Semiannual Technical Progress Report
for Period Ending March 1993

SPACE-R THERMIONIC
SPACE NUCLEAR POWER SYSTEM
Design and Technology Demonstration

Submitted to

UNITED STATES DEPARTMENT OF ENERGY
San Francisco Field Office
Contract: DE-AC03-92SF19441
May 1993

Submitted by
Space Power, Incorporated
621 River Oaks Parkway
San Jose, California 95134
(408) 434-9500
Fax: (408) 434-9891

DISCLAIMER
This report was prepared as an account of work sponsored by an agency of the United States
Government. Neither the United States Government nor any agency thereof, nor any of their
employees, makes any warranty, express or implied, or assumes any liability for the accuracy,
completeness, or usefulness of any information, apparatus, product, or process disclosed, or
representations that its use would not infringe privately owned rights. Reference
herein to any specific commercial product, process, or service does not necessarily constitute
endorsement, recommendation, or favoring by the United States Government or any agency thereof. The
views and opinions of authors expressed herein do not necessarily state or reflect those of the
United States Government or any agency thereof.

MASTER
DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

Space Power, Incorporated 621 River Oaks Parkway, San Jose, CA 95134-1907 (408) 434-9500 Fax (408) 434-9891
Table of Contents

Summary .................................................................. 2
1.0 Objective .......................................................... 6
2.0 SPACE-R Overview ........................................... 7
3.0 Task 1.2: Parametric Trade Study ......................... 10
  3.1 Objective and Trade Study Logic .......................... 10
  3.2 Point of Departure Design .................................. 10
  3.3 TFE .................................................................. 19
    3.3.1 Verification of Single Cell TFE Design Selection .......... 19
    3.3.2 TFE Material .................................................. 24
  3.4 Nuclear Fuel ....................................................... 26
  3.5 Emitter Material .................................................. 30
    3.5.1 Criteria for SPACE-R Emitter Material ................. 30
    3.5.2 Creep Strength of Refractory Material .................. 31
    3.5.3 SPACE-R Emitter Creep Life Limit for the Candidate Materials 36
  3.6 Core Neutronics and Reactor Safety ......................... 60
    3.6.1 Number of Cells per TFE .................................. 60
    3.6.2 Varying Number of TFEs .................................. 61
    3.6.3 Design Basis Accident ..................................... 70
    3.6.4 DBA Reactivity Control .................................... 71
    3.6.5 Radial Power Distribution ................................. 81
    3.6.6 Temperature Coefficients .................................. 86
    3.6.7 New Baseline Core .......................................... 88
    3.6.8 Neutron and Gamma Heating ............................. 88
  3.7 Moderator Stability and Containment ....................... 102
    3.7.1 Moderator Choices ......................................... 102
    3.7.2 Moderator Neutronics ....................................... 105
    3.7.3 Hydrogen Containment ..................................... 108
    3.7.4 Radiation Stability .......................................... 112
    3.7.5 Materials Compatibility .................................... 117
  3.8 Cesium Source .................................................. 124
    3.8.1 Central Cs Source .......................................... 124
    3.8.2 Integral Cs Reservoir ....................................... 127

i
Summary of Accomplishments
for the Reporting Period
from October 1992 to March 1993
for the
SPACE-R Thermionic Space Nuclear Power System
Design and Technology Demonstration Program
Summary

This Semiannual Technical Progress Report summarizes the technical progress and accomplishments for the Thermionic Space Nuclear Power System (TI-SNPS) Design and Technology Demonstration Program of the Prime Contractor, Space Power Incorporated (SPI), its subcontractors and supporting National Laboratories during the first half of the Government Fiscal Year (GFY) 1993. SPI’s subcontractors and supporting National Laboratories include: Babcock & Wilcox for the reactor core and externals; Space Systems/Loral for the spacecraft integration; Thermacore for the radiator heat pipes and the heat exchanger; INERTEK of CIS for the TFE, core elements and nuclear tests; Argonne National Laboratories for nuclear safety, physics and control verification; and Oak Ridge National laboratories for materials testing.

Parametric trade studies are near completion. However, technical input from INERTEK has yet to be provided to determine some of the baseline design configurations. The INERTEK subcontract is expected to be initiated soon. The Point Design task has been initiated. The thermionic fuel element (TFE) is undergoing several design iterations. The reactor core vessel analysis and design has also been started.

Parametric trade studies were performed in the following areas:

- TFE configurations including single cell and multicell designs
- Nuclear fuel material and configuration for safety
- Emitter material for a reliable, long life TFE design
- Preferred core neutronics design to meet functional and safety requirements
- Moderator choices for performance, stability, and containment
- Integral vs. central cesium source
- Reflector, control and safety element material, configuration and drive concept
- Actuators and bearings
- Radiation shield material and configuration optimization
- Reactor vessel material, mass, fabricability, reliability and performance.
- Thermal management subsystem including coolant material, piping, expansion compensator, EM pump, heat exchanger, heat pipes and radiating fins
- Electrical control
- Spacecraft integration and deployment
- System performance including TFE interconnection and decay heat removal.

Some issues have not been resolved yet. These issues will be resolved during the next reporting period with the INERTEK technical consultation to define the baseline system configuration.

TFE trade study results indicate that the single cell TFE with central Cs source(s) is a preferred designer for a reliable, long life system. Multicell TFESs can produce higher power density at high efficiency, but with a higher fuel burnup. When coupled with the high fuel smear density requirement because of the intercell spacing in multicell TFESs, this leads to greater fuel swelling and lower endurance. In addition, multicell TFESs have shown high degradation rate (about 25% per year) because of hydrogen leakage into the diode gap through the intercell region. The single cell TFE design presents significantly higher endurance/reliability potential because of the absence of intercell connection components. Topaz II single cell TFESs degraded only 0.5% per year over 20 months of operation.

Another advantage of the single cell TFE is its full testability with electric heaters. Because the fuel can be loaded after the final system assembly, the single cell TFE also provides greater safety and handling convenience. Therefore, the single cell TFE was chosen as the baseline design for the 40 kWe point design. The multicell TFE is attractive for a high power system (>50 kWe) with a relatively short lifetime requirement.

UO$_2$ was chosen as the baseline fuel material based on the extensive experience and database availability, materials compatibility, reliability and low risk. Safety measures being investigated include fixed or removable poison in the fuel cavity, burnable poison in moderator, and the water-resistant nut fuel. The monocrystal Mo-6% Nb alloy was chosen as the baseline emitter material.
Yttrium hydride contained in beryllium was tentatively chosen as the baseline moderator material. Technical consultation will be obtained form INERTEK for this choice. The zirconium hydride moderator has been developed and demonstrated up to two years by Topaz II system. However, a better hydrogen containment technology is needed for a 10 year life system with the zirconium hydride moderator because of its high hydrogen dissociation pressure and correspondingly high hydrogen loss rate of about 1% per year (5% reactivity loss in 10 years). The Be-YH$_{1.75}$ moderator has excellent potential for a long life system because of the very low hydrogen dissociation pressure (0.01 torr) of yttrium hydride. The hydrogen loss from the Be-YH$_{1.75}$ moderator is expected to be less than 0.1% in 10 years. The core radial power distribution can be flattened for optimum TFE performance with varying Be/YH$_{1.75}$ ratios across the core. In addition, the moderator temperature can be either positive or nearly zero, depending on the hydride-Be volume ratio in the core. It provides flexibility for the reactor control design.

A multiple cesium source concept with central Cs sources of the Topaz II type was identified as an attractive candidate for the preferred baseline configuration. The Topaz II central Cs source has been developed, and its performance has been tested extensively in CIS. The reliability and long-term endurance have been established. With multiple central Cs sources in the SPACE-R system, the single point failure mechanism will be eliminated. Even though the integral Cs reservoir allows a simpler core design with no Cs plumbing, a long life TFE performance is difficult to obtain because of the difficulty of outgassing and elimination of impurity gas accumulation. A long-term, acceptable incore performance of a TFE with an integral reservoir has not been demonstrated yet.

Recommended reactor configuration and materials were identified as a result of the parametric trade studies. For the 40 kWe system, 150 single cell TFEs with a 35 cm core length provide good thermionic and neutronic performance for a 10 year life. Component material, configuration and fabrication trades were also performed and preferred choices were identified.

The Point Design task was initiated based on the trade study results. Preliminary TFE layout was prepared. The single cell baseline TFE is being designed on a three-
dimensional parametric CAD system. Component fabrication and joining issues were also studied.

The reactor subsystem configuration is being defined. The core vessel structural analysis and design is progressing. Three-dimensional solid modeling of the reactor was initiated. Component thicknesses were calculated based on thermal/structural analysis, fabricability and material compatibility. The reactor vessel fabrication and assembly sequence was established.
1.0 Objective

The objective of this program is to design, develop, demonstrate, and advance the technology for thermionic space nuclear power system (TI-SNPS) for military applications. A 40 kWe TI-SNPS point design will be prepared, and key technologies supporting that design will be validated. The results of this program will be an assessed design of a 40 kWe-EOL space nuclear power system that:

1. Minimizes the potential safety risk to population, workers and environment (ALARA).
2. Provides the high reliability required by spacecraft designers formulating high value space systems (0.95) over system lifetime.
3. Provides the high performance necessary to enable high powered missions with currently available and planned near-term boosters and electric propulsion systems; 15-30 W/Kg EOL and compact enough to be incorporated into an Atlas II/Centaur launch vehicle to 800 Km circular orbits at 28° inclination.
4. Provides the proven lifetime potential of 18 months, with a near-term lifetime of 5 years and 10 year lifetime design goal.
5. Can be developed before 2000 at a minimum development cost and at minimum technical and budget risk based upon proven technology.

Phase 1 will provide for the performance of parametric trade studies and demonstration of key technologies, resulting in a preferred Conceptual Design for the TI-SNPS. The focus of the tasks is technology validation driven by the system design.

Phase 2, which is an option to the contract, will provide for the development of a detailed preliminary design and all supporting data, completion of a Preliminary Design Review (PDR), demonstration of technology readiness for key elements of the system, and completion of a draft flight Preliminary Safety Analysis Report (PSAR) covering the TI-SNPS and the mission payload to be defined before the Phase 2 initiation.
2.0 SPACE-R Overview

The Space Power Advanced Core-length Element-Reactor (SPACE-R) TI-SNPS power system concept is built on the foundation of demonstrated technology from the TOPAZ, RORSAT, and SNAP-10A programs. It is enhanced by modern technical advancements to produce a design that fully meets or exceeds all the desired requirements. The guiding principals of the design are:

- assured safety during all program phases,
- minimization of risk and cost of development,
- maximization of performance, and
- clear definition of program plan to achieve the desired performance goals.

These goals are met, under the constraints of the guiding principals, using an optimal mix of US and Russian technologies that incorporates the best features of both.

In brief, the SPACE-R design incorporates the proven UO₂ fueled thermionic converter, 800-900 K NaK heat transport system, stainless steel structure, beryllium metal reflector/control, and cold LiH shields. Safety and performance are enhanced by the use of the single-cell core length thermionic converters, which allow much of the fabrication and qualification testing to occur in an unfueled condition, and provide for ease of venting of fission products. Safety and performance are also enhanced by the use of beryllium contained YH₃ moderator, which can produce a zero moderator temperature coefficient as well as reduced release of hydrogen. These and other enhancements incorporated in the design represent only minimal increases in development risk of the concept which are justified by the large potential payoffs. The maturity level of the present concept design and technology is supported by the preponderance of flight-tested and/or extensively ground-tested components on which the SPACE-R design is based.

The SPACE-R technical features include the following:

1) Utilization of the CIS (formerly USSR) developed self-venting UO₂ fuel in Topaz II type core-length converters. The Topaz II type single cell thermionic fuel
elements are fully testable. The monocrystal moly alloy emitter (with CVD 110 W\textsuperscript{184} overlay) of Topaz II and the moly collector are slightly increased in thickness and diameter, and slightly shortened to permit an output power increase from 180 to 320 watts peak electric per element. The shorter core, better end reflectors and fuel axial redistribution resulting from higher power density and lower fuel smear density also improves the axial power flattening and contributes to the higher power output. The thermal power of SPACE-R TFE’s is about 15% greater than in Topaz II TFE’s.

2) Good reflector leakage control is maintained in the larger 46 cm diameter SPACE-R core, versus 26 cm diameter Topaz core, by diluting the hydride moderator with approximately 50% beryllium. The beryllium also serves as the primary hydrogen containment barrier. It’s effectiveness is enhanced by substituting YH\textsubscript{1,75} for the ZrH\textsubscript{1,85} of Topaz II. The use of YH\textsubscript{1,75} is made possible by the Be dilution and by a higher fuel loading. Use of YH\textsubscript{1,75} decreases the hydrogen overpressure of ZrH\textsubscript{1,85} by 4 orders of magnitude. The use of Be-clad YH\textsubscript{1,75} also alters the moderator temperature coefficient of reactivity from positive in Topaz I and II to essentially zero in SPACE-R thus enhancing the core safety.

3) The SPACE-R core is reflected with 10 cm of beryllium metal that incorporates 12 rotating poison-backed drums for safety and reactivity control as in the SNAP-4, SNAP-6, Topaz I, and Topaz II reactors. Extra control is available if rotating half drums as in SNAP-10A are incorporated for the 3 safety drums without shield or mass penalty. The present design incorporates 1 launch safety rod to provide extra launch abort water immersion subcriticality. A design objective is to eliminate the need for this rod.

4) The SPACE-R is cooled by a pumped liquid metal coolant in accordance with established power plant practice. The preferred coolant is state-of-the-art liquid metal NaK-78 operating at 825-925 K, which are the temperature limits for reliable 20 year life stainless steel and superalloy systems. The NaK is circulated by a static high reliability electromagnetic pump. The pump consists of 3 separate segments connected electrically in parallel and hydraulically in series. Each
segment is capable of 50% of full pumping power. Thus, failure of one segment results in no change of system output or temperature. Failure of a second segment would decrease the reactor inlet temperature about 15 K and increase the outlet temperature 15 K with no loss of power output.

5) Radiator Heat pipes utilize potassium working fluid, and operate at conservatively low radial heat fluxes of about 20 watts/cm². Short transverse diode heat pipes are bonded to each of the 120 "header" heat pipes to spread heat between headers. High conductivity carbon-fibre-reinforced carbon fins are bonded to each transverse spreader. All headers and fins are redundant. The SPACE-R system can continue to operate at full power after nearly 50% of the heat pipes have been destroyed. However, a reduced system life is expected in this case. The radiator also serves as a meteoroid bumper to scatter the projectile. This bumper plus a beryllium layer on NaK piping protect the primary NaK loop and components below the shield.

6) The SPACE-R shield incorporates a Be, B₄C and ZrH₁.₈ fast neutron scatter, absorption and gamma shield. Its primary purpose is to remove the heat generated by radiation absorption and to protect the LiH shield from excessive helium generation and consequent neutron damage swelling and pressure buildup. The radiation fluences entering the LiH in 10 years are reduced to those experienced by the cold (400 K) SNAP-10A and Topaz shields, in one year and 2 years of operation respectively, without damage. Due to the low temperature of operation and subsequent low dissociation pressures and low hydrogen diffusion rates, small meteorite damage to shield containment will not lead to system failure. Primary NaK piping is insulated and buried in the shield to protect the primary loop from debris and pellets.

7) Control remains simplified by maintaining constant power and temperature output from the reactor. Variations in payload demand is accommodated by a shunt regulator that is capable of converting excess power to heat for radiation to space.
3.0 Task 1.2: Parametric Trade Study

3.1 Objective and Trade Study Logic

The objective of this task is to establish the basis for the conceptual design through comparison of alternative components and system configurations. The optimum conceptual design is determined as a result of the parametric trade studies. System Functional Requirements should be considered to provide specific rationale and justification for the recommended components and configuration, including issues related to safety, reliability, performance, lifetime, testability, development risk, and development and recurring costs.

Figure 3.1-1 shows the parametric trade study logic. Fuel and emitter issues are studied first to establish preferred means for reliable thermal power generation for the required system life. The TFE issues are studied next to define the power conversion scheme. Then the reactor core component and ex-core elements are studied to establish the baseline reactor configuration. The reactor safety issues are also studied to satisfy the safety requirements for all credible accident scenarios. The balance of the system component designs are then evaluated. The overall system performance is evaluated along the way to satisfy the Functional Requirements. The baseline design will then be defined with the recommended components and configuration. The point design is developed under Task 1.3 based on the recommended baseline design.

3.2 Point of Departure Design

The Space Power Advanced Core-Length Element Reactor (SPACE-R) System is an incore thermionic, UO₂-fueled, pumped liquid metal (NaK) cooled, moderated core reactor power system. The baseline power level is 40 kWe. A summary of the SPACE-R system characteristics as proposed is described here to serve as the point of departure design for the parametric trade studies.

Figures 3.2-1 shows the SPACE-R system layout at 40 kWe (EOL) with a 10 m payload separation distance. The baseline 40 kWe power system consists of a Be/yttrium hydride moderated core with 150 TFE’s, a zirconium hydride neutron-gamma shield, a LiH
Figure 3.1 1 Parametric Trade Study Logic
Incore Thermionic Reactor Power System (40 kWe)

This is an incore thermionic reactor power system with UO₂ fuel, pumped liquid metal (NaK) cooling, and a moderated core. This baseline SPACE-R design generates 40 kWe (EOL) with 10 year life using 150 core-length, double-ended, single-cell TFE's. The radiator configuration and the shield cone angle are chosen to maximize a 10m separation between the reactor core and the payload (not shown). This system allows a full power operational test of the entire assembly by electric heating before fuel is loaded. A low cost, low-risk development program to produce this design is possible through extensive use of proven SNAP, LMFBR, and Soviet Topaz II technologies.

Figure 3.2-1  SPACE-R System Layout at 40 kWe (EOL) with a 10 m Payload Separation Distance
neutron shield, a primary NaK coolant loop with a three-segmented EM pump, and radiator heat pipes with high thermal conductivity carbon-carbon radiating fins. The entire NaK coolant loop is protected by thick Be armor for survivability.

The key feature of the SPACE-R system is the core-length, single-cell thermionic fuel element (SCTFE). The open end of the emitter cavity permits an electrical heater to be inserted for full power operational testing of each TFE and the entire reactor assembly before the UO₂ fuel is loaded. The technology basis for the single cell TFE has been demonstrated by CIS Topaz II power system, and is being verified by the Thermionic System Evaluation Test (TSET) program. The TSET program is part of a comprehensive joint thermionic Space Nuclear Power (SNP) program between the United States Air Force, the Strategic Defense Initiative Organization (SDIO) and the Department of Energy (DOE). The SPACE-R SCTFE is largely based on the existing Topaz II TFE design with modifications to meet the current RFP requirements.

Table 3.2-1 shows the characteristics of the baseline 40 kWe (EOL), 44 kWe (BOL) system design as proposed. The overall system efficiency is 7.2%. The radiator area is 28 m² for the 44 kWe (BOL) design. The system is 6.4 m long with the maximum diameter of 2.55 m for 10 m separation. A total of 150 single cell TFE's are used in the 46 cm (D) x 35 cm (L) cylindrical reactor core. The maximum/average emitter temperature at the core center is 1873/1823 K, and NaK coolant temperature ranges from 825 (inlet) to 925 K (outlet). The Net power output voltage to the payload is 30 V with center grounding (i.e. ±15V from ground) at 1520 Amps DC BOL, excluding the pumping power. A power loss of 3.6% is allowed through the bus bar. UO₂ fuel is used in the monocrystal Mo-7% Nb emitter cavity. A combination of the "strong" emitter (monocrystal) and the "weak" fuel (average smear density of 0.7) is utilized to reduce the emitter creep for the 10 year system life goal. The emitter material and thickness, the fuel smear density with acceptable enrichment, and the Be/hydride moderator composition are optimized to obtain a reliable, long life system design with adequate core reactivity and control margins. A 5-mil thick (110) tungsten layer is applied on the emitter surface to provide efficient thermionic performances.
<table>
<thead>
<tr>
<th>Table 3.2-1</th>
<th>Characteristics of 40 kWe Point of Departure Design</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reactor Thermal Power</td>
<td>611 kWt</td>
</tr>
<tr>
<td>Net Power Output to payload</td>
<td>44 kWe (BOL), 40 kWe (EOL)</td>
</tr>
<tr>
<td>System Efficiency</td>
<td>7.2%</td>
</tr>
<tr>
<td>Core Diameter/Length</td>
<td>46/35 cm</td>
</tr>
<tr>
<td>Power Conversion</td>
<td>Single Cell TFE's in Moderated Core</td>
</tr>
<tr>
<td>Number of TFE's</td>
<td>150</td>
</tr>
<tr>
<td>Number of Drivers</td>
<td>0</td>
</tr>
<tr>
<td>Avg. Emitter/Collector Temp.</td>
<td>1823/900 K</td>
</tr>
<tr>
<td>Axial Thermal Power Ratio</td>
<td>0.7 (end/midplane)</td>
</tr>
<tr>
<td>Radial Thermal Power Ratio</td>
<td>0.7 (min/max)</td>
</tr>
<tr>
<td>Reactor Output V/A</td>
<td>30V/1520 A DC</td>
</tr>
<tr>
<td>Fuel</td>
<td>( \text{UO}_2 )</td>
</tr>
<tr>
<td>( \text{U}^{235} ) Loading</td>
<td>84 kg (93% Enriched)</td>
</tr>
<tr>
<td>Metal Burnup</td>
<td>3.2% in 10 yrs.</td>
</tr>
<tr>
<td>Smear Density</td>
<td>0.7</td>
</tr>
<tr>
<td>Moderator</td>
<td>Be-YH(_{1.75})</td>
</tr>
<tr>
<td>Reflector</td>
<td>Be</td>
</tr>
<tr>
<td>Number of Control Drums</td>
<td>9 (10.4 cm Dia. Be + B(_4)C)</td>
</tr>
<tr>
<td>Safety Drums</td>
<td>3 (10.4 cm Dia. Be + B(_4)C)</td>
</tr>
<tr>
<td>Incore Safety Rods</td>
<td>1 (B(_4)C with Be/YH(_x) Follower)</td>
</tr>
<tr>
<td>Heat Rejection Primary Loop</td>
<td>EM Pumped NaK (6.45 kg/s)</td>
</tr>
<tr>
<td>Secondary Loop (Radiator)</td>
<td>K Heat Pipes</td>
</tr>
<tr>
<td>Avg. Radiator Temperature</td>
<td>815 K</td>
</tr>
<tr>
<td>Radiator Area</td>
<td>28 ( \text{M}^2 )</td>
</tr>
<tr>
<td>Shield</td>
<td>LiH + ZrH(_x)</td>
</tr>
<tr>
<td>Reactor Mass</td>
<td>621 kg</td>
</tr>
<tr>
<td>Shield Mass (10M)</td>
<td>569 kg</td>
</tr>
<tr>
<td>System Mass (10M)</td>
<td>2170 kg</td>
</tr>
</tbody>
</table>
The moderator is $\text{YH}_{1.75}$ in Be. The $\text{YH}_{1.75}$/Be moderator concept is a key innovation developed by SPI, which provides greatly improved nuclear, control and safety performance characteristics in a reasonably small core with reliable hydrogen containment by Be.

The reactor is controlled by nine control drums which are gang-driven by two redundant drive shaft-motors. The gang drive mechanism, which is also incorporated in the Topaz II system, provides higher reliability through simplicity and redundancy than nine independent drive mechanisms. Three safety drums with independent drive motors are used for safety shutdown. In addition, a central safety rod is used for launch/reentry/immersion/compaction safety.

The reactor is cooled by the EM-pumped NaK coolant. The EM pump consists of three independent segments, each with 50% full capacity for reliability, thus enabling full power operation with one segment failure. Full power operation can also be maintained with 2 pump segment failures with an increase in the coolant exit temperature from the core from 925 K to 940 K.

The $^{235}$U inventory is 84 kg. The fuel metal burnup is estimated to be 3.2% in 10 years. The average fuel smear density in the emitter cavity is 0.7. After the system startup, the fuel will be redistributed along the axial direction within the emitter cavity with a thin fuel shell at the TFE center (45% smear density at 1873 K) where the temperature is the highest, and thicker fuel shell at TFE ends (89% smear density at 1750 K). Thus a long TFE life is expected with the distributed fuel in a single cell TFE. The reactivity change due to the fuel redistribution is considered accordingly in the core nuclear design.

The single cell TFE and the core structure is shown schematically in Figure 3.2-2. Only one TFE is shown for clarity. The emitter cavity of the TFE contains UO$_2$ fuel (redistributed after the initial system startup), BeO, and Be axial reflectors with thermal radiation shields, fission product oxide traps and fission gas vent. Each TFE with plasma-coated alumina layer on the outer collector tube is inserted into a sheath tube welded into the core calandria structure which contains the NaK coolant space and moderator segments. The contact space between the TFE and the calandria tube wall is filled with low pressure (~50 kPa) helium gas.
Figure 3.2-2  Single Cell TRIG and the Core Structure
torr) helium for thermal conduction to cool the TFE by NaK in the calandria. The TFE is slip-fit into the calandria hole. The He gap is less than 2 mils during operation. Helium is used to reduce the contact resistance between the TFE outer surface and the calandria tube.

The He and NaK boundaries in the reactor core are protected against natural and hostile hazards by the 10 cm thick Be radial reflector, the 6 cm thick upper Be axial armor plate, and by ZrH$_{2+x}$ and LiH shields located below the core. Therefore the possibility of the He or the NaK boundary puncture in the reactor core by outside hazards are minimized.

Each assembled SCTFE without fuel can be fully tested with electrical heat in the"Rig" before core assemblies. After the TFE insertion into the core structure without fuel, the TFE lead connections are made. The fuel can be transported separately from the completed reactor power system assembly and installed in the core right before it is launched into space, providing added unique safety and convenience in handling.

The assembled TFE-core structure is highly tolerant to shock, vibration and acceleration. The emitter and the collector are free to move independently relative to the core structure in the axial direction through the extensive use of spacers and bellows springs. The stress buildup in insulator seals is thus prevented. In addition, the relative lateral movement of the TFE components is prevented by the use of roller bearing spacers, and especially by the insulator spacers at TFE ends. The active fueled zone of the TFE is 35 cm long.

The reactor core is reflected with 10 cm of beryllium metal radially and 6 cm of BeO axially. The end reflectors operate at temperatures between the coolant temperatures of 825 K inlet and 925 K outlet. The radial reflectors operate at 800 K. The temperature of the radial reflector can be decreased to 700 K with 0.1 cm of multi-foil insulation between the core and reflector. The baseline reactor has nine control drums and three safety drums.
located in the radial beryllium reflector matrix. These drums are 10.4 cm in diameter, and have B$_4$C poison located near the outer periphery for the reactivity control.

The neutron and gamma shields are designed to meet the RFP requirement of $10^{13}$ nvt (fast)/year and $10^5$ Rad (Si)/year which translates into $10^{14}$ nvt (fast) and $10^7$ Rad (Si) for the payload in 10 years. Shield mass decreases with separation distance between reactor and payload but extension boom and low voltage bus bar mass increases with separation. The shield assembly consists of an upper borated stainless steel plate, a zirconium hydride neutron/gamma shield, a boron carbide thermal neutron absorber region, a LiH neutron shield, and a lower borated stainless steel plate. The upper borated stainless steel plate is used to serve as the major reactor support member and to reduce the gamma heating load on the hydride shields. The lower borated stainless steel plate is used to serve as the support plate for mounting of drum drive actuators and the radiator support structure and to reduce the secondary gamma flux from the shield interior cavity.

The heat rejection subsystem in the SPACE-R system consists of two heat transport loops connected to the reactor core in series: the pumped NaK primary loop and the secondary radiating heat pipes. The heat generated in the reactor is carried out of the core by the NaK coolant to a heat exchanger located behind the LiH shadow shield and on the center axis of the radiator cone. Heat is transferred, at the heat exchanger, from the pumped primary loop to a heat pipe-fed radiator circuit. The reject heat is distributed over the radiator surface and radiated to space.
3.3 TFE

3.3.1 Verification of Single Cell TFE Design Selection

A brief investigation was made to verify the preference of a single-cell TFE design for SPACE-R.

Configuration

Figure 3.3-1 Shows the difference between single cell and multicell TFEs schematically. The core-length single cell TFE with electric power taken off either end is limited to about 40 cm in length because of resistive loss through electrodes. At longer lengths, emitter and collector thicknesses must be increased to reduce internal resistance losses due to the low voltage, high current power in each cell. There are also current limitations due to induced magnetic effects. However, for long life systems the power density and therefore current density are restricted because of fuel burnup limitations in either a single cell or a multicell TFE design. Also, 35 cm to 40 cm of TFE length is adequate to meet criticality requirements in a single cell, continuously fueled TFE without intercell gaps as in a multicell TFE.

Because of higher total TFE voltage, lower current, and shorter cells, multicell TFEs can produce higher power density at higher efficiency than single cell TFEs. Therefore, thinner emitter and collector walls can be used in multicell TFEs. Smaller number of multicell TFEs will be needed to meet the output power requirement because of the higher power density. However, this leads to a high fuel burnup requirement for a long life system. When coupled with the requirement to use higher fuel smear densities in multicell TFEs to meet the reactivity requirement because of the intercell spacing effect on criticality, this leads to greater fuel swelling, lower endurance and a need for stronger emitters for multicell TFEs.
Figure 3.3-1  Schematic comparison of single cell and multicell TFEs
Endurance and Reliability

A major feature of the single cell TFE is the absence of intercell connection components (especially ceramic seals) in the reactor core. Only the emitter tube, the collector tube, and nuclear fuel are present in the core. All the ceramic seals and other components are located outside of the hot core region, thus presenting significantly higher endurance/reliability potential than the multicell TFE.

The information gathered indicates that the TOPAZ-I multicell system in its most advanced form degraded at the rate of about 25% per year of operation. See Figure 3.3-2. The degradation was largely due to hydrogen leakage into the Interelectrode Gap (IEG) and reaction with oxygen. Thus, a water cycle was set up to transport tungsten from the emitter to the collector. Hydrogen also caused some altering of Cs pressure. The leakages of H₂ and O₂ into the IEG are primarily through the intercell areas of the multicell system.

The most modern experimental multicell TFEs made and tested by LUCH include a continuous trilayer sheath enclosing the full TFE with the Al₂O₃-He slip fit to the outer sheath as in Topaz II single cell TFEs. This arrangement should reduce hydrogen contamination. Preliminary testing of these elements for nearly a year showed about 2 percent degradation per year. However, to our knowledge they were not yet tested with a hot hydride hydrogen source.

About a thousand Topaz II single cell elements were fabricated and many were tested in flight-prototypic reactors with NaK and ZrH₂ at prototypic conditions. These units degraded only 1/2 percent per year over nearly 20 months of operation. Post irradiation examination showed very little tungsten transfer. The projected life of Topaz II TFEs is at least five years. The single crystal emitter and full length collector tubes prevent fission products and hydrogen from migrating into the interelectrode gap. Also, fission product venting configuration is simple in single cell TFEs because of only one fuel cavity in each TFE. Note that fission product vent passages for multicell TFEs are complex: fission products pass through very small passages from hot fuel region to cold intercell region in each cell leading to potential plugging of vent passages by condensible fission products.
ESTIMATE OF PRESENT TFE PERFORMANCE DEGRADATION
Based Upon Extrapolation of 1–3 Year Tests

Figure 3.3-2  TFE Endurance
Testability

The single cell TFE can be tested to full power output with an electrical heater prior to nuclear fuel loading. In addition, the entire reactor power system can be tested at full power and temperature using electrical heaters simulating nuclear fuel as demonstrated by CIS Topaz II and US TSET Programs. After assembly, multicell TFEs can only be tested for very limited functionality, such as leak tightness and isothermal cycling. Because of the diode gap in each cell, the electrical connection between cells is difficult to check out after the multicell TFE assembly. A reverse pulsing is one possible way for a limited electrical connection check out. Incore testing will be needed for even a reduced performance testing for multicell TFEs.

Handling and Safety

Single cell TFEs and the entire power system with single cell TFEs can be fabricated, assembled, tested, transported and stored without nuclear fuel, thus minimizing handling and safeguard concerns. In comparison, each fabricated multicell TFE always contains highly enriched nuclear fuel.

Cost

Preliminary estimate of TFE fabrication costs indicates that there could be a differential of 50% to over 100% increase in fabrication and assembly costs of multicell TFEs over single cell TFEs because of the sheath insulator, intercell components, and more refractory metal components and welds. TFE test cost is also higher for multicell TFEs because of the need for incore testing for any performance or life test.

In summary, the single cell TFE is recommended as the baseline TFE for the 40 kWe point design. The multicell TFE is attractive for a higher power system (>50 kWe) with a relatively short lifetime requirement.
3.3.2 TFE Material

Material options and fabrication trades for the baseline TFE were studied. Materials considered for the emitter include:

- Single crystal Mo-3%Nb; A material with a well established development history, but of limited life for TI-SNPS purposes.
- Mo-6%Nb and Mo-7%Nb; Process established in Russia, long life, but difficult to fabricate.
- CVD W; Process established in US, high cost associated with W\textsuperscript{184} and impurity diffusion concerns.
- W-HfC wire reinforced Mo; lower W\textsuperscript{184} costs, undemonstrated process.
- Single Crystal W alloy; Process established in Russia, high W\textsuperscript{184} costs.

Mo-Nb is the current baseline for near-term system development. However, a change in emitter material choice will not significantly affect reactor design, even if the change is done at a much later date.

The collector material candidates include single crystal Mo and polycrystal Mo. Because of H\textsubscript{2} diffusion concerns through the polycrystalline structure, single crystal Mo is preferred, even though there is some cost penalty.

The material considered for the inter-electrode gap spacers include scandium oxide, yttrium oxide, and sapphire. According to Russian test data, scandium oxide has the highest thermal and electrical stability with the lowest mass loss in cesium vapor at operating temperature and voltage gradient. See Figure 3.3-3. Scandium oxide was chosen as the baseline in-gap spacer material. Several domestic sources for the scandium oxide spacers were identified. Cost estimates for spacer fabrication were acceptable.

The materials considered for the TFE bellows include SS321 and Nb. Because the bellows will be located outside the core region where temperature and radiation flux are low, SS321 is considered adequate. The thickness of the bellows wall will be such that grain growth does not result in a significant containment degradation.
1 - Sc$_2$O$_3$, 2 - BeO, 3 - Y$_2$O$_3$, 4 - Al$_2$O$_3$, 5 - JV Characteristics

Figure 3.3-3  Ceramic spacer mass loss rate in cesium at 1870 K
Polycrystal and single crystal alumina are candidates for the insulator seal. Single crystal alumina (sapphire) is the baseline seal material which is an established technology in US and Russia. To minimize the differential thermal expansion and subsequent stress in the braze layer between the seal and the seal ring, the seal ring will be Nb. Several metalizing processes and brazing materials are under consideration for the insulator seal. The brazed joint should resist corrosion in the high temperature cesium environment.

Various joining options are being studied for the TFE assembly. Ti brazing, TIG welding and diffusion bonding are under consideration for the high temperature Mo-Mo emitter closure. For the copper-Kovar electric lead connection, EB welding, TIG welding with filler, and silver alloy brazing are candidate processes. For various dissimilar joints between Kovar and refractory metal or stainless steel, Cu or Au alloy brazing, and EB or TIG weld-braze are being considered. Final choices will be made concurrently with the baseline component materials because joining processes are highly dependent upon the materials concerned.

3.4 Nuclear Fuel

The qualities which are looked for in nuclear fuel for incore moderated thermionic space power reactors are:

- High uranium density for reactivity in a small, leaky core
- High temperature stability
- Compatibility with the emitter and balance of system
- Predictable irradiation behavior over long periods

A comparison of some candidate nuclear fuel materials is shown in Tables 3.4-1 and 3.4-2.

An extensive database is available for UO$_2$. Its thermomechanical and radiation swelling behavior is also reasonably well characterized. The compatibility with emitter materials is also well established. The low thermal conductivity and relatively high vapor pressure are potential disadvantages. However, fuel redistribution due to these factors
facilitates the long life single-cell TFE design with thin fuel layer in high temperature regions and thick fuel layer in low temperature regions. The Russians specify oxide fuel for the Topaz reactor series, and the US TFE Verification Program is based on oxide fueled converters. The strongest point in favor of UO₂ is probably its predictability: no other fuel has anywhere near the database that UO₂ has. As long as a major thrust of the program is to reduce technical risk, this must be considered a deciding advantage. The baseline fuel choice for the SPACE-R is UO₂.

Two other fuel candidates were assessed for SPACE-R. The first is UN. The SP-100 Program has devoted much effort to the development of UN for use in that reactor. It has thermal conductivity eight times higher than UO₂. Test results indicate that this fuel has excellent irradiation behavior and it also has a higher usable uranium density than the oxide has. However, the nitrogen containment during long-term operations is a challenging issue because the fission gas venting should be provided at the same time for long life TFEs.

The second alternative is the rather large class of carbide or carbonitride fuels. These fuels were developed for application in the US nuclear thermal propulsion programs of the 1950s and 1960s. They have also been under development in Russia. They have very high temperature stability with low vapor pressure, and their suitability to heat a hydrogen propellant stream for thrust has been demonstrated. Because of its high melting point and low vapor pressure, the Uₓ-Zrᵧ-C fuel can maintain its original fuel shape during operation without redistribution. Therefore, this fuel is a promising candidate for a long-life dual-mode space power system with both nuclear thermal propulsion and electric power generation capabilities, when single-cell TFEs are used with hydrogen passages through the Uₓ-Zrᵧ-C fuel region. A dual mode version of SPACE-R is envisioned in which the space now occupied by UO₂ would be filled with wires of coated carbide fuel. The SPACE-R TFE is a core-length, single cell converter which could be modified to accept an axial flow of hydrogen propellant. The hydrogen would flow between the fuel wires in the interior of this emitter. In this mode, SPACE-R would function as a nuclear thermal propulsion system. Since the basic geometry would not be altered by this application it also functions as a thermionic power system after the nuclear thermal propulsion mode. However, Uₓ-Zrᵧ-C system has limited database at high burnup and high uranium content. Compatibility with emitter material candidates needs to be established.
Table 3.4-1 Properties of Fuel Data by "LUCH", Russia (1991)

<table>
<thead>
<tr>
<th>Material</th>
<th>Density g/ce</th>
<th>U Content</th>
<th>Melting Point (°C)</th>
<th>Thermal Conductivity W/m-K</th>
<th>Thermal Expansion Coefficient at 1500°C (K⁻¹) x 10⁻⁶</th>
<th>Elastic Modulus at 1500°C (10⁹ kg/mm²)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>% by mol</td>
<td>g/ce</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>UO₂</td>
<td>10.97</td>
<td>88.14</td>
<td>9.67</td>
<td>2805</td>
<td>2.6</td>
<td>11.5</td>
</tr>
<tr>
<td>UC</td>
<td>13.63</td>
<td>95.19</td>
<td>12.97</td>
<td>2525</td>
<td>20</td>
<td>15</td>
</tr>
<tr>
<td>UC₂</td>
<td>11.67</td>
<td>90.80</td>
<td>10.70</td>
<td>2450</td>
<td>17</td>
<td>16.5</td>
</tr>
<tr>
<td>UN</td>
<td>14.32</td>
<td>94.44</td>
<td>13.52</td>
<td>2850</td>
<td>22</td>
<td>1.0</td>
</tr>
<tr>
<td>UP</td>
<td>10.23</td>
<td>88.48</td>
<td>9.05</td>
<td>2610</td>
<td>19.5</td>
<td>9.2</td>
</tr>
<tr>
<td>US</td>
<td>10.87</td>
<td>88.15</td>
<td>9.58</td>
<td>2480</td>
<td>13</td>
<td>10</td>
</tr>
<tr>
<td>U₃Si</td>
<td>15.51</td>
<td>96.23</td>
<td>14.98</td>
<td>930</td>
<td>--</td>
<td>--</td>
</tr>
<tr>
<td>UC₀₂N₀.₈</td>
<td>14.18</td>
<td>94.45</td>
<td>13.39</td>
<td>2860</td>
<td>25</td>
<td>9.2</td>
</tr>
<tr>
<td>U₀.₀₅Zr₀.₉₅C</td>
<td>7.0</td>
<td>4.8</td>
<td>0.7</td>
<td>3400</td>
<td>40</td>
<td>7.2</td>
</tr>
<tr>
<td>U₀.₉Zr₀.₁C₀.₃N₀.₅</td>
<td>13.50</td>
<td>90.64</td>
<td>12.23</td>
<td>3200</td>
<td>28</td>
<td>9.8</td>
</tr>
<tr>
<td>U₀.₁Zr₀.₉C₀.₃N₀.₅</td>
<td>7.68</td>
<td>5.0</td>
<td>1.54</td>
<td>3200</td>
<td>28</td>
<td>8.5</td>
</tr>
<tr>
<td>U₀.₅Zr₀.₅C₀.₅N₀.₅</td>
<td>10.49</td>
<td>25</td>
<td>7.03</td>
<td>3100</td>
<td>27</td>
<td>--</td>
</tr>
</tbody>
</table>
Table 3.4-2 The limit temperatures (°C) of metals and alloys used in contact with refractory uranium compounds.

<table>
<thead>
<tr>
<th></th>
<th>UO₂</th>
<th>UC</th>
<th>UN</th>
<th>US</th>
<th>UP</th>
</tr>
</thead>
<tbody>
<tr>
<td>W</td>
<td>2200</td>
<td>1500</td>
<td>1300</td>
<td>2200</td>
<td>2200</td>
</tr>
<tr>
<td>Mo</td>
<td>2200</td>
<td>1300</td>
<td>1300</td>
<td>1700</td>
<td>1700</td>
</tr>
<tr>
<td>Nb</td>
<td>1500</td>
<td>900</td>
<td>1300</td>
<td>1200</td>
<td>1400</td>
</tr>
<tr>
<td>Ta</td>
<td>1600</td>
<td>900</td>
<td>1300</td>
<td>1300</td>
<td>1600</td>
</tr>
<tr>
<td>V</td>
<td>1500</td>
<td>900</td>
<td>1300</td>
<td>1000</td>
<td>900</td>
</tr>
<tr>
<td>Steel</td>
<td>900</td>
<td>800</td>
<td>900</td>
<td>800</td>
<td>900</td>
</tr>
</tbody>
</table>

Limiting reasons: a) formation of fusible eutectics; b) high rates of components output through the coating; c) formation of new compounds; d) non-congruent nature of evaporation.

Congruently evaporating compositions (1500-2000°C)

<table>
<thead>
<tr>
<th></th>
<th>UO₂</th>
<th>UC</th>
<th>UN</th>
<th>US</th>
<th>UP</th>
</tr>
</thead>
<tbody>
<tr>
<td>O/U ≈ 1.999</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>C/U ≈ 1.02</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>No</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>S/U ≈ 1.0</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>P/U ≈ 1.0</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Total pressure of vapours over refractory compounds at 2000°C

<table>
<thead>
<tr>
<th></th>
<th>UO₂</th>
<th>UC</th>
<th>UN</th>
<th>US</th>
<th>UP</th>
</tr>
</thead>
<tbody>
<tr>
<td>P, atm.</td>
<td>3 \times 10^{-6}</td>
<td>2 \times 10^{-7}</td>
<td>5 \times 10^{-5}</td>
<td>7 \times 10^{-7}</td>
<td>4 \times 10^{-6}</td>
</tr>
</tbody>
</table>

Lowering of stoichiometry coefficient and alloying by Y₂O₃; La₂O₃, etc. allows to increase thermal stability of uranium dioxide and to widen temperature limits of its usage. The use of protective diffusion barriers (ZrC, TiC) and injection of additions UN,
US, ZrC, TaC, NbC allows to widen temperature fields of using uranium mononitride by 200-300°C.

3.5. Emitter Material

3.5.1 Criteria for SPACE-R Emitter Material

The long duration requirement and high temperature environment of the SPACE-R system limits the candidate emitter materials to a few refractory metals and alloys. The most important engineering criterion for the emitter material is high structural strength and stability. Only the high temperature creep resistant materials can accommodate the emitter deformation under the severe thermal/mechanical load during the lengthy TFE operation. Secondly, the emitter material should have high bare work function for electron emission in order to have high efficiency of the thermionic diode. Furthermore, material with low electrical resistivity is preferable for the emitter, as well as for the collector, to reduce the ohmic loss and thus to maximize the system efficiency. In addition, the emitter material should have low diffusion coefficient. The diffusion of fission product through the emitter to the electrode gap will adversely affect the thermionic performance.

Of all the requirements for the emitter material, creep strength is the most critical one. Based on this criterion, several emitter material candidates have been identified for SPACE-R moderated TFEs. They include monocrystal alloy(MA) Mo-3%Nb, MA Mo-6%Nb, Mo/W184-HfC composite, MA W184-Nb and polycrystalline CVD W184.

Among the candidate materials, MA Mo-6%Nb was tentatively selected for the baseline design of SPACE-R emitters. This is largely based on the LIFE IV simulation results. However, emitter material creep test data and fueled emitter irradiation results will be needed to confirm the choice. These tests will be performed in Phase I. The final choice for the baseline emitter material will be made after consultation with INERTEK engineers. After 25 years of development in the CIS, monocrystal alloys present certain superior properties compared to their polycrystal counterparts. They
demonstrate great potential for use in science and technology as a result of the following characteristics:

- Very low concentration of interstitial impurities such as oxygen, hydrogen, nitrogen and carbon which can lead to material embrittlement.
- Monocrystal Alloys have no grain boundaries which in polycrystals act as failure initiation sites, promote dislocation, and accelerate structural deformation. Absence of grain boundaries also significantly reduces fission gas diffusion.
- Flexibility of material property control. Certain desired monocrystal properties can be achieved by changing the crystal orientation and defect density during the crystal growth process.
- Low modules oriented solidification texture provides significant enhancement in thermal fatigue and irradiation resistance.
- Microstructural stability increases the material durability in corrosive environments such as alkali metal vapor and others.
- Low impurity diffusion reduces gas emission.
- Purity yields higher density.

Parametric studies were performed to understand the deformation behavior of SPACE-R emitter candidate materials. The results of this trade study served as the basis for the fueled emitter design. The study covers the effects of operating temperature, fuel density and dimensional variation on the life limit of the emitters. Details are discussed below.

3.5.2 Creep Strength of Refractory Materials

As an order-of-magnitude comparison, Fig. 3.5-1 summarizes the creep strength of several refractory materials, both monocrystalline and polycrystalline. Each creep curve represents isothermal creep behavior as a function of applied stress. The isotherms of CVD W\textsuperscript{184} and Mo/W\textsuperscript{184}-HfC composite cross each other at the stress level about 20 MPa. This is because the W\textsuperscript{184}-HfC reinforcing wire is more stress sensitive for creep and hence has higher creep rate than CVD W\textsuperscript{184} at higher stress level. The
Figure 3.5-1 Creep Strength of SPACE-R Emitter Candidate Materials
isotherm curve for Mo-6\%Nb is based on the creep rate measurement from 2 specimens tested in the US at 1922 K/19.6 MPa and 1873 K/5 MPa, shown in Figures 3.5-2 and 3.5-3. The creep strength of MA-W\textsuperscript{184} - Nb is far higher than the others in the applied stress range.

The Sherby-Dorn creep parameters are summarized in Table 3.5-1. The material constant (A), stress exponent (n) and the creep activation energy (Q) characterizes the creep sensitivity for a material to applied stress and temperature. Relatively speaking (not judging the absolute value of creep rate), MA Mo-3\%Nb creep will be most sensitive to temperature because of its lowest activation energy; the creep of MA W\textsuperscript{184}-Nb will be most stress prone above certain stress level (> 500 MPa) because of its highest stress exponent.

Table 3.5-1 Sherby-Dorn Creep Parameters of Candidate Materials

\[
(\dot{\varepsilon} = A S^n \exp(-Q/RT))
\]

<table>
<thead>
<tr>
<th>Material</th>
<th>A</th>
<th>n</th>
<th>Q (Cal/g. mole)</th>
</tr>
</thead>
<tbody>
<tr>
<td>MA Mo-3% Nb</td>
<td>1.9891 E4</td>
<td>3.2</td>
<td>0.966E5</td>
</tr>
<tr>
<td>MA Mo-6% Nb</td>
<td>1.4645E3</td>
<td>3.0</td>
<td>1.000E5</td>
</tr>
<tr>
<td>W\textsuperscript{184}-HfC wire</td>
<td>1.0900E-3</td>
<td>5.2897</td>
<td>1.055E5</td>
</tr>
<tr>
<td>CVD W\textsuperscript{184}</td>
<td>1.665.4E3</td>
<td>4.4380</td>
<td>1.255E5</td>
</tr>
<tr>
<td>MA W\textsuperscript{184}-Nb</td>
<td>1.1317</td>
<td>6.1800</td>
<td>1.500E5</td>
</tr>
</tbody>
</table>
Figure 3.5-2 Monocrystal Alloy Mo-6% Nb Creep Test Data (SPI 2.2-1)
Figure 3.5-3  Monocrystal Alloy Mo-6% Nb Creep Test Data (SPI 2.2-3)
3.5.3 SPACE-R Emitter Creep Life Limit for the Candidate Materials

3.5.3.1 Fueled Emitter Deformation Mechanism

The structural deformation of a fueled emitter is the consequence of fuel restructuring and fuel/emitter relative creep strength. Three potential mechanisms leading to emitter deformation were identified (SPI, 1984): thermal ratcheting, fission gas pressure buildup and \( \text{UO}_2 \) fuel swelling.

Thermal ratcheting occurs because the thermal expansion coefficient (TEC) of \( \text{UO}_2 \) is greater than that of the emitter cladding at the operating temperature. Undersizing the \( \text{UO}_2 \) pellets within the emitter structure will allow for initial stress free expansion. At fuel temperatures encountered within the emitter, \( \text{UO}_2 \) vaporizes readily. The fuel vapor will condense and fill any radial gap remaining between the fuel and the cladding inner surface soon after initial startup. If, at a later time, the power level and temperature of the TFE are decreased, the fuel may pull away from the emitter inner surface. If the fuel/emitter TEC differential is large, the substantial contraction of fuel will cause the emitter/fuel bond to break and leave an annular gap. Post-irradiation photos showed that emitter/fuel bond breakage takes place in the fuel structure, leaving a rough, fuel-coated, smaller emitter inner diameter. Evaporation of the relatively hot fuel will eventually close the gap again. When the fuel element temperature is subsequently raised, the emitter will be strained due to the fuel/emitter interference, resulting in irreversible plastic deformation. If the emitter/fuel bond is not broken during temperature excursion, the two structures will contract and expand together. Design studies have shown that temperature oscillations will not cause fatigue problems, but that temperature oscillation should be limited to <40K in order to avoid potential additional creep during the temperature up-swing.

The second deformation mechanism is related to the release of fission gas within the \( \text{UO}_2 \) fuel. When 70% dense pressed powder \( \text{UO}_2 \) fuel is heated to operating temperatures under a thermal gradient, the fuel redistributes due to vaporization and condensation to form a dense (typically 98%) outer shell and a large internal void.
About 0.27 inert gas atoms are formed per fission. Gaseous by-products from fission are released by the UO$_2$ fuel over system lifetime. Because of the radial temperature gradient in the fuel, these fission gases migrate inward, gather in the central cavity formed in the fuel and are eventually vented through the top of the central cavity. During the fuel restructuring the fission gases enter cracks and grain boundaries, and travel along the channels that link interior grain boundaries to external free surfaces. The rate of gas release strongly depends on the time of forming of these channels. This gas release incubation period is a function of temperature and fuel burnup. The gas retained during the incubation period, or even till the end of the system life, leads to a cumulated pressure. This is another component of mechanical load causing the fuel and cladding to creep when their structural strength is low at the TFE operating temperatures. If vent passage is plugged, the internal pressure buildup would become several thousand psi (about 14 MPa or higher) in 10 years which will shorten emitter lifetime. Therefore, the fission gas vent passage should be maintained during a long term operation.

Both solid and gaseous fission products are accumulated in the fuel due to uranium fissioning. Fission products decrease the fuel density, and the subsequent volume increase results in outward deformation of the enclosing structure.

Emitter deformation by all three mechanisms discussed above will be alleviated if the initial fuel density is reduced and the emitter structural strength is enhanced. Porosity introduced in the fuel to achieve fabrication density lower than 100% Theoretical Density (TD) reduces fuel/emitter interfacial pressure during operation in several ways. First, a porous structure is weaker in the cooler end regions where it does not restructure as much. Also, the existence of porosity permits a faster gas release to fuel cracks and grain boundaries and accelerates the forming of channels along grain boundaries. Both effects facilitate fission gas release and thus reduce the pressure accumulation caused by retained gases. Finally, fuel internal porosity accommodates solid and gaseous fission products and thus effectively reduces fuel swelling.

A proven design methodology to minimize emitter radial deformation is cladding structural strengthening and fuel weakening. Topaz TFE performance has demonstrated the merit of such a design approach. The principle for SPACE-R fueled emitter design to
lower the fuel smear density to the minimum permissible for reactor criticality and use the strongest material for the emitter structure that is available. Specifically, a fuel loading at 70% of its TD with monocrystal Mo-Nb alloy cladding is selected for the baseline emitter design.

3.5.3.2 Comparison of LIFE IV Solution and CIS Experimental Data

   Based on the creep property of the candidate materials, a parametric study was performed to examine the effects of fuel density, operating temperature and emitter dimensions on the emitter creep life limit. The analytical study was conducted by LIFE IV modeling and simulations. The LIFE IV (Rev. 0) code has its limitations due to its indefinite calibration for TFE applications. In order to achieve better service of LIFE IV for the application of SPACE-R TFEs, the models in the LIFE IV (Rev. 0) code were examined and adjustments were made. The LIFE IV simulation results were compared with CIS PIE data and their calculated results (Fig. 3.5-4). The LIFE IV solution for a typical Topaz II emitter underpredicted the radial deformation by an average of 33% for burn-up of up to 0.4% compared with CIS PIE data. Extrapolation of the CIS test data and the CIS analytical results from an adequately calibrated code yielded a compensating factor of 1.25 for LIFE IV solution.

   Inherent to each TFE design is the steady-state fuel and temperature distribution. Figure 3.5-5 depicts the axial profile of temperature and fuel density for a SPACE-R emitter. The heat loss through the TFE end pieces and the nonuniform power flux cause the temperature gradient from the axial midpoint to each end. Consequently, an axial gradient of fuel layer is formed in a reversed manner, i.e. a thicker and denser fuel layer is formed at the location of lower temperature and the reverse occurs at points of higher temperatures. This pattern of fuel redistribution was observed during post irradiation examination of EBR II test cells (SPI, 1984). The temperature and fuel distributions combined constitutes a self-compensating or negative-feed-back mechanism for emitter radial deformation. At the high temperature point, the cladding is more vulnerable to creep but the fuel at that location is not as dense and is weaker in exerting radially outward pressure. Near the cooler ends, the fuel is thicker and denser and has a stronger potential for structural deformation which is counteracted by a stronger cladding structure.
Figure 3.5-4  Topaz II Emitter Deformation LIFE IV & CIS Result Comparison
Figure 3.5-5 Axial Profile of Emitter Temperature and Fuel Density Along SPACE-R Fuel Elements
due to the lower temperature. This causes the fuel to deform axially when the emitter/fuel interfacial shear or friction is overcome. Structurally there is no restriction for the fuel axial movement. The effect, known as the "toothpaste squeezing", alleviates the severity of emitter deformation radially.

3.5.3.3 Creep Deformation of Emitters of 132-TFE and 150-TFE Reactors

The parametric study on emitter life limit examined two reactor design cases: 132 TFE reactor and 150 TFE reactor. The axial distribution of emitter temperature and fuel density for the two cases is practically the same, with a 2% difference of fuel density at the midpoint of the cells. For each design case the MA Mo-6%Nb emitter total deformation axial profile, including the thermal expansion and creep displacement at the end of 10 year life was calculated and summarized in Fig. 3.5-6. The combination of the clad material characteristic, operating temperature, and local fuel density constitutes the most severe deformation at the midplane of the cell. Moving away from the midpoint toward the end of the cell, the effects of the local temperature and the fuel density compensate for each other, contributing to less and less resultant radial displacement. Among the five SPACE-R emitter candidate materials, MA Mo-3%Nb and Mo-6%Nb have the lowest activation energy and stress exponent in the Sherby-Dorn creep correlation (Table 3.5-1). Hence, the creep of this type of material is more sensitive to temperature change and not as much influenced by stress variation. This is evidenced by the rather steep gradient of the axial profile of the MA Mo-6%Nb emitter deformation. The temperature effect near the end of the cell is predominant. The range of creep displacement (not including the thermal expansion) of 14 to 4 mils from the midpoint to the end of the cell is a consequence of the average creep rates of 3.66 E-7 hr\(^{-1}\) and 1.05 E-7 hr\(^{-1}\) respectively. As expected, the emitters from the 132-TFE-reactor deform slightly more than that of the 150-TFE-reactor. For the initial study, the two types of the emitters were designed with the same emitter thickness (0.2 cm) while the emitters for 132 TFE-reactor carry higher total thermal power load. LIFE IV simulation results for the maximum radial displacement for the 150-TFE-reactor and 132-TFE-reactor Mo-6%Nb emitters are given as 16.2 and 18.1 mils at the end of 10 year life respectively (Figs. 3.5-7, 3.5-8). During the operation, the collector also thermally expands, giving a 22 mils radial clearance referencing the emitter outer surface when cold. This result
Figure 3.5-6  Axial Profile of SPACE-R Emitter Deformation
(Total Radial Displacement EOL)
**Figure 3.5-7**  History of Emitter Radial Displacement-132 TFE (At the Location of Maximum Deformation)
Figure 3.5-8 History of Emitter Radial Displacement-150 TFE (at the Location of Maximum Deformation)
indicates that the 150-TFE-reactor emitters will have sufficient creep strength for 10 year normal operation, while 132-TFE-reactor cells will function without shorting up to 8 years.

Based on the findings from the LIFE IV simulations for the emitters of 132-TFE reactor and 150-TFE reactor, the parametric study for the emitter deformation focused on 150-TFE emitters.

3.5.3.4 Emitter Deformation and Temperature Variation

Whether the reactor control scheme is to regulate constant output voltage or to maintain constant emitter temperature, some variation in emitter temperature is expected during normal operation of the system even though it can be minimized with the load followed by the shunt regulator. For the former, thermal cycling is required for the variable load power demand and the emitter temperature change is significant. The random load disturbances and control system instability may result in emitter temperature fluctuation. These factors, in turn, will cause variation of the emitter radial displacement from thermal creep. Tolerance should be incorporated to accommodate for these thermally related perturbations.

The radial displacement of the 150-TFE reactor emitters for up to 15 years of operation is summarized in Figure 3.5-9. Table 3.5-2 lists the LIFE solutions and corresponding adjusted values using the 125% compensating factor (§3.5.3.2). The results indicate that emitters made of all candidate material will have sufficient creep resistance for life spans of 1.5 and 5 years with ample margins. However, only MA Mo-6%Nb, CVD W$_{184}$, Mo/W$_{184}$-HfC (32%) composite, and MA W$_{184}$-Nb emitters will endure the 10 year life.

Shown in Fig. 3.5-10 is the trend of the emitter deformation responding to the emitter temperature change. Each curve characterizes the creep behavior of each of the five candidate emitter materials related to a temperature variation of $\pm 50$ K from the operating point of 1873 K. In the temperature-stress-strain rate relation, the apparent activation energy $Q$, is the parameter characterizing the temperature influence on material creep elongation. $Q$ represents the energy barrier to be overcome for an atom to move
Figure 3.5-9  History of Emitter Radial Displacement - 150 TFEs
(@ The Location of Maximum Deformation).
Figure 3.5-10  Emitter Radial Displacement vs Emitter Temperature
(@ The Location of Maximum Deformation).
from one energy level to a lower energy level. Therefore, one expects that creep of emitters made of MA Mo-3%Nb and Mo-6%Nb should be more temperature sensitive as they have relatively lower activation energy than the other candidate materials (Table 3.5-1). The simulation results confirm this trend. As shown in the figure, the material with higher activation energy gives a flatter slope of the curve. The variation of the emitter radial displacement (at the maximum deformation location) for a 50 K emitter temperature change is summarized in Table 3.5-3. Also the correlation of emitter deformation as a function of the emitter temperature is derived from the simulation results (Table 3.5-4). For both tables, the compensation factor of 1.25 is incorporated. Figure 3.5-10 also provides information for operating temperature as a function of emitter candidate material. For instance, a MA Mo-3%Nb emitter with the local fuel density of 53% TD can operate at no high than 1865 K while a diode with CVD W emitter may perform properly at 1890 K for the same fuel loading and life requirement. The information in Table 3.5-4 enables one to determine the emitter deformation as a function of temperature for the range of 1823 -1923 K with 53% fuel TD. An envelope of tolerances of multiple variables for fuel emitter design can be defined using this correlation incorporated with others (§3.5.3.6).
Table 3.5-2 Emitter Radial Displacement BOL 10 Years LIFE IV Solutions & Adjusted Values

<table>
<thead>
<tr>
<th>Emitter Material</th>
<th>Emitter Radial Displacement @ max Deformation Location</th>
<th>LIFE IV sol. (mil)</th>
<th>Adjusted values*</th>
</tr>
</thead>
<tbody>
<tr>
<td>MA Mo-3%Nb</td>
<td></td>
<td>17.6</td>
<td>22.0</td>
</tr>
<tr>
<td>MA Mo-6%Nb</td>
<td></td>
<td>16.2</td>
<td>20.2</td>
</tr>
<tr>
<td>CVD W&lt;sup&gt;184&lt;/sup&gt;</td>
<td></td>
<td>15.4</td>
<td>19.2</td>
</tr>
<tr>
<td>Mo/W&lt;sup&gt;184&lt;/sup&gt;-HfC (32%)</td>
<td></td>
<td>14.8</td>
<td>18.5</td>
</tr>
<tr>
<td>MA W&lt;sup&gt;184&lt;/sup&gt;-Nb</td>
<td></td>
<td>7.3</td>
<td>9.1</td>
</tr>
</tbody>
</table>

* The compensating factor 125% was determined based on the comparison of LIFE IV solution with CIS Topaz II PIE data. (3.5.3.1).

Table 3.5-3 Emitter Displacement Change for Temperature Variation of 50 K (@ The Location of Maximum Deformation: 1873 K-53% TD Fuel)

<table>
<thead>
<tr>
<th>Emitter Material</th>
<th>*Δr for ΔT&lt;sub&gt;e&lt;/sub&gt; = ±50K (0.001 in)</th>
<th>Activation Energy (Cal/g.mole)</th>
</tr>
</thead>
<tbody>
<tr>
<td>MA Mo-3%Nb</td>
<td>+6.8</td>
<td>0.965E5</td>
</tr>
<tr>
<td>MA Mo-6%Nb</td>
<td>+5.8</td>
<td>1.00E5</td>
</tr>
<tr>
<td>Mo/W&lt;sup&gt;184&lt;/sup&gt;-HfC (32%) Comp</td>
<td>+4.1</td>
<td>1.055E5</td>
</tr>
<tr>
<td>CVD W&lt;sup&gt;184&lt;/sup&gt;</td>
<td>+4.3</td>
<td>1.255E5</td>
</tr>
<tr>
<td>MA W&lt;sup&gt;184&lt;/sup&gt;-Nb</td>
<td>+1.0</td>
<td>1.500E5</td>
</tr>
</tbody>
</table>

*Δr - Differential emitter radial displacement at EOL of 10 years.
Table 3.5-4 Correlation of Emitter Deformation vs Emitter Temperature
(Fuel OD= 1.8cm, Emitter Thick. = .2cm, Fuel Dens. = 53%TD, Tc: 1823-1923 K) *r(Tc) = A + B (Tc) + C (Tc²)

<table>
<thead>
<tr>
<th>Emitter Material</th>
<th>A</th>
<th>B</th>
<th>C</th>
</tr>
</thead>
<tbody>
<tr>
<td>MA Mo-3%Nb</td>
<td>1.6865E3</td>
<td>-1.8910</td>
<td>5.3329E-4</td>
</tr>
<tr>
<td>MA Mo-6%Nb</td>
<td>1.4086E3</td>
<td>-1.5801</td>
<td>4.4672E-4</td>
</tr>
<tr>
<td>Mo/W¹⁸⁴-HfC(32%)</td>
<td>8.9380E2</td>
<td>-1.0064</td>
<td>2.8676E-4</td>
</tr>
<tr>
<td>CVD W¹⁸⁴</td>
<td>8.9691E2</td>
<td>-1.0115</td>
<td>2.8876E-4</td>
</tr>
<tr>
<td>MA W¹⁸⁴-Nb</td>
<td>3.4182E2</td>
<td>-0.3918</td>
<td>1.1436E-4</td>
</tr>
</tbody>
</table>

*r - Emitter radial displacement at EOL of 10 years.

3.5.3.5 Emitter Deformation and Fuel Density Variation

While the temperature affects the emitter deformation by mainly changing the creep strength of clad material and fuel, the variation of oxide fuel density contributes to the emitter creep by changing the mechanical load, i.e. the stress. The increase in UO₂ fuel density leads to increased of gaseous fission product generation and hence more fuel swelling. Fuel swelling promotes the fuel/clad interfacial pressure, and the stress on the clad shortens the TFE life by electrode shorting or by degrading the clad structural integrity, such as fatigue from thermal cycling. The principle for SPACE-R fuel loading is to fuel the emitter to the minimum neutronically permissible level for the reactor power requirement. Margins and tolerances for the fuel requirement were investigated in regards to creep life limit of the emitter.

The trade study result for fuel variation effect (Fig. 3.5-11) summarizes the response of emitter radial displacement for a fuel smear density range of 50%-70% (TD), with the nominal operating point at the maximum deformation location being 1873 K emitter temperature and local fuel density of 53% TD. Accounting for the 25% correction, 4 candidate materials can withstand the creep for 10 year life in a 150-TFE SPACE-R reactor. With the loading upto 65% TD fuel at 1873 K, MA Mo-6%Nb, CVD W and MA W-Nb.
Figure 3.5-11  Emitter Radial Displacement vs Fuel Density
(@ The Location of Maximum Deformation).
remain adequate for creep life. Again, all five candidate materials will not have creep life limitation problem for 1.5 and 5 year operations.

The sensitivity of emitter deformation for a fuel density variation of +20% TD above the minimal design point is listed in Table 3.5-5. -20% TD from the design point (53% TD at the maximum deformation location) is too low for any practical consideration and thus was not investigated. The emitter creep deformation vs UO₂ fuel density is further characterized by the correlation in Table 3.5-6.

<table>
<thead>
<tr>
<th>Emitter Material</th>
<th>*Δr for +20% Fuel DT (0.001 in)</th>
</tr>
</thead>
<tbody>
<tr>
<td>MA Mo-3%Nb</td>
<td>0.35</td>
</tr>
<tr>
<td>MA Mo-6%Nb</td>
<td>0.44</td>
</tr>
<tr>
<td>Mo/W-HfC (32%) Comp.</td>
<td>0.74</td>
</tr>
<tr>
<td>CVD W</td>
<td>0.52</td>
</tr>
<tr>
<td>MA W-Nb</td>
<td>0.14 (Δr for +15%TD)</td>
</tr>
<tr>
<td>Fuel (1776K - 83% TD)</td>
<td></td>
</tr>
</tbody>
</table>

* Δr - Differential emitter radial displacement at EOL of 10 years.

<table>
<thead>
<tr>
<th>Emitter Material</th>
<th>A</th>
<th>B</th>
<th>C</th>
</tr>
</thead>
<tbody>
<tr>
<td>MA Mo-3%Nb</td>
<td>1.7334 E1</td>
<td>-2.4101</td>
<td>5.8275</td>
</tr>
<tr>
<td>MA Mo-6%Nb</td>
<td>1.8747E1</td>
<td>-10.3800</td>
<td>10.6070</td>
</tr>
<tr>
<td>Mo/W-HfC (32%)</td>
<td>1.6252E1</td>
<td>-7.7776</td>
<td>9.5108</td>
</tr>
<tr>
<td>CVD W</td>
<td>1.6582E1</td>
<td>-6.4212</td>
<td>7.8176</td>
</tr>
<tr>
<td>MA W-Nb</td>
<td>1.0857E1</td>
<td>-12.1900</td>
<td>9.5238</td>
</tr>
<tr>
<td>(1776K/80%-98% TD)</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

*r - Emitter radial displacement at EOL of 10 years.
3.5.3.6 Emitter Deformation as a Function of Multiple Variables

Emitter radial displacement variation is a function of many variables. In addition to the previously discussed variables, emitter temperature and UO$_2$ fuel density, other variables include fuel diameter (a function of the number of TFES), emitter thickness, and fuel and emitter material properties. The sensitivity of emitter deformation for each variable must be analysed to evaluate total deformation. Or, inversely, given a constraint for emitter radial displacement differential, allowable deviation in the individual design variables can be defined. This combination of component uncertainty in overall system deviations can be determined by the method of error propagation.

In the two preceding sections, differential emitter deformation with the combined effects of emitter temperature and fuel density variations was calculated. For prevention of electrode shorting during a 10 year life, the limit of displacement variation of emitters made of each type of material has been computed individually. In turn, the permissible range for these two variables' deviations is shown in Table 3.5-7. In Table 3.5-7, a $\Delta \rho$ of +10% was assumed. Thus, $\Delta T_e$ is the allowable variation in operating temperature for a 10% fuel density increase. Similarly, given a constraint of $\Delta T_e$, the resolution of controlled variable $T_e$, a corresponding $\Delta \rho$ can be defined. The functions governing the emitter deformation and other variables will be defined by further parametric studies. This will refine the determination of the resultant.

<table>
<thead>
<tr>
<th>Material</th>
<th>$\Delta \rho$ Allowed from 53% TD (% TD)</th>
<th>$\Delta T_e$ Allowed from 1873K (K)</th>
<th>$\Delta r$ Developed (.001 in)</th>
</tr>
</thead>
<tbody>
<tr>
<td>MA Mo-3%Nb</td>
<td>+10</td>
<td>-9</td>
<td>-1.1</td>
</tr>
<tr>
<td>MA Mo-6%Nb</td>
<td>+10</td>
<td>+6</td>
<td>+0.7</td>
</tr>
<tr>
<td>CVD W$^{184}$</td>
<td>+10</td>
<td>+22</td>
<td>+1.7</td>
</tr>
<tr>
<td>Mo/W-HC(32%) Comp.</td>
<td>+10</td>
<td>+28</td>
<td>+2.6</td>
</tr>
<tr>
<td>MA W$^{184}$-Nb</td>
<td>+10</td>
<td>+84 (beyond correlation range)</td>
<td>+11.8</td>
</tr>
</tbody>
</table>
3.5.3.7 Time History of Emitter Deformation

One of the major causes for emitter deformation in a vented TFE is fuel swelling (§ 3.5.3.1). Xe and Kr along with other fission products generated in the fuel cause fuel swelling. Since noble gases are essentially insoluble in the fuel matrix, Xe and Kr will be released from the fuel matrix. Gases released from the fuel contribute to the gaseous atmosphere within the fuel pin or they precipitate as small pockets of gas or bubbles within the fuel body. Either route is detrimental to TFE performance. When vented, the fission gases released from the fuel body contribute no part in the fuel internal pressure build up. Swelling and fission gas release are complementary phenomena. A piece of fuel that releases a large portion of its fission gases exhibits low swelling because there is little gas remaining in the fuel to form bubbles. Conversely, complete retention of the fission gasses in a section of fuel is usually accompanied by significant swelling. The fission gas release from the fuel interior to the free surface is accomplished by surface-diffusion mechanism for gas bubbles and vapor transport mechanism for pores (Olander, 1976). The migration of fission gas bubbles and pores proceeds at a defined velocity under irradiation, and fission gas release is a lengthy process.

The LIFE IV solution depicts the history of the fission gas release process in the SPACE-R baseline emitter up to 15 years (Fig. 3.5-12). The gas release is hardly appreciable for the first two years. Specifically, the gas release at the end of year 1 and 2 for the cases under study typically are 0.453% and 1.836% respectively. Beyond that point, the gas release is augmented almost exponentially and the curve inflection takes place between 7.5 and 10 years. Fuel swelling, reflected by the fuel/emitter interfacial pressure and the emitter stress, is shown in Figures 3.5-13 and 3.5-14. For the MA Mo-6%Nb and the Mo/W-HfC(32%) composite emitters, the maximum hoop stresses are experienced at the time of one and half years. Other emitters experience approximately the same time constant of pressure/stress development. The different magnitudes of the emitter tangential stress correspond to the different mechanical properties of the two viscoelastic materials, specifically, their creep parameters, Young’s moduli, relaxation moduli and/or creep compliances. Phenomenologically, the emitter material with high tensile strength has a higher resistance to the fuel swelling. As a result, a more severe stress is developed in the emitter structure because of the higher fuel/emitter interfacial pressure. The higher stress
Figure 3.5-12 History of Fission Gas Release and Fuel Burn-up - 150 TFEs
Figure 3.5-13 History of Emitter/Fuel Pressure & Emitter Hoop Stress
(@ The Location of Maximum Deformation).
Figure 3.5-14  History of Emitter/Fuel Pressure & Emitter Hoop Stress (@ The Location of Maximum Deformation).
causes faster creep, depending on the creep exponent of the material and, as a consequence, the stress is relaxed in the material. This emitter radial displacement and stress relaxation proceeds until a new equilibrium is achieved. Another mechanism that constitutes the emitter resultant displacement is fuel creep. Subject to compression, the fuel at the elevated temperature (~2000 K) readily creeps inward. The final contributor to emitter deformation is the various irradiation induced phenomena in the clad and fuel.

3.5.3.8 Discussion of Mo/W-HfC Issues

There is an issue of carbon diffusion in W-HfC alloy, the reinforcing fiber of Mo/W\textsuperscript{184}-HfC composite. There are two parts to this issue, the degradation of creep strength by changing morphology of the hafnium-carbide particles and the effects of material diffusion within the emitter structure leading to possible changes in work functions or other properties.

The high creep strength of W-HfC alloy is associated with the dispersion of very fine carbide particles that retard the movement of dislocations and sub-boundaries. With increase in temperature, HfC becomes thermally unstable and particles grow due to diffusion. Particle coarsening leads to a reduction of total particle number available in the material, increased mean slip distance of dislocation, and a decrease in the strain-hardening rate. The phenomenon of carbide diffusion and coarsening for polycrystalline materials have been experimentally characterized (Klopp, Witske 1969 and Luo, et al 1991). According to Klopp’s correlation of the rate of HfC particle coagulation based on the cubic law of precipitate growth in a binary system (Ardell, 1967), it takes about 1.5 year for HfC particles to grow from 200 Å to 500 Å in radius, at which the solid-solute-strengthened alloy starts to lose structural strength. Luo’s experimental finding defines 2450 K as the characteristic temperature up to which the HfC particle are thermally stable. For SPACE-R design, the maximum stress is encountered near the end of 1.5 year of life and monotonically decreases afterwards. Also the maximum diode operating temperature, 1873 K, is 580 K below the carbide growth characteristic temperature. This indicates that the carbide alloys may still serve as good emitter materials. However, more experimental investigation is required to fully characterize carbide coarsening in the polycrystalline W-HfC, in terms of temperature, time duration and residual tensile strength.
If the carbide particles decomposed into elemental carbon and/or hafnium, then these elements may diffuse as interstitial atoms. A crude estimate based on relative diffusion rates indicates that such elemental materials would be more or less completely dispersed throughout the tungsten material in the course of a multiyear lifetime at emitter temperatures. However, the tendency for the particles to decompose is not observed in tests; instead a agglomeration of particles has been observed (Luo, 1991). Furthermore, the hafnium-carbide strengthened tungsten alloys are actually formed by repeated vacuum arc melting of material feed stock based upon elemental tungsten, graphite, and hafnium. This mixture is melted together and it produces the hafnium-carbide precipitate in the grain boundaries. These facts indicate that the carbide material has a much greater affinity for hafnium than for tungsten.

The small amount of elemental hafnium or carbon which might make its way to the surface may or may not be a performance issue for thermionic system with the low current density used in the SPACE-R concept. First of all, SPI’s concept for the wire-reinforced strong emitter is based upon a wire wrapped mandrel, over which molybdenum has been deposited followed by a final deposit of oriented <110> tungsten on the outer surface. Any free carbon or hafnium within the wire would first have to make its way through the body of the wire and then into matrix which has been deposited over it and finally through the outer surface of a few mills of chloride deposited <110> tungsten. The hafnium-carbide is typically only ~1/3% of the wire composition and the wire itself is 3.0-3.5% of the emitter wall. This double dilution indicates that there would be very small amount of carbon available from decomposed hafnium-carbide to actually pollute the emitter surface. Furthermore, it is possible to consider diffusion barriers either on the surface of the wire or as an intermediate layer between the bulk CVD material and the outer chloride deposited tungsten coating. Our present view is that such effects are not expected to be significant because of the small amount of material involved and the low current density, approximately 2-3 A/cm² in the baseline design, which is significantly less than typical laboratory thermionic converters.
3.6 Core neutronics and reactor safety

3.6.1 Number of cells per TFE

A number of point calculations were made with a multicell version of the SPI system code and with MCNP to do preliminary scoping of a multicell design using the basic SPACE-R balance of system. Table 3.6-1 shows reactivity result for a 150 TFE flashlight design with three cells per TFE and for a 150 TFE flashlight design with 5 cells per TFE. The fuel smear densities are representative of what would be required for even relatively short emitter life. Full LIFE 4 analysis was not performed, and no attempt was made to optimize to a particular system life. The lifetimes given correspond approximately to the reactivity life of the system at a net output of 40 kWe. The 3 cell/TFE design could be operated at somewhat higher power while the 5 cell/TFE design could be operated at much higher power, but for shorter time. The net conclusion of this study is that the multicell TFE design is suited primarily to high power output (> 50 kWe) and shorter life (< 3 years).

Table 3.6-1 Multicell TFE Study

<table>
<thead>
<tr>
<th>Be-YH$_{1.7}$ Moderator</th>
<th>Mo-7Nb MCA Emitters Same Diameter as SPACE-R</th>
</tr>
</thead>
<tbody>
<tr>
<td>150 TFEs, 3 cells/TFE, 43 cm Core Height</td>
<td></td>
</tr>
<tr>
<td>60% Fuel Smear Density</td>
<td></td>
</tr>
<tr>
<td>BOL $k_{\text{eff}} = 1.040$</td>
<td></td>
</tr>
<tr>
<td>=&gt; Life ~ 4 years at 40 kWe</td>
<td></td>
</tr>
<tr>
<td>150 TFEs, 5 Cells/TFE 41 cm Core Height</td>
<td></td>
</tr>
<tr>
<td>70% Fuel Smear Density</td>
<td></td>
</tr>
<tr>
<td>BOL $k_{\text{eff}} = 1.0265$</td>
<td></td>
</tr>
<tr>
<td>=&gt; Life ~ 2 years at 40 kWe</td>
<td></td>
</tr>
</tbody>
</table>
3.6.2 Varying Number of TFEs

3.6.2.1 Ground Rules for Trades

When changing a design parameter in the SPACE-R system in a trade study it is important to assure that the parameter either does not change the performance, or else is accompanied by some compensating change which maintains the nominal levels of performance. In particular, when varying the number of TFEs, it is necessary to assure that the systems considered are capable of producing the required 44 kWe, net, at BOL, and also capable of the same 10 year endurance. These simultaneous constraints are so restrictive that to specify a different number of converters is almost to specify a complete new design. The important parameters such as emitter thickness, collector thickness, core height, and center-to-center pitch are almost uniquely determined, for a single cell core, by the required power, life, and number of TFEs. This restrictiveness simplifies the problem of varying the number of TFEs.

3.6.2.2 Maintaining System Power

Long single cell TFEs characteristically exhibit a sharp maximum in output power per TFE vs. center emission density. Moreover, the maximum efficiency point of operation is usually quite close to the maximum power point. As a result, the optimal design will be at the maximum power point or very close to it. These maximums exist because, in the limit of zero emission density, efficiency and power are zero; as emission density increases so do power and efficiency. As emission density increases further, the ohmic losses increase as the square of both the emission density and the length. These losses significantly decrease both the power and the efficiency of the converter.

As a result, a collection of single cell TFEs of a given length, and with given maximum emitter temperature will produce a well defined maximum electrical power, as a function of emitter inner diameter and thickness; there is a well defined minimum number of TFEs to produce a given electrical power. Curves showing the minimum number of TFEs needed to produce 44 kWe of system power are shown in Figure 3.6-1, for core
**Figure 3.6-1** Thermionic Emitter Thickness Trades
heights of 35 cm and 37.5 cm. These curves were obtained using the SPI system code. They reflect pumping power losses as well as electrical losses in the low voltage power section, power conditioning, and high voltage transmission. Optimal collector thickness for the given emitter thickness was used, and optimal cesium pressure was also used in all results.

A number of salient points emerge from these plots. The first is that there is little difference in maximum power capability between TFEs 35 cm long and TFEs 37.5 cm long: these lengths are close to the optimal. The only incentive for a longer element would be to add reactivity.

A second point is that an emitter thickness of 2 mm combined with a fuel diameter of 1.8 cm is capable of more than 44 kWe from 150 TFEs. In a separate study (see Section 3.6.3.8), it was determined that for short (~ 100 day) durations the 150 TFE baseline design could produce slightly in excess of 60 kWe with only minor system design changes.

A third point is that in order to reduce the number of TFEs to 120 or fewer, the use of thicker emitters and larger fuel diameter is required. Subsequent nuclear studies have shown that both would be required because the core with the smaller number of fuel elements and thicker emitter falls short of reactivity and requires more fuel.

3.6.2.3 Nuclear Characteristics

Nuclear studies were done to find candidate cores with fewer than 150 TFEs which would be capable of ten year system life. For this trade study, the ten year endurance requirement was interpreted to be satisfied if two criteria were met: the cold BOL $k_{en}$ of the candidate core must be 1.069 (or nearly so), and the average fuel smear density should be no greater than 70%. The reason for the first criterion is fairly straightforward. A ten year core requires a certain BOL reactivity to account for burnup, etc. (see section 3.6.4 for design basis reactivity table). The reason for the second criterion is more complex. Detailed analysis with the LIFE 4 code on the baseline Mo7Nb monocrystal emitter shows that creep strain of a 2 mm emitter, with 1.8 cm inner diameter, operating at a peak temperature of
1873 K, will probably not cause the diode to short electrically within 10 years, provided the average BOL fuel smear density is not greater than 70%.

A matrix of 3 D MCNP calculations was performed to identify potential candidate designs with fewer than 150 TFEs. The first analysis was of the 132 TFE core which showed that the core size and mass increase with smaller number of TFEs. For this reason, full scale nuclear analysis was not extended to the study of cores with fewer than 132 TFEs. The result of the MCNP analysis are summarized in Figures 3.6-2 through 3.6-4. These curves show that, given the acceptance criteria, a core with 132 TFEs, a fuel pin diameter of 2.0 cm, a core height of 35 cm, and an emitter thickness of 2.0 mm is a viable candidate, provided the TFE center-to-center pitch is 3.82 cm, or greater. A similar core with 120 TFEs, 2.2 cm diameter fuel pin, and a 2.25 mm emitter is capable of producing 44 kWe. However, it is estimated that a pitch greater than 4 cm would be required to give this core a 10 year reactivity life.

The salient characteristics of 10 year cores with 150, 132, and 120 TFEs are given in Table 3.6-2 for comparison. The most strongly varying design parameters are fuel and TFE pitch. These variations produce strong changes in fuel load, core diameter, and moderator mass.

The data in Table 3.6-2 allow us to estimate the mass penalty associated with a smaller number of TFEs. This quantity is shown in Table 3.6-2. As can be seen, the mass penalty at 132 TFEs is modest--a little more than 50 kg for the entire system, while the mass penalty at 120 TFEs exceeds 10% of the system mass.

System mass is not the only applicable criterion for comparison. In fact, the primary motive, for looking at candidates with smaller number of TFEs is cost. TFE fabrication and core assembly costs are not yet well defined, but are expected to be substantial. The first-order mass estimates given in Table 3.6-2 were the basis for a rough estimate of cost benefit to be expected as a result of a smaller number of TFEs per core. In making this estimate, it was assumed that uranium at 93\% enrichment in U\textsuperscript{235}, and tungsten enriched to 96\% in W\textsuperscript{184} each cost about $50/g. It was further assumed that the marginal cost of launch to LEO is $10,000/kg. With these assumptions we can estimate the
Figure 3.6-2
Figure 3.6-3
BOL \( k_{\text{eff}} \) vs. Emitter Thickness

- 132 TFEs
- Fuel Diameter: 2.0 cm
- Fuel Smear Density: 70%
- TFE Pitch: 3.82 cm

**Core Height**
- 35 cm
- 37.5 cm

**Figure 3.6-4**
Figure 3.6-4A  Cost Effect of Varying the Number of TFEs.
TFE fuel and assembly cost differences (relative to a 150 TFE core) for cores of 132 and 120 TFEs. These costs are plotted in Figure 3.6-4A. The breakeven TFE fab cost variation is almost linear with decreasing TFE numbers. At a TFE cost in excess of $70,000 TFE the 132 TFE core is cost effective. The fab cost must exceed $120,000/TFE before the 120 TFE core is attractive. It is estimated that the single cell TFE production cost will be less than $50,000 per TFE once the TFE production capability is established. Therefore, the 150 TFE core appears to be most attractive.

Table 3.6-2

<table>
<thead>
<tr>
<th>NUMBER OF TFE’S</th>
<th>150</th>
<th>132</th>
<th>120</th>
</tr>
</thead>
<tbody>
<tr>
<td>$K_{eff}$</td>
<td>1.068</td>
<td>1.068</td>
<td>1.068</td>
</tr>
<tr>
<td>Core Height</td>
<td>35</td>
<td>35</td>
<td>35</td>
</tr>
<tr>
<td>Fuel Diameter</td>
<td>1.5 cm</td>
<td>2.0 cm</td>
<td>2.2 cm</td>
</tr>
<tr>
<td>Emitter Thickness *</td>
<td>2.0 mm</td>
<td>2.0 mm</td>
<td>2 ¼ mm</td>
</tr>
<tr>
<td>TFE Pitch</td>
<td>3.45 cm</td>
<td>3.82 cm</td>
<td>&gt; 4 cm</td>
</tr>
<tr>
<td>Fuel Load</td>
<td>102.4 kg</td>
<td>111.25 kg</td>
<td>122.4 kg</td>
</tr>
<tr>
<td>Fuel Surface Area</td>
<td>$2.97 \times 10^4 \text{ cm}^2$</td>
<td>$2.90 \times 10^4 \text{ cm}^2$</td>
<td>$2.90 \times 10^4 \text{ cm}^2$</td>
</tr>
<tr>
<td>Core Area</td>
<td>$1.5 \times 10^5 \text{ cm}^2$</td>
<td>$1.67 \times 10^5 \text{ cm}^2$</td>
<td>$2.0 \times 10^5 \text{ cm}^2$ (est)</td>
</tr>
<tr>
<td>Molybdenum Mass</td>
<td>122 kg</td>
<td>117 kg</td>
<td>132 kg</td>
</tr>
<tr>
<td>Hydride Mass</td>
<td>56 kg</td>
<td>76 kg</td>
<td>120 kg (est)</td>
</tr>
<tr>
<td>Enriched Material</td>
<td>93% U</td>
<td>90.1 kg</td>
<td>97.9 kg</td>
</tr>
<tr>
<td></td>
<td>W</td>
<td>8.7</td>
<td>8.3</td>
</tr>
<tr>
<td>Nominal Shield</td>
<td>569 kg</td>
<td>634 kg</td>
<td>759 kg (est)</td>
</tr>
</tbody>
</table>

* The corresponding optimally thick collector is also assumed.
3.6.3 Design Basis Accident

**DBA Definition**

SPI has identified a single worst case reactor configuration for safety analysis which serves as a guide for investigating special safety design provisions and for evaluating the important safety implications of selected variants. The Design Basis Accident (DBA) is assumed to be an event in which the control drums and radial reflector are cleaned from the reactor; the reactor is buried in wet sand; all reactor coolant channels and plena are filled with water; the fuel void space is filled with water.

**Possible Scenarios**

The DBA configuration could conceivably result from an abortive launch, followed by an impact into a beach. In such a situation the large density mismatch between the core and the radial reflector and drums makes it possible that the entire radial reflector would be stripped off from the core and that the core would penetrate to some depth in the sand without the reflector and drums. A second scenario which could result in the DBA configuration is a launch pad fire involving solid propellants. Exposure to high flame temperature for a short period or explosion could conceivably destroy the reflector structure and drums while not significantly cooking out hydrogen from the moderator. Subsequently the reactor might fall into mud and be buried.

**Modeling**

For purposes of analysis all dimensions are cold nominal. "Wet sand" is a mixture of water, SiO₂, and Al₂O₃ such that the density of silicon, aluminum, and oxygen are close to average for the earth’s crust. The mixture is 70% sand, by weight.

**Compaction Accidents**

Because of the complexity and difficulty of analyzing compaction transients, no effort in this phase has been made to define compaction scenarios or to bound their
consequences. However, in the design study which SPI performed for the Air Force, and which resulted in the SPACE-R point-of-departure design, an important factor in SPI’s rejection of heat pipe cooling of the reactor was the belief that heat pipe voids presented an unacceptable risk in a compaction accident. The SPACE-R core is intentionally designed with minimum void volume, in part to minimize the potential for compaction.

3.6.4 DBA Reactivity Control

Replacing the beryllium radial reflector with wet sand and flooding internal cavities with water will result in criticality of the reactor even with a central safety rod in place if no other reactivity control is employed. Ancillary reactivity control is needed to assure subcriticality.

SPI has investigated a number of design remedies for this DBA reactivity problem.

3.6.4.1 Neutron Leakage Control

It is possible to influence the amount of core leakage by arranging fuel more toward the center of the core or more toward the periphery. This design choice is largely a function of the number of TFEs in the core. SPI recognized that a possible benefit to a smaller number of TFEs, e.g., 132 instead of 150, might be the ability to improve the DBA reactivity response through aggressive leakage control. Five candidate arrangements of a core with 132 TFEs were investigated. The cores are identical, except for the arrangement of TFEs. The arrangements, pin powers, and DBA response of these candidates are shown in Figures 3.6-5 through -9. The conclusion of our study was that, while it is possible to effectively eliminate the possibility of DBA reactivity by aggressive leakage control, the resulting core lacks operability because of too little reactivity. The candidate arrangement which shows this is given in Figure 3.6-6.
Figure 3.6-5 Effect of Loading Pattern
44 kWe SPACE-R
132 TFE
BOL Startup $k_{\text{eff}}$: 0.925 (31%)
DBA $k_{\text{eff}}$: 0.97056 (55%)

**Figure 3.6-6** Effect of Loading Pattern
44 kWe SPACE-R
132 TFE
BOL Startup $k_{\text{eff}}$: 1.096141 (0.24%)
DBA $k_{\text{eff}}$: 1.103077 (0.22%)

Figure 3.6-7
44 kWe SPACE-R
132 TFE
BOL Startup $k_{\text{eff}}$: 1.066191 (.27%)
DBA $k_{\text{eff}}$: 1.082040 (.23%)

Figure 3.6-8
44 kWe SPACE-R
132 TFE
BOL Startup $k_{eff}$: 1.088012 (.16%)
DBA $k_{eff}$: 1.112603 (.34%)

Figure 3.6-9
3.6.4.2 Burnable poison in the Moderator

Reactivity control can be achieved by the use of burnable poison. Boron, samarium, and gadolinia have been used in terrestrial thermal reactors for reactivity control. SPI has investigated the use of gadolinia (Gd₂O₃) pins placed within moderator elements to improve the DBA response. Figure 3.6-10 shows a 150 TFE layout which includes 90 gadolinia pins located at the center of moderator elements. The pins are full core length and 4 mm od. Natural gadolinia is assumed.

This DBA remedy is effective. A detailed study is in progress to quantify the penalty to be associated with displacing hydride moderator with gadolinia, and the residual poisoning effect of burnt gadolinia. However, it appears to be feasible to make the DBA \( k_{\text{eff}} \) less than unity this way.

3.6.4.3 Control Elements Within the Fuel Cavity

A significant part of the DBA problem comes from the assumed penetration of water into the interior of the fuel column. It has already been remarked that low smear density is needed to achieve even relatively modest system life. The lower the smear density, the stronger the reactivity response to DBA.

One possible approach to the problem would be a fuel column of 100% dense UO₂ drilled out to the prescribed smear density. This fuel would have an interior cavity when cold and fresh. Into the fuel cavity material would be introduced which would exclude water and (perhaps) also absorb neutrons. The requirements for such a material would be:

1. It must not make the fuel stronger or result in more swelling under irradiation.
2. It must be stable in the operating environment of the fuel cavity.

Four candidate materials were identified and investigated for effectiveness in ameliorating the DBA. These are: zirconia (ZrO₂), hafnia (HfO₂), gadolinia (Gd₂O₃) and boron tripleny (B(C₆H₅)₃). The first two of these are chemically similar and very stable at high temperature. The expected atmosphere in the cavity is primarily UO₃ [Olander, 1976],
BOL Startup $k_{\text{eff}}$: 1.077
DBA $k_{\text{eff}}$: 1.072
Peak Power w/o Gd$_2$O$_3$: $\approx$1.15

Figure 3.6-10 Baseline Design 150 TFEs Uniformly Spaced
which would not be expected to reduce either of them. Hafnia is a moderately strong absorber of thermal neutrons, while zirconia is an anomalously weak absorber of neutrons. The effectiveness of either in mitigating DBA reactivity would depend on excluding water from the fuel cavity and on subsequently (perhaps) absorbing neutrons. It would be considered to introduce the hafnia or the zirconium into the cavity as a fine powder. This arrangement would be expected to exclude DBA water, on the one hand, but allow fission gas to escape.

A third material investigated is gadolinia. Gadolinia has a long history of use in terrestrial light water power reactors for reactivity control. In boiling water reactors, gadolinia powder is fabricated directly into the high density UO$_2$ pellet. Placement of gadolinia in the SPACE-R moderator has previously been discussed. Placement in the fuel cavity would reduce the adverse effects, such as the residual reactivity penalty and it would retain the current simplicity of the moderator design. An issue is gadolinia's apparent solubility in water: some means would have to be found to assure its long term stability. Solid solution with another oxide is being considered to avoid the water solubility.

A fourth material considered is borine triphenyl (B(C$_6$H$_5$)$_3$). This light, waxy material could readily be poured into the cold fuel cavity when fuel is loaded. It would exclude DBA water and also absorb thermal neutrons. However, it should be verified that it would not cook out due to aerodynamic heating in an accident.

The results of MCNP calculations of DBA reactivity for these four candidate materials in the fuel cavity are given in Figure 3.6-11. From these results we can conclude:

- zirconia in the fuel cavity is insufficiently effective
- hafnia at greater than 50% volume fraction is effective, but with large reactivity
- gadolinia at 40% volume fraction is effective, but needs further study.
- (B(C$_6$H$_5$)$_3$) is effective, but needs further analysis.
Figure 3.6-11 DBA Response with Powder in Fuel Cavity
3.6.4.4 Nut Fuel

"Nut" fuel, with high-density closed-porosity surface and low density (~60%) center, as proposed is a promising candidate. It effectively prevents water from entering the fuel cavity during DBA. This fuel type has been fabricated by Luch of INERTEK. However, high pressure water immersion test data are required to qualify this fuel form.

3.6.5 Radial Power Distribution

3.6.5.1 SPACE-R 150 TFE Baseline Core

Highly nonuniform radial fission power is undesirable for two reasons: hot TFEs may require special cooling provisions; large thermal power variance makes TFE hookups complicated and leads to inefficiency. The SPACE-R 150 TFE core radial power has been calculated for two cases to investigate radial power distribution. The first case is for a radially uniform core-uniform TFE pitch, and uniform moderator. The second case is one in which the pitch remains uniform, but the moderator is divided into two radial zones. The inner zone has a hydride volume fraction (in the moderator) of 0.41. The outer zone has a hydride moderator fraction of 0.61. The results are summarized in Figures 3.6-12 and 3.6-13.

These figures show 30° core sections with TFE phantoms at their radial locations. The numbers within the phantoms give the BOL pin power (relative to an average of 1.0). The numbers are averaged over the length of the TFE and are the result of full 3D calculations using MCNP. The center phantom is a safety rod. Outside the core periphery is a radial reflector and two half-drums with poison turned out (not shown).

A feature of the SPI core design approach is the use of many individual moderator elements interspersed within a uniform array of TFEs. This simplicity promotes ease of core assembly. The moderator elements can each be fabricated with differing volume fractions of hydride within its beryllium container. The elements all have the same outer dimensions. This design approach allows the hydride fraction to be varied radially without complicating the mechanical assembly of the core.
**Figure 3.6-12** SPACE-R Radial Power Distribution
Figure 3.6-13 SPACE-R Radial Power Distribution
The radial power distribution of the uniform core shows a power variance that is fairly satisfactory, except for the innermost 6 TFEs. These pins have BOL radial power factors of 1.34. SPI has adopted an interim goal of 1.25 for maximum pin radial power factor.

The simple 2 zone variant shows a marked improvement for the innermost TFEs, which now meet the interim goal. One edge TFE has now exceeded the goal, but it is located adjacent to an irregular local region of excess moderator. This irregularity can be easily dealt with, and the conclusion of our study is that even simple zoning, using the SPI approach, can flatten the radial power to acceptable levels. However, some loss of reactivity is to be expected from this power flattening.

3.6.5.2 TOPAZ III Core

The Russian 50 kWe (gross) TOPAZ III core has been calculated for radial power distribution as well. The result is shown in Figure 3.6-14. The TOPAZ III consists of an inner core (which is, in fact, a TOPAZ II) surrounded by four (4) quadrants of separately contained zirconium hydride and converters. The TOPAZ III core is quite large - 40 cm long and 50 cm in diameter. Each quadrant contains a control rod for water immersion safety.

Note that the power distribution is quite poor. The center TFE has a peaking factor of well over 2. The six TFEs surrounding the center rod have peaking factors of over 1.6. In addition, the control rod follower in the outer core region induces a large azimuthal power variation in this region as well. Given the monolithic nature of the calandria arrangement and the already crowded core it seems unlikely that these power variances can be corrected without resorting to TFE-by-TFE enrichment variation. This, in turn, would surely result in a large reactivity penalty.
Figure 3.6-14 Topaz III Radial Power Distribution
3.6.6 Temperature Coefficients

As is well known, moderated incore thermionic reactors possesses positive moderator temperature coefficients. Recently, the TOPAZ II was analyzed by a US Government team to evaluate the safety implications of many issues including its moderator's positive temperature coefficient. The conclusion on the TOPAZ II moderator positive temperature coefficient is that it improves the overall safety of the power system.

The positive moderator temperature coefficient is readily understood as arising from the enhanced ability of more energetic thermal neutrons to penetrate from the moderator to the fuel. The coolant contributes a temperature effect due to its change in density from room temperature to operating conditions. This effect is negative, for the YHx moderated core. A negative coolant density coefficient is an important safety advantage for the YHx moderation option, since it guarantees that coolant boiling will not increase core reactivity.

Various components of the SPACE-R temperatures effect are shown in Figure 3.6-15. The upper curve gives the moderator temperature effect for a 150 TFE core with a 2 mm emitter and a 3/5 mm (30 mil) SS316L sheath. The next lower curve gives the same effect for the same core in which TFEs have a thinner SS316L sheath. The influence of the sheath thickness is clearly dramatic. The moderator temperature effect was also calculated for a 150 TFE core with a thinner emitter. This curve is indistinguishable from the temperature axis (ie, the effect is nil). This core also had a 30 mil SS316L sheath. The effect of emitter thickness is even more dramatic than the effect of sheath thickness. Also shown in the Figure are the coolant and core density reactivity effects.

A significant point to be made here is that the net temperature reactivity coefficient (excluding the fuel and emitter Doppler coefficients) is close to zero, at operating temperature, for the SPACE-R baseline design. This means that the prompt (and negative) Doppler effect should make the SPACE-R stable in power operation. The TOPAZ II has shown that inherent power stability is not necessary for safe operation of thermionics incore reactors; however, long term reliability of the control system may require a duty cycle close to zero.
Figure 3.6-15 SPACE-R Temperature Effect
3.6.7 New Baseline Core

At the beginning of this effort the nominal SPACE-R core design was defined by the parameters given in the left-hand Point-of-departure design column of Table 3.6-3. Recent refinements include a thicker emitter and collector, and a thinner sheath. The sheath is now molybdenum rather than steel. The steel clad on the moderator element has been eliminated.

The thicker emitter and collector have been specified to improve conversion efficiency and specific power. The steel sheath has been replaced with molybdenum in order to improve compatibility with beryllium, and to allow elimination of the beryllium clad.

Table 3.6-3 Baseline Core Design Parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Point of Departure</th>
<th>Current</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number TFEs</td>
<td>150</td>
<td>150</td>
</tr>
<tr>
<td>Core Height</td>
<td>35 cm</td>
<td>36</td>
</tr>
<tr>
<td>Emitter Material</td>
<td>Mo7Nb/W</td>
<td>Mo7Nb/W</td>
</tr>
<tr>
<td>Emitter Thickness</td>
<td>1.4875/.0125</td>
<td>1.9875/.0125</td>
</tr>
<tr>
<td>Collector Material</td>
<td>Mo</td>
<td>Mo</td>
</tr>
<tr>
<td>Collector Thickness</td>
<td>3/4 mm</td>
<td>1 1/3 mm</td>
</tr>
<tr>
<td>Sheath Material</td>
<td>SS316L</td>
<td>Mo</td>
</tr>
<tr>
<td>Sheath Thickness</td>
<td>3/4 mm</td>
<td>1/2 mm</td>
</tr>
<tr>
<td>Coolant Channel</td>
<td>3/4 mm</td>
<td>3/4 mm</td>
</tr>
<tr>
<td>Moderator</td>
<td>YH_{1.75} in Be</td>
<td>YH_{1.75} in Be</td>
</tr>
<tr>
<td>Moderator Clad</td>
<td>SS316L</td>
<td>none</td>
</tr>
<tr>
<td>Moderator Clad Thickness</td>
<td>1/4 mm</td>
<td>0</td>
</tr>
<tr>
<td>TFE Pitch</td>
<td>3.41</td>
<td>3.5</td>
</tr>
</tbody>
</table>

3.6.8 Neutron and Gamma Heating

In support of the mechanical design of control drums and reflector requires, Argonne National Laboratory (ANL) has performed detailed neutron and gamma heating analysis for selected radial and axial regions of interest. These calculations were done using the 3D MCNP Monte Carlo code.
ANL has performed MCNP calculations of steady operational heating rates due to neutron and gamma interaction in various parts of the core and reflector. These heating rates are sufficient to define axial and radial heating loads with both the control drums "out" and control drums "in". Results are given in Tables 3.6-4 through 3.6-11. These values are keyed to the geometry defined in Figures 3.6-16 through 3.6-20.

A fully coupled neutron gamma calculation was performed for each of these studies (drums "in", and drums "out"). The gamma field defined by this study include all fission photons plus secondary production in all structural materials except yttrium. Yttrium gamma production cross sections are not currently in any U.S. data set.

3.6.4 Design Basis Reactivity Tables

In order to establish design reactivity goals for different core design options and to provide a parity basis for comparing trade options, SPI has developed design basis reactivity tables for power systems with 1.5 year, 5 year, and 10 year life. See Tables 3.6-12 to -14. The entries in these tables are of three general types: those for depletion, those for margin, and those for heatup.

The depletion allowances are for fuel burnup (plus fission product buildup) and for gadolinia residual. The burnup reactivity was calculated using REBUS, and used a 7.2% net system efficiency. The gadolinia residual was also estimated. The heatup allowances are calculated values which correspond to the losses (gains) in reactivity associated with: fuel Doppler; emitter Doppler (Mo7Nb); collector Doppler (Mo); moderator temperature; coolant density decrease; core and fuel density decrease; fuel expansion; reflector temperature increase; and fuel axial redistribution. The reactivity change due to expected hydride axial redistribution was calculated and verified to be consistent with zero.
### Table 3.6-4 Neutron and Gamma Heating - Upper Drum Be

<table>
<thead>
<tr>
<th>SEGMENT NUMBER</th>
<th>AREA (sq cm)</th>
<th>VOLUME (cu cm)</th>
<th>TOTAL/AVERAGE ENERGY DEPOSITION [MeV and (W/cu cm)]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>B4C in</td>
</tr>
<tr>
<td>1</td>
<td>3.0643875</td>
<td>180.79886</td>
<td>--</td>
</tr>
<tr>
<td>2</td>
<td>5.2393917</td>
<td>309.12411</td>
<td>--</td>
</tr>
<tr>
<td>3</td>
<td>6.2444792</td>
<td>368.42427</td>
<td>9.4E-3/2.0E-2</td>
</tr>
<tr>
<td>4</td>
<td>6.6647508</td>
<td>393.22030</td>
<td>1.3E-2/2.5E-2</td>
</tr>
<tr>
<td>5</td>
<td>6.3899344</td>
<td>377.00613</td>
<td>1.6E-2/3.3E-2</td>
</tr>
<tr>
<td>6</td>
<td>5.5349988</td>
<td>326.56493</td>
<td>1.7E-2/4.0E-2</td>
</tr>
<tr>
<td>7</td>
<td>4.1444646</td>
<td>244.52341</td>
<td>1.6E-2/5.1E-2</td>
</tr>
<tr>
<td>8</td>
<td>1.7872284</td>
<td>105.44648</td>
<td>1.5E-3/1.1E-2</td>
</tr>
<tr>
<td>9</td>
<td>6.5165507</td>
<td>384.47649</td>
<td>7.4E-3/1.5E-2</td>
</tr>
<tr>
<td>TOTAL</td>
<td></td>
<td></td>
<td>8.0E-2/2.8E-2</td>
</tr>
</tbody>
</table>

### Table 3.6-5 Neutron and Gamma Heating - Upper Drum B4C

<table>
<thead>
<tr>
<th>SEGMENT NUMBER</th>
<th>AREA (sq cm)</th>
<th>VOLUME (cu cm)</th>
<th>TOTAL/AVERAGE ENERGY DEPOSITION [MeV and (W/cu cm)]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>B4C in</td>
</tr>
<tr>
<td>A</td>
<td>0.3231811</td>
<td>19.067685</td>
<td>1.7E-2/6.9E-1</td>
</tr>
<tr>
<td>B</td>
<td>0.7094804</td>
<td>41.859344</td>
<td>3.2E-2/5.9E-1</td>
</tr>
<tr>
<td>C</td>
<td>2.3720861</td>
<td>139.95308</td>
<td>--</td>
</tr>
<tr>
<td>D</td>
<td>1.7872284</td>
<td>105.44648</td>
<td>--</td>
</tr>
<tr>
<td>E</td>
<td>1.4481248</td>
<td>85.262446</td>
<td>6.7E-3/6.1E-1</td>
</tr>
<tr>
<td>F</td>
<td>2.71419</td>
<td>160.13721</td>
<td>1.5E-2/1.7E+1</td>
</tr>
<tr>
<td>TOTAL</td>
<td></td>
<td></td>
<td>2.7E-2/8.8E-1</td>
</tr>
</tbody>
</table>
Table 3.6-6 Neutron and Gamma Heating - Lower Drum Be

<table>
<thead>
<tr>
<th>SEGMENT NUMBER</th>
<th>AREA (sq cm)</th>
<th>VOLUME (cu cm)</th>
<th>TOTAL/AVERAGE ENERGY DEPOSITION [MeV and (W/cu cm)]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>B4C in</td>
</tr>
<tr>
<td>1</td>
<td>3.0643875</td>
<td>180.79886</td>
<td>--</td>
</tr>
<tr>
<td>2</td>
<td>5.2393917</td>
<td>309.12411</td>
<td>--</td>
</tr>
<tr>
<td>3</td>
<td>6.2444792</td>
<td>368.42427</td>
<td>1.0E-2/2.1E-2</td>
</tr>
<tr>
<td>4</td>
<td>6.6647508</td>
<td>393.22030</td>
<td>1.4E-2/2.7E-2</td>
</tr>
<tr>
<td>5</td>
<td>6.3899344</td>
<td>377.00613</td>
<td>1.7E-2/3.5E-2</td>
</tr>
<tr>
<td>6</td>
<td>5.5349988</td>
<td>326.56493</td>
<td>1.8E-2/4.2E-2</td>
</tr>
<tr>
<td>7</td>
<td>4.1444646</td>
<td>244.52341</td>
<td>1.8E-2/5.7E-2</td>
</tr>
<tr>
<td>8</td>
<td>1.7872284</td>
<td>105.44648</td>
<td>1.6E-3/1.2E-2</td>
</tr>
<tr>
<td>9</td>
<td>6.5165507</td>
<td>384.47649</td>
<td>8.1E-3/1.6E-2</td>
</tr>
<tr>
<td>TOTAL</td>
<td></td>
<td></td>
<td>8.6E-2/3.0E-2</td>
</tr>
</tbody>
</table>

Table 3.6-7 Neutron and Gamma Heating - Lower Drum B4C

<table>
<thead>
<tr>
<th>SEGMENT NUMBER</th>
<th>AREA (sq cm)</th>
<th>VOLUME (cu cm)</th>
<th>TOTAL/AVERAGE ENERGY DEPOSITION [MeV and (W/cu cm)]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>B4C in</td>
</tr>
<tr>
<td>A</td>
<td>0.3231811</td>
<td>19.067685</td>
<td>1.9E-2/7.7E-1</td>
</tr>
<tr>
<td>B</td>
<td>0.7094804</td>
<td>41.859344</td>
<td>3.5E-2/6.4E-1</td>
</tr>
<tr>
<td>C</td>
<td>2.3720861</td>
<td>139.95308</td>
<td>--</td>
</tr>
<tr>
<td>D</td>
<td>1.7872284</td>
<td>105.44648</td>
<td>--</td>
</tr>
<tr>
<td>E</td>
<td>1.4451245</td>
<td>85.262346</td>
<td>7.3E-2/6.6E-1</td>
</tr>
<tr>
<td>F</td>
<td>2.71419</td>
<td>160.13721</td>
<td>1.6E-1/7.7E-1</td>
</tr>
<tr>
<td>Total</td>
<td></td>
<td></td>
<td>2.9E-1/7.3E-1</td>
</tr>
</tbody>
</table>
### Table 3.6-8 Neutron and Gamma Heating - Radial Reflector

<table>
<thead>
<tr>
<th>SEGMENT NUMBER</th>
<th>AREA (sq cm)</th>
<th>VOLUME (cu cm)</th>
<th>TOTAL/AVERAGE ENERGY DEPOSITION [MeV and (W/cu cm)]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>B4C in</td>
</tr>
<tr>
<td>a</td>
<td>1.900121</td>
<td>112.10714</td>
<td>1.2E-2/8.3E-2</td>
</tr>
<tr>
<td>b</td>
<td>4.8145493</td>
<td>284.05841</td>
<td>2.7E-2/7.3E-2</td>
</tr>
<tr>
<td>c</td>
<td>12.98819</td>
<td>766.30321</td>
<td>6.5E-2/6.5E-2</td>
</tr>
<tr>
<td>d</td>
<td>6.4269697</td>
<td>379.19062</td>
<td>2.3E-2/4.7E-2</td>
</tr>
<tr>
<td>e</td>
<td>5.991505</td>
<td>353.94988</td>
<td>1.6E-2/3.5E-2</td>
</tr>
<tr>
<td>f</td>
<td>6.7646372</td>
<td>399.05459</td>
<td>1.4E-2/2.7E-2</td>
</tr>
<tr>
<td>g</td>
<td>16.118208</td>
<td>954.74272</td>
<td>2.2E-2/1.8E-2</td>
</tr>
<tr>
<td>h</td>
<td>9.3343696</td>
<td>550.72782</td>
<td>9.4E-3/1.3E-2</td>
</tr>
<tr>
<td>TOTAL</td>
<td></td>
<td></td>
<td>1.9E-1/3.9E-2</td>
</tr>
</tbody>
</table>

### Table 3.6-9 Neutron and Gamma Heating - Yttrium Hydride in Core

<table>
<thead>
<tr>
<th>SEGMENT NUMBER</th>
<th>AREA (sq cm)</th>
<th>VOLUME (cu cm)</th>
<th>TOTAL/AVERAGE ENERGY DEPOSITION [MeV and (W/cu cm)]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>B4C in</td>
</tr>
<tr>
<td>30</td>
<td>0.4345259</td>
<td>15.208407</td>
<td>2.5E-2/1.3E+0</td>
</tr>
<tr>
<td>31</td>
<td>1.3035777</td>
<td>45.625195</td>
<td>7.5E-2/1.3E+0</td>
</tr>
<tr>
<td>32</td>
<td>2.1726296</td>
<td>76.042036</td>
<td>1.2E-1/1.2E+0</td>
</tr>
<tr>
<td>33</td>
<td>3.0416814</td>
<td>106.45885</td>
<td>1.4E-1/1.0E+0</td>
</tr>
</tbody>
</table>
### Table 3.6-10  Neutron and Gamma Heating (W/cu cm) -Axial Segments  
(MCNP Model: 30 degrees sector/half height)

<table>
<thead>
<tr>
<th>AXIAL SEGMENT  (CM)</th>
<th>RADIAL REFLECTOR Cell # 4</th>
<th>RADIAL REFLECTOR Cell # 5</th>
<th>UPPER DRUM Be Cell # 6</th>
<th>UPPER DRUM B4C Cell # 7</th>
<th>LOWER DRUM Be Cell # 8</th>
<th>LOWER DRUM B4C Cell # 9</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0 to 3.0</td>
<td>5.6E-2 6.1E-2</td>
<td>1.3E-1 1.4E-1</td>
<td>4.5E-2 6.9E-2</td>
<td>1.1E+0 5.4E-1</td>
<td>4.8E-2 6.6E-2</td>
<td>1.2E+0 5.9E-1</td>
</tr>
<tr>
<td>3.0 to 6.0</td>
<td>5.3E-2 5.9E-2</td>
<td>1.3E-1 1.3E-1</td>
<td>4.5E-2 6.0E-2</td>
<td>1.1E+0 5.4E-1</td>
<td>4.8E-2 6.6E-2</td>
<td>1.1E+0 5.9E-1</td>
</tr>
<tr>
<td>6.0 to 9.0</td>
<td>4.9E-2 5.7E-2</td>
<td>1.2E-1 1.3E-1</td>
<td>4.1E-2 5.7E-2</td>
<td>9.9E-1 5.0E-1</td>
<td>4.5E-2 6.0E-2</td>
<td>1.0E+0 5.2E-1</td>
</tr>
<tr>
<td>9.0 to 12.0</td>
<td>4.5E-2 5.1E-2</td>
<td>1.1E-1 1.2E-1</td>
<td>3.8E-2 5.1E-2</td>
<td>8.9E-1 4.5E-1</td>
<td>4.1E-2 5.4E-2</td>
<td>9.4E-1 4.8E-1</td>
</tr>
<tr>
<td>12.0 to 15.0</td>
<td>3.8E-2 4.5E-2</td>
<td>9.0E-2 1.0E-1</td>
<td>3.2E-2 4.4E-2</td>
<td>7.7E-1 4.1E-2</td>
<td>3.4E-2 4.7E-2</td>
<td>7.9E-1 4.5E-1</td>
</tr>
<tr>
<td>15.0 to 17.5</td>
<td>3.2E-2 3.7E-2</td>
<td>7.3E-2 8.2E-2</td>
<td>2.6E-2 3.8E-2</td>
<td>6.5E-1 3.5E-1</td>
<td>2.9E-2 3.8E-2</td>
<td>7.1E-1 3.8E-1</td>
</tr>
<tr>
<td>17.5 to 20.5</td>
<td>2.5E-2 2.9E-2</td>
<td>5.2E-2 5.8E-2</td>
<td>2.1E-2 2.9E-2</td>
<td>5.4E-1 2.7E-1</td>
<td>2.2E-2 3.0E-2</td>
<td>5.9E-1 3.2E-1</td>
</tr>
<tr>
<td>20.5 to 23.5</td>
<td>1.8E-2 2.0E-2</td>
<td>3.4E-2 3.9E-2</td>
<td>1.6E-2 2.1E-2</td>
<td>4.2E-1 2.2E-1</td>
<td>1.7E-2 2.2E-2</td>
<td>4.5E-1 2.3E-1</td>
</tr>
<tr>
<td>23.5 to 29.5</td>
<td>1.0E-2 1.1E-2</td>
<td>1.9E-2 2.1E-1</td>
<td>9.1E-3 1.2E-2</td>
<td>2.0E-1 1.1E-1</td>
<td>9.8E-3 1.2E-2</td>
<td>2.1E-1 1.1E-1</td>
</tr>
<tr>
<td>TOTAL</td>
<td>3.4E-2 3.7E-2</td>
<td>7.8E-2 8.4E-2</td>
<td>2.8E-2 3.9E-2</td>
<td>6.8E-1 3.5E-1</td>
<td>3.0E-2 4.2E-2</td>
<td>7.3E-1 3.7E-1</td>
</tr>
</tbody>
</table>

Energy Deposition with "B4C in" and "B4C out".
Table 3.6-11  Neutron and Gamma Heating (MeV and W/cu cm) - Yttrium Hydride Axial Segments (MCNP Model: 30 degrees sector/half height)

<table>
<thead>
<tr>
<th>AXIAL SEGMENT (CM)</th>
<th>CELL # 30 (MeV and w/cu cm)</th>
<th>CELL # 31 (MeV and w/cu cm)</th>
<th>CELL # 32 (MeV and w/cu cm)</th>
<th>CELL # 33 (MeV and w/cu cm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0 to 3.0</td>
<td>5.2E-3/1.5E+0</td>
<td>1.5E-2/1.5E+0</td>
<td>2.3E-2/1.4E+0</td>
<td>2.9E-2/1.2E+0</td>
</tr>
<tr>
<td>4.4E-3/1.2E+0</td>
<td>1.3E-2/1.2E+0</td>
<td>2.1E-2/1.1E+0</td>
<td>2.7E-2/1.0E+0</td>
<td>2.6E-2/1.0E+0</td>
</tr>
<tr>
<td>3.0 to 6.0</td>
<td>4.9E-3/1.4E+0</td>
<td>1.5E-2/1.5E+0</td>
<td>2.3E-2/1.4E+0</td>
<td>2.8E-2/1.2E+0</td>
</tr>
<tr>
<td>4.3E-3/1.2E+0</td>
<td>1.3E-2/1.2E+0</td>
<td>2.0E-2/1.1E+0</td>
<td>2.6E-2/1.0E+0</td>
<td>2.5E-2/9.7E-1</td>
</tr>
<tr>
<td>6.0 to 9.0</td>
<td>4.6E-3/1.4E+0</td>
<td>1.4E-2/1.4E+0</td>
<td>2.1E-2/1.2E+0</td>
<td>2.7E-2/1.1E+0</td>
</tr>
<tr>
<td>4.0E-3/1.1E+0</td>
<td>1.2E-2/1.1E+0</td>
<td>1.9E-2/1.0E+0</td>
<td>2.5E-2/9.7E-1</td>
<td>2.2E-2/8.5E-1</td>
</tr>
<tr>
<td>9.0 to 12.0</td>
<td>4.1E-3/1.2E+0</td>
<td>1.3E-2/1.3E+0</td>
<td>1.9E-2/1.1E+0</td>
<td>2.4E-2/1.0E+0</td>
</tr>
<tr>
<td>3.8E-3/1.0E+0</td>
<td>1.1E-2/9.9E-1</td>
<td>1.7E-2/9.2E-1</td>
<td>2.2E-2/8.5E-1</td>
<td>2.0E-2/7.7E-1</td>
</tr>
<tr>
<td>12.0 to 15.0</td>
<td>3.7E-3/1.1E+0</td>
<td>1.1E-2/1.1E+0</td>
<td>1.7E-2/1.0E+0</td>
<td>2.1E-2/8.9E-1</td>
</tr>
<tr>
<td>3.4E-3/9.2E-1</td>
<td>9.7E-3/8.8E-1</td>
<td>1.5E-2/8.1E-1</td>
<td>2.0E-2/7.7E-1</td>
<td>2.0E-2/7.7E-1</td>
</tr>
<tr>
<td>15.0 to 17.5</td>
<td>2.5E-3/7.4E-1</td>
<td>7.5E-3/8.9E-1</td>
<td>1.2E-2/8.5E-1</td>
<td>1.4E-2/7.1E-1</td>
</tr>
<tr>
<td>2.3E-3/6.2E-1</td>
<td>6.6E-3/7.2E-1</td>
<td>1.1E-2/7.2E-1</td>
<td>1.4E-2/6.5E-1</td>
<td>1.4E-2/6.5E-1</td>
</tr>
<tr>
<td>TOTAL</td>
<td>2.5E-2/1.3E+0</td>
<td>7.4E-2/1.3E+0</td>
<td>1.2E-1/1.2E+0</td>
<td>1.4E-1/1.0E+0</td>
</tr>
<tr>
<td></td>
<td>2.2E-2/1.0E+0</td>
<td>6.6E-2/1.0E+0</td>
<td>1.0E-1/9.3E-1</td>
<td>1.3E-1/8.6E-1</td>
</tr>
</tbody>
</table>

Energy Deposition with "B4C in" and "B4C out".
Figure 3.6-16 SPACE-R 30° Radial Reflector Control Drums "In"
Figure 3.6-17 SPACE-R 30° Radial Reflector Control Drums "Out"
Figure 3.6-18 SPACE-R Moderator Heating Cells
Figure 3.6-19  SPACE-R 30° Sector Neutron Poison Heating Cells
Figure 3.6-20  SPACE-R Control Drum "Out" Heating Cell
The margin allowance corresponds to two control drums stuck in the "in" position. In fact, the SPI plan is to design the drums to fail "as is." Consequently, this allowance corresponds to no specific scenario. It is appropriate to include such an allowance, however, and appropriate that it increases with the system design life.

These tables have been issued as part of the configuration control documentation.

Table 3.6-12 Design Basis Reactivity for 10 Year System Life

<table>
<thead>
<tr>
<th>Depletion Allowances</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Burnup</td>
<td>3.6% ΔK/K</td>
</tr>
<tr>
<td>Gadolinia Residual</td>
<td>0.5%</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Margin</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Stuck Drums</td>
<td>1.3%</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Heat Up</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Core Temperature</td>
<td>-0.8%</td>
</tr>
<tr>
<td>Coolant Density</td>
<td>0.2%</td>
</tr>
<tr>
<td>Core Density</td>
<td>0.37%</td>
</tr>
<tr>
<td>Fuel Axial Redistribution</td>
<td>0.4%</td>
</tr>
<tr>
<td>Hydride Stoichiometry</td>
<td>---</td>
</tr>
<tr>
<td>Doppler</td>
<td></td>
</tr>
<tr>
<td>Fuel plus Emitter (2000K)</td>
<td>1.13%</td>
</tr>
<tr>
<td>Collector (850K)</td>
<td>0.1%</td>
</tr>
</tbody>
</table>

Cold BOL $k_{\text{eff}}$ required: 1.068
Table 3.6-13 Design Basis Reactivity for 5 Year Life

<table>
<thead>
<tr>
<th>Depletion Allowances</th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Burnup</td>
<td>1.8% ΔK/K</td>
<td></td>
</tr>
<tr>
<td>Gadolinia Residual</td>
<td>0.5%</td>
<td></td>
</tr>
<tr>
<td>Margin</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stuck Drums</td>
<td>0.65%</td>
<td></td>
</tr>
<tr>
<td>Heat Up</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Temperature</td>
<td>-0.8%</td>
<td></td>
</tr>
<tr>
<td>Coolant Density</td>
<td>0.2%</td>
<td></td>
</tr>
<tr>
<td>Core Density</td>
<td>0.37%</td>
<td></td>
</tr>
<tr>
<td>Fuel Axial Redistribution</td>
<td>0.4%</td>
<td></td>
</tr>
<tr>
<td>Hydride Stoichiometry</td>
<td>---</td>
<td></td>
</tr>
<tr>
<td>Doppler</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fuel plus Emitter (2000K)</td>
<td>1.13%</td>
<td></td>
</tr>
<tr>
<td>Collector (850K)</td>
<td>0.1%</td>
<td></td>
</tr>
<tr>
<td>Cold BOL k(_{\text{eff}}) required:</td>
<td>1.0435</td>
<td></td>
</tr>
</tbody>
</table>

Table 3.6-14 Design Basis Reactivity for 1.5 Year Life

<table>
<thead>
<tr>
<th>Depletion Allowances</th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Burnup</td>
<td>0.54% ΔK/K</td>
<td></td>
</tr>
<tr>
<td>Gadolinia Residual</td>
<td>0.5%</td>
<td></td>
</tr>
<tr>
<td>Margin</td>
<td>-0-</td>
<td></td>
</tr>
<tr>
<td>Heat Up</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Core Temperature</td>
<td>-0.8%</td>
<td></td>
</tr>
<tr>
<td>Coolant Density</td>
<td>-0.2%</td>
<td></td>
</tr>
<tr>
<td>Core Density</td>
<td>0.37%</td>
<td></td>
</tr>
<tr>
<td>Fuel Axial Redistribution</td>
<td>0.4%</td>
<td></td>
</tr>
<tr>
<td>Hydride Stoichiometry</td>
<td>---</td>
<td></td>
</tr>
<tr>
<td>Doppler</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fuel plus Emitter (2000K)</td>
<td>1.13%</td>
<td></td>
</tr>
<tr>
<td>Collector (850K)</td>
<td>0.1%</td>
<td></td>
</tr>
<tr>
<td>Cold BOL k(_{\text{eff}}) required:</td>
<td>1.0244</td>
<td></td>
</tr>
</tbody>
</table>
3.7 Moderator Stability and Containment

The moderator is a critical component of the SPACE-R reactor since it controls the reactivity. In the beryllium - yttrium hydride moderator, the beryllium shell acts both as a moderating agent and as a barrier for the containment of hydrogen from the yttrium hydride. A number of trade studies were undertaken to evaluate the Be/YH₃ moderator in comparison with other alternate choices.

3.7.1 Moderator Choices

Some of the moderator materials and configurations, considered for the SPACE-R reactor are as follows:

(1) Zirconium hydride clad in hastelloy or stainless steel with solaramic glass (SNAP)
(2) Zirconium hydride in calandria (Topaz)
(3) Zirconium hydride clad in Hastelloy or stainless steel with single wall SPI glass
(4) Zirconium hydride clad in Hastelloy or stainless steel with double wall SPI glass
(5) Zirconium hydride clad in hastelloy or stainless steel with one layer of SPI glass and one layer of CIS coating
(6) Zirconium hydride clad in beryllium
(7) Yttrium hydride clad in beryllium.

Advantages of using zirconium hydride are as follows:

1) The technology has been developed and demonstrated by Topaz II systems up to 2 years.
2) It has good moderating characteristics.
3) The positive moderator temperature coefficient allows a lower excess reactivity in the reactor.
Disadvantages of using zirconium hydride include the following:

1) High dissociation pressure (200 to 500 torr) makes hydrogen containment difficult for a long life system. SNAP solaramic glass had 1 to 2% hydrogen loss per year (5 to 10% reactivity loss due to hydrogen loss in 10 years). Topaz II has about 1% hydrogen loss per year (5% reactivity loss in 10 years) even with a glass layer, a coating on the hydride block and the cover gas. Topaz II contains two 40-liter cover gas bottles to make up the cover gas loss to maintain the 1%/year H₂ loss rate. SPI double glass technology with H₂ getter in between will provide less than 0.1% hydrogen loss in 10 years. However, the double walls make the core size bigger. In addition, the technology requires further development and demonstration under irradiation.

2) The uniform hydriding process of large Zr blocks has not been established yet in the U.S. It is, however, an established technology in Russia.

3) The positive moderator temperature coefficient requires an active reactor control during operation. This could be a preferred approach for a short life system. However, a long life (10-year) system might prefer a passive reactor control.

Advantages of using yttrium hydride include the following:

1) When combined with Be, it provides good moderating performance (close to zirconium hydride) in thermionic reactors sized for a 40 kWe power level.

2) The hydrogen loss problem is practically avoided because of the low hydrogen dissociation pressure (0.01 to 0.03 torr) of yttrium hydride at the moderator operating temperature. Therefore, reliable hydrogen containment for a 10 year life system is easily provided. Less than 0.1% of hydrogen is expected to be lost from the moderator in 10 years when yttrium hydride is contained in Be, Mo or even in stainless steel with an oxidized surface. Be is a good hydrogen barrier.
3) The moderator temperature coefficient can be either positive or nearly zero, depending on the hydride volume fraction in the core, providing flexibility for the reactor control design.

4) The radial power distribution can be flattened for optimum TFE performance with varying Be/$\text{YH}_x$ ratios across the core.

Disadvantages of using the yttrium hydride - Be moderator are as follows:

1) Joining beryllium is more difficult than stainless steel. Electron beam welding and laser welding are promising candidates.

2) A molybdenum plasma-spray coating will be needed on Be for NaK compatibility when the oxygen level in NaK is above 5 ppm. If the oxygen level in NaK can be maintained below 5 ppm, cladding on Be may not be needed.

3) Because of the low $\text{H}_2$ pressure on $\text{YH}_{1.75}$, He filling (1–2 psi) is needed between the hydride and the Be containment for thermal control. Be end cap will be brazed on the Be tube before EB welding to contain He inside. However, the He fill provides convenient leak checking after the moderator element fabrication and welding.

4) Experimental demonstration of the Be-$\text{YH}_{1.75}$ is needed. Incore and out-of-core experiments are planned in Phase I.

Based on the above reasoning and the following moderator trade study results, the Be-$\text{YH}_{1.75}$ moderator concept was tentatively chosen as the baseline moderator. Technical input from INERTEK (after the INERTEK subcontract initiation) will be assessed before the final choice is made. The zirconium hydride moderator based on the Topaz II design can also be used for the 18 month life system if desired without changing the core structural design.
3.7.2 Moderator Neutronics

In order to evaluate the relative merits of the various moderator alternatives, the neutronic properties of the moderator materials, beryllium, yttrium hydride and zirconium hydride, given in Table 3.7-1, were considered. The lower macroscopic thermal neutron cross section of zirconium hydride compared to yttrium hydride is due to the low microscopic thermal neutron cross section of zirconium (0.180 barns) compared to that of yttrium (1.28 barns). Judging from the critical mass which is a measure of reactivity, zirconium hydride appears to be the most desirable moderator material. However, zirconium hydride has a hydrogen dissociation pressure about 4 orders of magnitude higher than that of yttrium hydride as shown in Figure 3.7-1. Since hydrogen containment is the major criterion to maintain the reactivity over the long life of the reactor, the high hydrogen dissociation pressure of zirconium hydride puts it at a disadvantage compared to yttrium hydride.

Table 3.7-1 Moderator Neutronics Comparison of Beryllium, Yttrium Hydride and Zirconium Hydride

<table>
<thead>
<tr>
<th>Material</th>
<th>Slowing Down Power ((\xi\Sigma_s))</th>
<th>Absorption Cross Section, (\Sigma_a) (cm )</th>
<th>Moderating Ratio ((\xi\Sigma_s/\Sigma_a))</th>
<th>Critical Mass (lb(^{235})U)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Beryllium</td>
<td>0.16</td>
<td>0.0011</td>
<td>150</td>
<td>39.82</td>
</tr>
<tr>
<td>(\text{YH}_{1.85})</td>
<td>1.22</td>
<td>0.054</td>
<td>23</td>
<td>15.43</td>
</tr>
<tr>
<td>(\text{ZrH}_{1.84})</td>
<td>1.54</td>
<td>0.030</td>
<td>62</td>
<td>12.77</td>
</tr>
</tbody>
</table>

In the course of recent studies of thermionic space systems, both SPI and LANL independently discovered that under the right conditions, a hybrid moderator of beryllium and hydride could be better than either beryllium or hydride alone. This effect is shown in Figure 3.7-2, where the reactivity is shown as a function of hydride volume fraction for both \(\text{YH}_{1.75}\) and \(\text{ZrH}_{1.84}\) in a 40 cm thick reactor. The performance of the zirconium hydride moderated lattice is better than the yttrium hydride moderated lattice except at very low
Figure 3.7-1 Hydride Dissociation Pressures
Figure 3.7-2 Reactivity of metal hydride contained in beryllium
volume fraction of hydride. However, the optimal performance of the beryllium - yttrium hydride moderated lattice is practically as good as the lattice with 100% zirconium hydride. The superior performance of the beryllium-hydride moderator depends on the core size and on the competitive effects of neutron leakage and parasitic thermal neutron absorption.

3.7.3 Hydrogen Containment

A key performance criteria of the moderator is hydrogen containment to maintain the core reactivity over a 10 year system life. Also, hydrogen leaking out of the moderator has the potential to diffuse into and contaminate the inter-electrode space of the TFE.

The permeation of hydrogen in beryllium is strongly affected by surface condition, the thickness of surface oxide, surface and bulk traps, impurity content and microstructure. The permeation coefficient, K, of a gas through a material is given by the product of the diffusion coefficient, D, and solubility coefficient, S. The widely accepted experimental values of D and S of hydrogen in beryllium, obtained by Jones and Gibson (1967) are as follows:

\[
\log D \text{ (cm}^2/\text{sec}) = -(6.53 + 0.15) - (965 + 106)/T(°K) \quad [1]
\]

\[
\log S \text{ (scc/g.atom)} = (-2.12 + 0.27) + (0.095 + 0.221)/T(°K) \quad [2]
\]

Equation 2 indicates that the solubility of hydrogen in beryllium is almost independent of temperature. Swansinger (1986) measured the solubility of tritium in high purity beryllium and obtained an endothermic heat of solution of about 1 eV in the temperature range 440-510°C and exothermic behavior at lower temperature.

Causey et al (1990) measured the plasma driven permeation of deuterium through high purity (99.8%) beryllium of thickness in the range 0.05-0.25mm. They did not observe any permeation below 673K. In the temperature range 673-823K, the magnitude of permeation did not change monotonically with temperature and the slope of the log permeation versus time curve after the deuterium plasma was turned off was independent of temperature. They concluded that the permeation of deuterium through beryllium is strongly affected by the oxide layers on both sides of the material.
Figure 3.7-3 Hydrogen diffusion coefficient of various materials
Abramov et al (1990) measured the permeability and diffusibility of deuterium by gas driven permeation technique and used a multilayer permeation theory to eliminate the effect of the surface oxide. For beryllium samples of 99.8% and 99.0% purity they obtained 
\[ D = 6.7 \times 10^9 \exp(-28.4/RT) \text{ m}^2/\text{sec} \]
and 
\[ D = 8.0 \times 10^8 \exp(-35.1/RT) \text{ m}^2/\text{sec}, \]
respectively. These values are almost 2 orders of magnitude higher than those obtained previously as shown in Figure 3.7-3. The lower diffusion coefficients of earlier investigations is attributed to the presence of BeO which is an extremely strong barrier to the diffusion of hydrogen. Figure 3.7-3 shows that diffusivity of hydrogen in beryllium is lower than in most other materials.

In an experiment on the permeation of hydrogen through beryllium using low energy ion beam, Andrel et al (1992) found measurable permeation only above 750K. However, the time to permeate was too long and the amount that permeated was too small to be accounted for by simple diffusion theory. They attributed the results to surface effects and a diffusive permeation barrier. The kinetics of tritium release from irradiated beryllium during annealing measured by Baldwin et al (1991) showed that irradiation damage and the presence of helium bubbles affect the permeation of hydrogen in beryllium.

The hydrogen permeation through the cladding of various alternate moderator choices for the SPACE-R is shown in Figure 3.7-4. Hydrogen permeation calculated from Jones and Gibson's (1967) data shows beryllium as an excellent hydrogen barrier compared to SNAP solaramic glass and SPI single wall glass. For laboratory-grade "ultra high" purity beryllium, theoretical permeability of hydrogen calculated from the data of Swan Singer (1986) and Billone et al is higher as shown in Figure 3.7-4. When the effect of oxide is "completely" eliminated, the value calculated from the data of Abramov et al (1990) gives a large value of permeability. However, ultra high purity Be with the surface oxide layer completely removed is not practical for mass production. Beryllium to be used in the moderator of the SPACE-R will not be of ultra high purity. In addition, the surface permeation barrier layer will not be removed. Therefore, the permeation rate through the Be layer in the SPACE-R moderator will be much lower than the ultra pure Be. The low permeation rate combined with the low hydrogen dissociation pressure of yttrium hydride is more than adequate for hydrogen retention in SPACE-R.
Figure 3.7-4 Summary of Hydrogen Permeation Rate Measurements
3.7.4 Radiation Stability

Neutron swelling is the major effect of irradiation on beryllium. It is primarily due to the nucleation and growth of transmutation helium bubbles. Tritium, produced by the reaction of fast neutrons with beryllium also diffuses into the helium bubbles and adds to the swelling problem. Beeston et al (1984) examined beryllium specimens irradiated at 710K to a neutron fluence of $1.0\times10^{22}/\text{cm}^2$ by electron microscopy and found a uniform distribution of voids at the grain boundaries. Gelles and Heinisch (1991) found bubbles at matrix and grain boundary dislocations.

Beeston et al (1984) have measured swelling as a function of irradiation temperature in the range 700-760K and fast neutron fluence in the range $0.7-1.3\times10^{22}/\text{cm}^2$ and derived the following equation using regression analysis:

$$\Delta V/V = 1.83 \times 10^{-58} \Phi^2 T^4$$

where $\Phi$ is the fluence in $\text{n/cm}^2$ for $E>1\text{MeV}$ and $T$ is the irradiation temperature in $\text{°K}$.

The fluence of neutrons with $E>1\text{MeV}$ in the proposed SPACE-R over a 10 year life time has been calculated by Gunther (1992) using MCNP. The calculated value of $1.77\times10^{21}/\text{cm}^2$ and the maximum possible temperature of 930 K in the beryllium shell of the hybrid moderator of the SPACE-R reactor are substituted in equation 3 to give a swelling value $\Delta V/V$ of 0.042% in 10 years. Data of Beeston et al (1984) along with the calculated data for the SPACE-R reactor operation range are shown in Figure 3.7-5.

Billone et al (1991) have also derived the following relationship between swelling and helium content in irradiated beryllium from experimental data:

$$\Delta V/V = (0.086+0.031)G_{n}^{\text{ppm}}\left[1+22.2G_{n}^{0.511}\right]\exp(-9322/T)\%$$

where $G_{n}$ is the helium concentration in appm/1000. Gunther (1992) has calculated $G_{n}$ for the SPACE-R using MNCP and obtained a value of 0.082. This value was substituted in
Figure 3.7-5  Swelling of Irradiated Beryllium
equation 4 and swelling was calculated as $0.043 \pm 0.016\%$. This is in excellent agreement with the value calculated using equation 3.

There is very little information in the literature on the neutron swelling of beryllium irradiated at temperature and fluence similar to those in the propose SPACE-R reactor. Data from Golchev et al (1977), covering the range where SPACE-R will operate, are shown in Figure 3.7-6. The grain size of beryllium appears to have a substantial effect on the amount of swelling. The swelling value calculated from equations 3 and 4 for the SPACE-R reactor at a neutron fluence of $1.77 \times 10^{21}/\text{cm}^2$ are slightly lower than the values of Golchev et al (1977).

Neutron swelling of beryllium is isotropic and, therefore, the linear dimensional change of the beryllium moderator in the SPACE-R reactor will be given by $\Delta L/L = (1/3) x (\Delta V/V) = 0.014\%$. The linear dimension in the cross section of the beryllium shell would increase by about 0.0002 cm due to neutron swelling. This amount is so small that it should not affect the integrity of the moderator or the NaK coolant gap in the core of the reactor.

The swelling of the hydride in the hybrid moderator due to neutron irradiation should also be taken into consideration. Volkov et al (1990) have investigated the swelling of ZrH$_{1.8}$, irradiated to fast neutron fluence in the range $2 \times 10^{20}-1.5 \times 10^{21}\text{cm}^{-2}$. Figure 3.7-7 shows their swelling data as a function of irradiation temperature. As indicated in this figure, if ZrH$_{1.8}$ is used in the SPACE-R moderator, the swelling would be about 1% in 10 years. Brager et al (1991) at HEDL have irradiated YH$_{1.7}$ to $3-5 \times 10^{21}\text{cm}^{-2}$ at temperature of 400-500°C in FFTF. This irradiation is to a greater fluence than expected in SPACE-R, but at a lower irradiation temperature. Their swelling data (Figure 3.7-7) shows no significant swelling of yttrium hydride.
Figure 3.7-6  Russian Beryllium Swelling Data
Figure 3.7-7  The Dependence of Hydride Swelling on Temperature and Fluence (Volkov et al)
3.7.5 Materials Compatibility

The materials used in the moderator and in the space between the TFE and moderator should be compatible. Any reaction, corrosion and deterioration of mechanical properties should be low enough so that the functionality and the integrity of the reactor components are not affected. A number of potential materials compatibility problems in the SPACE-R have been investigated and reported here.

3.7.5.1 Beryllium - Yttrium Hydride

In the technical literature, no information was found on the formation of yttrium beryllide by the reaction of beryllium and yttrium hydride. This reaction is not thermodynamically favorable because yttrium hydride is a very stable compound with a large negative free energy of formation whereas unstable beryllium hydride has a large positive free energy of formation and can only form by catalytic organometallic synthesis. Also below the eutectic temperature of 1070°C, beryllium and yttrium are insoluble in each other and at the moderator temperature of 650°C, any reaction to form YBe$_3$ is unlikely.

3.7.5.2 Beryllium - Stainless Steel

To enhance the compatibility of the moderator with NaK, a stainless steel cladding on Be was examined. From a consideration of the low solubility of constituents in the Be-Fe and Be-Ni phase diagrams and the low diffusivity of the elements in these systems at 650°C, it may appear that the formation of nickel beryllide and iron beryllide at the beryllium-stainless steel interface is not likely to occur. Beeston et al (1984) did not observe any reaction between beryllium and stainless steel in stainless steel encapsulated beryllium specimens held in EBR-II at 750K for 4 years.

Hoffman and Dienst (1990) have recently investigated the chemical reaction between beryllium and type 316 stainless steel in the temperature range 873-1173K with annealing times of 100, 400 and 1000 hours. Substantial reaction occurred at the beryllium-stainless steel interface and the reaction kinetics are shown in Figure 3.7-8. The growth of the reaction zone is thermally activated and at any temperature, the thickness of the reaction
Figure 3.7-8 Reaction between beryllium and 316 SS.
zone is proportional to the square root of the reaction time. Beryllium diffuses into stainless steel and reacts with nickel to form nickel beryllide. Auger analysis of a specimen after a 100 hour anneal at 1173K showed substantial nickel depletion of the steel matrix due to the formation of NiBe. At 873K, the reaction between beryllium and stainless steel was localized.

In the SPACE-R, considering a life span of 10 years and a moderator temperature of 930 K, Hoffman and Dienst’s relationship:

$$X^2/t = 0.26 \exp(-187950/RT)$$ \[5\]

where $X$ is the thickness of the reaction zone in cm and $t$ is the time in hours, gives a reaction zone thickness of 0.47mm. Therefore, to avoid the reaction between beryllium and the moderator cladding, a different cladding material should be used. Molybdenum, which is also compatible with NaK, is a good candidate. Alternately, a barrier layer between beryllium and stainless steel may be used to prevent their reaction. Another approach is to use beryllium without cladding if the oxygen level in NaK can be maintained at a low level (below 5 ppm) for compatibility with Be.

3.7.5.3 Stainless Steel - NaK

Corrosion of austenitic stainless steel in liquid sodium and NaK occurs by the solution of its constituents at the surface and the formation of a ferritic layer through which nickel and chromium diffuse from the interior. The chemical reaction of the coolant impurities, chiefly oxygen, is involved in the release of the alloying constituents to liquid sodium and NaK. Corrosion rate of stainless steel decreases linearly with decreasing oxygen content and is independent of oxygen content below 1 ppm oxygen.

Bagnall and Jacobs (1974) have normalized the oxygen concentration in sodium for a large volume of data on the corrosion of stainless steel for sodium velocity of $> 10$ ft/sec and obtained a relationship:

$$R/Q = 6.68525 \times 10^7 \exp(-18120/T)$$ \[6\]
where \( R \) = corrosion rate in mils/year

\[
\frac{R}{Q} = \frac{1.704 \times 10^7 + V \times 3.496 \times 10}{\exp\left(-\frac{18120}{T}\right)}
\]  

[7]

If the TFE sheath is made of stainless steel, the corrosion of this sheath will be critical since the thickness of the material is small (20 mils) and the NaK temperature in the core is high. In the core of the SPACE-R, the velocity of Nak is 0.92 m/sec (3.0 ft/sec). The maximum temperature of the NaK in the core is 925 K. Using these values in equation 7, \( \frac{R}{Q} = 6.898 \times 10^{-2} \). If the oxygen content of the NaK is maintained at 10 ppm or less, near the bottom of the stainless steel sheath, at most 7 mils of material may be corroded in 10 years. At the top of the sheath were the temperature is 825 K, approximately 0.7 mils of material will be corroded. This should be acceptable. If a lower oxygen level is maintained, the corrosion rate can be lowered further.

To prevent the sensitization of the stainless steel due to carbide formation, the NaK and stainless steel should be extra low carbon or the steel should contain carbide forming elements, such as niobium or tantalum. The formation of brittle Fe-Cr sigma phase in some cases may reduce the impact strength of stainless steel. Thorley et al (1982) have done a detailed investigation of the effect of sodium on type 316 and type 321 stainless steels. In the hot region of the sodium loop, the material was removed due to corrosion. In the intermediate temperature region, both surface and subsurface oxidation occurred with the formation of NaCrO_2, resulting in the removal of chromium and the break up of the surface material. The corrosion products deposited as nickel enriched iron particles in the cooler parts of the loop.
Figure 3.7-9  Corrosion of stainless steel in sodium normalized by the oxygen concentration
Austenitic stainless steel shows satisfactory mechanical properties in liquid sodium and NaK. High carbon sodium increases tensile and creep strengths, but decreases ductility. Exposure to liquid sodium increases the fatigue life, but decreases the impact strength.

3.7.5.4 Beryllium - NaK - Stainless Steel

If beryllium is used without any cladding and the TFE is housed in stainless steel sheath, the compatibility of both beryllium and stainless steel in liquid NaK should be considered. Beryllium forms a much more stable oxide than sodium. It reduces Na₂O by the reaction: Na₂O + Be = BeO + 2Na. The free energy of formation of BeO by this reaction is -52 Kcal/mol. In Liquid sodium or NaK containing oxygen, BeO formed at the surface of beryllium is nonadherent and is dislodged by the liquid metal. Also, in a dynamic system, the spalling of the nonadherent oxide increases with increasing velocity of liquid NaK. Calcium and thorium, which are stronger oxide formers than beryllium, can be added to NaK to react with oxygen and reduce the effective oxygen content, thus decreasing the corrosion rate of beryllium. Calcium is a much more effective gettering agent of oxygen than thorium. Nitriding of beryllium in the presence of nitrogen to form Be₃N₂ has been reported.

In the presence of stainless steel which contains nickel, material transfer of beryllium is an issue. Beryllium dissolves in sodium, gets transferred to stainless steel and forms BeNi or Be₂₁Ni₅. Table 3.7-2 taken from Balkwill et al (1979) shows that material transfer strongly depends on the distance between beryllium and stainless steel. Using chromium coating was of little benefit, since chromium forms Be₂Cr intermetallic compound with beryllium. In SPACE-R, the close proximity between the stainless steel sheath of the TFE and the beryllium shell of the moderator may cause material transfer. The depth of reaction in stainless steel due to material transfer from beryllium for a distance of separation of 15 mils, has been calculated to be 3.6 mil/year. This is larger than the normal corrosion of stainless steel in liquid NaK. Also, the problem will be more severe in areas where stainless steel may be in contact with beryllium.
Table 3.7-2 Material Transfer Between 304 Stainless Steel and Beryllium, Immersed in NaK-44 at 800°C for 500 Hours

<table>
<thead>
<tr>
<th>Distance between Beryllium and Stainless Steel (mils)</th>
<th>2</th>
<th>5</th>
<th>10</th>
<th>15</th>
<th>25</th>
<th>50</th>
</tr>
</thead>
<tbody>
<tr>
<td>Equivalent Concentration of Beryllium in Stainless Steel surface, (10^3 mg/cm²)</td>
<td>4950</td>
<td>2300</td>
<td>800</td>
<td>310</td>
<td>170</td>
<td>140</td>
</tr>
</tbody>
</table>

3.7.5.5 Beryllium - NaK - Molybdenum

If the stainless steel sheath of the TFE is replaced with a molybdenum sheath and the beryllium-hydride moderator is used without any cladding, the compatibility of beryllium and molybdenum in NaK will be of interest. Molybdenum is exceptionally inert to sodium and NaK. In experiments, no evidence of corrosion has been found in molybdenum held in liquid sodium for 5000 hours at 1500°C. As material transfer from beryllium requires a nickel containing material in proximity with beryllium, there will not be any material transfer.

There is little available data in literature on the corrosion of beryllium in liquid sodium or NaK at 925 K. Using low temperature corrosion data from Balk Will et al (1979) and an extrapolation method, the corrosion of beryllium in liquid sodium was calculated for a velocity of 3 ft/sec at 925 K. The calculated value varied from less than 3 mil/year for low oxygen sodium to 425 mil/year for sodium of unknown (high) oxygen content. Therefore, if beryllium is used without any cladding or protective coating, steps must be taken to ensure low oxygen content of liquid NaK.
3.7.5.6 Molybdenum - NaK

If the TFE sheath is made of molybdenum and the beryllium of the moderator is also coated (clad) with molybdenum, only the compatibility of molybdenum with NaK should be taken into account. Due to the strong corrosion resistance of molybdenum in NaK, there should be no compatibility problem. Plasma-spraying of thin Mo on Be has been identified as a promising process for Mo coating on Be. The thickness of Mo layer can be controlled to accommodate the thermal expansion differential.

3.8 Cesium Source

The most important trade for the Cs source is whether to utilize an integral reservoir in each TFE or use proven external central reservoir(s). The key issues are assembly, outgassing, loading, testing, reactor startup, endurance, proven experience, and reliability. The central Cs source is being considered as the baseline design for SPACE-R.

3.8.1 Central Cs Source

A well developed and demonstrated central Cs source is the Cs source used in Topaz I and II systems. The cesium pressure level is controlled by capillary force in the Topaz central source. The Topaz II cesium source basically consists of two cesium envelopes connected with each other by a capillary tube. Cesium vapor is generated in the cesium reservoir heated by the hot NaK in the first cesium envelope. The cesium vapor is then condensed in the capillary tube with a very small, wire-wound gap which is cooled partly by a radiation window open to space. The condensed cesium evaporates again from the other side of the capillary tube and fed to TFEs in the reactor core. The cesium pressure in the second cesium envelope is dynamically controlled by the pressure loss through the capillary gap. When the NaK temperature is high, the vapor pressure in the first Cs envelope and thus the flow rate through the capillary gap is increased, leading to a higher pressure drop in the gap. When the NaK temperature is low, the opposite occurs. As a result, the cesium pressure in the second cesium envelope is maintained at a near constant level (within 10% with a NaK temperature variation of up to 100 K). See Figure 3.8-1. The central reservoir provides a means of continuous outgassing of the emitter and collector with
Figure 3.8-1  Cesium pressure vs. the coolant temperature in Topaz II
a loss of Cs of only 1/2 g per day or up to 1 kg per 10 years per reservoir. Endurance test of nearly 6 years are completed.

The advantages of the central Cs reservoir as demonstrated by existing Topaz II and improved designs are as follows:

1. It is well developed, demonstrated on ground, and flight proven.

2. The open Cs system allows continuous purging of impurity gas from the diode gap, thus eliminating a key TFE degradation life-limiting mechanism.

3. Long life testing is in process in CIS. The existing central Cs system has been tested for nearly 6 years with 0.5 – 1 g of Cs flow rate per day. An improved Cs system is under test with about 2 years of successful demonstration to date at less than 0.5 g/day of Cs flow rate.

4. The Topaz II Cs source is readily available for early system flight test.

5. Modification for a closed Cs source design is possible with flexible options for impurity (O_2 and H_2) getters.

6. The Cs source is heated with the reactor coolant. No active control with an electrical heater is needed. The central Cs source can be configured to provide ground control of Cs pressure if needed.

7. Potential structural damage during startup due to arcing can be prevented with He fill. The proven and repeatedly tested startup procedure for incore thermionic converters is to fill the Interelectrode Gap (IEG) with helium when cold. Helium stays in place until startup and sufficient temperature is achieved to provide near full pressure Cs vaporization. Thus arcing and TFE damage can be avoided. Helium is then vented overboard to vacuum through the Cs condenser.
Disadvantages of the central Cs reservoir include the following:

1. Additional Cs plumbing, welds and insulating seals are required.

2. Open Cs system requires about 3.5 kg of Cs for a 10 year life with a 100% safety margin.

3. One central reservoir presents a potential single point failure mechanism.

A multiple cesium reservoir concept with central Cs sources was studied to eliminate the single point failure mechanism. B&W engineers identified reliable fabrication and assembly schemes to provide six separate cesium plena. No modification in the baseline core structure is needed. See Figure 3.8-2. About 25 TFEs can be connected to each cesium plenum. The loss of one cesium plenum will result in less than 12% of the total power loss. TFE connections can be cross-clustered so that each parallel-connected TFE cluster spans across a cesium plenum boundary. This concept is an attractive candidate for the baseline Cs source design.

3.8.2 Integral Cs Reservoir

Integral reservoir systems have been the baseline choice for the TFE Verification Program, and there is considerable ongoing development to produce intercalated graphite reservoirs for this application. The SPACE-R system design can make use of this technology base, if a graphite reservoir is selected. However, there are still questions about the long term stability of graphite reservoirs in the reactor core, although recent test results have been encouraging. There's also a question about the stability of thermionic performance in a system with a graphite integral reservoir, compared with a system using liquid Cs source.

A number of converters have been successfully operated in bell jar tests with graphite reservoirs. However, in general their performance was never quite as good as those which had the liquid Cs reservoirs. The reasons for these are several. One is that the Cs vapor from the integral reservoir is a compromised value achieved by the loading of the intercalated graphite and the environmental temperature of the converter itself. There is no
Figure 3.8-2 Cesium plenum configuration for multiple central Cs reservoirs
capability to independently change the temperature of the integral reservoir without changing some other parameters in the converter operation at the same time. In addition, the integral reservoirs do not have the capability to remove impurity gases by distillation of the Cs, and this may lead to possible performance degradation depending upon how well the components are outgassed and upon the chemical constituents of the construction materials. Finally, some of the carbon material form the graphite reservoir may be transported to the electrodes, thereby affecting their work functions. Post-test examination of some converters with graphite reservoirs have shown evidence of material transport onto the collector.

The integral reservoir requires extremely good outgassing prior to loading Cs. The CKBM, KIAE, and LUCH scientists expressed doubt that this could be done in a multicell TFE, because it, especially the emitter, cannot be heated to operating temperatures after closure. Two people, one at LUCH and one at Sukhumi, believe we might be able to outgas a single cell TFE (emitter) at full operating temperatures in "RIG" stand well enough to use an integral reservoir. However, no long term operating test results are available. They say calculations and judgement could not support more than 1 or 2 year life. However, this is very dependent upon how well hydrogen in-leakage and getter control can be achieved.

The experienced people at CKBM, KIAE, LUCH, and Red Star agree that the external Cs control that permits drainage of O₂, H₂, N₂, H₂O, etc and condensation and return of Cs can permit very long endurance. This is evidenced in the endurance tests at CKBM.

How long life can be obtained with an integral Cs reservoir would be the subject of extensive further testing and development. This seems to be incompatible with the limited TI SNPS budget, schedule and risk allowance, as well as the reliability, multi-start system testing and endurance requirements.

SPI studies indicated that the integral reservoir temperatures and resulting pressures were significantly influenced by neutron and gamma heating as well as heat conduction from the emitter at ambient NaK temperature. See Figure 3.8-3. Proper control of this temperature through startup and partial load would be difficult. TFE checkout and testing in the "Rig" stand would not have neutron or gamma heating.
Figure 3.8-3 Emitter stem temperature distribution during normal operation
Advantages of the integral Cs reservoir can be summarized as follows:

1. Core design and plumbing is simplified.

2. Cs pressure level can be optimized for each TFE for an optimal power generation.

3. The design presents no single point failure mechanisms.

4. No outside control is needed (this can also be a disadvantage).

5. Once the TFE is assembled with the integral Cs source loaded and tested out, no additional Cs connection is needed for subsequent TFE testing.

Disadvantages of the integral Cs reservoir can be summarized as follows:

1. TFE design and development is more involved because the integral Cs source needs to be designed, developed and tested concurrently with the TFE. In comparison, the central cesium source can be developed separately.

2. Acceptable incore performance of the integral reservoir has not been demonstrated yet.

3. It is difficult to outgas completely after the assembly.

4. Impurity gas accumulation during operation is expected to lead to potentially rapid performance degradation. Limited options exist for O₂ and H₂ getters in a TFE, but they have not been demonstrated yet.

5. Lifetime beyond 1 to 2 years appears to be difficult to obtain.
6. No outside control is possible after assembly for any adjustment needed except for a limited Cs loading change.

3.8.3 Conclusion

The multiple central Cs reservoir concept is being considered as the baseline design for SPACE-R.

3.9 Reflector, Control, and Safety Trade Studies

Trade studies are being performed on various reflector, control, and safety system components to develop the starting configuration for the point design effort. These trades include materials selection, control configuration, and safety system approaches. These studies will result in a recommended approach for each system which minimizes technical risk and development cost for the design work.

The control system has been based on the TOPAZ II configuration. The neutronics calculations have been performed to size the drums and poison segments for the SPACE-R core. A trade study is currently underway to determine whether swelling of the B4C poison will necessitate a change from the current cylindrical poison geometry to a different geometry or the use of leakage control. Initial assessment based on SP-100 data suggests that the swelling will be on the order of a few percent. The current cylindrical geometry should be able to accommodate this swelling with little or no impact on the drums performance. This assessment will be confirmed with a detailed analysis. A finite element model has been developed which incorporates irradiation swelling of the B4C, heating of the beryllium drums, and radiation to space to predict the overall response of the drums. The baseline and alternate poison configurations are shown in Figure 3.9-1.

Poison materials studied for control and safety drums include boron carbide, boron nitride, silver-indium-cadmium and gadolinium. Gadolinium was discarded due to burnup and silver-indium-cadmium was discarded due to high gamma production outside the core which requires a larger shield. B4C and BN remain as promising candidates.
Figure 3.9-1 Poison configurations for control and safety drums
The poison will be applied as plasma sprayed coating, sections or encapsulated rods. See Figure 3.9-2 for candidate configurations. Requirements for poison are allowance for vent, allowance for volumetric expansion, minimum mass, adequate worth, minimum degradation of material properties during operation, and adequate cooling and low secondary gamma emission. Drum worth was calculated as a function of poison thickness for various poison section coverage angle. See Figure 3.9-3.

Several drive train configurations are being studied including the originally-proposed ring gear concept, all individually driven drums, dual drum concept with each motor driving two drums, and three-drum drive concept with each motor driving three drums. See Figure 3.9-4. The originally proposed design remains as the baseline configuration.

Preliminary requirements were established for the drum drive motors. These parameters are based on SNAP and SP-100 information as well as initial SPACE-R calculations for control drum worth. They are: drum rotation of 0.5 deg/step, angular velocity of 10 deg/sec, maximum angular acceleration of 20 deg/sec², angular resolution of 0.2 deg, and powered or unpowered scram. These requirements will be refined as further calculations are performed for the reactor performance and control.

Drive train material candidates were identified and trade-offs are being performed. Precision gear materials considered include Ti (6A1-4V), nitride 15-5PH CRES/nitrallloy 135, and stellite 6-B. Ti (6A1-4V) is being considered as the shaft material. Titanium and inconel are under study for the welded bellows, which should accommodate misalignment of the shafts and eliminate the shaft backlash.

The choice of Be as the baseline reflector material has been evaluated to ensure that technological risk is minimized. Beryllium and beryllium oxide are obvious candidates for the reflector materials. Trades have been performed to determine which would provide the necessary reflection for the least mass in the neutron energy range of interest. The mass is nearly the same for either material in fast reactors. The high density BeO reflectors are thinner than Be reflectors for Fast reactors. In moderated reactors the low density Be reflector results in lower mass. In addition, anisotropic swelling of BeO when irradiated
Figure 3.9-2 Candidate poison segments
Figure 3.9-3 Control drum worth vs. poison thickness and angle
Figure 3.9-4(a) Drivetrain configuration (ring gear concept)
Figure 3.9-4(b) Drivetrain configuration (duel drum concept)
Figure 3.9-4(c) Drivetrain configuration (multidrum concept)
leads to micro-cracking of the grain boundaries. Swelling and the effect on the structural integrity of the BeO reflector depends on the temperature and the neutron flux. Beryllium metal will be stable under the design conditions.

Rotating drums were selected over sliding segments because they are a more mature technology and have less risk associated with their potential failure modes. Sliding segments, either axially or radially, are potentially susceptible to seizure at the interfaces of the sliding surfaces. This seizure may be caused by a foreign material between the two surfaces, such as space debris or a hostile projectile, or could result from the surfaces vacuum welding together should they come in contact.

Half drums and full drums were compared to provide control and safety to the system. A half drum system has features which may have advantages over the full drum system which is currently the SPACE-R baseline; however, there are also technological risks which must be evaluated. The half drum system does not use boron poison to control reactivity. Therefore, issues of boron carbide swelling, heating and cracking are not a concern. Nor is there concern over depletion of the boron over the long lifetime. The half drum concept uses less space than the full drums and therefore in some systems mass reduction can be obtained. When the half drums swing out to allow neutron leakage, they extend outside the radius of the reflector and could scatter radiation around the shield. However, for safety drums this will not occur because the safety drum is always rotated in during operation. A drawback to the half drum concept is the effect on the radial power distribution in the core. Neutrons would leak out through the space left vacant by the drum, thus reducing the power in those regions. The power in the solid reflector regions would remain high, leading to a sinusoidal power shape in the circumferential direction. The half drum concept could be used for safety drums. However, it is not recommended for control drums because of the non-uniform power distribution and the bigger shield requirement.

3.10 Actuator Trade Study

The actuator trade study assesses the SP-100, SNAP-10A, and TOPAZ drive motors to determine whether the point design will modify one of these designs or whether a new drive will be developed. To date work has focused on gathering information on the existing
actuator hardware. SP-100 data has been received from the Joint Propulsion Laboratory (JPL).

Preliminary assessment of the SP-100 motor indicates that it can meet the draft performance requirements developed for SPACE-R. However, the mass of the SP-100 motors is quite large due to redundant components. This redundancy is built in to increase the reliability of the motor. The SPACE-R control system has built reliability into the control configuration and may well be able to tolerate a lower reliability in the motor without significant mission impact. Thus a promising approach appears to be a modification of the SP-100 motors, reducing the mass by eliminating redundant components. The modified drives would take full advantage of the many technology advances of the SP-100 program such as bearing materials and coatings for contacting surfaces.

The Satellite Systems Division of Honeywell Inc. in Durham, North Carolina was identified as a potential supplier for the SPACE-R actuators. The actuators being considered for use on SPACE-R are based on the actuator design utilized in the SP-100 thermionic reactor. The experience Honeywell has acquired in the field of space nuclear actuator design will be utilized during the design and development of various SPACE-R components, including the bearings, stators, windings, and brakes. The motors developed for the SP-100 program have been shown to operate for a duration comparable to the life expectancy of the SPACE-R reactor.

Stepper motors are being considered for the SPACE-R actuators with a torque capacity of 50 in-oz. The actuator is capable of 3.75 degrees/pulse, with a speed of 80 pulses/second. This yields a motor speed of 300 degrees/second. A geartrain, with a 13:1 gear ratio, will be utilized to increase the torque of the drivetrain in order to meet the requirements of the drum drive configuration. The addition of the geartrain will also bring a corresponding reduction in the angular velocity to a value of approximately 23 degrees/second. This speed falls within the initial specification of 20 degrees/second for the speed of the drums.

Bearing materials and configurations being studied for the drive train include: silicon nitride balls with stellite races, graphite bushing, alumina-coated sockets and shaft,
and silicon carbide-coated socket and shaft. Sputter deposited molybdenum disulfide is being considered as lubricant. The bearing design requirements established for the SPACE-R include tight tolerance to minimize play during positioning, no vacuum welding, and compensation for potential bowing of drums.

3.11 Shield

The SWAN code, which utilizes perturbation theory to calculate shielding performance, was modified to perform the shield trade study in the SPACE-R. The entire SPACE-R system, from the core all the way to the payload, was modeled using a simplified 1-D spherical geometry. The shield calculations were done by solving the inhomogeneous (or source-driven) transport equation across the entire system, from the core center to the payload. The source of neutrons and photons was taken to be the fission density distribution across the core. The spatial distribution of the fission density was obtained by solving the eigenvalue equation for the core surrounded by the reflector and plenum. The source neutron spectrum was taken to be the fission spectrum. Nine photons were assumed to be generated per fission neutron; their spectrum was taken from the Reactor Handbook.

SWAN was applied to the reference shield design (1-D model) to generate weight effectiveness functions (WEF) and equal weight replacement effectiveness functions (EWREF) of various materials in system components as well as in shields. Total of 28 materials were considered. In the following discussions, Shield 1 indicated the primary gamma shield (ZrH$_{1,8}$ in the baseline design), and Shield 2 indicates the LiH shield.

In all the WEF and EWREF plots included here, a dashed line implies a negative effectiveness function (lower dose) which is desirable, whereas a solid line implies a positive (increased dose) effectiveness function. A large negative value is most desirable. The desirability of replacing a given constituent by a candidate constituent at a given location in the system can be judged by comparing the WEF values of the two materials; if the WEF of the candidate material is smaller than the WEF for the reference material, a replacement of some of the reference material by the candidate material will lead to a reduction in the dose (which is desirable). The larger the difference between the WEF's, the more effective will be the replacement.
The WEFs pertaining to the out of shield zones provide information which can guide the design of the corresponding components so as to minimize the shielding requirements. Following is a summary of general observations.

1. By far, the most effective design modification for reducing the gamma dose is to add a neutron absorber (Such as 90%B₄C, 90%Li, or 90%LiH. The "90%" implies that the B or the Li are enriched to 90% in ¹⁰B or in ⁶Li) to Shield 1 (i.e., ZrH₁₈). See Figures 3.11-1 to 3.

2. Next in effectiveness for reducing the gamma dose is the use of 90%B₄C (or 90%Li) in the heat exchanger (HX) zone. See Figure 3.11-2.

3. Next in effectiveness for reducing the gamma dose is the use of tantalum in the plenum, as well as in the inner part of Shield 1. See Figure 3.11-3.

4. By far the most effective way for reducing the neutron dose. See Figures 3.11-4 and 5.

5. Next in effectiveness for reducing the neutron dose is to increase the Be volume fraction in the core.

6. Another effective measure for reducing the neutron dose is to add 90%Li (or 90%B₄C) to the plenum. See Figure 3.11-5.

7. Use of more Be in moderator in the core will reduce the neutron dose and will also slightly reduce the gamma dose.

8. Use of Al instead of Cu for the electrical conductors will reduce both the gamma dose and the neutron dose.

9. Substitution of Ni for Fe in the core and reflector will reduce both the gamma dose and the neutron dose.
Figure 3.11-1  Gamma dose WEF in system components as a function of the distance from the reactor core
Figure 3.11-2  Gamma dose WEF in system components as a function of the distance from the reactor core
Figure 3.11-3  Gamma dose WEF in system components as a function of the distance from the reactor core
Figure 3.11-4  Neutron dose (DPA) WEF in system components as a function of the distance from the reactor core
Figure 3.11-5 Neutron dose (DPA) WEF in system components as a function of the distance from the reactor core
10. Substitution of Mo for SS in the core and reflector will reduce the gamma dose, but increase the neutron dose.

11. Substitution of ZrH$_{1.75}$ for YH$_{1.75}$ in the core will reduce the gamma dose and the neutron dose. However, this will lead to a high hydrogen loss over the system life.

Also generated with SWAN are EWREFs for selected materials versus the reference hydrides in the shield zones. Figures 3.11-6 to 8 present the EWREF of the selected materials versus ZrH$_{1.8}$ in Shield 1 (the innermost shield) and Figures 3.11-8 and 9 versus LiH in the LiH shield (Shield 2). Following are a summary of observations:

1. Replacing some ZrH$_{1.8}$ by 90%B$_4$C in Shield 1 is expected to significantly reduce the gamma dose (Figure 3.11-6), as well as to reduce the neutron dose.

2. Intermixing 90%LiH with ZrH$_{1.8}$ in Shield 1 is expected to significantly reduce gamma and neutron doses (Figure 3.11-6). However, this option may not be attractive because of materials compatibility issues and hydrogen containment issue for LiH with hot ZrH$_{1.8}$.

3. Replacing some of the ZrH$_{1.8}$ by tungsten is not expected to reduce the gamma dose (Figure 3.11-6) unless restricted to the central part of Shield 1. The neutron dose will be increased.

4. Replacement of some ZrH$_{1.8}$ by Be in the front part of Shield 1 will reduce the neutron dose and slightly reduce the gamma dose.

5. Replacement of some ZrH$_{1.8}$ by Ta will reduce the gamma dose (Figure 3.11-7) but will increase the neutron dose.

6. Enriching the Li of the LiH shield by Li-6 will reduce both the gamma dose (Figures 3.11-8 and 9). In fact, this is the only way identified for modifying that will reduce the dpa.
Figure 3.11-6  Gamma dose EWREF vs ZrH$_{1.8}$ in Shield 1
Figure 3.11-7 Gamma dose EWREF vs. ZrH$_{1.8}$ in Shield 1
Figure 3.11-8 Neutron dose EWREF vs. LiH in Shield 2.
Figure 3.11-9  Gamma dose EWREF vs. LiH in Shield 2.
7. Replacing some of the LiH by ZrH$_{1.8}$ will reduce the gamma dose but will increase the neutron dose (Figures 3.11-8 and 9). The same applies to W.

8. As far as the gamma dose is concerned, it pays to replace some of the LiH adjacent to the SS walls by 90%B$_4$C or 90%Li (Figure 3.11-9). However, this effectiveness is reduced if the LiH is to use lithium enriched to 90% 6Li.

9. Of the various heavy materials, Bi and Pb are the best replacements for LiH, as far as gamma dose reduction is concerned. The best location for these materials is in the inner side of the LiH shield.

10. Isotopically tailored tungsten (W-184) is significantly better than natural W, if used in the inner part of the LiH shield.

Based on the above observations the reference shield design is being optimized with the aim of quantifying the dose and dpa reduction possibilities. Following is a summary of the systems calculated, and their performance. Note that the performance reported is to indicate the trend; detailed 2-D calculations which properly take into account the system geometry and shield geometry variations will be performed later for reliable determination of the maximum dose and dpa in the payload plane.

1. Replacing the LiH of Shield 2 by equal weight of 90%LiH, the neutron dose and the gamma dose went down by a factor of, respectively, 1.41 and 1.08. The gamma dose EWREF for 90%LiH and for 90%B$_4$C have dropped by one to two orders of magnitude. The EWREFs in Shield 1, however, are practically unchanged by replacing natural lithium by enriched lithium in Shield 2.

2. Replacing 10% of the ZrH$_{1.8}$ by equal weight of 90%B$_4$C, uniformly distributed, in Shield 1, the neutron and gamma doses went down by a factor of, respectively, 2.01 and 5.85. The neutron dose EWREFs of 90%B$_4$C (as well as 90%Li and 90%LiH) dropped by 4 to 5 orders of magnitude.
magnitude. The addition of 90%B₄C to Shield 1 appreciably reduced the EWREF of 90%LiH in Shield 2. The neutron dose EWREFs slightly dropped in both Shield 1 and Shield 2 by the addition of 90%B₄C to Shield 1.

3. As a homogeneous mixture of ZrH₁.₈ and B₄C may not be practical, we tried to introduce the 90%B₄C in layers. The EWREFs were calculated for the system in which 3 90%B₄C layers, each 2 cm in thickness, replace the ZrH₁.₈ in three locations in Shield 1: by the SS cladding and at the center. The gamma and neutron doses were lower than in the reference design by a factor of, respectively, 4.12 and 4.72.

4. By adding two extra 90%B₄C layers instead of ZrH₁.₈ in between the 3 layers considered before, the neutron and gamma dose values dropped relative to the reference design values by a factor of, respectively, 8.33 and 5.10. By changing from 3 to 5 layers of 90%B₄C, the maximum (negative) dose EWREF dropped by two orders of magnitude. It would have been more effective if a strong thermal neutron absorbing material could have been homogeneously mixed with the ZrH₁.₈ rather than placed in discrete layers.

5. So far the shield volume has not been changed in each calculation; rather, artificial densities were used in a fixed volume. As the material replacements will lead to an increase in the shield volume, the above gamma dose and neutron dose reduction factors are to be taken as upper bound estimates. To get a lower bound estimate on the shield performance improvement we calculated the last case (Described in point # 4) assuming that the densities of the replacement materials are the nominal densities, and maintaining the shield geometry (and volume) as in the reference design. The result was no change in the neutron dose and a factor of 1.92 reduction in the gamma dose. The resulting shield is, however, of a lower weight. The weight reduction relative to the reference shield is 28% for Shield 1 and 10% for Shield 2. As Shield 1 is significantly heavier than Shield 2, the total weight reduction is estimated to be approximately 25% of the combined weight of ZrH₁.₈ and LiH. An even larger weight reduction is expected by increasing the amount of 90%B₄C or by
distributing it more uniformly across Shield 1. The determination of the maximum practical weight reduction needs to take into account material compatibility, heat removal and structural design considerations, and will require more elaborate calculations which involve shield geometry variations.

6. A further reduction in the gamma dose is expected if more 90%B\(_4\)C will be added to the core side of Shield 1. Replacing some of the 90%B\(_4\)C by W in all 4 out of the 5 90%B\(_4\)C layers (excluding the closest to core layer) will reduce the gamma dose. This indicates that a combination of W with a good neutron absorber may offer an interesting alternative (or additive) to the ZrH\(_{1.8}\) based shield.

7. An upper bound estimate of the reduction in the gamma dose at the payload plane that can be expected by mixing 90%B\(_4\)C with ZrH\(_{1.8}\) in Shield 2 without changing the geometry of the shields was obtained as follows. SWAN was applied to minimize the gamma dose by identifying the mix of the two constituents in 20 zones while conserving the volume fraction available for these materials. It was found that the optimal 90%B\(_4\)C volume fraction varies from about 17% in the core side, to about 12% in the payload side of Shield 2. The corresponding gamma dose was approximately 1/4 of the reference shield design, while the neutron dose was also lower than in the reference design.

In conclusion, promising shield material configurations and design approaches were identified during the shield trade study. Quantification of these approaches will be continued in the point design phase of the project. In addition to improvements in the design of the shield itself, we identified design variations in ex-shield components of SPACE-R which might significantly reduce the shielding requirements and, hence, the overall system specific mass.

A two-dimensional representation of the SPACE-R system was modelled to calculate heating rates in the shield region 2-D discrete ordinate code DORT with the DABL69 cross section data (including Kerma Factors). Scattering anisotropy was accounted for using the P\(_1\) approximation.
The calculations were performed in two steps. In the first step the eigenvalue equation was solved for subsystems. The results obtained include the eigenvalue (\( K_{\text{eff}} \)), fission density distribution across the core, heat deposition distribution across any zone of interest, neutron and photon leakage, and a boundary source. The boundary source is used as the source term for the second phase of the calculations. It is taken to be between the core and the reflector on the shield direction. The boundary source manipulation program \( \text{BNDRYS} \) is used for defining the surface (or boundary) source for the next step of the calculation. In this next phase we applied DORT to solve the inhomogeneous transport equation from the core - reflector interface all the way to the payload. The system was represented by a right circular cylinder the radius of which coincides with the outer radius of the \( \text{LiH} \) shield. The second step calculation gives the distribution across the payload plane, or any other location in the system, of a number of radiation damage characteristics. The solution of the eigenvalue equation required 56 outer iterations and 6653 inner iterations. It took 491 min of an IBM RISK/600-350H CPU time (and 1035 clock minutes).

Figures 3.11-10 displays the DORT calculated fission density distribution across the core. Notice that the axial power distribution is asymmetric. This is due to differences in the design of upper and lower axial reflectors.

The heat deposition rate distribution across the core, as calculated using the Kerma Factors (per unit volume), is shown in Figure 3.11-11. It is found to have a similar shape as the fission density (even though the two distributions are calculated using entirely different data). Table 3.11-1 summarizes the heat deposition calculated with the K. F. for the core and other zones. Not included in this table is the heat deposited in the radial reflector and in the axial reflector and plenum located towards the top of the cone. Taking the heat deposition in the unaccounted for zones to be proportional to that in the zones facing the payload times the ratio of neutron leakage from the core towards the unaccounted for zones over the neutron leakage towards the payload, we come up with an estimate of 615 kW of total heat deposition in the system. This is in good agreement with the fission power generated in the reactor - 611 kW. This agreement provides a validation of the Kerma Factors per unit volume that were generated for this project.
Figure 3.11-10  Fission Neutron Source Distribution in the Core
Figure 3.11-11  Power Distribution in the Core
Table 3.11-1  Heat Deposition Balance Table

<table>
<thead>
<tr>
<th>Zone Type</th>
<th>Total Heat Deposition (kWatt)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core</td>
<td>594.2</td>
</tr>
<tr>
<td>Down Reflector</td>
<td>2.15</td>
</tr>
<tr>
<td>Plenum</td>
<td>1.33</td>
</tr>
<tr>
<td>SS316L can #1</td>
<td>0.025</td>
</tr>
<tr>
<td>ZrH1.8 Shield</td>
<td>0.84</td>
</tr>
<tr>
<td>SS316L can #2</td>
<td>0.0015</td>
</tr>
<tr>
<td>LiH Shield</td>
<td>0.034</td>
</tr>
<tr>
<td>Pumps &amp; Drives</td>
<td>0.002</td>
</tr>
</tbody>
</table>
Figure 3.11-12 to 15 show heat deposition rate distributions and corresponding temperature distributions in radiation shields. Note that the LiH temperature ranges from 300 to 350 K. Therefore, the LiH radiation swelling is not expected to be a significant concern (about 1% by volume). See Figure 3.11-16 for the LiH radiation swelling data.

Figure 3.11-17 shows the neutron flux calculated with DORT for various locations across the payload plane. The Fast Flux is the summation of the flux of neutrons pertaining to the first 22 energy group of the DABL69 library, spanning the energy range above 0.96 MeV; it corresponds to the constraint specified for the SPACER-R shield design. The DPA (E>1 MeV) is the 1 MeV equivalent Fast Flux; i.e., the summation of the flux of each of the 22 highest energy groups, weighted by the effectiveness of each group neutrons to displace Si atoms relative to that of 1 MeV neutrons; it is approximately 60% higher than the Fast Flux itself. The DPA accounts for the contribution of neutrons of all energies to the displacements of Si atoms.

The maximum Fast Flux is found to be $2 \times 10^5$ n/(cm$^2$ s), corresponding to an annual fluence of $6.3 \times 10^{12}$ n/cm$^2$. This is within the maximum permissible annual fluence of $10^{13}$ n/cm$^2$. The maximum photon dose (Gamma-Ray Ionization) is found to be $5.5 \times 10^{-5}$ W/Kg Si, corresponding to an annual dose of $1.7 \times 10^6$ Rads. If this EWRED result is translated into the dose with the actual shield volume as described above, the annual dose is estimated to be $0.9 \times 10^5$ Rads. This is within the maximum permissible annual photon dose of $10^5$ Rads.
Figure 3.11-12  Heat Deposition Distribution in ZrHₓ Shield (watt·cm⁻³)
Figure 3.11-13 Heat Deposition Distribution in LiH Shield (watt*cm^-3)
Figure 3.11-14  Temperature distribution in ZrH$_{1.8}$ shield during normal operation.
Figure 3.11-15 Temperature distribution in LiH shield during normal operation
SNAP-10A and SP-100 Data

Figure 3.11-16 LiH radiation swelling data
Figure 3.11-17  Fast Neutron Flux and DPA ($E > 1$Mev) Distribution in the Payload
A significant effort was devoted to understand the trend of the changes in the radial distribution of the radiation damage rate across the payload, as well as the effect of the various components in between the shield and the payload, and of the tapering of the shield thickness.

The effect of the pumps, control rod drives, and HX located between the shield and the payload was studied on the DPA distribution in the payload plane. The presence of components (particularly the HX) between the shield and the payload, near the centerline, contributes to lower radiation damage rates towards the payload centerline.

For a reliable estimate of the damage rate in the payload plane it is necessary to model the after-shield components accurately enough. A more realistic representation of the reactor system will be incorporated in the shield calculation during the point design to account for realistic system configuration, such as penetrations through the shields.

3.12 Reactor Vessel

3.12.1 Structural Materials

A number of candidate structural materials were evaluated for strength, NaK compatibility, neutronic compatibility, and cost. These materials included various stainless steels, hastalloys, molybdenum, and inconel. See Table 3.12-1.

Mo has high strength, low thermal expansion and high corrosion resistance. However, recrystallization after welding is an issue. A co-extruded transition piece can be utilized to avoid direct welding of Mo.

Hastelloy has good strength at elevated temperatures, good operational experience from the SNAP program, and lower thermal expansion coefficient than SS316L. However, W in conventional Hastelloy needs to be replaced by Mo to be used in the moderated core.
### Table 3.12-1 Material Properties

<table>
<thead>
<tr>
<th></th>
<th>304</th>
<th>304L</th>
<th>304LN</th>
<th>316L</th>
<th>Haynes 230 ( Hastalloy )</th>
<th>Hastalloy-N</th>
<th>Inconel 617</th>
<th>Molybdenum</th>
<th>Molybdenum TMA</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>MODULUS OF (GSI)</strong></td>
<td>21,100</td>
<td>21,500</td>
<td>25,300</td>
<td>24,700</td>
<td>24,600</td>
<td>42,000</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>T. S. (GSI)</td>
<td>44</td>
<td>37</td>
<td>58</td>
<td>98</td>
<td>84.2</td>
<td>84</td>
<td>69.6</td>
<td>72.93</td>
<td></td>
</tr>
<tr>
<td>Y. S. (GSI)</td>
<td>12.5</td>
<td>12</td>
<td>20</td>
<td>39.5</td>
<td>35.4</td>
<td>33</td>
<td>49.8</td>
<td></td>
<td></td>
</tr>
<tr>
<td>KIC</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>1% CREEP - 87,000 HRS.</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>MODULUS OF (GSI)</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>T. S. (GSI)</td>
<td>79</td>
<td></td>
<td>94.2</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>155</td>
</tr>
<tr>
<td>Y. S. (GSI)</td>
<td>66</td>
<td></td>
<td>32.6</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>66.6</td>
</tr>
<tr>
<td>KIC</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>1% CREEP - 87,000 HRS.</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>COEFFICIENT OF THERMAL EXPANSION</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Poisson's Ratio</td>
<td>285</td>
<td>285</td>
<td>315</td>
<td>32</td>
<td>32</td>
<td>3</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>WEARIBILITY</strong></td>
<td>Excellent</td>
<td>Excellent</td>
<td>Excellent</td>
<td>Good</td>
<td>Good</td>
<td>Good</td>
<td>Good</td>
<td>Poor</td>
<td>Poor</td>
</tr>
<tr>
<td><strong>MACHINABILITY</strong></td>
<td>Excellent</td>
<td>Excellent</td>
<td>Excellent</td>
<td>Good</td>
<td>Good</td>
<td>Good</td>
<td>Good</td>
<td>Good</td>
<td>Good</td>
</tr>
<tr>
<td><strong>Mod Exposure</strong></td>
<td>Good</td>
<td>Excellent</td>
<td>Excellent</td>
<td>No Exp.</td>
<td>No Exp.</td>
<td>No Exp.</td>
<td>No Exp.</td>
<td>No Exp.</td>
<td>No Exp.</td>
</tr>
<tr>
<td><strong>Mod Exposure</strong></td>
<td>Good</td>
<td>Excellent</td>
<td>Excellent</td>
<td>Excellent</td>
<td>Excellent</td>
<td>Excellent</td>
<td>Excellent</td>
<td>Excellent</td>
<td>Excellent</td>
</tr>
<tr>
<td><strong>CONT, 02 TUBE</strong></td>
<td>300.00</td>
<td>300.00</td>
<td>300.00</td>
<td>750.00</td>
<td>750.00</td>
<td>900.00</td>
<td>900.00</td>
<td>900.00</td>
<td>900.00</td>
</tr>
<tr>
<td><strong>CONT, 04 TUBE</strong></td>
<td>300.00</td>
<td>300.00</td>
<td>300.00</td>
<td>750.00</td>
<td>750.00</td>
<td>900.00</td>
<td>900.00</td>
<td>900.00</td>
<td>900.00</td>
</tr>
<tr>
<td><strong>CONT, 06 TUBE</strong></td>
<td>300.00</td>
<td>300.00</td>
<td>300.00</td>
<td>750.00</td>
<td>750.00</td>
<td>900.00</td>
<td>900.00</td>
<td>900.00</td>
<td>900.00</td>
</tr>
<tr>
<td><strong>N.S. (GSI)</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>Density, g/m³</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
SS316L has good mechanical properties, high corrosion resistance, large data base including irradiation data, good fabricability, and good experience in high temperature NaK environment. Topaz II vessel material is very close to SS316L under a Russian material specification. However, the relatively large thermal expansion coefficient needs to be considered when used with dissimilar materials.

The leading candidate at this point is SS-316L. This material has good structural properties, corrosion resistance, NaK compatibility, and fabricability. It is readily available and inexpensive. There is one more material to be evaluated before a final selection is made. Information is still being gathered on Hastalloy-N. Once this information is complete, it will be compared to the SS-316L data to select the baseline for the point design.

3.12.2 Welding Techniques

A welding/joining trade was performed to determine whether the design should use single or double containment. While double containment sounds like it would provide greater reliability, past programs have demonstrated that this is not necessarily the case. Often the added complexity of the second boundary make welds more difficult to perform and inspection techniques almost impossible to implement. This can actually decrease the reliability of the components. Single boundary systems often provide greater accessibility to the weld locations and therefore the welds can be performed much more reliably. It is also easier to perform 100 percent inspections of these welds to ensure their integrity prior to releasing the component. These factors have led to selection of a single containment configuration as the baseline for Phase I design.

An assessment of various weld techniques was performed as a part of the single versus double containment trade. Welding processes which were considered for the joining of the tube sheaths to the tube sheet (upper plate) included gas tungsten arc welding (GTAW), plasma arc welding (PAW), laser beam welding (LBW), and electron beam welding (EBW). After a thorough evaluation of the various methods of joining the baseline 316L stainless steel reactor vessel, it was determined that the optimum welding method would be the utilization of LBW due to the lower heat transferred into the component. The
benefit of having less heat deposited into the component allows for less distortion and a smaller heat affected zone, compared to conventional methods (i.e. GTAW & PAW).

EBW has a smaller heat affected zone, but, LBW is a more economical method of attaching the component compared to EBW. Saving both monetary and in setup time are obtained by eliminating the requirement of placing the component in a large vacuum chamber as required for EDW. Additionally, the laser welder can go into difficult to reach places since the laser light can be directed into tight areas using fiber optic cables, whereas, the electron beam cannot go.

Weld Inspection

The integrity of the welds will be verified by three methods. First, the welds will be inspected for surface flaws and cracking with dye penetrant inspection. This method detects flaws on the surface of the weld, once detected, repairers can be performed.

Through the weld cracks will be detected through the utilization of a helium leak tester. The method employs a vacuum on the inside of the vessel with a helium detector. The weld is interrogate with a supply of helium from a localized jet outside surface of the weld. If there is a leak, the helium will be sucked into the leak detector via a crack in the vessel.

An alternate to the above test would be to pressurize the vessel with helium and place the ultra sensitive helium detector on the welds.

Additional weld testing will be performed in high stressed, high fatigue areas. This testing in addition to the above, Microradiography will be deployed to determine if there are internal voids or internal cracks in the weld which will act as stress risers.
3.12.3 Strength Considerations

The preliminary sizing of the SPACE-R vessel and calandria tubes under normal operating conditions have been completed. No attempt was made to size these components during off nominal operation, launch or accident scenarios.

The analysis was performed using the following operating environment.

Maximum NaK operating temperature - 925 K
Operating pressure for NaK - 1.5 Atmospheres (22 psia)
Helium pressure - 1.0 psia

The following materials were evaluated 304, 304L, 316L stainless steel, Hastalloy-230, Hastalloy-X, Inconel 617, Molybdenum and Molybdenum alloy TZM.

Mechanical properties at the stated operating conditions are listed in Table 1 for the evaluated materials.

The following factors of safety (F.S.) are used in sizing the vessel and calandria tubes. Note that yield factors of safety are applied to the 1% creep condition and ultimate factors to creep rupture.

F.S.-1.25 Yield
F.S.-2.0 Ultimate
F.S.-2.0 Buckling

The following loadings are assumed for the tubes.

ΔP = 21 psi (compressive hoop stress)
Axial Force = 22 * endplate NaK area - 1 * endplate He area
Endplate NaK area = 25.0 in²
Endplate He area = 132.2 in²
Axial Force per tube = 2.8 lbs (tension)
Assumed vessel temperature - 825 K (1025°F)
Assumed average calandria temperature - 925 K (1200°F)

Using the material and factor of safety criteria listed, the limiting condition using for the calandria tube is hoop collapse/buckling for all the materials considered. The calculated critical thicknesses for buckling with a factor of safety of 2 are shown below. The analysis assumes no support from the molybdenum collector. Since these two parts are essentially in contact, it may be desirable to make the tubes flexible so that intimate contact can be achieved. The down side is the molybdenum tube would then be loaded in compression and may deflect/creep towards the emitter. This interaction analysis can be performed with ANSYS if desired/required in the future.

### Calandria Tube Required Thicknesses

<table>
<thead>
<tr>
<th>Material</th>
<th>Required Thickness</th>
</tr>
</thead>
<tbody>
<tr>
<td>304</td>
<td>0.0063 inches</td>
</tr>
<tr>
<td>304L</td>
<td>0.0063 inches</td>
</tr>
<tr>
<td>316</td>
<td>0.0062 inches</td>
</tr>
<tr>
<td>617</td>
<td>0.0060 inches</td>
</tr>
<tr>
<td>Haynes 230</td>
<td>0.0059 inches</td>
</tr>
<tr>
<td>Molybdenum</td>
<td>0.0050 inches</td>
</tr>
</tbody>
</table>

The above results are based on the material strength only. When the corrosion resistance and the fabricability is considered, the practical thickness is estimated to be about 20 mils.

The following loading are assumed for the dual pressure boundaries:

- **Outer Boundary**: 22 psi Internal pressure
- **Central Inner Boundary**: 5 psi External Pressure (assuming a 5 psi drop)
- **Ends Inner Boundary**: 21 psi External Pressure

The following required thicknesses are calculated for the inner an outer vessel sleeves along with the governing sizing condition.
Vessel Sizing Results

<table>
<thead>
<tr>
<th>Material</th>
<th>Inner Vessel Thickness (in)</th>
<th>Condition</th>
<th>Outer Vessel Thickness (in)</th>
<th>Condition</th>
</tr>
</thead>
<tbody>
<tr>
<td>304 SS</td>
<td>0.069</td>
<td>Hoop Crush</td>
<td>0.060</td>
<td>1% Creep/10 yrs</td>
</tr>
<tr>
<td>304L SS</td>
<td>0.069</td>
<td>Hoop Crush</td>
<td>0.036</td>
<td>Creep Rupture/10 yrs</td>
</tr>
<tr>
<td>316 SS</td>
<td>0.068</td>
<td>Hoop Crush</td>
<td>0.036</td>
<td>1% Creep/10 yrs</td>
</tr>
<tr>
<td>Inc 617</td>
<td>0.065</td>
<td>Hoop Crush</td>
<td>0.021</td>
<td>1% Creep/10 yrs</td>
</tr>
<tr>
<td>Haynes 230</td>
<td>0.064</td>
<td>Hoop Crush</td>
<td>0.019</td>
<td>Creep Rupture/10 yrs</td>
</tr>
<tr>
<td>Molybdenum</td>
<td>0.054</td>
<td>Hoop Crush</td>
<td>0.016</td>
<td>Creep Rupture/10 yrs</td>
</tr>
</tbody>
</table>

Note that actual thicknesses required may vary from those listed due to locally higher stresses, lower mechanical properties due to hot spots, welded joints, irradiated properties, and additional loading conditions (launch, accident, etc). Detailed analyses will be required to accurately assess these components.

3.12.4 Electrical Feed Throughs

To transfer the electrical power from the TFE's to the external bus bars, electrical feed throughs are required to transfer the current out of the vessel without electrifying the vessel and while separating the 15.7 psig helium from the vacuum of space $2 \times 10^9$ atm.

Various tradeoff studies were performed on the development of the electrical feed throughs that pass through the upper and lower planum in six places.
The feed-throughs pass through the inner and outer vessel immersed in the NaK coolant. Concerned with the corrosive effects the NaK will have on the feed-throughs, a vertical feed through was assessed to alleviate this problem. With the present design, the feed-throughs passing through the upper plenum will pierce one wall horizontally, while the feed-throughs in the lower plenum pass vertically through the lower helium plate. This design addresses the concern of possible manufacturers of this device by allowing the feed-through to be attached to one plate, allowing the feed-through to move through thermal expansions.

Present products available in this amperage category are rated at 600 amps. Per discussions with the vendors, the development of the required 762 amp system would be minimal and low risk.
3.13 Thermal Management System (TMS)

The major efforts for the SPACE-R Thermal Management System (TMS) in the reporting period were in the area of conceptual design trade studies and supporting analysis. The function of the TMS is to transport waste heat from the reactor and reject to space by radiation the waste heat generated in the reactor during startup, full power operation, reactor scram and other identified reactor transients.

3.13.1 SPACE-R Thermal Management

This section reports a conceptual trade evaluation of various possible methods of waste heat acquisition, transport, dispersal and rejection to determine the best approach to thermal management for the conceptual design of the Thermal Management System (TMS) for the SPACE-R system.

The methods that were evaluated for SPACE-R thermal management are the use of:

1. Concept I - heat pipes only for waste heat acquisition, transport, dispersal and rejection;
2. Concept II - single pumped loop only for waste heat acquisition, transport, dispersal and rejection;
3. Concept III - single pumped reactor loop for waste heat acquisition and transport to multi-pumped single or two phase radiator loops for its dispersal to and rejection from the radiator fins;
4. Concept IV - pumped reactor loop for waste heat acquisition and transport to heat pipes for its dispersal to and rejection from the radiator fins (the baseline design as proposed)

Figures 3.13-1(a)-(d) show the basic working fluid flow schematics for the four TMS approaches identified above. Figure 3.13-2 shows the TMS temperature profiles from waste heat acquisition in the reactor core to its rejection to space from the radiator fins for the TMS approaches evaluated. As can be seen in the latter figure, the reactor core axial temperature profile is nearly isothermal (i.e. constant) in Concept I where the waste heat is
Figure 3.13-1 TMS Concept Working Fluid Flow Schematics
Figure 3.13-2 SPACE-R TMS Concept Waste Heat Acquisition, Transport and Rejection
acquired from the reactor core by the latent heat associated with the change in phase of the
heat pipe working fluid. In the other TMS approaches, where the waste heat from the
reactor core is acquired by sensible heating of the TMS working fluid, the reactor core axial
temperature rises linearly from the TMS fluid entry into the reactor core to its exit. As can
also be seen in the same figure, the axial temperature profiles for the waste heat rejection
by the radiator fins are the same as for its acquisition for all of the TMS concepts evaluated,
except that the slopes of the working fluid temperature profile curves are reversed for the
heat rejection phases from that for their respective heat acquisition phases.

The issues that were considered in the evaluation of the potential SPACE-R thermal
management approaches are:

(1) Single point failure potential
(2) Reactor design impacts
   - core size
   - control
   - design & assembly complexity
(3) Mission abort safety
(4) Local reactor core meltdown potential
(5) TMS mass requirements
(6) TMS parasitic electrical power requirements
(7) Accommodation of initial reactor startup & its restart
(8) Acceptability of candidate working fluids:
   - melting temperature
   - thermal merit
   - other pertinent physical & chemical properties
(9) TMS robustness
(10) TMS survivability
(11) 1-G testability

Robustness is defined as the preclusion of contaminants or working fluid vapor and/or non
condensible gas from blocking the liquid return flow path and causing deprive of the heat
transfer and transport device.
If survivability is not an important issue for SPACE-R applications, the baseline diode fin heat pipes can be eliminated saving both cost and their 44 kg of mass for all of the TMS approaches evaluated.

Startup of heat transport devices where the heat transfer to and from the loop is by phase change cannot take advantage of the low freezing temperature of NaK78 which is 260 K, because there exists no azeotropic mixture of sodium and potassium1. Since the saturation pressure of potassium is lower than that for sodium, the potassium will preferentially vaporize leaving behind the sodium to act as a thermal flow resistance in the evaporator of the device and alter the constituents of the mixture such that the low freezing temperature of the mixture cannot be sustained through repeated phase change cycles. Over the operating temperature of the SPACE-R TMS, potassium with a freezing temperature of 337 K (147°F) is the best candidate two-phase working fluid as discussed in Section 3.13.2. Since the baseline average sink temperature for anticipated SPACE-R missions is 255 K (-1°F), the issue of working fluid freeze/thaw for the initial startup and re-start after dormancy periods must be weighed and addressed in the consideration of any two-phase heat transport devices for use in the SPACE-R TMS.

The following discussion deals with evaluations of the suitability of the above TMS approaches for anticipated SPACE-R thermionic reactor electrical power generation applications.

3.13.1.1 Heat Pipes Used for TMS Waste Heat Acquisition, Transport, Dispersal and Rejection

A sketch of this TMS concept is shown in Figure 3.13-1(a). Figure 3.13-2 shows the temperature profile from waste heat acquisition in the reactor core to its rejection to space from the radiator fins for this TMS concept. In this SPACE-R thermal management approach, heat pipes are used to acquire the waste heat from the reactor and to transport it to and disperse it over the radiator fin for ultimate rejection to space.

([i.e. one for which the composition of the liquid and vapor phases are identical])
The advantages to this concept are:

(1) elimination of:
   (a) the TMS single point failure mechanism; and
   (b) the TMS parasitic electrical power requirement; and
   (c) the TMS armoring requirement for reliability.

(2) isothermality of the approach (increasing radiator effectiveness)

(3) 1-G testability

The disadvantages are:

(1) the introduction of voids into the reactor core
(2) an adverse impact on water immersion safety
(3) local reactor core heatup with heat pipe failure
(4) the approach complicates the reactor core design & assembly
(5) potential for freezing of the heat pipe working fluid prior to startup and during dormancy periods.

This concept for the SPACE-R thermal management appears to be attractive from the standpoint of TMS mass and its parasitic power demand. The use of a nearly isothermal device for SPACE-R waste heat acquisition and rejection reduces the size of the radiator needed by up to 1/3. The pump reliability issue and the need for 2.7 kWe of parasitic electrical power to operate the pumped loop EM pump are eliminated as well. However, the introduction of voids into the reactor core dilutes core reactivity necessitating a large increase in core size and mass. It also has adverse impacts on reactor controls effectiveness and core compaction safety considerations. The reactor controls effectiveness is diminished, because of the changes in core reactivity caused by the introduction of voids into the reactor core. Water immersion safety is adversely impacted, because water replacing the voids in the core increases neutron moderation, which in turn increases core reactivity. For this reason, the neutron absorption worth of the water immersion safety rod would have to be greatly increased for the subject TMS approach.
Failure of a heat pipe will cause local core heatup. Failure of a number of the heat pipes in the same region of the core could result in local core meltdown. The subject TMS approach complicates the core design and assembly due to the need to incorporate the heat pipes into the core design and further to integrate them into the core elements during assembly. In addition, the heat pipe working fluid freeze/thaw startup/restart heat pipe priming issues involved with this concept are important considerations that would have to be addressed to make the concept viable for the subject application.

The adverse impacts of this thermal management concept on core design and control, mass and safety and the working fluid freeze/thaw startup/restart heat pipe priming issues make this approach unattractive for SPACE-R application.

3.13.1.2 Single Pumped Loop for TMS Heat Acquisition, Transport, Dispersal and Rejection

A sketch of the working fluid flow schematic for this TMS concept is shown in Figure 3.13-1(b). Figure 3.13-2 shows the temperature distributions from waste heat acquisition in the reactor core to its rejection to space for this TMS approach\(^1\). This concept is the basic approach to TMS use by NASA shuttle and in various thermal management applications in the Space Station Freedom although more than one TMS loop is used in each of these applications. The use of a single pumped loop with liquid for the working fluid for waste heat acquisition, transport, dispersal and rejection by sensible heating and cooling of the loop working fluid, while it is attractive from the standpoint of simplicity 1-g testability, it does introduce the potential of a likely single point failure mode into the SPACE-R reactor design. A single leak in the loop will result in irreparable failure in applications such as currently envisioned, where on-orbit maintenance and repair is not feasible. However, redundancy provisions for critical loop components and adequate armoring of the loop flow channels can potentially reduce the failure risk to within acceptable bounds.

The use of a two-phase pumped loop for this approach creates the same issues as discussed in Section 3.13.1.1 for the use of heat pipes in the reactor core and, therefore, further discussion of this TMS concept is limited to consideration of a single phase pumped loop using sensible heating and cooling of the loop working fluid in the liquid state for heat acquisition and rejection from the loop.

182
Forty two-pass flow channels, each of 11 meters length incorporated into the radiator cone, will provide the same or shorter spacing for waste heat delivery to the radiator fin, than that for the baseline TMS concept. Flow channels of 1.5 cm I.D. will result in fluid velocities of \( \leq 1.3 \text{ m/s} \) through the radiator passages and passage pressure losses of about 6.9 kPa (1 psi), from which the pressure drop through the baseline TMS approach heat exchanger of 7.9 kPa (1.15 psi) can be subtracted. From the above it can be seen that no net increase in loop \( \Delta P \) for this concept over the baseline concept 28 kPa (4 psi) for the pumped reactor loop need be incurred. The subject approach, therefore, need not increase the parasitic pump power required to operate the loop beyond that already needed for the pumped reactor loop in the baseline TMS approach.

The flow passages through the radiator cone must be adequately protected to provide for acceptable reliability for the concept. If it is assumed that 2.5 cm thickness of beryllium is adequate to protect the radiator flow channels as has been calculated for the baseline TMS approach, where there is no added protection from bumpering, then the increase in TMS mass would be about 485 kg partially offset by a reduction of 182 kg resulting from the forshortening of the pumped loop and the elimination of the heat exchanger and header heat pipes which are TMS mass reductions gained by using the subject approach. The total mass increase of using the subject TMS approach, therefore, is about 300 kg. The capacity of the pumped loop NaK expansion compensator, discussed in Section 3.13.6, would have to be increased by about 12,000 cm\(^3\) to allow for expansion of the NaK added to the single pumped loop beyond that required for the baseline TMS concept over the loop working fluid temperature range.

If survivability is a key requirement, either:

1. the baseline diode fin heat pipes must be retained from the baseline design to block the excess heat incident upon the radiator fins resulting from an attach from boiling off the NaK in the radiator cone flow channels; or

2. the capacity of the NaK expansion compensator discussed in Section 3.13.6 would have to be increased by about 80,000 cm\(^3\) to accommodate the large change in fluid specific volume when the NaK in the radiator
cone flow channels boils off as a result of the fin temperature rising well above the loop working fluid saturation temperature.

In summary, this TMS approach would reduce the technical risk somewhat by eliminating the header heat pipes and the magnitude of the TMS startup/restart heat pipe priming problems associated with the freeze/thaw of the baseline heat pipe working fluid. These advantages would be gained at the expense of decreasing the TMS reliability caused by the increase in single point failure potential, in addition to an increase in the TMS mass by about 300 kg over the baseline TMS approach.

3.13.1.3 Single Pumped Loop for TMS Heat Acquisition & Transport to Multi-Pumped Loops for its Dispersal & Rejection

A sketch of this TMS concept working fluid flow schematic is shown in Figure 3.13-1(c). Figure 3.13-2 shows the temperature distributions from waste heat acquisition in the reactor core to its rejection to space for this TMS approach. In the single loop/multi-loop TMS approach, a single pumped loop is used to acquire and transport the waste heat away from the reactor, which is the same approach as used for the baseline design. The waste heat can be acquired and rejected from the multi-pumped loops using liquid (i.e. by sensible heating and cooling of the working fluid) or by use of a two-phase working fluid (i.e. by the latent heat of vaporization and condensation). This approach differs from the baseline concept in that the single pumped loop would interface with multi-pumped loops rather than with heat pipes in the heat exchanger for the dispersal of the waste heat over the radiator surface for rejection to the space sink.

The evaluation of this concept is limited to comparing the advantages and disadvantages of performing the heat dispersal to the radiator cone using multi-pumped radiator loops against using heat pipes for that purpose as is done in the baseline approach, since the heat acquisition and transport phases for this concept are the same as for the baseline TMS concept.

---

mainly due to the added armoring required for adequately protection of the multi-channel radiator section of the single pumped loop from micrometeoroids and space debris
The key advantages to using multi-pumped loops to disperse the waste heat to the radiator fins rather than heat pipes are:

1. The potential for reducing the TMS technical risk; and
2. The potential for reduction in the heat rejection loop working fluid freeze temperature; and
3. The elimination of header heat pipe development; and
4. The potential for elimination of the baseline header heat pipe startup/restart, freeze/thaw issues; and
5. The robustness of the concept (single-phase alternative concept only).

The potential for reducing the TMS technical risk involves the elimination of any uncertainties and complexities that might exist with respect to the development of acceptable header heat pipes for the baseline TMS approach. It also eliminates the considerable non-recurring and recurring cost associated with them. Reduction in the radiator loop working fluid freeze temperature offers the potential of reducing or eliminating the freeze/thaw issues compared with the baseline approach that utilizes potassium with its higher freeze temperature as the working fluid for the heat pipes compared with the use of NaK78 as the working fluid for single-phase pumped loops.

The disadvantages to using multi-pumped loops to disperse the waste heat to the radiator fins rather than heat pipes are:

1. The potential for additional TMS parasitic power demand
2. Radiator loop mini-EM pump development
3. The potential for reduced reliability by reducing the number of independent heat transport paths to the radiator
4. The additional reactor loop hardware that may be required

The additional TMS parasitic power demand should be about the same as that required to overcome the pressure losses in the radiator channels for the single pumped loop TMS approach discussed in Section 3.13.1.2. However, if a two-phase working fluid approach is taken to the multi-pumped radiator loops those parasitic electrical power demands would
be considerably reduced. The risk associated with radiator loop pump development should not be significantly greater than that for the development of the EM pump for the reactor loop.

The added hardware for the pumped radiator loops might involve:

1. the added hardware and design complexity EM pumps;
2. independent loop working fluid expansion compensators; and
3. independent loop working fluid purification hardware.

The number of flow passages through the radiator cone can be the same as that envisioned for the single pumped loop TMS approach discussed in Section 3.13.1.2. The forty two-pass radiator flow passages can be separated into any number of independent loops as required by the trade-off between reliability and TMS mass minimization. For this evaluation, based on anticipated reliability requirements, it was assumed that each of the two-pass flow channels constitutes a single independent pumped loop. The heat transfer to and from the radiator loops could be by sensible heating and cooling of a flowing liquid or by phase change.

(A) Single Phase Radiator Loops - The use of single phase radiator loops, where liquid NaK78 is used as the working fluid, offers the advantage of eliminating the startup issues associated with two-phase radiator loops and diminishing the working fluid freezing issue from that for the baseline TMS approach, since the freezing temperature of NaK78 is 260K (8°F). The concept is also robust and 1-g testable in all ground test article orientations. The architecture of the radiator loops could be the same as that described for the radiator channels for the single pumped loop given in Section 3.13.1.2. Figure 3.13-3(a) shows the reactor and radiator pumped loop temperature profiles in the counterflow tube-in-shell heat exchanger selected for this concept. As can be seen in the figure, the selection of a counterflow heat exchanger, and the same working fluid and flow rates for the sum of the pumped radiator loops as for the reactor loop, maximizes the heat rejection potential for this TMS concept.

subject to optimization of the loop armor requirements for fewer independent loops vs trading armor thickness against the greater reliability gained by increasing the number of independent loops
Figure 3.13-3 SPACE-R Heat Exchanger Temperature Distributions for TMS Concepts
Forty two-pass loops each of 11 meters length incorporated into the radiator cone, will provide the same or shorter spacing between the flow channels for waste heat delivery to the radiator fin, than that for the baseline TMS concept. Flow loops of 1.5 cm I.D. will result in fluid velocities of \( \leq 1.3 \text{ m/s} \) through the radiator loops and loop pressure losses of about 6.9 kPa (1 psi) requiring only about 0.5 kWe of parasitic electric power to operate the EM pumps for all of the 40 radiator loops.

The mass required for the subject concept would be about 65 kg greater than that required for the baseline design. The mass requirements for the subject approach differ from the single loop approach, described in Section 3.1.2.1, in that armoring of the radiator loops would not be required, since the 40 loops are independent of each other and a leak in a single loop will not result in failure of the TMS.

(B) Two-Phase Pumped Radiator Loops - Adoption of a two-phase rather than single phase pumped radiator loop has the advantages of:

1. the potential for eliminating the need for fin heat pipes for survivability considerations
2. significantly reducing the parasitic pump power needed for the radiator loops
3. potential for reduction in the radiator loop fluid inventory; and
4. some enhancement in heat rejection efficiency.

Reduction in reliability by reducing the number of independent heat flow paths to the radiator cone is not inherent to the subject concept, but reducing the number of independent flow paths for this concept to the minimum needed to satisfy the reliability requirement does make it more attractive as the TMS approach when the other evaluation criteria are being considered.

The additional mass would consist of that for the EM pump (+40 kg), the individual loop expansion compensators (+20 kg) and +60 kg of NaK78 for liquid packing the radiator loops minus 20 kg for the foreshortening of the pumped reactor loop the mass of the header heat pipe wicks (-29 kg) and their working fluid (-9 kg) are factored into the mass balance.
The disadvantages include:

1. Demonstration is needed for pumped two-phase fluid behavior in the 0-g environment; and
2. It raises loop working fluid freeze/thaw and startup/restart issues.

Figure 3.13-3(b) shows the reactor and radiator pumped loop temperature profiles in the crossflow tube-in-shell heat exchanger selected for this concept. As can be seen in the figure, the selection of a crossflow heat exchanger maximizes the heat rejection potential for this TMS concept. The use of two-phase pumped radiator loops, where the waste heat is acquired and rejected by latent heat of phase change, rather than by the sensible heating and cooling of the loop's working fluid, offers the advantage of greatly reducing the parasitic pump power demands for the pumped radiator loops. However, there would be only slight additional advantage with respect to radiator effectiveness offered by this compared with the single-phase approach. The minimum allowable cross section of the loop piping would be dictated by the sonic limit to loop working fluid vapor flow in the same manner and for the same reasons that the working fluid mass flow rates are limited in heat pipes as discussed in Section 3.13.10.2. If the waste heat dispersal chore is divided between forty pumped two-phase radiator loops, then the minimum tube diameters for each loop would range between 2.25 and 3.4 cm I.D., if the sonic limit is taken as 30% of the working fluid vapor flow rate at Mach 1 in potassium. If it is taken as 50%, then the minimum required tube diameters range between 1.74 and 2.63 cm I.D. The pressure drop in the former case would average about 2 kPa (0.3 psi). The parasitic electrical power required to operate all forty independent pumped radiator loops is < 100 We.

Even beyond the radiator loop working fluid freeze/thaw issue associated with the subject approach discussed below, startup and re-start of the pumped two-phase loop would be difficult without first liquid packing the loop before the loop startup begins. In the on-orbit microgravity environment, gravitational forces are not available to separate the phases

---

where potassium and NaK are the two-phase and single-phase loop working fluids respectively and a 100 K drop in the single phase working fluid temperature is factored into the equation and assuming a pump efficiency of 1.5%. The ratio of single to two-phase loop mass flow rates to achieve the same heat transport through the loop is about 23:1. The density ratio for liquid NaK to potassium vapor is 4500:1.
in the loop and, therefore, there can be no guarantee that there would be an assured continuous supply of liquid to the pump unless the loop were liquid packed prior to the startup. This would require providing each loop with an accumulator to take the excess liquid as the loop operation begins, as well as a complete working fluid liquid charge for each loop. This would make the two-phase pumped radiator loop approach unattractive compared to using the single-phase approach instead. The alternative would be to provide a single common valved-off accumulator, whose liquid could be shared by all of the pumped radiator loops each in turn during loop startup. Freezing of the pumped loop working fluid prior to reactor startup and during dormancy periods would further exacerbate the difficulties associated with this approach.

A two-phase pumped TMS for unmanned satellite applications is an added technology risk that probably is not warranted by the parasitic power and weight advantages offered by this variation on the pumped single-phase multi-radiator loop approach.

(C) **EMF Enhanced Heat Pipe** - The EMF enhanced heat pipe, a concept of which is shown in Figure 3.13-4, consists of an ordinary heat pipe, whose heat throughput potential is to be increased by the addition of an EM pump to the heat pipe to boost the working fluid pumping head beyond that achievable by the heat pipe wick capillary pumping head alone. The EM pump is placed in the adiabatic section of the heat pipe to increase the pumping head of the liquid returning to the heat pipe evaporator as is shown in Figure 3.13-4. The interface between the liquid and vapor will not support a pressure difference between the fluid states greater than that dictated by the heat pipe wick pore size, which already defines the maximum heat pipe heat throughput if the heat pipe is capillary limited as discussed in Section 3.13.10. This condition will not be altered by the introduction of a pump into the heat pipe flow loop. In addition, while the liquid/vapor interface at the wick inner surface will support up to a maximum negative pressure difference between the liquid and vapor without comingling of the phases dictated by the wick capillary pore size at the interface, it will not support a positive difference. Therefore, wherever there is communication between the two phases in a given heat pipe cross section and the liquid pressure is greater than that of the adjacent vapor, the liquid can weep through the interface due to the positive ΔP and be entrained and transported back through the condenser by the vapor before the liquid undergoes evaporation.
Figure 3.13-4 Hybrid Wick and Pressure Distributions Through the EMF Enhanced Heat Pipe
In the EMF enhanced heat pipe concept reported by Chi et al (1987), a thin-walled tubular shell is introduced in the adiabatic section of the heat pipe downstream of the pump (i.e. in the liquid flow direction towards the evaporator) at the interface between the phases. The radial flow barrier is extending part way into the heat pipe evaporator section by the placement of a thin perforated sheath attached to the shell under the outer layer of screens in the evaporator.

This approach to performance enhancement can only provide limited improvement in heat pipe heat throughput and then only in heat pipes that have long adiabatic sections because:

1. the pressure head enhancement is limited by the condition that the liquid pressure cannot exceed the vapor pressure in any heat pipe location where there is open communication between the phases
2. it is only the adiabatic section of the heat pipe where the pressure in the liquid flow path can be allowed to appreciably exceed the pressure in the adjacent vapor channel
3. the sheath will greatly diminish or even preclude proper function in the section of the evaporator in which the sheath resides; and
4. the sonic limit to heat throughput is not altered by the boost in heat pipe pumping head.

At the low end of the heat pipe operating temperature range, the heat pipe performance is sonic limited as discussed in Section 3.13.10.2. The minimum sizes required for the heat pipes are the same as those for the two-phase pumped radiator loops provided above. Since the minimum header heat pipe sizes required to accommodate the required heat loads (Section 3.13.10.2) are of the same magnitude as those needed for the EMF enhanced heat pipe as defined above, there can be no mass benefit gained by using the subject concept. The approach does provide subcooling of the returning liquid downstream of the pump inlet, which will preclude blockage of the liquid return by vapor. However as was shown by the discussion of the two-phase pumped radiator loops above, the parasitic pump head needed to drive two-phase pumped radiator loops can be measured in mere watts. Consequently,
the added complexity to the TMS introduced by the EMF enhanced heat pipe is not warranted, since the subject concept suffers all of the disadvantages of the pumped two-phase radiator loop approach discussed above, without offering sufficient benefits to warrant further consideration of the subject approach.

(D) Capillary Pumped Loop - In a capillary pumped loop, the liquid flows in the same direction as the vapor in a separate channel in its return to the evaporator section as shown in Figure 3.13-5. Pumping head to return the liquid to the evaporator is provided by capillary forces at the wick liquid/vapor interface, which pumps the liquid back into the evaporator wick. This is in contrast to the heat pipe, where the liquid and vapor counterflow in the same fluid boundary separated by the vapor capillary flow resistance at the interfacing wick surface all along the common boundary.

The major advantage offered by the capillary pumped loop concept over heat pipes are are that:

(1) the liquid can be subcooled to preclude its boiling during the return journey to the evaporator; and
(2) no wick is needed in the adiabatic sections of the heat pipe.

The disadvantages to the capillary pumped loop include:

(1) the liquid line must always be liquid packed to prevent depriming of the loop;
(2) the concept lacks robustness in that gas or vapor in the liquid return line can deprime the loop;
(3) opportunities for contaminants to form blockages in the liquid return line and deprime the loop; and
(4) the concept suffers from many of the same limitations and problems as do the other two phase heat transfer and transport devices evaluated as well.
Figure 3.13-5 Capillary Pumped Loop (Anderson, 1992)
The pumping head provided by the capillary loop is no greater than that afforded by conventional heat pipes with the same capillary pore size. The subject application need not have a long adiabatic section in the header heat pipes if the alternative design is selected and so there probably would be no significant potential for mass saving in adapting the capillary pumped loop for the radiator loops. Any blockages forming in the liquid return line such as contaminants and vapor and/or non-condensible gas can deprime the loop and prevent repriming of the loop as well. A significant fraction of the development work being done on this concept deals with providing barriers at the entrance to the liquid return line to prevent depriming from occurring.

Since the concept lacks robustness and offers little additional benefit beyond the potential to subcool the liquid before it enters the return line and suffers from the same lack of a suitable candidate working fluid for the subject application as the other two-phase devices considered, it is not considered as a prime candidate for the radiator loop unless a two-phase radiator loop is baselined and more detailed evaluations determine that the subcooling of the returning liquid is critical to the acceptable operation of the radiator loops.

3.13.1.4 Pumped Reactor Loop for TMS Heat Acquisition and Transport to Heat Pipes for Dispersal & Rejection (baseline design as proposed)

The SPACE-R Thermal Management System, as described in the conceptual design Phase I proposal and whose flow schematic is shown in Figure 3.13-1(d), calls for the waste heat generated in the reactor to be acquired by the sensible heating of the working fluid of a single pumped loop. The waste heat is then transported by the liquid NaK to a cross flow tube-in-shell heat exchanger, where it is distributed by heat transfer to a number of potassium filled heat pipes. The potassium heat pipes then transport and distribute the waste heat to widely dispersed radiator fins for ultimate rejection by radiation to space. Figure 3.13-2 shows the pumped loop and heat pipe working fluids temperature distributions from waste heat acquisition in the reactor core to its rejection to space for this TMS approach. Figure 3.13-3(b) shows the reactor and heat pipe temperature profiles in the cross flow heat exchanger baselined for this TMS approach. As can be seen in the figure, the selection of a cross-flow type tube-in-shell heat exchanger maximizes the heat rejection potential for this TMS concept.
The major advantages to the use of a single-phase pumped loop for acquisition of the waste heat from the reactor are that:

(1) it is based on proven technology; and

(2) it does not introduce voids into the reactor core as would the use of heat pipes for that purpose.

The chief uncertainty associated with this concept involves the development of acceptable header heat pipes for the subject approach and satisfactory resolution of the heat pipe working fluid freeze/thaw and startup/restart heat pipe priming issues. Table 3.13-6 in Section 3.13.10 shows the performance demanded of the header heat pipes used in this concept to transport and disperse the waste heat to the radiator fins for disposal to space for the baseline and alternative heat exchanger designs that are discussed in Section 3.13.5. The large heat throughput requirements (i.e. 8.3 and 11 kW for the baseline and alternative heat exchanger TMS designs shown in the figure and the 3.3 and 5.15 m length for the respective TMS designs result in maximum heat transport requirements for the header heat pipes of $1.8 \times 10^6$ watt-cm (i.e. $7.2 \times 10^5$ watt-inches) and $2.8 \times 10^6$ watt-cm (i.e. $1.1 \times 10^6$ watt-inches) respectively. The high heat pipe performance demanded by the baseline concept is an issue that must be addressed. Further the baselined heat pipe working fluid potassium freezes at a temperature of 337 K (147°F), which is well above the currently envisioned average sink temperature. This means the working fluid will be frozen in the heat pipes prior to startup and during reactor dormancy periods on-orbit. That fact raises heat pipe thaw and startup priming issues that must be addressed in the development of this TMS concept. It also raises the issue of 1-g testability of the concept which is certainly not insolvable as is discussed in Section 3.13.5 and below.
3.13.1.5 Summary of SPACE-R Thermal Management Conceptual Approach Evaluation

The baseline SPACE-R TMS approach offers the most attractive alternative of the concepts evaluated except that:

(1) it is not 1-G testable unless the ground test article is oriented horizontally; and

(2) the freeze/thaw startup/restart heat pipe priming issues associated with using potassium as the working fluid for the waste heat dispersal header heat pipes must be resolved satisfactorily.

The first item above can be resolved by adapting the alternative heat exchanger design discussed in Section 3.13.5, which will add about 32 kg of mass or more to the TMS. Resolving the second item will require either:

(1) demonstration of frozen long heat pipe startup within the system startup time allowed; or

(2) replacement of the header heat pipes with pumped single-phase two-pass reactor loops utilizing NaK78 or Na-K-Cs as the working fluid.

The latter will entail adding about 65 kg or somewhat more to the SPACE-R TMS mass and up to 0.75 kWe to the TMS parasitic power demand, beyond the respective values for those items reported in the proposal. The non-recurring and recurring cost for the latter approach should be significantly less than that required for the baseline TMS approach. Another approach is to use closed two-phase NaK secondary loops. NaK evaporates in the heat exchanger, condenses in the cone-shaped condenser-radiator, and NaK condensate is pumped back to the heat exchanger. The NaK flowrate is minimized because the heat of vaporization is utilized. Miniature EM pumps can thus be used. This concept was conceived by SPI in 1980’s. However, it needs to be demonstrated. The baseline TMS design will be chosen after further assessment of these approaches.
3.13.2 Pumped Loop Working Fluid

An evaluation was done to determine the most suitable working fluid for the pumped loop. The key issues that must be addressed in determining the best working fluid for the pumped loop are the following fluid characteristics:

- Its freezing temperature
- Its saturation temperature over the loop operating pressure range
- Its heat transport merit (i.e. $\rho c_p$)
- Its heat transfer merit
- Its dynamic viscosity
- Its electrical resistivity (EM pump issue)
- Its chemical reactivity
- Its toxicity
- Its neutron capture cross section
- Its cost

The most important characteristic of the pumped loop working fluid is that it remain in the liquid state over the SPACE-R operating envelope. The following potential pumped fluids were considered in the evaluation:

- Sodium
- Potassium
- Lithium
- Cesium
- Mixtures of the above

All of the candidate fluids evaluated are liquid metals because of their characteristically high heat transfer and transport merit and high saturation temperatures. A phase diagram for the Na-K-Cs ternary liquid-metal mixtures considered in the evaluation is presented in Figure 3.13-6.
Figure 3.13-6 Freezing Temperatures of Na-K-Cs Mixtures
### Table 3.13-1 Comparison of Pumped Loop Candidate Working Fluid Thermophysical Properties

<table>
<thead>
<tr>
<th>Pumped Loop Candidate Working Fluid</th>
<th>Freezing (L.E. Melting) Temperature</th>
<th>Heat Transport Merit $\rho C_p \approx$ kJoules/m²·K (at 600 DEG C)</th>
<th>Viscosity ~ Centipoises (at 600 DEG C)</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>POTASSIUM</strong></td>
<td>~ K +147</td>
<td>540</td>
<td>0.14</td>
</tr>
<tr>
<td><strong>SODIUM</strong></td>
<td>~ K +208</td>
<td>1011</td>
<td>0.21</td>
</tr>
<tr>
<td><strong>LITHIUM</strong></td>
<td>~ K +354</td>
<td>1983</td>
<td>0.575</td>
</tr>
<tr>
<td><strong>Na-K BINARY MIXTURES</strong></td>
<td>260 TO 371 +9 TO +208</td>
<td></td>
<td></td>
</tr>
<tr>
<td><em><em>Na-K</em>; Na(22%); K(78%) (BINARY EUTECTIC MIXTURE)</em>*</td>
<td>260 +9</td>
<td>636</td>
<td>0.166</td>
</tr>
<tr>
<td><strong>Na-Cs BINARY MIXTURES</strong></td>
<td>242 TO 371 -24 TO +208</td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>K-Cs BINARY MIXTURES</strong></td>
<td>225 TO 337 -55 TO +147</td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>Na-K-Cs TERNARY MIXTURES</strong></td>
<td>194 TO 244 -110 TO -20</td>
<td></td>
<td></td>
</tr>
<tr>
<td><strong>Na(13.5%); K(46.5%); Cs(40%) (TERNARY EUTECTIC MIXTURE)</strong></td>
<td>194 -110</td>
<td>491</td>
<td>0.163</td>
</tr>
</tbody>
</table>

* BASELINE PUMPED LOOP WORKING FLUID
Table 3.13-1 compares the melting temperature, heat transport merit and viscosity of the candidate fluids. As can be seen in the table, lithium has the highest heat transport merit of the fluids considered (i.e. 1983 kJ/m^3-K), but it also has the highest melting temperature (i.e. 337 K), which raises the critical issue of working fluid freezing. For that reason lithium was not considered further. Sodium has the next highest heat transport merit of the fluids considered about 1/2 that of lithium (i.e. 1011 kJ/m^3-K), but it also has the next highest melting temperature (i.e. 371 K) which is still far too high. The heat transport merit of potassium (i.e. 540 kJ/m^3-K) is low being only 1/2 that for sodium and its melting temperature (i.e. 337 K) is still too high. As can be seen in the table, binary mixtures of sodium and potassium (i.e. NaK) offer the potential of lower melting temperature than for of either sodium or potassium. The melting temperature of eutectic NaK is 260 K. The heat transport merit of the NaK78 (i.e. 78% K by weight) eutectic mixture is 636 kJ/m^3-K which is not high but sufficient to make eutectic NaK a viable pumped loop working fluid candidate. To further lower the fluid melting temperature, the characteristics of ternary mixtures of sodium, potassium and cesium were investigated. The melting temperature of Na-K-Cs mixtures ranges between 194 and 244 K, depending on the relative concentrations of the constituents. The melting temperature of eutectic Na-K-Cs (Na 13.5%; K 46.5%; Cs 40% by weight) is 194 K and its heat transport merit is 491 kJ/m^3-K making it a viable candidate if its melting temperature need be the prime consideration. The dynamic viscosity of all of the candidate liquid-metal fluids considered are sufficiently low that viscosity is not an issue in the fluid selection. This is also the case with electrical resistivity.

Eutectic Na-K-Cs would be the best candidate fluid except for the following:

- The high neutron absorption cross section of cesium (i.e. 26 barns) and its high 40% constitueny in the eutectic mixture.
- Cesium is chemically aggressive, highly corrosive and readily reacts with other materials and is highly explosive.
- Cesium is costly ($1500/kg)
Due to the above reasons, Na-K-Cs mixtures were rejected in the evaluation, in spite of the possibility of reducing the working fluid temperature to as low as 194 K. High concentrations of cesium are required as can be seen in Figure 3.13-6 in the ternary mixtures to achieve the high gains in lowering the working fluid freezing temperature offered by the mixtures. Eutectic NaK78 was selected as the SPACE-R pumped loop working fluid due to its low freezing temperature of 260 K, relatively lower chemical aggressiveness and neutron capture cross sections, its acceptable heat transport merit and the reasonableness of the other characteristics considered pertinent.

3.13.3 NaK Loop Architecture Trades

An evaluation was done to characterize the baseline TMS NaK loop architecture for the TMS configuration defined in the SPACE-R proposal and shown in Figure 3.13-7 such that the tradeoff between parasitic pump power demand on the one hand and TMS mass on the other was optimized.

The optimized NaK loop architecture (i.e. piping and component fluid passage and other needed dimensions) and performance characteristics determined in the study are as follows:

- Single Pipe Run O.D. \( \sim 3.25" \) (8.125 cm)
- Double Pipe Runs O.D. \( \sim 2.25" \) (5.625 cm)
- Reactor Bypass Annulus NaK Flow Gap \( \sim 0.125" \) (0.3 cm)
- TFE Annulus NaK Flow Gap \( \sim 0.03" \) (0.0762 cm)
- NaK Upper & Lower Vstem Heights \( \sim 0.8" \) (2 cm)
- Heat Exchanger Heat Pipe Spacing
  - Radial \( \sim 1.25" \) (3.125 cm)
  - Circumferential \( \sim 1.25" \) (3.125 cm)
- Heat Exchanger Length \( \sim 14" \) (35 cm)
- NaK loop \( \Delta P \) \( \sim 28 \) to 35 kPa (4 to 5 PSI)
  - Loop \( \Delta P \) consists of approximately 1/2 frictional & 1/2 form losses
  - Parasitic pump power loss \( \sim 2.7 \) kWe
- Reactor Core Flow Distribution Orifice \( \Delta P \) \( \geq 0.6895 \) kPa (0.1 PSI)
Figure 3.13-7  Shell Heat Exchanger Cross Section
The values assumed in the evaluation for the reactor bypass annular flow gap, the height of the NaK inlet and exit plenums and for the TFE coolant passage annuli are not critical in defining the magnitude of the loop pressure losses.

The NaK loop performance requirements assumed for the evaluation are as follows:

- Waste heat removal & transfer rate \( \sim 576 \text{ kWt (@ BOL)} \)
- NaK flow rate \( \sim 6.5 \text{ Kg/s} \)
- NaK temperature rise \( \sim 100 \text{ K} \)

The 28 kPa (4 psi) pump pressure head requirement presented in the proposal for the SPACE-R Thermal Management System appears to be reasonably achievable, so long as flow lines of sufficiently large cross section with reasonably smooth walled piping and gradual flow transitions where changes in flow cross section and flow direction occur are specified for the system. About 50% of the pressure losses in the system were calculated to be irreversible form pressure losses and the remaining 50% are due to frictional pressure losses. The pressure losses entering, exiting and in the NaK cleanup system (e.g. cold/hot traps, etc) are not included in the subject evaluation. The pressure loss in the NaK cleanup system should be kept below 0.5 psi. If it is kept below that value, the conservatism in the form factors assumed for the evaluation will allow sufficient margin to keep the system pressure drop within range of the targeted 28 kPa (4 psi) upper limit.

The irreversible form pressure losses due to changes in cross section and direction entering and exiting the SPACE-R Thermal Management System piping are key factors in defining the magnitude of the system pressure losses. For that reason the flow velocities in the Thermal Management System piping should be kept below about 1.8 m/s (6 ft/sec). In order to achieve that result, the run of single piping should have a tube diameter of 3.25" or greater and the double pipe runs should have tube diameters of 2.25" or greater. The form loss coefficients assumed for the evaluation were conservative and take little or no credit for measures that can be taken to reduce them by design. The maximum difference in the pressure losses through the various reactor TFE coolant passages was calculated to be about 0.0685 kPa (0.01 psi). Therefore, the core coolant inlet flow distribution orifice
pressure drop should be 0.685 kPa (0.1 psi) or greater assuming a flat reactor radial power profile.

A simplified algorithm for the fluid pressure loss in the SPACE-R Thermal Management System was prepared as a function of the fluid loop geometry assumed for this evaluation. The NaK mass flow rate \( M \) expressed in kg/s and NaK reactor fluid inlet density \( \rho_{in} \) expressed in kg/m\(^3\) can be defined as follows:

\[
\Delta P \text{ (psi)} = 70 \frac{M^2}{\rho_{in}} \quad \text{or} \quad \Delta P \text{ (kPa)} = 483 \frac{M^2}{\rho_{in}}
\]

The above algorithm tends to somewhat overpredict the system \( \Delta P \) at \( \frac{M^2}{\rho_{in}} \) values other than the design value used in the evaluation. Since the heat exchangers design characteristics have not been set yet, the pressure losses in the heat exchange were estimated based on the heat exchanger design in the SPACE-R proposal, but with a somewhat larger coolant passage outer diameter of 59.9 cm to allow adequate room for heat pipe spacing. The parasitic electrical power required to increase the coolant pressure head by 4 psi assuming a NaK flow rate of 6.5 kg/s and 10% pump efficiency is 2.7 kWe.

The wet mass of the plumbing for the SPACE-R Thermal Management System, assuming the reactor shield/payload spacing, is 10 meters and the required tube sizes defined above with tube wall thicknesses of 30 mils, is estimated to be about 80 kg, compared with the 97 kg given in the proposal.

For the evaluation, it was assumed that the system is to be designed to reject 567 kW of heat with a coolant temperature rise in the reactor of 100 K from 825 K to 925 K during normal operation with 2 of the 3 EM pump segments functioning. This requires a NaK flow rate of 6.5 kg/s (139 gpm).

The pressure losses in the the SPACE-R Thermal Management System piping and component fluid passages consist of frictional losses and irreversible form losses caused by changes in flow cross section and direction. Pressure losses due to acceleration of the fluid were not considered since they are negligible.
Evaluation Results

The results of the evaluation for pressure losses in the SPACE-R Thermal Management System during steady state normal operation at the beginning-of-life are summarized in Tables 3.13-2 and 3 below for the irreversible form and frictional pressure losses respectively. Fluid acceleration pressure losses were considered negligible.

3.13.4 Electromagnetic (EM) Pump

Requirement. The reactor for the SPACE-R 40 kW, power system requires an EM pump to circulate the NaK coolant. At the design point, this pump must provide a net pressure differential of 4 to 5 psi at a flowrate of 6.45 kg/sec. The normal pump operating temperature is 825 K; maximum anticipated coolant temperature is 925 K. The point-of-departure baseline design incorporates a three segment annular induction EM pump for redundancy. Pump power is supplied by switching the nominal 28 V DC bus voltage into a \( \pm 28 \) volt three phase square wave. The baseline coolant loop design is based on SS316L.

The gamut of EM pump types was reviewed as part of the design trade study. Within the two main EM pump families, induction versus conduction, the polyphase annular induction and the DC conduction represent the best choices within their respective classes. Russian designers selected the simple and robust DC conduction pump for the Topaz II reactor system. Annular induction pumps, designed and fabricated by M.H.D. Systems, Inc., have been routinely used in our liquid metal test loops at Space...
Table 3.13-2 Pumped Loop Irreversible Form Pressure Losses

<table>
<thead>
<tr>
<th>Section</th>
<th>Type of Loss</th>
<th>Velocity m/s</th>
<th>K</th>
<th>ΔP (kPa)</th>
<th>ΔP (psi)</th>
<th>% of Total ΔP</th>
</tr>
</thead>
<tbody>
<tr>
<td>Exit from HX</td>
<td>A &amp; C</td>
<td>1.76</td>
<td>0.75</td>
<td>0.8622</td>
<td>0.125</td>
<td>2.81</td>
</tr>
<tr>
<td>2 to 1 line Tee</td>
<td>C</td>
<td>1.76</td>
<td>1.5</td>
<td>1.7245</td>
<td>0.250</td>
<td>5.57</td>
</tr>
<tr>
<td>Inlet to EM Pump</td>
<td>A &amp; C</td>
<td>2.87</td>
<td>0.5</td>
<td>1.737</td>
<td>0.252</td>
<td>5.61</td>
</tr>
<tr>
<td>Exit from EM Pump</td>
<td>B &amp; C</td>
<td>2.87</td>
<td>1</td>
<td>3.528</td>
<td>0.505</td>
<td>11.25</td>
</tr>
<tr>
<td>1 to 2 line Tee</td>
<td>C</td>
<td>1.67</td>
<td>1</td>
<td>1.0236</td>
<td>0.150</td>
<td>3.34</td>
</tr>
<tr>
<td>Inlet to Ring Plenum</td>
<td>B &amp; C</td>
<td>1.76</td>
<td>1</td>
<td>1.150</td>
<td>0.167</td>
<td>3.72</td>
</tr>
<tr>
<td>Inlet to Nak Annulus</td>
<td>A &amp; C</td>
<td>0.238</td>
<td>1.2</td>
<td>0.6469</td>
<td>0.094</td>
<td>2.09</td>
</tr>
<tr>
<td>Inlet to Upper Nak Plenum</td>
<td>B &amp; C</td>
<td>1.21</td>
<td>1.5</td>
<td>0.8087</td>
<td>0.117</td>
<td>2.61</td>
</tr>
<tr>
<td>Flow Distrib. Orifice</td>
<td>A</td>
<td>0.921</td>
<td>As Reg'd</td>
<td>0.6895</td>
<td>0.1</td>
<td>2.23</td>
</tr>
<tr>
<td>Inlet to Lower Nak Plenum</td>
<td>B &amp; C</td>
<td>0.921</td>
<td>1.5</td>
<td>0.5363</td>
<td>0.078</td>
<td>1.74</td>
</tr>
<tr>
<td>Inlet to Return Line</td>
<td>A &amp; C</td>
<td>1.82</td>
<td>0.75</td>
<td>4.5</td>
<td>0.129</td>
<td>2.87</td>
</tr>
<tr>
<td>Inlet to HX</td>
<td>B &amp; C</td>
<td>1.82</td>
<td>1.5</td>
<td>1.723</td>
<td>0.259</td>
<td>5.77</td>
</tr>
<tr>
<td><strong>Totals</strong></td>
<td></td>
<td></td>
<td></td>
<td>15.34</td>
<td>2.23</td>
<td>49.6</td>
</tr>
</tbody>
</table>

*Definitions for Types of Irreversible Form Loss -
A- sudden contraction of flow cross sectional area
B- sudden enlargement of flow cross sectional area
C- sudden change in flow direction*
### Table 3.13-3 Pumped Loop Frictional Pressure Losses

<table>
<thead>
<tr>
<th>Pipe Section or Component</th>
<th>Velocity ( \text{m/s} )</th>
<th>Path Length ( \text{cm} )</th>
<th>( f )</th>
<th>( \Delta P ) (kPA)</th>
<th>( \Delta P ) (psi)</th>
<th>% of Total ( \Delta P )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inlet single pipe</td>
<td>1.67</td>
<td>207.5</td>
<td>0.0131</td>
<td>0.3443</td>
<td>0.050</td>
<td>1.11</td>
</tr>
<tr>
<td>EM Pump</td>
<td>2.87</td>
<td>40</td>
<td>0.015</td>
<td>1.455</td>
<td>0.211</td>
<td>4.70</td>
</tr>
<tr>
<td>Inlet double pipe</td>
<td>1.76</td>
<td>122.5</td>
<td>0.0139</td>
<td>0.3488</td>
<td>0.051</td>
<td>1.14</td>
</tr>
<tr>
<td>NaK Ring Plenum</td>
<td>0.64</td>
<td>2.6</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>--</td>
</tr>
<tr>
<td>NaK Bypass Annulus</td>
<td>2.42</td>
<td>77</td>
<td>0.022</td>
<td>0.9057</td>
<td>0.136</td>
<td>3.03</td>
</tr>
<tr>
<td>Upper Plenum</td>
<td></td>
<td>Varies</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>--</td>
</tr>
<tr>
<td>In Core</td>
<td>0.91</td>
<td>77</td>
<td>0.032</td>
<td>6.281</td>
<td>0.911</td>
<td>20.29</td>
</tr>
<tr>
<td>Lower Plenum</td>
<td></td>
<td>Varies</td>
<td>--</td>
<td>--</td>
<td>--</td>
<td>--</td>
</tr>
<tr>
<td>Exit Piping</td>
<td>1.82</td>
<td>330</td>
<td>0.0139</td>
<td>0.9710</td>
<td>0.141</td>
<td>3.14</td>
</tr>
<tr>
<td>Heat Exchanger</td>
<td>0.99</td>
<td>34.3</td>
<td>0.407*</td>
<td>5.271</td>
<td>0.765</td>
<td>17.04</td>
</tr>
<tr>
<td>Totals</td>
<td></td>
<td></td>
<td></td>
<td>15.58 (kPA)</td>
<td>2.26 (psi)</td>
<td>50.3% of Total (psi)</td>
</tr>
</tbody>
</table>

* friction factor for tube side heat exchanger cross flow
Power, Inc. These two pump types are differentiated primarily by the current required for their operation: the annular induction pump requires a three-phase AC current of several hundred amperes while the DC conduction design requires DC current of about a thousand amperes. This basic difference leads to opposing implementation features with multiple impacts on system integration and subtle design trade issues. A thorough design evaluation of each pump approach is required before the optimal selection can be made.

**DC Conduction Pump.** This design utilizes a large DC current which flows directly through the liquid metal via two electrodes on opposite sides of the coolant channel. Large currents are required at a low voltage (less than 0.5 volt). Coincidentally, this power loading is almost perfectly matched by a few single cell TFEs. The device is small, simple, and relatively efficient for an EM pump. Preliminary design work for SPACE-R indicates a current requirement of 1100 amperes by using a self-excited magnetic field of 1 Tesla. This level of field can be provided by using about 22 turns of conductor in series with the pump power cable. The primary loss mechanisms are the resistive dissipation within the NaK coolant and stray current loss through the sides of the stainless steel channel. Pump efficiencies of 30% higher are predicted. Additional losses are incurred in the power cables and the integrity of the cable to electrode bond. A significant trade in mass results from the pump location (above or below the shield), acceptable cable voltage drop, versus the pump internal component mass trade.

The design of the very compact Topaz II pump was reviewed to validate our own design approach. This smaller pump operates with a self-excited magnetic field build up from five turns of the power conductor. This compact design is actually two pumps in parallel, one on either side of the magnetic structure. Working from the available performance curves, an overall efficiency of 13 percent is indicated. These results, at a reduced scale and probably decreased field level, confirm our ongoing design procedures. A computer code for predicting and optimizing the DC conduction pump performance is currently under development.
Annular Induction Pump. This approach eliminates the electrode bonding issue by inducing current within the pump annulus. However, the DC power output of the SPACE-R power system must be conditioned into three-phase AC power. By double switching the nominal 28 volt DC bus, a compact power converter can be designed with solid state switches; this converter outputs AC power in the form of a square wave with a nominal RMS voltage of 28 volts. Pump performance at the relatively low AC voltage level was checked by the potential annular pump designer, M.H.D. Systems, Inc. Preliminary results indicate that typical performance can be achieved at this level of voltage. Predicted efficiencies under SPACE-R conditions ranged from 15 to 19 percent. The best switching frequency appears to be around 180 Hz. At this point, the proposed baseline design appears viable. A relatively simple switching (at pump frequency) power converter could be developed to operate the induction pump.

M.H.D. Systems, Inc. is currently using their in-house computer program for the evaluation of the annular induction EM pump for the SPACE-R application. A matrix of pump design cases are under consideration for implementation into the overall system trade study. For comparison purposes, we prescribe a baseline design requirement of NaK flowrate = 6.45 kg/sec, net pressure rise = 5 psig, coolant temperature = 825 K, and three phase AC power (square wave output) with an RMS voltage = 25 volts. For purposes of applying the single-segment, sine-wave based design code results to the SPI three segment design powered by a three-phase square wave, the following assumptions are made:

1. Assume three-phase sine wave power with an RMS voltage of 25 volts (equivalent peak-to-peak voltage of 70.7 volts).

2. Carry out two pump designs to reflect the limiting cases: A. a full pump approximated by a single segment, and B. a reduced Δp design to reflect the operation of one of the three segments. For case A, use a Δp = 5 psig at a design flowrate of 6.45 kg/sec and for case B, use a Δp = 1.67 psig at a flowrate of 6.45 kg/sec.
The overall dimensions, mass, and efficiency are being computed for a range of pump configurations which meet the baseline operating point. A trade of mass versus efficiency is anticipated from these results. In addition, the effect of frequency on pump performance is under consideration (in particular, the frequency range of 40 to 400 Hz).

3.13.5 Heat Exchanger Trade Study for the SPACE-R Thermal Management System

This section deals with the results and current status of a trade study to determine the best location and architecture for the SPACE-R Thermal Management System heat exchanger and NaK loop. The baseline design defined in the proposal calls for the heat exchanger to be located at mid-plane in the radiator cone as is shown in Figure 3.13-8. The suggested alternative design calls for it to be located at the upper end of the radiator cone as is shown in the same figure. The main purpose of the trade study is to identify means of improving the 1-g testability of the SPACE-R reactor by increasing its thermal vacuum test heat rejection capability to allow for near or full power operation simulation in ground testing.

The subject alternative SPACE-R Thermal Management System heat exchanger design offers the following nominal to significant advantages over the baseline heat exchanger design:

- potential for improved 1-g testibility;
- considerable increase in the space within the radiator cone available for other than TMS usage (such as the extension boom storage);
- some recurring cost reduction;
- some simplification in heat exchanger assembly; and
- somewhat easier header heat pipe fabrication.
### MAJOR DIFFERENCES BETWEEN THE BASELINE & ALTERNATIVE DESIGNS

<table>
<thead>
<tr>
<th>ITEM OF DIFFERENCE</th>
<th>BASELINE DESIGN</th>
<th>ALTERNATIVE DESIGN</th>
</tr>
</thead>
<tbody>
<tr>
<td>HX LOCATION IN RADIATOR CONE</td>
<td>MIDPLANE</td>
<td>TOP</td>
</tr>
<tr>
<td>HEADER HEAT PIPE SHAPE</td>
<td>CANE SHAPED</td>
<td>ELONGATED S SHAPED</td>
</tr>
<tr>
<td>HEADER HEAT PIPE LENGTH</td>
<td>3.3 METERS</td>
<td>5.15 METERS</td>
</tr>
</tbody>
</table>

---

**Figure 3.13-8** Alternative Heat Exchanger Trade Study
3.13.5.1 1-g Testability

One-g testability as the expression is used in this section is defined as being the measure of the fraction of full operating power waste heat that can be rejected in thermal vacuum testing by the radiator test article with the baseline or alternative heat exchanger configuration and a given test vehicle orientation. One-g testability of individual heat pipes is not an issue because they can be tested in horizontal direction. The 1-g testability of the radiator depends upon the ability to wet the header heat pipe evaporator sections in a given ground test article orientation. The following sections discuss the 1-g testability of the SPACE-R ground test article using the baseline and alternative heat exchangers in various ground test article orientations.

SPACE-R 1-G Testability with the Baseline Heat Exchanger

Following is a discussion of the potential SPACE-R heat rejection capability during ground testing using the baseline heat exchanger and with the test article oriented in various postulated vertical and horizontal test configurations.

Baseline Heat Exchanger with Test Article Oriented Vertically in the Upright Position

Using the baseline heat exchanger and with the test article oriented vertically in the upright position, the 80 heat pipes connected to the bottom section of the radiator cone and which also have their evaporator legs located well above the bulk of their condenser legs will not prime or wet (i.e. operate), since the adverse gravity force will more than overcome the capillary forces attempting to return the heat pipe working fluid condensate back to the heat pipe's evaporator section as can be seen in Figure 3.13-9. In the subject ground test article orientation, the ability of the evaporator sections of the 40 heat pipes connected to the upper radiator cone to wet depends upon the capillary wicking height limitation compared with the elevation of the evaporator over the low point in the heat pipe.
Figure 3.13-9 Baseline Heat Exchanger 1-G Testability Issues
Liquid will rise in the heat pipe's evaporator wicking structure only to a height dictated by the evaporator's wicking structure capillary pumping head limit. The equation defining the theoretical capillary head limit is as follows:

\[ H = 2 \sigma_l / \rho_l r_e \]

where:

- \( H \) is the theoretical capillary pumping head limit
- \( r_e \) is the wick pour radius
- \( \sigma_l \) is the heat pipe working fluid liquid surface tension
- \( \rho_l \) is the heat pipe working fluid liquid density

Figure 3.13-9 shows the theoretical capillary pumping head limit plotted as a function of the wick pour radius with a potassium filled heat pipe at an operating temperature of 850 K. Assuming that the evaporator wick has a pour radius of 30 \( \mu \text{m} \), then the theoretical capillary wicking head limit in the evaporator section of the heat pipe at 850 K is 0.7625 m. In theory, the smaller the capillary wick pour size the greater is the capillary pumping head available. However, pour plugging due to mass transfer deposits dictates a minimum allowable pour size. The smallest practical wick pour radius for long duration applications using potassium as the working fluid was assumed to be 30 \( \mu \text{m} \). As can be seen in Figure 3.13-9, the elevation of the top of the heat pipe shown in the figure approaches the capillary head limit assuming the wick pour radius to be 30 \( \mu \text{m} \). In practice it is difficult to achieve the theoretical capillary wicking head limit due to a number of factors but mainly because it is not possible to achieve uniform wick pour size. Figure 3.13-10 plots the evaporator leg maximum allowable adiabatic section vertical height as a percentage of the theoretical wick capillary pumping head actually achievable. As can be seen in the figure, if 67% is assumed for the percentage of theoretical wick capillary pumping head limit achievable, then the maximum allowable evaporator leg adiabatic section vertical height is 15.85 cm. When the evaporator is to be completely wetted in ground testing in the subject test article orientation. If the evaporator leg adiabatic section vertical height is between 15.85 and 50.85 cm, then the
fraction of the evaporator that will wet is shown in Figure 3.13-11 expressed as a % of the theoretical wicking height:

\[ X = \frac{50.85 - H_a}{H_c} \]

where:

- \( H_a \) ~ vertical height of heat pipe evaporator leg adiabatic section (cm)
- \( H_c \) ~ vertical height of heat pipe evaporator section (cm)
- \( X \) ~ fraction of the heat pipe evaporator wick wetted

For the baseline heat exchanger design with the test article oriented vertically, the vertical height of the evaporator and adiabatic sections above the heat pipe low point for header heat pipes, which are connected to the upper radiator cone, approach the theoretical capillary wicking height limit with the test article oriented upright as is shown in Figure 3.13-9. In this test article orientation, the evaporators of the header heat pipes connected to the lower radiator cone section will not wet and consequently not operate at all. The reverse condition will exist if the test article is ground tested in the inverted vertical orientation.

In practice it is not possible to achieve the theoretical wick capillary pumping limit. Typically 50 to 70% of the limit have been achieved. If it is assumed that 67% of the theoretical limit can be achieved by the heat pipes, then the vertical height of the evaporator leg adiabatic section cannot exceed 15.85 cm for the header heat pipes in the most advantageous orientation to fully wet with the test article oriented vertically. The heat pipes in the opposite vertical orientation will not wet regardless of the evaporator leg adiabatic section vertical height.

Assuming that:

(a) the test article is oriented vertically; and
THEORETICAL CAPILLARY WICKING HEIGHT = 76.26 CM
EVAPORATOR HEIGHT = 35 CM

30μM WICK POUR RADIUS

MAXIMUM VALUE RECOMMENDED

* TO ASSURE COMPLETE WETTING OF THE HEAT PIPE EVAPORATOR

Figure 3.13-10 One-G Testability of Baseline Heat Exchanger Design
(b) the maximum allowable evaporator leg adiabatic section vertical height is not exceeded; and

(c) therefore, full wetting of the most advantageously orientated header heat pipes occurs in ground testing,

then the heat rejection capacity of the SPACE-R reactor in ground testing is as follows:

(a) 55% of full operating power heat rejection with the test article upright with 33% of the total number of heat pipes that are connected to the upper radiator cone are functioning; and

(b) 80% of full operating power heat rejection with the test article inverted with 67% of the total number of heat pipes that are connected to the lower radiator cone functioning.

If the SPACE-R with the baseline heat exchanger is ground tested on its side, then 72% of the heat pipes will be fully functional, as is shown in Figure 3.13-12. The excess radiator capacity and re-radiation will raise the heat rejection capacity in this ground test article to nearly 100% of that needed to simulate full power operation.

If the SPACE-R with the alternative heat exchanger is ground tested in the vertical inverted position then all of the header heat pipes will be fully functional and, consequently, the heat rejection capacity will be 109% of that needed to simulate baseline full power on-orbit operation.

Figure 3.13-13 and Table 3.13-4 summarize the 1-G testability evaluation of SPACE-R incorporating the baseline and alternative heat exchangers in various ground test article orientations. In the figure, the predicted NaK reactor exit temperature at fully operating power simulation is give for the baseline and alternative heat exchangers and various ground test article orientations. For the baseline heat exchanger design and with the test article oriented vertically, the NaK reactor exit temperature is plotted as a function of the header heat pipe evaporator leg adiabatic section length.
Actual Capillary Wicking Height Assumed to be 67% of Theoretical

$30 \mu \text{M WICK POURED RADIUS}$

THEORETICAL CAPILLARY WICKING HEIGHT = 76.25 CM
EVAPORATOR HEIGHT = 35 CM

Baseline HX Ground Tested Vertical Inverted
Baseline HX Ground Tested Vertical Upright
Current Baseline Heat Exchanger

Figure 3.13-11 One-G Testability of Baseline Heat Exchanger Design
• WICKING HEIGHT ASSUMED ~ 50 cm (I.E. 2/3 RDS OF THEORETICAL)
• NUMBER OF HEAT PIPES WITH FULLY WETTED EVAPORATOR SECTIONS
  - UPPER RADIATOR SECTION ~ 40 \times \frac{278^\circ}{360^\circ} = 31 \text{ HEAT PIPES}
  - LOWER RADIATOR SECTION ~ 40 \left( \frac{246^\circ}{360^\circ} + \frac{252^\circ}{360^\circ} \right) = 55 \text{ HEAT PIPES}
  - TOTAL ~ 86 HEADER HEAT PIPES (I.E. 72% OF THE 120 HEAT PIPES)

Figure 3.13-12 1-G Testability of SPACE-R Baseline Tested on its Side

220
**ASSUMPTIONS**

- Waste Heat Generation Rate ~ 567 kW
- Radiator Area = 28 SQM
- Radiator Fin ε = 0.85
- NaK Flow Rate ~ 6.5 KG/S
- NaK ΔT = 100 K
- ΔT (HX NaK to Fin Ave) = 40 K
- Evap Wick Pour Radius = 30 μM
- Evaporator Length ~ 35 CM
- Wicking Height = 50 CM (65.6% of Theoretical)
- Sink Temperature = 255 K
- Re-radiation from Radiator Fins Included

**Figure 3.13-13 NaK Reactor Exit Temperature vs Radiator Effectiveness**
Table 3.13-4 also compares the TMS ground test performance with that on-orbit at B.O.L.

Table 3.13-4 One-G Testability of SPACE-R

<table>
<thead>
<tr>
<th>Heat Exchanger</th>
<th>Ground Test Article Orientation</th>
<th>Header Heat Pipe Evaporator Leg Adabatic Section Vertical Length (cm)</th>
<th>Radiator Effectiveness (in ground test compared with the on-orbit baseline*)</th>
<th>NaK Reactor Outlet Temperature (K)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline</td>
<td>on-orbit</td>
<td>40</td>
<td>116</td>
<td>893</td>
</tr>
<tr>
<td>&quot;</td>
<td>Upright Vertical</td>
<td>40</td>
<td>25.3</td>
<td>1171</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>≤ 15.85</td>
<td>54</td>
<td>1068</td>
</tr>
<tr>
<td>&quot;</td>
<td>Inverted Vertical</td>
<td>40</td>
<td>39.3</td>
<td>1064</td>
</tr>
<tr>
<td>&quot;</td>
<td>&quot;</td>
<td>≤ 15.85</td>
<td>80</td>
<td>974</td>
</tr>
<tr>
<td>&quot;</td>
<td>Horizontal</td>
<td>Varies</td>
<td>99.5</td>
<td>926</td>
</tr>
<tr>
<td>Alternative</td>
<td>Inverted Vertical</td>
<td>n.a.</td>
<td>109</td>
<td>905</td>
</tr>
</tbody>
</table>

* The baseline 28 m² of radiator area is 16% in excess of that needed to assure a NaK reactor exit temperature of 925 K at the on-orbit BOL baseline full reactor operating power and 6.5 kg/s NaK flow rate with 2 of 3 1:1M pump segments functioning.
As can be seen in the table, ground testing the baseline heat exchanger on its side with full power operation simulated in the test will produce a pumped loop NaK reactor exit temperature of nearly the baseline on-orbit 0-G value of 925 K with the loss the 16% excess radiators capacity provided for in the baseline.

Candidate Alternative Heat Exchanger Arrangements

Table 3.13-4 shows the dimension and mass impacts of several candidate TMS heat exchanger arrangements.

Impact on the Header Heat Pipe Design of Adapting the Alternative Design

The alternative design calls for decreasing the number of header heat pipes from 120 as shown in Table 3.13-5 to 80 or 84 heat pipes and increasing their length from approximately 3.3 m to about 5.15 m. Due to the increase in heat pipe heat throughput necessitated by these changes, the diameter of the pipes must be increased from 2.5 to 3.3 cm to compensate for the increase in performance (i.e. heat transport) demanded of the header heat pipes.

The maximum evaporator radial specific heat flux required to achieve the baseline value of 925 K for the NaK reactor exit temperature at full operating power with 2 of the 3 pump segment operating is 30.2 W/cm².

Mass Impact

Alternative heat exchanger designs consisting of 1, 2 or 3 rings of 80 header heat pipes will add about 140, 53 or 32 kg to the SPACE-R Thermal Management System mass respectively. The increase in armor mass is dictated by the need to increase the alternative heat exchanger armor thickness from 2 to 2.5 cm to compensate for the loss of the bumpering effects of the missing radiator fins in the immediate proximity of the alternative heat exchanger as shown in Figure 3.13-8. This heat exchanger armor mass increase is mostly compensated for by the mass savings gained from shortening of the NaK loop in the alternative design. There would be no inherent significant difference in the on orbit heat transfer performance between the baseline and the various postulated alternative heat exchanger designs.
**Table 3.13-5** Candidate Alternative Heat Exchanger Arrangements

<table>
<thead>
<tr>
<th>No. of Rings of Heat Pipes</th>
<th>No of Header Heat Pipes</th>
<th>Heat Pipe Dia. (~ cm)</th>
<th>No In Inner Ring</th>
<th>No In Center Ring</th>
<th>No In Outer Ring</th>
<th>HX ID* (~ cm)</th>
<th>HX OD* (~ cm)</th>
<th>Mass of HX Armor** (~ kg)</th>
<th>ΔMass of Alternative (compared with baseline) (~ kg)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline</td>
<td>3</td>
<td>120</td>
<td>2.7 (U)</td>
<td>36</td>
<td>40</td>
<td>44</td>
<td>39.9</td>
<td>49.9</td>
<td>50.7</td>
</tr>
<tr>
<td>Alternatives</td>
<td>3</td>
<td>84</td>
<td>3.3</td>
<td>24</td>
<td>28</td>
<td>32</td>
<td>32.4</td>
<td>44.8</td>
<td>47</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>84</td>
<td>3.3</td>
<td>40</td>
<td>--</td>
<td>44</td>
<td>51.3</td>
<td>59.8</td>
<td>68.7</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>80</td>
<td>3.3</td>
<td>24</td>
<td>28</td>
<td>28</td>
<td>31.3</td>
<td>43.7</td>
<td>45.5</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>80</td>
<td>3.3</td>
<td>40</td>
<td>--</td>
<td>40</td>
<td>50.3</td>
<td>58.7</td>
<td>67</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>80</td>
<td>3.3</td>
<td>--</td>
<td>--</td>
<td>80</td>
<td>100.2</td>
<td>104.8</td>
<td>154</td>
</tr>
</tbody>
</table>

* Heat Exchanger NaK channel

** Assuming the heat exchanger is encapsulated in armor of 2 & 2.5 cm thickness for baseline & alternative respectively

*** Includes -20 kg for forshortening the pumped loop; -3 kg for reducing the no. of radiator fins & +12 kg for increase in fin heat pipe mass & +49 kg for the increase in header heat pipe mass
Heat Exchange Fabrication Complexity

Significantly reducing the heat exchanger fabrication complexity depends mainly on arranging the header heat pipes in a single rather than multiple rings within the heat exchanger. This arrangement is viable for either heat exchanger location, but only at the expense of increasing the heat exchanger diameter from 50 to 105 cm, its mass by about 100 kg and the NaK loop pressure drop by up to 1 psi\(^1\). The adverse impact on reliability of reducing the number of header heat pipes by 1/3 is probably not of overwhelming statistical significance.

3.13.6 Pumped Loop Expansion Compensators

It is necessary to provide for the expansion of both the pumped loop itself and of its working fluid by separate means. This section deals with the evaluations that have been conducted on the best methods available to meet those requirements.

3.13.6.1 Loop Working Fluid (NaK) Expansion Compensator

The function of the pumped loop NaK expansion compensator is to maintain the loop pressure within its baseline allowable range over the system environmental and operating envelopes without the incursion of irrecoverable loss of loop NaK.

From its freezing temperature to 1000 K, the density of eutectic liquid NaK78 varies by about 20% from 0.88 to 0.7 g/cm\(^3\). If this difference in the density of the liquid NaK over the anticipated loop operating range is not compensated for by providing the pumped loop with a variable volume within which to expand as the NaK temperature rises, the loop pressure will increase to the point where either:

\(^1\) Significant differences in the mass resulting from differences in the heat exchanger diameter are dictated by the selected arrangement of the header heat pipes in the heat exchanger not by its location.
(1) it exceeds the loop pressure relief valve setpoint and the excess is dumped from the loop to the surroundings until such time as the loop pressure falls below the pressure setpoint; or

(2) the pressure buildup will burst the loop piping.

The NaK loop pressure need not be tightly controlled to afford satisfactory loop function and that fact affords considerable latitude in the selection of the means to provide for the pumped loop pressure control. With the baseline pumped NaK loop fluid volume of about 0.1 m$^3$, the size of the variable volume required with margin is about 0.02 m$^3$ (20000 cm$^3$). In addition, if means are to be provided to accommodate the boiling of all of the NaK in the TFE annuli, then added capacity of about 3500 cm$^3$ needs to be provided in the NaK expansion compensator raising its total capacity to change the loop volume to 23500 cm$^3$.

The issues involved in the selection of an approach and the design of the pumped loop NaK expansion compensator are reliability, mass, cost, materials compatibility, control and packaging.

The alternative expansion compensator concepts addressing the means of providing volume control, the force driving the control and the control mechanism include:

- **Type of Volume Control**
  - Piston
  - Bellows
  - Free gas or vapor interface

- **Drive Mechanism**
  - Spring
  - Forward/Reverse motor driven screw
  - Gas or fluid liquid/vapor phase change

- **Control mechanism**
  - Spring force
  - Command signal to activate motor
Volume Control - The piston type variable volume devices suffer from seal leakage and piston hangup problems making them unattractive for this application. The free gas or vapor interface type of volume control suffers from complexity as is the case with capillary control of the interface, the potential for gas mixing with the working fluid at the free interface since the gravitational force is not present to separate the cover gas from the liquid on orbit. The bellows variable volume devices are the ones that have been typically selected for the subject type applications.

Drive Mechanism - Forward/Reverse motor driven screws suffer from reliability, complexity, response time and hangup problems and are not attractive for this application. Pressurized gas control requires the use of consumables, which rules out that approach for a non-servicable long life application. Large volume passive gas control requires the use of a large volume gas tank to adequately dampen the pressure changes in the pumped loop due to the large range in NaK temperature that must be accommodated. The volume involved is too great to make the approach attractive for this application. Phase change of a control fluid is attractive from the standpoint of reliability, size constraints, but it suffers from technical complexity, slow response time and setpoint control fidelity. The spring drive is simple and can be made reliable to the level required for this application without excessive cost or effort. It is the drive that typically has been selected for the subject type application.

Control Mechanism Evaluation

Spring force provides passive control at minimal cost and complexity. It can be used to control the loop pressure to within an acceptable range of the loop setpoint pressure over a wide range in loop fluid temperature. The activation of a motor initiated by readings from loop pressure sensors is a reasonable active control mechanism, as is sourcing or venting of pressurized non-condensible gas on command. The heating or non-heating upon command of a cold biased control fluid at or near its saturation temperature controlled by
readings from loop pressure sensors is a reasonable active control mechanism. The use of a large volume non-condensible gas reservoir provides a simple passive means of controlling the pumped loop pressure over its environmental and operating envelopes.

The best means of affording variable loop volume to compensate for NaK expansion over the SPACE-R environmental and operating envelopes is the spring driven bellows expansion tank.

3.13.6.2 Pumped Loop Expansion Compensation Means

The baseline NaK pumped loop will expand or attempt to expand by about 3.6 cm (1.4") over the environmental and operating envelopes for SPACE-R. This thermal expansion needs to be accommodated and/or restrained by design.

Restraint consists of providing supports, tubing and loop components of sufficient adequacy and strength to be able to restrain the loop movement, while at the same time providing sufficient structural integrity to withstand the stresses. This type of pumped loop expansion control would add considerable mass to the loop and create other problems of similar magnitude that would have to be addressed as well.

Means of accommodating pumped loop thermal expansion include the following:

- Expansion Loops
- Slide-joints
- Free Hanging Loop
- In-Line Bellows Type Joints

The use of expansion loops is the preferred means of accommodating loop thermal expansion if they can be used. However, expansion loops occupy considerable space and, therefore, are not practical as the only means considered for the accommodation of loop thermal expansion. Slide joints are compact and afford considerable thermal expansion accommodation capacity. However, their use suffers from their considerable leak and hangup potential. The free hanging loop approach to loop thermal expansion accommodation
is attractive from the standpoint of simplicity, but does not offer superior or perhaps even adequate non-thermal (i.e. static & dynamic) load accommodation potential and further allows uneven radial thermal expansion of the loop. In-line bellows type joints offer an attractive means of loop thermal expansion accommodation, but have leak potential due to the inherent thin wall construction. Bellows have often been successfully used for liquid-metal applications.

The best approach to pumped loop thermal expansion accommodation appears to be the use of a proper combination of expansion loops and in-line bellows type joints.

3.13.7 Pumped Loop NaK Purification

This section deals with the evaluation done for the pumped loop NaK purification. The purpose is to keep the concentrations of the various undesirable contaminants in the NaK to within acceptable levels over the SPACE-R operating life. The potential adverse effects of NaK impurities include the plugging of flow passages, corrosion, nitriding, carborization with consequential detrimental changes in the structural and refractory material properties of those materials in contact with the NaK.

The issues that must be addressed in evaluating NaK purification are:

- The NaK constituent impurities and whether they are soluable or insoluble
- The level of NaK purity required for each of the potential impurities (i.e. tolerable impurity concentrations)
- The potential sources for the identified undesirable impurities (e.g. materials, welding, cleaning, etc)
- The rates & masses of impurity removal anticipated
- Impurity retention
- Getter material selection
- NaK purification trap or filter:
  - bypass flow ratio(s)
  - dwell time
- Filter mesh size(s)
• Assurance of full exposure of getter materials to contaminants
• plugging potential for pumped loop flow passages
• Trap heat source(s) or sink(s)
  - performance required
  - design

The important contaminants that must be kept to within acceptable tolerance concentrations are oxygen, hydrogen, carbon and nitrogen and combinations thereof such as hydroxyls, etc. However, there are a number of other potential contaminants that are also of importance and must be avoided and or kept to within acceptable concentrations in the NaK.

Approaches to NaK purification can include avoidance of contaminants by procurement specifications and protection, fabrication & assembly specifications and procedures, use of contaminant-free cleaning solvents, steam cleaning, bakeout, purge and the removal of contaminants from the working fluid during operation.

Of considerable importance is to minimize the scope and magnitude of contaminants introduced into the loop by procurement practices or introduced during fabrication and assembly, cleaning, charging, checkout, etc. It is important that the procedures be adapted that stress cleanliness and contaminant removal during fabrication and assembly. Use should be made of bagging, plugs and/or seals to protect fluid cavities from contamination during fabrication and assembly. Steam cleaning and cleaning using approved solvents should be done at appropriate steps along the fabrication and assembly flow. The contaminants that must be addressed in specifications and procedures for fabrication and assembly include mill scale, grease and oil, dirt and dust, rust, adhesives, welding flux, splatter and others. The use of protective coating should be avoided and the use of halogen and boron free cleaning solvents need be specified. The use of pressurized inert gas atmospheres in the fluid cavities of cleaned loop components should be emphasised. Bakeout at elevated temperature should be done once the parts and assembly are completed.

However, no matter what efforts are taken to assure that the NaK has the specified level of purity at the start of SPACE-R operation it is still of critical importance to provide
an effective means of cleaning the NaK during operation over the SPACE-R operating life. Methods of removing contaminants, from the working fluid during loop operation which can be considered, include:

- Precipitation methods (e.g. cold traps)
- Chemical reaction methods (i.e. the chemical bonding of impurities)
  - hot traps using solid getters
  - soluble getters
- Filtration methods
- Settling methods
- Centrifuging method

Of the methods mentioned above for the removal of contaminants from the working fluid during loop operation, the centrifuging and settling approaches are impractical for the given application due to the excessive mass needed, use of consumable resources and in the latter instance the lack of gravitational forces to settle out the heavier contaminants. Of the remainder, all but the use of soluble getters are viable. The use of a hot trap(s) with solid getters offers an effective means of controlling the concentrations of NaK contaminants without undue penalties being incurred. Candidates for the getter material include Zr, Ti, Zr-Ti, Ta, Nb, V and Y. Of these materials zirconium, yttrium, titanium and combinations thereof are the prime candidate getter materials. If needed a cold trap can also be introduced into the loop to reduce the concentrations of specific contaminants if the task cannot be accomplished with the use of a cold trap only. The hot or cold trap should be introduced into a bypass line to reduce the potential for flow passage plugging and to afford control of dewell time in the proximity of the getter material. The bypass flow rate must be adequate to assure timely removal of the contaminant introduced into the NaK. A bypass filter should be introduced into a bend in the loop for the removal of insoluble solid particulate matter of greater than a specified size. The flow cross section of the filter should be of sufficient size to preclude filter plugging.
3.13.8 NaK Loop Leak Avoidance and Protection

There are three issues that must be addressed in providing the loop with adequate protection against NaK leakage over the 10 year life goal. They are:

- Protection against space debris and micrometeoroid puncture
- Strength against hoop stresses
- Strength against structural failure of the loop
- Providing joints that will not leak

Puncture Protection

Protection of the NaK loop against space debris and micrometeoroid impact and puncture consists of surrounding the loop and its components with armor of sufficient thickness and strength to reduce the risk of loop puncture to the specified low probability of loop puncture over the 10 year operating life goal. It has been determined using the Space Debris and Micrometeoroid probability model, that beryllium armor of 2 cm thickness is sufficient to provide adequate protection in regions where the radiator cone fins can act as bumpers to intercept the projectiles before they reach the pumped NaK loop. In areas where that protection is not afforded, 2.5 cm thickness of beryllium armor is required according to the analysis. The mass of armor provided for the NaK pumped loop in the baseline design is about 65 kg.

Protection Against Hoop Stresses

The baseline operating pressure for the pumped loop is about 150 kPa (22 psi) which will not produce high hoop stresses. The loop will be provided with a NaK expansion compensator as discussed in Section 3.13.6.1, which will protect the loop against large increases in pressure due to decreases in the NaK density as its temperature rises and also will act to dampen the amplitude of pressure waves caused by other transient conditions such as NaK boiling, etc.
Structural Support

The loop will be protected against excessive bending stresses caused by the anticipated static and dynamic loads by providing adequate supports. Static and dynamic analyses will be done during the conceptual design phase to assure the adequacy of the conceptual loop support design. Adequate protection will be provided against thermal shock by design and/or operating procedures as is discussed in Section 3.13.9.

Pumped Loop Joint Design

Joints are prime NaK leak site candidates and care must be taken in the design, fabrication and assembly of the pumped loop to provide adequate assurance of leak tight operation over the operating life of the loop. In the first instance this will be done by minimizing the number of joints called for in the conceptual design. Further, only joining techniques of proven adequate reliability will be called out in the conceptual design. Basically this means that only welding will be specified for the NaK loop assembly joints. Flanged, threaded and other mechanical type joints will be avoided. The materials selected for the fluid channel walls will be compatible with the welding technique(s) specified for the loop. Full penetration butt welds will be specified for the loop joints using the tungsten inert-gas (TIG) welding method wherever possible. Laser welding is a relatively new welding technology that is probably also appropriate for the subject application. Adequate weld preparation will be specified including alignment and fitup. The integrity of the root weld pass will be checked out thoroughly by non-destructive testing, before proceeding with further weld passes as well as cleaning of the weld site before proceeding to the next weld pass. Compatible weld cleanup tools and cleaning agents will be specified. Inspection of the completed welds will be by radiography and dye penetration methods. The joints will be checked for cracking, structural strength, leak tightness and porosity. An inert gas will be pressurized to above atmospheric pressure in the fluid cavity being addressed if such precaution is needed to assure the structural integrity of the part during the weld process. The individual pumped loop components and assembly, when completed, will be tested to the proof pressure using an inert gas preferably seeded with helium for a thorough leak checkout.
Some of the suggested welding approaches are shown in Figure 3.13-14.

3.13.9 Pumped Loop Thermal Shock Avoidance and/or Protection

Due to their low Prandtl number, the temperature gradients in the walls of flow passages, in which liquid metals are used as the working fluid, can be large introducing the issue of thermal shock avoidance and/or protection in the pumped loop design and operation. The high convective heat transfer coefficients associated with the liquid metals can result in large thermal gradients existing in the walls of the flow passages associated with the flow under steady state conditions, but especially during thermal transients. Rapid rates of temperature change in the flow passage walls can occur during transient operation as well.

The introduction of large thermal gradients and rapid temperature changes in the walls of flow passages introduce the prospect of:

- excessive thermal stresses
- local thermal fatigue due to thermal cycling

Thermal stresses must be limited to $\leq 2$ times the yield strength of the flow passage wall material.

The pumped loop setpoint pressure need not be large and, therefore, thick walled piping need not be used, since the hoop stresses in the piping will not be great. The use of thin walled tubing reduces the scope of the thermal shock issue, since thin walls cannot support large radial thermal gradients through them. For this reason in liquid metal flow applications, thin walled tubing actually offers greater strength against the anticipated limiting stresses than do thick walled flow passages.

Large thermal stresses can occur at joints where abrupt and large changes in flow passage wall thickness exist. Therefore, rapid transitions in flow passage wall thicknesses will be avoided by design wherever possible.
Figure 3.13-14 Single and Double Welding Approaches
3.13.10 SPACE-R Heat Rejection System (HRS)

This section reports the progress made to date on the SPACE-R Heat Rejection System trade studies and conceptual design. The SPACE-R baseline HRS consists of the following six elements:

1. the interface with the pumped loop heat exchanger
2. header heat pipes
3. fin heat pipes
4. the interface between the header and fin heat pipes
5. the interface between the fin heat pipes and radiator fins
6. radiator fins

Waste heat from the reactor is transported by the pumped loop working fluid NaK liquid to the cross flow tube-in-shell heat exchanger, where it is transferred to the header heat pipe evaporators. The header heat pipes transport the heat to the fin heat pipes, which distribute the heat to the radiator fins for rejection by radiation to the space sink. The following sections deal with the work accomplished to date on the individual SPACE-R HRS elements mentioned above.

3.13.10.1 Header Heat Pipe/Pumped Loop Heat Exchanger Interface

The work done to date on the heat exchanger is described in Section 3.13.5. Methods of assembling the header heat pipes into the heat exchanger are discussed in Section 3.13.8. Predictions of the minimum required heat exchanger lengths to keep within the header heat pipe evaporator specific radial heat flux limits are given in Section 3.13.10.2 below.

3.13.10.2 Header Heat Pipes

The functions of the header heat pipes are to transport and disperse the waste heat taken from the pumped NaK loop in the heat exchanger to the radiator, where it is then transferred to the fin heat pipes for subsequent radiation to the space sink.
Estimates are made of the minimum header heat pipe sizes needed to accommodate the required heat pipe heat loads, using limits to heat pipe heat throughput estimates based on conventional heat pipe design practice and information gathered from outside sources.

**Heat Pipe Heat Throughput Limits** - Maximum heat throughput defines the maximum rate at which heat can be transported from the evaporator to the condenser of a given heat pipe at a given heat pipe operating temperature. The maximum rates of heat throughput, over the heat pipe operating range, are limited at any given operating temperature by the dominating heat pipe performance limit from among the following restraints on heat pipe heat throughput:

1. sonic limit
2. entrainment limit
3. capillary limit
4. viscous limit
5. evaporator specific heat flux limit

The sonic limit to heat transport through the heat pipe is encountered because at elevated vapor velocities that represent significant fractions of the speed of sound, compressibility effects and viscous drag reduce the heat pipe heat transport efficiency. The sonic limit is typically the controlling limit to heat pipe heat throughput at low heat pipe operating pressures, where it is the low pressure that limits the maximum rate of working fluid flow through the heat pipe. The specific heat pipe heat throughput sonic limits in potassium are given for the subject range of header heat pipe operating temperatures in Figure 3.13-15.

The sonic heat pipe design limit is usually taken as the heat throughput at 20 to 30% or even greater fractions of the heat pipe heat throughput based on the working fluid vapor flow rate at Mach 1. The next limit normally encountered is the entrainment limit, which occurs when the vapor velocity is sufficiently great to tear the liquid away from the wick as it flows back to the evaporator. If a sufficiently conservative fraction is used for the sonic limit, as is the case in the subject evaluation, the entrainment limit will not be encountered as the heat pipe operating temperature rises. The next limit normally encountered, as the heat pipe working fluid operating pressure and saturation temperature
Figure 3.13-15  Sonic Limits in Potassium
increase, is the capillary pumping limit. The capillary limit to heat pipe heat throughput defines the balance between the maximum pressure difference between vapor and liquid phases of the working fluid that can be supported at the interface between the phases and the pressure losses incurred by the working fluid as it circulates through the heat pipe or:

\[ \Delta P_a \geq \Delta P_i + \Delta P_l \pm \Delta P_v \]

where:

- \( \Delta P_a \) - capillary pumping head
- \( \Delta P_g \) - gravity head (in ground testing)
- \( \Delta P_l \) - liquid flow pressure drop
- \( \Delta P_v \) - vapor flow pressure drop

For a given heat pipe, the capillary limit to the flow of its working fluid is mainly defined by the pore size of the capillary wick and the flow pressure losses, which are mainly incurred in the vapor flow but also includes the liquid flow pressure losses as well. The heat pipe design capillary limit is typically taken as from 50 to 70% of the theoretical capillary pumping limit to account mainly for variations in wick pore size and uncertainties in the pressure drop predictions. The capillary limit is relatively independent of the working fluid operating temperature and saturation pressure, once the pressure has risen to well above the level of the complete flow loop \( \Delta P \) incurred as the working fluid circulates. This is not the case, however, with the sonic limit, which rises rapidly as the working fluid saturation pressure increases, as can be seen in Figure 3.13-15. The viscous limit is usually encountered in those heat pipes whose working fluid have relatively high dynamic viscosities, which does not apply in the subject application. The evaporator specific heat flux limit is encountered when the rate of potassium boiling at the heat pipe evaporator surface approaches the rate at which the working fluid liquid can be supplied to it by the heat pipe wick.

In summary for the subject heat pipe application, when the heat pipe is at the low end of its operating temperature range, one would expect the heat pipe heat throughput to be restricted by either the sonic or capillary limit and at higher operating temperatures, the capillary limit would be expected to control it.
Header Heat Pipe Performance - Heat pipe performance is defined in terms of the heat transport capability of a given heat pipe at a given operating temperature. Heat transport is expressed as the rate at which heat is transported through the heat pipe times the average distance it travels in the heat pipe from acquisition by to rejection from the heat pipe. The average distance the heat travels through the heat pipe is determined as follows:

\[ L_s = \frac{(L_a + L_c)}{2} + L_e \]

Where:

- \( L_a \) ~ Length of the adiabatic section of the heat pipe
- \( L_c \) ~ Length of the condenser section of the heat pipe
- \( L_e \) ~ Length of the evaporator section of the heat pipe
- \( L_s \) ~ Average distance the heat travels in the heat pipe

For the baseline and alternative heat exchanger designs the various header heat pipe lengths are as shown in Table 3.13-6 below, where \( L_m \) is the length of the given heat pipe.

<table>
<thead>
<tr>
<th>Concept</th>
<th>( L_{hp} )</th>
<th>( L_c )</th>
<th>( L_a )</th>
<th>( L_e )</th>
<th>( L_s )</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline</td>
<td>515</td>
<td>35</td>
<td>25</td>
<td>455</td>
<td>270</td>
</tr>
<tr>
<td>Alternative</td>
<td>330</td>
<td>35</td>
<td>68</td>
<td>227</td>
<td>217</td>
</tr>
</tbody>
</table>

From Table 3.13-6 it can be seen, that the header heat pipes in the alternative heat exchanger design are required to transport the heat to about 24% greater distances than is the case for the baseline heat exchanger design.

The term heat throughput is used to describe the performance of a heat pipe of known configuration. Heat throughput is the rate at which heat is transported from the evaporator to the condenser of the heat pipe.
Figure 3.13-16 SPACE-R Point of Departure Radiator Design
Header Heat Pipe Performance Requirements
SPACE-R Heat Rejection System

<table>
<thead>
<tr>
<th>Heat Pipe Numbers and Dimensions</th>
<th>Header Heat Pipes for:</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of HPs</td>
<td>Baseline HX</td>
</tr>
<tr>
<td>Overall Length (cm)</td>
<td>120</td>
</tr>
<tr>
<td>Evaporator Length (cm)</td>
<td>330</td>
</tr>
<tr>
<td>Outer Diameter (cm)</td>
<td>35</td>
</tr>
</tbody>
</table>

Maximum Header Heat Pipe Heat Throughput

Maximum Header Heat Pipe Evaporator Radial Specific Heat Flux

ΔT (HX NaK to Fin Ave.) (K)
Radiator Fin ε IR Assumed = 0.85

Figure 3.13-17 Header Heat Pipe Performance Requirements
Figure 3.13-18(a) SPACE-R Header Heat Pipe Heat Throughput Limits vs Req'ts
(for 3.25 cm O.D/2.67 cm I.D. Heat Pipes with "D" Shaped Condensers & 2.6 cm O.D/2.2 cm I.D. Heat Pipes with Circular Shaped Condensers)

% of Theoretical Capillary Limit
for 30 μm Wick Pour Radius

100%
70%
50%

30% of Sonic Limit
20% of Sonic Limit
Baseline
Alternative

Required HP Heat Throughput for HPs Attached to the:
Alternative HX & the Radiator
Baseline HX & the Upper Radiator Cone
Baseline HX & the Lower Radiator Cone

Figure 3.13-18(b) SPACE-R Header Heat Pipe Heat Throughput Limits vs Req’ts
Figure 3.13-18(c) SPACE-R Header Heat Pipe Heat Throughput Limits vs Req'/ts
Figure 3.13-18(d) SPACE-R Header Heat Pipe Heat Throughput Limits vs Req’ts
Header Heat Pipe Heat Throughput Required - Figure 3.13-16 shows the baseline SPACE-R radiator design that was presented in the SPACE-R proposal. The baseline 28 m² of radiator surface area affords 16% capacity beyond that needed to reject the 567 kW of waste heat generated with the reactor operating at full power at BOL. This provides for the failure of 27 of the 120 heat pipes before the NaK temperature at the reactor exit will begin to exceed the baseline temperature of 925 K. Figure 3.13-17 shows the maximum header heat pipe performance requirements (i.e. heat throughput and evaporator radial specific heat flux for the baseline and alternative heat exchangers) as a function of the ΔT between the NaK temperature in the heat exchanger and the radiator fin average temperature, assuming the radiator design shown in Figure 3.13-16 and a radiator fin infrared emissivity of 0.85. The value assumed for this ΔT for the subject evaluation was 40 K. The value assumed for the ΔT between the NaK in the heat exchanger adjacent to the heat pipe evaporator and the heat pipe operating temperature was 10 K. Figure 3.13-18 (a) through (d) show the header heat pipe heat throughput required, as a function of the location of the heat pipe evaporator in the heat exchanger NaK flow stream and the heat pipe operating temperature, for the baseline and alternative heat exchangers. As can be seen in the figure, the header heat pipe heat throughput requirements for the alternative heat exchanger design, which features the use of 80 header heat pipes, are somewhat higher than those for the baseline heat exchanger design which uses 120 heat pipes. As can be seen in the figure, the heat throughputs required of the header heat pipes drop as one proceeds through the NaK flow stream in the heat exchanger and the NaK temperature decreases. The heat throughputs required of the heat pipes for the alternative heat exchanger are greater than those for the baseline heat exchanger design, because the radiator fin area associated with each of those heat pipes is 3500 cm², compared with 2773 cm² and 1977 cm² for those connected to the upper and lower radiator cones respectively with the baseline heat exchanger design.

The header heat pipe maximum performance requirements assuming the radiator design, which was defined in the proposal and is presented in Figure 3.13-16, are shown in Figure 3.13-17. As can be seen in the figure and assuming:

The effective excess capacity increases to about 23%, if the heat radiating from the radiator fins attached to active header heat pipes to the back sides of fins attached to non-functioning heat pipes and the subsequent re-radiation of that heat to the space sink is factored into the excess radiator capacity as well.

247
(1) \( \Delta T(\text{HX NaK to fin ave}) = 40 \text{ K} \);

(2) \( \Delta T(\text{HX NaK to header heat pipe operating}) = 10 \text{ K} \); and

(3) fin emissivity = 0.85,

the heat throughputs required of the header heat pipes attached to the upper and lower radiator cones are as given in Table 3.13-7 below.

Table 3.13-7 - Header Heat Pipe Heat Throughput Required

<table>
<thead>
<tr>
<th>Heat Exchanger</th>
<th>Radiator Cone Heat Pipe is Connected to</th>
<th>Header Heat Pipe O.D.</th>
<th>Condenser Shape</th>
<th>Maximum Evaporator Radial Specific Heat Flux</th>
</tr>
</thead>
<tbody>
<tr>
<td>Design Length</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Baseline</td>
<td>39 cm Upper 2.7 cm 2.4 cm <em>O</em> <em>O</em></td>
<td>25 w/cm(^2)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>*</td>
<td>39 cm Lower 2.4 cm <em>O</em> <em>O</em> 22 w/cm(^2)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Baseline</td>
<td>31 cm Upper 3.4 cm 3.1 cm <em>D</em> <em>D</em></td>
<td>25 w/cm(^2)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>*</td>
<td>31 cm Lower 3.1 cm <em>D</em> <em>D</em> 22 w/cm(^2)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Alternative</td>
<td>42 cm Both 3.3 cm <em>O</em> 25 w/cm(^2)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Alternative</td>
<td>36 cm Both 4.1 cm <em>D</em> 25 w/cm(^2)</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Figures 3.13-18 (a)-(d) also show plots of the header heat pipe heat throughput limits for header heat pipes varying in size from 3 to 4 cm O.D. and from 2.4 to 3.2 for heat pipes with "D" and circular shaped condensers respectively. The data shown in the figure were plotted as a function of the NaK temperature adjacent to the heat pipe evaporators in the heat exchanger, which was assumed to be 10 K greater than the heat pipe operating temperature. The average sum of the heat pipe wick and wall thicknesses was assumed to be 0.2925 cm. The following header heat pipe heat throughput limits are plotted in the figure along with the heat pipe heat throughput requirements:
(1) 20 & 30% of the sonic limits based on the vapor flow rates at Mach 1 for 100%; and

(2) 50, 70 & 100% of the theoretical capillary pumping limits based on the maximum liquid vapor \( I/F \Delta P \) supportable in wicks with pore radii of 30 micrometer.

When the above design criteria are used as the performance criteria for the conceptual design of the header heat pipes, the following minimum header heat pipe sizes are needed to accommodate the required heat pipe heat loads for the SPACE-R TMS using the baseline heat exchanger concept:

(1) the O.D. of the heat pipes connected to the upper radiator cone must be \( \geq 2.5 \) to \( 2.7 \) cm; and

(2) the O.D. of the heat pipes connected to the lower radiator cone must be \( \geq 2.4 \) cm.

If, however, the condensers of the heat pipes are "D" shaped, then each of the minimum allowable diameters mentioned above must increase by 0.7 cm. Therefore, the heat pipe design criteria suggested in this report increase the header heat pipe mass by only about 5 kg beyond that presented in the SPACE-R proposal if circular shaped heat pipe condensers are adapted, or by 16 kg if they are "D" shaped.

For the alternative heat exchange design, where each of the 80 heat pipe carries an average 33% greater heat load over an average 24% greater distance compared with the 120 header heat pipes in the baseline heat exchanger design, the minimum header heat pipe O.D. needed to handle their respective required heat loads are raised to between 3.0 and 3.3 cm if the heat pipe condenser shape is circular and from 3.9 to 4.1 cm if they are "D" shaped. Therefore, changing to the alternative heat exchanger design would increase the SPACE-R header heat pipe mass from values given above for the baseline heat exchanger design by about 49 kg if the heat pipe condenser shape is circular and 66 kg if it is "D" shaped. However, some of the additional header heat pipe mass will be offset by the...
foreshortening of the pumped loop which is called for in the alternative heat exchanger TMS configuration.

Tailoring the sizes of the heat pipes to meet their individual maximum heat loads saves only about 3 kg of SPACE-R launch mass for the baseline heat exchanger design, compared with using single sizes for the heat pipes connected to the upper and lower radiator cones for each application. The comparable mass saving for the alternative heat exchanger design would be about 8 kg.

**Evaporator Radial Specific Heat Flux**

Assuming the proposed baseline heat exchanger length of 35 cm and the header heat pipe O.D. of 2.5 cm, the maximum required evaporator radial specific heat flux is about 30 w/cm² as shown in Figure 3.13-17. The evaporator radial specific heat flux can be limited to \( \leq 25 \text{ w/cm}^2 \) for more reliability in heat pipes having potassium as their working fluid.

With the minimum required heat pipe sizes predicted above, the active heat exchanger lengths needed to limit the radial specific heat flux in the header heat pipe evaporators to \( \leq 25 \text{ w/cm}^2 \) are shown in Table 3.13-8 below.

---

However, if the minimum allowable header heat pipe pore size is > 40 mm, then the minimum allowable heat pipe size will increase proportionally as the wick pore size is increased. The subject evaluation has confirmed that the capillary limits to heat pipe heat throughput need be considered, as well as, sonic limits to heat pipe performance in the conceptual design of the Space-R header heat pipes.
### Table 3.13-8 Minimum Required Heat Exchanger Lengths

<table>
<thead>
<tr>
<th>Heat Exchanger</th>
<th>Radiator Cone Heat Pipe O.D.</th>
<th>Header Heat Pipe O.D. Connected to</th>
<th>Condenser Shape</th>
<th>Maximum Evaporator Radial Specific Heat Flux</th>
</tr>
</thead>
<tbody>
<tr>
<td>Baseline</td>
<td>39 cm Upper 2.7 cm Lower 2.4 cm</td>
<td><em>O</em></td>
<td>25 w/cm²</td>
<td>22 w/cm²</td>
</tr>
<tr>
<td>Baseline</td>
<td>31 cm Upper 3.4 cm Lower 3.1 cm</td>
<td><em>D</em></td>
<td>25 w/cm²</td>
<td>22 w/cm²</td>
</tr>
<tr>
<td>Alternative</td>
<td>42 cm Both 3.3 cm</td>
<td><em>O</em></td>
<td>25 w/cm²</td>
<td></td>
</tr>
<tr>
<td>Alternative</td>
<td>36 cm Both 4.1 cm</td>
<td><em>D</em></td>
<td>25 w/cm²</td>
<td></td>
</tr>
</tbody>
</table>

**1-G Testability** - Liquid will rise in the header heat pipe evaporator leg during ground tests only to the vertical height dictated by the leg’s wicking structure capillary pumping head limit, which is dictated by the heat pipe working fluid properties and the wick pore size as defined by the equation given below.

\[ H = \frac{2 \sigma_i}{\rho_i r_e} \]

where:

- \( H \) ~ the theoretical capillary pumping head limit
- \( r_e \) ~ the evaporator leg wick pore radius
- \( \sigma_i \) ~ the heat pipe working fluid liquid surface tension
- \( \rho_i \) ~ the heat pipe working fluid liquid density

The theoretical capillary pumping head limit in potassium at 850 K, with a wick having a pore radius of 30 micrometer, is 0.7625 m. It is not, however, possible to achieve the theoretical capillary pumping head due to variations in the pore size, etc. The actual
limit to vertical capillary pumping height against gravity is usually between 50 and 70% of the theoretical capillary pumping head (i.e. between 0.4 and 0.5 m with a wick having a pore radius of 30 micrometer).

The use of abrupt (i.e. up to 90°) transitions in flow direction to minimize the header heat pipe evaporator leg adabatic section axial length will greatly improve the 1-G testability of the SPACE-R reactor ground test article oriented vertically. However, that approach will require assembly of the header heat pipe segments rather than bending the assembled heat pipe into the desired shape, raising the issue of the continuity of the capillary wick at the heat pipe assembly interfaces. Also the sharp bends will increase the pressure losses and consequently reduce the maximum heat throughput capacity of the heat pipes somewhat.

SPACE-R can also be ground tested on its side, with only the heat pipes on the bottom of the radiator cone cross section unable to overcome gravitational forces.

The SPACE-R alternative heat exchanger TMS configuration described in Section 3.13.5 is 1-g testable to full simulated reactor operating power levels with the ground test article oriented vertically. In this ground test configuration, all of the header heat pipes will operate as thermal syphons. One-g testability of the SPACE-R reactor is also addressed in Section 3.13.5.

3.13.10.3 Header/Fin Heat Pipe Interface

Figure 3.13-19 (a) and (b) show the interfaces between the header and fin heat pipes being evaluated. In the first, the fin heat pipes are wrapped around the header heat pipes at 8 cm intervals. In the alternatives, the header heat pipe condensers are "D" or "U" shaped to provide a flat surface for interfacing the header and fin heat pipes. The former approach allows for a mass savings of 11 kg in header heat pipe mass and the latter affords easier fin heat pipe fabrication.
Figure 3.13-19  Header/Fin Heat Pipe Interface.
3.13.10.4 Fin Heat Pipe

The one-way thermal diode fin heat pipes serve two functions in the SPACE-R TMS. Their normal purpose is to receive the waste heat from the header heat pipes and distribute it over the fin for rejection to space. During a high energy incident scenario, they act to protect the TMS by not transferring the high intensity heat incident upon the radiator fins to the header heat pipes. This is accomplished by providing a discontinuity in the fin wick for a condensate reservoir and trap. However, if survivability requirements is relaxed, then the fin heat pipes can be eliminated from the SPACE-R HRS design saving their mass of 44 kg. Instead, the header heat pipes can be used to transfer heat directly to the radiator fin roots saving the ΔT inherent in imposing the fin heat pipes in the heat flow path between the header heat pipes and radiator fins. The specific header heat pipe condenser radial heat flux would actually be reduced from 19 w/cm² using the fin heat pipes to < 15 w/cm² using that approach. The maximum distance from the fin root to the center of the fin would only increase from the baseline 4 cm using the fin heat pipes to 7.4 and 5 cm using the baseline and alternative TMS header heat pipe arrangements respectively. Further, some of the mass saved by eliminating the fin heat pipes could be used to increase the thickness of the radiator fins. This, together with the reduction in heat path ΔT gained, will reduce cost and maintain the radiator effectiveness without incurring a mass penalty for the benefit offered by the approach to TMS design.

SPI reviewed a number of candidate alloys for the fin heat pipe envelope. These materials include Nb-1Zr, SS316, CP Ti, Ti-6242, Be, and a composite of SiC in a Ti-64 matrix. Creep studies for ten year life were completed as a function of three envelope geometries for an advanced SP-100 radiator with the support of NASA-Lewis. Low mass candidates, capable of ten year life, based on Ti-6242 and Be were identified. While meeting structural requirements, liquid metal compatibility for the titanium alloy appears undetermined; to resolve this data gap, SPI initiated an 1000 hour compatibility test of Ti-6242 samples in potassium and in NaK at an exposure temperature of 870 K in the NASA-Lewis program.
Another candidate fin heat pipe envelope material for the SPACE-R is a composite formed from titanium foil/titanium powder/titanium diboride powder. Potassium heat pipes with a circular cross-section were previously fabricated by Thermacore for WRDC from two different composite materials, then tested at 900 K. One composite tube used titanium foil/titanium powder/titanium diboride powder, while the second used niobium-1% zirconium foil/titanium powder/titanium diboride powder. Both heat pipes were life tested for over 700 hours, with no signs of incompatibility. The material has also been shown to be resistant to hypervelocity impact.

3.13.10.6 Radiator Fins

The minimum allowable radiating fin thickness on fin heat pipes is 0.020". Carbon-Carbon is the lightest material, with the highest thermal conductivities. The lowest mass radiator designs used Carbon-Carbon fins. With NASA support, SPI received a shipment of prototypic C-C fins from our subcontractor. These fins were fabricated in prototypic thickness (0.5 mm and 1 mm) from both vapor grown carbon fibers as well as from high conductivity pitch fibers (K-1100). Materials samples were prepared and sent out for thermal conductivity measurements in the NASA Lewis program.

The coefficient of thermal expansion (C.T.E.) for Carbon-Carbon is near zero, while the coefficient of the composite fin heat pipes is near $7 \times 10^{-6}$ m/m °C. The large difference in C.T.E. causes stresses as the fin/fin heat pipe is thermally cycled, and may cause the joint to fail. SPI and Thermacore believe that a reliable joint can be obtained by using a relatively soft, pliable material in the joint to reduce the stresses due to the C.T.E. mismatch.

3.14 Control and Startup Trades

Electromagnetic (IEM) pump controller is needed for the SPACE-R program if induction EM pump is chosen as the NaK pump. A similar IEM pump has been installed and operated by SPI with two different types of control methods. There are three basic approaches to control the IEM pump: variable frequency, variable amplitude, and variable duty cycle.
The variable frequency method has the potential of more efficient startup if the control circuit can be made smart enough to adjust the frequency to minimize the slip at startup. However, the circuit needed to sense the flow and to control the frequency to minimize the slip will add significant complication to the controller. Also, the magnetic core designed to accommodate the low frequency at startup will not be optimized for steady state operation. In other words, if the magnetic core is designed for a particular steady state frequency, the pump at lower frequency at startup will result in core saturation and pump overheating.

The variable amplitude approach has been demonstrated successfully at SPI and other applications. Variac, such as the one used in SPI, is an ideal variable amplitude control mechanism. If the weight is not an issue, a motor driven variac could be used to control an IEM pump. However, it is not recommended for a space system. Except for a variac, no other efficient method was identified to vary the amplitude of the pump drive voltage. The method of using a series pass regulator will have very low efficiency and the method of using a large reactive filter will have very heavy weight.

The duty cycle approach is the most promising approach. It includes the all Pulse Width Modulation (PWM) methods and the phase-control type SCR controller. For the same reason as the variable frequency control, the fixed frequency PWM is preferred.

SCR phase-control AC drives are almost as efficient as the variac drives. This fact indicated that a square waveform with duty cycle control may also work well with the IEM pump. The advantages of the square waveform duty cycle approach are the simplicity and the efficiency. The drive can be generated from a D.C. bus directly with minimum component count and maximum efficiency.

Using higher frequency will cut down the core size significantly. More importantly, the size of the EMI filter required to suppress interference on the D.C. bus is also dependent on the operating frequency. Lower operating frequency needs bigger filters. Pump design issues are being evaluated for a range of operating frequency (40 to 400 Hz). A 180 Hz design seems to be most promising at this point.
3.15 Deployment/Spacecraft Integration

A wide range of payload deployment issues and concepts have been identified and studied during this report period. A preliminary spacecraft design has been prepared for an electric propulsion payload for orbit transfer missions. Integration issues of this spacecraft into an Atlas II launch vehicle have been identified and assessed. Application of the SPACE-R power system to the electric propulsion spacecraft introduces a host of requirements on extension boom stiffness, electric power transmission, EMI, and current-induced magnetic torque. Structurally, the generic nuclear powered spacecraft results in a "dumbbell" shape with two large masses at the opposite ends of a long, low mass separation boom. In addition to deployment and structural considerations, high current bus bars are required to transfer the output power to the payload. As payload separation is increased, mass savings from the radiation shield are traded against increases in the extension boom mass and the bus bar mass. Furthermore, complex packaging and reliability issues are raised as deployment concepts for increased separation are defined. Twelve deployment/boom concepts are under study which allow a range of separation distances from 8.8 to 14 meters. These concepts range from relatively simple configurations to complex mechanisms. Under consideration are the: Astro Telescopic Cylinders, the Astro Stac/Z Beam, the Scissors Boom, the Flex Cage, the Telescopic Truss, the Folding Truss, the Pivoting Base-Rigid, the Fixed Base-Flex, the Single Central Truss, the Fixed Rigid Truss, the Hinged Payload, and the Integrated Rail/Bus.

Two deployment schemes are briefly described here: the Astro Telescopic Cylinders and the Fixed Rigid Truss. These concepts are selected to illustrate the diversity of approaches under consideration and should not be taken as the prime candidates at this stage of the trade study. The Astro Telescopic Cylinders concept is shown in Figure 3.15-1 for a 12 meter separation (later studies have included a separation of 14 meters). The stowed configuration for the Atlas II launch vehicle is shown on the left in Figure 3.15-1 (note that launch vehicle dimensions are in inches). This boom concept is based on the use of four or more telescopic cylindrical sections. These thin aluminum cylinders are secured with tapered pin lock up devices at the end of each section. An Astro Bi-stem deployment mechanism, mounted inside the inner cylinder on the payload, is used for deployment. Separate coiled cables are used as bus bars. Fifty to one hundred #8 AWG cables are under study for each polarity. The fundamental boom frequency for this concept was calculated as 0.8 Hz.
Figure 3.15-1  Astro Telescopic Cylinders
A less complex, but shorter (9 meter separation) concept is illustrated by the Fixed Rigid Truss configuration shown by Figure 3.15-2. Here, four triangular rigid trusses are used on each side. Each truss utilizes a central deployment guide/track in which the track is isolated from the main structure. These tracks are also used as the bus bars with electrical contacts engaged at the end of deployment. A single deployment motor gear unit with a rotating lead screw is used to deploy the power system. This relatively stiff configuration results in a fundamental longitudinal boom frequency of 7.5 Hz at a boom mass of only 8.4 kg. The structural analyses of most of the concepts have now been completed with the result that a boom frequency of several Hertz can be achieved with boom masses of only 8 to 18 kg. Clearly, the mass of the extension boom system is driven by the mass of the low voltage bus.

An electric propulsion payload module has been designed to fit within the parameters of the Atlas II launch vehicle. This module, shown by Figure 3.15-3, is based on the use of six SPT-200 electric thrustors. Using Xenon propellant, an ISP of 1990 seconds is achieved while absorbing about 27 kW of electric power. Launch vehicle feasibility depends on the spacecraft stowed center of mass (C.M.) as well as its total mass. The mass characteristics of the SPACE-R system are summarized by Table 3.15-1. Two launch scenarios were studied for spacecraft bus and propellant masses. These mass characteristics for the spacecraft are summarized by Table 3.15-2. These center of mass results fall within the Atlas II C.M. launch limits provided that a high strength interface attachment fitting is used. This issue is under evaluation with General Dynamics. Preliminary results indicate that the use of the modified C/C1 adaptor will allow stowage of the power system/payload module within the launch vehicle fairing (see Figure 3.15-2) while meeting launch mass and C.M. constraints.
Figure 3.15-2 Fixed Rigid Truss
Figure 3.15-3 Thruster Arrangement
Table 3.15-1  SPACE-R 44 kWe BOL System Mass & C.M.

<table>
<thead>
<tr>
<th>Component</th>
<th>Mass (kg)</th>
<th>C.M. above Radiator Aft Plane (cm)</th>
<th>Mass C.M. (kg-cm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reactor</td>
<td>621</td>
<td>604</td>
<td>375084</td>
</tr>
<tr>
<td>ZrHx Shield</td>
<td>437</td>
<td>535</td>
<td>233795</td>
</tr>
<tr>
<td>LiH Shield</td>
<td>132</td>
<td>511</td>
<td>67452</td>
</tr>
<tr>
<td>Drum Control Elements</td>
<td>70</td>
<td>511</td>
<td>35770</td>
</tr>
<tr>
<td>Wet Plumbing</td>
<td>97</td>
<td>400</td>
<td>38800</td>
</tr>
<tr>
<td>EM Pump</td>
<td>50</td>
<td>313</td>
<td>15650</td>
</tr>
<tr>
<td>LV Bus Bar (1/2)</td>
<td>49</td>
<td>310</td>
<td>15190</td>
</tr>
<tr>
<td>Be Armor</td>
<td>160</td>
<td>300</td>
<td>48000</td>
</tr>
<tr>
<td>Radiator/Heat Exch.</td>
<td>250</td>
<td>233</td>
<td>58250</td>
</tr>
<tr>
<td>Ext. Boom/Bus Bar (1/2)</td>
<td>191</td>
<td>120</td>
<td>22920</td>
</tr>
<tr>
<td>Control Elec.</td>
<td>98</td>
<td>100</td>
<td>9800</td>
</tr>
<tr>
<td>Battery</td>
<td>15</td>
<td>50</td>
<td>750</td>
</tr>
<tr>
<td><strong>Total</strong></td>
<td><strong>2170</strong></td>
<td></td>
<td><strong>921461</strong></td>
</tr>
</tbody>
</table>

Power System C.M. above Radiator = 424.6 cm  
167.2 inches
Table 3.15-2 Launch Vehicle Lift Requirements

<table>
<thead>
<tr>
<th>Launch Estimates</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>#1 SPACE-R</td>
<td>2170</td>
</tr>
<tr>
<td>Spacecraft Bus</td>
<td>1157</td>
</tr>
<tr>
<td>Xenon Fuel</td>
<td>1125</td>
</tr>
<tr>
<td>Biprop Fuel</td>
<td>18</td>
</tr>
<tr>
<td>Separated Mass</td>
<td>4470 Kg.</td>
</tr>
<tr>
<td>Center of Mass</td>
<td>465.1 cm</td>
</tr>
</tbody>
</table>

| #2 SPACE-R       | 2170 |
| Spacecraft Bus   | 1306 |
| Xenon Fuel       | 1175 |
| Biprop Fuel      | 19   |
| Separated Mass   | 4670 Kg. |
| Center of Mass   | 451.1 cm |

Equipment Module Modifications
Adapter (Modified C/C1)
Separation System
Mission Peculiar Mods

3.16 System Performance

3.16.1 Electrical Performance

A system electric performance study was performed on the effect of failure of TFEs and open circuit in the TFE cluster connection in the SPACE-R power system.

Micro-cap IV electrical simulation program was used for the analysis. The program shows not only the circuit diagram but also the simulation results on the schematic circuit, such as bias DC voltage and current on each leg of all of the components to be studied in this simulation. Micro-Cap IV can offer DC, AC, and transient analyses with temperature variations. This program also allows one to study the waveforms of voltage, current, charge, capacitance, flux inductance, B field and so on at any point of the schematic during power transient periods.
A single cell TFE electrical model was developed based on a linear voltage source and an internal resistor. A temperature-control voltage source has been connected in series with the linear source to simulate three different temperature zones in the SPACE-R core.

In the arrangement of the TFE interconnection, three or four TFEs are connected in parallel as triple or quadruple clusters in three different temperature zones.

In the simulation of the electrical power system of SPACE-R, six different cluster models have been employed. The six different models are triple and quadruple TFEs in parallel in low temperature zone (B zone), medium temperature zone (C zone) and high temperature zone (A zone).

Every model has been simulated in DC sweep to confirm its V-I characteristic curve with respect to different temperatures. The center tap is made through a fuse link which is simulated by a grounded 10Ω resistor.

A 0.9 mΩ resistor has been added into every model to approximate the effect of TFE lead connections. Power bus bar and the EM pump at the load were also included in the simulation.

Open Circuit in TFE Connections

The V-I characteristic curves of SPACE-R system for stepping temperatures were plotted out, before any TFE cluster is opened. The voltage and current on each leg of a TFE cluster were also generated for reference. "Ladder Connections" separate the TFE clusters into five different areas. Different area contains different numbers of TFE clusters.

In the first open circuit test, half of a TFE cluster in a given area was disconnected to observe the power loss in the whole system and the voltage and current changes in each leg of the TFE cluster. When half of a TFE cluster in one area is disconnected, the current in the remaining half is almost doubled and the corresponding voltage across this remaining half decreases, resulting in a reduction in the output power. The power loss ranges from 2 to 6 kWe depending on the location of the open circuit.
When half of TFE clusters in any two different areas are disconnected simultaneously in the same manner as described above, the power loss of SPACE-R is slightly smaller than the sum of power losses caused by individual disconnection in any two single areas. Even though the power loss due to a open circuit in the TFE cluster connections will be a significant fraction of the total power output, the TFE cluster connection is not expected to suffer open circuit because of the use of flexible copper leads.

**TFE degradation or loss**

In order to simulate the effect of TFE loss in cluster circuit, three models of double paralleled TFE cluster with three different temperature settings have been built to replace the triple and quadruple models.

The power loss on the whole system was about 200 W when one TFE was taken out from a quadruple cluster. The location of that quadruple cluster did not make much difference on the overall power loss.

The power loss on the whole system was about 400 W when one TFE was taken out from a triple cluster. The location of that triple cluster did not make much difference on the overall power loss.

### 3.16.2 Decay Heat Removal

An evaluation was done to determine if radiation from the core to its surroundings along with waste heat removal by residual NaK circulation through the core is sufficient to preclude the core moderator temperature from rising to the point where the NaK in the TFEs will begin to boil during reactor scram.

If this proves not to be the case, then:
(1) the waste heat rejection from the core will have to be augmented by extending the circulation of NaK through the core using backup battery power provided for that purpose; and/or

(2) compensation must be provided for NaK boiling in the TFEs by design; and/or

(3) the NaK loop operating pressure must be increased to raise the NaK boiling temperature.

The NaK circulation through the core just after reactor scram is driven by residual electrical power generation from the TFEs as the temperature of their emitters decay to the level where electrical power is no longer generated by the TFEs. Then the NaK flow through the core either ceases or must be driven by a backup source of electrical power.

After initiation of reactor scram and the temperature of the reactor fuel and TFE emitters begin to decay, a portion of the waste heat crossing the TFE cesium caps will go toward sensible heating of the reactor core. The remainder is continuously being removed from the core by radiation to the core surroundings and by transport away from it via the pumped loop working fluid as the transient progresses. The sources of the waste heat are the sensible heat stored in the reactor fuel and the TFE emitters at the start of the transient, as well as the continuously decaying rates of heat generation by delay fission and gamma heating. At the beginning of reactor scram for a short time sufficient electrical power will continue to be generated by the TFEs, for the EM pump to continue circulating NaK through the TFEs. However, when the temperature of the TFE emitters decays to about 1400 K electrical power generation in the TFEs ceases.

A simplified thermal/hydraulic analysis was done to determine if the residual NaK circulation through the reactor core is of sufficient duration to preclude NaK boiling in the TFEs during reactor scram. The thermal/hydraulic model used for the decay heat removal

---

During the critical initial period after scram, the sensible heat stored in the fuel and TFE emitters are by far the largest source of the heat raising the core temperature during the reactor shutdown not the decay heat from delayed fission and fission product and activation gamma heating, which are only contributing sources adding to the the core temperature buildup during scram.

266
analysis is shown in Figure 3.16-1. As the figure shows, the TFEs were lumped together to create a single TFE residing on the fourth TFE ring. The lumped TFE was divided into 5 nodes as shown in the figure. Decay heat was loaded on the TFE fuel node. Radiation, conduction and electronic cooling heat paths across the cesium gap were considered in the TFE thermal model, as was heat removal by NaK circulation through the TFE NaK annuli so long as the EM pump continues circulating the NaK through the core. The TFE is thermally connected to the core O.D. in the model by a conduction heat path. Radiation from the core O.D. to the reflector I.D. was included in the model, as was conduction through the reflector and the subsequent radiation from the reflector O.D. to the space heat sink. Axial heat rejection from the reactor core, beyond that afforded by the circulation of NaK through the TFEs, is not considered in the model, adding a significant degree of conservatism to the analysis results. The NaK flow was modeled based on a postulated typical EM pump performance curve combined with equations that describe the electrical power generation in the TFEs.

Typical results from the decay heat evaluations are presented in Figures 3.16-2 (a) & (b), which show the temperature histories of the various reactor core elements after reactor scram, assuming a current density output from the TFEs of 0.05 amps/cm². As can be seen in Figure 3.16-2 (a), the TFE fuel and emitter temperatures decay rapidly until the emitter temperature drops to 1400 K and pump operation and consequently NaK circulation through the TFEs ceases. This occurs about 200 seconds after scram begins. The TFE emitter temperature initially rises to about 1840 K in the first 10 seconds or so and then decays at the average rate of about 3 K/s. Figure 3.16-2 (b) shows that the temperature of the NaK in the fourth ring of TFEs initially rises 50 K to about 925 K in the first 10 seconds after scram begins. It then drops until pump operation ceases, at which point the NaK temperature begins to rise again to peak at about 2500 seconds after the start of scram at a temperature of about 1010 K, which is about 85 K above the maximum core temperature during normal operation and 100K below the boiling temperature of NaK. The average core temperature lags behind the TFE NaK temperature by about 50 K as the transient progresses, with the reflector temperature lagging the average core temperature by about 180 K dropping to about 750 K one hour after scram has began.
Figure 3.16-1  The Thermal/hydraulic Model Used for the Decay Heat Removal Analysis.
Figure 3.16-2 Temperature Histories of the Various Reactor Core Elements After Reactor Scram.
Since the TFE distribution in the core is nearly homogeneous, it can be expected that the radial thermal gradient in the core will be nearly linear, once the NaK circulation through the core ceases 200 seconds after reactor scram begins. As can be seen in Figure 3.16-2, the maximum predicted radial ΔT from the fourth TFE ring to the outer edge of the core achieved during the transient is < 50 K. Therefore, conservative extrapolation of that gradient from the fourth to the first TFE ring adds at most an additional 50 K to the prediction of the maximum TFE NaK temperature achieved during scram. This raises the peak predicted TFE NaK temperature to not more than about 1060 K, which is still more than 50 K below the boiling temperature of NaK78 at the pumped loop operating pressure. Adding an additional 50 K to the predicted peak TFE NaK temperature to account for the axial temperature gradient through the core, which exists during normal operation, would raise the peak TFE NaK temperature to the threshold of NaK boiling, which is 1110 K at the NaK loop operating pressure. However, that magnitude of axial core ΔT cannot be sustained once scram starts, due mainly to axial heat rejection from the core, which was not considered by the simplified 2-D thermal model used for the analysis. The conclusion from the analysis, therefore, is that residual NaK circulation through the core alone is sufficient to prevent nearly all if not all of the NaK in the TFEs from boiling during a reactor scram.

Additional evaluations were done to determine the effect on the peak NaK temperature reached in the TFEs during scram by using backup power to extend the time of NaK circulation through the core beyond that achievable by residual pump operation alone. Figure 3.16-3 shows the effect of extending the period of NaK circulation through the core using a backup electrical power source provided for that purpose. The data in the figure include the maximum radial and axial core thermal gradients added to the thermal model predicted peak TFE NaK temperature in the fourth ring of TFEs at the reactor core mid-plane.

As can be seen in the figure, the peak predicted TFE NaK temperature falls rapidly as the duration of extended NaK pump operation is increased.
Figure 3.16-3 The Effect of Extending the Period of NaK Circulation Through the Core.
Finally, however, it must be pointed out that any significant NaK boiling will raise the NaK loop pressure to above the NaK saturation pressure if expansion of the NaK going through the phase change is not accommodated by increasing the loop volume. Consequently, significant quantities of NaK boiloff in the TFEs will not occur, even if the NaK temperature anywhere in the TFE annuli do achieve the loop operating pressure saturation temperature during scram. Therefore if the pumped NaK loop proof pressure is sufficiently high, the SPACE-R TMS can accommodate some boiling in the NaK loop without irreversible consequences occurring.

4.0 Task 1.3: Point Design

4.1 Objective

The main objective of Task 1.3 Point Design is to develop a conceptual system design which meets all the Functional Requirements at the 40 kWe power level. The sensitivity of the point design parameters is also to be studied as the power level is varied around the design point, including the system and component configuration, mass and volume of the power system. The methodology, assumptions, and any experimental data are to be provided to support the design and analyses.

This task is based on the Task 1.2 Parametric Trade Study results to define the point design. This task will be closely coordinated with Task 1.4 Design Assessment, Task 1.5 Key Technology Demonstration, Task 1.6 Failure Modes and Effects Analyses, Task 1.7 Flight Planning Document, and Task 1.8 Critical Component Demonstration to incorporate any changes or new information as the design progresses.

4.2 Progress to Date

The TFE design was initiated first based on the parametric trade study results. The baseline TFE design being established has the following features: single cell TFE, single crystal Mo-Nb alloy emitter, Mo collector, and fuel smear density of 70%. The fuel configuration has not been finalized yet. Some of the design parameters may be changed as
the design progresses. Specifically, technical input from Luch engineers after the INERTEK subcontract initiation will be fully assessed and incorporated in the design when applicable.

The parametric three-dimensional modeling of the TFE layout is in progress on a Pro/Engineer CAD workstation. It allows frequent design modifications with ease because most of the TFE dimensions are parametrically connected to a few key dimensions, such as the fuel diameter and the core length. The Pro/Engineer solid model will be used later for thermal and mechanical analysis of the design after the design definition is established.

The reactor subsystem design is in good progress. The reactor design is also being performed with a parametric solid modeling. The vessel structural layout was prepared based on the structural analysis. Vessel component fabrication and joining processes are being established. The reactor vessel fabrication and assembly sequence was also defined.

4.3 TFE

The baseline parameters of the SPACE-R single cell thermionic fuel element (TFE) have been set by the parametric trade study. Table 4.3-1 itemizes SPACE-R baseline TFE characteristics.
### Table 4.3-1 SPACE-R Baseline TFE Characteristics.

<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of Cells per TFE</td>
<td>1</td>
</tr>
<tr>
<td>Cell Length</td>
<td>35 cm</td>
</tr>
<tr>
<td>Emitter Temperature (Avg/Max)</td>
<td>1823/1873 K</td>
</tr>
<tr>
<td>Collector Temperature (avg)</td>
<td>900 K</td>
</tr>
<tr>
<td>Cesium Pressure</td>
<td>1.5 torr (Tcs=566 K)</td>
</tr>
<tr>
<td>Mid-Diode Current Density</td>
<td>2.7 A/cm²</td>
</tr>
<tr>
<td>Axial Thermal Power Ratio</td>
<td>0.7 (End/Midplane)</td>
</tr>
<tr>
<td>Diode Gap (Hot/Cold)</td>
<td>16/20 mils</td>
</tr>
<tr>
<td>Fuel</td>
<td>UO₂</td>
</tr>
<tr>
<td>Smear Density</td>
<td>0.7</td>
</tr>
<tr>
<td>U235 Enrichment</td>
<td>93%</td>
</tr>
<tr>
<td>O.D.</td>
<td>1.8 cm</td>
</tr>
<tr>
<td>Emitter O.D.</td>
<td>2.2 cm</td>
</tr>
<tr>
<td>Thickness</td>
<td>0.2 cm</td>
</tr>
<tr>
<td>Material</td>
<td>Mo-Nb Single Crystal + 5 mil W</td>
</tr>
<tr>
<td>Bare Work Function</td>
<td>4.88 eV</td>
</tr>
<tr>
<td>Collector O.D.</td>
<td>2.57 cm</td>
</tr>
<tr>
<td>Thickness</td>
<td>0.13 cm</td>
</tr>
<tr>
<td>Material</td>
<td>Mo</td>
</tr>
<tr>
<td>Insulator</td>
<td>Plasma-Sprayed Al₂O₃</td>
</tr>
<tr>
<td>He-Bonded to Sheath</td>
<td></td>
</tr>
<tr>
<td>In-Gap Spacer</td>
<td>Sc₂O₃</td>
</tr>
</tbody>
</table>
In design detail the SPACE-R TFE is similar to the TOPAZ II TFE although somewhat larger in diameter and shorter in length. SPACE-R TFE has thicker emitter and collector for higher power output, lower fuel smear density for longer life, and thus flatter axial power distribution resulting in a higher efficiency. A significant difference between the two TFE designs is the closer spacing between TFEs in the SPACE-R reactor core arrangement. Another significant difference is the shorter "stem" portions, that is the portion of the TFE which extends out from the core vessel into the helium plena at each end where connection fittings are located including the TFE metal-ceramic seals, material transitions, thermal expansion compensating bellows and electrical lead connection points.

Table 4.3-2 compares the TFE design parameters. The closer spacing (pitch) between TFEs places constraints on the maximum diameter of the TFE metal-ceramic seals, the sizes of bellows accommodating differential thermal expansions and the configuration of the TFE-to-TFE electrical conductors. All of these impact the design of the portions of the TFE which are located in the helium plena in the reactor design.

Table 4.3-2 Comparison of TFE Parameters

<table>
<thead>
<tr>
<th></th>
<th>TOPAZ II</th>
<th>SPACE-R</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel Diameter</td>
<td>1.73 cm</td>
<td>1.8 cm</td>
</tr>
<tr>
<td>Emitter Outside Diameter</td>
<td>1.96 cm</td>
<td>2.2 cm</td>
</tr>
<tr>
<td>TFE Outside Diameter</td>
<td>2.37 cm</td>
<td>2.57 cm</td>
</tr>
<tr>
<td>Axial Length of Core Zone</td>
<td>37.5 cm</td>
<td>35 cm</td>
</tr>
<tr>
<td>Axial Length of Core Vessel</td>
<td>~ 54 cm</td>
<td>51 cm</td>
</tr>
<tr>
<td>TFE-to-TFE Pitch</td>
<td>4.2 to 4.4 cm</td>
<td>3.45 cm</td>
</tr>
<tr>
<td>Axial Power Distribution Ratio</td>
<td>0.375</td>
<td>0.7</td>
</tr>
<tr>
<td>Avg Fuel Smear Density</td>
<td>0.82</td>
<td>0.7</td>
</tr>
<tr>
<td>Avg Power Output</td>
<td>170 We</td>
<td>320 We</td>
</tr>
<tr>
<td>Avg Thermal Power</td>
<td>3400 Wt</td>
<td>4070 Wt</td>
</tr>
</tbody>
</table>

275
The preliminary TFE design is being performed on a Pro/Engineer workstation which incorporates parametric three-dimensional solid modeling. Four iterations of the TFE design were concluded in the reporting period. Each iteration involved either one or all of the following: technical discussion with potential suppliers or vendors of fabrication services, review by SPI system designers and managers and formal technical design review meetings. This process of design was to have continued culminating with a final design review and preliminary design freeze in accordance with the task schedule.

Figures 4.3-1 to 4.3-3 are illustrations of the TFE design in its status at the end of the reporting period. Shown in the figures are the top and bottom details. The core region consists of concentric emitter and collector tubes. Note that, although there will be differences in TFE-to-TFE electrical connection in various sections of the core, the one design of TFE is used throughout. As illustrated in the figure, the TFEs are identical and interchangeable.

The basic design criterion for the TFE is that the materials and configuration be the same or similar to those proven in the TOPAZ II TFE. Differences exist where the SPACE-R design diverges from that of TOPAZ II and where different technology is being applied.

The electric lead attachments are configured to fit on the SPACE-R TFE as a last step -- after the outgassing of the assembly. This design also permits a high final TFE processing temperature without risk of "spray" from the copper onto the metal-ceramic seals. For the SPACE-R TFE joints are chosen to be automated electron beam or laser welds rather than brazes or TIG welds where a choice exists because complexity and risk are less with automated electron beam or laser welds in small production rates.

The emitter and collector are brazed to transition pieces of Kovar at each end of the core zone (which includes the active core zone and the axial reflector and core coolant plenum zones). Kovar is an alloy of iron containing 29% nickel and 17% cobalt. It was developed for sealing glass and ceramic, although it is not used in the TFE for that purpose. Kovar was the chosen transition material in the TOPAZ II apparently because its thermal expansion is conveniently in-between that of molybdenum and the copper electric lead
Figure 4.3-1  SPACE-R TFE Top Section
Figure 4.3-2 SPACE-R TFE Bottom Section
Figure 4.3-3  SPACE-R TFE Overall Schematic.
material and the stainless steel bellows material. Also, it is sufficiently ductile to accommodate the strains that result from the unavoidable differential thermal expansions, and it can be joined to the other TFE materials by either brazing or welding. However, based upon a recommendation from a vendor of braze services, an alternative alloy of iron with 42% nickel is also being considered. The 42Ni-Fe alloy has a thermal expansion curve that is linear to a higher temperature than Kovar, and hence it is probably a better transition material for the high temperatures that will be experienced by a TFE in fabrication and in service environments. The final decision will be made after the consultation with INERTEK engineers. The INERTEK subcontract is expected to be in place soon.

The emitter will be fabricated from a tube of single crystal Mo-Nb alloy which will be machined by gun-drilling and centerless grinding. The tube will be terminated with a molybdenum plug. The preferred method for the molybdenum-to-molybdenum joint between tube and plug is diffusion bonding, for which parameters have been published in texts. This joint also has been discussed with potential vendors of electron-beam welding services and vacuum brazing services. The choice between joint methods may require some development testing.

The emitter and collector are assembled with the inter-electrode space maintained by polycrystalline scandium oxide bumpers located in recesses in the collector inside wall and cylindrical aluminum oxide spacers at the ends of the core zone. The aluminum oxide spacers align the emitter and collector and maintain the uniformity of the gap under normal conditions. The scandium oxide bumpers are for the dual purposes of restricting lateral resonances in the emitter and collector tubes when subject to launch vibration and limiting local distortion of the emitter into the inter-electrode space during operating life due to emitter creep or thermally induced distortions.

Three potential domestic sources of the scandium oxide material have been located in addition to INERTEK. Quotations are in hand for bumpers machined to final form and tolerances at 99.5%, 99.9% and 99.99% purity and full density. Design criteria for purity will be established later after consultation with Lach Topaz II engineers for the scandium oxide design specification.
The pockets to hold the aluminum oxide spacers and the recesses on the inside of the collector for the scandium oxide bumpers will be made by electric discharge machining (EDM), although the accuracy of the sinking of the recesses for the scandium oxide bumpers remains to be verified. As an alternative, the collector can be made up of sections welded together or brazed with the recesses preformed at the joints. This weld or braze step would be identical to the process step to be developed for the emitter end plug joint.

Four additional subassemblies comprise the TFE. These are four sets made-up of a metal-ceramic seal and bellows. Inner upper and lower metal-ceramic seal and bellows subassemblies close the inter-electrode space between the emitter and collector. An outer lower metal-ceramic seal and bellows subassembly conveys the cesium volume (inter-electrode space) from the core zone through the lower helium plenum of the reactor to the cesium supply plena that are serviced by the multiple central cesium vapor sources. (In a design with a cesium reservoir integral to each TFE this metal-ceramic seal and bellows subassembly is not present and is replaced by an inter-electrode space closure and a TFE alignment feature.) An outer upper metal-ceramic seal and bellows subassembly provides for penetration of the upper helium plenum to allow access to the inside of the emitter from the exterior of the reactor for fuel loading or electric heater insertion.

Each metal-ceramic seal and bellows subassembly utilizes a stainless steel bellows with Kovar end cuffs. The metal-ceramic seals are of two designs. A more compact seal design is used for the two inner subassemblies. Both designs are sapphire ceramics with brazed niobium skirts utilizing a palladium alloy braze.

Copper electric current leads are attached as a last step in the assembly by welding to either Kovar or molybdenum attachment points at four points on the emitter and collector exterior.

Table 4.3-3 is a summary of the presently conceived priority listing of fabrication processes. Approximately two dozen material suppliers or vendors of fabrication services have been contacted concerning the development engineering of the SPACE-R TFE,
and all expressed willingness to assist in building test specimens as well as the ultimate TFE components.

Table 4.3-3 Priority List of Fabrication Processes

<table>
<thead>
<tr>
<th></th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.</td>
<td>Scandium oxide bumper pocket in collector</td>
</tr>
<tr>
<td>2.</td>
<td>Emitter tube to plug bond, weld or braze</td>
</tr>
<tr>
<td>3.</td>
<td>Metal-ceramic seal braze</td>
</tr>
<tr>
<td>4.</td>
<td>Cu to Mo and Cu to Kovar lead welds</td>
</tr>
<tr>
<td>5.</td>
<td>Kovar to Nb seal skirt electron beam weld</td>
</tr>
<tr>
<td>6.</td>
<td>Kovar to Mo braze (emitter and collector tube)</td>
</tr>
<tr>
<td>7.</td>
<td>Scandium oxide bumper fabrication</td>
</tr>
<tr>
<td>8.</td>
<td>Stainless steel bellows to Kovar cuff braze, electron beam or laser weld</td>
</tr>
<tr>
<td>9.</td>
<td>Kovar to Kovar closure welds (TIG and laser)</td>
</tr>
<tr>
<td>10.</td>
<td>Collector inside diameter straightness</td>
</tr>
<tr>
<td>11.</td>
<td>Deposition of oriented W on 24 inch Mo tube</td>
</tr>
<tr>
<td>12.</td>
<td>Weld leads to attachment on TFE (Cu to Cu)</td>
</tr>
</tbody>
</table>

The TFE design will continue to be subject to modification as detailed calculations are made. The calculations will verify electrical lead resistances and thermal boundary conditions on the active zone. These calculations will assure that the system performance criteria are met. Other calculations will determine stresses in fabrication and in operation. Bellows deformation and other mechanical conditions will be assessed.

Additional work ahead includes the following: detailed designs of the TFE internals, the TFE-to-TFE electrical connector and its attachment, TFE performance analysis during startup and shutdown as well as normal operation, and the TFE axial mechanical support.

4.4 Reactor Subsystem

Conceptual design of the reactor subsystem was initiated during the reporting period. The reactor vessel, along with its internal and external supports, forms the main load bearing structure for the SPACE-R reactor. In addition to carrying the core loads,
vessel must also contain the NaK coolant. Parametric trade study results were used to insure both the structural integrity of the vessel and the chemical compatibility of the vessel material with the NaK coolant at operating conditions. Design considerations include mass, life, cost, strength, manufacturability and weldability characteristics. The fabrication and welding issues have also been integrated into the design process and selection criteria.

The baseline reactor vessel consists of two thin concentric cylinders in the barrel section and also dual top and bottom plates. The calandria structure is attached to the inner cylinder and provides the moderator structural support and forms inner wall of the NaK annulus (return from the radiator). The design of the calandria tubes which surround each TFE, is to minimize their impact on core neutronics. As a result, the buckling strength, minimum thickness for weldability, creep strength and chemical degradation effects have been investigated.

A preliminary thermal hydraulics analysis was performed to study the potential for flow instability in the reactor core NaK channels. Using stability criteria developed for common liquids, a preliminary assessment suggests that there is little potential for flow instability to occur in the reactor at the design operating condition. The calculated safety margin to onset of instability is about 100% which suggests that the coolant channel flow rate can be reduced by 50% at the design operating condition before a flow instability becomes an issue in the core.

The vessel and calandria-sheath tube were sized under normal operating conditions. The calandria is constructed from 150 right circular tubes. The calandria tubes carry the differential pressure loads between the NaK and the helium. In addition, they carry an axial load resulting from the differential pressure of the NaK and helium because they are welded to both endplates. In these calculations, it is assumed that each tube will carry an equal amount of this axial load since the endplates are relatively thin. In reality, each tube will carry an amount proportional to the distance from the center of the vessel, and the vessel itself will support a portion. Probably the easiest method to determine the actual distribution of the axial load is using a 3 dimensional symmetric model that includes the tubes and endplate. This analysis will be performed in the future when design iterations subside. Because the tube is operating at a higher average temperature than the vessel, a
compressive axial load due to the differential thermal expansion is also present. This loading is conservatively estimated and evaluated.

Using the material and factor of safety criteria, the limiting condition for the calandria tube is hoop collapse/buckling. The calculated critical thickness for buckling with a factor of safety of 2 is about 10 mils. The analysis assumes no support from the molybdenum collector. Since these two parts are essentially in contact, it may be desirable to make the tubes flexible so that intimate contact can be achieved. The final thickness selection will therefore be based upon longterm corrosion and welding considerations.

The reactor pressure boundary consists of a dual cylindrical vessel arrangement so that the upflow of the NaK is accommodated in the annulus. The design results in the outer vessel subjected to an internal pressure of 22 psi. The inner vessel has a compressive delta pressure load (albeit small) due to the pressure drop in the NaK. For purposes of conservative design, this compressive pressure is assumed to be 5 psi. The axial loads are relatively small due to the small exposed areas of the NaK. For this reason, the vessel walls were sized by the hoop stress conditions.

Fabrication and assembly process of the reactor subsystem is being defined. Fabrication of the SPACE-R reactor will make full use of current state-of-the-art manufacturing techniques in order to minimize the development risk and cost to the program. The components as described will likely utilize spin-forming/superplastic forming, chemical milling, laser welding as well as other standard machining techniques.

Three-dimensional parametric solid modeling of the reactor was initiated to form a basis for the thermal/structural analysis and assembly sequence definition. The assembly of the SPACE-R reactor is depicted in the following figures. The baseline assembly sequence begins with the calandria tubes. This component consists of a 0.02 inch thick wall tube which is fluted to form a standoff between itself and the moderator tricusp. In this void, the NaK coolant flows up through the core. One approach for producing the calandria tubes with the fluted standoffs built-in is to use chemical milling. In this operation, the fluted standoffs would be masked off so that the acid bath does not remove material from this region. The acid bath is then used to very precisely reduce the wall thickness to the
required thickness. The calandria tubes are sized by minimum material wall thickness for the welding operation since the wall thickness required for adequate buckling strength is on the order of 5 to 6 mils for all the candidate materials. At 20-mil wall thickness, the calandria tubes have a relatively large margin of safety including the estimated degradation due to chemical incompatibilities.

The upper and lower helium plenums, and the inner reactor vessel, Figure 4.4-1 can be formed using the spin forming process. This manufacturing process is baselined over other process methods due to its inherent simplicity and increased reliability due to the reduction of weld joints. Once formed, the holes required for the NaK and the calandria tubes can be machined into the bottom surface.

The outer reactor vessel, Figure 4.4-2 utilizes a different manufacturing technique compared to other components due to its design complexity. Superplastic forming is suggested because it allows the outer reactor vessel to be manufactured from a single billet with welded joints of any kind.

The base plate will be machined from stock plate material. It is designed to have standoffs machined into it in order to form a plenum for the NaK and to provide provisions to attach the inner and outer pressure vessel.

The baseline 150 calandria tubes must be precisely located, most likely with hard tooling and will be laser welded to the inner vessel on the bottom face. Upon completion, the inner vessel will be sealed and pressurized with helium so that helium leak checks can be performed. This assembly operation is shown in Figure 4.4-3.

Figure 4.4-4 depicts the installation of the tricusp moderator elements and expansion adaptor into the inner vessel. Hard tooling will still be required to keep the 150 calandria tubes properly positioned and to prevent the calandria tubes from bowing or distorting which could cause stresses in the welds.
Figure 4.4-1 Inner Reactor Vessel

BASELINE CONFIGURATION
Manufacturing Technique: Spin form
Material: 316L
Thickness: 0.070 inches
BASELINE OUTER PRESSURE VESSEL
- Manufacturing Technique: Spin form/Super Plastic Form
- Material: 316L
- Thickness: .08 inches

Figure 4.4-2 Outer Reactor Vessel
Figure 4.4-3  Installation of Calandria/Sheath Tubes into Inner Reactor Vessel
Figure 4.4-4 Installation of Moderator Elements into Inner Reactor Vessel
Figure 4.4-5 Integrated Upper Helium Chamber to Inner Pressure Vessel
Integration of the upper helium plenum to the inner vessel subassembly is shown in Figure 4.4-5. In this operation, the 150 calandria tubes will be properly positioned and laser welded. After the calandria tubes are welded to the upper helium plenum, the closure plate is laser welded through a butt joint. Each individual weld can be inspected with dye penetrants and leak checked using helium sniffing equipment.

Figure 4.4-6 shows the integration of the inner vessel assembly to the outer vessel. This subassembly can also be inspected using dye penetrants and perhaps other non-destructive techniques.

Figure 4.4-7 shows the integration of the lower vessel subassembly to the upper subassembly. Hard tooling will again provide proper alignment. The two subassemblies will be laser welded with the interface forming the NaK plenum. Inspection techniques follow.

Upon completion of the vessel assembly, 150 TFE’s will be installed into the calandria tubes. The electrical interconnections can be made and the unit is then nearly ready for system qualification tests if needed. The lower closure plate(s) with 150 holes can then be laser welded to the vessel and to the TFE end fittings. Weld rings will be installed over the TFE’s and welded in place. The fission gases are vented through the upper vessel plate(s).

The reactor assembly is complete and can subsequently be mated to the reflector, control and safety drums and ultimately the balance of plant.
Figure 4.4-6 Upper Vessel Integrated
Figure 4.4-7 Integrated Vessel
REFERENCES


Technical Progress Report Distribution List

A. U.S. Department of Energy
San Francisco Operations Office
1301 Clay Street, Room 700N
Oakland, CA 94612-5208
Attn: Gary Peterson
Nuclear & Energy Programs Division

B. U.S. Department of Energy
San Francisco Operations Office
1301 Clay Street, Room 700N
Oakland, CA 94612-5208
Attn: Ronna Promani
Contracts Management Division

C. U.S. Department of Energy
Office of Defense Energy Projects, NE-52
19901 Germantown Road
Germantown, MD 20984
Attn: Colette Brown

D. U.S. Department of Energy
Office of Defense Energy Projects, NE-52
19901 Germantown Road
Germantown, MD 20874
Attn: Wade Carroll

G. Strategic Defense Initiative
Organization/TNK
The Pentagon
Washington, D.C. 20301-7100
Attn: Fred Tarantino

H. Air Force Phillips Laboratory
Kirtland AFB/STPP
Albuquerque, NM 87117
ATTN: Capt. J. Fisher

I. U.S. Department of Energy
Office of Patent Counsel
P.O. Box 808
Livermore, CA 94550

J. U.S. Department of Energy
Office of Scientific and Technical Information
Technical Information Center
P.O. Box 62
Oak Ridge, TN 37831

O. see attached for additional distribution of technical reports
Other Addressees for Technical Reports

<table>
<thead>
<tr>
<th>No.</th>
<th>Name</th>
<th>Address</th>
<th>Attn:</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.</td>
<td>Los Alamos National Laboratory</td>
<td>P.O. Box 1663, Los Alamos, NM 87545</td>
<td>Michael Houts</td>
</tr>
<tr>
<td>2.</td>
<td>Los Alamos National Laboratory</td>
<td>P.O. Box 1663, Los Alamos, NM 87545</td>
<td>Will Ranken</td>
</tr>
<tr>
<td>3.</td>
<td>Sandia National Laboratory</td>
<td>P.O. Box 5800, Albuquerque, NM 87185</td>
<td>James H. Lee</td>
</tr>
<tr>
<td>4.</td>
<td>Sandia National Laboratory</td>
<td>P.O. Box 5800, Albuquerque, NM 87185</td>
<td>Don Gallup</td>
</tr>
<tr>
<td>5.</td>
<td>U.S. Department of Energy</td>
<td>19901 Germantown Road, Germantown, MD 20874</td>
<td>Cpt. Randy Wharton, NE-52</td>
</tr>
<tr>
<td>8.</td>
<td>Jet Propulsion Laboratory</td>
<td>4800 Oak Grove Drive, Pasadena, CA 91109</td>
<td>Jan Vandersande</td>
</tr>
<tr>
<td>9.</td>
<td>NUS Corporation</td>
<td>910 Clopper Road, Gaithersburg, MD 20878</td>
<td>Don Beard (c/o Abe Weitzberg)</td>
</tr>
<tr>
<td>10.</td>
<td>NUS Corporation</td>
<td>910 Clopper Road, Gaithersberg, MD 20878</td>
<td>Len Topper (c/o Abe Weitzberg)</td>
</tr>
<tr>
<td>12.</td>
<td>NASA Lewis Research Center</td>
<td>2100 Brookpark Road, Cleveland, OH 44135</td>
<td>Joe Sovie, MS 301-3</td>
</tr>
<tr>
<td>13.</td>
<td>Wright Laboratories (WRDC/POO2)</td>
<td>Thermal Systems Group, Wright Patterson AFB, OH 45435</td>
<td>Tom Mahesky</td>
</tr>
<tr>
<td>14.</td>
<td>Wright Laboratories (WRDC/POO2)</td>
<td>Thermal Systems Group, Wright Patterson AFB, OH 45435</td>
<td>Tom Lamp</td>
</tr>
<tr>
<td>15.</td>
<td>Bettis Atomic Power Laboratory</td>
<td>P.O. Box 79, West Mifflin, PA 15122-0079</td>
<td>Mr. S. White</td>
</tr>
<tr>
<td>16.</td>
<td>Mr. J. Mosquera</td>
<td>NR-60, NC2 Building, 2521 Jefferson Davis Hwy, Arlington, VA 22202</td>
<td></td>
</tr>
</tbody>
</table>
17. Air Force Phillips Laboratory
Kirtland AFB/STPP
Albuquerque, NM 87117
Attn: Lt. Col. Ernie Herrera

18. Strategic Defense Initiative
Organization/TNK
The Pentagon
Washington, D.C. 20301-7100
Attn: Dan Mulder

19. Mr. Al Schock
Fairchild

Internal Distribution

Edward Britt
Monte Davis
Joseph Dodson
Norm Gunther
Kent Koester
Hyop Rhee
Joseph Wetch
See-pok Wong
Dan Nordeen
Peter Oleson
Cinian Zheng
Dave Knittle
Gauri Das
Jon Shapiro
Hugh Denham
Raymond Lin
File

Bruce Minners, Space Systems Loral
John Rosenfeld, Thermacore
Kurt Westerman, Babcock & Wilcox
Sam Bhattacharayya, Argonne National Laboratory
DATE
FILMED
11/16/93
END