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SNAP 8 REACTOR
PRELIMINARY DESIGN SUMMARY
(Title Unclassified)

AEC Research and Development Report

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SNAP 8 NUCLEAR SYSTEM

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ABSTRACT

The purpose of this report is to describe the present concept of the SNAP 8 reactor, reactor control system, safety systems, and shield and to outline the reasoning behind the design point selection. The power conversion system, which is being developed by National Aeronautics and Space Administration, will only be covered briefly.

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I. INTRODUCTION

A. SNAP 8 SYSTEM (Figure 1)

SNAP 8 is a nuclear-powered electrical generating system intended to produce 30 to 60 kwe net continuously for 10,000 hours for use in space. It utilizes a compact Zr-10 wt % U (fully enriched) hydride reactor with beryllium reflector elements for control. NaK-78 is used to cool the reactor; it enters the reactor at 1100°F and exits at 1300°F, then passes through a countercurrent mercury boiler and back to the reactor inlet. The boiler produces a superheated mercury vapor at 1200°F and 275 psia which passes through a turbine (which drives the alternator) exhausting at 20 psia and 706°F. The turbine exhausts to a radiator-condenser which returns 560°F subcooled mercury to the boiler. The power conversion system is being developed for 30 kwe. For 60 kwe operation two power conversion systems would be fed by the single reactor. The reactor is designed to produce 600 kw of thermal power to meet the heat source requirements for 60 kwe operation.

The total weight objectives for the complete SNAP 8 system are ~1800 lb for the 30 kwe system and 3000 lb for the 60 kwe system.

B. REACTOR PERFORMANCE SPECIFICATIONS

In order to meet system requirements the reactor is designed for the following specifications.

1. Power and Temperature

The reactor produces 1300°F outlet NaK-78 at 600 kw power with an inlet coolant temperature of 1100°F. The power and temperature remain essentially constant over the life of the reactor so that the reactor is not required to follow wide changes in load.

2. Operating Life

The reactor is designed for an operating life of 10,000 hours.

3. Orbital Startup

The reactor is to be started in orbit on command from the ground. The reactor is to be launched subcritical and cold.

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Figure 1. SNAP 8 System Cycle Diagram

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4. Radiation Dose to Payload

Sufficient shielding will be provided to reduce the neutron and gamma dose emanating directly from the nuclear system, or scattered by the nuclear system, to 10^{13} neutrons/cm² and 10^7 roentgen respectively over a 3-ft diameter circle which is 15 ft from the center of the reactor and on the reactor axis.

C. DESIGN SUMMARY

1. Nuclear System

The heat source for SNAP 8 is a compact nuclear reactor cooled with NaK and utilizing a Zr-U hydride fuel-moderator with movable beryllium reflectors for control (see frontispiece). The reactor is a direct extension of the technology developed for SNAP 2. The total package, including the LiH shield, weighs ~ 900 lb (component weights are summarized in Table I) and occupies a space 4-1/2 ft long with a maximum diameter of ~ 2-1/2 ft. A layout of the system is shown in Figure 2 and a cross section of the reactor core in Figure 3. System characteristics are summarized in Table II.

2. Fuel Elements

The fuel and moderator are contained in rods of Zr-10 wt % U (fully enriched) alloy hydrided to hydrogen content of 6.0×10^{22} atoms/cc. These rods are 0.532-in. in diameter and 14.25 in. long. The rods are shrink-fitted into tubes of Hastelloy N with 0.540 in. ID, 0.010-in. wall, and 14.75 in. long (see Figure 4). The tubes will be coated with Solaramic S14-35A bearing samarium oxide. End plugs are then welded and the coating blended over the weld.

3. Core Assembly

The 211 fuel elements are assembled into a 9-3/16-in. diameter core vessel of type 316 stainless steel. The elements rest on a 3/8-in. thick grid plate of Hastelloy C and are held down by a 11/16-in. upper grid plate of type 316 stainless steel. The space between the elements and the vessel is filled with internal reflectors of Hastelloy N clad BeO.

4. Reflector Control System

The 3-in. thick beryllium reflector is formed of four large rotating control elements and two small fixed filler pieces (see Figure 3). At launch

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NUCLEAR SYSTEM WEIGHT SUMMARY

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Figure 2. SNAP 8 Reactor and Shield

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Figure 3. SNAP 8 Reactor Core - Section

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TABLE II

SNAP 8 NUCLEAR SYSTEM CHARACTERISTICS

Operating Conditions

Reactor thermal power	600 or 300 kw
Net electrical power	60 or 30 kw
Outlet coolant temperature	1300°F
Inlet coolant temperature	1100°F
Operating pressure	25 psia
Operating life	10,000 hours
Maximum fuel temperature (600 kw)	1456°F
Maximum cladding temperature (600 kw)	1405°F
Maximum fuel thermal stress/ultimate tensile strength	12,000 psi/25,000 psi

Reactor Design

Fuel elements

Number	211
Diameter	0.560 in.
Length	14-1/4 in.
Fuel	Zr-10 wt % U $N_H = 6 \times 10^{22}$ atom/cc
Cladding	10 mil Hastelloy N
Spacing between elements	10 mil

Internal reflectors

18 Hastelloy N clad BeO fillers

Lower grid plate

3/8 in. Hastelloy C

Upper grid plate

11/32 in. 316 stainless steel

Core vessel

Internal diameter	9-3/16 in.
Length	20 in.
Thickness	1/16 in. 316 stainless steel

Reflector control elements

Number	4
Material	Beryllium
Thickness	3 in.

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TABLE II (Continued)

Nuclear Parameters

Average thermal flux	10^{13} neutrons/cm ² -sec
Mean fission energy	0.19 ev
Average isothermal temperature coefficient (70 to 1300°F)	-0.3¢/°F
Uranium loading	7 kg (fully enriched)
Mean prompt neutron lifetime	6 microseconds
Burnable poison	9.8 gms natural Sm ₂ O ₃ incorporated in coating
Worth of burnable poison	3.7% $\delta k/k$
Initial cold excess reactivity	5.9% $\delta k/k$
Total reactivity losses	9.6% $\delta k/k$
Xenon	1.5
Samarium	0.9
Temperature and power	3.2
Hydrogen loss	1.5
Burnup	0.5
Other fission product poisons	0.7
Contingency for fuel element failures	1.3
Available control (4 elements)	18.2% $\delta k/k$

Shield Parameters

Shield material	LiH
Thermal shield	4 in.
Overall shield dimensions (frustrum of cone, approximate)	
Maximum diameter	24 in.
Minimum diameter	19 in.
Thickness	25 in.
Cladding	1/8 in. 316 stainless steel
Neutron dose to payload (10,000 hr)	10^{13} nvt
Gamma dose to payload (10,000 hr)	10^7 r

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Figure 4. SNAP 8 Fuel Rod

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Figure 5. S8ER Schematic

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The support frame for the reflector assembly will be made in two halves so that the reflector can be removed to simplify ground handling procedures, and so that under scram conditions the two halves spring apart completely separating from the reactor. Scram may be actuated by a telemetered signal, re-entry heating, or reactor cool-off (end of life shutdown).

5. Shield

The shield design has not yet been fixed. One concept being considered resembles a frustum of a right circular cone 19 in. in diameter at the upper end and 24 in. in diameter at the lower end. The height of the frustum is ~ 20 in. The shielding materials are enclosed in a stainless steel (type 316) container (of the geometry described above) with 1/8-in. thick walls. The portion of the shield adjacent to the reactor consists of 4 in. of hydride which is separated from the remainder of the shield, composed of LiH, by a thin layer of thermal insulation.

D. SNAP 8 DEVELOPMENT PROGRAM

1. Development Responsibilities

SNAP 8 is being developed jointly by the National Aeronautics and Space Administration and the Atomic Energy Commission. The AEC is responsible for the development of the reactor system and NASA for the complete system. Atomics International is developing the reactor system as prime contractor of the AEC and will operate nuclear system tests and test facilities for the SNAP 8 program.

2. SNAP 8 Experimental Reactor (Figure 5)

The first major milestone of the nuclear development program is the SNAP 8 Experimental Reactor (S8ER) which is scheduled to begin operation in April 1962. The S8ER will be an engineering test of a prototype reactor core with a simple heat dump to remove the reactor power. Although the reactor core will be a prototype, the reactor control system, safety systems, and shielding will not be flight-developed hardware.

Basic reactor data will be obtained from this test to aid in the development of a prototype control system for the second reactor, the SNAP 8 Development System (S8DS) or Ground Prototype System.

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3. SNAP 8 Ground Prototype System (Figures 6 and 7)

This system, scheduled to begin operation in July 1963, is a breadboard test of prototype components and the first test of the nuclear electrical generating system. This system will include a preflight-qualified reactor, control system, and shield. Automatic startup of the complete system will be demonstrated as well as 90 days life.

4. SNAP 8 Flight Prototype System

The SNAP 8 Flight Prototype System, scheduled to be tested in July 1964, will be a complete SNAP 8 system in its vehicle configuration designed for flight. The full-scale test of this system will constitute a proof test for the flight system which will be essentially identical.

5. SNAP 8 Flight Test

The launch of a SNAP 8 system into orbit is scheduled for the Spring of 1965. This shot will be for the purpose of testing SNAP 8 in the space environment and probably will be associated with an electrical propulsion flight test mission.

Figure 6. SNAP 8 Ground Prototype System, Test Facility

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PLAN

SECTION

SECTION A-A

SECTION B-B

Figure 7. SNAP 8 Ground Prototype System Test Facility

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II. REACTOR MATERIALS

A. FUEL-MODERATOR

1. Fuel Selection

The selection of Zr-10 wt % U alloy hydride as a homogeneous fuel-moderator for SNAP 8 was based on the successful performance of this material in the SNAP 2 Experimental Reactor (SER) and on data obtained in the SNAP 2 program. While the SNAP 8 conditions will impose higher temperatures and greater burnup on the fuel, the experience and knowledge gained through fuel development and evaluation on the SNAP 2 program enables the prediction of satisfactory performance under SNAP 8 requirements.

2. Phases

The fuel is a Zr-10 wt % U alloy which has been massively hydrided to an N_H 6×10^{22} atoms H/cc of alloy. X-ray diffraction, metallography, and thermodynamic studies have shown that the fuel rods consist of a mechanical mixture of zirconium hydride and uranium. The uranium is very finely and homogeneously dispersed throughout the zirconium hydride matrix as shown in Figure 8. (The uranium particles are the small black grains). The uranium particles tend to inhibit grain growth of the zirconium hydride and thereby improve the strength of the material. The other physical properties are essentially unaltered by the presence of uranium.

Figure 8. Microstructure of SNAP 8 Fuel (250X)

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Since uranium does not enter into chemical combination with the hydride, the zirconium-hydrogen phase diagram can be used to describe the phase equilibria of the SNAP 8 fuel. In Figure 9, the shaded area describes the compositions and temperatures under consideration. X-ray diffraction work has shown that two hydride phases co-exist at room temperature.¹ The delta-hydride phase is face-centered cubic, $a_0 = 4.78 \text{ \AA}$. The higher composition phase, epsilon, is face-centered-tetragonal $a_0 = 4.88$, $c_0 = 4.60$ and is formed by a martensitic shear of the cubic delta phase. The epsilon phase contributes the lamellar microstructure in Figure 8. From thermodynamic studies this hydride, which is two-phase at room temperature, appears to be single phase at reactor operating temperatures. However, thermal cycling tests have shown that if a phase transformation does occur, it does not adversely affect the mechanical stability of the fuel.

3. Physical Properties

The average thermal expansion of the fuel material has been found to be $6.8 \times 10^{-6}/^\circ\text{F}$ between room temperature and 1500°F .²

The thermal conductivity of the hydride fuel up to 1200°F was determined to be $13 \pm 1 \text{ Btu/hr-ft-}^\circ\text{F}$ on eight separate samples.³ Both electron motion and lattice oscillation are believed to contribute toward the good thermal conductivity of this material.

To prevent hydrogen loss at elevated temperatures, the fuel elements are completely enclosed in cladding. In such a closed system, hydrogen can redistribute under a thermal gradient. In a recent test on epsilon zirconium hydride (1.82 H/Zr atom ratio) extensive hydrogen redistribution occurred under a gradient of 1300° to 1000°F . After 1300 hours under these conditions, the hydride composition in the 1300°F zone was 1.75 H/Zr, while that of 1000°F was 1.92 H/Zr. In spite of this composition gradient, no damage to the hydride rod occurred.

Pressure-temperature-composition relationships of the hydride fuel have been thoroughly studied.¹ Uranium was found to have no effect, other than to act as a composition diluent, on the PCT relationships. The dissociation pressure-temperature curves (isochores) are given in Figure 10. The SNAP 8 hydrogen compositions range from 1.65 H/Zr to 1.75 H/Zr.

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Figure 9. Phase Diagram for the Zirconium-Hydrogen System

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Figure 10. Hydrogen Dissociation Pressures of Zr-10 U Alloy Hydrides

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The mechanical properties of the fuel are summarized in Table III. In general, the strength of the hydride increases with temperature. The effect of hydrogen on the alloy is to increase its creep resistance and the UTS at elevated temperatures. The tensile strength of the hydride is several times that of the unhydrided material at 1200°F.

TABLE III
MECHANICAL PROPERTIES OF SNAP 8 FUEL

	75°F	500°F	1000°F	1200°F
Rupture strength, psi x 10 ⁻³	17	14	32	27
Young's Modulus, psi x 10 ⁻⁶	13.6	12.5	11.7	11.3

4. Compatibility

The compatibility of the fuel with the ceramic hydrogen barrier on the fuel element cladding has been found to be excellent at 1500°F.⁴ Should the ceramic become chipped to the extent that there is intimate contact between fuel and cladding, a compatibility problem might exist. In such a case, the uranium particles on the surface of the hydride might form a low melting eutectic with the components of the cladding. Studies are under way to determine the extent of such a reaction.

5. Burnup

The Zr-10 wt % U hydride with N_H of 6 is now being irradiated to the design burnup of 0.32 metal at. %. In previous experiments the same alloy has been exposed to 0.03 metal at. % burnup at 1300°F⁵ and a lower hydride alloy (N_H of 4) has received 0.75 metal at. % burnup at 950°F. In both cases, the material did not show any deterioration and the dimensional stability was excellent. A test is now in progress to give a burnup of 0.4 metal at. % at 1300° and 1500°F (Figure 11) and a subsequent test will follow for 1.0 metal at. % under the same conditions.

B. EVALUATION OF COATINGS

It is necessary that the SNAP 8 cladding be coated with some hydrogen diffusion barrier. It can be seen in Figure 12 that the hydrogen leak rate through

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Figure 11. X-ray Photograph of In-Pile Fuel Irradiation Capsule

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Figure 12. Comparison of Hydrogen Permeation Rate Through Solaramic and Metals

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a metallic cladding is many times in excess of the maximum allowable hydrogen loss. An investigation has been made of the performance of the coating used in SNAP 2, Solaramic S14-35A, to determine if it is satisfactory for the higher temperature application to SNAP 8.

1. Effects of Long-Term Heating on the Hydrogen Permeation Rate

A tubular type 347 stainless steel membrane (membrane I) was coated with three layers (3.6 mils) of Solaramic S14-35A enamel. The enamel firing took place in a static air atmosphere at 1750°F. The enamel was then exposed to 1 atmosphere of hydrogen at $1400 \pm 5^\circ\text{F}$ for 1000 hours. The rate at which hydrogen permeated through this enamel during the 1000 hour period was continuously monitored with a mass spectrometer (insert, Figure 13). The pressure on the "downstream" or metallic face of membrane I was maintained at less than 0.01 microns of Hg during the entire period. The results of this test are presented in Figure 13. The error associated with any single measurement is estimated to be $\pm 15\%$. Initially, the rate rose to $\sim 4.0 \times 10^{-3} \text{ cc/hr/cm}^2$. After 100 hours of exposure the rate had decreased to 1.4×10^{-3} . After this, the rate continued to decrease in an almost linear manner with time.

A permeation rate of $1.5 \times 10^{-3} \text{ cc/hr-cm}^2$ at 1400°F would yield a 1% loss of hydrogen in 10,000 hours at reactor design point conditions. Up to about 5% loss would be tolerable. This corresponds to $7.5 \times 10^{-3} \text{ cc/hr-cm}^2$.

Upon completion of the 1400°F -1000 hour Solaramic-hydrogen durability-permeation test, the membrane cap was removed and the internal enamel coating was examined. Visually, the coating appeared to have lost its glaze and furthermore, was extremely pitted and erupted. In fact, when the membrane tube was inverted, coating flakes fell out. Nevertheless the membrane performed satisfactorily when it was resealed (i. e., the cap was rewelded) and again heated to 1410°F and internally pressurized with 1 atmosphere of hydrogen. This permeation test lasted for 120 hours. The membrane was then cycled to room temperature and reheated to 1400°F for a second time. The results of these tests are summarized in Table IV.

After the thermal cycle tests were completed, membrane I was heated in an induction coil to 1400°F and air quenched to room temperature. The membrane was then reinstalled in the permeation tester and heated to 1410°F . The

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Figure 13. Permeation Rate Through Solaramic Coated 347 SS (Membranes I and II) and Hastelloy N

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initial permeation rate with 1 atmosphere of hydrogen was 12×10^{-3} cc(STP)/cm²-hr. One hundred hours later this rate had dropped to 1.0×10^{-3} cc(STP)/cm²-hr. The membrane was then heated to 1500°F and internally pressurized with 1 atmosphere of hydrogen. The initial leak rate was 1.4×10^{-3} decaying to 0.9×10^{-3} cc(STP)/cm²-hr after about 100 hours. Two other membranes were tested, another stainless steel membrane (membrane II) and a Hastelloy N membrane. The second stainless steel membrane was tested at 1500°F and behaved in a similar manner to the first giving a leak rate about twice that at 1400°F. The Hastelloy N membrane was superior to stainless steel at 1400°F giving a leak rate of only 0.5×10^{-3} at 500 hours.

TABLE IV
EFFECT OF THERMAL CYCLING ON THE
HYDROGEN PERMEATION RATE OF SOLARAMIC S14-35A
AT 1410°F (MEMBRANE I)

Test Conditions	Permeation Rate cc(STP)/cm ² -hr
1) At the conclusion of the 1000 hr test before first thermal cycle	0.6×10^{-3}
2) Second through fifth hr of permeation test after first cycle	1.0×10^{-3}
120th hr of permeation test after first cycle	0.8×10^{-3}
3) Third through fifth hr of permeation test after second cycle	1.27×10^{-3}
23rd hr of permeation test after second cycle	1.0×10^{-3}

2. Pressure Dependence

In the time period 1400 to 1500 hours the pressure dependence of the leak rate was measured at various temperatures on membrane I. Results are given in Figure 14.

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Figure 14. Pressure Dependence of Permeation Rate

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3. Visual Inspection and Conclusions

Upon completion of the pressure dependence tests, membrane I was cooled to room temperature and sectioned for metallographic examination. A photograph of the as-aged coating is shown in Figure 15. Figure 16a is a photomicrograph of a typical section of the enameled membrane after the experiment is over. For the sake of comparison, a photomicrograph of a section of an enameled membrane in the as-fired condition is presented (Figure 16b). Observation of Figures 15 and 16a reveals the broken and nonuniform nature of the coating after testing. It is believed that these bubbles and surface eruptions are caused by water vapor formed by the hydrogen reduction of oxides in the enamel (i. e., ZnO and K_2O). Work is presently underway to eliminate these unstable components from the enamel.

It can be concluded from this work that while the Solaramic S14-35A coating does suffer physical damage at temperatures of 1400°F and over, its value as a hydrogen barrier is not seriously degraded. Despite the application of severe thermal shocks and temperatures exceeding those to be encountered in SNAP 8, the hydrogen permeability of the coating remains within acceptable limits.

Figure 15. Membrane I After 1500 Hours of Testing

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Figure 16a. Photomicrograph (300X) of Membrane I
After 1500 Hours of Testing

Figure 16b. Photomicrograph (300X) of Membrane I Before Testing

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C. COATING FORMULATION

A more severe ceramic coating performance requirement is necessary for SNAP 8 reactors due to increased operating temperatures and pressures over SNAP 2. Although the Solaramic coating presently being used in SNAP 2 reactors appears adequate as a hydrogen barrier in the temperature range of 1400° to 1500°F, the coating can be improved if volatilization and deterioration of the coating can be eliminated.

Solaramic S14-35A (SNAP 2 coating) contains many metal oxides which are easily reduced. Oxides such as ZnO , K_2O and Na_2O are reduced at elevated temperatures by hydrogen and the subsequent volatilization of the reaction products (H_2O , Zn, K and Na) can form bubbles in the coating that erupt at the surface due to the fluid nature of the coating at elevated temperatures.

As a result, new coatings have been formulated which are relatively free of volatile components and have a higher softening temperature to deter the formation of bubbles. It is felt that such a coating will be more stable at SNAP 8 operating conditions than the present SNAP 2 coating.

These new ceramic coatings differ from Solaramic S14-35A in that they are free of ZnO , Na_2O and K_2O and fire at 1900° to 2000°F instead of 1760°F. The reduction of expansion of the frit caused by the removal of Na_2O and K_2O and the increase in smelting temperature caused by this modification are compensated for by increasing the Li_2O content. It is believed that Li_2O is not easily reduced by hydrogen because of its high negative free energy of formation.

The coefficient of thermal expansion of several formulations is presently being measured at the University of Illinois by the Interferometer Method. Several methods of adjusting the coefficient of expansion of high-temperature coatings have been obtained from the North American Aviation, Columbus Division. These methods consist of additives to the frit in the milling operation. Nickel powder is reported to be a good material for increasing the thermal expansion of the glass.

Another approach being followed in the SNAP 8 coating program utilizes controlled crystallization as a means of producing a stable, high-temperature hydrogen barrier. An apparatus to study the nucleation and crystallization of ceramic materials has been constructed at AI for use in developing a uniformly crystallized ceramic coating for possible use in SNAP 8 reactors.

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D. FUEL ELEMENT ASSEMBLY

1. Shrink Fitting

Because of the necessity for good heat transfer from the fuel to the coolant (see Section IV), the SNAP 8 cladding is shrink fit upon the fuel rod. To maintain a thermal bond at 1400°F, the room temperature fuel rod diameter must slightly exceed the inside diameter of the ceramic coated cladding tube. To assemble the fuel element a shrink fitting apparatus (Figure 17), which consists of a filament furnace, a fuel rod cradle and a powder actuated tool (RAMSET), is employed. The cladding tube is heated to 600°F to provide a clearance between the tube and the fuel rod (which is at room temperature). The RAMSET provides the kinetic energy required to drive the fuel rod into the cladding. Rim fire 22 caliber wads are exploded against a 4130 air hardened steel firing bar which transmits driving energy to the fuel rod. A coil spring placed at the far end of the cladding tube controls the distance to which the fuel rod penetrates.

The fuel rod is centerless ground to an accuracy of ± 0.0001 on the diameter. The same accuracy is produced on the internally coated tube as a result of several straightening and honing operations. The tubing straightness is held within 0.002 in. over its entire length.

Assembly of a number of test samples has shown that this process can be accomplished without damaging the ceramic coating. Figure 17a is a photomicrograph (500X) of a cross section of a shrink fit rod.

2. End Blending

Following the shrink fitting operating, fuel element assembly is completed by inserting closures at either end of the element, welding these into place and heating the tubing-end closure junctions in a salt bath to ceramic firing temperature. This heating causes the formation of a uniform hydrogen barrier (or blend) at these points.

E. CORROSION STUDIES

1. Introduction

NaK-78 has been selected as the primary coolant for the SNAP 8 system primarily because of its low vapor pressure, good heat transfer characteristics

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Figure 17. Shrink Fitting Apparatus

Figure 17a. Photomicrograph - Cross Section of a Shrink Fit Rod (500X)

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TABLE V

1400°F NaK-78 CORROSION DATA

Specimen	Duration (hours)	Type and Rate of Attack
Stainless Steel 304	1500	No apparent attack, slight evidence of decarburization
	2500	0.0014 in. pitting
	3500	0.0013 in. intergranular corrosion
	4500	0.0023 in. decarburization
Stainless Steel 316	1000	Slight evidence of general corrosion
	2000	0.0004 in. decarburization
	3000	0.0008 in. decarburization
Stainless Steel 347	1500	0.0006 in. intergranular corrosion
	2500	0.0004 in. pitting
	3500	General corrosion to a depth of 0.0006 in.
	4500	0.0025 in. decarburization
Hastelloy N	1500	Slight evidence of intergranular attack
	2500	General corrosion to a depth of 0.0005 in.
	3500	No apparent attack
	4500	Very slight surface attack
Haynes 25	1000	0.0002 in. pitting
	2000	Very slight evidence of general corrosion
	3000	0.0011 in. decarburization
Molybdenum	1000	No apparent attack
	2000	No apparent attack
	3000	Slight pitting, no depth
Inconel X	1000	No apparent attack
	2000	Very slight general corrosion
	3000	Very slight pitting, no depth
Hastelloy C	1000	0.0010 in. pitting
	2000	0.0017 in. decarburization

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4. Stainless Steel Type 347

The total exposure of this specimen at 1400°F was 4500 hours. The specimen at 1500 hours showed intergranular attack to a depth of 0.0006 in. with massive carbides and no evidence of grain growth. At 2500 and 3500 hours, the structure appeared as before with depths of penetration of 0.0004 and 0.0006 in. respectively. At 4500 hours very slight surface attack was still evident with decarburization depths of 0.0025 in. observed. It is therefore concluded that no additional corrosion occurred between 1500 and 4500 hours.

5. Stainless Steel Type 316

This specimen has accumulated 3000 hours of exposure at 1400°F and the results proved to be the best of the stainless steels tested. At 1000 hours no attack was observed. At 2000 hours there was evidence of some leaching (possibly decarburization) to a depth of 0.0004 in. The general structure appeared typical of stainless steel 316 exposed to 1400°F. At 3000 hours the specimen showed light general attack with decarburization to a depth of 0.0008 in.

6. Hastelloy N

Hastelloy N has been tested in a 1400°F environment up to 4500 hours. Since this material has been tentatively selected as the cladding for the SNAP 8 fuel elements, it should be noted that it performed better from a corrosion standpoint than did the alternate stainless steels.

The 1400°F specimen was examined at 1500, 2500, 3500, and 4500 hours. All specimens showed negligible depths of penetration and little evidence of grain boundary precipitates. The 2500-hour specimen did exhibit the possibility of some general corrosion up to 0.0005 in.

7. Haynes 25

The Haynes 25 specimen has logged 3000 hours of exposure at 1400°F. At 1000 hours the structure appeared typical with possible pitting occurring to depths of 0.0001 and 0.0002 in. The 2000-hour examination showed very slight evidence of general corrosion. The general structure appeared normal with equiaxed grains and fine precipitates. At 3000 hours the specimen exhibited slight general corrosion and the material was decarburized to a depth of 0.0011 in.

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8. Hastelloy C

This specimen has been exposed for an accumulative duration of 2000 hours with metallographic analysis being performed at the 1000 and 2000-hour interval. At 1000 hours, insignificant pitting attack was noted to depths less than 0.001 in. A possible diffusion zone appeared to approximately a 0.001 in. depth. The 2000-hour specimen exhibited slight pitting and decarburization to a depth of 0.0017 in.

9. Inconel X and Molybdenum

Both these specimens have been exposed to 1400°F NaK for 3000 hours and have been examined at 1000, 2000 and 3000 hours. These specimens exhibited no intergranular corrosion or significant pitting.

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III. NUCLEAR ANALYSIS

A. REACTIVITY REQUIREMENTS

The reactivity requirements for 10,000-hour operation at 600 kw are shown in Table VI. In order to achieve full design life the reactor requires 9.6% of excess reactivity initially. The total isothermal temperature defect is based on the experimental values measured on the SNAP 2 Experimental Reactor (SER) with corrections for the difference in temperatures and power between SER and SNAP 8. The defect of 3.2% δk is comprised of an average isothermal temperature coefficient of $-0.3\text{¢}/^{\circ}\text{F}$ for a total temperature defect of 2.9% $\delta k/k$ and a power coefficient, defined for constant axial coolant temperature differential of $-0.07\text{¢}/\text{kw}$ for a total power defect of 0.3% $\delta k/k$.

TABLE VI
SUMMARY OF EXCESS REACTIVITY REQUIREMENTS
(6000 Mw-hr)

Xenon	1.5%
Samarium	0.9%
Temperature and power defect	3.2%
Hydrogen leakage	1.5%
U ²³⁵ burnup	0.5%
Fission product poisons	0.7%
Loss of cladding integrity in 3 central rods	<u>1.3%</u>
	9.6%

The reactivity requirement for hydrogen leakage is based on current laboratory measurements of hydrogen permeation rates. An additional allowance has been made for hydrogen loss by including as a design requirement the necessary reactivity to tolerate complete hydrogen loss from three of the centrally located (high reactivity importance region) fuel rods.

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B. FUEL SELECTION

Although SNAP 8 was intended to be essentially a higher power SNAP 2, numerous surveys were conducted to insure that the basic criteria of low reactor weight and high reliability were indeed being attained in SNAP 8 as they had in SNAP 2.

A major modification, that of changing the moderator matrix from zirconium to yttrium was considered. Yttrium appeared desirable as a matrix because of its comparatively low dissociation pressure at high temperature. A comparison of two yttrium hydride and two zirconium hydride moderators was made using as a model a spherical bare core. The weights associated with the calculated critical dimensions together with the fuel moderator compositions are shown in Table VII.

TABLE VII
MODERATOR MATERIAL COMPARISON

Material	$N_H \times 10^{-22}$	Critical Radius (in.)	Weight (lb)
10 wt % U-90 wt % Zr	5.4	6.77	290
10 wt % U-90 wt % Zr	6.0	6.38	235
12 wt % U-83 wt % Y-5 wt % Cr	5.16	7.65	325
13.6 wt % UO_2 -86.4 wt % Y	4.8	8.27	370

The fourth material, with UO_2 , was assumed to be a compressed powder with the density taken as 93% of the theoretical value. The comparatively gross difference in critical size between the zirconium and yttrium based materials is due to the lower N_H 's of the yttrium based fuels and to the much higher neutron absorption of yttrium - the macroscopic absorption in yttrium is about six times that in zirconium.

A comparison of the two zirconium based materials provides an estimate of the importance of hydrogen from a weight standpoint of about 5 lb per percent change in N_H . (Later minimum weight reflected reactor studies show that this number decreases to 3 lb per percent change in N_H for the N_H range of 6.0 to 6.5.)

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From the results of this comparison, the 10 wt % U-90 wt % Zr hydrided to $N_H = 6.0$ was selected as the SNAP 8 fuel-moderator material. Although higher N_H 's at first appear desirable for weight reduction, the increase in dissociation pressures associated with the SNAP 8 operating temperatures lead to intolerable hydrogen leak rates and cladding stresses. The uranium weight percent was selected on the basis of SNAP 2 studies which showed that further increases in uranium loading above 10 wt % do not significantly decrease the reactor weight.

C. CORE MATERIALS

1. Cladding

The selection of Hastelloy N as the cladding material was based on several factors: it is readily available in tube form of standard diameters; experience has shown that it can be successfully coated with the Solaramic hydrogen barrier; it possesses the necessary long and short term strength requirements; and it has a thermal expansion coefficient not appreciably different from that of zirconium hydride. From the nuclear standpoint the cladding constitutes about one-half of the parasitic neutron capture and therefore it is desirable to limit the amount present in the core. A cladding thickness of 10 mils was selected as a minimum thickness consistent with strength and fabricability requirements.

2. Internal Reflectors

For the internal reflector pieces between the core vessel and fuel rods, beryllium oxide was selected. The possibility of using nickel for these inserts was analyzed and found to be highly unfavorable from nuclear considerations. When nickel is interposed between a fast neutron source (the core) and a thermalizing reflector (i.e., a material which acts not only to reflect neutrons but also to slow down fast neutrons), the nickel behaves as a strong thermal neutron absorber. This characteristic can be seen in Figure 18 in which the power and thermal neutron flux distributions are compared for the two cases. In each case, the external reflector is beryllium. The reactivity difference between BeO and Ni is calculated to be \$3.40 for an insert thickness of 1/4 in. followed by 2.5 in. of beryllium.

3. Reactor Vessel

The reactor vessel will be type 316 stainless steel. Hastelloy C was also considered because of its higher strength characteristics at operating

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Figure 18. Power and Thermal Flux Distribution

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temperatures; however, its high nickel content is disadvantageous. From recent experiments performed on SNAP 10, it is estimated that a Hastelloy C SNAP 8 vessel would cost about 80¢ in reactivity over a comparable stainless steel vessel.

4. External Reflectors

The external radial reflectors are reactor grade beryllium. The thickness is determined from the results of the parametric weight surveys and from a consideration of control requirements. The grid plates and end reflector materials are also determined from the results of the parametric survey.

D. PARAMETRIC STUDIES

After selecting the fuel moderator of 10 wt % U-90 wt % zirconium hydrided to $N_H = 6.0$, the following quantities remained to be determined by systematic parametric surveys:

- 1) A choice of end reflector material and thickness.
- 2) The minimum weight dimensions consistent with the radial reflector control requirements.
- 3) The effect on the minimum weight dimensions of changing the N_H .

In all the surveys the AIM-6 one dimensional diffusion theory reactor code was employed utilizing the code's dimensional search capability. The multiplication factor, k , was kept constant at a value corresponding to an excess reactivity requirement of 10%. The compositions and densities used in the calculations are given in Appendix D.

E. END REFLECTOR STUDIES

In SNAP 2, the reactor weight was decreased by incorporating a beryllium reflector plug at each end of the fuel rod. A survey showed that a minimum weight resulted when the beryllium was about 1.5 in. in length (at each end). A similar survey was conducted for SNAP 8, but because of the incompatibility of Be and the coating at the higher SNAP 8 temperatures, the plug was changed to BeO. In addition, the weight penalty associated with having no end reflector plugs or using a thick grid plate as an end reflector was investigated. Hastelloy C is ~55% nickel and is thus an excellent fast neutron reflector material.*

*NOTE: In this case, the nickel is not interposed between the core and a thermalizing reflector as in the case of the aforementioned inserts.

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The grid plate-reflector concept was taken one step further. By making the grid plate sufficiently thick at one end, it could also serve as a thermal shield to the gamma shield separating the reactor and payload. Although this idea undoubtedly results in a weight penalty in the reactor, it could provide a substantial shield weight savings. The biggest disadvantage is that by specifically incorporating a shield in the reactor the latter loses flexibility, i. e., the shield and not the reactor should be tailored to the specific payload.

The results for various end reflector possibilities are shown in Figure 19. The physics core radius corresponds to a circular area equal to the hexagonal fuel array area. For each end reflector at a given physics core radius, and for a constant reactor multiplication factor, k , trade off between core height and radial reflector thickness results in a minimum reactor weight. The curve denoted "1.5-in. thermal shield" assumes an upper grid plate thickness of 3/16 in. and a thermal shield-lower grid plate thickness of 1.5 in.

The weight penalty associated with a thicker grid plate (1 in.) at each end was considered excessive. The thermal shield concept was discarded primarily because the above stated disadvantage of reactor inflexibility. The design choice of no end reflectors was based on several considerations: increased fuel element rigidity, ease of rod fabrication, no reflector compatibility problems, a decrease in the percent hydrogen leakage because of higher hydrogen content together with a decreased leakage surface area, and a relatively small weight penalty of only 6 pounds.

F. WEIGHT OPTIMIZATION

The results of the parametric survey for weight optimization are shown in Figures 20, 21, and 22. The effect on weight of all possible tradeoffs between core radius, core height, and radial reflector thickness for a given multiplication factor are contained on each graph. The radial reflector thicknesses shown are external to the core vessel, i. e., they do not include the internal BeO reflector inserts. Figure 20 is for an N_H of 6.0, 1/2-in. Hastelloy C grid plates, and no end reflectors. (This survey corresponds most closely to the present design.) Figure 21 is for an $N_H = 6.0$, 1/4-in. Hastelloy C grid plates, and 1-in. BeO end reflectors. Figure 22 is the same as Figure 21 except that the N_H has been increased to $N_H = 6.5$. All cases assume an excess multiplication of 10%.

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Figure 19. Effect of Axial Reflector on Reactor Weight

Figure 20. Reactor Weight Survey, $N_H = 6.0$, 1/2-in. Hastelloy C Grid Plate

Figure 21. Reactor Weight Survey, $N_H = 6.0$, 1/4-in. Hastelloy C Grid Plates

Figure 22. Reactor Weight Survey, $N_H = 6.5$, 1/4-in. Hastelloy C Grid Plates

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The intersection of lines of constant reflector thickness and constant core height are indicated by the small circles. Referring to Figure 20, one can read off the following example: Starting with a fuel height of, say 13 in. and a radial reflector thickness of 3 in., the corresponding physics core radius for an excess multiplication of 10% is 11.27 cm corresponding to a core vessel of about 9.4-in. ID. The weight of this system is 359 pounds.

The reactor weights shown include the core, reflectors, grid plates, NaK inventory, and core vessel. Several assumptions on the calculations should be mentioned: The volume fractions of fuel, cladding, etc., in all regions of the reactor were kept constant. This is rigorous as long as the fuel diameter and the rod spacing remain constant. The interface region was assumed to consist of a homogeneous mixture of the beryllium oxide inserts, the core vessel, and the gap separating the vessel from the reflectors. In the SNAP 2 analysis there was no significant difference in the results of calculations performed in the above manner to calculations in which the interface was assumed broken into separate regions. Further, the interface region thickness was assumed constant independent of core radius. Actually this region varied in thickness from about 0.37 to 0.41 in. A value of 0.394 in. (1 cm) was chosen for these calculations.

G. SUMMARY OF WEIGHT OPTIMIZATION

The weight optimization study indicates that a minimum weight reactor is achieved with about an 11.5 cm physics core radius and a 2 to 2.5-in. reflector thickness. As can be seen from comparing Figures 20, 21, and 22, these values are essentially independent of N_H or end reflector material. However, in order to insure adequate control capability, the reflector thickness has been selected at 3 in. instead of 2 or 2.5 in. From Figure 20 for this reflector thickness the corresponding minimum weight physics core radius lies between 11 and 11.4 cm with corresponding fuel heights of 14 and 13 in. respectively. A physics core radius of 11 cm or vessel ID of 9-3/16 in. was selected for the preliminary design. The corresponding fuel height is 14 in. and the reactor weight is about 360 pounds.

The weight penalties associated with increasing the reactivity by small amounts (of order 1% $\delta k/k$) by various changes from the selected design point have been calculated and are summarized in Table VIII.

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TABLE VIII

WEIGHT PENALTIES ASSOCIATED WITH INCREASED REACTIVITY

Method of Increasing Reactivity	Reactor Weight Cost
Increase radial reflector thickness	18 lb/% δk
Increase core height	10 lb/% δk
Increase core radius	11 lb/% δk
Increase wt % uranium (range: 10 to 12.5 wt %)	0.8 lb/% δk /wt % U
Increase N_H	3 lb/% $\delta N_H/N_H$

H. CONTROL

1. Control Elements

The SNAP 8 reactor will employ neutron leakage and burnable poison control as does the SNAP 2. However, in order to make more reactivity available for control, extended reflector elements, as opposed to semicircular drums, will be employed, Figure 23. A two-dimensional reactor code calculation (diffusion theory) indicates an element worth of \$6.00 (4.5% $\delta k/k$) for a rotation of 100 degrees from full in and a total external reflector worth of \$23.75. These values appear reasonable and consistent with calculations performed in the SNAP 2 analysis and with measured values from the SER. The calculated worth removed as a function of element position is shown in Figure 24. The average reactivity worth per degree of rotation between 0 and 100 degrees is 6¢/degree. The maximum is 20¢/degree and occurs at full closure. Comparable worths measured for SNAP 2 drums are 3.6¢/degree average between 90 and 180 degrees (full in) and 5¢/degree maximum.

2. Reactivity Losses

The excess reactivity requirements have already been discussed, Table VIII. They can be conveniently grouped into short and long term requirements. The latter, totaling 4.9% in reactivity and requiring some form of long term compensation, consist of samarium buildup, hydrogen leakage and possible loss of cladding integrity, uranium burnup, and the miscellaneous fission product

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Figure 23. SNAP 8 Reactor

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Figure 24. Control Element Worth vs Position

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poisons. Their effect on reactivity as a function of time is shown in Figure 25 in which hydrogen leakage, fission product buildup, and fuel burnup have all been assumed to be linear with time.

3. Utilization of Burnable Poison

By employing suitable burnable poisons, the total change in reactivity due to the long term effects can be restricted to less than 4.5%, a desirable feature since this would allow the long term control to be achieved with a single element. Figure 26 shows the excess reactivity depletion or the actual control requirements as a function of time for three conditions - no prepoisoning, prepoisoning only for Sm-149 buildup, and prepoisoning in excess of the Sm-149 compensation. The burnable poison is samarium oxide. Either of the prepoisoned cases appear acceptable from the standpoint of being controllable with a single element since the maximum excess is always less than 4.5% in reactivity.

Figure 25. Reactivity Loss vs Time

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Figure 26. Effect of Prepoisoning

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IV. HEAT TRANSFER ANALYSIS

A. FUEL ELEMENT TEMPERATURES AND PRESSURES

The reactor coolant outlet temperature is limited by several factors:

- 1) Hydrogen dissociation pressure increases exponentially with temperature.
- 2) Permeability of hydrogen barrier coating on cladding increases directly with pressure and exponentially with temperature.
- 3) Cladding strength decreases with temperature.
- 4) Hydrogen barrier coating may flake off cladding metal above some limiting temperature. Although this limiting temperature has not been evaluated, it is suspected to be around 1500°F.
- 5) NaK corrosion becomes significant above 1500°F.

In order to achieve the required performance (1100°F NaK inlet, 1300°F NaK outlet, 600 kw, 10,000 hours life) within these temperature limitations, a careful design is required to minimize the temperature of the hot spots on the fuel cladding.

Several factors affect the maximum coating-cladding temperature. These are rod size, rod spacing, coolant flow distribution and outlet temperature, and degree of thermal contact between the fuel and cladding. Figures 27 and 28 demonstrate the effects of certain fuel element design features on the maximum clad-coating temperature and include the effects of hot channel factors.

If the fuel rods are slip fit onto the cladding a temperature drop of 55°F would be experienced across the gas gap (1 mil). By shrink fitting the cladding onto the fuel rod this gap is eliminated and cladding temperatures are reduced. Development work has shown that a shrink fit can be achieved without cracking the coating by use of the techniques discussed in the Reactor Materials Section (Section II).

Another method of decreasing cladding temperatures is to increase the spacing between fuel elements (see Appendix A). Increased spacing does, however, lead to a lower fuel density which requires a larger and heavier reactor.

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Figure 27. Effect of Heat Transfer Modifications on Maximum Cladding Temperature
(Unclad Fuel Rod Diameter = 0.532 in.)

Figure 28. Maximum Cladding Temperature vs Unclad Fuel Rod Diameter

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Spacing the rods at a pitch/diameter ratio of 1.015 (~ 8 mil spacing) gives a reduction in cladding temperature of about 50°F over the touching case without causing a large weight penalty because of reduced fuel density. Work is continuing to determine if the fuel elements will warp during fabrication or operating life so as to touch. It is possible to wire wrap or otherwise separate the elements but this could introduce core assembly difficulties.

Coolant flow distribution and outlet temperature are also important in reduction of the maximum fuel element temperatures. The SNAP 8 core with its present design configuration has a lifetime-average peak/average radial power distribution of 1.3 with the distribution shown in Figure 29. A peak/average flow of 1.45 was chosen, Figure 29, to give radially uniform maximum clad-ceramic temperatures. A more complete discussion of the flow distribution is given in the following Subsection (B).

Use of small fuel rods causes a decrease in cladding and fuel temperatures. Figure 28 shows the effect of fuel rod diameter on maximum cladding temperature for various fuel element designs. It can also be seen in Figure 28 that the number of fuel rods in the core assembly increases rapidly as the rod diameter decreases. A very large number of fuel rods is undesirable because of increased fabrication costs, assembly problems, and also because of increased quantities of cladding (a nuclear poison) in the core. A rod diameter of 0.532 in. was chosen. This gives a 211 rod core with a maximum cladding temperature of slightly above 1400°F (for optimum fuel element design).

Hot channel factors were developed for the core by assuming the most pessimistic conditions for fabrication and assembly tolerances. A factor of 1.09 was obtained for variation in hydrogen loading and uranium loading to be applied to the radial and axial ΔT 's. Factors to be applied to the coolant temperature rise only are, for uncertainty in prediction and variation of power distribution during life of core, 1.08, and maldistribution in flow, 1.10. The effect of hot channel factors is shown in Figure 30.

B. FLOW DISTRIBUTION STUDIES

1. Analysis

The radial flow distribution in the SNAP 8 core has an important effect on the maximum clad-coating temperatures. These maximum temperatures may be controlled to some extent by choice of the proper flow distribution.

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Figure 29. Flow Distribution

Figure 30. Core Temperature Distribution
(Unclad Fuel Rod Diameter = 0.532 in.)

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In order to achieve fairly uniform radial temperatures in the core, the flow should have approximately the same radial distribution as the power distribution which has a peak/average value of 1.3 (Figure 29). Because of the larger temperature rises through the cladding, coating, and fuel in the region of highest power, it has been determined that a peak/average flow of 1.45 with the distribution shown in Figure 29 would be the optimum. This flow distribution gives uniform maximum clad-coating temperatures throughout the reactor. The reduction of maximum clad-coating temperatures was the criterion for determining the flow shape rather than minimum hydrogen loss as it appears that high temperature integrity of the coating may be a more serious problem than reduction of hydrogen loss in the central high importance region of the core.

The hot channel factor described in the preceding Subsection (A) includes an uncertainty of 10% in the flow distribution; the 10% was set as a maximum deviation for the experimental effort in achieving the optimum flow profile shown in Figure 29. This flow profile gives a minimum axial ΔT of 179°F at the reactor center and a maximum axial ΔT of 228°F at the core edge.

The selection of the proper flow profile is complicated by the reactor control method. If one element reflector control is used, a flow profile must be chosen that is based on the average radial power distribution during the life of the core. If the one element control method is employed, an azimuthal power variation is anticipated. A nonsymmetric radial flow distribution could be specified, although it would be based on a lifetime average power distribution. Ignoring the azimuthal power variation would yield temperatures 44°F above the present maximum clad-ceramic temperature of 1406°F if the radially symmetric flow distribution in Figure 29 is used. If the angular power distribution is compensated for by using a nonsymmetric flow profile, the 44°F excess could be halved.

2. Experimental

A full scale mockup of the core has been built to determine the inherent core hydraulic characteristics. This model uses water as the fluid with the flow adjusted to give dynamic similarity to 1200°F NaK. The testing of various core configurations was performed to determine the necessary geometry to accomplish the desired flow distribution to minimize the plenum heights and pressure drops. The measured flow line in Figure 29 shows the best hydraulic

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results to date. These results were obtained with the use of a three-hole orifice plate in the lower plenum and are close to the desired flow. The low flow in the outer channels is being increased by opening the clearance between the outer fuel elements and the BeO internal reflectors.

To obtain the flow distribution in the core for the various flow profiles the salt injection method was used (Reference 6). This method is based on the increase of electrical conductivity of water when salt is added. Two electrodes are installed in each of the instrumented dummy fuel elements at various points in the core. These electrodes are a set distance apart. A single charge of concentrated salt solution is injected upstream of the first station. The time of passage of the solution between the two stations is obtained from the conductivity measurement yielding the mean velocity. Since the model being tested uses water, it is necessary to maintain a particular operating range for the model to obtain dynamic similarity. The hydraulic model is illustrated in Figures 54 and 55 and details are given in Appendix B.

C. HYDROGEN LOSS

The SNAP 8 core should be capable of operating throughout its specified lifetime without experiencing a great degradation in reactivity due to loss of the hydrogen moderator. Recent laboratory tests have shown that cladding coated with Solaramic has a permeation rate at SNAP 8 temperatures such that the hydrogen loss during the 10,000 hour operating life at 600 kw is below the 5% acceptable hydrogen loss. Figure 31 gives the percentage hydrogen loss expected for SNAP 8 as a function of outlet temperature. The loss for the SNAP 8 core is expected to be $2.3\%/10^4$ hours at the design outlet coolant temperature of 1300°F as shown in Figure 31. The method of calculating hydrogen loss is given in Appendix A.

D. THERMAL STRESS

The maximum stress in the fuel rods should not exceed the ultimate tensile strength of the fuel material in order to maintain fuel rod integrity and optimum heat transfer. Figure 32 gives the maximum fuel rod stress as a function of rod diameter for the reactor operating at 600 kw. Figure 32 is for the central rod with hot channel factors taken into account. The maximum thermal stress

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Figure 31. Hydrogen Loss in 10,000 Hours

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Figure 32. Fuel Thermal Stress

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expected is 15,500 psi. There are additional stresses set up due to hydrogen redistribution, but these stresses are small and also opposite in direction to the thermal stress. The ultimate tensile strength of the fuel material has been measured to be 27,000 psi at 1200°F (Reference 7) by transverse rupture test. A sample calculation of thermal stress is given in Appendix A.

E. CLADDING CREEP AND HYDROGEN REDISTRIBUTION

The extent and effect of creep in the Hastelloy N fuel rod cladding in SNAP 8 is an important design consideration. The cladding should not creep appreciably during the core lifetime in order to maintain the clad-fuel thermal bond.

After the reactor has operated for 700 hours, the internal pressure in the hottest fuel rod has dropped below the operating pressure of the reactor due to the effect of hydrogen redistribution (Figure 52, Appendix A). Figure 52 is based on the hottest rod and includes hot channel factors. The data used to determine the time dependence of the hydrogen redistribution is given in Reference 8. The maximum extent of hydrogen redistribution is a 12.6% N_H variation between the maximum and minimum temperature point on the hottest rod.⁹ Based on the maximum fuel rod pressure, the creep experienced by the fuel rod cladding is 0.03%/10⁴ hours, Figure 33. The creep then is negligible since the internal fuel rod pressure is greater than the reactor pressure for only 700 hours.

F. MAXIMUM TEMPERATURES AND RADIATION DAMAGE TO REFLECTOR

The maximum temperature reached in the beryllium reflector is 1170°F. This temperature occurs at a height of 6/10 of the core height. The gamma and neutron heating peaks to a heat rate of 10,100 Btu/hr ft² at 5/10 of the core height. The maximum temperatures in the reflector and the gamma and neutron heating as a function of core height are shown in Figure 34. The swelling due to radiation damage¹⁰ is negligible as shown in Figure 35.

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Figure 33. Creep Properties of Hastelloy N

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Figure 34. Radiation Heating and Maximum Temperatures in the Beryllium Reflector

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Figure 35. Radiation Damage in Beryllium

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V. REACTOR CONTROL AND AUTOMATIC STARTUP SYSTEM ANALYSIS

The purpose of the reactor control and automatic startup system is to maintain stable reactor operation under the following operational situations:

- 1) Reactor startup once a suitable orbit has been established and verified.
- 2) Preheating of the power conversion system.
- 3) Startup of the power conversion system.
- 4) Xenon buildup, normal fuel burnup and fission products poison buildup.
- 5) Burnout of the prepoison from the reactor core.
- 6) Normal hydrogen loss from the fuel elements.
- 7) Abnormal hydrogen loss (failure of fuel element hydrogen barrier) from the fuel elements.
- 8) Changes in parasitic load.

A. DESIGN CRITERIA

In order to accomplish the purposes of the control and automatic startup system and the overall flight system requirements, the following design criteria have been established:

- 1) The reactor control and automatic startup system complexity, weight, power requirements, startup time, and telemetering channels will be kept to a minimum.
- 2) The reactor automatic startup system must be capable of:
 - a) Maintaining the rate of change of outlet coolant temperature within predetermined limits.
 - b) Maintaining the temperature difference across the reactor core within predetermined limits.
- 3) The reactor control system must be capable of:
 - a) Maintaining the reactor outlet coolant temperature within predetermined limits.

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- b) Inserting and withdrawing reactivity at a preset rate as needed, over the entire mission life.
- c) Maintaining adequate control over the reactor when the hydrogen barrier of a predetermined number of fuel elements is lost.

B. CONCEPTUAL DESIGN

The conceptual design of the control and automatic startup system uses four large reflector control elements each of which circumscribes a 90° arc around the core vessel when completely inserted. Briefly, the startup system involves insertion of three control elements without feedback. At this time the reactor will be critical and $\sim 1/2$ the power and temperature defect will be overcome. Control is then switched to the active control system using outlet coolant temperature feedback, shown schematically in Figure 36. This latter system then controls the reactor for the remainder of the startup sequence plus all long-term disturbances. The operation is described in more detail in the following Subsection (C).

Figure 36. Reactor Control Block Diagram

Selection of the conceptual control and automatic startup system was based upon the design criteria given in the preceding Subsection (A). In particular, it was deemed desirable to have the simplest control system possible to obtain maximum reliability with minimum weight. The first concept of reactor control and startup was to use only one control system to accomplish all of the reactor

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operations. This was found to impose severe tolerance limitation upon the control element movement. The next approach was to divide the reactor control and startup into two independent systems. This approach looked promising and from it evolved the conceptual control system using initially a passive (no feedback) control subsystem followed by an active (reactor outlet coolant temperature feedback) control subsystem.

C. OPERATION

As previously described, the control and automatic startup system is composed of two subsystems. Both the passive and active control subsystems are used to start up the reactor. Long term control is accomplished by use of the active control system only. The operation of the two subsystems during startup and long-term control is as follows:

1. Startup Operation

During reactor startup, three large control elements are inserted. It is possible that two elements could be fully inserted at launch as the reactor would still have a significant shutdown margin. This has the effect of reducing the startup time and the structural weight of the system. However, this point has not yet been resolved. If the reactor is launched with all four control elements fully withdrawn, the first phase of the startup procedure will be to insert two control elements rapidly. The speed of insertion will be such that it takes less than 5 minutes to fully insert these elements. The third element will then be inserted at a programmed rate. This insertion rate will be nonlinear in order to reduce the startup time to a minimum. Before, or during, the insertion of the third element, the startup flow will be established such that there is primary coolant flow prior to reactor criticality. The above phase of the reactor startup procedure will take the reactor through critical and heat the system up to 500° to 700°F (essentially isothermal). At this time the active control system will take over and rotate the fourth element in while sensing the outlet temperature for feedback control.

The active control is accomplished by measuring the reactor outlet coolant temperature and comparing the measured value with a preset value of 1300°F. If the reactor outlet coolant temperature is less than 1300°F minus the dead band of 30°F, the fourth drum will be rotated in towards the core in preset steps and at a preset stepping time interval. It is possible that the angular step size will be

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nonuniform in order to decrease the startup time. This element continues to rotate inward until the reactor outlet coolant temperature reaches the dead band. The reactor will remain in this state (low reactor power - low flow rate) until the entire system has been sufficiently preheated for the power conversion subsystem startup.

After preheating has been completed, the mercury will be injected into the boiler and the reactor coolant flow will be programmed upward. The increased coolant flow plus the added heat removal rate will cause the reactor outlet coolant temperature to drop. The reactor power will increase due to both the reactivity input from the lower temperature and from insertion of the control element.

It is desirable to establish rated ΔT across the reactor core during the preheat phase in order to minimize the transient effect on the reactor due to mercury injection upon PCS startup. It is also desirable to optimize the startup flow rate to minimize startup power requirements and hence battery weight. The startup flow rate for these two requirements is probably not identical. Hence, a compromise startup flow rate will be determined.

It is presently anticipated that the reactor startup operation will take between 3 and 7 hours. This will depend largely upon the startup flow rate, the amount of nonuniform angular element rotation to be used, and the limitations imposed by the rates of temperature change. The time taken to complete the entire startup, i. e., time to preheat and start the power conversion system is not fixed as yet.

2. Long Time Control Operation

Long time control is defined as that phase of the reactor operations which occurs after full power stable operation has once been achieved. Long time control involves reactivity changes due to normal hydrogen loss, abnormal hydrogen loss, burnout of prepoison, burnup, fission product buildup and xenon buildup. Table IX lists the maximum reactivity rate for each of the above reactivity changes.

The minimum reactivity insertion rate being presently considered is 0.5¢/min with a maximum reactivity rate of 6¢/min. Hence, referring to Table IX, the control system can adequately compensate for all the reactivity disturbances encountered during the reactor lifetime.

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TABLE IX
REACTIVITY REQUIREMENTS

Disturbance	Maximum Reactivity Change (ϵ /min)	Total Reactivity (ϵ)
Normal H ₂ loss	-3.1×10^{-4}	-188
Abnormal H ₂ loss	-7×10^{-3}	-54 (for one element)
Prepoison burnup	+0.288	+435
U-235 burnup and fission product poisons	-2.5×10^{-4}	-138
Xenon buildup	-0.1	-188

Because of the PCS characteristics, the reactor system will see essentially a constant heat removal rate (disregarding the parasitic load). The reactor outlet coolant temperature control system has a dead band set at $\pm 30^\circ\text{F}$ about the nominal value of 1300°F . Therefore the reactor outlet coolant temperature can drop to 1270°F causing the reactor inlet coolant temperature to drop to 1070°F (flow rate and specific heat of the NaK remain essentially constant) before any correction is made or the temperatures may reach 1330°F and 1130°F before correction.

Figure 37 gives a plot of the peak change in reactor outlet coolant temperature as a function of step size. With the dead band tentatively set at $\pm 30^\circ\text{F}$, one single step rotation of the control element will not cause any overshoot of the dead band with the $6\epsilon/\text{min}$ rate limitation (one step per minute). Hence, the reactor will not limit cycle around the dead band during long-term control operation.

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Figure 37. Effect of Reactivity Steps on Outlet Coolant Temperature

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VI. CORE MECHANICAL DESIGN

A. DESIGN REQUIREMENTS

The requirements for the mechanical design of the SNAP 8 core are as follows: all components to be of minimum weight capable of withstanding launch environment (shock, acceleration, and vibration) and with an operating life in a space environment of 10,000 hours minimum. The reactor output shall be 600 kwt. NaK-78 will be utilized as the reactor coolant. Coolant flow rate will be $\sim 49,000$ lb/hr. The mixed mean reactor outlet temperature is to be $1300^{\circ}\text{F} \pm 30^{\circ}\text{F}$ and the inlet temperature will be $1100^{\circ}\text{F} \pm 30^{\circ}\text{F}$. The reactor operating pressure is 20 to 25 psia with a pressure drop through the reactor (including inlet and outlet nozzle losses) of 3 psi maximum. Fuel material is ZrH-10 wt % U $\pm 0.4\%$ U with an N_{H} of 6.0 ± 0.1 .

The parameters established for the design of the SNAP 8 core to meet the above requirements are as follows:

Design pressure	35 psia (creep $< 1\%$)
Design temperature - lower plenum	1200°F
Design temperature - core and upper plenum	1400°F
Launch loads	± 20 g in longitudinal direction and ± 3 g in lateral direction
Vibration	Up to 15 g rms random at 5 to 3000 cps
Vacuum	Trans-lunar space conditions
Piping reactions on reactor vessel	10 ft-lb maximum
Reactor vessel material	316 stainless steel
Allowable stress - lower plenum	$11,200 \text{ lb/in.}^2$
Allowable stress - core and upper plenum	3900 lb/in.^2
Nozzle size (inlet and outlet)	2 in.

B. REACTOR COMPONENTS

1. Reactor Vessel

The reactor vessel is a cylindrical pressure vessel of type 316 stainless steel (see Figures 2, 3, and Frontispiece). It is 9-3/16 in. ID and ~ 20 in. in

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length (overall). The vessel is constructed of two cylindrical courses of different thicknesses. The upper course which circumscribes the upper plenum and core is 1/16 in. thick. The lower course which comprises the lower coolant plenum is 1/8 in. thick. The bottom of the vessel is enclosed with a reversed dished torispherical head of 1/8 in. thickness, and the top pressure head is a standard torispherical head 1/16 in. thick. The coolant outlet nozzle is located in the center of the top pressure head, and nozzle reinforcement is accomplished by increasing the upper head thickness to 1/8 in. in the vicinity of the outlet opening. The coolant inlet nozzle is elliptically shaped and is located in the 1/8 in. thick cylindrical course as close as practical to the circumferential weld joining the bottom of the vessel shell to the lower head. Since the vessel wall thickness in the area is 1/8 in., no additional reinforcement is required for the nozzle opening.

2. Grid Plates

The upper and lower grid plates are circular discs 9-3/16 in. diameter. The lower grid is made of Hastelloy C plate 3/8 in. thick, and, the upper grid is made of type 316 stainless steel 11/32 in. thick. Both grid plates are pierced by 211, 9/64 in. diameter holes in a triangular array on 0.570 in. centers. These holes are used to position and hold the fuel elements. In addition to these fuel element locating holes, both grid plates are pierced with 420 coolant flow holes arranged in a hexagonal array superimposed on the triangular lattice of the fuel element positioning holes. The coolant flow holes in the upper grid plate are 5/32 in. in diameter; in the lower grid plate they are 1/8 in. in diameter. Thirty-six 1/8 in. diameter holes are located on the periphery of both grid plates for the positioning and holding of the internal reflectors.

3. Baffle Plate

A stainless steel flow baffle plate 9-3/16 in. in diameter and 3/32 in. thick is located in the lower plenum between the coolant inlet nozzle and the lower grid plate. The function of this baffle plate is to adjust the coolant flow pattern through the core to a cosine distribution with a velocity ratio of 1.45 peak to average. This coolant flow distribution is necessary to reduce the maximum fuel cladding temperature to a value more compatible with the hydrogen barrier coating (see Section IV).

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4. Core

The reactor core is composed of 211 fuel elements and 18 internal reflector pieces. The outline of the fuel element complex is a regular hexagon with slightly chopped corners. Internal reflectors occupy the space remaining between the hexagonal fuel element pattern and the cylindrical reactor vessel. These internal reflectors are made of BeO compacted to $\sim 96\%$ theoretical density and clad with 0.010 in. thick Hastelloy N. The internal reflectors extend the full length of the core from upper to lower grid plate.

5. Fuel Elements

Fuel elements consist of right cylinders of ZrH-10 wt % U hydrided to $N_H 6.0$ and clad with Hastelloy N tubing and end plugs. Fuel rods are 0.532 in. in diameter and 14.25 in. in length. The Hastelloy N tubing is 0.540 in. ID and 14.75 in. long with a wall thickness of 0.010 in. The inside of the tubing is coated with a hydrogen barrier coating ~ 0.035 in. thick. The fuel rods are inserted into the cladding by shrink fitting thus assuring adequate fuel element heat transfer (see Section IV) and alleviating fuel element vibration problems during launch environment. Fuel element end plugs are 0.534 in. in diameter and 0.25 in. thick. The end plugs are coated with a 0.035 in. thick hydrogen barrier and inserted into the ends of the tubing. Circumferential welds are made joining the tubing to the end plugs and the hydrogen barrier coatings between the tubing and the end plugs are then blended by a heat treating operation. There is no axial clearance between the tubing and the end plugs. The finished fuel elements are 0.560 in. in diameter and 14.75 in. in length (excluding the $9/64$ in. diameter, $1/2$ in. long locating pins positioned at each end).

C. REACTOR ASSEMBLY

Core components (i.e., grid plates, flow baffle plate, fuel elements, and internal reflectors) are integrated into a complete package and tied into the reactor vessel by six stainless steel, shouldered tie rods which screw into brackets attached at equal intervals around the circumference of the lower head of the vessel. After the tie rods are assembled into the brackets the flow baffle plate is placed over the rods and allowed to seat on the rod shoulders. Spacing washers $5/8$ in. long are then slipped onto the rods followed by the lower grid plate. Fuel elements and internal reflectors are then inserted into the positioning

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holes in the lower grid plate until the core is completely loaded. The upper grid plate is then placed on the top of the core assembly and nuts are fixed to the tie rod ends. The reactor is now ready for the final closure weld of the upper head to the shell.

Although the stainless steel rods will operate at a lower temperature than some of the Hastelloy N fuel element cladding, the tie rods will expand more than the fuel element cladding due to the difference in rate of thermal expansion between type 316 stainless steel ($\alpha = 10.5 \times 10^{-6}$ in./in./°F) and Hastelloy N ($\alpha = 7.61 \times 10^{-6}$ in./in./°F). Consequently the fuel elements can be held tightly between the grid plates thus alleviating shock and vibration loading problems incurred during launch environment at ambient temperature without incurring excessive compressive loads in the fuel elements and fuel element cladding at reactor operating temperatures.

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VII. MECHANICAL DESIGN OF REACTOR CONTROL ASSEMBLY

Preliminary sketching of the control elements, structural support and bearings has been done to get an overall concept of the package (see Figure 38).

A. CONTROL ELEMENTS

1. Structure

The four elements will be of beryllium mounted on a stainless steel support with journals that rotate in bearings on another frame attached to the reactor core housing.

2. Bearings

The bearings will be the sleeve type and will be made of materials developed as a result of high temperature tests in a high vacuum. Material tests in a vacuum chamber at 1000°F are in process in coordination with SNAP 2 development.

Preliminary results of this testing show the following promising bearing material combinations.

TABLE X
BEARING FRICTION AT 1000°F UNDER VACUUM

Combination	Maximum Friction Coefficient During Test	
	Starting	Running
Al ₂ O ₃ (sprayed) against carbon (National Carbon CDJ83)	0.23	0.30
Al ₂ O ₃ (sprayed) against TiC	0.38	0.38
Stellite No. 3 against carbon (CDJ83)	0.47	0.24

3. Springs

Torsion springs for loading the reflectors for scram have been tested for relaxation at high temperatures. Inconel X stressed to 48,000 psi showed

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Figure 38. SNAP 8 Reactor Control Assembly

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an unacceptable 13% creep after 1000 hours at 1000°F so that the test was discontinued. A Rene 41 spring under the same conditions has relaxed only 8% in 2000 hours and appears to be stabilizing.

B. CONTROL DRIVES

Based on the conceptual control scheme discussed in Section V, two types of control drives are being developed, Figure 39. One is a startup control and the other is a long term control.

The startup control is an open loop type of control which rotates the reflectors according to a time-rotation schedule. Various devices were considered including clock mechanism, friction motors, springs and dashpots, bellows and gas bleed, thermal expansion devices and others. Development was started on the gas bleed device for the startup control.

The long term control being developed is a closed loop control in which the outlet NaK temperature will be sensed and amplified and this signal will be fed to an interval timer which will switch power to a stepping motor to step the control reflector in or out as required to correct the temperature. This development is being carried on in conjunction with the SNAP 2 actuator development. At present, the first prototype of the stepping actuator is being assembled for testing and breadboard components of the interval timer are being tested. A photograph of the actuator parts is shown in Figure 40. Development of other SNAP 2 control components such as sensors, timers, and bearings will also be directly applicable to the SNAP 8 reactor.

C. SCRAM SYSTEM

Three types of scram devices are being developed to satisfy the three scram situations. Any of the three result in separation of the reflectors for the reactor.

An electrical signal by cable or telemetry from prelaunch to orbit will be used to operate an unlatching device and affect separation. In the event of re-entry into the earth's atmosphere, a heat sensitive device will unlatch the reflectors. A third device will be mounted on the NaK discharge line and will be armed as the NaK heats up during startup and then will operate an unlatching device when the NaK cools down after the life of the power conversion system.

A prototype device for the re-entry scram is being fabricated, and will soon undergo testing. Figure 41 is a photograph of a model now undergoing test.

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Figure 39. Control Systems

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Figure 40. Stepping Motor

Figure 41. Re-entry Scram Device

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VIII. SHIELDING

A. SHIELD DESIGN CRITERIA

1. Radiation Damage

The shield requirements for the SNAP 8 vehicle are dictated by the amount of radiation which the various components may absorb without undue impairment of their normal functioning. The payload's electronic equipment is the most susceptible to radiation damage. The maximum permissible radiation level for this equipment was established by NASA at 10^{13} nvt for fast neutrons and 10^7 r for gamma radiation. This does not imply that all payload equipment can withstand these levels. Particularly sensitive components may require some local shielding.

2. Mission Length

The mission length has been established as 10,000 hours. This corresponds to the approximate time requirement for a Mars flight, one of several missions proposed for the SNAP 8 vehicle. The specification of the total permissible radiation and the mission length define the average neutron current and the average gamma-ray dose rate at the payload: 3×10^5 n/cm²-sec and 1×10^3 r/hr, respectively.

3. Vehicle Configurations

The vehicle configuration strongly influences the shield design. Although the configuration is still in the preliminary design stage, some of its features appear to be relatively fixed. The overall length of the vehicle, for instance, is limited to about 23 ft, which, in turn, limits the maximum amount of geometrical attenuation which may be realized. The radiator area also remains constant at about 900 ft² because of definite heat dump requirements, thereby fixing the total number of scattering centers.

Three different vehicle configurations are illustrated in Figures 42, 43, and 44. Figure 42 represents the vehicle design established at a joint meeting of personnel from NASA Lewis Laboratory, JPL, Aerojet, and AI, upon which initial work was to be based. The basic features of this model are the location of the payload in the nose section, the location of the reactor at the opposite end

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of the vehicle, and the arrangement of the radiators so that they are wrapped around the external structure during launch and unfold to form two fins during flight. Figure 43 represents a SNAP 8 vehicle configuration which is currently being evaluated. The approximate positions of reactor and payload for the configuration of Figure 42 are maintained in this version. The major difference involves the radiator arrangement. During launch the external housing is formed by four radiator, each in the shape of a quarter cylinder. During flight the radiators swing out from a pivot point at the bottom to form a flower petal arrangement. Figure 44 represents a design similar to that of Figure 43. The prime feature of this configuration is the end-to-end arrangement of the power conversion units.

B. SHIELD MATERIAL SELECTION

An evaluation was made of the radiation levels at the payload from the unshielded reactor. The investigation revealed that the gamma-ray and fast neutron radiation levels were about 10 and 5000 times, respectively, in excess of the levels which were established by radiation damage considerations. This situation clearly indicated that the shield material should be selected on the basis of good neutron attenuation properties. Lithium hydride was chosen because of its high effective removal cross section per unit weight (Goldstein¹¹ reports a relaxation length of 5.44 gm/cm^2 for thicknesses up to 60 gm/cm^2). In addition, lithium hydride has exhibited good thermal and radiation stability. It is planned that the material will be canned with stainless steel, the shield's specific gravity of 0.9 reflecting the inclusion of this can weight.

C. SHIELD DESIGN RESULTS*

1. First Vehicle Configuration

a. Unshielded Radiation Levels

In the absence of any shielding for the basic vehicle configuration (see Figure 42) the following list tabulates the components of the fast neutron current integrated over one year at the under surface of the payload:

*Calculational methods are summarized in Appendix E.

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Figure 42. Vehicle Configuration No. 1

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<u>Contributor</u>	<u>Integrated Current (nvt)</u>
Reactor, direct neutrons	4×10^{16}
Power conversion units, scattered neutrons	1×10^{16}
Ion motors, scattered neutrons	7×10^{14}
Radiator fins, scattered neutrons	1×10^{13}

The relatively large component due to scattering off the power conversion units is due to the comparatively large solid angle which the units subtend. The relatively small component due to scattering from the radiator fins is a result of the fin placement perpendicular to the external shell of the vehicle and thus, in effect, perpendicular to the reactor itself. If the fin were positioned tangent to the external shell, the radiator scattering contribution would be increased by a factor of about 30.

In the absence of any shielding the gamma-ray dose at the under surface of the payload is about 10^8 r, 10 times the design value. The predominant component is from direct gamma rays.

b. Shield Design

Shielding material is used directly under the payload as well as at the front of the reactor. The purpose of the payload shield is to reduce the fast neutron current which results from scattering off the ion motors and the radiator fins. While it is possible to reduce the scattered current by interposing shield material between the reactor and these components, the resulting shield weight is greater than that of the payload shield. This situation exists because the shield cannot be wrapped directly about the reactor, since space must be allotted for lateral movement of the reflector pieces.

The shadow shield has to be large enough to provide shielding for the power conversion equipment as well as the payload itself. If this provision were not made, the scattering from this component would be far in excess of the design neutron current.

Three shield designs, corresponding to three different locations of the power conversion units, are summarized in Table XI. The lightest shield results for positioning of the units directly below the payload shield. It is seen that this arrangement yields a shield weight reduction of about 500 pounds over the original specification that the units be located directly above the shadow shield.

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Figure 43. Vehicle Configuration No. 2

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TABLE XI

SHIELD DESIGN SUMMARY FOR VEHICLE CONFIGURATION NO. 1 (Figure 42)

Power Conversion System Location	Directly Above Shadow Shield	Directly Below Propellant Tank	Directly Below Payload Shield
Payload shield thickness	38.0 cm	35.8 cm	32.4 cm
Extended payload shield thickness	21.3 cm	21.3 cm	21.3 cm
Shadow shield thickness (maximum)	32.6 cm	35.8 cm	41.5 cm
Payload shield weight	496 lb	467 lb	423 lb
Extended payload shield weight	405 lb	405 lb	405 lb
Shadow shield weight	638 lb	309 lb	225 lb
Total shield weight	1539 lb	1181 lb	1053 lb

Figure 45 illustrates the variation in total shield weight with the quantity a , the ratio of the scattered neutron current from the ion motors to the design neutron current.

2. Alternate Vehicle Configurations (No. 2 and 3)

a. JPL Proposal (No. 2)

Figure 43 represents a configuration currently under investigation by Jet Propulsion Laboratory (JPL). Design changes from the basic model include relocation of the ion motors behind the shadow shield and a radiator surface which approximates a clam shell shape during flight. No extension of the shield below the payload surface is necessary to protect the side area, since the scattered neutron current levels at the latter surface are below the design value. In addition, the cesium tank is designed to provide a reduction by a factor of about four in the integrated direct neutron current at the payload. The power conversion equipment is located directly below the cesium tank to reduce the solid angle which the shadow shield must cover. The shield weight for this design is summarized in Table XII.

b. Idealized Configuration (No. 3)

Figure 44 represents the type of configuration which is desirable from the standpoint of shield weight. The cesium tank is located directly below the

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Figure 44. Vehicle Configuration No. 3

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Figure 45. Shield Weight vs Fraction of Allowable Dose Which is Scattered

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payload and thus provides a reduction in the scattered, as well as the direct neutron current at the payload. The power conversion units are strung out end to end to minimize the solid angle covered by the shadow shield. This configuration satisfies the 23-ft length constraint but does not meet the additional requirement that the center of gravity be within 12 ft from the base of the vehicle. This design, although not entirely practical, is included to exhibit the resultant low shield weight, as shown in Table XII.

TABLE XII
SHIELD DESIGN SUMMARY FOR ALTERNATE
VEHICLE CONFIGURATIONS

Vehicle Configuration No.	2	3
Payload shield weight	224 lb	0 lb
Shadow shield weight	351 lb	295 lb
Total shield weight	575 lb	295 lb
Payload shield thickness	17.2 cm	0 cm
Shadow shield thickness	40.8 cm	62.7 cm

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IX. SAFETY

A. SAFETY CRITERIA

The nuclear safety requirements, design features, and procedures for SNAP 8 will be derived to a large extent from the SNAP 2 and 10A flight test programs. SNAP 2 and 10A are scheduled for flight test in one and two years, respectively, before SNAP 8.

A preliminary list of the safety criteria that will be incorporated into the SNAP 8 reactor is given below. Although these safety criteria may be modified to conform to a particular launch condition of a SNAP 8 flight, the following basic safety philosophy and criteria will be used as guidelines to evaluate the reactor system flight safety.

1) Prevention of Accidental Criticality

The reactor system will be designed with adequate safety mechanisms, such as scram, shutdown and destruct systems, to insure that criticality of the reactor cannot be achieved except under controlled operation.

2) Safety and Ease of Handling

The reactor system will be designed so that personnel can test, handle, ship, integrate and repair the system with ease and within safety requirements.

3) Establishment of Safety Procedures

Adequate safety procedures will be established and personnel trained in these procedures prior to actual SNAP 8 reactor system operation to insure safety during the operational sequence.

4) Orbital Startup

Reactor operation will not be initiated until after an established suitable orbit has been verified.

5) Reactor Operation Termination

After completion of useful mission life, reactor operation will be terminated.

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6) Reactor Characteristics for Excursion Limitation and Prevention

Reactor system will have inherent shutdown characteristics such as negative temperature coefficient, hydrogen loss at time of fuel cladding failure and fail-safe shutdown mechanisms. These characteristics will limit the energy released in the event of an accidental criticality and prevent recurrence of criticality.

7) Re-entry Burnup

Re-entry burnup and dispersal of SNAP 8 fuel elements above 100,000 ft is a design objective, but will not necessarily be demonstrated prior to the first SNAP 8 flight test.

8) Failure of Safety Systems

In order for an unsafe reactor condition to exist, at least two system failures must occur in sequence. Thus all systems designed to prevent an unsafe reactor condition will act independently of each other.

9) Comply with Range Safety

All SNAP 8 flight procedures will be performed safely within the range safety rules and regulations designated for a similar non-nuclear missile launch.

10) Comply with Restriction Areas

The radiological hazard exclusion area deemed necessary on the basis of maximum probable accident of a SNAP 8 missile launch will not be greater than the hazard exclusion area required for the same launch of a similar missile on a non-nuclear mission.

B. SAFETY PROCEDURES FOR OPERATIONAL SEQUENCE

1. Acceptance Tests

The nuclear reactor acceptance tests will be performed in an AEC test facility at the Santa Susana test site. The safety precautions to be taken during these tests will be given in a separate report and will be based upon similar operations for land based nuclear reactors. Only millicurie amounts of fission products will be present at the time of shipment. Further fission product production will be kept at a minimum prior to the operation of the reactor in space.

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2. Shipment

The SNAP 8 reactor system will be shipped as a complete unit with the NaK and the reflector removed. The reactor system will be packaged so that immersion in any environment will not cause criticality. Containment and prevention of all chemical and nuclear accidents during shipping will be provided by the shipping cask and a reactor sleeve. Basically, the system will be inert and designed to remain inert during the shipment operation.

3. Integration and Transport

During integration of the SNAP 8 reactor system with the space craft, strict safety procedures will be followed. Removal or mechanical lock-out of the reflector control elements will prevent accidental criticality of the reactor during integration. Instrumentation will be present to detect any malfunction of the system during integration procedures.

During transport of the SNAP 8 space craft to the launch site from the integration facility, safety procedures will be followed to prevent accidents. The lock-out of the reflectors will remain to prevent accidental criticality during all transport conditions. Monitoring instrumentation will be present to detect any malfunction of the system during transport.

4. Mating and Check-out

The mating of the SNAP 8 space craft with the launch vehicle will be performed within detailed safety procedures. The lock-out of the reflectors will remain in effect to prevent accidental criticality. Monitoring instrumentation will be in operation during these procedures. The check-out of the combined system will be non-nuclear and all previous safety precautions will remain in effect.

5. Pre-launch

The pre-launch period will extend up to the launch of the vehicle. During this period, personnel will be evacuated beyond the exclusion radius or into the operations blockhouse. The umbilical and telemetered scram systems remain activated and the backup reactor destruct system is activated. The overall safety procedures during this period will be the normal range safety procedures. Monitoring instrumentation will be in operation and proper decontamination

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personnel and equipment will be on standby. The reactor system will remain inert during this operational period.

6. Launch

All personnel will be protected by the operations blockhouse or will be beyond the exclusion radius during launch. The range exclusion area for missile launch will be in effect. The missile destruct system will be activated to insure missile dispersion as well as deactivation of the reactor under any impact condition. The reactor destruct system will be activated to prevent accidental criticality in case of malfunction in the missile or reactor system. The reactor system will remain inert during this operational period.

Monitoring instrumentation, decontamination procedures, personnel and equipment and telemetering equipment will be available in case of missile abort or malfunction during the launch to orbit operation.

7. Operation

Orbital startup of the reactor system will be initiated after an established suitable orbit has been verified. The orbital shutdown system on the reactor will be automatically activated to insure reactor shutdown after completion of its useful mission life.

8. Shutdown and Re-entry

After completion of useful mission life, reactor operation will be terminated by the orbital shutdown system. If the system is to re-enter the earth's atmosphere, the following sequence of events will occur. Reactor separation from the vehicle will occur along with shutdown of the reactor. The reactor shutdown will allow maximum decay of the fission product inventory prior to re-entry.

Depending upon the mission and the lifetime of the established orbital altitude, re-entry burnup and dispersal of the fuel elements above 100,000 ft may or may not be necessary to insure that no radiological hazard will exist due to the re-entry of a SNAP 8 reactor into the biosphere. No radiological hazard will be present in the biosphere due to the intact re-entry of a SNAP 8 reactor if the established orbital altitude is high enough to insure an orbital decay time greater than 1000 years. Such lifetimes will allow adequate decay of the fission product inventory in the SNAP 8 reactor to a non-hazardous level.

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C. FLIGHT SAFETY SYSTEM EVALUATION AND DESCRIPTION

1. Safety Systems for Prevention of Accidental Criticality

Proposed safety systems to prevent accidental criticality of the SNAP 8 during its operational sequence include various fail-safe mechanisms and reactor design parameters. The shipping sleeve, which will replace the reflector during shipment, will be designed to prevent criticality of the reactor core even if the core should be filled with water and immersed in water during shipment. The shipping container will be designed to be watertight even after the most severe shipping accident and to prevent chemical accidents during shipment. The fail-safe mechanical lock-out design on the reflector elements will prevent accidental insertion of the elements under the most severe shocks and impacts during integration, transport, and mating. The reflectors will remain inert and positioned out during launch; and, if technically feasible, the reactor will be designed such that immersion in water will not cause the reactor to become critical under such conditions. The reactor destruct system will be capable of destroying the geometry of the reactor core so that with subsequent water immersion, and water in the core, the reactor will be subcritical.

2. Personnel Safety

The SNAP 8 reactor will contain negligible quantities of fission products prior to shipment to the launch site. Acceptance tests performed at Atomics International facilities will require only low power, short time nuclear calibration tests. Adequate fission product decay times will be determined to insure that personnel exposure during handling and shipment are minimal. Additional fission products will not be produced in the SNAP 8 reactor until orbital altitude has been achieved.

The safety systems to prevent accidental criticality during the operational sequence in which personnel are present will assure the safety of the operations personnel. In order to adequately protect personnel during SNAP 8 shipment, handling, integration, mating and maintenance, specific operational procedures will be established and the concerned personnel trained to perform their duties within these safety procedures and requirements.

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3. Shutdown Systems for Safe SNAP 8 Reactor Re-entry

A system failure which permits reactor operation to continue although little power is produced could present a hazard even though the orbital altitude of SNAP 8 would be adequate to allow sufficient decay time for the SNAP 8 fission product inventory if reactor operation would cease at the end of useful power. Since such a malfunction could create a significant radiological hazard at the time of vehicle re-entry if re-entry burnup cannot be assured, an orbital shutdown system will be designed which will shut down the reactor at the end of useful mission life. The proposed method for activating the orbital shutdown mechanism involves the temperature decay of the primary coolant after the failure of pumping power and the breaking of a link due to thermally induced stresses. A schematic of a proposed device is shown in Figure 46 (also see Section VII).

4. Safety Systems for SNAP 8 Reactor Burnup During Re-entry

Simulated re-entry arc-jet studies performed on SNAP fuel element material have confirmed the re-entry burnup analysis of SNAP fuel elements. These results indicate that re-entry burnup for SNAP 8 fuel elements can be assured if the fuel elements are released and exposed to re-entry heating during the early phases of the re-entry trajectory. Further analysis and development work is in progress to determine if early release of the fuel elements on re-entry is a probable circumstance or can be incorporated as a design feature.

Until complete re-entry burnup and high altitude dispersal have been demonstrated, there exists an immediately available solution to the hazards associated with the intact re-entry of a SNAP system. The problem can be solved by allowing sufficient time for radioactive decay such that intact re-entry does not constitute a radiological hazard. This decay time is achieved by limiting the use of SNAP systems to orbital altitudes which have the requisite orbital lifetime for decay. This approach must be supplemented by orbital startup of the system which allows a complete safety appraisal of the orbit prior to system startup and fission product generation.

Figure 47 shows total amount of radioactivity in the core after re-entry and Figure 48 gives the dose rate at 1 meter from the SNAP 8 core.

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Figure 46. Automatic Scram Mechanism

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Figure 47. Radioactivity in SNAP 8 Core After Operation

Figure 48. Dose Rate at 1 Meter from SNAP 8 Core After Operation and Re-entry

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D. RELIABILITY AND REDUNDANCY OF SAFETY SYSTEMS

The safety of the SNAP 8 reactor system is demonstrated by the sequence of unlikely events which must occur to result in personnel injury or property damage.

1. Shipment

During shipment of the SNAP 8 reactor system, all of the safety barriers that must be breached as well as conditions that must exist in order to cause a nuclear accident and as a consequent a radiological hazard are the following:

- a) Shipment accident must occur in a water media
- b) Shipping container must be opened
- c) Reactor sleeve must be removed
- d) Water must enter reactor core without reaction with NaK coolant or reactor core geometry damage.

2. Integration, Transportation, and Mating

During integration, transportation, and mating of the SNAP 8 reactor system, all of the following safety barriers must be breached:

- a) Water media must be present at time of accident
- b) Exclusion area of launch site must be breached
- c) Water must enter reactor core without reaction with NaK coolant or reactor core geometry damage
- d) Skin of fairing around reactor section must be opened and/or containment of assembly building breached.

3. Launch Site Operations

During launch site operations of the SNAP 8 reactor system, all of the following safety barriers must be breached prior to a nuclear accident resulting in a radiological hazard:

- a) Water media must be present at time of accident
- b) Skin of fairing around reactor section must be opened
- c) Exclusion area of launch pad must be breached

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- d) Exclusion area of launch site must be breeched
- e) Either water must enter reactor core without reaction with NaK coolant or all reflector elements must be driven in simultaneously without scram.

4. Launch

During launch of the SNAP 8 reactor system, all of the following safety barriers must be breeched prior to a nuclear accident resulting in a radiological hazard:

- a) During initial launch path over land, all of the following safety barriers must be breeched:
 - (1) Missile destruct system must not operate
 - (2) Reactor destruct system must not operate
 - (3) Exclusion area of launch site and pad as well as launch path must be breeched
 - (4) Water media must be present in area of accident
 - (5) Either water must enter reactor core without reaction with NaK coolant or all reflector elements must be driven in simultaneously without scram or loss of safety elements.
- b) During launch path over water, all of the following safety barriers must be breeched:
 - (1) Exclusion area of launch path must be breeched
 - (2) Missile destruct system must not operate
 - (3) Reactor destruct system must not operate
 - (4) Skin of fairing around reactor section must open upon impact with water
 - (5) Either water must enter reactor core without reaction with NaK coolant, or all reflector elements must be driven in simultaneously without scram or loss of safety elements while the reactor is in shallow depths and no reactor core damage has occurred.

5. After Orbit

After the SNAP 8 reactor system has attained a satisfactory orbit to justify startup, all of the following safety barriers must be breeched prior to

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a nuclear accident resulting in a radiological hazard:

- a) Malfunction of the propulsion system causing non-orbital trajectory re-entry from orbital altitude
- b) Telemetered scram signal fails to scram SNAP 8 reactor
- c) Orbital shutdown system fails to operate due to re-entry heat not activating fusible link
- d) Re-entry heating must not destroy reactor core geometry
- e) Fission product inventory must build up and not have adequate decay time during re-entry trajectory
- f) Impact area must be in water allowing water to enter reactor core without reaction with NaK coolant or reflectors must be driven in simultaneously to most reactive position upon impact due to negative temperature coefficient of operating reactor.

6. On Re-entry

During re-entry of SNAP 8 reactor system after completion of mission or at end of useful life, all of the following safety or inherent barriers must be breeched prior to impact resulting in a radiological hazard:

- a) Orbital shutdown system fails to operate at end of useful life and/or due to re-entry heating of fusible link
- b) Reactor continues to operate at a given power level during orbital lifetime if at a long lived orbit - not important for short lived orbits
- c) Re-entry heat is not sufficient to burnup and disperse fuel elements
- d) Remains of SNAP 8 reactor or fuel elements must fall on land and in populated area with no destruction and without burial due to impact
- e) Such remains or fuel elements undetectable by local populace and such an event not reported so that proper decontamination action can be taken according to possible extent of radiological hazard.

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E. DEMONSTRATION OF SAFETY OF NUCLEAR SPACE POWER SYSTEMS

It is anticipated that SNAP 8 will depend heavily on developments of the SNAP 2 and 10A programs for both safety criteria and demonstrations of the safety of nuclear space power systems. The SNAPSHOT program will involve flight tests of both SNAP 2 and 10A systems, one and two years respectively, before SNAP 8 flight test.

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APPENDIX A HEAT TRANSFER CALCULATIONS

A. REACTOR TEMPERATURES

Coolant, cladding, coating, and fuel temperatures were calculated throughout the core by the methods described below.

1. Coolant Temperature

The coolant temperature, $T_c(R, Z)$ at a core radius R was calculated by:

$$T_c(R, Z) = T_i + \frac{Q(R)}{2M(R)C_{p_c}L} \int_0^Z \epsilon(Z) dZ \quad \dots (1)$$

where T_i is the inlet temperature, $Q(R)$ is the length averaged heat generation rate per fuel rod at reactor radius R (Figure 29), $M(R)$ is the mass flow rate of coolant in the coolant channel at radius R , C_{p_c} is the heat capacity of the coolant, L is the final length, and $\epsilon(Z)$ is the normalized power generation in the axial direction (Figure 49).

2. Coating Temperature

The maximum coating temperature, T_s , which occurs at the point of closest approach in the fuel rod array may be calculated by the following method. The various temperature rises through the coolant, cladding, coating, hydrogen gap, and the temperature rise to the closest approach point from the average surface point were summed to get T_s .

$$\begin{aligned} T_s(R, Z) = & T_c(R, Z) + \Delta T_{NaK} \epsilon(R, Z) + \Delta T_{cl} \Omega \epsilon(R, Z) + \Delta T_s \Omega \epsilon(R, Z) \\ & + \Delta T_H \Omega \epsilon(R, Z) + \Delta T_T \epsilon(R, Z) \quad \dots (2) \end{aligned}$$

where $T_s(R, Z)$ is the coating temperature at core radius R at a height Z , and $\epsilon(R, Z)$ is the normalized radial and axial power variation (Figures 29 and 49). The ΔT_s and the quantity Ω shown in Equation 2 are described below.

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Figure 49. Axial Power Distribution

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ΔT_{NaK} is the average film temperature rise from the coolant to the cladding surface at the average power. (Calculated to be 5°F at the average power.)

ΔT_{cl} = the temperature drop through the fuel rod cladding at the average power with uniform coolant flow along the fuel rod surface.

ΔT_s = the temperature drop through the coating at the average power with uniform coolant flow along the fuel rod surface.

ΔT_H = the temperature drop through the fuel-coating gap at the average power with uniform coolant flow along the fuel rod surface.

ΔT_T = the temperature drop from the average fuel surface temperature to the point of closest approach. ΔT_T is a function of the rod separation as shown below. $\Delta T_T = (\gamma + \beta) \Delta T_o$ where ΔT_o is the temperature rise from the fuel surface to the fuel center at the average power if there is uniform coolant flow over the fuel rod surface; γ and β are a function of the rod separation.¹²

Ω = the ratio of the heat transfer area of the rod to the effective heat transfer area in a close packed array.

or

$$\begin{aligned}\Omega &= \frac{\Delta T}{\Delta T_{\infty}} = \frac{\text{temperature drop in close array}}{\text{temperature drop if rods are infinitely separated}} \\ &= \frac{\int_0^{\theta_o} \frac{q(\theta)}{h} \frac{l}{l_{\max}} d\left(\frac{\theta}{\theta_o}\right)}{\frac{q_{\text{ave}}}{h} \int_0^{\theta_o} \frac{l}{l_{\max}} d\left(\frac{\theta}{\theta_o}\right)} \quad \dots (3a)\end{aligned}$$

$$\begin{aligned}&= \frac{\int_0^{\theta_o} \frac{q(\theta)}{q_{\text{ave}}} \frac{l}{l_{\max}} d\left(\frac{\theta}{\theta_o}\right)}{\int_0^{\theta_o} \frac{l}{l_{\max}} d\left(\frac{\theta}{\theta_o}\right)} \quad \dots (3b)\end{aligned}$$

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where ℓ and θ are as shown in Figure 50, h is the heat transfer coefficient of the interface.

By geometry

$$\frac{\ell}{\ell_{\max}} = \frac{\frac{a}{\cos \theta} - 1}{1.1547a - 1} \quad a = \text{pitch/diameter} \quad \dots (4)$$

Equation 3b may be evaluated by numerical methods using the data given in Reference 7.

Since $\Omega = \frac{\Delta T}{\Delta T_{\infty}}$, Ω may be reduced to $\frac{A_{\infty}}{A_{\text{eff}}}$. Figure 51 is a plot of Ω vs rod separation.

Although ΔT_H is the temperature rise in the gap between the coating and the fuel surface, it was included in the calculation of the maximum coating temperature as the clad-coating may touch the fuel surface at the point of closest approach.

3. Fuel Center Temperatures

The temperature at the fuel rod center $T_f(R, Z)$ may be calculated by including the temperature rise from the fuel surface to the fuel center in Equation 2.

$$T_f(R, Z) = T_s(R, Z) + (\Delta T_{fo} - \Delta T_T) \epsilon(R, Z), \quad \dots (5)$$

where ΔT_{fo} = the temperature rise from the point of average fuel surface temperature to the fuel rod center at the average power, and is a function of the fuel rod separation. That is $\Delta T_{fo} = (1 + \beta) \Delta T_o$.

4. Hot Channel Factors

The effect of the hot channel factors may be incorporated in Equations 1, 2, and 5 by multiplying ΔT_{NaK} , ΔT_{cl} , ΔT_H , ΔT_T , and ΔT_f by 1.18 and multiplying $Q(R)$ in Equation 1 by 1.3.

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Figure 50. Flow Channel Cross Section

Figure 51. Effective Heat Transfer Area

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5. Optimum Flow

The selection of the flow distribution shown in Figure 29 was accomplished by a trial and error method to determine $M(R)$ in Equation 1 that would give uniform maximum coating temperatures (Equation 2) for all R .

B. CALCULATION OF HYDROGEN LOSS

It has been determined by semi-empirical methods¹³ that hydrogen permeation through ceramics like the coating material is governed by Equation 6.

$$\phi_H = \frac{S_o P}{1 + aP} e^{-\bar{\Delta}E/T} \quad \dots (6)$$

where

ϕ_H = hydrogen permeation rate $\frac{\text{cc(STP)}}{\text{hr-cm}^2}$

S_o = permeation rate at infinite temperature and is defined by the system, $\frac{\text{cc(STP)}}{\text{hr-cm}^2\text{-atm}}$

P = hydrogen pressure, atm

a = a constant determined by the system, 1/atm

$\bar{\Delta}E$ = $\Delta E/R$ where ΔE is the energy involved with the diffusion process and R is the universal gas constant in consistent units.

T = coating temperature, °R

Applying Equation 6 to a reactor core

$$L_H = \frac{1}{V \epsilon_s} \int_0^t \int_A \frac{\phi_H}{N} dA dt \quad \dots (7)$$

where

L_H = fraction of hydrogen lost

V = core volume, cm^3

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ϵ_s = fuel fraction in core

t = time, hours

A = area for hydrogen leakage in core, cm^2

N = hydrogen concentration in core, $\frac{\text{cc(STP)}}{\text{cc}_{\text{fuel}}}$

If L_H is small, N is roughly constant with time, and Equation 7 may be rewritten:

$$L_H = \frac{t}{NV \epsilon_s} \int_A \phi_H dA \quad \dots (8)$$

or

$$L_H = \frac{t S_o}{NV \epsilon_s} \int_A \frac{P(x, r)}{1 + aP(x, r)} e^{-\bar{\Delta E}/T(x, r)} dA \quad \dots (8a)$$

where the pressure and temperature variation over (x, r) is known. For a cylindrical reactor of height L , and assuming that T is invariant in r at constant height x , Equation 6 may be applied to the core configuration^{13,14} such that

$$\phi_H' = t \int_0^1 \phi_H A_v d\left(\frac{x}{L}\right) \quad \dots (9)$$

where A_v/L = area/unit volume of core per unit length and ϕ_H' is the total hydrogen loss/unit volume in time t .

The fraction of hydrogen lost $L_H = \frac{\phi_H'}{\epsilon_s N}$, where $A_v = \frac{2 \epsilon_s}{r}$

where

r = the fuel rod radius

and

L = the reactor length.

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Then

$$L_H = \frac{2t}{Nr} \int_0^1 \phi_H d\left(\frac{x}{L}\right) . \quad \dots (10)$$

Substituting for ϕ_H in Equation 10 and converting to % hydrogen loss,

$$\%L_H = \frac{200t}{Nr} \left(\frac{P}{1 + aP} \right) S_o \int_0^1 e^{-\Delta E/T} d\left(\frac{x}{L}\right) \quad \dots (11)$$

where P is the pressure due to the average fuel rod temperature, and is the pressure throughout the rod.⁸

Equation 11 is analytically integrable if a linear axial temperature distribution is assumed.¹³ Figure 30 demonstrates the nonlinearity of the axial temperature rise in the average fuel rod; because of this nonlinearity, Equation 11 was integrated numerically to estimate the hydrogen loss as shown in Figure 31.

For application of Equation 11 to the SNAP 8 core design,

$$a = 0.15 \text{ (Reference 8)}$$

$$s_o = 3.5 \times 10^5 \frac{cc_{H_2} \text{ (STP)}}{\text{atm-cm}^2\text{-hr}} \quad \text{(Reference 13)}$$

and

$$\bar{\Delta E} = 33,200^\circ R$$

S_o is based on two coats Solaramic with one blended end cap.

C. CALCULATION OF THERMAL STRESS

The maximum thermal stresses in a fuel rod with an (r, θ) temperature distribution is given in Reference 15. These stresses due to the radial temperature distribution are:

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$$\sigma_{rr} = \frac{E\alpha\gamma_o}{4(1-\nu)} (a^2 - r^2) \quad \dots (12)$$

$$\sigma_{\theta\theta} = \frac{E\alpha\gamma_o}{4(1-\nu)} (a^2 - 3r^2) \quad \dots (13)$$

$$\sigma_{zz} = \frac{E\alpha\gamma_o}{2(1-\nu)} (a^2 - 2r^2) - E\alpha\omega\gamma_o a^2 \quad \dots (14)$$

where

E = modulus of elasticity

α = thermal expansion coefficient

ν = Poisson's ratio

γ_o = Q/4k at the point at highest power

Q = the volumetric heat generation rate

k = the thermal conductivity of the fuel

a = the fuel rod radius

ω = a function of the rod separation.¹²

The stress due to the θ temperature distribution is:

$$\sigma_{rr} = \sigma_{\theta\theta} = 0; \quad \sigma_{zz} = -E\alpha\gamma_o r^2 \cos \theta. \quad \dots (15)$$

The maximum stress is in the axial direction and is greatest at $r = a$, $\theta = 0$. Combining Equations 14 and 15,

$$\sigma_{zz_{\max}} = -E\alpha\gamma_o a^2 \cos (0) - \frac{E\alpha\gamma_o}{2(1-\nu)} a^2 - E\alpha\omega\gamma_o a^2. \quad \dots (16)$$

The values of the constants used are

$$E = 10.4 \times 10^6 \text{ psi}$$

$$\alpha = 7.1 \times 10^{-6}$$

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$$\nu = 0.3$$

$$k = 14 \text{ Btu/hr-ft-}^\circ\text{F}$$

$\omega = 0.58$ which corresponds to a pitch to diameter ratio of 1.015
(Reference 12).

D. CALCULATION OF CLADDING CREEP

The stress developed in the cladding due to internal fuel element pressure is:

$$\sigma = \frac{(P - P_o) D}{2t} \quad \dots (17)$$

where

σ = stress, psi

P = internal fuel rod pressure, psi

P_o = reactor operating pressure, psi

D = fuel element diameter, in.

t = cladding thickness, in.

Figure 33 shows creep in 10,000 hours vs stress for Hastelloy N at 1400°F (Reference 16). Internal fuel rod pressure is plotted as a third parameter. Examination of Figure 33 in light of the time dependent internal fuel rod pressure (Figure 52) indicates that the creep experienced by the fuel rod cladding is negligible.

E. CALCULATION OF HYDROGEN REDISTRIBUTION

The maximum extent of hydrogen redistribution in the fuel due to the axial and radial temperature distributions has been calculated to be 12.6% (Reference 9). This calculation is based on the fact that after hydrogen redistribution has been accomplished, each point in the fuel rod will have the same equilibrium hydrogen vapor pressure as any other point. The time for hydrogen redistribution was determined empirically from the data presented in Reference 8.

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Figure 52. Maximum Internal Fuel Rod Pressure vs Time

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F. CALCULATION OF RADIATION HEATING

The neutron heating in a material consisting of elements n, m, may be estimated in the following manner:

For heating due to neutron reaction in element n:

$$Q_{nr} = \sum_{nr} \rho_n \phi(E_r) E_n V_n \quad \dots (18)$$

where

$$Q_{nr} = \text{energy due to the neutron - n reaction, } \frac{\text{Mev}}{\text{sec-cm}^2}$$

$$\sum_n = \text{macroscopic cross section for the neutron reaction with element n, cm}^{-1}$$

$$\rho_n = \text{atom fraction of element in the material}$$

$$\phi(E_r) = \text{neutron flux above the threshold energy for the reaction to take place, neutrons/cm}^2\text{-sec}$$

$$E_n = \text{energy produced in reaction, Mev/neutron}$$

$$V_n = \text{volume per unit area of material.}$$

For energy deposition due to scattering:

$$Q_{ns} = \rho_n \xi V_u \int_E \sum_{ns}(E) \phi(E) E dE \quad \dots (19)$$

where

$$Q_{ns} = \text{energy due to scattering process with element n}$$

$$\xi = \text{logarithmic energy decrement}$$

$$\sum_{ns}(E) = \text{macroscopic scattering cross section of n at energy E, cm}^{-1}$$

$$\phi(E) = \text{neutron flux at energy E per unit energy, neutrons/cm}^2\text{-sec-Mev.}$$

$\int_E \sum_{ns}(E) (E) dE$ may be evaluated in a step wise manner using various intervals of E.

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The total neutron heat flux is then:

$$Q_{nr} + Q_{ns} + Q_{mr} + Q_{ms} + \dots = Q_{\text{neutron}}$$

The energy deposited due to gammas may be calculated by: again in element n

$$Q_n\gamma = \int_E e^{-\mu_c(E)t_c} \left[1 - e^{-\rho_n \mu_n(E)t_n} \right] \phi(E) dE \quad \dots (20)$$

where

$Q_n\gamma$ = energy dissipated in the material due to gamma interactions with element n

$\phi(E)$ = energy flux of gammas, Mev/sec-cm² per Mev interval

$\mu_c(E)$ = macroscopic gamma adsorption cross sections at energy E for the core¹⁷ (cm⁻¹)

t_c = characteristic distance that gammas must traverse through the core, cm

$\mu_n(E)$ = macroscopic energy cross section for material n (Reference 17), cm⁻¹

t_n = thickness of material containing element n, cm

Equation 20 may be evaluated by numerical methods.

G. CALCULATION OF REFLECTOR TEMPERATURES

The reflector temperatures shown in Figure 34 were calculated in the following manner.

A heat balance may be written around the reactor vessel - reflector interface with the resulting equations:

Considering only radial conduction,

$$\frac{Q_i}{A_i} = \sigma \epsilon_i (T_i^4 - T_B^4) \quad \dots (21)$$

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$$\frac{Q_B + Q_i}{A_o} = \sigma \epsilon_o (T_o^4 - T_s^4) \quad \dots (22)$$

$$\frac{Q_B + Q_i}{\bar{A}} = \frac{k}{x} (T_B - T_o) \quad \dots (23)$$

where

Q_i = the thermal radiation heat transferred from the reactor vessel to the reflector, Btu/hr-ft²

Q_B = nuclear radiation heat rate, Btu/hr-ft²

σ = Stefan-Boltzman Constant 1.73×10^{-14} Btu/hr-ft² (°R)
(Reference 13)

ϵ_i = emissivity at interface assured to be 0.8

ϵ_o = emissivity at beryllium surface assured to be 0.8

T_i = reactor coolant temperature at the core edge, °R

T_B = beryllium temperature at interface, °R

T_o = exterior beryllium temperature, °R

T_s = temperature of space = 0°R

A_i = area at reactor vessel reflector interface, ft²

$\bar{A}$ = average area for heat conduction through beryllium, ft²

A_o = area at reflector exterior, ft²

k = thermal conductivity of beryllium, Btu/hr-ft-°F

x = beryllium thickness, ft.

The conservative assumption was made that all of the heat generated due to gamma and neutron heating was generated at the reactor vessel - reflector interface.

Equations 21, 22, and 23 may be combined such that

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$$T_B = \frac{x A_o \sigma \epsilon}{k \bar{A}} (T_o^4 - T_s^4) + T_o = \left[\frac{Q_B}{\sigma \epsilon A_i} - \frac{A_o}{A_i} (T_o^4 - T_s^4) + T_i^4 \right]^{1/4} \dots (24)$$

assuming $\epsilon_i = \epsilon_o$.

Equation 24 may be solved by a trial and error method to produce Figure 34.

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APPENDIX B

DESCRIPTION OF EQUIPMENT FOR HYDRAULIC STUDIES

A. DESCRIPTION OF EQUIPMENT

The test apparatus for the core hydraulic studies can essentially be broken down into two categories - the fluid system and associated instrumentation.

The fluid system consists of a type 304 stainless steel loop with the following components: hydraulic core mockup, centrifugal water pump, flat plate orifice meter tube, and surge tank and built in water cooling system. Figure 53 is a photograph of the loop and instrumented core.

B. HYDRAULIC CORE MOCKUP (Figures 54 and 55)

This model consists of 163 5/8-in. diameter dummy fuel elements arranged in a hexagonal lattice. These elements are positioned in the core by the upper and lower grid plates. The lower grid plate assembly is identical to the prototype, while the upper grid plate is fabricated of two plates so as to form a hollow structure to accommodate the fuel element electrode wiring as shown.

The dummy fuel elements used to sense the change in coolant conductivity are fabricated of hollow stainless steel tubes. Two electrodes set 12 in. apart are mounted in each of the instrumented elements. A description of this element will be given in the instrument section. The addition of a solution of NaNO_3 salt to the effluent upstream of the lower plenum inlet is accomplished by an assembly welded into the pipe 10 in. from the inlet. This assembly consists of a right angle section of stainless steel tube welded into the pipe so that the salt solution enters the center of the water stream. The salt injection is accomplished by attaching a spring to the open side of the tube with the use of a clamped piece of polyethylene tubing.

C. CENTRIFUGAL WATER PUMP

The water pump is a standard Peerless centrifugal pump; it has a 400 gpm 175 ft of head at 3450 rpm. This pump is driven by a standard 25-hp motor.

D. SURGE TANK

The surge tank with internal diameter of 24 in. and height of 30 in. has a capacity of ~60 gallons. It is fabricated of stainless steel with a tangential inlet

Figure 53. Hydraulic Loop

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Figure 54. Instrumented Mock Fuel Elements

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Figure 55. Top View of Core Mockup

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to insure mixing. Inserted in the tank is a four turn 12-in. diameter coil fabricated from stainless steel tubing. This coil has cold water running through it and serves as a heat sink to maintain dynamic similarity of the loop. The cooling coil loop is equipped with a globe valve which varies the flow and in turn controls the temperature of water.

E. INSTRUMENTATION

The instrumentation required to obtain the coolant channel velocity by the salt injection method is as follows: sensing device of electrode, bridge network, direct recording oscillograph, flow meter, and coolant temperature control.

1. Electrode

Each instrumented dummy fuel element is equipped with a pair of electrodes 12 in. apart. The electrodes measure the electrical resistance from its tip to ground using the water or effluent as the conducting medium.

The electrodes are installed flush with the fuel element wall so that they introduce no flow perturbations.

2. Bridge Network

The electrical system of the test consists of a wheatstone bridge-type circuit. Prior to salt injection the bridge network is balanced by the use of a variable carbon resistor. When salt is injected into the water it causes an unbalance of the bridge giving a galvanometer deflection.

3. Direct Reading Oscillograph

The galvanometer deflection is recorded on a 36-channel visicorder oscillograph. Millisecond time intervals are pointed out on recording paper and the recorder time between galvanometer deflections directly describes the coolant velocity.

4. Flow Meter

The 2.5-in. flat plate orifice is read out on a standard 30-in. manometer. This manometer is filled with Merriam fluid with a specific gravity of 2.95.

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5. Coolant Temperature Control

The water temperature is read out on laboratory-type mercury thermometer located in an insert.

The temperature is observed and thermal stability is considered obtained when the temperature remains constant for a 5-minute period under steady coolant flow.

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APPENDIX C

DESCRIPTION OF EQUIPMENT FOR CORROSION STUDIES

A. THERMAL CONVECTION LOOP

The loop shown in Figure 56 is fabricated from 1 in. OD by 0.065 in. wall tubing; stainless steel type 304 and Inconel X loops have been used. All joints are full penetration arc-welds performed in an argon atmosphere and chromel-alumel thermocouples are Heliarc'd to the loop at critical points.

The heat source for the hot leg of the loop is a 230 v, 700 watt clamshell heater. The loop is wrapped with Cerro-felt insulation. All valves are bellows sealed, needle type.

The NaK-78 oxide content is controlled by means of a cold extension finger wrapped with copper tubing at the lower end of each loop. The copper coil is water-cooled, and can be metered to control the temperature at about 100°F. Based on Atomics International's plugging meter tests (Reference 6) and past experience in coolant cleanup, the oxide content is less than 20 ppm. Flow velocity on the order of 0.1 ft/sec is maintained by a ΔT of 200°F around the loop.

B. INSTRUMENTATION AND CONTROL

The clamshell heater is controlled by a Variac auto transformer. The loop temperature difference is maintained by natural convection cooling on the cold leg. To insure a constant convective heat transfer coefficient and adequate safety, the loops are contained in a concrete wall laboratory cell.

To prevent oxide contamination of the coolant, a 3 psig argon gas cover is maintained over the NaK in the loop.

C. SPECIMENS

The specimens as received are cut from 0.030 in. sheet and are 1/2 in. wide and 2 in. long. At the end of a predetermined length of exposure, a 1/4 in. piece is cut from each specimen for metallographic analysis, and the remainder is reinserted into the loop. Standard metallographic procedures are followed.

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Figure 56. Typical NaK Corrosion Loop

APPENDIX D
NUCLEAR PARAMETER SURVEY

Tables XIII, XIV, and XV summarize the compositions and densities used in the nuclear parameter survey.

TABLE XIII
RADIAL MODEL

<u>Core</u>		
<u>Material</u>	<u>Core Volume Fraction</u>	<u>Core Atom Density (atoms/cc x 10⁻²⁴)</u>
H	0.8071	0.04843
Zr	0.8071	0.02849
U ²³⁵	0.8071	0.00116
U ²³⁸	0.8071	0.0000862
Na	0.1130	0.001147
K	0.1130	0.0008573
Hastelloy N	0.0799	0.0799 x (Hastelloy N composition)
<u>Interface</u>		
Be	0.6092	0.04435
O	0.6092	0.04435
Hastelloy C	0.3351	0.3351 x (Hastelloy C composition)
<u>Reflector</u>		
Be	1	0.1236

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TABLE XIV

AXIAL MODEL

(t = thickness of region)

<u>Region 1</u> Core vessel, cold end of reactor		t = 1.27 cm
Material	Regional Volume Fraction	Regional Atom Density atoms/cc x 10 ⁻²⁴ See Table XV
Hastelloy C	1	
<u>Region 2</u> NaK plenum		t = 7.62 cm
Na	1	0.01014
K	1	0.007585
<u>Region 3</u> Lower grid plate		t = 0.635 cm or 1.27 cm
Na	0.0304	0.0003083
K	0.0304	0.0002306
Hastelloy C or 316 stainless steel	0.9696	See Table XV
<u>Region 4-A</u> End reflector BeO		t = variable
Na	0.1130	0.001145
K	0.1130	0.000856
Hastelloy N	0.0799	0.0799 x (Hastelloy N composition)
Be	0.8071	0.0587
O	0.8071	0.0587
<u>Region 4-B</u> End reflector Hastelloy C		t = variable
Same as Region 3		
<u>Region 5</u> Core		t = variable
See Table XIII		
<u>Region 6</u> End reflector		t = variable
Same as Region 4-A or 4-B		
<u>Region 7</u> Upper grid plate		t = 0.635 cm or 1.27 cm
Same as Region 3		
<u>Region 8</u> Upper NaK plenum		t = 1.27 cm
Same as Region 2		
<u>Region 9</u> Top of reactor vessel and NaK outlet		t = 1.27 cm
Na	0.1132	0.001148
K	0.1132	0.0008588
Hastelloy C	0.8868	0.8868 x (Hastelloy C composition)

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TABLE XV
COMPOSITION OF 316 STAINLESS STEEL, HASTELLOY C,
AND HASTELLOY N

Material	wt %	316 Stainless Steel	$\rho = 8.01 \text{ gms/cc}$
		atoms/cc x 10^{-24}	$\Sigma_a(2200), \text{ cm}^{-1}$
Cr	17	0.01577	0.0489
Ni	12	0.00986	0.474
Mo	2.5	0.001258	0.0034
Si	1	0.001722	0.0003
Mn	2	0.001754	0.0232
Fe	65.5	0.056607	0.1435
		Hastelloy C	$\rho = 8.94 \text{ gms/cc}$
Cr	15.5	0.01604	0.0497
Fe	5.5	0.005306	0.01345
Ni	55	0.05045	0.2420
Mn	1	0.00981	0.01295
Si	1	0.001918	0.00031
W	4	0.001171	0.00248
Mo	16	0.008985	0.02426
Co	2	0.001827	0.06760
		Hastelloy N	$\rho = 8.79 \text{ gms/cc}$
Cr	7	0.007125	0.00210
Fe	5.6	0.005311	0.01345
Ni	69.6	0.06277	0.3019
Mo	16.5	0.009106	0.0246
Si	0.5	0.000941	0.0001
Mn	0.8	0.000774	0.01021

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APPENDIX E

SHIELD DESIGN METHODS

The following is a summary of methods used in the shield studies discussed in Section VIII.

1. Neutrons

The fast neutron current at the payload is due to both direct and scattered neutrons. The direct contribution results from those neutrons which escape from the reactor and penetrate the shadow shield. The scattered contribution results from those neutrons which escape from the reactor and scatter off various vehicle components in a direction toward the payload. The radiators are the most prominent of these potential scattering surfaces. The cesium tank, power conversion unit, and ion motors may be important, depending upon the particular vehicle configuration, but it is possible to minimize their importance by judiciously locating them behind the shadow shield.

The direct current may be evaluated by means of the following set of equations:

$$\phi_r^D = \sum_{l, p} \frac{m+1}{2\pi} \frac{S_l}{P} \cos^m \alpha_{rlp} \frac{1}{\rho_{rlp}^2} \cos \beta_{rlp} \quad \dots (25)$$

where

ϕ_r^D = direct fast neutron current (n/cm²-sec) at rth radial position on payload

S_l = rate (n/sec) at which neutrons penetrate lth annulus of shadow shield

P = number of angular divisions of shadow shield

α_{rlp} = angle between normal to surface of lth shadow shield annulus and ray from rth payload radial position to center of pth angular division of lth shadow shield annulus

$\cos^m \alpha$ = angular distribution of neutrons which penetrate shadow shield

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β_{rlp} = angle between normal to payload surface and ray from r^{th} payload radial position to center of p^{th} angular division of l^{th} shadow shield annulus

ρ_{rlp} = distance (cm) from r^{th} payload radial position to center of p^{th} angular division of l^{th} shadow shield annulus.

$$S_l = A_l \sum_{i, j, R} \frac{S_{ijR} e^{-b_{ijR} l}}{4\pi \rho_{ijR}^2} \cos \gamma_{ijRl} \quad \dots (26)$$

where

A_l = surface area (cm²) of l^{th} shadow shield annulus

S_{ijR} = fast neutron production rate (n/sec) in reactor volume element defined by i^{th} radial division, j^{th} angular division, and R^{th} axial division

ρ_{ijRl} = distance (cm) from source point to l^{th} shadow shield annulus

b_{ijRl} = total number of mean free paths between source point and l^{th} shadow shield annulus

γ_{ijRl} = angle between normal to l^{th} shadow shield annulus and ray from source point.

The scattered current may be evaluated by means of the following expression:

$$\phi_{rs}^S = \sum_{i, j, R, u, v} \frac{S_{ijR} e^{-b_{ijR} uv} \cos \delta_{ijRuv}}{4\pi \rho_{ijRuv}^2} N_{uv} \left[\frac{d\sigma}{d\Omega} \right]_{ijRuvrs} e^{-b_{uvrs}} \quad \dots (27)$$

$$\frac{\cos \epsilon_{uvrs}}{\rho_{uvrs}^2}$$

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where

ϕ_{rs}^S = scattered fast neutron current (n/cm²-sec) at
rth radial position and sth angular position of
payload surface

S_{ijR} = fast neutron production rate (n/sec) in reactor
volume element ijR

ρ_{ijRuv} = distance (cm) from source volume element ijR
to scattering surface element uv

b_{ijRuv} = number of mean free paths between source volume
element ijR and scattering surface element uv

δ_{ijRuv} = angle defined by normal to radiator surface and ray
from source volume element ijR to scattering sur-
face element uv

$\left[\frac{d\sigma}{d\Omega}\right]_{ijRuvrs}$ = differential scattering cross section (cm²/atom-ster.)
for neutrons which leave source volume element ijR
and scatter from scattering surface element uv
toward payload surface position rs

ϵ_{uvrs} = angle defined by normal to payload surface and ray
from scattering surface element uv to payload sur-
face position rs

ρ_{uvrs} = distance (cm) from scattering surface element uv
to payload surface position rs

b_{uvrs} = total number of mean free paths between scattering
surface element uv and payload surface position rs.

2. Gamma Rays

The gamma-ray dose rate at the payload is due to direct and scattered gamma rays and to gamma rays from external activated materials such as the coolant. Of these contributions, the predominant one is made by the direct gamma rays, those which escape the reactor and penetrate the shadow shield. The direct gamma dose rate may be evaluated by means of the following set of expressions:

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$$D_r^D = \sum_{E, l, p} \frac{m+1}{2\pi} \frac{K(E) \phi_l(E) A_l}{P} \cos^m \alpha_{rlp} \frac{1}{\rho_{rlp}^2} \cos \beta_{rlp} \dots (28)$$

where

D_r^D = direct gamma-ray dose rate (r/hr) at r^{th} payload radial position

$K(E)$ = conversion factor (r/hr/Mev/cm²-sec) from energy flux to dose rate

$\phi_l(E)$ = gamma-ray energy flux (Mev/cm²-sec) at surface of l^{th} shadow shield annulus

A_l = surface area (cm²) of l^{th} shadow shield annulus

P = number of shadow shield angular divisions

α_{rlp} = angle defined by normal to surface of l^{th} shadow shield annulus and ray from annulus to r^{th} payload radial position

$\cos^m \alpha$ = angular distribution of gamma rays which penetrate shadow shield

ρ_{rlp} = distance (cm) from r^{th} payload radial position to center of p^{th} angular division of l^{th} shadow shield annulus

β_{rlp} = angle defined by normal to payload surface and ray from center of p^{th} angular division of l^{th} shadow shield annulus

$$\phi_l(E) = \sum_{i, j, R} \frac{S_{ijR}(E) e^{-b_{ijR}l}}{4\pi \rho_{ijR}^2} \cos \gamma_{ijRl} \dots (29)$$

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where

$S_{ijR}(E)$ = gamma-ray source strength (Mev/sec) in reactor volume element defined by i^{th} radial division, j^{th} angular division, and R^{th} axial division

ρ_{ijRl} = distance (cm) from source volume element to l^{th} shadow shield annulus

b_{ijRl} = total number of mean free paths (calculated with a cross section which contains an allowance for buildup) between source volume element and l^{th} shadow shield annulus

γ_{ijRl} = angle defined by normal to surface of l^{th} shadow shield annulus and ray from source volume element

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