle of 90 degrees was assumed; with the cold-to-hot swing and drum span, this indicated a cold critical drum position of 115 to 120 degrees.

The material composition of the reactor itself along with the above considerations determines the necessary uranium inventory. The present NR-1 fuel element, i.e., 0.110-inch diameter reamed channels, allowed a reduction in the total uranium inventory of about 65 kilograms relative to the 0.115-inch channels. The present inventory shown in Table 5-1 is 235 kilograms. One particular core parameter which can vary and has a strong effect on the uranium inventory is the niobium coating mass per element. The NR-1 design includes 1.5-mil niobium coatings or the equivalent of about 70 grams per element and a total core inventory of 294 kilograms (307 kilograms for the 115-mil design). A change in the niobium content would affect the uranium inventory at about a 2.5 to 1 ratio; that is, a 2.5-kilogram increase in niobium inventory requires a compensating increase of approximately 1 kilogram of uranium.

5.3.2 Cold-to-Hot Reactivity Change

The cold-to-hot reactivity swing is the net reactivity change in the reactor with the introduction of coolant hydrogen and increase in temperature to the full power, full flow condition. A summary of the physical characteristics at full power and the resulting reactivity effects is presented in Table 5-2. As shown in Table 5-2, the sum of the negative reactivity effects is over-compensated by the positive worth of the coolant, resulting in a net reactivity increase of $\sim 2\%$ at the minimum tie tube coolant flow rate.

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TABLE 5-2
NR-1 DESIGN
SUMMARY OF COLD-TO-HOT REACTIVITY CHANGES

<u>Hydrogen Reactivity Effects</u>		
	<u>Hydrogen Mass (kg)</u>	<u>Reactivity Worth (\$)</u>
Core at Full Power	1.1 (Fuel Channels)	+2.86
	0.6 (Tie Tubes at	
	13.1 lb/sec	+2.25
Lateral Support	0.3	} +0.36
Reflector	1.9	
Subtotal		+5.47

<u>Temperature Reactivity Effects</u>		
	<u>Temperature (°K)</u>	<u>Reactivity Worth (\$)</u>
Core	2000	
Doppler and Thermal Base		-1.77
Expansion		-1.58
Lateral Support	600	} -0.14
Reflector	175	
Subtotal		-3.49
Net Cold-to-Hot Change		+1.98

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Determination of the coolant reactivity worth involves several calculations. First, the worth of a kilogram of hydrogen distributed uniformly over the core volume is calculated. This calculation, performed with the composition shown in Table 5-1, yielded a value of 4.70\$ per kilogram; the control drums were represented at 90 degrees by a boron ring. As a check on the method the Phoebus Zepo experiment of hydrogen worth was calculated, and the calculated result was 4.2\$ per kilogram versus a measured value of 4.3\$ per kilogram.

Because the axial density of hydrogen in the core is not uniform, it is necessary to develop an axial worth distribution for hydrogen. The calculated hydrogen worth profile for NR-1 is shown in Figure 5-3. Separate calculations were performed with hydrogen in each of the six regions. The resulting reactivity changes were then compared to a base case containing no hydrogen and converted to the dollars/kilogram values shown in Figure 5-3. These calculations were performed with the DTK code and used room temperature cross sections based on smeared-core atom densities.

The actual hydrogen density profiles for the fuel element flow and the first- and second-pass tie tube flow are shown in Figure 5-4.

The effective density ρ_{eff} is given by

$$\rho_{\text{eff}} = \frac{\sum_i \rho_i \text{RC}_i \Delta Z_i}{\sum_i \text{RC}_i \Delta Z_i},$$

where

ρ_i is the hydrogen density at axial station (i)

RC_i is the hydrogen reactivity coefficient at axial station (i), and

ΔZ_i is the symmetric interval about axial station (i).

The effective hydrogen masses thus obtained are 698 grams in the fuel element channels and 475 grams in the tie tubes versus the actual masses of 1.1 kilograms and 600 grams, respectively.

As shown in Table 5-2, the nominal tie tube flow being considered is 13 lb/sec. This is the minimum flow required to adequately cool the tie tubes, which allows sufficient

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variation in the reactivity associated with the tie tube to employ tube flow as a corrosion compensating mechanism. This topic is treated in a later section.

The reactivity contribution of hydrogen in the lateral support and reflector were obtained from radial DTK calculations normalized to the LASL measured coefficients in these regions. The contribution of hydrogen in these regions has been shown analytically and experimentally to be a second-order effect compared to hydrogen in the core, and the detailed procedure outlined above was considered unnecessary to account for axial distribution.

The temperature effects can be treated in two distinct categories. First, there is the direct change in the nuclear properties, i. e., cross sections, due to a change in temperature; these are the Doppler and thermal base effects. Second, the change in temperature, primarily the increase in core temperature, affects the composition and hence the nuclear characteristics of the system.

The Doppler and thermal base effects were determined directly with radial DTK calculations. The temperatures shown in Table 5-2 were input to the cross-section generating code for the various regions, and these values were used in turn to calculate eigenvalues. The core effect was calculated separately; however, the effect of regions external to the core were combined in a single calculation. It should be noted that this series of calculations was performed with the core composition defined by 0.115-inch reamed channels and is therefore not an exact value for the 0.110-inch design. A review of earlier parametric studies (Reference 7) indicates that the temperature effect in the core increases with decreasing void fraction, with all other parameters (core diameter, reflector thickness, etc.) held constant. It is estimated from this that the increase would be less than 10 percent of that reported in Table 5-2. Thus, final temperature coefficient calculations for the present NR-1 design may yield a net cold-to-hot swing slightly less positive than the one shown in this report.

To determine the effects of expansion on reactivity, a series of axial and radial DTK calculations are required. Using temperature profiles derived in the thermal analysis, the increased core volume and, hence, decreased core atom densities and resulting cross sections were determined.

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To determine the radial component of the expansion effect, a radial calculation was run with expanded core geometry and cross sections. Suppression of any effect in the axial direction in this calculation ensures that the true radial component results. This is accomplished simply by changing the axial buckling B_z^2 such that $(DB_z^2)_0 = (DB_z^2)_1$, where $(DB_z^2)_0$ is the axial leakage in the base case, and $(DB_z^2)_1$ is the axial leakage in the expanded case. The axial component of the leakage is determined by the same procedure except that in the expanded axial calculation the radial effect is suppressed. The sum of the radial and axial components is taken to be the total leakage effect, which is the value given in Table 5-2.

COHOT coefficients for estimating the reactivity changes of NR-1 over the operating map may be readily derived from the above results. These coefficients are integrated with the TNT code so that the reactivity is determined automatically for each thermal state point investigated.

The reactivity effect of the introduction of the propellant is calculated by dividing the core into six axial regions. Each region is assigned a hydrogen worth according to the average defined by the axial H_2 worth profile, Figure 5-3. Region width increments of 4.11 inches were chosen to facilitate incorporation in TNT, and the number of increments per region was chosen to equalize the total H_2 worth per region. Table 5-3 summarizes the selections. The coefficients are normalized to yield a uniform H_2 worth of 4.7\$/kg, as calculated in the radial DTK problem described previously.

TABLE 5-3
AXIAL HYDROGEN WORTH OF COHOT

<u>Region Number</u>	<u>Number of Increments</u>	<u>Lower Axial Station (Inch)</u>	<u>Region Average H_2 Worth (\$/kg)</u>
1 inlet	1	4.1	0.43
2	2	12.3	3.2
3	2	20.6	6.3
4	2	28.8	7.8
5	2	37.0	7.5
6 outlet	4	53.5	2.8

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Temperature coefficients are obtained directly from the full power cold-to-hot reactivity calculations. The results are $0.00066\$/^{\circ}\text{R}$ for increases in average Be reflector temperature, and $0.00109\$/^{\circ}\text{R}$ for increases in average core temperature. The core coefficient includes Doppler effect, core expansion, and shift in thermal base.

The minor hydrogen reactivity effects, estimated from a combination of data and calculations, are as follows:

- 1 $\$/\text{kg}$ in the lateral support
- 0.05 $\$/\text{kg}$ in the Be reflector
- 0.05 $\$/\text{kg}$ in the chamber region, and
- 0 worth in the core inlet plena.

The zero plena H_2 worth results from locating the shield below the support plate, which decouples the regions above it from the core.

5.4 CONTROL DRUM SPAN

The discussion of control drum span involves two areas. First, the control span requirements are developed for an all-mechanical control mode of operation. The second area deals with a control plate material selection study performed in support of the design effort to provide the necessary control margin.

Several factors must be considered in an evaluation of the control span requirement. As a basis for such an evaluation, the requirements of the control drums must be specifically known. In the NRX reactor series, for example, reactivity span provided by rotating drums must be sufficient to control the reactor through the initial criticality, the full power startup, and the full power duration test. From any programmed position in this cycle, the control drums must also be capable of making the system subcritical by a specified margin. These conditions were included in the design of the NR-1 control drum span, along with an additional requirement. In the NR-1 design, the control drums must also provide sufficient variation to enable a partial-power, full-temperature test to be performed at some point in the test cycle. This power throttling (60 percent of NR-1 design power) was included in the design of the NR-1 control system.

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The design of the counterflow tie tubes provides for the potential use of hydrogen flow variation to compensate for part or all of the reactivity loss due to core corrosion during the full-power run. If such a mechanism is employed, the requirements on the mechanical control system are reduced. This effect is treated in detail under the section on counterflow tie tubes.

The primary criterion for the mechanical control span in the NR-1 is, however, that the design must be adequate for an all-mechanical control mode. Results of the early NR-1 test series should permit incorporation of flow control for later NR-1 units and a reduction in mechanical span.

Figure 5-5 illustrates the control requirement in NR-1. The various factors which affect the control drum requirement are represented by the length of lines on the one-dollar unit scale. The total mechanical span requirement is 7.0\$ or the sum of the safety margin (2.0\$), the corrosion loss estimate (3.0\$), and the 60 percent power throttling (2.0\$). To this must be added an uncertainty margin which is estimated for this analysis as 1.5\$. Two important considerations, implicit in this total span requirement, should be emphasized. First is the 60 percent throttling factor which adds a 2.0\$ requirement only if this test is performed at the end of the full-power run. If performed at the beginning or up to a corrosion loss of 1.0\$, no additional span would be required over the 5.0\$ total required for safety and corrosion. Since no definite sequence has been established, the part-power test was assumed to occur at the end-of-life, thus ensuring a conservative value for the total mechanical span requirement. The second factor to be noted is that the cold-to-hot reactivity swing is positive and less than the corrosion loss estimate, and does not explicitly add to the total requirement in an all-mechanical control mode.

In parallel with the anticipated total span must be a control drum design capable of providing the required amount of control. The original material considered for NR-1 application was the standard enriched boron-aluminum composition used in the NRX and KIWI reactors. These plates were 50 mils thick and had a boron-10 surface density of $\sim 0.065 \text{ gm/cm}^2$. In the present NR-1 reflector-drum mechanical design, these plates provide a control span of $\sim 9.4\%$.

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However, a desire for a material with higher temperature capability as well as a potentially lower cost led the Los Alamos Scientific Laboratory to select natural boron carbide-copper as the material for the Phoebus-2 control plates. This material has a considerably lower boron-10 surface density (less than 0.020 gm/cm^2 at a 50-mil thickness) which necessitated doubling the thickness to 100 mils to ensure a control span of 8.0 to 9.0\$.

Even with the advantages of the LASL material, its use in NR-1 is questionable because of the more stringent neutronic design requirements. An investigation of the effect of reduced boron-10 surface density on the NR-1 control span was made using R,Θ EXTERMINATOR calculations. Figure 5-6 shows the calculated effect of reducing boron-10 surface density from the NRX to the LASL value. LASL experimental data, also shown, indicate the calculated results to be substantially correct. A loss of approximately 1.0\$ is shown as the result of the boron-10 surface density reduction. An additional 0.20\$ is lost with the LASL composition because of the increase in plate thickness from 50 to 100 mils.*

In light of the present control requirement, it is considered undesirable to reduce the boron-10 content to the LASL level. A design goal density of 0.065 gm/cm^2 was selected, and various materials were examined which would provide the required amount of boron-10.

Table 5-4 summarizes the various compositions along with the nuclear, radiation, and thermal effects. All of the natural B_4C materials require substantial increases in the plate thickness to obtain the desired boron-10 density. The reduction in mean vane diameter results in the reduced spans shown in the table. In order to guarantee that the NR-1 control system will not be marginal, it was concluded that an enriched boron-10 bearing plate should be selected as a tentative NR-1 poison plate material.

The NR-1 integral control drum span versus control drum position is shown in Figure 5-7.

* Because of thermal and mechanical constraints in the compact lateral support spring-reflector structure, the drum hole size cannot be increased. Thus, for constant clearance between the vane OD and wall of the drum hole, the effective vane diameter is reduced in the 100-mil thick plates and the drum span worth is thereby further reduced from 8.4\$ to 8.2\$.

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TABLE 5-4

CONTROL VANE MATERIAL SUMMARY

Vane Composition	Thickness (mils)	B ¹⁰ Density (gm/cm ²)	Drum Span (\$)	Vane Heat Rate (watts/cc)	Deposit in Vane (watts/cc)	Deposit in Ref. (watts/cc)	Surface Vane Temp. (°R)	Max. Vane Temp. (°R)
Boron in Aluminum [*] (22 w/o)	50	0.065	9.4	3130	2600	2.8	503	504
Boron in Copper (4 w/o)	100	0.065	9.2	3560	2740	4.3	520	521
Nat. B ₄ C-Cu (40 v/o)	178	0.065	8.9	1040	840	3.3	549	551
Nat. B ₄ C-Al (40 v/o)	178	0.065	8.9	900	770	2.4	521	524
Nat. B ₄ C-Cu (30 v/o)	237	0.065	8.7	820	700	3.1	584	586
Nat. B ₄ C-Al (30 v/o)	237	0.065	8.7	690	590	2.4	527	530

* Boron means 92 percent enriched in boron-10

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5.5 POWER DISTRIBUTION

This section describes the power distribution characteristics of the NR-1 reactor. Radial, axial, and azimuthal fission density curves provide a three-dimensional synthesis of the gross power distribution over the entire NR-1 core. A detailed power distribution, giving channel by channel power factors as required for orificing of the core, was not derived. This final phase of the power distribution analysis will require a complete description of the reactor fuel zoning scheme. In the NR-1, final fuel zoning will be based on the results of the NR-1 mockup in the Flexible Critical Experiment at the Waltz Mills Facility. The derived final loading scheme will be used to produce fuel for the NERVA Critical Experiment (NCX-the counterpart of PAX in the NRX series) in which accurate room temperature fission density distribution measurements will be performed. The power factor specification procedure then combines the PFENCE code with the experimental results to produce channel by channel power factors at a specified hot orificing drum position.

Three factors have been considered in the specification of the few (five) zone loading scheme described in Table 5-1; these are the initial full-power drum position (hot critical), the expected end-of-life drum position as determined by the corrosion loss, and the thermal design constraints on the power factors. For the sake of a complete and consistent analysis, a hot critical drum position of 90 degrees was chosen, and other parameters (loadings, cold critical drum position) were evaluated relative to the control drum position.

The corrosion characteristics of NR-1 have not been defined. A significant amount of corrosion furnace test data is not available on the high void fraction (0.110-inch channel) fuel, and the reactor operating experience is solely with smaller channel diameters and lower power densities. A corrosion loss of 3.0% was assumed for the NR-1 analysis, which is of the same magnitude as experienced in the NRX-A4 and NRX-A5 tests.

Consistent with the thermal design constraints, the estimated NR-1 fuel zone loading scheme has been chosen to produce a radial fission density distribution with a peak to average ≤ 1.20 over the anticipated operating drum angle range.

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It should be emphasized that the five-zone loading scheme presented in Table 5-1 is in no way intended to imply a complete description either from the number or magnitude of the individual loadings.

Figure 5-8 shows the radial fission density distribution for the reactor at room temperature based on the five-zone loadings described. This distribution represents an average azimuthal position from the standpoint of power distribution. In Figure 5-9 the variation in fission density as a function of core azimuth is shown for various drum angles. The effect of full-power conditions on the radial power distribution is given in Figure 5-10. The R, θ hot power distribution at the 90-degree position would therefore be the product of the results of Figures 5-8, 5-9, and 5-10.

The effect of control drum position, i.e., rotation outward from the hot critical angle of 90 degrees, is shown in Figure 5-11. These results show the change in distribution for a 3.5\$ reactivity insertion. The final drum position is determined by the differential drum span over this interval. Two-dimensional control drum calculations at 0, 90, 130, and 180 degrees indicate that the change in fission density at any given radius is effectively linear with reactivity insertion. Thus, the changes illustrated in Figure 5-11 would apply to any 3.5\$ change in drum position even though the starting position and total drum swing may differ because of the variation in differential span over different intervals. As shown in Figure 5-11, the preliminary five-zone fuel loading meets the design constraint on maximum-to-average radial fission density. It may be possible to achieve a lower maximum value by increasing the number of loading zones.

Some number of zones greater than five will be required in the final fuel specification. These will be necessary to adequately zone-in the periphery of the core; the irregular core boundary and scalloping were not factored into the few zone loadings presented in this report.

An important conclusion of the NR-1 power distribution studies is that the gradients as calculated in the outermost elements are not any larger in the NR-1 than in the NRX reactors. Similar calculations were compared, specifically radial DTK's with ring prescriptions

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to simulate the drums at 90 degrees. The gradient is defined as the ratio of the peak fission density to the average in a radial increment representing one fuel element width.

The axial power distribution is shown in Figure 5-12. Although the present NR-1 configuration places the bulk of the shield material below the support plate, the cluster plate is retained, and the resulting axial fission density distribution is essentially unchanged relative to the original design which had the support plate aft of the shield.

5.6 COUNTERFLOW TIE TUBE

The counterflow tie tubes in the present NR-1 reactor were designed to permit a large variation in the stored mass of core hydrogen. Figure 5-13 shows the stored mass of hydrogen per tie tube versus the mass flow rate in the tie tubes. The right-hand ordinate gives the reactivity associated with a full core complement of the tie tubes. As discussed under the cold-to-hot reactivity changes, the nominal start-of-life flow rate would be about 13 pounds per second. With the present estimate of corrosion, the flow would have to increase to about 35 to 40 pounds per second to provide a +3.0\$ change in the reactivity associated with the tie tube hydrogen. The original conceptual tie tube design, shown at the bottom of Figure 5-13, had considerably less capability as a corrosion compensating mechanism although it could be employed for start-of-life shimming.

Two significant performance gains are potentially available with the counterflow tie tube design.

First, there is the possible reduction in required mechanical control drum span. Referring back to Figure 5-5, if the corrosion factor were removed, the power throttling component would be simultaneous with the cold-to-hot swing component. This would reduce the total span reactivity required to 4.5\$ plus uncertainty, which would allow a reduction in the reflector thickness and hence in the total reactor subsystem weight.

The second performance gain is realized in the radial power distribution. Figure 5-14 shows the change in radial power distribution for a 3.5\$ reactivity insertion either by rotation of the drums outward from the start-of-life position or by increasing hydrogen flow in the counterflow tie tubes. In absolute magnitude, the tie tube flow mode of corrosion

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compensation results in approximately one-fourth the radial power distribution change as does drum rotation for the comparable amount of reactivity insertion. Thus, the radial temperature distribution would not have as large a variation, particularly at the core periphery, as the present drum controlled reactors. Because the effects are opposite in sign (drums increasing edge power and hydrogen decreasing edge power relative to the average), it is possible through a combination of control drum rollout and increased tie tube flow to design a system with negligible change in the radial power distribution over the full-power operating lifetime.

The counterflow tie tube mode of control is discussed in detail in Reference 4.

5.7 REACTOR PARAMETER STUDIES

The NERVA Subsystem Optimization Study (Reference 4) considered a number of variables in deriving the weight savings reported. Core height, reflector thickness, core void fraction, and the use of an axial reflector were examined. However, in all these cases the fueled core diameter was held constant because of the ground rule that the NR-1 reactor be compatible with the Phoebus-2 nozzle.

Since the issue of Reference 4, some estimates have been made of the effect of core diameter change on the reactor weight. These results are based largely on the original optimization analysis; i. e., the reactivity coefficients of carbon and uranium, drum span - number of drums - reflector thickness relationship, the effect of uranium inventory on drum span, and other information generated during the optimization study were utilized. The NRX and NR-1 designs served as anchor points to which the estimated data could be normalized.

In particular, curves were generated of the reactor weight versus core diameter for core heights of 42, 52, and 62 inches. Figures 5-15, 5-16, and 5-17 show the reactor weight versus these parameters for constant drum spans of 9.0\$, 6.0\$, and 3.0\$, respectively. These values of the mechanical span correspond to the all-mechanical, mechanical-flow, and all-flow control modes developed in the Optimization Study. Also shown in Figure 5-15 are lines of constant power density at the NR-1 design point and at +50 percent and -25 percent. These power density curves also apply to Figures 5-16 and 5-17.

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6.0 RADIATION ANALYSIS

6.1 GENERAL

This section is a summary of the detailed radiation and shielding analysis performed in support of the NR-1 reactor conceptual design.

One-dimensional radiation and heating environment data were calculated for the NR-1 reactor. A comparison is made of calculated internal and external radiation for an Inconel versus an aluminum pressure vessel. Shield weight penalty estimates for an Inconel vessel are included for a clustered module configuration. An analysis of cluster plate requirements is included. Detailed external radiation environment data and propellant tank heating rate data are given. In addition, the design analysis which led to the selection of the current NR-1 flight shield design is presented.

6.2 GEOMETRICAL MODELS FOR RADIATION ANALYSIS

6.2.1 NR-1 Geometrical Models for Radiation Analysis

Geometrical models or mockups of the NR-1 reactor for radiation analysis are shown in Figure 6-1. The two-dimensional model for the unshielded reactor was based on the NR-1 design presented in Reference 1 and includes a Ag-In-Cd cluster plate and a 1.095-inch thick aluminum pressure vessel. The two-dimensional model for the shielded reactor was based on the NR-1 design presented in this report which includes a B_4C -Cu cluster plate and a 0.27-inch thick Inconel-718 pressure vessel.

In the two-dimensional mockup of the shielded reactor, the shield and the adjacent region above and below the shield were homogenized to form a single region. A similar homogenization was made for the cluster plate region which includes the cluster plate and adjacent plena regions. The axial description of the shielded NR-1 reactor is presented more explicitly in the one-dimensional model as shown in Figure 6-2.

The two-dimensional reactor mockups are subdivided into twenty-seven and twenty-nine regions for the unshielded and shielded reactors, respectively. Both mockups include fifteen core regions (five radial and three axial zones) to allow representation of radial fuel

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zoning and axial variation of hydrogen density. Axial hydrogen variation in the tie tubes and core determined by thermal analysis was used to develop the hydrogen density in each discrete axial zone in the core. A summary description of the regions used to describe the reactor is presented in Table 6-1. Region compositions for the NR-1 are presented in Tables 6-2 and 6-3.

6.2.2 Propellant Tank Geometrical Models for Heating Analysis

The model for propellant tank heating analysis used in this study is the in-line cluster of three reactor modules shown in Figure 6-3. Heating rates for the center tank in this configuration include contributions from the center reactor and the two adjacent reactors since heating in the center tank is the maximum in the in-line cluster of three geometry. Heating rates are also calculated for a single-module configuration.

6.2.2.1 Point Kernel Analysis

The analytical model of a propellant tank for point kernel tank heating analysis is shown in Figure 6-4. It consists of a cylindrical section with a conical base. The tank diameter is 33 feet.

The mockup of a single module, shown in Figure 6-4, closely approximates the actual single-module tank geometry. The off-axis tank geometry cannot be mocked up explicitly because of the geometrical limitations in the point kernel method. Hence, an off-axis tank was assumed to be coaxially aligned with the centered tank and positioned at an axial distance such that the solid angle subtended by the tank model is equivalent to the solid angle subtended by the actual off-axis tank. The off-axis tank solid angle is determined from the subtended area of the actual tank, using the geometric center of the adjacent reactor core. The projection of the off-axis tank in $\Theta - \phi$ coordinates is shown in Figure 6-5, which also defines the solid angle subtended by the center and off-axis tanks.

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TABLE 6-1

NR-1 REGION DESCRIPTION FOR RADIATION ANALYSIS
(Refer to Figure 6-1 for Region Location)

NR-1 (Unshielded)		NR-1 (Shielded)	
<u>Region Number</u>	<u>Region Description</u>	<u>Region Number</u>	<u>Region Description</u>
1	Core Support Block Region	1	Core Support Block Region
2	Aft End Reflector Region	2	Aft End Reflector Region
3-17	Core Regions	3-17	Core Regions
18	Filler Strip Region	18	Filler Strip Region
19	Lateral Support Region	19	Lateral Support Region
20	Beryllium Reflector Region	20	Beryllium Reflector Region
21	Pressure Vessel Region	21	Pressure Vessel Region
22	Cluster Plate Region	22	Cluster Plate Region
23	Core Support Plate Region	23	Shield
24	Tie Tube Plenum Region	24	Drive Shaft Region
25	Forward End Plenum Region	25	Peripheral Shield
26	Drive Shaft Region	26	Control Vane Region
27	Control Vane Region	27	Core Support Plate
		28	Tie Tube Plenum
		29	Forward End Plenum

TABLE 6-2
 NR-1 MATERIAL DENSITIES (WITHOUT SHIELD) FOR RADIATION ANALYSIS
 (GM/CM³ OF REGION AT ROOM TEMPERATURE)
 (Refer to Figure 6-1 and Table 6-1 for Region Identification)

Region	Beryllium	Carbon	Aluminum	Titanium	Manganese	Chromium	Iron	Nickel	Niobium	Molybdenum	Uranium	Silver	Indium	Cadmium	Hydrogen*
1	---	1.24(0)	---	---	---	3.04(-2)	3.47(-2)	7.95(-2)	---	---	---	---	---	---	6.52(-6)
2	---	5.29(-2)	5.84(-2)	3.07(-3)	4.26(-3)	4.05(-2)	1.48(-1)	2.02(-2)	---	---	---	---	---	---	1.62(-2)
3, 8, 13	---	1.13(0)	---	---	---	9.12(-2)	1.04(-2)	2.39(-2)	1.38(-1)	---	1.20(-1)	---	---	---	**
4, 9, 14	---	1.13(0)	---	---	---	9.12(-2)	1.04(-2)	2.39(-2)	1.38(-1)	---	1.64(-1)	---	---	---	**
5, 10, 15	---	1.13(0)	---	---	---	9.12(-2)	1.04(-2)	2.39(-2)	1.38(-1)	---	1.94(-1)	---	---	---	**
6, 11, 16	---	1.14(0)	---	---	---	9.12(-2)	1.04(-2)	2.39(-2)	1.38(-1)	---	8.45(-2)	---	---	---	**
7, 12, 17	---	1.15(0)	---	---	---	9.12(-2)	1.04(-2)	2.39(-2)	1.38(-1)	---	5.18(-2)	---	---	---	**
18	---	1.77(0)	---	---	---	---	---	---	---	---	---	---	---	---	---
19	---	1.09(0)	---	---	---	---	---	---	---	---	---	---	---	---	1.48(-3)
20	1.56(0)	1.24(-2)	2.58(-2)	---	---	1.54(-2)	1.76(-2)	4.03(-2)	---	---	---	---	---	---	3.50(-3)
21	---	---	2.70(0)	---	---	---	---	---	---	---	---	---	---	---	1.67(-7)
22	---	---	1.26(-1)	---	4.38(-3)	7.82(-2)	1.90(-1)	1.22(-1)	9.84(-3)	5.88(-3)	---	2.41(-1)	4.56(-2)	1.56(-2)	7.12(-3)
23	---	---	2.26(0)	---	1.94(-3)	5.45(-2)	1.04(-1)	1.09(-1)	9.73(-3)	5.81(-3)	---	---	---	---	1.64(-3)
24	---	---	1.35(-1)	---	1.04(-2)	1.11(-1)	3.74(-1)	8.48(-2)	3.46(-3)	2.07(-2)	---	---	---	---	3.26(-2)
25	---	---	---	---	---	---	---	---	---	---	---	---	---	---	9.69(-3)
26	---	---	3.72(-3)	4.63(-3)	1.48(-3)	1.41(-2)	5.16(-2)	8.81(-3)	---	---	---	---	---	---	1.03(-2)

* Based on preliminary information from the Thermal Design Group

**
 3.94(-4) in Regions 3-7
 6.52(-4) in Regions 8-12
 1.69(-3) in Regions 13-17

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TABLE 6-3
 NR-1 MATERIAL DENSITIES (SHIELDED CASE) FOR RADIATION ANALYSIS
 (GM/CM³ OF REGION AT ROOM TEMPERATURE)
 (Refer to Figure 6-1 and Table 6-1 for Region Identification)

Region	Beryllium	Carbon	Aluminum	Titanium	Chromium	Iron	Nickel	Niobium	Molybdenum	Uranium	Boron	Copper	Hydrogen*
1	---	1.24(0)	---	---	3.04(-2)	3.47(-2)	7.95(-2)	---	---	---	---	---	6.52(-6)
2	---	5.28(-2)	5.84(-2)	3.07(-1)	4.05(-2)	1.52(-1)	2.02(-2)	---	---	---	---	---	1.62(-2)
3, 8, 13	---	1.13(0)	---	---	9.12(-3)	1.04(-2)	2.39(-2)	1.38(-1)	---	1.26(-1)	---	---	**
4, 9, 14	---	1.13(0)	---	---	9.12(-3)	1.04(-2)	2.39(-2)	1.38(-1)	---	1.65(-1)	---	---	**
5, 10, 15	---	1.13(0)	---	---	9.12(-3)	1.04(-2)	2.39(-2)	1.38(-1)	---	1.19(-1)	---	---	**
6, 11, 16	---	1.14(0)	---	---	9.12(-3)	1.04(-2)	2.39(-2)	1.38(-1)	---	8.45(-2)	---	---	**
7, 12, 17	---	1.14(0)	---	---	9.12(-3)	1.04(-2)	2.39(-2)	1.38(-1)	---	5.18(-2)	---	---	**
18	---	1.77(0)	---	---	---	---	---	---	---	---	---	---	---
19	---	1.09(0)	---	---	---	---	---	---	---	---	---	---	1.48(-3)
20	1.59(0)	1.24(-2)	2.58(-2)	---	1.54(-2)	1.76(-2)	4.03(-2)	---	---	---	---	---	1.60(-3)
21	---	---	---	7.47(-2)	1.57(0)	1.61(0)	4.35(0)	4.26(-1)	2.53(-1)	---	---	---	---
22	---	1.86(-3)	1.39(-2)	3.27(-4)	4.49(-2)	2.03(-1)	3.81(-2)	1.86(-3)	1.11(-3)	---	3.41(-2)	5.07(-1)	9.69(-3)
23	---	1.93(-2)	1.27(0)	5.37(-1)	3.25(-2)	3.32(-2)	8.97(-2)	8.77(-2)	4.6(-2)	---	6.95(-2)	---	4.96(-2)
24	---	---	6.03(-1)	---	3.38(-1)	5.04(-1)	6.88(-2)	3.46(-3)	2.07(-2)	---	---	---	7.04(-3)
25	---	1.27(-2)	1.72(0)	3.37(-1)	---	---	---	---	---	---	4.57(-2)	---	3.07(-2)
26	---	---	---	---	---	---	---	---	---	---	---	---	---
27	---	---	2.17(0)	---	2.67(-2)	7.46(-2)	3.32(-2)	2.37(-3)	1.41(-3)	---	---	---	4.43(-3)
28	---	---	2.57(-1)	---	2.12(-1)	7.20(-1)	1.62(-1)	6.60(-3)	3.95(-3)	---	---	---	3.26(-2)
29	---	---	---	---	1.68(-2)	6.34(-2)	8.41(-3)	---	---	---	---	---	8.32(-3)

*Based on preliminary information from the Thermal Design Group

3.94(-4) in Regions 3-7

6.52(-4) in Regions 8-12

1.69(-3) in Regions 13-17

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Radiation leakage from the reactor model shown in Figure 6-1 into the solid angles defined in Figure 6-5 is basic data for the point kernel tank heating analysis.

6.2.2.2 Monte Carlo

The geometrical model used in the Monte Carlo calculation of propellant heating rates is shown in Figure 6-6. The basic propellant tank model consisted of a hemispherical bottom and cylindrical barrel with a common radius of 493 cm, an aluminum tank wall of 1.38 grams/cm² mass thickness and contained hydrogen at a density of 0.0706 grams/cm³. The central tank was included in the geometrical model since it affects the environment in the off-axis tank.

6.3 METHODS OF ANALYSIS

The radiation and shielding analysis of the NR-1 reactor system based on nuclear and thermal design is given in this report as a comprehensive study of the internal and external radiation environment and heating in the reactor, shield, and propellant tank system.

The four main methods used in this study are based on (1) one-dimensional neutron and photon transport, (2) two-dimensional neutron transport, (3) point kernel analysis, and (4) Monte Carlo. Techniques employed in obtaining multi-group neutron and photon cross sections are based on the standard design method for the NRX reactors which is discussed in detail in References 6 and 8. The four major methods and the types of information obtained from each method are summarized in Figure 6-7 and are discussed in detail in the following paragraphs.

6.3.1 Neutron and Photon Transport Methods

6.3.1.1 One-Dimensional Neutron and Photon Transport

S_n transport methods (References 9, 10, 11) in one-dimensional slab and cylindrical geometries were used in this study to obtain the internal environments at the radial midplane and axial centerline of the reactor system.

These one-dimensional transport calculations utilize the TAPAT system which includes the following output data:

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- 1) Multigroup neutron flux distributions
- 2) Neutron induced distributions of
 - a) Fission
 - b) Radiative capture
 - c) Inelastic scatter
 - d) Kinetic energy depostion
- 3) Gamma source distribution by energy group and mesh point
- 4) Gamma energy flux distributions
- 5) Gamma dose distributions
- 6) Gamma energy absorption distributions

The neutron transport calculations utilize an S_n angular quadrature with isotropic transport-corrected neutron cross sections. Sixteen-group flux distributions* obtained are based on transverse leakage effects for radial and axial calculations and a control drum representation in the reflector as supplied by the Nuclear Design Group.

The neutron-induced reactions and the gamma sources from each type of reaction are calculated from the multigroup neutron flux distribution and gamma emission spectra probability data for each element. Neutron radiative capture, inelastic scatter, and elastic scatter cross sections for the neutron reaction calculations are completely consistent with the spectral-averaged neutron cross sections used in the neutron transport calculation performed to obtain the fluxes. The automated method used in obtaining gamma source distributions maintains the gamma emission spectra from all reactions at each mesh point, and this distribution data is used directly as input to the photon transport problem that follows.

The photon transport calculations utilize an S_n angular quadrature with isotropic total photon cross sections with linear anisotropic (P_1) scattering approximations. Thirteen-group gamma energy flux distributions are obtained from the fixed source distribution as calculated intermediate to neutron and photon transport problems, and the gamma dose and gamma energy absorption calculations are based on these multigroup distributions.** An additional description of the TAPAT system is found in Reference 13.

* Group structure is defined in Reference 6, page 4-23.

** Group structure is defined in Reference 12.

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6.3.1.2 Two-Dimensional Neutron Transport

Neutron transport calculations in the two-dimensional (R-Z) geometry for the unshielded reactor, as shown in Figure 6-1, were performed using the TDC-2 S_n transport program (Reference 14). Region-dependent neutron cross sections were used for the reactor model described in Figure 6-1 and Tables 6-2 and 6-3. S_n transport calculations utilize an S_4 angular quadrature with the isotropic transport corrected neutron cross sections. Two-dimensional multigroup neutron flux distributions were obtained for further calculations of gamma source distribution data.

Two-dimensional neutron induced gamma sources are calculated from the multigroup neutron flux distributions in the NAGS program (Reference 12) in a manner similar to that used for the one-dimensional calculations described previously. The calculated mesh point gamma spectra are reduced to the average (or integral) multigroup gamma source strengths and relative radial and axial source distributions in each discrete region. These data are then directly applicable as input to the point kernel attenuation methods.

6.3.2 Point Kernel Method

Fast neutron and gamma ray radiation environments external to the reactor were computed with the point kernel program KAP (Reference 15). The geometrical and material mockups defined in Section 6.2 were used as basic input to this code.

The KAP code evaluates material attenuation functions and integrates by source point over cylindrical source regions to perform reactor-shield attenuation calculations to obtain fast neutron or gamma ray external environment. The fast neutron calculations employ monovariant fits of calculated neutron differential number spectra (moments data) for carbon (Reference 16). Output from the code consists of the integral neutron flux for $E > 0.1$ Mev and the fast neutron dose rate in Rads (tissue)/hr, both of which are based on the moments data calculations.

Gamma rays are attenuated exponentially with a buildup factor applied to include radiation scattered to the receiver points. A buildup factor for water is used throughout this analysis. Gamma ray data obtained as output from the KAP code include gamma ray dose rates in units of Rads (carbon)/hr and gamma heating rates in units of watts/gm-steel.

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Fast neutron calculations for both the shielded and unshielded reactor are based on region-averaged radial and axial source distributions for fifteen NR-1 core regions. These distributions are obtained from the two-dimensional transport calculations previously described.

Similarly, region averaged gamma source terms were developed for the fifteen core regions in the unshielded configuration. These same core sources were used to determine the core gamma dose rate for the calculation of the external environment of the shielded configuration. Source terms for the core regions include the contributions of prompt, delayed (at the end of a 15-minute operation), radiative capture, and neutron inelastic scatter gamma rays.

Source terms for extra-core reactor regions include secondary gammas from radiative capture and neutron inelastic scattering. These source terms were developed for the unshielded reactor configuration by means of the two-dimensional transport methods previously described. However, extra-core source terms were not developed for the shielded reactor. Instead, the gamma ray leakage from a one-dimensional transport problem was the basis for obtaining the total (core plus extra core) dose rates in the external environment of the shielded reactor.

The multiple region summation option in KAP is used to obtain the total response from the major reactor regions.

6.3.3 Propellant Tank Heating Methods

6.3.3.1 TIC-TOC and TOE (Point Kernel)

Computer program TIC-TOC was utilized to calculate fast neutron kinetic and gamma heating (Reference 17) in the center and off-axis propellant tanks for the NR-1 reactor. Program TOE (Reference 17) was used to calculate the heating resulting from the absorption of secondary gamma rays originating from the n, γ reaction in hydrogen.

Point kernel calculations of the fast neutron flux for $E > 0.1$ Mev and gamma dose rate as a function of polar angle were made at 30 feet from the center of the cores to provide basic input to the TIC-TOC and TOE Codes. The 0.1 Mev threshold was chosen as an arbitrary cutoff energy for obtaining fast neutron leakage, both for the propellant tank heating analysis and for radiation damage considerations, since the bulk of the fast

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neutron energy flux leaking from the reactor is above this energy. The total fast neutron flux and gamma dose obtained from these distributions was averaged over the solid angles subtended by the actual tanks to provide the magnitude of the radiation sources for the TIC-TOC and TOE Codes. The three-group energy distributions required to describe the fast neutron source spectra in the TIC-TOC Code were obtained from point kernel calculations at polar angles of 0 and 50 degrees for the centered and off-axis tanks, respectively. Three-group gamma energy distributions for use in the TIC-TOC Code were obtained from a two-dimensional photon transport at the representative polar angles of 0 and 50 degrees. Sixteen-group neutron spectra from a two-dimensional neutron transport problem were collapsed into five groups, normalized to the point kernel fast flux greater than 0.1 Mev, and used in Program TOE to calculate secondary gamma heating.

The TIC-TOC and TOE Codes are limited to tank geometries which can be described as a combination of cylinders and truncated cones with a common axis which contains an effective point source of radiation. Thus, the off-axis tank, which cannot be mocked up explicitly, is represented as a centered tank at a greater distance from the core center than the on-axis tank, as described in Section 6.2.2. However, the magnitude of the fast neutron and gamma radiation levels within the actual angle subtended by the tank (Figure 6-5) is calculated explicitly.

6.3.3.2 Monte Carlo Propellant Heating Analysis

The FASTER Monte Carlo Code^{*} was used to calculate the azimuthal and spatial variation of propellant heating rates in the off-axis tanks. This code incorporates the latest developments in the analytic Monte Carlo method and includes the following special features:

- 1) Treatment of either neutrons or gamma rays
- 2) Alternate use of multi-group constants for neutrons (employed in this study)
- 3) Simultaneous treatment of particles with different energies
- 4) Consistent use of biasing

^{*}Unpublished WANL Code

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- 5) Treatment of multiple point, line, surface, and/or volume sources in rectangular, cylindrical, or spherical geometry
- 6) Quadric surface representation of the material distribution
- 7) Multiple number flux response functions and their associated variances (calculated)
- 8) Analytic estimation of fluxes for multiple point, surface, and volume detectors

Neutron and gamma ray volume and surface source distributions for this study were obtained from the NAGS program. Neutron calculations used twelve fast-group cross sections, while twenty-three groups were used for gamma rays.

6.4 ONE-DIMENSIONAL INTERNAL RADIATION AND HEATING ENVIRONMENTS

Internal radiation and heating environments at full power (5000 megawatts) are presented as one-dimensional radial midplane and axial centerline traverses in Figures 6-8 through 6-19. These traverses are axial and radial average values based on the one-dimensional neutron and photon transport calculation methods described in Section 6.3.1.

The internal radiation environment presented in this analysis includes neutron fluxes of neutron energy greater than 2.87, 1.0, and 0.1 Mev and of neutron energy less than 1.86 ev. Gamma dose rates are presented in units of Rads (carbon)/hr.

The calculated heating rates are volumetric heating rate densities in a region (watts per cubic centimeter) which include gamma and neutron kinetic heating components and, in the shielded configuration, the n, α component. The maximum (not radially averaged) heating rate in the shield and the support plate is shown in Section 6.4.2.6 to be 181.0 and 14.3 watts/cc of the respective regions.

6.4.1 Radial Data

Axially averaged neutron flux, gamma dose rate, and neutron plus gamma heating rates are presented as a function of distance from the reactor axis. Midplane values can be obtained from these data by multiplying by a maximum/average ratio of 1.44. The radial location of key regions can be found by reference to Figure 6-1.

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6.4.1.1 Neutron Flux

Figure 6-8 presents the axially averaged fast neutron flux versus the core radius for neutron energies greater than 2.87, 1.0, and 0.1 Mev.

Figure 6-9 shows similar data for the thermal neutron flux for neutron energies less than 1.86 ev. The sharp depression in the thermal neutron flux at a radius of approximately 85 cm is due to the flux depression in the ring mockup of the control vanes.

6.4.1.2 Gamma Ray Dose Rate

The gamma dose rate in Rads (carbon)/hr at full power conditions are plotted versus reactor radius in Figure 6-10. These data are again axial average values which include primary and secondary source components.

6.4.1.3 Heating Rates

The radial heating rates, as presented in Figure 6-11, show axial average heating rate values, i.e., neutron kinetic heating, gamma heating, and total (neutron plus gamma) heating plotted in watts/cc of region versus radius. These heating rates do not include the n, a heating in the control vanes. Midplane heating rates would be a factor of 1.44 higher than the average values. The somewhat lower heating rate between a radius of 73 and 76.4 cm represents the heating rate in the lateral support region. The heating rate in this region is lower than that in the adjacent filler strip and Be reflector regions mainly due to the decreased region density. The increased heating rate at 93.55 cm, as compared with the adjacent reflector region, is due to the increased density and higher capture cross section of the aluminum pressure vessel which results in higher primary and secondary gamma heating rates in this region than in the adjacent Be region.

6.4.2 Axial Data

Figures 6-12 through 6-19 present radially averaged neutron flux, gamma dose rate, and total (neutron plus gamma) heating rates as a function of distance from the nozzle end of the core. Centerline values would be a factor of 1.19 higher. Dashed lines indicate the nozzle and dome ends of the reactor core.

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6.4.2.1 Neutron Flux - Without Shield

Radially averaged axial fast neutron flux data are presented in Figure 6-12. These data show a cosine shape in the core and a relatively low attenuation (horizontal slope) through the forward plenum region. It should be pointed out, however, that previous comparisons of one-dimensional calculations of neutron flux through low density hydrogen regions with two-dimensional analysis give significant discrepancies which are believed to be due to changes in the radial leakage component. Hence, neutron flux levels in and above the forward end plenum are not expected to be very reliable.

Similar data are presented in Figure 6-13 for the thermal neutron flux with energies less than 1.86 ev. The flux depression at the dome end of the core, in this case is due to the Ag-In-Cd cluster plate present in the conceptual (Reference 1) design of the NR-1.

6.4.2.2 Neutron Flux - With Shield

Radial average fast neutron flux data for the shielded NR-1 reactor are presented in Figure 6-14. These neutron fluxes are considered to be accurate up to and through the core support plate. The fluxes in and above the tie tube plenum are not expected to be very reliable for reasons previously stated. The thermal neutron fluxes for the shielded reactor, shown in Figure 6-15, exhibit severe flux depression at the dome end of the core which, in this case, is due to the B_4C -Cu cluster plate.

6.4.2.3 Gamma Ray Dose Rate - Without Shield

Gamma dose rates in Rads (carbon)/hr are presented versus axial distance from the nozzle end of the core in Figure 6-16. Local increases in gamma dose rates near the dome end of the core and at an axial distance of about 170 cm are due to the increased secondary gamma production in the Ag-In-Cd cluster plate and the tie tube plenum, respectively.

6.4.2.4 Gamma Ray Dose Rate - With Shield

Radial averaged gamma dose rates for the shielded NR-1 reactor are presented in Figure 6-17. The local increases in gamma dose rates which resulted from gamma production in the Ag-In-Cd cluster plate in the unshielded NR-1 model are eliminated in

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this case. The secondary gamma production in the B_4C -Cu cluster plate, in which the absorptions are principally n, α , is considerably less than it is in the Ag-In-Cd cluster plate.

6.4.2.5 Heating Rates - Without Shield

The neutron plus gamma heating rate values given in Figure 6-18 follow the customary cosine shape in the core except at the inlet (dome) end of the core where the gamma heating rate is increased locally due to the increased secondary gamma production in the Ag-In-Cd cluster plate. The plenum region immediately above the core is characterized by relatively low heating rates due to the low density of this region. This is followed by a rather severe increase in heating rate up to about 150 watts/cc in the core side of the core support plate. This rapid increase is due to the relatively higher density of the support plate and the secondary gamma generation in both the support plate and cluster plate regions. The slight increase in heating rate on the dome side of the support plate is due to secondary gammas generated in the tie tube plenum immediately forward of the support plate. Average heating rates in the tie tube plenum are lower than in the support plate region, due to the lower average density in this region. The forward end plenum which is forward of the tie tube plenum region contains essentially only hydrogen coolant. Hence, because of the extremely low density of hydrogen, the heating rate in this region is lower than that in any other region. Finally, heating rates are again increased in the forward aluminum pressure vessel region due to the relatively high density of the vessel which results in high gamma heating in this region.

6.4.2.6 Heating Rates - With Shield

Heating rates in regions above the dome end of the core are presented in Figure 6-19. Fast neutron (n, α) and gamma heating are included. Based on a maximum/average ratio of 1.19, the maximum heating values in the shield and support plate regions are 181.0 and 14.3 watts/cc, respectively.

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6.5 INCONEL VERSUS ALUMINUM PRESSURE VESSEL

6.5.1 Comparison of Internal Radiation and Heating Environment

At the request of SNPO-C, studies of the substitution of an Inconel-718 pressure vessel for the aluminum pressure vessel used in an earlier design and the resultant effect on radiation environment and shielding requirements were performed. These studies are limited to one-dimensional analyses. The calculations are made with identical internal reactor systems (without an internal flight shield). Radiation sources due to neutron radiative capture and neutron inelastic scatter in the pressure vessel region are considered. Pressure vessel thicknesses as set by mechanical design requirements are 1.095 inches of aluminum versus 0.27 inch of Inconel-718. However, the aluminum vessel thickness requirements may decrease from 1.095 to 0.86 inch because of the improved strength properties of aluminum now utilized in the AGC analysis (Reference 18).

NR-1 radial distributions of the fast neutron flux in three groups, thermal neutron flux, and gamma dose rates at full power (5000 megawatts) are presented in Figures 6-20 through 6-24 for the Inconel-718 vessel and 1.095 and 2.5-inch aluminum vessels. NR-1 axial distributions (without the flight shield) for Inconel and aluminum vessels are compared in Figures 6-25 through 6-29.

The variation in the gamma dose rate through the aluminum and Inconel-718 vessels is shown in Figures 6-30 and 6-31.

The radiation heating in the pressure vessel and the adjacent beryllium reflector region is shown in Figure 6-32 for the two pressure vessel materials. The increased heating rate with the Inconel vessel is due to the increased secondary radiation source in the Inconel.

6.5.2 Comparison of External Radiation Environment

The relative increase in external radiation environments for the Inconel vessel is approximately 28 to 30 percent over the aluminum vessel. One-dimensional calculations are used to determine the additional shielding for the Inconel-718 pressure vessel required to maintain the environmental gamma dose rate at the same level as for the 1.095-inch thick aluminum vessel. The additional side shielding required for an Inconel-718 pressure vessel

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to achieve the same external environment as for the 1.095-inch aluminum vessel is 0.75 inch of aluminum. Only 0.46 inch of aluminum shielding is required for the Inconel-718 pressure vessel to achieve the same gamma dose rate external environment as for the 0.86-inch aluminum vessel.

6.5.3 Comparison of Shield Weights for Aluminum and Inconel-718 Pressure Vessels

Shield requirements for the reactor systems with an Inconel-718 pressure vessel instead of an aluminum pressure vessel are based on simple annular shields of aluminum which supply partial angular coverage of the reactor system lateral (cylindrical) surface.

The conceptual side shield arrangement assumes that a three in-line modular arrangement requires four side shields for the three reactors. The center reactor requires two side shields diametrically opposite to one another while the two outer reactors require one shield each. Hence, the average side shield weight per reactor is a factor of 1.33 greater than for a single side shield. The angular coverage of each side shield is limited to 120 degrees in the direction of the adjacent reactor.

On this basis, an unshielded reactor would require 720 pounds of additional shielding per reactor for an Inconel-718 pressure vessel to achieve the same propellant tank heating in off-axis tanks as occurs with a 1.095-inch aluminum vessel. For the unshielded reactor configuration, if the aluminum vessel thickness is decreased to 0.86 inch, the weight penalty for the Inconel-718 vessel is 450 pounds.

A lower weight penalty would be incurred for the reactor with an internal flight shield. In this case, the extent of the axial coverage on the lateral surface of the reactor pressure vessel is limited to the lower 60 percent of the vessel height. This assumption is based on the consideration that for the shielded reactor configuration the principal lateral surface leakage is limited to the region of the reactor core.

These calculations are non-optimum in the sense that the shields which are added to the Inconel vessels are not shaped or tapered to make the most effective use of the shield weight.

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6.6 EVALUATION OF THE CLUSTER PLATE

A study was performed to evaluate the cluster plate requirements and the effect on shield performance. The one-dimensional photon-neutron transport system TAPAT was used to evaluate the effect of the cluster plate on reactor leakage and internal reactor heating rates. Two TAPAT calculations were run, with and without a B_4C -Cu cluster plate at the core inlet, for an NR-1 configuration with a TiH_2 - B_4C -Al composite shield between core and core support plate. From these calculations, it was found that the B_4C -Cu cluster plate (20%Vol. B_4C) had a worth of only 25¢ in reactivity while it significantly reduced the neutron n , α heating in the lower 2 cm (core side) of the flight shield. The cluster plate also significantly reduced core inlet power peaking and secondary gamma production in the flight shield. Thus, the cluster plate decreased gamma leakage from the forward end of the reactor by 20 percent. Because of the extra benefit in reducing radiation leakage and shield heating, the design with the cluster plate was selected for the NR-1.

Figures 6-33 through 6-36 indicate the extent of the neutron flux redistribution caused by the cluster plate. Figures 6-37 and 6-38 show the gamma dose rate and heating rate distributions, respectively. The traverses presented in Figures 6-33 through 6-38 are for 5000-megawatt reactor operation.

6.7 EXTERNAL RADIATION ENVIRONMENT

Fast neutron radiation levels outside the reactor were obtained by the point kernel method, using reactor sources from two-dimensional transport techniques as described in Section 6.3 in order to permit evaluation of various control detector positions. Core gamma dose rates and heating rates outside the reactor were similarly developed. "Extra-core" secondary gamma radiation levels were developed, using sources from two-dimensional transport techniques in the point kernel code for the unshielded configuration. However, in the case of the shielded reactor configuration, the extra-core gamma radiation levels were estimated from a one-dimensional transport problem as described in Section 6.3. Sources from the two-dimensional transport are currently being developed for the shielded reactor configuration. These sources will be utilized in the KAP Code to calculate the external radiation environment by the same technique employed for the unshielded reactor. Hence, radiation

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levels outside the reactor as presented in this section will probably undergo minor revision as the calculational technique is refined.

Gamma dose rates in Rads (C) per hour, gamma heating rates in watts/gm of steel, and the fast neutron flux of neutrons with energies greater than 0.1 Mev in neutrons/cm²-sec at several locations outside the reactor are presented for the unshielded configuration in Table 6-4. Heating in the control drum actuator is 1.2 watts/gm(steel) in the unshielded configuration which is higher than in any of the engine components considered except the turbopump. Radiation levels were calculated along polar traverses at 30 feet from the center of the reactor core to provide data for tank heating calculations. Gamma dose rate polar traverses calculated for core gammas only are presented in Figure 6-39. Total (core plus extra-core) gamma ray dose rates are presented in Figure 6-40. Figure 6-41 shows the fast neutron flux distributions.

Curves are presented in each of these figures for the unshielded and shielded reactor configurations and for the special case in which the reactor is shielded by the top portion of the shield only, without the peripheral shield. The significance of the curve labeled "top shield only" is discussed in Section 6.9.4.

Polar angles subtended by the center tank and off-axis tank in the clustered module configuration are identified in these figures. The neutron and gamma distributions over the polar angle were integrated for the solid angle subtended by the tanks between the polar angle limits as shown in Figures 6-40 and 6-41 to obtain gross heating estimates of propellant tank heating (Section 6-8).

6.8 NR-1 PROPELLANT TANK HEATING

6.8.1 TIC-TOC and TOE (Point Kernel)

Propellant tank heating rates were calculated for a single-module configuration (to see if the shield meets the tentative shield criteria) and for the center tank in the in-line cluster of three configuration. The basis for the tank heating calculations is the point kernel calculation of fast neutron flux and gamma dose rate at 30 feet from the reactor which was used in the TIC-TOC and TOE codes as outlined in Section 6.3. These point kernel data are presented in Figures 6-40 and 6-41.

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TABLE 6-4

NR-1 UNSHIELDED RADIATION LEVELS AT SELECTED ENGINE COMPONENTS

Component	Location		Gamma Dose Rate (Rads - carbon/hr)	Gamma Heating Rate (Watts/gm - steel)	Neutron Flux ($E > 0.1$ Mev)
	R(cm)	Z(cm)			
1 Turbopump (bottom)	0.0	248.0	5.503(8)	1.852	2.645(13)
2 Turbopump Control Valve	108.0	289.0	1.716(8)	0.574	1.118(13)
3 Pneumatic Tanks	108.0	418.0	7.331(7)	0.244	5.306(12)
4 Actuator	88.0	200.0	3.60(8)	1.200	5.975(13)
5 Gimbal Actuator	168.0	442.0	5.381(7)	0.179	4.199(12)
6 Propellant Shutoff Valve	0.0	474.0	6.218(7)	0.206	4.996(12)
7 Check Valve	147.0	399.0	7.042(7)	0.234	5.189(12)

Notes:

- 1) These levels do not include perturbation of the radiation environment due to the engine.
- 2) An uncertainty factor of 2 is assigned to these radiation levels.
- 3) The location is given in a cylindrical coordinate system with the origin on the reactor axis at the nozzle end of the core support blocks. Z is positive in the direction of the dome end. The system is assumed to have azimuthal symmetry.

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Tank heating calculations for the unshielded NR-1 are summarized in Table 6-5. With no shield, the tank heating for a single-module configuration is 1.3 megawatts. Heating in the center tank of the clustered module configuration, i.e., the sum of tank heating for a single module plus twice the heating in an off-axis adjacent tank is 2.3 megawatts. For the tank heating in the single-module configuration, 31 percent results from neutron interactions, i.e., neutron kinetic heating plus capture gamma heating.

Tank heating reduction factors for the shielded reactor configuration and the special case in which the reactor is shielded by the top portion of the shield only, i.e., without the side extensions, are presented in Table 6-6. These factors can be multiplied by the unshielded heating rates presented in Table 6-5 to obtain heating for the shielded configurations. Thus, for the shielded configuration, tank heating in the single module is 0.15×1.34 megawatts or 201 kilowatts. Heating in the center tank of the clustered module for the shielded reactor is 0.32×2.295 megawatts or 734 kilowatts.

Work is continuing in this area, particularly in the calculation of tank heating for the shielded reactor configuration. Refinement of the calculation of the radiation leakage from the shielded reactor as discussed in the previous section may result in some revisions in the heating data presented in this section.

6.8.2 Monte Carlo

Preliminary Monte Carlo propellant heating calculations were performed with the FASTER code for both the central and off-axis tanks with a single reactor below the central tank. (Refer to Figure 6-6.) Radiation sources included reactor gamma rays and neutrons. The neutron capture gamma source in the tanks will be treated in subsequent analysis. Further description of the method of analysis is given in Section 6.3.3.2.

6.8.2.1 Reactor Gammas

The reactor gamma heating distributions include both core and extra core sources, e.g., reflector, shield, pressure vessel secondaries, a total of fourteen volume sources. The radial and axial source distributions and source spectra were taken directly from an ODDK NAGS calculation.

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TABLE 6-5

PROPELLANT TANK HEATING (MEGAWATTS)

Single Module Configuration

	<u>NR-1 (Unshielded)</u>
Reactor Gammas	0.921
Fast Neutron	0.158
Capture Gammas in Tank	<u>0.260</u>
Total	1.339

Clustered Module Configuration - Heating in the Off-Axis Tank from an Adjacent Reactor

	<u>NR-1</u>
Reactor Gammas	0.367
Fast Neutron	0.061
Capture Gammas in Tank	<u>0.050</u>
Total	0.478

Clustered Module Configuration - Heating in the Center Tank

	<u>NR-1</u>
Total	2.295

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TABLE 6-6
 SHIELD ATTENUATION FACTORS FOR NR-1 REACTOR
 SINGLE AND CLUSTERED MODULE CONFIGURATIONS

	<u>Unshielded</u>	<u>Top Shield Only</u>	<u>Top Plus Side Extension Shield</u>
<u>Single Module</u>	1.0	0.44	0.15
<u>Clustered Module</u>			
Centered Tank	1.0	0.59	0.32
Off-Axis near Tank	1.0	0.81	0.57

Note:

The attenuation factor is defined as the factor needed to multiply unshielded heating rates in Table 6-5 to obtain the total heating in the propellant tanks for various configurations.

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Calculations were performed in various vertical planes rotated azimuthally about the tank centerline axis. Azimuthal angles are measured from the line between the two tanks as shown in Figure 6-6.

Gamma heating distributions in the central tank, Figures 6-42 and 6-43, were obtained by performing separate calculations for thirty-six point detectors. The results for traverses in the hemispherical tank bottom were similar for the 0 degree detector plane (in line with off-axis tank) and the 60 degree detector plane. However, the traverses in the cylindrical tank barrel indicate higher heating rates in the 0 degree detector plane due to gamma rays scattered from the off-axis tank.

Gamma heating distributions in the off-axis tank, Figures 6-44 through 6-47, were obtained from sixty-six Monte Carlo point detector calculations. The effect of the on-axis tank on heating rates in the off-axis tank is most notable for the 0 degree detector plane (in line and closest to the on-axis tank) where it shields those points closest to it. Generally, heating rates were higher in the 60 degree detector plane than in the zero degree plane for corresponding radial and axial points since the central tank no longer provided self-shielding. Heating rates for the 120 and 180 degree detector planes decrease from the 60 degree rates because of the self-shielding of the tank.

Calculated error estimates for these point detectors ranged, in general, from 15 to 35 percent. This statistical error could be reduced by running more histories.

6.8.2.2 Neutrons

The neutron kinetic heating distributions were obtained from a core fission source represented by five volumes with radial and axial distributions and source spectra obtained from NAGS. Because of the large attenuation factors due to self attenuation in the tank bottom and adjacent tank, detector traverses in the tank barrel were not calculated. Both heating and secondary sources would be insignificant in the tank barrel region. Those traverses which were obtained in the tank bottom had computed errors on the order of 20 to 40 percent.

Neutron heating distributions in the on-axis tank (Figure 6-48) were obtained for both the 0 and 60 degree detector planes, using twenty-eight point detectors. However, no

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effect of the off-axis tank was observable; therefore, only one set of distributions is presented. The large decrease in heating rates near the tank surface from the 30 to the 60 degree radial traverse (along polar angles measured from the centerline axis, see insert in Figure 6-48) is attributed to attenuation caused by the curvature of the tank bottom. Heating rates were also calculated on a radial traverse along the tank barrel-tank bottom intersections (90 degree polar angle) but these were orders-of-magnitude lower than those obtained for 60 degrees.

Neutron kinetic heating distributions in the off-axis tank, Figures 6-49, 6-50, and 6-51, were obtained from forty point detectors. On the 0 degree detector plane (Figure 6-49) the large decrease in heating rates along the tank barrel-tank bottom intersection and close to the tank wall is due to shielding by the hemispherical bottom of the on-axis tank. Heating distributions on the side opposite to the reactor - the 120 and 180 degree detector planes - were very low and are presented for only one traverse, 30 degree polar angle from the centerline in the 120 degree azimuthal plane.

6.9 DESIGN ANALYSIS OF THE NR-1 FLIGHT SHIELD

The shield design analysis consisted of the selection of (1) the basis for shield design, (2) shield materials, and (3) shield geometry for both single-module and clustered-module configurations. Finally, a detailed evaluation of this preliminary design is presented.

6.9.1 Shield Design Consideration

The design of the shield is based on three considerations: (1) reactor component radiation environment limitations, (2) engine component radiation environment limitations, and (3) tank heating. A comparison of the maximum test environment experience to date with calculated levels for reactor and engine components is shown in Table 6-7. The most sensitive reactor components in terms of radiation damage are the control drum actuators which have been tested to a total integrated flux of $(0.8 \text{ to } 1.8) \times 10^{11}$ ergs/g-C gammas and $(4 \text{ to } 7) \times 10^{16}$ n/cm² for E > 1 Mev without malfunction or noticeable damage, but have not been tested to the high rates expected for the unshielded environment (see Table 6-4). These tests were conducted at the General Dynamics/Ft. Worth GTR facility. The maximum predicted radiation levels at the

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TABLE 6-7

SHIELD DESIGN CONSIDERATIONS

	Reactor Component (Actuators)	Engine Component	Tank Heating
Maximum Test Environment	$(0.8 \text{ to } 1.8) \times 10^{11}$ ergs/g-C Gammas ⁺	2.8×10^8 ergs/g-C	
Experience to Date	$(4 \text{ to } 7) \times 10^{16}$ n/cm ² (E > 1 Mev) ⁺	(Complete Reactor Engine Assembly) ^{**} 2×10^9 ergs/g-C (TPA only) ⁺	
Reactor Environment *	3×10^{10} ergs/g-C Gammas	1.4×10^{10} ergs/g-C	1100 kw
Unshielded	5.5×10^{16} n/cm ² (E > 1 Mev)		
Shielded Environment *	1×10^{10} ergs/g-C Gammas	2×10^9 ergs/g-C	156 kw
(Reference 19 Shield)	6×10^{15} n/cm ² (E > 1 Mev)		

* Reactor and engine environment based on three 20-minute full power runs

** NRX/EST test

+ GD/Ft. Worth tests

Conclusions:

1. Reactor components do not appear limiting even without a shield, however, rate effects have not yet been simulated.
2. Engine components do not appear limiting for shielded case if radiation hardened engine components are used.
3. Tank heating for single module not limiting for shielded case since maximum temperature rise of propellant is only 1°F based on complete mix model.

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actuator location for an unshielded NR-1 reactor are 3×10^{10} ergs/g-C gammas and 5.5×10^{16} n/cm² for $E > 1$ Mev, assuming three 20-minute full power runs, respectively. Hence, the predicted unshielded levels are within the safe operating levels experienced to date. It should be noted, however, that although the above tests exceeded the predicted maximum unshielded integrated flux values, the experimental test rates were considerably below NR-1 predicted rates. The conclusions from this comparison are that reactor components, in particular the actuators, do not appear to be limiting based on integrated levels; however, rate effects have not been simulated to date.

Considering engine component test experience, AGC has conducted complete engine assembly operation in a radiation field of about 2.8×10^8 ergs/g-C on NRX/EST. A turbine-pump assembly was tested at GD/Ft. Worth to a total integrated gamma dose of 2×10^9 ergs/g-C. These tests were made with non-radiation hardened components. The predicted integrated gamma dose, again based on three 20-minute full power runs, was found to be 2.0×10^9 ergs/g-C for a shielded configuration and 1.4×10^{10} ergs/g-C for an unshielded configuration. The conclusions from this comparison are that engine components do not appear limiting for the shielded case, especially if radiation hardened engine components are used. The shielded environment used for this comparison was arbitrarily based on the shield design presented in Reference 19.

The tank heating rates predicted for a single-module NR-1 configuration with and without the shield are 156 kilowatts and 1100 kilowatts, respectively. Assuming a complete mix coolant flow model, the maximum temperature rise for a 20-minute reactor run is about 1°F. This temperature rise seems very nominal and a separate mission analysis and systems optimization study by Lockheed Missiles and Space Corporation (Reference 20) indicated that the shield discussed in Reference 19 may be near optimum.

Furthermore, in a clustered engine configuration, the heat input to the propellant tanks without the addition of side shielding is some four to six times as high as for a single module. Thus, some additional margin may be necessary for heating the propellant in clustered reactor modules.

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Hence, in the absence of more specific radiation limitations or shield design requirements, the shielding design criterion for this study has been set to maintain the shield attenuation as presented in Reference 19.

6.9.2 Selection of Shield Materials

In order to select materials for the NR-1 flight shield, a fairly complete evaluation of some fifty sets of potential shielding materials was made (Reference 21). This evaluation considered the nuclear attenuation properties and radiation damage limitations as well as the practical aspects of the cost of fabrication and manufacture of a flight-type shield for the NR-1 reactor. A range of attenuation factors from 0.04 to 0.25 was studied, but it was determined that the selection of materials on the basis of lowest weight in this range of attenuation was virtually independent of the attenuation factor.

Table 6-8 presents some typical results from this study for an attenuation factor of 0.1. Shield weights are given in terms of mass thickness in g/cm^2 . A total shield weight may be obtained by multiplying by the cross-sectional area of the shield. Attenuation factor, in this case, is defined as the reduction factor in propellant tank heating rates. Thus, an attenuation factor of 0.1 corresponds to a 90 percent heat removal in terms of tank heating. As seen in Table 6-8, the lightest optimum or practical set of shielding material from this study was a LiH-steel combination. There are two sets of weights (mass thickness) in this table, those listed under "optimum" and a second set listed under "practical." "Optimum" in this case refers to the unconstrained minimum weight values obtained directly from the nuclear optimization study explained more fully in Reference 21. "Practical" refers to minimum weight values consistent with some minimum structural requirements set by mechanical and thermal design requirements. In most cases, it should be noted, the optimum weight is obtained with a moderator volume fraction of 1.0 and with a structural volume fraction of zero. In any practical design, some 10-30 % by volume of structure is required for cladding or for simply holding the shield together. Thus, the minimum weight or optimum shield was obtained for 90 % by volume LiH and 10 % by volume borated steel, while some 30 % by volume is believed to be required to provide structural support and cladding to maintain the integrity of the shield. Thus, the structural requirements resulted in a 3 g/cm^2 increase in shield weight from 47 g/cm^2 to

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TABLE 6-8
OPTIMUM VERSUS PRACTICAL SHIELD WEIGHTS

<u>Alloy</u>	<u>Attenuation Factor = 0.1</u>				
	Optimum Moderator Volume Fraction	Mass Thickness	Practical Moderator Volume Fraction	Mass Thickness	Percent Increase Versus Practical LiH
LiH - Steel	0.90	47.0	0.70	50.0	0
TiH ₂ - Steel	1.0	49.3	0.70	53.7	7
TiH ₂ - B ₄ C - Al	1.0	47.0	0.30	53.9	8
BeO - Al	1.0	53.5	0.85	56.0	12
H ₂ - Steel	0.85	57.5	0.85	57.5	15
B ₄ C - Al	1.0	54.0	0.30	61.1	22
B ₄ C - Steel	1.0	54.0	0.65	58.4	17
Be - Steel	0.80	62.0	0.80	62.0	24

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50 g/cm². Considerations of cost and radiation environment, however, considerably weaken the argument for a LiH-steel shield. Based on the WANL experience with the KIWI-B5 shield which was designed and built at WANL, the cost of a LiH-borated steel shield for NR-1 is estimated to be about \$700,000. Furthermore, the extreme radiation environment of both heating rate density and integrated flux and dose in the shield region show the LiH material to be marginal in this application. For example, a maximum integrated thermal flux of about 3×10^{18} nvt and a fast flux of some ten times this value is predicted for NR-1. Severe swelling of the KIWI-B5 LiH shield capsules was experienced with as little as 2×10^{16} nvt thermal radiation. Furthermore, the maximum heating rate in the shield is expected to be about 130 watts/cc (shield above support plate). For these reasons, LiH-steel was not the first choice for the NR-1 flight shield design.

The second lightest material, TiH₂-steel was likewise rejected on the basis of cost and resultant complexity in the shield design. Finally, the prime candidate for the NR-1 design was the TiH₂-B₄C-Al composite. As a shield, this material is some 8 percent heavier than the lightest practical material, LiH-steel, but is also one of the cheapest shield materials.

However, it should be noted that the radiation resistance of this material must be verified experimentally by in-pile tests to the levels expected for NR-1. Although the radiation resistance of all composite materials is known to be above the levels required for NR-1, the combined material should be tested because of possible interaction effects.

Be and borated steel have been selected for backup shield study on the basis that mechanical and radiation properties for these materials are already known.

6.9.3 Selection of Shield Geometry

In the Reactor-Shield NR-1 Nuclear Subsystem Parametric Study (Reference 4), three basic shield geometries were considered (Figure 6-52). The first geometry was the conventional top shadow shield arrangement as utilized in the shield design of Reference 19. The second configuration considered the incorporation of the shield as part of the core in the form of unfueled graphite extension, both with and without additional shielding between the core and support plate. The third configuration considered relocation of the shield between the core and the core support plate. The additions of depleted uranium in the graphite and a small

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amount of boron (0.2 % by weight) were also considered but were rejected because they resulted in inferior designs.

In all configurations, it should be noted, a 1200-pound vertical side shield extension was maintained for reasons which will be more fully described below.

The shield design criterion for this study was to maintain the same tank heating as for the shield design of Reference 19, i.e., 156 kilowatts for a single-module tank. The calculations utilize the one-dimensional neutron-photon transport system TAPAT which is described in Section 6.3.1.

The results of this study as shown in Table 6-9 indicate the best (lightest shields) of the three configurations given in Figure 6-52. First, the conventional top shield design of BeO and Al had an estimated weight of 2900 pounds. This weight would be reduced to approximately 2500 pounds by changing to the lighter $\text{TiH}_2\text{-Al-B}_4\text{C}$ composite material. Next, the lightest shield was obtained for the third configuration, i.e., with the shield relocated between the core and the core support plate. The savings in weight result from (1) a reduction in secondary gamma production in the support plate and tie tube plenum regions, and (2) reduction of the shield radius with the shield subtending the same solid angle for the principal radiation source (the core). This shield, designed to the same conditions as for case 1, weighs about 1400 pounds. Thus, by changing the shield materials and relocating the shield below the support plate, some 1500 pounds of shielding weight is saved.

The second configuration, which uses a combined shield and unfueled graphite core extension 10 inches in length together with a 3.5-inch thick $\text{TiH}_2\text{-B}_4\text{C-Al}$ composite shield between the unfueled graphite and core support plate, resulted in a combined reflector and shield weight of 2130 pounds. The latter configuration, although it does not have the lowest shield weight, may allow further reduction in the reactor weight by introducing extra reactivity in the form of the axial graphite reflector and may allow further thinning of the radial side reflector. The potential advantage of such combined reactor and shield weight savings and the consequences on the nuclear and thermal design of the system should be investigated more fully.

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TABLE 6-9
SUMMARY OF SHIELD DESIGN CALCULATIONS

<u>Shield Configuration</u>	<u>Composition</u>	<u>Thickness (in.)</u>	<u>Weight (lb)</u>	<u>Application *</u>
Reference 19	0.42 B-BeO 0.39 Al 0.19 coolant void	9.0	2900	Reference 1
Shield Below Support Plate	0.192 TiH 0.048 B ₄ C ² 0.56 Al 0.20 coolant void	5.9	1360	D-1
Combined Shield - Axial Reflector	0.192 TiH 0.048 B ₄ C ² 0.56 Al 0.20 coolant void	3.5	810	D-2, D-4, D-6, D-7
	0.6C, 0.4 coolant void	10.0	1320	
Shield Below Support Plate	0.192 TiH 0.048 B ₄ C ² 0.56 Al	6.0	1390	D-3, D-5

* Reactor designation is given in Table 2-1 of Reference 4.

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6.9.4 Calculated Shield Effectiveness

Following the selection of the optimum location of the central shadow shield below the support plate, the shield effectiveness both with and without the side shield extension was evaluated by determining the effect on external radiation environment and tank heating. Figure 6-3 shows the reactor, shield, and propellant tank geometry for single and for clustered module configurations. The polar angle limits of the solid angles subtended by the propellant tanks of the various modules are clearly indicated in this figure.

External radiation environment calculations were performed by the point kernel program KAP, using two-dimensional fast neutron, primary gamma, and secondary gamma source strengths and source strength distributions generated by the NAGS-ODDK code system as described in Section 6.3.1.2.

The top shield listed in this study is the 6-inch thick $\text{TiH}_2\text{-B}_4\text{C-Al}$ shield located below the support plate. Detailed atom densities and dimensions are given in Section 6.2.

The side extension shield, when used, is made up of about 1000 pounds of $\text{TiH}_2\text{-B}_4\text{C-Al}$ which is the approximate equivalent of the 1200-pound BeO-Al side shield extension specified in Reference 19. The results of the KAP point kernel calculations are shown in Figures 6-39 through 6-41 for gammas and fast neutrons, respectively. In these figures, the fast neutron flux for > 0.1 Mev and the gamma dose rate, Rads (carbon)/hr, are plotted versus polar angle (α) for three configurations: no shield, with top shield only, and with top shield plus side extension shield. These polar angle plots are designated as applying to certain tanks in the clustered configuration. For example, 0 - 32 degrees is the polar angle subtended by the tank directly forward of the reactor and with the same axis as the reactor 32 - 69 degrees is the polar angle region subtended by the off-axis near tank and 69 - 79 degrees is the polar angle subtended by the off-axis far tank.

By comparing the calculated radiation environment for the "top shield only" configuration with the "no shield" configuration, the shield effectiveness is reduced with increasing polar angle even for the single-module or centered-tank geometry, and the shield effectiveness is reduced almost to the unshielded case for the off-axis tanks. Hence, the use of a side extension shield appears almost mandatory if a reasonable attenuation is required for a clustered module configuration. These results are summarized in Tables 6-5 and 6-6.

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8.0 DRAWINGS

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Figure 3-1. NR-1 Reactor, Horizontal Cross Section

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Figure 3-2. NR-1 Reactor, Vertical Cross Section

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STATION	DESCRIPTION	FLOW	TEMP.	PRESSURE
4	Pump Discharge	296 lb/sec	60 °R	1315 psia
5	Nozzle Manifold	246	60	1285
6	Nozzle Tube Exit	246	170	885
7	Reflector Inlet Plenum	239	170	885
8	Reflector Exit Plenum	239	220	810
9	Peripheral Shield Exit Plenum	239	222	810
10	Dome End Plenum	289	235	805
11	Support Plate Exit Plenum	289	237	800
12	Core Inlet Plenum	289	265	780
13	Fuel Exit Plenum	285	4500	625
14	Core Exit Plenum	289	4500	625
15	Nozzle Chamber	285	4500	625
19	Support Element Control Valve	50	60	1285
20	Support Element Inlet	32	60	1095
21	Support Element Turn	32	250	1078
22	Support Element Exit Plenum	32	400	810
23	Cold Gas Tap Off-Inlet	7	170	885
25	Bleed Port Hot Gas Inlet	4	4500	625

Figure 3-3. NR-1 Cooled Periphery State Point Diagram (100 Percent Power, 100 Percent Flow, 37 Percent Void Fuel - Nominal Tie Tube Flow)

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STATION	DESCRIPTION	FLOW	TEMP.	PRESSURE
4	Pump Discharge	178 lb/sec	60 °R	765 psia
5	Nozzle Manifold	148	60	745
6	Nozzle Tube Exit	148	170	543
7	Reflector Inlet Plenum	144	170	543
8	Reflector Exit Plenum	144	230	493
9	Peripheral Shield Exit Plenum	144	231	493
10	Dome End Plenum	174	243	490
11	Support Plate Exit Plenum	174	244	487
12	Core Inlet Plenum	174	285	475
13	Fuel Exit Plenum	171	4550	376
14	Core Exit Plenum	174	4500	376
15	Nozzle Chamber	171	4500	376
19	Support Element Control Valve	30	60	745
20	Support Element Inlet	19	60	665
21	Support Element Turn	19	308	661
22	Support Element Exit Plenum	19	525	493
23	Cold Gas Tap Off-Inlet	4	170	543
25	Bleed Port Hot Gas Inlet	3	4500	376

Figure 3-4. NR-1 Cooled Periphery State Point Diagram, (60 Percent Power, 60 Percent Flow, 37 Percent Void Fuel - Nominal Tie Tube Flow)

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Figure 3-5. Flow Distribution Diagram (100 Percent Power, 100 Percent Flow, 37 Percent Void Fuel - Nominal Tie Tube Flow)

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Figure 3-6. NR-1 Lateral Support System

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Figure 3-7. NR-1 Fuel Cluster Assembly

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	100% POWER 100% FLOW SOL & EOL			60% POWER 60% FLOW SOL & EOL		
	W	P	T	W	P	T
A. TIE TUBE FEED SYSTEM	50	1285	60	19	745	60
B. REFLECTOR INLET	239	885	170	144	543	170
C. NOZZLE CHAMBER	285	625	4500	171	376	4500
D. HOT BLEED	4	625	4500	3	376	4500
E. COLD BLEED	7	885	170	4	543	170

W = FLOW RATE (LB/SEC)

P = PRESSURE (PSIA)

T = TEMPERATURE ($^{\circ}$ R)

SOL = START OF LIFE

EOL = END OF LIFE

Figure 3-8. Reactor Engine Interface Data with Constant Tie Tube System Flow, Design Point and Throttled Condition at Start of Life and End of Life

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	START OF LIFE			END OF LIFE		
	W	P	T	W	P	T
A. TIE TUBE FEED SYSTEM	10	1355	60	50	1224	60
B. REFLECTOR INLET	279	898	177	239	874	170
C. NOZZLE CHAMBER	285	625	4500	285	625	4500
D. HOT BLEED	4	625	4500	4	625	4500
E. COLD BLEED	7	885	170	7	874	170

W = FLOW RATE (LB/SEC)

P = PRESSURE (PSIA)

T = TEMPERATURE ($^{\circ}$ R)

Figure 3-9. Reactor Engine Interface Data with Variable Tie Tube System Flow, Design Point at Start of Life and End of Life

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Figure 4-1. NR-1 Beryllium Reflector, Coolant Hole Diameter in Inches

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Figure 4-2. NR-1 Beryllium Reflector, Flow and Axial Temperature Distributions, Cooled Periphery

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Figure 4-3. NR-1 Beryllium Reflector, On-Seal Axial Heat Flux at Pyro Tile OD

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Figure 4-4. NR-1 Beryllium Reflector, On-Seal Axial Heat Flux to Beryllium

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Figure 4-5. NR-1 Beryllium Reflector, Material Axial Average Temperature Distribution

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Figure 4-6. NR-1 Beryllium Reflector, Axial Variation of Average and Differential Temperatures

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Figure 4-7. NR-1 Beryllium Reflector, On-Seal Sector
Temperature Distribution - Station 0

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Figure 4-8. NR-1 Beryllium Reflector, On-Seal Sector
Temperature Distribution - Station 12

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Figure 4-9. NR-1 Beryllium Reflector, On-Seal Sector
Temperature Distribution - Station 38

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Figure 4-10. NR-1 Beryllium Reflector, On-Seal Sector
Temperature Distribution - Station 52

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Figure 4-11. NR-1 Beryllium Reflector, On-Seal Sector Temperature Distribution - Station 26

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Figure 4-12. NR-1 Beryllium Reflector, Off-Seal Sector Temperature Distribution - Station 26

Figure 4-13. NR-1 Beryllium Reflector, Inner Tie Bolt Hole
Local Temperature Distribution - Station 26

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Figure 4-14. NR-1 Beryllium Reflector, On-Seal Sector Temperature Distribution - Station 26 (Seals adjacent to Pyro Tile)

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Figure 4-15. NR-1 Beryllium Reflector, Beryllium Maximum Temperature Versus Heating Rate

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Figure 4-16. NR-1 Beryllium Reflector, Effect of Beryllium Heating Rate on Average and Differential Temperatures

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Figure 4-17. NR-1 Beryllium Reflector, Maximum Beryllium Materials Temperature Versus Relative Coolant Flow

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Figure 4-18. NR-1 Beryllium Reflector, Effect of Heat Flux from Seals on Beryllium Average and Differential Temperatures

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Figure 4-19. NR-1 Beryllium Reflector, Effect of Control Drum Annulus Flow on Beryllium Average and Differential Temperature

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Figure 4-20. NR-1 Beryllium Reflector, Effect of Heat Flux from Single Seal on Beryllium Average and Differential Temperatures

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Figure 4-21. NR-1 Beryllium Reflector, Temperatures During 150°R/Sec Startup

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Figure 4-22. NR-1 Beryllium Reflector, Axial Location of Maximum Material Temperature Difference During 150°R/Sec Startup

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Figure 4-23. NR-1 Beryllium Reflector, On-Seal Sector Temperature Distribution at Worst Time and Station During 150°R/Sec Startup

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Figure 4-24. NR-1 Beryllium Reflector, On-Seal Sector Temperature Distribution, Throttled 60 Percent Flow - Station 26

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Figure 4-25. NR-1 Beryllium Reflector, Axial Temperature Distribution - Cooled Periphery

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Figure 4-26. NR-1 Beryllium Reflector, Radial Temperature Profile - Cooled Periphery - Station 28

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Figure 4-27. NR-1 Beryllium Reflector, Cooled Periphery, Lateral Support System Temperature Distribution - Station 25

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Figure 4-29. NR-1 Beryllium Reflector, Cooled Periphery, Lateral Support System Temperature Distribution - Station 15.3

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Figure 4-30. NR-1 Beryllium Reflector, Cooled Periphery, Lateral Support System Temperature Distribution - Station 32.5

Figure 4-31. NR-1 Beryllium Reflector, Cooled Periphery, Lateral Support System Temperature Distribution - Station 50

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Figure 4-32. Hot Periphery Axial Temperature Distribution

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Figure 4-33. NR-1 Beryllium Reflector, Hot Periphery Radial Temperature Profile - Station 28

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Figure 4-34. NR-1 Beryllium Reflector, Hot Periphery Lateral Support System Temperature Distribution

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Figure 4-35. NR-1 Axial Temperature Distribution - 5000 MW

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Figure 4-36. NR-1 Axial Pressure Distribution, 5000 MW

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Figure 4-37. NR-1 Axial Temperature Distribution, 3000 MW

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Figure 4-38. NR-1 Axial Pressure Distribution, 3000 MW

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Figure 4-39. NR-1 Nominal Maximum Fuel Temperature Versus Power Factor

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Figure 4-40. Effect of Chamber Pressure on Interelement Surface Corrosion Without Bore Induced Surface Effects

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Figure 4-41. Maximum Material Temperature Deviation from Average Radial Temperature

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TIE TUBE GEOMETRY

TIE TUBE GEOMETRY SELECTED FOR MAXIMUM REACTIVITY CONTROL SPAN

TIE TUBE GEOMETRY SELECTED FOR MINIMUM FLOW IMPEDANCE

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Figure 4-42. Tie Tube Geometry

Figure 4-43. Tie Tube Hydrogen Flow Rate Versus Mass Storage and Reactivity
Worth - NR-1 (Reactor Power 100 Percent, Flow 100 Percent)

Figure 4-44. Tie Tube Hydrogen Flow Rate Versus Mass Storage and Reactivity
Worth - NR-1 (Reactor Power 60 Percent, Flow 60 Percent)

Figure 4-45. NR-1 Steady-State Operating Map (Maximum Tie Tube Flow)

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Figure 4-46. Tie Tube Flow Rates and Tie Tube Feedback Reactivity (Maximum Tie Tube Flow)

Figure 4-47. NR-1 Startup Transient - Preliminary Fixed Tie Tube Inlet Flow Impedance

Figure 4-48. Tie Tube Transient Temperature and Flow Characteristics During Startup - Preliminary

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Figure 4-49. Reactivity Feedback and Drum Motion for Startup for 13 Lb/Sec Tie Tube Flow at 100 Percent Power and Flow - Preliminary

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Figure 4-50. NR-1 Shutdown Transient - Preliminary Fixed Tie Tube Inlet Flow Impedance

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Figure 4-51. Tie Tube Transient Temperature and Flow Characteristics During 150°R/Sec Shutdown - Preliminary

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Figure 4-52. Reactivity Feedback and Drum Motion for Shutdown for 13 Lb/Sec Tie Tube Flow at 100 Percent Power and Flow - Preliminary

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Figure 5-1. NR-1 Radial Power Distributions. Comparison of NR-1 and NRX Spectrum-Weighted Cross Sections

Figure 5-2. Integrated Fission Fraction in NR-1 Versus Energy. Comparison of NR-1 and NRX Spectrum-Weighted Cross Sections

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Figure 5-3. NR-1 Axial Hydrogen Worth Profile

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Figure 5-4. Axial Hydrogen Density Profiles in the NR-1 Fuel Channels and Tie Tubes

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Figure 5-5. Comparison of NR-1 and NRX Control Span Requirements

Figure 5-6. NR-1 Control Drum Span Versus Boron-10 Surface Density in Control Plates

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Figure 5-7. NR-1 Control Drum Span Versus Drum Bank Position

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Figure 5-8. NR-1 Radial Power Distribution (Average Azimuthal Edge Power)

Figure 5-9. NR-1 Power Distribution (Azimuthal Variation at Core Periphery)

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Figure 5-10. Effect of Operating Conditions on NR-1 Radial Power Distribution

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Figure 5-11. NR-1 Radial Power Distribution at 90 and 130 Degrees (Average Aximuthal Edge Power)

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Figure 5-12. NR-1 Axial Power Distribution

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Figure 5-13. NR-1 Tie Tube Flow - Reactivity Relationship

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Figure 5-14. NR-1 Radial Power Distribution Variation. Tie Tube Versus Drum Reactivity Compensation

Figure 5-15. NR-1 Reactor Weight Versus Fueled Core Diameter (Control Drum Span = 9.0%)

Figure 5-16. NR-1 Reactor Weight Versus Fueled Core Diameter (Control Drum Span = 6.0%)

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Figure 5-17. NR-1 Reactor Weight Versus Fueled Core Diameter (Control Drum Span = 3.0\$)

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Figure 6-1. NR-1 Configurations for Radiation Analysis

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Figure 6-2. NR-1 One-Dimensional Axial Mockup (With Shield)

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Figure 6-3. Three-Module Clustered Engine Configuration

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	CENTERED TANK	OFF-AXIS ADJACENT TANK
ϕ	32°	19°
H	411cm	1067cm

Figure 6-4. Propellant Tank Geometrical Model for Point Kernel Tank Heating Calculations

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A. PROJECTION OF TANKS ON SPHERICAL SURFACE - THREE DIMENSIONAL SKETCH

B. PROJECTION OF TANKS ON SPHERICAL SURFACE - θ, ϕ MAPPING

Figure 6-5. Defining Geometry for Solid Angles Subtended by Propellant Tanks

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Figure 6-6. Geometric Model for Monte Carlo Propellant Heating Calculations

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Figure 6-7. Analytical Methods and Results

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Figure 6-8. NR-1 Radial Traverse Fast Neutron Flux

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Figure 6-9. NR-1 Radial Traverse Thermal Neutron Flux, $E < 1.86$ ev

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Figure 6-10. NR-1 Radial Traverse Gamma Dose Rate

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Figure 6-11. NR-1 Radial Traverse Heating Rates

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Figure 6-12. NR-1 Axial Traverse Fast Neutron Flux (Without Shield)

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Figure 6-13. NR-1 Axial Traverse Thermal Neutron Flux (Without Shield), $E < 1.86$ eV

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Figure 6-14. NR-1 Axial Traverse, Fast Neutron Flux (With Shield)

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Figure 6-15. NR-1 Axial Traverse, Thermal Neutron Flux (With Shield), $E < 1.86$ ev

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Figure 6-16. NR-1 Axial Traverse Gamma Dose Rate (Without Shield)

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Figure 6-17. NR-1 Axial Traverse Gamma Dose Rate (With Shield)

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Figure 6-18. NR-1 Axial Traverse Heating Rates (Without Shield)

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Figure 6-19. Axial Traverse of NR-1 Region Heating Rate (Core Average)

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Figure 6-20. NR-1 Neutron Flux, Radial Traverse, $E > 2.87$ Mev, Effect of Varying Pressure Vessel Thickness and Material

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Figure 6-21. NR-1 Neutron Flux, Radial Traverse, $E > 1.0$ Mev, Effect of Varying Pressure Vessel Thickness and Material

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Figure 6-22. NR-1 Neutron Flux, Radial Flux, $E > 0.1$ Mev, Effect of Varying Pressure Vessel Thickness and Material

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Figure 6-23. NR-1 Neutron Flux, Radial Traverse $E < 1.86$ ev, Effect of Varying Pressure Vessel Thickness and Material

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Figure 6-24. NR-1 Gamma Dose Rate, Radial Traverse, Effect of Varying Pressure Vessel Thickness and Material

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Figure 6-25. NR-1 Neutron Flux, Axial Traverse, $E > 2.87$ Mev, Effect of Varying Pressure Vessel Material

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Figure 6-26. NR-1 Neutron Flux, Axial Traverse, $E > 1.0$ Mev, Effect of Varying Pressure Vessel Material

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Figure 6-27. NR-1 Neutron Flux, Axial Traverse, $E > 0.1$ Mev, Effect of Varying Pressure Vessel Material

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Figure 6-28. NR-1 Neutron Flux, Axial Traverse, $E < 1.86$ ev, Effect of Varying Pressure Vessel Material

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Figure 6-29. NR-1 Gamma Dose Rate, Axial Traverse, Effect of Varying Pressure Vessel Material

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Figure 6-30. NR-1 Comparison of Gamma Dose Rates for Aluminum and Inconel-718 Pressure Vessels (Radial Traverse)

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Figure 6-31. NR-1 Comparison of Gamma Dose Rates for Aluminum and Inconel-718 Pressure Vessels (Axial Centerline Traverse)

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Figure 6-32. NR-1 Comparison of Region Heating Rates for Aluminum and Inconel-718 Pressure Vessels (Radial Traverse)

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Figure 6-33. NR-1 Neutron Flux, Axial Traverse, $E > 2.87$ Mev,
With and Without the B_4C -Cu Cluster Plate

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Figure 6-34. NR-1 Neutron Flux, Axial Traverse, $E > 1.0$ Mev,
With and Without the B_4C -Cu Cluster Plate

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Figure 6-35. NR-1 Neutron Flux, Axial Traverse, $E > 0.1$ Mev,
With and Without the B_4C -Cu Cluster Plate

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Figure 6-36. NR-1 Neutron Flux, Axial Traverse, $E < 1.86$ ev,
With and Without the B_4C -Cu Cluster Plate

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Figure 6-37. NR-1 Gamma Dose Rate, Axial Traverse, With and Without the B_4C -Cu Cluster Plate

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Figure 6-38. NR-1 Axial Traverse Heating Rates, With and Without the B₄C-Cu Cluster Plate

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Figure 6-39. Comparison of NR-1 Shielded Versus Unshielded Core Gamma Dose Rate at 30 Feet from the Core Center

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Figure 6-40. Comparison of NR-1 Shielded Versus Unshielded Gamma Dose Rate at 30 Feet from the Core Center. (Extra-Core Secondary Gamma Are Included.)

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Figure 6-41. Comparison of NR-1 Shielded Versus Unshielded Fast Neutron Flux ($E > 0.1$ Mev) at 30 Feet from the Core Center

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Figure 6-42. Reactor Gamma Energy Deposition in On-Axis Tank from On-Axis Reactor (0 Degree Detector Plane)

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Figure 6-43. Reactor Gamma Energy Deposition in On-Axis Tank from On-Axis Reactor (60 Degree Detector Plane)

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Figure 6-44. Reactor Gamma Energy Deposition in Off-Axis Tank from On-Axis Reactor (0 Degree Detector Plane)

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Figure 6-45. Reactor Gamma Energy Deposition in Off-Axis Tank from On-Axis Reactor (60 Degree Detector Plane)

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Figure 6-46. Reactor Gamma Energy Deposition in Off-Axis Tank from On-Axis Reactor (120 Degree Detector Plane)

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Figure 6-47. Reactor Gamma Energy Deposition in Off-Axis Tank from On-Axis Reactor (180 Degree Detector Plane)

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Figure 6-48. Neutron Kinetic Energy Deposition in On-Axis Tank from On-Axis Reactor

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Figure 6-49. Neutron Kinetic Energy Deposition in Off-Axis Tank from On-Axis Reactor (0 Degree Detector Plane)

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Figure 6-50. Neutron Kinetic Energy Deposition in Off-Axis Tank from On-Axis Reactor (60 Degree Detector Plane)

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Figure 6-51. Neutron Kinetic Energy Deposition in Off-Axis Tank from On-Axis Reactor (120 Degree Detector Plane)

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Figure 6-52. Conceptual Shield Arrangement

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