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SNAP 8 SUMMARY REPORT

AEC Research and Development Report

Atomics International Division
Rockwell International

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Canoga Park, California 91304

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SNAP 8 SUMMARY REPORT

BY
STAFF

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ABSTRACT

The SNAP 8 Program was directed toward the development of lightweight nuclear reactors which would operate in space for greater than 10,000 hr at a power level of 600 kwt. Toward this objective, two reactors were designed, built, and successfully operated. The experimental reactor (S8ER) was an engineering test of a prototype core, but did not have flight-developed hardware. The developmental reactor (S8DR) was a nuclear system test to verify the operability of the integrated flight configuration. This report discusses, in detail, for each of the two reactors, their development, design characteristics, operating experience, experimental results, and supporting tests.

I. INTRODUCTION

The significant peaceful utilization of space will demand large quantities of reliable and long-lived power. The first 10 years of the space age have seen an ever-increasing number of satellites and space vehicles of growing complexity and sophistication. All spacecraft require electrical power for instrumentation, electronic data handling, guidance, and communication. To date, the electrical power has been supplied primarily by batteries, fuel cells, and solar cells. The power level has been of the order of a few watts up to a few hundred watts. The first nuclear power system in space was SNAP 3B, a 2.7-watt radioisotope unit fueled by Pu^{238} , which was launched in June 1961. The first reactor power system in space was SNAP 10A, a 500-watt unit, which was launched in April 1965.

The successful utilization of the large payload capability of future booster systems will only be realized with the concurrent availability of lightweight, long-lived, high power, reliable, electrical generating systems. The availability of large boosters coupled with nuclear auxiliary power sources will allow the space program to progress beyond the current era of space exploration. The future era of space utilization will reap untold benefits to all of mankind through world-wide communications, weather forecasts, navigational aids, etc. As nuclear power sources grow in power output and reliability, the promising ion and plasma forms of electric propulsion will allow interplanetary exploration without the fantastic size and cost of pure chemical systems.

At power levels of the order of a few kilowatts, the various solar and nuclear radioisotope or reactor power systems offer their own specific advantages and disadvantages. The selection of a particular solar or nuclear power system can only be accomplished in the context of specific mission requirements, payload considerations, reliability, costs, etc. In this evaluation, the nuclear system offers definite advantages of ruggedness, high power per unit area, no collector deployment, or orientation, continuous power, minimum power storage requirements, etc. In many cases, the added power availability of a nuclear system should offer significant operational flexibility and improved reliability through application of more conventional circuitry and instrumentation and

through active redundancy. As the power requirements are increased to the order of tens of kilowatts, the nuclear reactor systems have an increasingly favorable weight, size, and cost advantage over any of the presently envisioned solar power systems. For power levels in the hundreds of kilowatts, and above, a nuclear reactor system is the only one which appears at all feasible. In recognition of this superiority, the U. S. Atomic Energy Commission initiated the SNAP (Systems for Nuclear Auxiliary Power) program for the development and demonstration of a spectrum of units to fill future needs.

The difficulty of supplying power for the useful application of space and the unique density advantage of nuclear sources were recognized in the early 1950s. Atomics International, now a division of Rockwell International Corporation, conducted studies on the relative merits of radioisotope and fission heat sources in 1953. It was immediately recognized that significant power in space would require a reactor heat source. System conceptual design studies identified the mutually dependent reactor heat source, power conversion requirements, and concept selection criteria. By 1955, these studies resulted in the identification of a reactor and power conversion concept for the 0.5 to 10-kwe power range. The hydride reactor was chosen on the basis of minimum reactor weight, applicable operating temperature, and a reasonable extrapolation of reactor and materials state of the art. A mercury Rankine cycle power conversion subsystem was selected on the basis of minimum radiative heat rejection area requirement within the temperature constraint implied by reactor heat source development cost and schedule.

In the SNAP reactor system program, specific objectives for a 3-kwe mercury Rankine system, SNAP 2, were established in 1956 and system development was initiated. Reactor concept verification by means of a critical assembly was achieved concurrent with the start of the space age in October 1957. Interest in the thermoelectric direct power conversion for space systems was initiated in 1958. The specific objectives of the SNAP 10A 500-watt system were established in December 1960. This system progressed through the complete developmental cycle from concept identification to flight demonstration in orbit in the spring of 1965.

Anticipated increased mission power requirements coupled with increased booster system payload capability prompted a second generation of SNAP nuclear power systems. SNAP 8 was such a system, designed to provide 30 to 60 kw of electrical power, with a 10,000-hour life, for space vehicle propulsion. It was based on the SNAP 2 concept of a NaK-cooled, uranium-zirconium hydride reactor coupled to a mercury Rankine cycle power conversion system. The SNAP 8 system was a joint AEC-and-NASA-sponsored development program that started in 1960 and extended in its various conceptual designs through 1972.

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II. OBJECTIVES

The SNAP 8 system was conceived to meet the requirements for electrical power in the range of 30 to 60 kw in space applications. It was based upon the SNAP 2 concept of a NaK (sodium-potassium eutectic) cooled, uranium-zirconium hydride fueled reactor coupled to a mercury Rankine cycle conversion system.

The program at Atomics International had as its objectives the following:

- 1) To develop the SNAP 8 reactor including the associated controls, shield, and safety features.
- 2) Operate the complete SNAP 8 system.

The design and development of the SNAP 8 Electrical Generating System* was performed by Aerojet General Corporation under a contract with NASA.

The objectives for the reactor design as set forth in the beginning of the program in 1960 were as follows:

- 1) Power output up to 600 kw
- 2) Life of 10,000 hours
- 3) 1300° F NaK outlet temperature
- 4) Reactor weight of 350 lb.

The shield weight was a function of the mission application and depended upon the space vehicle configuration and the radiation sensitivity of the payload.

The development program consisted of several activities:

- 1) The preparation and updating of a reference design for the SNAP 8 flight reactor
- 2) The development of the fuel elements and other system components necessary for the reactor
- 3) The operation of test reactors to demonstrate the ability of the system to meet the requirements, and to provide data on the performance of integrated systems.

*NASA-CR-1907 SNAP-8 Electrical Generating System Development Program, Nov. 1971

The development of the SNAP 8 reactor can be traced through the test systems that were built. The systems that contributed to the program included the following:

A. SNAP 8 EXPERIMENTAL REACTOR (S8ER)

The S8ER was operated to provide data on the performance of the uranium-zirconium hydride fuel element at the design conditions for SNAP 8, 600 kw power and 1300°F outlet temperature. The reactor was operated in a small, inert atmosphere containment vessel using components which were not representative of flight configuration. The test program produced useful data on the performance of the fuel material and on the dynamic characteristics of the reactor.

B. SNAP 8 DEVELOPMENTAL REACTOR MOCKUP (S8DRM-1)

The flight reactor design as it existed in about July 1962 was fabricated as a nonnuclear mockup. This mockup was used in a test program that included all phases of its intended operation except nuclear operations.

C. SNAP 8 DEVELOPMENTAL REACTOR (S8DR)

The SNAP 8 design for the flight system was built for use in a combined system test with the NASA power conversion system to be designated S8DS. Programmatic redirection resulted in this becoming a reactor only test designated as S8DR. The components were arranged in a "breadboard" fashion in the test cell with the reactor and shield located in a vacuum vessel. This test provided data on the performance of the reactor system under simulated operating conditions.

D. SNAP 8 FLIGHT SYSTEM (S8FS)

The S8FS tests, intended to provide a full-scale test of the SNAP 8 reactor and power conversion system in a flight configuration, were not performed. Planning and facility construction were initiated, and it is mentioned here for the sake of completeness in the description of the program.

Following the design of the S8DR a series of studies were conducted on reactors within the SNAP 8 concept. They were all based on uranium-zirconium hydride fuel, NaK coolant, and beryllium reflector. Their thermal output was

from 300 to 600 kwt at outlet temperatures from 1200 to 1300°F. Their design points varied because of the differing system requirements imposed.

Three reactor designs were studied in some detail. These were the Reference Zirconium Hydride Reactor, the Reactor Core Test, and the Space Power Facility (SPF) Reactor. The major features of each of these reactors are given in Table 1.

TABLE 1
SNAP 8 DESIGN PARAMETERS

Parameter	Reference ZrH Reactor	Reactor Core Test	SPF
Thermal Power (kwt)	600	600	300
Number of Fuel Elements	295	295	199
Outlet Temperature (°F)	1300	1200	1200
Reflector Control	10 poison-back drums	8 void-backed drums	8 void back drums
Design Life (yr)	2.3	5	5

The Reference Zirconium Hydride Reactor was designed as a heat source for a power system utilizing a thermoelectric power conversion system. It was designed for use in a space station which led to a requirement for a shield that totally surrounded the reactor to permit rendezvous with shuttle vehicles.

The Reactor Core Test and the SPF (Space Power Facility-Plumbrook) were conceptual designs of systems to be used in conjunction with shadow shield systems. The designs of these reactors incorporated features which would circumvent problems identified in the testing of S8ER and S8DR.

Figure 1. S8ER Reactor Assembly View

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III. SYSTEM DESCRIPTION

The design of the SNAP 8 system was a continuously evolving entity as understanding increased and requirements were further defined. The system will be described as it existed for S8ER, S8DR, Reference Zirconium Hydride Reactor, Reactor Core Test, and the Space Power Facility Reactor. Of these, all but S8ER were conceived as representative of a flight configuration reactor. S8DRM is omitted from discussion since it was built to essentially the same design as S8DR.

A. SNAP 8 EXPERIMENTAL REACTOR

1. Reactor

The S8ER reactor was a compact nuclear reactor designed to operate for 3,000 hours at a power level of 600 kw. The reactor consisted of a homogeneously combined uranium fuel and hydrogen moderator with movable beryllium reflectors for control and a liquid NaK as the heat transfer medium. The system was a direct extension of the technology developed and tested for the lower SNAP 2 reactors. A view of the reactor assembly is illustrated in Figure 1. Design and operating characteristics are summarized in Tables 2 and 3. Figure 1 shows a sectional view of the reactor and external reflector assembly.

a. Fuel Elements

The reactor contained 211 fuel-moderator elements. Each element consisted of an individual fuel rod, a hydrogen diffusion barrier containing burnable poison, exterior cladding, end caps, and grid plate indexing pins (see Figure 2).

The rods were composed of enriched (93.15% U^{235}) fuel and hydrogen moderator in the form of a solid zirconium-uranium alloy (10% uranium) hydrided to a hydrogen density of 6×10^{22} atoms/cc. The rods were 0.532 in. in diameter and 14.0 in. long. The hydrogen diffusion barrier was a 0.003-in. -thick ceramic coating (AI-8763D) applied on the inside surface of the cladding to prevent the loss of the hydrogen moderator. A burnable poison (3.34 mg Sm_2O_3 per linear inch of fuel element) was added to the ceramic coating, to compensate for reactivity loss due to buildup of fission-product poison. The cladding consisted of 0.540 in. ID Hastelloy N tubing with a 0.010-in. wall

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Figure 2. S8ER Fuel Element

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TABLE 2 (Sheet 1 of 4)
S8ER DESIGN DATA SUMMARY

Design Parameters

Design power level	600 kw
Life	10,000 hr
Primary coolant	NaK 78 eutectic
Secondary coolant	NaK 78 eutectic
Number of fuel elements	211
Fuel loading	6.56 kg of U enriched 93.15%
Reflector	3 in. nominal thickness Be
Control	6 rotating control and safety drums

Fuel Elements

Composition	Hydrided Zr-U alloy
Hydrogen concentration	6×10^{22} atoms/cc
Alloy	90 wt % Zr - 10 wt % U
Cladding (Hastelloy N)	0.010-in. walls and 0.250-in. end caps
Fuel rod diameter	0.532 in.
Fuel rod length	14.0 in.
H ₂ diffusion barrier	0.003-in.-thick ceramic coating on inside of cladding surface
Burnable poison	48 mg of Sm ₂ O ₃ per element
Maximum cladding temperature at design power level	1440°F
Maximum core temperature at design power level	1542°F

Reactor Core

Core vessel size	9.34 in. OD by 21 in. nominal height
Core vessel material	316 SS
Core vessel wall thickness	1/16 in.
Upper grid plate material	316 SS
Upper grid plate diameter	9-3/16 in.
Upper grid plate thickness	11/32 in.
Upper grid plate coolant passages	420 5/32-in. -diameter holes

TABLE 2 (Sheet 2 of 4)

Lower grid plate material	Hastelloy C
Lower grid plate diameter	9-3/16 in.
Lower grid plate thickness	5/16 in.
Lower grid plate coolant passages	420 1/8-in.-diameter holes
Internal reflector	18 BeO filler pieces
Internal reflector cladding	Hastelloy N
<u>Control and Safety Elements</u>	
Number of elements	6
Material	Beryllium
Length	14.5 in.
Drum radius of curvature	4.69 in.
Nominal thickness	3 in.
Weight of each control element (including support and shim)	26.5 lb
Radial bushing clearance (self aligning)	0.004 to 0.006 diametral
Friction torque	10 in. -lb
<u>Control Element Drives</u>	
Drive means	1 reversible a-c motor per control element (only one element can be inserted at one time)
Element rotation rate	0.055 rpm (2¢/sec)
Element rotation range	105°
Time to rotate full range	319 sec
Coast rotation	0.033° (0.2¢)
Rotation stops	Limit switches and mechanical stop
Overload protection	Slip clutch
Drive system backlash	None - taken out by scram spring
<u>Scram System</u>	
Scram power	Torsional spring
Scram torque	30 in. -lb total 20 in. -lb for acceleration
Scram activation	De-energizing of electromagnetic clutch

TABLE 2 (Sheet 3 of 4)

Scram time (including 0.04-sec clutch release)	13° (2.50) = 0.169 sec, 85° = 0.342 sec, 105° = 0.405 sec
Number of elements scrambled	6
Number of elements required for shutdown	4
Scram velocity (maximum)	8.22 rad/sec
Scram acceleration (maximum)	27.3 rad/sec ²
Scram snubber	Pneumatic cylinder
Snubbing travel	20°
Deceleration during snub (maximum)	200 rad/sec ²
<u>Position Indication</u>	
Coarse	
Type	Potentiometer and vertical scale voltmeter
Number of elements sensed	6
Number of elements indicated	6 simultaneously
Accuracy of readout	±5° ~ ±30¢
Precision	
Type	Precision sensor and readout
Number of elements sensed	6
Number of elements indicated at one time	Active element plus any one of other five
Accuracy of readout	±0.070° = ±0.4¢
Type of readout	Digital lights
<u>Safety Circuit Trips</u>	
High neutron flux level (3 channels)	130% of design power level
Short reactor period (1 startup and 2 intermediate channels)	3 sec
Flux channel malfunction	2 high level channels
Low primary coolant flow	30% of design flow
Low primary coolant surge tank level	12 in.
High primary coolant outlet temperature	1375°F

TABLE 2 (Sheet 4 of 4)

Containment Vessel Pressure	15 psig
Earthquake	Grade VI on Modified Mercalli scale
High Primary System Pressure	40 psig
Manual pushbutton	
<u>Startup Source</u>	
Type	Po-Be
Size	0.5 in. diameter by 2 in. long
Activity	15 curies
Neutron emission	5×10^7 n/sec
Location	Outside of core vessel at top
<u>Primary Coolant</u>	
Flow rate	140 gpm
Reactor inlet temperature	1100°F
Reactor inlet pressure	38.7 psia
Reactor outlet temperature	1300°F
Reactor outlet pressure	36.8 psia
NaK inventory	315 lb
<u>Secondary Coolant</u>	
Flow rate	140 gpm
Heat exchanger inlet temperature	1050°F
EM pump inlet pressure	30.4 psia
Heat exchanger outlet temperature	1250°F
EM pump outlet pressure	54.5 psia
NaK inventory	622 lb
<u>Nuclear Characteristics</u>	
Lattice spacing	0.57 in. center-to-center
Mean prompt neutron lifetime	$6.7 \mu\text{sec}$
Effective delayed neutron fraction	0.0077
Median fission energy	0.21 ev
Thermal flux	2×10^{12} n/cm ² /sec

TABLE 3
S8ER REACTIVITY INVENTORY
(Based on 10,000-hr Operation at 600 kwt)

<u>Item</u>	<u>Reactivity Estimate (% $\Delta k/k$)</u>
1. Xe ¹³⁵ (equilibrium)	1.3
2. Sm ¹⁴⁹ ($\simeq$ equilibrium)	0.9
3. U ²³⁵ burnup	0.4
4. Other fission products	0.8
5. Cold to power	<u>1.6</u>
6. Total excess required	5.0
7. Initial unpoisoned excess (3-in. reflector; in vault)*	8.6
8. Sm ₂ O ₃ worth	<u>-2.6</u>
9. Initial poisoned excess	6.0
10. Reactivity available for H loss, contingencies†	3.6
11. Additional shim worth (beyond nominal 3 in.)	2.5
12. Control capability§ (six 3-in. drums; 0° to 105°)	14.9
13. Individual drum worth	2.7
14. Initial shutdown margin**	8.9

*The reflective properties of the reactor vault are expected to add 1.1% to the free space k_{eff} of 1.075.

†Deduced from Items 6 and 7.

§This value has been calculated to be less than six times the worth of an individual drum by 1.3% because of interaction effects.

**Deduced from Items 9 and 12.

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Figure 3. S8ER Core and Reflector, Section and Plan View

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thickness. The cladding envelope was completed by Hastelloy end caps which were seal-welded to the tubing. The manufacturing process ensured that a continuous diffusion barrier was provided on the inside surface of the cladding. The finished fuel element was 0.560 in. in diameter and 14.47 in. long (excluding the 0.140-in. -diameter positioning pins located at the ends of each element). The 211 elements were arranged in a triangular pattern on 0.570-in. centers. The assembly resulted in a hexagonal right cylinder, with slightly chopped corners, approximately 8 in. across flats and 14.47 in. long.

Fuel element design characteristics are summarized in Table 2.

b. Grid Plates, Internal Reflectors, and Coolant Baffle Plate

In addition to the fuel elements, the core contained an upper and lower grid plate, internal reflector filler pieces, and a coolant baffle plate (illustrated in Figure 3).

The grid plates supported and positioned the fuel elements and internal reflector filler pieces in the core. The lower grid plate (inlet plenum grid plate) was a 9-3/16-in. diameter and 5/16-in. -thick disc made of Hastelloy C plate. The upper grid plate (outlet plenum grid plate) was a 9-3/16-in. diameter, 11/32-in. -thick disc made of Type 316 stainless steel plate. Both grid plates were pierced by 211 holes of 0.144-in. diameter, spaced in a triangular array on 0.570-in. centers. These holes were used to position and hold the fuel elements in the core. The lower grid plate was also pierced with 420 holes of 1/8-in. diameter, arranged in a hexagonal pattern superimposed on the triangular lattice of the fuel-element-positioning holes. The upper grid plate was provided with 420 similarly spaced holes, 5/32 in. in diameter. These holes permitted the coolant to pass through the grid plates and around the fuel elements. In addition to the above holes, 30 holes were located on the periphery of each grid plate for positioning and holding 18 internal reflector filler pieces. Six holes were provided on the periphery of each plate for tie rods, which were used to assemble the core components and to anchor the core to the reactor vessel.

The 18 internal reflector filler pieces occupied the space on the periphery of the core between the hexagonal fuel element pattern and the cylindrical

reactor-vessel shell. The internal reflectors were made of beryllium oxide (BeO) clad with 0.010 in. Hastelloy-N, and extended the full length of the core between the upper and lower grid plates.

A coolant baffle plate was located in the inlet plenum of the reactor core vessel, 5/8 in. below the lower grid plate and just above the top of the coolant inlet line. The plate was a 9-1/16-in. -diameter disc of Type 316 stainless steel, 1/16 in. thick. A pattern of openings in the plate had been established empirically by the SNAP 8 core hydraulic experiments to adjust the coolant flow profile through the core to a cosine shape with a peak-to-average-velocity ratio of 1.45, matching the calculated radial power distribution. This coolant flow distribution was necessary to minimize the fuel-element cladding and hydrogen-barrier-coating temperature during full-power operation.

c. Core Vessel

The core and core-structure components, (i. e. , fuel elements, grid plates, internal reflectors, and flow baffle plate) were integrated into a complete package by means of six vertical tie rods at the circumference of the core. The core assembly was fixed to the core vessel by the lower end of the tie rods which screwed into brackets attached to the inner circumference of the lower plenum head of the core vessel. The core vessel was constructed of a cylinder of Type 316 stainless steel, 9-3/16 in. in ID and approximately 15-1/2 in. in length, with a 1/16-in. -thick wall. The lower end of the cylinder was closed with a reversed dish, Type 316 stainless-steel head, 1/8 in. thick and approximately 4 in. deep. Coolant entered the cylindrical portion of the lower head via a side inlet nozzle and a 2-in. stainless-steel pipe. The upper end of the vessel was closed by means of a 1/4-in. -thick, Type 316 stainless-steel cylinder (transition cylinder), with the lower end welded to the top of the reactor vessel wall and the upper end welded to the bottom of the shutdown-shield assembly above the core. This cylinder served as the upper coolant plenum, with coolant discharging through a side nozzle and a 2-in. stainless steel pipe. To simulate the flight configuration, a dished core-vessel head was located in the plenum above the core. A 2-9/16-in. -diameter hole was located in the center of the dished head, which served as the coolant exit port. Seven thermocouples were located in the plenum below the dished head to measure

temperature distributions in the coolant leaving the core. Three thermocouples were located in the exit port to measure bulk-coolant exit temperature. A summary of the reactor design characteristics was shown in Table 2.

2. Reflector and Control System

a. Reflector and Support

The core vessel was surrounded by 3 in. of beryllium (nominal thickness of equivalent annular cylinder) which formed the external radial reflector. The reflectors were approximately 14.5 in. long (approximately same as fuel element length) and were divided into six stationary and six movable sections. The portions of the reflector which formed the six movable sections (control drums) were segments of right circular cylinders with the axis outside the reflector assembly. Upper and lower bushings (shown on Figure 3) were located on the axis of each segment, permitting rotation of the control drums. Reactor control was accomplished by rotation of the control drums toward or away from the core, consequently changing the neutron economy and reactivity of the core. The control drums were also used as safety elements to scram the reactor. Removable shim plates were provided on the control drums to permit adjustment of the control drum worth prior to reactor startup.

The reflectors were supported by two stainless-steel support structures which were attached to a flange on the transition cylinder which connected the core vessel to the shutdown shield. The support structures were also pinned to brackets attached to the bottom of the core vessel to provide radial support. The movable control drums were connected to the support structures by means of the upper and lower bushings.

The bushings were of the self-aligning type, which permitted operation with limited misalignment due to thermal expansion. The upper bushings carried both radial and thrust loads. The lower bushings carried only a radial load.

b. Drives

The control mechanisms (shown in Figures 4 and 5) which positioned and scrammed the control drums were located above the shutdown shield; each

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Figure 4. Control Drive and Scram Mechanisms (Assembled)

1 POSITION SENSOR ELECTRICAL CONNECTOR	25 BRG RETAINING PLATE
2 TRANSMITTER MOUNTING CLAMP & BOLT	26 BRG RETAINING RING
3 POSITION INDICATOR TRANSMITTER	27 DUPLEX DRIVE SHAFT BRG
4 TRANSMITTER INPUT SHAFT	28 SCRAMSTOP ARM LOCK KEY
5 MOUNTING BOLT	29 SCRAM CLUTCH LOCK KEY
6 TRANSMITTER SUPPORT	30 CONTROL DRUM DRIVE SHAFT
7 TRANSMITTER FLEXIBLE COUPLING	31 DRIVE SHAFT BRG HSG
8 SCRAM SPRING	32 MOUNTING BOLT
9 LIMIT SWITCH ADJ ACTUATOR	33 "IN" LIMIT SWITCHES
10 ARM BRG	34 "IN" ADJ STOP
11 SCRAM STOP ARM	35 MOUNTING BOLT
12 CLAMPING BOLT	36 STOP BRACKET
13 ELECTROMAGNETIC SCRAM CLUTCH BODY	37 LEADS
14 LEADS	38 DRIVE MOTOR
15 SCRAM CLUTCH ENGAGING TEETH	39 ATTACHING BOLT
16 SCRAM CLUTCH ARMATURE	40 SLIP CLUTCH
17 ATTACHING BOLT	41 DRIVE WORM
18 LOCATING PIN	42 DRIVE SUPPORT
19 SCRAM CLUTCH ADAPTER	43 SCRAM SPRING ANCHOR
20 SCRAM CLUTCH SPACER	44 SNUBBER RELIEF PADS
21 DRIVE GEAR BRG	45 SNUBBER BLEED ADJ
22 DRIVE GEAR	46 SNUBBER PRESS TEST PORT
23 ATTACHING BOLT LOCKNUT	47 "OUT" LIMIT SWITCHES
24 DUST SHIELD	48 SNUBBER PLUNGER & OUT STOP
	49 SNUBBER ASSY
	50 MOUNTING BOLT

Figure 5. Control Drive and Scram Mechanisms (Exploded View)

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control drum had its individual drive system. The major components of the control system were a drive shaft, an electromagnetic clutch, a scram spring, a reversible a-c motor, a pneumatic snubber, and a position indicator.

Each drive shaft was ball-bearing mounted in a removable sleeve housing mounted in the shutdown shield. The connection between this drive shaft and the control-drum pivot shaft was made via two torsionally rigid, flexible couplings and an intermediate shaft. This arrangement allowed relative motion between the drive shaft and the control drum due to differential thermal expansion of components, while maintaining angular register. A 1/32-in. minimum clearance (at operating temperatures) was provided between the drive shafts and the control rod sleeves in the shutdown shield to ensure clearances for operation. Clearances for thermal expansion and misalignment were allowed, using conservative assumptions.

The drive shaft was keyed to an electromagnetic, rotary, scram clutch (Figure 4, Item 4) and to a scram drive arm (Figure 4, Item 5). It was also connected by a flexible coupling (with zero backlash) to the shaft of a position indicator transmitter (Figure 4, Item 9). Thus, the indicator sensed the angular displacement of the control drum directly. The inner end of a flat, spiral torsion scram spring (Figure 5, Item 8) was connected to the scram drive arm (Figure 5, Item 11) so that the spring torque always tried to rotate the reflector toward the scram position. The solenoid portion (Figure 5, Item 13) of the scram clutch was keyed to the drive shaft; the armature section (Figure 5, Item 16) was attached to a worm gear (Figure 5, Item 22) meshed with the drive worm. The drive worm was connected through a torque limiting coupling to a reversible a-c drive motor.

The connection between the two sections of the scram clutch was made with crown tooth face gears, providing a creepless engagement. When the electromagnet was de-energized, the connection between the two halves of the clutch was broken, and the force of the scram spring drove the reflector to the full-out position. With the clutch energized, the reflector could be positioned by operating the drive motor. Because nonreversible worm gearing was used, the scram spring could not drive the drive motor. However, the force of the scram spring on the drive worm eliminated any backlash in the worm gearing.

Dual, electrical, limit controls and physical stops limited the travel of the reflector in both the "in" and "out" directions. A pneumatic snubber was utilized to slow down the control drum at the end of the scram rotation. Mechanical keys were provided to lock each reflector shaft in the least reactive position during maintenance work. These keys engaged milled slots on the shaft and were bolted to the top of the shutdown shield. Electrical connections at the control console allowed only one of six drums to be positioned at any time. Actuation of a set-back switch, however, drove all six drums out simultaneously. Additional circuitry was provided to drive any control drum to its full "out" position in case the spring failed to scram the drum. Only four of the six drums were needed to shut down the reactor.

Prior to installation of the control mechanism in the test facility, a simulated reflector and a control drive mechanism were assembled, and operational tests were performed to determine satisfactory operation. These tests were conducted at operating temperatures as well as room temperatures. Scram tests were also made during reactor shakedown tests and as soon as normal environmental conditions were established during nuclear operation.

Data on the reflectors and their control system are summarized in Table 2.

3. Reactor Installation

The reactor and control system were located below grade level in a shielded, carbon steel, pressure vessel (see Figure 6). The pressure vessel contained the following:

- 1) A lower compartment containing the reactor core and external reflector assembly
- 2) A steel-encased, concrete, shutdown-shield assembly above the lower compartment, which supported the reactor assembly and reduced flux levels to equipment located above the shield
- 3) An upper compartment above the shutdown shield containing the control drive mechanisms and heat transfer system connections (inaccessible during reactor operation)

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Figure 6. S8ER Containment Vessel, Cutaway View

- 4) A steel-encased, concrete, operating shield above the upper compartment, which provided radiation protection for personnel in the reactor room during reactor operation.

The reactor core and external reflector assemblies were located in the lower compartment of the pressure vessel below the shutdown shield. The assemblies were suspended from the shield with the bottom of the core tank approximately 17 in. above the bottom of the pressure vessel. The free volume below the core tank was sufficient to hold the entire primary NaK inventory ($\sim 10 \text{ ft}^3$ NaK inventory). The total free volume of the compartment below the shutdown shield was approximately 20 ft^3 .

The shutdown shield was a stepped steel-lined plug which separated the upper and lower compartments. The plug was made of high-density borated concrete, lead and insulation. The plug was approximately 40 in. in diameter and 4 ft thick overall (including $3\text{-}1/8$ in. of insulation and 5 in. of lead on the bottom, 36 in. of high-density concrete, and 2 in. of lead on top). A $10\text{-}3/4$ -in. diameter, removable, stepped plug was provided in the center of the shield to permit fueling of the reactor. The shield assembly was cooled by air which was circulated through copper cooling coils embedded in the concrete and lead. The inlet and outlet coolant lines from the core, and the drive rods which controlled the reflector control drums, were routed through the shutdown shield to the upper compartment in the pressure vessel.

The upper compartment was located between the shutdown shield and operating shield. The compartment was approximately 48 in. in diameter and 27 in. high and had an overall volume of approximately 30 ft^3 . Radiation levels in this compartment were relatively low as a result of the attenuation of core gamma and neutron radiation by the shutdown shield. These low levels were necessary to minimize radiation damage and neutron activation of the drum drive mechanisms and controls located in this area. The low radiation levels also permitted access to the equipment after shutdown of the reactor. Loading and removal of the core, and connections to the heat transfer system were effected from this area after removal of the operating shield and shutdown-shield center plug.

LETTER	FIRST	SECOND	THIRD
A		ALARM	
C	CONDUCTIVITY	CONTROL	CONTROL
E		ELEMENT	ELEMENT
F	FLOW		
H	HAND		HIGH
I		INDICATOR	
M	MOISTURE		
P	PRESSURE		
R	RADIATION	RECORDER	
S	SPEED	SWITCH	SWITCH
T	TEMPERATURE		
L	LEVEL		LOW
Ph	POSITION	POSITION	
X		SCRAM	

Figure 7. Heat Transfer System P&I Diagram

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The operating shield was a stepped steel-lined plug located just below the pressure-vessel dome at floor level (see Figure 6). The plug was made of ordinary concrete, approximately 4-1/2 ft thick, and reduced the radiation levels from the operating reactor to permissible levels at the reactor room floor level. The dome of the pressure vessel, which protruded above the reactor room floor level, was also filled with concrete (approximately 2 ft thick) primarily to prevent the accumulation of gases in the dome.

Four instrument thimbles, containing eight neutron detectors, were located in the concrete shield adjacent to the containment vessel.

4. Heat Transfer System

The function of the heat transfer system was to remove the heat generated in the reactor core (600 kw) and reject it to the atmosphere. The system consisted of (1) a primary heat transfer loop which removed heat from the core by means of a circulating NaK coolant, (2) an intermediate heat exchanger which effected transfer of the heat from the radioactive primary coolant to a secondary nonradioactive heat transfer loop, (3) the secondary heat transfer loop, also containing circulating NaK coolant which did not become radioactive, and (4) an airblast heat exchanger which removed heat from the secondary loop and rejected it to the atmosphere. Figure 7 shows a simplified P&I diagram of the heat transfer system.

a. Primary Heat Transfer Loop

The primary heat transfer loop consisted of NaK coolant, a surge tank, an intermediate heat exchanger, a linear induction electromagnetic pump, an electromagnetic flowmeter, a side loop containing a plugging meter, associated valves, piping, and instrumentation.

The coolant was eutectic NaK (78% potassium). A total inventory of approximately 315 lb of NaK was contained in the primary loop. All metal surfaces in contact with the NaK were Type 316 stainless steel or Hastelloy. The primary system loop piping and equipment were located in a shielded, steel-lined vault containing an inert atmosphere.

During operation, the NaK coolant flowed from the reactor core at 1300° F via a 2-in. pipe. The coolant stream entered the 5 ft³ primary surge tank which contained a helium cover gas maintained at 15 psig. From the surge tank, the coolant flowed into the tubes of the intermediate heat exchanger. The NaK left the heat exchanger at 1100° F and flowed through the electromagnetic pump. A small stream (about 0.5 gpm) was taken from the exit side of the pump for flow through the plugging loop, used for NaK impurity determination, and back to the pump inlet. From the electromagnetic pump, the NaK flowed through a bellows seal valve and back to the reactor core lower plenum. The average primary NaK coolant flow rate was about 140 gpm.

Normally, the flow rate through the primary system loop was varied by means of a manually operated autotransformer which controlled the output of the electromagnetic pump. The coolant flow could also be varied by the valve in the primary loop (remote manual operation); the main purpose of this valve, however, was to aid in draining and purging the primary loop.

Helium was used to pressure-transfer the NaK from the primary loop to a shielded primary drain tank located in the secondary system equipment pit (drain tank not part of primary heat transfer system) and to purge the system. Vent and purge gases from the primary system were routed to a gas holdup tank for monitoring prior to release to the stack. Contact maintenance on the primary loop was performed after the loop had been drained and/or an appropriate waiting period had elapsed to permit decay of the activity remaining in the primary loop (primarily 15 hr Na²⁴).

A rupture disc assembly was provided to protect the primary heat transfer system from overpressure. The assembly was installed on the NaK reactor inlet line inside of the containment vessel above the shutdown shield. It consisted of two rupture discs in series with a leak detector located between the discs. The rupture disc assembly was appropriately installed in the system to prevent accumulation of gas under the disc (see Figure 6) which could interfere with the proper operation of the leak detector (conductivity type detector). The unit was designed to rupture at 50 psig and to withstand a backpressure of 168 psig. The primary system was designed for 100 psig at 1400° F. During the maximum credible accident the primary system in the vault could be subjected to a maximum possible overpressure of 167 psig and a temperature of

1560°F (this pressure drops to 100 psig in approximately 1 min). Stress calculations indicated that the EM pump was the controlling component. However, the primary system would withstand the maximum conditions for more than 2000 hr, and would, in fact, withstand a pressure of 400 psig for 5 to 10 hr. Hence, disc rupture pressure had been conservatively selected to protect that portion of the primary system located outside the containment vessel.

b. Secondary Heat Transfer Loop

The secondary heat transfer loop consisted of NaK coolant, an intermediate heat exchanger, an airblast heat exchanger, a secondary surge tank, an electromagnetic pump, an electric heater, associated valves, piping, and instrumentation.

The coolant was NaK 78 (same as primary loop). All metal surfaces in contact with the NaK were made of Type 316 stainless steel. A total inventory of approximately 622 lb of NaK was contained in the loop. Except for the intermediate heat exchanger, the piping and equipment associated with the secondary loop were located in unshielded areas above grade level (secondary fill and drain tank were located in the secondary system equipment pit). During normal operation, the pressure in the shell side (secondary coolant side) of the intermediate heat exchanger was approximately 22 psi greater than the pressure in the tube side; if leaks should occur in the heat exchanger, therefore, the secondary system would not become contaminated with the radioactive primary NaK. With no flow, the shell-side pressure was 4.3 psi greater than the tube-side pressure owing to static head differences. The airblast heat exchanger was located outside the building on a concrete pad. Because the secondary loop contained no radioactive NaK, shielding was not necessary.

During operation, the NaK coolant flowed through the shell of the intermediate heat exchanger and exited at 1250°F. The flow was then to the airblast heat exchanger where the heat was removed and dissipated to the atmosphere. The NaK coolant exited the airblast heat exchanger at 1050°F and entered the secondary surge tank, which was maintained at 15 psig by a helium cover gas. From the surge tank, the coolant flowed through the electromagnetic pump and back to the intermediate heat exchanger. The average normal flow in the secondary loop was about 140 gpm.

Flow through the loop was normally varied by means of a manually operated autotransformer which controlled the output of the electromagnetic pump. A manually operated valve was also provided in the loop to aid in draining and purging the loop. The secondary fill and drain tank was used to fill, drain, and purge the secondary loop. Helium was used to move NaK through the loop during draining, purging, and filling.

An electric heater was installed in parallel with the airblast heat exchanger. The reactor and its heat transfer system could be heated electrically with this heater by allowing NaK flow through the heater instead of the airblast heat exchanger. The capacity of the electric heater was 150 kw.

A rupture disc, designed to burst at 50 psig, was provided on the surge-tank vent line to protect the secondary heat transfer system from overpressure. The rupture disc discharged to the building stack.

5. Reactor Support Equipment

a. Instrumentation

S8ER instrumentation was centralized in the control room. Eight channels of nuclear instrumentation were used to monitor neutron flux and to provide signals representing reactor power and period for control and for safety. The reactor could be automatically scrammed by any of the following conditions:

- 1) High reactor power level on two of the three power channels
- 2) A fast reactor period
- 3) Low primary coolant flow
- 4) High core outlet coolant temperature on two of the three channels
- 5) Earthquake
- 6) High pressure in the primary coolant system or the containment vessel.

Process monitoring instrumentation provided indication and/or recording of flows, temperatures, pressures, neutron flux, reactor period, control-drum positions, radiation and activity levels, rotating equipment speeds, and electrical parameters. Annunciation in the control room indicated off-normal conditions.

To provide rapid, accurate recording of a large number of process variables, manual data collection from control room instrumentation was augmented with a data acquisition and processing system or "data logger." The data logger, which was located at the SNAP Environmental Test Facility, monitored 50 channels of S8ER plant signals and provided a typewritten record of its output in the S8ER control room. Data logger printout frequency was varied manually according to test condition requirements or was made continuous automatically if any process channel exceeded its alarm point setting. In addition to typewritten output, a punch paper tape was prepared, which was transcribed to a magnetic tape for digital computer use.

b. Building and Services

The S8ER Test Facility (Figure 8) was located in the Atomics International Nuclear Development Field Laboratories near Canoga Park, California. The facility was originally used for testing the 50-kwt SNAP 2 Experimental Reactor and was subsequently modified to accommodate the S8ER.

Figure 8. S8ER Test Facility

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Figure 9. S8ER Test Facility, Cutaway View

Figure 10. S8ER Control Room

Figure 9 illustrates in cutaway view the interior of the Test Facility; showing the control room (see Figure 10), the reactor room, and change room. A mezzanine in the control room provided desk space and an observation area isolated from the control room work area.

Within the reactor room was the shielded containment vessel housing the reactor, the shielded primary equipment vault containing the primary coolant system, and a secondary equipment pit in which a portion of the secondary coolant equipment was located. Other secondary coolant equipment as well as a variety of other process equipment was located at grade level in the reactor room.

Installed outside of the facility on concrete pads were:

- 1) The airblast heat exchanger
- 2) Air compressors
- 3) Refrigeration equipment
- 4) Electrical switchgear, emergency power equipment, and motor generator set
- 5) Miscellaneous service system equipment.

S8ER service systems included:

- 1) Water cooling systems for the exterior of the containment vessel and primary vault
- 2) Air cooling systems for the shutdown shield and containment vessel atmosphere
- 3) A helium atmosphere system for the containment vessel and a nitrogen system for the primary vault, as well as helium blanket gas systems for blanketing the primary and secondary coolants
- 4) Radioactive gas storage and disposal systems.

A 4160-volt, 3-phase feeder supplied electrical power which was reduced to 480, 240, and 120 for S8ER use. Electrical power failure resulted in immediate reactor shutdown and loss of the primary and secondary EM pumps;

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Figure 11. S8DR Ground Test Assembly

however, a 10-kw gasoline engine generator automatically started to provide emergency power to critical instrumentation and lighting. Regulated power for noise-sensitive instrumentation and circuitry was supplied by a 10-kw flywheel motor generator in series with a voltage regular.

B. SNAP 8 DEVELOPMENTAL REACTOR

1. Reactor

The SNAP 8 developmental reactor assembly included the fuel elements, the core vessel assembly, and the reflector and control components assembly (Figure 11). The 211 fuel elements were supported in the core vessel by an upper and a lower grid plate. The core had an active height of 16.825 in. and an equivalent diameter of 8.7 in. The reflector assembly was composed of six movable semi-cylindrical control drums and a stationary beryllium reflector. A summary of the principal parameters and characteristics of the S8DR system is presented in Table 4. The data reflect the "best estimate" values used for the design and evaluation of the system, and are not to be interpreted as the operating limits for the system. The reactor was designed for 600 kw power at 1300°F outlet temperature. The alternate operations condition of 1 Mw and 1100°F outlet temperature has a lower peak fuel temperature than the above design condition.

a. Fuel and Cladding

The fuel elements were designed to the requirement that an element failure from fuel swelling have a probability of less than 0.5%, under an anticipated operating conditions of the 12,000-hr S8DR test. Also, because of the increased swelling rate which was anticipated with the presence of β -phase zirconium hydride fuel material, the first appearance of β -phase material was to be used as an end-of-life design criteria.

Design limitations were imposed to minimize cladding failure from the following possible failure mechanisms:

- 1) Creep-induced strain to the point of rupture
- 2) Creep collapse due to cladding wall pressure differential
- 3) Fatigue due to thermal cycling during unscheduled reactor scrams and scheduled startups.

TABLE 4
SNAP 8 DEVELOPMENTAL REACTOR CHARACTERISTICS

General		Control Drums	
Design Power Level	600 kw	Number of Drums	6
Reactor outlet temperature	1300°F	Material	Beryllium
Reactor inlet temperature	1100°F	Length	15 375 in (total reflector length = 18 5 in)
Average power density	37 kw/liter of core	Drum Radius of Curvature	4 5 in
Maximum Power Level	1 Mw	Nominal Thickness	5 125 in
Reactor outlet temperature	1100°F	Drive Means	Electromagnetic stepping motor
Reactor inlet temperature	830°F	Rotation Range	0 to 109°
Design Life	12,000 hr	Full-In Stop	1.0 ± 1.0°
Primary Coolant	NaK	Full-Out Stop	135 ± 2.0
Secondary Coolant	NaK	Drive-System Backlash	±0.05°
Number of Fuel Elements	211	Scram Power	Torsional spring
Fuel Loading	8 2 kg U enriched to 93 15% U ²³⁵	Nuclear Characteristics	
Reflector	5 125 in, nominal thickness	Mean Prompt Neutron Lifetime	8 4 μsec
Control	6 rotating Be reflector drums	Effective Delayed Neutron Fraction	0 0080
Reactor Core		Median Fission Energy	0 15 ev
Core Vessel Size *	9 244 in ID by 32 2 in height to outlet line	Thermal Flux @ 600 kw (<1 ev)	3 x 10 ¹² n/cm ² sec
Material	316 stainless steel	Radial Peak to Average Power	1 29
Wall thickness	0 091 in	Axial Peak to Average Power	1 36
Grid Plates		Clean, Wet, Excess Reactivity	\$14 40
Coolant passages	420 (0.156 in. diam.)	NaK Reactivity Worth	\$0 25
Spacing of fuel element positioning holes	0.573 in. (triangular lattice)	Total Isothermal Temperature Coeff. of Reactivity	-0.18¢/F° at 70°F
Spacing of coolant holes	0 331 in. (hexagonal lattice)	Power Coefficient of Reactivity	-0 20¢/F° at 1200°F
Upper grid plate		Rise to 600 kw Power and 1200°F Core Average Temperature	-0 035¢/kw
Material	316 stainless steel	Reactivity Inventory (12,000 hours)	
Diameter	9 217 in.	Hot, end of life excess (600 kw)	\$5 45
Thickness	0.850 in.	Samarium burnout	\$2 90
Lower grid plate		Fission product poisoning	
Material	Hastelloy C	Xe	-\$1 05
Diameter	9.217 in.	Sm	-\$1 02
Thickness	0.65 in.	Other	-\$1 17
Internal Reflectors		Axial hydrogen redistribution	-\$0 65
Material	30 stainless-steel clad BeO and 12 solid stainless-steel pieces	Hydrogen loss	-\$5 15
Cladding thickness	0 030 in	U ²³⁵ burnup	-\$0 66
Cladding material	316 stainless steel	Control Drum Worth	
Fuel Elements		Total - 6 drums	\$21 06
Fuel Rods		Single drum	\$3 90
Composition	Hydrided ZrU alloy	Maximum differential worth	5 75¢/deg
Fuel	10 5 wt % U	Thermal and Hydraulic Characteristics	
Enrichment	93 15 wt % U ²³⁵	Average Heat Flux (at 600 kw)	47,000 Btu/hr-ft ²
Hydrogen concentration	6 05 x 10 ²² atoms/cc	Fuel to Coolant Heat Transfer Area	43 6 ft ²
Diameter	0 529 in	Coolant Flow Area in Core	
Length	16.825 in	Cold	0 0614 ft ²
Axial hydrogen gap	0 240 in	Hot	0 0653 ft ²
Radial hydrogen gap	3 5 mils	Hydraulic Diameter (individual coolant channel)	0 00675 ft
H ₂ diffusion barrier	2-mil ceramic coating on inside of cladding surface	Mass Flow Through Core (600 kw and 200°FΔT)	48,800 lb/hr
Burnable Poison	58 mg Sm ₂ O ₃ /element (in ceramic)	Average velocity through core, hot	4 6 ft/sec
Cladding (Hastelloy N)	0 540 in ID, 11 3 mils thick	Mass Flow Through Core (1000 kw)	60,000 lb/hr
Fuel Elements		Average velocity through core, hot	5 6 ft/sec
Diameter	0 563 in		
Length	17 325 in (excluding positioning pins)		
Peak fuel temperature at design power level	1485°F		
Peak cladding temperature at design power level	1367°F		
Average fuel burnup (12,000 hr at 600 kw)	0 22 metal atom %		

All dimensions are at room temperature conditions unless otherwise specified

External pressure on the elements (reactor coolant pressure) was limited to 35 psia during 1300°F coolant temperature operation. For the 1100°F outlet temperature operation (1 Mw condition), or any lower temperature, the reactor pressure could be increased to 50 psia.

The S8DR core contained 211 fuel elements cooled by NaK-78 flowing axially between them. Each fuel element consisted of a cylindrical rod of fuel-moderator, clad in Hastelloy-N tubing, as illustrated in Figure 12.

The fuel-moderator material was composed of a hydrided alloy of zirconium and fully enriched ($93.15 \pm 0.15\%$ U^{235}) uranium. The uranium content constituted 10.5 ± 0.3 wt % of the unhydrided alloy. This material also contained 0.15 ± 0.05 wt % carbon, added in the form of zirconium carbide (ZrC) prior to the initial arc melting of the zirconium-uranium alloy. This carbon additive resulted in a refinement of the alloy grain structure and a consequent improvement in the production yield during the hydriding process.

The 270 fuel rods were hydrided to have N_H^* values between 5.97 and 6.15 with an average for all 270 rods of 6.07. The required average N_H of the 211 rods finally selected was 6.05 ± 0.05 . At the operating temperatures characteristic of S8DR, hydrogen pressures in the range of 1 or 2 atm are produced in the annulus between the fuel rod and the cladding. To minimize hydrogen loss from the fuel element, the interior surface of the cladding was coated with a ceramic material. This coating reduced the permeation of hydrogen through the cladding wall by a factor of about 1000. The ends of each cladding tube were enclosed by Hastelloy-N end caps. These end closures were welded to the tubing. The ceramic coating on the final end closure was fused to that on the tubing to form a continuous hydrogen seal.

The active length of the S8DR fuel element (i. e. , the length of the fuel rod) was 16.825 in.† An axial gap of 0.240 ± 0.015 in. between the top surface of the fuel rod and the ceramic layer on the end cap was designed to accommodate axial fuel swelling and to minimize the heat transfer effect due to fission gas

* N_H is defined as the number of hydrogen atoms ($\times 10^{-22}$) per cubic centimeter of the hydrided zirconium-uranium alloy.

†All dimensions quoted in this section correspond to the unirradiated condition at 70°F, unless otherwise specified.

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Figure 12. S8DR Fuel Element

accumulation. The 0.529-in. -diameter fuel rod was separated from the 2-mil-thick ceramic layer on the cladding by a nominal hot diametral gap of 7.4 mils which served to accommodate fuel swelling. The Hastelloy-N tubing had an ID of 0.540 in. and a nominal thickness of 11.3 mils.

b. Core Vessel

The core vessel assembly included the reactor core vessel, the fuel elements, the upper and lower grid plates, the flow distributor and flow baffle, the internal reflectors, and the support structure for the reactor. The fuel elements were described in Section III-B-1-a.

The reactor core vessel was designed to minimum weight for space application. The basic design was modified to allow thermocouple penetrations to the upper plenum. The allowable operating stresses for the core vessel design were based on the criteria of 0.2% creep during the 12,000-hr reactor lifetime. Pipe reactions at the inlet and outlet nozzles were not allowed to exceed 50 ft-lb.

Calculational methods from the ASME Boiler and Pressure Vessel Code, Section III. Nuclear Vessels, and Section VIII. Unfired Pressure Vessels, were used in the design of the reactor vessel.

The reactor core vessel was a right circular cylinder consisting of a lower plenum head, an intermediate cylinder, and an upper plenum head, all fabricated from Type 316 stainless steel. The vessel (without the upper head) is shown in Figure 13.

Figure 13. S8DR Reactor Core Vessel

The lower plenum head had a flat external bottom and an 18-in. -radius convex internal bottom surface. This head had internal brackets for mounting the flow distributor, the baffle plate, and the lower grid plate. An external mounting ring was used to couple the core vessel to the support structure.

The intermediate cylinder, with a nominal wall thickness of 0.091 in. (0.085 in. minimum), was welded to the upper and lower plenum heads. It had internal lugs to support and position the upper grid plate, and external brackets on which to mount the external reflector and control system.

The upper plenum head contained a coolant outlet nozzle, 3 holddown lugs for the upper grid plate, 3 support lugs for the thermocouple bridge, and 27 thermocouples to measure coolant temperature at the reactor outlet. This head was

composed of a reducer section which connects the intermediate cylinder to a 5-in. Schedule 10 pipe tee which contained the 2-in. outlet nozzle. The other end of the tee was connected to the double bulkhead thermocouple penetration assembly. The thermocouples in the upper plenum were chromel-alumel, MgO insulated in Type 316 Stainless-steel sheath. Each thermocouple sheath was brazed at both bulkheads where it penetrates the plenum head. Inside the plenum the thermocouples were bundled in a 1-in. pipe and then distributed on a bridge to locations at the outlet of the core coolant channels. The core vessel was designed for 35-psig operation with an inlet temperature of 1150° F and an outlet temperature of 1350° F.

The core vessel design criteria of not more than 0.2% creep during the 12,000-hr lifetime restricted the allowable stress levels to less than 0.6 times the average 12,000-hr rupture stress or 0.8 times the minimum 12,000-hr rupture stress as required by the ASME code. Allowance of a four times reduction in rupture time due to irradiation effects gave allowable stresses which were not as restrictive as those from the 0.2% creep criteria. The reactor normally operated at 1100° F inlet and 1300° F outlet temperature, or 50° F below the design temperature.

The operating stresses in the lower plenum result from the 35-psi internal pressure and the 50 ft-lb moment on the inlet nozzle. The plenum was designed for 70-psi internal pressure, thus providing a 2.0 safety factor. During isothermal testing of the reactor, the inlet plenum operated above the 1150° F design temperature. Analysis showed it to be capable of 1000 hours operation at 1300° F, and an additional 11,000 hours at 1150° F without exceeding the 0.2% creep criteria while still maintaining the 2.0 safety factor.

The intermediate cylinder was the thinnest portion of the vessel. Coolant flow around the edge of the reactor core keeps the intermediate cylinder temperature more than 50° F cooler than the mixed mean outlet temperature, and the cylinder was therefore designed for only 1300° F. A factor of four increase in creep rate was allowed for irradiation effects to still meet the basic creep criteria. This allowance was slightly more restrictive than a 2.0 safety factor on the 35-psi pressure.

The upper plenum and thermocouple bulkhead was designed in accordance with Section III of the ASME Code and Code Case 1331-1. A coolant annulus was provided around the thermocouple bulkhead to limit the steady-state thermal gradients in the material. The design had a 16% safety margin from the maximum stress allowed by the ASME Code.

The worst stresses were anticipated to occur during transient conditions at the double bulkhead thermocouple penetration. This had been designed to withstand the startup transient of 150°F/min for 4 min. A series of baffles were placed in the bulkhead annulus to inhibit mixing during the transient and thus prevent large temperature gradients in the upper bulkhead. Without the baffles, about 100 cycles of the above transient are allowed by the Code. With the no-mixing baffles, about 600 cycles are allowable. As many as 55 such transients were expected. Shorter transients (reactor scrams) are acceptable because of their smaller magnitude (200 to 270°F).

c. Grid Plates

The grid plates supported and positioned the fuel elements and internal reflectors in the reactor. The upper grid plate (outlet plenum) was a 9.217-in.-diameter by 0.85-in.-thick disc of Type 316 stainless steel. Its lower edge was notched at three places to position it on the support lugs of the core vessel.

The lower grid plate (inlet plenum) was a 9.217-in.-diameter by 0.65-in.-thick disc of Hastelloy C. The plate was bolted to the reactor vessel support lugs through three holes drilled through the peripheral area of the plate.

Both grid plates contained 211 0.25-in.-diameter holes, located in a triangular array, to provide for positioning of the fuel elements. Four-hundred-twenty 5/32-in.-diameter holes for coolant flow were located on a hexagonal lattice around the fuel element positioning holes. These were straight-through holes in the bottom grid plate, but tapered outward 10° from bottom to top in the upper grid plate. Each grid plate contained an additional 42 holes on the outer periphery to position the internal reflectors.

The grid plates were designed to withstand a 35-g launch load with a 1.4 safety factor as specified for a flight-type reactor. This design provided a very high margin of safety for the S8DR operational requirements.

Hastelloy C was chosen for the lower grid plate material because of its low thermal expansion coefficient with the resulting low temperature coefficient of

reactivity. Since a high temperature coefficient was desirable at the outlet of the reactor for self-regulation, the upper grid plate was fabricated of Type 316 stainless steel.

d. Flow Distributor and Flow Baffle Plate

The coolant flow distributor and baffle plate are located in the inlet plenum below the lower grid plate. The flow distributor was a basket-shaped piece which split the flow coming into the lower plenum, prevented vortexing, and provided a constant pressure profile across the lower face of the flow baffle plate. The distributor had a wedge-shaped piece which faced the coolant inlet nozzle and two rows of 32 holes each around the sides. The unit was manufactured of 1/8-in. Type 316 stainless steel. The pressure drop across the distributor was about 1/2 psi. This did not give rise to any significant stresses in the material.

The coolant flow baffle plate was fabricated of Type 316 stainless steel. It had a diameter of 9.2 in. and a thickness of 0.5 in. Seventy-three holes were drilled through the plate in a configuration to give the desired flow distribution through the core. The exact configuration for the baffle plate was determined by hydraulic tests. The pressure drop through the baffle plate of ~ 2 psi did not result in any significant stresses.

e. Internal Reflectors

The reactor core vessel contained 42 internal reflectors to fill the void space (Figure 14) between the hexagonal-shaped fuel array and the circular vessel. Thirty of the internal reflectors were beryllium-oxide pieces clad in Type 316 stainless steel. The 12 smallest reflector pieces were solid Type 316 stainless steel.

The internal reflectors were designed for operation at 1350° F. Space had been allowed inside the cladding both for BeO swelling and for helium buildup from neutron reactions with the beryllium during reactor operation.

f. Core Support Structure

The core was installed in the vacuum vessel on a support structure as shown in Figure 15. The core support structure consisted of the base ring, a lower section, and an upper section. The base ring was bolted to the bottom of the containment vessel. The lower section consisted of three vertical legs welded

Figure 14. Partial Fuel Array in Reactor Core Vessel

to a top and bottom flange. Three leveling screws and three anchor bolts were provided on the bottom flange to level and anchor the lower section to the base ring. The upper section consisted of a cylindrical piece which provided a means of supporting the reactor vessel and the gamma shield, and three diagonal legs which were welded to the cylinder. The three legs were bolted to the upper flange of the lower section. The gamma shield and reactor core vessel sat in the upper section. Flanges on the vessel and support cylinder were locked together by a Marmon-type clamp. The core support structure also provided support for the inlet and outlet lines of the primary coolant system. The entire support structure was constructed of Type 304 stainless steel.

2. Reactor Reflector and Control Drums

The reactor core vessel was surrounded by a beryllium reflector, which was divided into two stationary and six movable sections. Reactor control was provided through the movable sections. Figure 16 shows a drawing of the reactor with reflector and drive assembly.

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Figure 15. Reactor Core Support Structure

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Figure 16. Schematic of Reflector and Drive Assembly

a. Reflector Assembly

The beryllium reflector assembly has a nominal thickness of 5-1/8 in. and a length of 18-1/2 in. The reflector assembly was divided axially into two halves. Each half consisted of a stationary reflector and three control drums together with drives, bearings, and support brackets. The reflector assemblies were attached to and supported by brackets which were an integral part of the reactor core vessel. Latching mechanisms were provided at the upper end of the assembly to lock the reflector halves together.

The length of the reflector assembly for the S8DR was increased over that used for the S8ER to provide additional reactivity. This increase was provided by stationary sections above and below the control drums, which were essentially the same as the S8ER drums.

The drums were shaped as segments of right-circular cylinders with 4-1/2 in. radii. The basic drum had a length of 15.3 in. and a maximum thickness of 2-1/8 in. A 3-in. beryllium shim was attached to the back of each drum to make up the total 5-1/8 in. thickness. Mechanical stops for the drum were positioned at $1^\circ \pm 1^\circ$ from full-in and at $135^\circ \pm 2^\circ$ for the out position. Each drum contacted a snubber at 109° from full-in.

All beryllium surfaces were anodized to provide them with a high emissivity. The outside beryllium surfaces (those not facing the reactor vessel) were coated with an additional high emissivity coating since the emissivity of the anodized coating had been found to degrade slightly at the high temperature and vacuum conditions of the S8DR environment. The reflector assembly was designed to limit the beryllium temperatures to less than 1350°F .

The anodized coating on the similar S8ER reflectors served also to inhibit corrosion. Since all high temperature operation for S8DR would be performed with the reactor in a 10^{-5} torr vacuum, no beryllium corrosion was anticipated.

Each control drum rotated in a pair of upper and lower self-aligning bearings. These bearings were a journal type with the journal bushings held in a spherical cartridge, which, in turn, moved in a socket to provide self-alignment. Contact surfaces between the ball and socket were coated with aluminum oxide and then covered with a molybdenum disulfide base dry-film lubricant. The balls were made of P-5 carbon-graphite. The shafts which ran in the ball were

also coated with dry-film lubricant. The bearings were designed for operation at 1150°F in an environmental pressure of 10^{-5} torr or less.

The maximum beryllium reflector temperature calculated for 600 kw operation at 1300°F core outlet temperature was 1240°F. At 1 Mw and 1100°F outlet temperature, the maximum beryllium temperatures reaches 1348°F, just below the design temperature. These peak temperatures would occur in the stationary beryllium cusps. The calculated maximum control drum temperature was estimated at 1283°F for 1-Mw operation. Temperature measurements of the similar S8ER reflectors indicated that the above calculations were conservative by as much as a factor of two in the amount of power dependence (irradiation heating) assumed. This factor would reduce the peak beryllium temperature (1 Mw operation) by as much as 100°F. Several thermocouples were located on both the stationary and the movable beryllium sections to monitor their temperature during operation.

The maximum control drum bearing temperature during 600 kw, 1300°F outlet temperature, operation was expected to be 1080°F. At 1000 kw, 1100°F outlet temperature, the maximum bearing temperature is expected to be approximately 1190°F, slightly above the original design criteria. Qualifications bearing tests were run at 1250°F to cover this condition. The carbon-graphite balls were restricted to operation below 500°F when in an oxidizing atmosphere. Maintenance of the 10^{-5} torr vacuum was necessary at full power and temperature operation to prevent oxidation of the balls, and, also, of the dry-film lubricant coating.

b. Control-Drum Actuator

The S8DR control-drum actuators used the same operational principle as did the SNAP 10 actuators. The S8DR actuators, however, were designed for the higher operating temperatures of the SNAP 8 environment. Figure 17 shows an actuator and drive mechanism.

The control-drum actuator was an 8-pole reluctance motor which produced torque by the interaction of magnetic forces between stationary teeth on the fixed poles and the notched teeth cut into the cylindrical rotor. The field coils for the poles were sequentially energized to produce a revolving magnetic field which turned the rotor. The stator-pole tooth alignment was such that, as each pole is energized, the rotor teeth were in a position to provide maximum torque.

Figure 17. Control-Drum Actuator and Drive Mechanism

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The brake contained a solenoid assembly which held the actuator rotor fixed while de-energized and released it for movement when energized. The braking disk attached to the actuator rotor contained 100 teeth which mated with the 50 teeth on the bellows-supported disk when the brake was applied. One step of the actuator was a turn through 3.6° or one tooth-pitch. This was geared through a 13.8 reduction to the control drum, which made a 0.26° step. A breakdown drawing of the actuator assembly is shown in Figure 18.

The actuator assembly was designed to operate at 1025°F in a pressure of 10^{-5} torr or less. In order to minimize differential expansion problems over the operating temperature range of the actuator, a single material was used for the fabrication of structural and magnetic components. The material chosen was Hiperco 27, a cobalt-iron alloy which has the required structural and magnetic properties. An exception was the flexure assembly of the brake, which was Inconel X. Bearing surfaces were coated with aluminum oxide and run in carbon-graphite bearings. The magnetic coils were wound with stainless-steel-clad copper (oxalloy) wire. The wire was insulated with a glass yarn and encapsulated with a ceramic encapsulant. The brake teeth were coated with MoS_2 dry-film lubricant to prevent adhesion during high temperature and vacuum operation.

The maximum actuator operating temperatures were calculated to be 940°F for operation at 600 kw and 1300°F average core outlet temperature and 945°F for 1000 kw and 1100°F outlet temperature. Operation at 10^{-5} torr pressure or less was required to prevent oxidation of the carbon-graphite bushings and the dry-film lubricant.

Coil pairs, which operated simultaneously, were wired in parallel so that failure of one coil did not prevent operation of the actuator.

c. Drive and Scram Mechanism

The drive and scram mechanism for S8DR provided the link between the actuator and the control drum. Each unit consisted of a drive gear, two concentric shafts, an electromagnetic clutch, and a scram spring. The mechanisms are shown in Figure 17 and 19.

The scram spring and its release clutch operated through concentric shafts. The outer shaft was attached to the lower half of the clutch and extended downward

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Figure 18. S8DR Control Drum Actuator

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Figure 19. Reflector Control System
Scram Mechanism

to the gear which was driven by the actuator. The inner shaft was connected to the upper half of the clutch, which contained the magnet coils, and extended down to attach to the control drum through a flexible coupling. The lower portion of the clutch assembly contained the clock-type scram spring. The outside end of the scram spring was connected to the spring housing and the inside end was attached to the inner shaft. An interlock prevented the drums from operating without first winding the scram spring and energizing the clutch. The clutch magnet was faced with teeth which mated with the teeth on the lower housing when the clutch closed.

During normal operation, the entire clutch assembly was locked together and rotated as a single unit. The drive path from the actuator was through the rotor pinion to the large diameter drive gear, up through the outer tube, through the clutch, and down through the inner shaft to the drum.

To scram a drum, power to the clutch magnet was interrupted, and the clutch disengaged by gravity and by the bellows spring on the lower housing (Figure 19). With the clutch open, the scram spring unwound to rotate the control

drum out. The spring was rewound by driving the actuator in the drum-out direction with the clutch open. With the drum already out against the snubber, the actuator drove only the outer shaft and spring housing, and thus rewound the spring. A limit switch on the spring housing was tripped when the spring was fully wound. This allowed the clutch to be energized and closed for normal drum operation.

In the event of an actuator failure, the capability for manually setting a control drum to a predetermined position was provided. This shimming was accomplished by shutting down the reactor, entering the vacuum vessel, and remotely cranking the control drum to the required position. This operation was administratively controlled such that the shutdown criteria would not be compromised.

The scram mechanism was designed to operate at temperatures up to 1000° F in pressures of 10^{-5} torr or lower. The scram spring was designed for an allowable load relaxation of 25%. The design criteria for drum scram times were 0.5 sec, maximum, to move the drum 31° and 2.0 sec, maximum, to move the drum full-out. These times were measured from the moment that power to the scram bus was interrupted. The scram times actually measured for the assemblies were 1/4 sec for 23° and 1/2 sec for full-out, well within the design requirements.

The scram spring was fabricated from Rene' 41, a high temperature spring alloy of the nickel-cobalt class. It was coated with MoS₂ dry-film lubricant to prevent any sticking tendency between the leaves of the wound spring. The teeth of the clutch mating surfaces were also covered with dry lubricant to prevent adhesion during the high temperature and vacuum operation. The magnet coil was wound with chrome plated copper wire, insulated with a glass yarn, and encapsulated in a ceramic encapsulant.

The scram mechanism was failsafe because a power failure would automatically open the clutch and result in control-drum scram. Also, in the event of burnout of the clutch coil, the mechanism would automatically scram the drum. Because of the lower operating temperature of the scram clutches as compared to the actuator mechanisms, these units were not redesigned to replace the chrome plated windings.

The scram clutch housing was expected to operate at 840°F during 600 kw, 1300°F outlet temperature operation, and at 860°F for 1000 kw, 1100°F outlet temperature.

d. Snubber Assembly

The snubber assembly contained a spring which absorbed the control-drum energy when it was scrambled. An arm on the drum drive shaft struck the snubber rack which pushed against the snubbing spring. A spring-loaded pawl engaged the teeth of the rack as it was pushed in and prevented the spring from bouncing the drum back from the snubber region. After the drum was driven out of the snubber region it tripped the pawl to release the snubber rack.

Three limit switches were mounted on a bracket attached to each snubber assembly. One switch indicated when the drum was full-in. The other two indicated when the drum was full-out and when it was beyond full-out and in the snubber zone.

e. Position Sensor

The position sensor was a rotary variable reluctance device which was attached to the drive mechanism. The sensor operated from a constant a-c excitation and generated an amplitude and phase-varying output which was proportional to the angular position of the sensor. The output signal from the sensor was transmitted to a demodulator, which converted it into a 0 to 5 vdc signal. This d-c signal was used by the coarse position indicator to show drum position, and, also, by the precision drum-position indicator to verify that the drum had moved. The position sensor is shown in Figure 17.

The design and material selection for the position sensors was very similar to that for the drive actuators. Hiperc 27 was used as both a structural and a magnetic material in the fabrication. The coils were wound with stainless-steel clad copper wire. The units operated below 700°F during both 600 and 1000 kw operation.

3. Reactor Installation

The reactor was installed within a vacuum chamber in a shielded test pit below the test vault floor level (Figure 20). A vacuum of 10^{-5} torr, or better, was established and maintained within the chamber by two mechanical pumps

Figure 20. S8DR Test Facility

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and two oil diffusion pumps located in an equipment room adjacent to the test vault. The vacuum chamber was pumped through a 5-ft diameter, 63-ft long pumping duct, which penetrated the vault west shield wall, made four right angle turns, and rose through a shielded "chimney" in the vacuum equipment room. The room beneath the vacuum equipment room was filled with sand to provide the necessary radiation shielding.

All radioactive primary NaK piping and equipment was located within the sealed test vault. All nonradioactive secondary coolant loop piping and components, except for the secondary drain system and piping from the intermediate heat exchanger, were located in accessible areas at grade level.

Normal electrical equipment received 460-volt service directly from the electrical substation. Emergency equipment, which could not be left without power for more than a few minutes, was automatically supplied from a 200-kw, natural-gas-powered, emergency generator. On loss of electrical power, the emergency generator started and picked up the load through an automatic transfer switch within 13 sec.

Critical test and facility equipment, which could not be off even momentarily without causing a scram or other test interruption, were supplied electricity from an uninterruptible power supply (UPS). A 600-hp motor drove the 400-kw UPS generator which was also coupled to a diesel generator through an electromagnetic clutch. On loss of electrical power to drive the motor, a 9-1/2-ton flywheel at 1750 rpm maintained the motor-generator at 1200-rpm synchronous speed through an eddy-current coupling, while the diesel generator started and came up to speed. When the diesel engine reached synchronous speed, the electromagnetic clutch automatically energized and picked up the motor-generator load, so that UPS buses never lost power. After site power returned, the UPS was returned to the standby mode manually.

A prototype SNAP 8 shadow shield was mounted directly underneath the reactor. The purpose of the shield was to reduce the neutron and gamma flux at flight system instruments and sensitive components.

The shield used for the S8DR ground test was modified to accommodate test conditions. The lithium used was the Li^7 isotope, which has a slightly smaller neutron absorption cross section than Li^6 and offsets the added neutron flux

reaching the shield by scattering. To further offset the added load due to ground test conditions, the distance between reactor and shield was increased slightly.

The shield case was modified by connecting a pipe, for measuring and adjusting shield pressure, and for routing ground test instrumentation leads. The entire shield was encased in a confinement vessel for secondary containment in event of a leak.

4. Heat Transfer Systems

Auxiliary systems used in ground testing S8DR included the primary heat transfer loop, intermediate heat exchanger, and secondary heat transfer loop.

a. Primary Loop

The primary loop consisted of the NaK coolant, the reactor core vessel, the primary surge tank, the intermediate heat exchanger, the primary system pump, a parasitic load heater, and associated instrumentation and piping. NaK was pumped through the coolant passages in the core, where it absorbed thermal energy. The hot NaK then flowed through the surge tank to the intermediate heat exchanger where the thermal energy was transferred to the coolant in the secondary heat transfer loop. The pump then received the cooled NaK and pumped it back to the reactor.

The primary surge tank was mounted at the high point of the primary system. NaK entered the surge tank through a normally submerged side nozzle and exited through a bottom nozzle fitted with a vortex breaker. An Inconel "J" tube provided remote level measurement. The primary pressure relief system consisted of a rupture disc and pressure relief valve acting through a locked-open gate valve to a knockout drum (with a demister to separate entrained NaK from vent gas) and holdup tank. Overpressurization through the helium supply station was prevented by automatic closure of the helium valve on high pressure.

The primary cover gas sampling system was an auxiliary gas loop which allowed sampling for Xe^{135} , Xe^{133} , and other fission gases. The surge tank helium cover gas was drawn from the surge tank through a NaK trap, gas blower, and sample chamber, then returned to the surge tank. Cover gas samples were taken periodically by isolating, removing, and replacing the

sample chamber. The gas was analyzed periodically with a multichannel gamma spectrometer while the gas was circulating.

The intermediate heat exchanger was a counter-flow spiral tube-and-shell unit with a design rating of 600 kwt at a coolant flow of 49,000 lb/hr on each side. Primary NaK flowed through the tube side and secondary NaK flowed through the shell side. The heat exchanger was Type 316 stainless steel all-welded construction, designed for 1450° F at 100 psig.

The primary NaK pump was a three-phase linear induction electromagnetic pump with a coolant flow capacity of 140 gpm with a 40-psi head. The pump was vertically mounted with coolant flow in the upward direction. Flow was controlled by adjusting power input to the stator coils through an autotransformer. Stator coils were cooled by a blower which circulated ambient vault atmosphere over the coils.

The parasitic load heater was a clamshell design with a nichrome wire heating element, with 30-kwt capacity. The heater was used to supplement the secondary heater during nonnuclear thermal testing, and to make small adjustments for fine control of reactor inlet temperature.

b. Secondary Loop

The secondary loop consisted of the secondary NaK coolant, the intermediate heat exchanger, the airblast heat exchanger (ABHX), the secondary heater, the secondary surge tank, the secondary system pump, and associated instrumentation and piping. NaK was pumped through the shell of the intermediate heat exchanger, where it absorbed thermal energy from the primary NaK. The hot NaK then flowed through the coils of the ABHX, where excess heat was dumped to the environment, then the cooled NaK passed through the secondary surge tank and returned to the pump to complete the cycle. When the reactor was not supplying heat, for example, following a reactor scram, the NaK loops were heated with the secondary heater by diverting secondary flow from the ABHX to the secondary heater. Flow could be divided between the ABHX and heater in any proportion, but a small "trickle" flow was normally maintained at all times, to keep the unused component hot and prevent cold trapping of impurities in the coolant.

TABLE 5
S8DR THERMAL AND HYDRAULIC
DESIGN SUMMARY

Parameter	Reactor Power Level	
	600 kw	1000 kw
Reactor Coolant	NaK-78	NaK-78
Coolant Temperatures (°F)		
Reactor Inlet	1100	830
Reactor Outlet	1300	1100
Coolant Flow Rate (lb/hr)	48,800	60,000
Channel Coolant Velocity (ft/sec)		
Nominal	4.6	5.6
Maximum	6.7	8.2
Flow Area (ft ²)	0.065	0.065
Heat Flux (Btu/hr-ft ²)		
Average	47,000	78,000
Maximum	83,000	138,000
Heat Transfer Area (ft ²)	43.6	43.6
Coolant Pressure, Reactor Inlet (psia)	35.0	40
Coolant Pressure Drops, Including Static Heads (psi)		
Across Reactor	4.1	6.2
Across Pump	-18	-27

Figure 21. S8DR Peak
Fuel and Cladding
Temperature

The ABHX consisted of two units connected in parallel, only one of which was required for operations up to 600 kw. The units were slightly different in design, but each was equipped with a variable speed blower which circulated outside air up through a tube bundle through which the hot secondary NaK was flowing. Manually-adjusted louvers over the top of the tube bundle provided additional control flexibility.

The secondary heater was an in-line, immersion-type unit designed to deliver 150 kw with 140 gpm flow. Power was supplied through a variable output transformer which was interlocked with the heater inlet valve and secondary NaK flowmeter, to prevent operation without sufficient flow.

The secondary surge tank was slightly more than twice as large as the primary surge tank. Separate nozzles were provided for NaK inlet from the ABHX and from the secondary heater. NaK exited from the tank through a bottom nozzle fitted with a vortex breaker. An Inconel "J" tube provided remote level measurement. Overpressure protection was provided by a rupture disc which relieved to the building exhaust stack.

The secondary NaK pump was identical in operation and design to the primary pump. However, pumping direction was down instead of up.

5. Thermal and Hydraulic Design

The thermal and hydraulic design operating conditions for the S8DR are summarized in Table 5.

a. Thermal Characteristics

Figure 21 shows the peak cladding and fuel temperatures during S8DR operation. In this context, "peak" refers to the nominal predicted value at the highest temperature location in the S8DR fuel element (i. e., these values do not include uncertainty factors). Table 6 presents the uncertainties associated with these calculated temperatures. These uncertainties are estimated to be outside (3σ) limits for the calculations.

The overall heat transfer coefficient, UA , for the S8DR core was estimated at $5.70 \text{ kw/}^\circ\text{F}$, where UA is defined as the reactor thermal power divided by the average temperature difference in the core between fuel and coolant. Because of the low hydrogen pressure in the fuel-to-cladding gas gap at low

TABLE 6
S8DR TEMPERATURE UNCERTAINTIES

Peak Fuel Temperature Uncertainty Factor	Temperature Uncertainty (%) ^a	
	BOL ^b	EOL ^c
Gas gap conductance (gap thickness and thermal conductivity)	5.3	17.5
Velocity profile	8.3	8.3
Power profile	6.0	6.0
Fuel conductivity	4.0	4.0
Core power	3.5	3.5
Local flow ^d	9.0	9.0
Other	3.1	3.1
Total ^e	15.9	23.0

Peak Cladding Temperature Uncertainty Factor	Temperature Uncertainty (%)
Velocity profile	10.0
Power profile	6.0
Core power	3.5
Local flow ^d	17.0
Total ^e	20.9

- a) Defined as the tolerance on the difference between the coolant inlet temperature (1100°F) and the peak temperature of the fuel or cladding.
- b) Designates beginning of life.
- c) Designates end of life.
- d) Worst case of zero spacing between an element and its six adjacent neighbors for 1/2 the core length.
- e) Square root of sum of squares.

temperatures, UA is only about 0.1 kw/°F at 100°F. Around 800°F, the effect of H₂ pressure causes UA to increase abruptly to the operating value. The fuel material has a thermal conductivity of 13 Btu/hr-ft-°F initially and has been estimated to decrease to 10 Btu/hr-ft-°F at end of life. The convective heat transfer coefficient, h, is 2900 Btu/hr-ft²-°F. Because of the expected fuel swelling and consequential gas gap decrease, UA is expected to increase slightly

during reactor lifetime even though fuel thermal conductivity decreases. Specific heat for the fuel material is 0.16 Btu/lb-°F. The time constant (UA/WC) for the fuel is about 4.4 sec.

b. Hydraulic Characteristics

Figure 22 presents the average radial flow profile for the S8DR core. This coolant flow distribution was designed primarily to minimize the peak cladding temperature with a slight compromise to reduce peak fuel temperature.

The radial profile of the coolant flow through the S8DR core was designed to maximize core lifetime. More specifically, the design flow distribution was a compromise between minimizing the peak cladding temperature, which results in maximum cladding strength, and minimizing the peak fuel temperature, which maximized the time to appearance of β -phase zirconium hydride in the fuel. This phase change is accompanied by accelerated fuel swelling. The fuel element was not designed to accommodate this increased swelling, and thus, the attainment of β -phase was a design limitation for reactor life. Since β -phase formation is a consequence of hydrogen loss, the end-of-life (EOL) temperature profile is the important one. Figure 21 shows it to be considerably flattened. The temperature profile change results from the flattening of the power distribution with time. The main cause of power flattening is the inward movement of the control-drum reflectors during reactor lifetime.

Figure 22. S8DR Average Flow Profile

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C. ADVANCED ZIRCONIUM HYDRIDE REACTOR

The Advanced Zirconium Hydride Reactor was designed as a heat source for a 25-kwe space power system using thermoelectric power conversion. It was conceived for use in conjunction with the Manned Orbital Workshop and was shielded in accordance with the requirements of that program. This degree of shielding, which permitted rendezvous missions to be flown, resulted in certain unique design features such as poison-backed control drums.

While considerable study, component development, and testing was performed in support of this system, a test reactor was never built because of cancellation of the program.

1. Requirements

The reactor system was based to the maximum practical extent on S8DR and other state-of-the-art SNAP technology. In particular, the basic S8DR fuel element was used except that certain modifications necessary for extending the fuel lifetime were allowable. As described in Section III-C-2, the major modifications consisted of increasing the fuel hydrogen content from an N_H of 6.05 to 6.3, the cladding thickness from 0.011 to 0.015 in., and internal clearances between fuel and cladding, and adding a burnable poison on the surface of the fuel or in a foil in the radial-gap region. A final modification might have been the substitution of Incoloy-800 cladding for Hastelloy-N, which was chosen for the current reference design.

The reactor system was to have a minimum lifetime capability of 20,000 hr at 600 kw, with 1100°F core-inlet and 1300°F core-outlet temperatures. Under these operating conditions, the system reliability objective would be 0.995 with no credit for possible maintenance. The reactor would also be capable of operation under alternate conditions of power, temperature, and lifetime. The design parameters are given in Table 7.

At any given power level and temperature, the fuel-element design was optimized to maximize the reactor lifetime. In general, the limit was considered to be the simultaneous occurrence of β -phase fuel in the worst-cast (0.5%) element and a 0.5% probability of sufficient fuel growth to crack the cladding.

TABLE 7
REFERENCE ZrH REACTOR DESIGN PARAMETERS

<u>1. General Performance</u>		<u>3. Reactor Vessel</u>	
Design thermal power	600 kw	Outside diameter	22.0 in.
Design outlet-temperature	1300°F	Vessel wall thickness	3/16 in.
Temperature rise	200°F	Inside diameter of dry-wells	4.62 in.
Design lifetime	26,000 hr	Drywell wall thickness	0.065 in.
Coolant flowrate	49,000 lb/hr	Number of inlet and outlet nozzles	4 ea
Pressure drop	0.65 psi		
Operating pressure capability	35 psia		
<u>2. Fuel and Core Internals</u>		<u>4. Reflector Control System</u>	
Number of fuel elements	295	Control-drum type	Reflector-poison
ZrH hydrogen content	6.3×10^{22} atoms/cm ³	Number of control drums	10
Uranium content	10.5 wt %	Reflector material	BeO
Hydrogen-barrier material	SCB-1	Poison material	60% Eu ₂ O ₃ in Ni
Cladding material	Hastelloy-N	Structure material	Nb-10 W
Cladding thickness	0.015 in.	Cladding material	Nb-1 Zr
Burnable poison material	Gd-155	Drum diameter	4.5 in.
Active fuel length	16.8 in.	Drum length	19.0 in.
Overall (flat-to-flat) element length	17.5 in.	Structural strongback thickness	0.25 in.
Outside diameter of fuel element	0.570 in.	Cladding thickness	0.030 in.
Fuel-element spacing (variable for flow control)	0.025 to 0.045 in.	Drum bearing shaft material	Ta-10 W
Internal reflector material	BeO	Control-drum actuator	Modified S8DR
Internal reflector cladding	316 SS	Control-drum bearings	Modified S8DR
Gridplate material	316 SS		
Core shell, inside diameter	11.4 in.	<u>5. Shield</u>	
Core shell thickness	0.030 in.	High-temperature gamma shield	Molten Lead
Core shell material	316 SS	Lead container material	Croloy 2-1/4
Reactor coolant material	NaK-78	Low-temperature gamma shield	Uranium
		Neutron-shield material	LiH
		LiH container material	316 SS

The structural design of the reactor system was based on the most stringent shock and vibration requirements of either a Saturn 1B, Saturn V, Titan 3C, or Titan 3F launch vehicle. During reactor operation in orbit, the system would be capable of withstanding the acceleration loads during normal attitude-change maneuvers and vehicle docking. Since the exact requirements for these loads are mission-dependent, for this design they were established as ± 1 -g axial and $\pm 1/4$ -g radial acceleration, and 3-g shock. The reactor would be capable of operating in a variety of environments ranging from ground test facilities in 10^{-5} torr vacuum to deep-space vacuum. The reactor system would operate

in either a shadow or 4π shield environment. Supplemental cooling of the 4π shield to maintain acceptable shield temperature or provide a heat sink for the reactor control drums was allowed, but only if designs employing inherent shield and control-drum cooling were determined to be impractical.

The reactor system would be capable of automatic startup and unattended operation following the receipt of a single startup command signal. It would be capable of 50 startup and shutdown cycles in which the maximum rate of change of coolant temperature would be $150^{\circ}\text{F}/\text{min}$ from room temperature to 400°F , and $60^{\circ}\text{F}/\text{min}$ to 1300°F .

The inlet-to-outlet nozzle pressure drop through the reactor was as low as practicable and would not exceed 1.0 psi at 49,000 lb/hr NaK flow (600 kwt, 200°F ΔT). All pressure-containing members would be designed to conform to the ASME Pressure Vessel Code, Sections III and VIII. The design pressure for the system was 35 psia.

2. System Design Description

This section provides a summary design description of the reference reactor system including fuel elements and integration with a typical 4π shield. Detailed design and performance descriptions of system components are given in later sections.

a. Overall Reactor Vessel

The reference design of the reactor vessel is shown in Figure 23. In this design the cooling for the shield in a 4π application and for the control drums was provided by radiation to surfaces cooled by the inlet NaK. This was done by locating the control drums in dry wells surrounding the core and causing the inlet NaK to flow upward through the cusp-shaped regions outside the dry wells, and between the outer pressure-vessel and inner core-can. The NaK entered the vessel through multiple inlet nozzles in the vicinity of the lower control-drum bearings, passed through openings provided in the lower dry-well support ring, and continued upward to the top of the vessel through the cusp-shaped regions described above. The flow then passed to the core inlet plenum through the fuel-element array and exited through multiple outlets in the core outlet plenum. The inlet NaK stream was separated from the NaK in the core by means of a 0.030-in. -thick core-can. The core-can was welded directly to the

8-A30-075-2

Figure 23. Zirconium Hydride Reactor Reference Design

vessel lower structure. The upper end of the core-can was free to move relative to the core upper gridplate to accommodate differential thermal expansion. As the core-can separated the inlet from the outlet NaK a thermal shunt of a fraction of the core power occurs. The quantity of heat transferred was determined to be 6% of the core power under the base point operating conditions. The net result of this effect was to increase the temperature of the NaK entering the core relative to the reactor system NaK inlet temperature. This effect, plus the heat resulting from control-drum cooling, resulted in an increase in NaK inlet temperature of only 18°F. The penalty was manifested in an increase in fuel temperature of 6°F to attain the 1300°F mixed-mean, core-outlet temperature.

To meet the power and lifetime requirements, a core consisting of 295 fuel elements of the S8DR type was selected. The core reactivity was controlled both by Gd-155 burnable poison selectively loaded in the central region of the core and ten poison-back beryllium oxide control drums. The reference poison material for the drums was 60 wt % natural europium oxide dispersed in a nickel matrix.

The control-drum bearings consisted of a tantalum-10% tungsten inner shaft and outer socket rotating against a P5 graphite ball. The shaft and socket were flame-spray-coated with an ~ 0.003 in. thickness of aluminum oxide where they contact the graphite ball. Thrust loads were carried by the upper bearing. This basic bearing type has been used successfully on various SNAP reactors including S8DR with the major exception that the metal parts were previously Inconel. The tantalum-10% tungsten was an improvement, which gave superior performance due to a better match of thermal-expansion coefficients.

Cooling was provided in the lower bearing by spring-loading the bearing socket to maintain thermal contact with the NaK-cooled bearing housing. Thermal contact in the upper bearing was provided by a combination of spring-loading the bearing socket into the threaded stainless-steel housing, and a jam nut which provided positive loading of the housing into the threaded dry well. The bearing sockets were spring-loaded with René 41 Belleville washers bearing against snap rings.

To provide the desired low pressure drop for the core, a variable pitch fuel-element array was used. Previous SNAP 8 reactors used orifice-type devices in the core inlet-plenum to provide the required matching of coolant mass-flow, velocity-to-core-power distribution. In this ZrH reactor, the flow areas between elements were adjusted to obtain the required matching. The preliminary spacing ranged from 0.045 in. in the center of the core to 0.025 in. at the periphery.

Neutron-reflector elements were used both in the irregular area defined by the outermost row of fuel elements and core-can, and in the cusp regions between control-drum dry wells. The purpose of the reflector elements was to increase the reactor excess reactivity and, hence, the allowable poison loading. This, in turn, minimized the reactivity-change requirement of the control drums.

As in S8DR, the core internal reflectors were D-shaped BeO rods canned in austenitic stainless steel. At locations where the BeO rod dimensions become unacceptably small, solid stainless-steel rods were used. The fixed reflector elements in the flow area between dry wells were of two sizes dictated by the dimensions of the inner and outer cusp regions. These reflector

elements were BeO clad in stainless steel for the reference design. Unclad beryllium metal was a backup design approach.

All NaK-wetted materials with the exception of the fuel elements were fabricated from Type 316 stainless steel. The conceptual configuration and sizing of the pressure vessel were based on preliminary detailed stress analyses using the AVCO computer code. Allowable-stress values were based on the ASME Pressure Vessel code, Sections III and VIII.

Both steady-state and startup transient stresses were considered. With the exception of the core outlet plenum, all pressure members were maintained at the NaK-inlet temperature. The outlet plenum operated at the core-outlet temperature. Differential expansion of the outlet-plenum relative to the remainder of the vessel was accommodated by the inherent flexibility of the design configuration. The structural studies also showed that reduced stresses resulted when the control-drum dry-well structure was solidly attached to the lower vessel structure rather than freely suspended from the upper head. Hence the solid attachment approach was selected.

In the design of the vessel, x-ray-inspectable butt-welded joints were used exclusively with the exception of the final closure weld. This closure was made at the core outlet plenum after the fuel elements and lower gridplate were installed. Although the problem of x-ray film and source placement makes x-raying of this joint of limited value, dye-penetrant and ultrasonic methods may be used.

The weight of the reference ZrH reactor with the addition of control-drum actuators was 1420 lb. The volume of NaK contained within the vessel was 1.75 ft³.

b. Fuel

The reference fuel element design is shown in Figure 24. The basic features consisted of the zirconium hydride fuel pin containing 10.5 wt % uranium, a 0.015-in. -thick Hastelloy-N cladding tube coated on the inside with a ceramic hydrogen barrier, and a gadolinium oxide burnable poison vapor-deposited on the surface of the fuel rod. The hydrogen density of the fuel rod was 6.3×10^{22} atoms/cc (i. e., $N_H = 6.3$).

8-JY19-119-3

Figure 24. Reference Fuel Element

The design represented an evolution from the S8ER and S8DR designs. The significant differences between the reference design and the element used in S8DR were an increase in N_H from 6.05 to 6.3 and in cladding thickness from 0.011 to 0.015 in., the use of Gd_2O_3 poison on the fuel rather than Sm_2O_3 in the ceramic, and increased radial and axial clearances. A device to dampen the effects of launch shock and vibration would also have to be included in the fuel element.

The 40% additional fuel elements in the reference reactor resulted in a lower core-power density than in S8DR for comparable power levels. This resulted in lower fuel-element temperatures and was the principal method used to extend the fuel life from the S8DR design value of 12,000 hr to the present one in excess of 20,000 (at 600 kwt, 1100°F inlet, 1300°F outlet). The predominant life-limiting factor was hydrogen loss from the fuel element. This factor represented the major component of core-reactivity loss. Excessive hydrogen loss can also result in a fuel material transformation to the beta phase.

The hydrogen loss rate was governed by the hydrogen pressure in the gas annulus in the fuel element and by the properties of the ceramic permeation barrier both of which are strongly temperature-dependent. The average fuel temperature in the reference reactor was 1300°F at the nominal operating point

as compared with 1330°F for the S8DR at the same operating conditions. This relatively small temperature difference would result in about a 50% change in the hydrogen-dissociation pressure for fuel with an N_H of 6.3.

D. REACTOR CORE TEST

With the completion of the S8DR test, it became evident that the design point for the SNAP 8 reactor concept was in need of revision. It was necessary to limit maximum fuel temperatures to less than 1400°F and to provide a positive mechanism to restrict fuel element bowing. Hastelloy N was dropped as the reference cladding material because of its poor ductility. The requirement for man-rated shield was deleted and, hence, the poison-backed control drum was dropped.

The design summary is presented in Table 8. The new configuration had 0.670-in. element-to-element pitch, 0.032-in. -thick Incoloy 800 cladding, fin heights of 30 and 15 mils in the inner and outer zones, respectively, and fin-on-tube element support with elements arranged in a triangular pitch having one element with fins spiraled right, one spiraled left, and one with no fins (R-L-N configuration). The core had an uncoated cladding ID of 0.576 in. (inner zone) and 5.591 in. (outer zone), fuel meat diameter of 0.560 in. (inner zone) and 0.575 in. (outer zone), and active fuel length of 18.00 in. A six-drum, void-backed reflector configuration was chosen with control drums which are full core length, 6.25 in. thick, and have a 6.25 in. radius of curvature. The design employed a fin geometry which inherently promotes high interchannel mixing efficiency through elimination of flow opposition characteristics of prior designs. The core excess reactivity and control provided by the selected geometry was considered adequate to meet design requirements.

The 0.032-in. -thick Incoloy cladding with a 0.0025-in. glass coating thickness provided a 43% margin in the cold, assembled condition and 50% in the hot operating condition based on cladding proportional limit stresses. The stress analysis did not include core "clamping" loads, thermal gradients incurred in startup, effect of fin undercutting to provide needed clearances for element bowing, or the effect of "loose" neutral elements which may be involved due to zone boundary geometry mismatch.

TABLE 8

REACTOR CORE TEST CONCEPTUAL MODEL DESIGN SUMMARY

Nominal thermal power	600 kw
Number of elements	295
Lattice pitch	0.670 in.
Inlet temperature	1065° F
Outlet temperature	1200 ±20° F
Reflector type	Void-backed Be
Inner reflector type	Clad BeO
Core diameter	12.580 in.
Core length (active fuel)	18 in.
Fuel rod diameter (inner, outer)	0.560, 0.575 in.
Fuel cladding	0.032 in. Incoloy 800
Barrier	SCB
Gap	6 mils (radial)
Fin height (inner, outer)	30, 15 mils
Fuel element spacing - nominal (inner, outer)	30, 15 mils
Poison	Eu ¹⁵¹ + Gd ¹⁵⁵
Fuel	ZrH + 10.5 wt % U ²³⁵ , N _H = 6.3
Fuel element dimensions	
ID (inner zone)	0.576 in.
(outer zone)	0.591 in.
OD neutral elements (inner zone)	0.640 in.
(outer zone)	0.655 in.
Fin OD (inner zone)	0.700 in.
(outer zone)	0.685 in.
Fin length	15.5 in.
Fin spiral	R-L-N
Fin pitch	5.25 in. /turn
Fin relief cut	
Hot end 6.5 in.	12 mils
Center 2.0 in.	0 mils
Cold end 7.0 in.	8 mils

E. SPACE POWER FACILITY (SPF) REACTOR

The last reactor design executed in the SNAP 8 family was intended for use in a test program at the NASA Space Power Facility (Plumbrook) and was referred to as the SPF reactor. The design summary of this reactor is given in Table 9.

It may be noted that the cladding material had been selected to be Hastelloy X with Incoloy 800 as a backup. Right-left-neutral fins were used, and the core design allowed for two regions with different provisions for swelling.

Delta-phase zirconium hydride was selected over epsilon phase because of the lesser problems with fuel swelling. A core temperature rise of 75° F was selected to minimize hydrogen redistribution reactivity effects. A pre-poison loading of natural gadolinium and europium-151 was chosen to give the desired objectives minimizing the beginning-of-life to end-of-life reactivity change while maintaining a reasonable negative temperature coefficient.

TABLE 9
SPF REACTOR DESIGN SUMMARY

Reactor Core Design Summary	
Number of fuel elements	199
Lattice pitch	0.737 in.
Core vessel ID	11.6 in.
Active fuel length	18 in.
Number of elements (inner, outer zones)	85, 114
Fuel rod diameter (inner, outer zones)	0.635, 0.650 (0.625, 0.640)* in.
Cladding material	Hastelloy X (Incoloy 800)
Cladding thickness (average)	28 (33) mils
Hydrogen barrier material	SCB
Hydrogen barrier thickness	2 mils
Fuel-to-barrier gap (diametral)	12 mils
Fin type	Right/left/neutral
Number of fins per element	3
Spiral pitch	5.5 in.

*Numbers in parentheses refer to backup design using Incoloy 800 cladding.

TABLE 9
SPF REACTOR DESIGN SUMMARY

<u>Reactor Core Design Summary (Cont'd)</u>	
Fin height (inner, outer zones)	27, 12 mils
Fuel element spacing (inner, outer zones)	30, 15 mils
Active core diameter	10.92 in.
Active core volume	27.61 ℓ
Fissile mass	11.22 (10.87)* kg U ²³⁵
N _H of fuel	5.80
<u>Reflector Design Summary</u>	
Control drum type	Reflector - void
Drum radius	3.52 in.
Drum material	Be metal
Number of drums	8
Shim capability	±1 in.
Drum envelope diameter	26 in.
<u>Thermal Performance Summary</u>	
Thermal power	300 kw
Average power density	10.86 kw/ ℓ
Average specific power	26.75 (27.60)* kw/kg U ²³⁵
Average linear power	1.005 kw/ft
Radial peak-to-average power	1.37
Axial peak-to-average power	1.35
Outlet temperature	1200° F
Core temperature rise	75° F

IV. DEVELOPMENT AND TEST PROGRAMS

Information necessary for the design of SNAP 8 reactor systems was obtained through component development and test programs, reactor critical assembly tests, and reactor system tests. The incorporation of these data into the base-line design resulted in a steady evolution over the life of the program.

A. DEVELOPMENT PROGRAMS

The most significant development program for SNAP 8 was that of the fuel element. The concept of the fuel material, uranium - zirconium hydride, had been developed under the SNAP 2 and 10A Programs. The SNAP 8 operation, however, required a much higher level of performance from the fuel element which necessitated studies of the fuel material, cladding, and hydrogen permeation barrier. In-pile irradiations of single elements were performed to gather information on the fuel element system. The reactor system tests provided data on the performance of 211 elements in the total system environment. The results of this work are described in Reference 1.

The control of the SNAP 8 reactors is provided by rotating control drums. Their use required the development of rotary actuators, bearings, limit switches, and position transducers that would operate successfully for the required combination of time, temperature, vacuum, and radiation imposed by the environment. Fundamental tests were performed on materials, components were designed, and tests were performed on these components under simulated operating conditions. Qualification tests indicated that all of the requirements of the SNAP 8 system could be met. This program is discussed in References 2 through 4.

The radiation shielding requirements established the need for both neutron and gamma attenuating materials. Lithium hydride had been selected early in the SNAP program as the most suitable neutron shield. Uranium-molybdenum alloys, tungsten alloys, and lead were considered for the gamma ray shield. The program covered materials studies to establish the characteristics and compatibilities of the shield materials and their containment. Fabrication studies were conducted to establish means of constructing the shields with a

maximum density and free from cracks. The details of this program are contained in Reference 5. The analytical program for the design of the shield is contained in the same reference.

B. REACTOR ANALYSIS

The design point selection of the SNAP 8 reactor became, by late in the program, a complex trade study in the stress, heat transfer, fuel performance, and nuclear design areas. The design of the S8ER was based on the S2ER data, the critical assembly experiments, and some limited laboratory data on the fuel material. The S8DR initial design did not have too much additional data, but was revised as the fuel examination data became available from S8ER. The design of the last of the conceptual reactors, the SPF reactor, had the advantage of a detailed model of the fuel performance based upon the S8DR, together with extensive information on the characteristics of the burnable poisons.

Extensive discussions of the analytical techniques used in this work are contained in References 6 through 10.

C. CRITICAL ASSEMBLY EXPERIMENTS

Critical assembly experiments are designed to provide data to support the nuclear analysis of the reactor. The SNAP program supported a number of experiments which were related to the SNAP 8 design. Most were identified by the designation SNAP Critical Assembly (SCA). Those which contributed to the SNAP 8 program are listed below:

1. SCA-1

A pseudo-spherical, uranium - zirconium hydride core with a beryllium and graphite reflector. This was the first U-ZrH critical assembly and served as a benchmark for early calculations.

2. S2ER

The S2ER critical assembly provided the first data on cores composed of cylindrical fuel and utilization of reflector control drums.

3. SCA-4A

The SCA-4A assembly had a flexible reflector configuration that permitted investigation of control drums with different shapes.

4. S8ER

Prior to the operation of the S8ER, the fuel was utilized in a critical assembly to determine the critical loading, reflector shim requirements, power distribution, and related quantities prior to installation in the test facility.

5. SCA-4B

The SCA-4B was used to investigate the effect of water immersion on a SNAP reactor. The S8ER fuel was used in this assembly to provide safety data for the SNAP 8 system.

6. SNAPTRAN

The SNAPTRAN reactor was used to investigate transient characteristics of SNAP reactors. Temperature-limited prompt transients were utilized to determine the temperature coefficients associated with various burnable poison materials.

D. SNAP 8 EXPERIMENTAL REACTOR TEST PROGRAM

The S8ER was operated to characterize the performance of uranium - zirconium hydride fuel in a reactor environment representative of the design conditions for SNAP 8. The program produced useful information on the temperature coefficients, control characteristics, long term reactivity effects, and fuel element performance.

1. Experimental Results - Preoperational Power Tests

a. Isothermal Temperature Coefficients

The isothermal temperature coefficients were determined at 50°F intervals of average core temperature from 300 to 1250°F by use of "positive period measurement" and "level position" methods.

The positive period measurement method uses two identical drum positions for producing positive periods at successive temperature conditions. The difference in the periods produced is the defect for the temperature change. This method eliminates the inherent difficulties of the level-position method, which measures the defect as differences in control drum position when "exactly critical" at different core temperatures; the difficulties stem from determining

TABLE 10
SUMMARY OF RESULTS - S8ER POWER EXPERIMENTS

Operating Characteristic	Design Prediction	Experimental Result	Current* Prediction	Significance
Temperature coefficient (isothermal) @ 300°F @ 800°F @ 1200°F	-0.18¢/F° -0.26¢/F° -0.31¢/F°	-0.16¢/F° ± 0.01 -0.18¢/F° ± 0.01 -0.20¢/F° ± 0.01	-0.20¢/F°	Reflector contribution was not as large as predicted
Temperature defect (68°F to 1200°F)	\$2.92	\$2.07 ± 0.05		
Partial temperature coefficients at design conditions Fuel Upper plenum Lower plenum	-0.12¢/F° -0.03¢/F° -0.02¢/F°	-0.12¢/F° ± 0.05 0.00¢/F° ± 0.01 -0.10¢/F° ± 0.03	-0.07¢/F° -0.02¢/F° -0.11¢/F°	The major part of the reflector temperature coefficient was found to occur in combination with the lower plenum coefficient.
Xenon poison equilibrium @ 600 kw	\$1.47	\$1.26 ± 0.09	\$1.32	
Overall samarium effect	\$2.21	\$1.47 ± 0.31	\$1.3	Actual samarium oxide prepoison content of the fuel was ~16% less than that on which the design prediction was based.
Total hydrogen loss at 5.1 x 10 ⁶ kwh	\$2.21	\$6.86		Prediction did not account for the deleterious effect of rapid thermal cycling of the core experienced during reactor operations.
Flow coefficient	0	0		
NaK coolant reactivity effect	-\$0.06	\$0.41 ± 0.02	\$0.4	Calculational technique improved
Reactor stability	Stable	Stable		No stability problems with SNAP 8

*Current predictions of S8ER operating characteristics are presented where the calculational techniques for predictions are new or have been revised as a result of the S8ER experiments. The upgraded techniques will and have been employed in predictions for future generations of the SNAP 8 reactor

"exactly critical" and from dependence on drum calibrations. The level position measurements corroborated the positive period measurement analysis. Isothermal temperature coefficients are presented in Figure 25.

Figure 25. Isothermal Temperature Coefficient (from period-measurement data)

b. Power Coefficients

Determination of power coefficients at several different operating conditions was accomplished by a series of rapid changes in reactor power, under various constraints of coolant inlet temperature, coolant outlet temperature, and coolant flow. Reactivity measurements were taken at steady state just prior to, and from 1/4 to several hours after, a power change. Corrections for xenon and hydrogen redistribution were made where necessary. The results of these tests are given in Table 10.

The power coefficients were interpreted according to the general reactivity equation:

$$\Delta \rho = \alpha_p \Delta P = \alpha_f \Delta T_f + \alpha_{up} \Delta T_o + \alpha_{ip} \Delta T_i + \alpha_r \Delta T_r \dots (1)$$

where

$\Delta \rho$ = reactivity change (¢)

α_p = power coefficient (¢/kw)

ΔP = power change (kw)

TABLE 11
POWER COEFFICIENT MEASURE-
MENT RESULTS

Average Temperature ● 200 ΔT (°F)	Power Coefficient		
	Test A (¢/kw)	Test B (¢/kw)	Test C (¢/kw)
620	-0.026 ± 0.011	-	-
750	-0.021 ± 0.006	-	-
900	-0.035 ± 0.015	-0.038 ± 0.013	+0.037 ± 0.015
1050	-0.028 ± 0.006	-0.047 ± 0.005	+0.022 ± 0.016
1200	-0.035 ± 0.010	-0.045 ± 0.008	+0.017 ± 0.020

TABLE 12
UPPER AND LOWER PLENUM
TEMPERATURE COEFFICIENTS

Temperature (°F)	Upper Plenum Coefficient (¢/F°)	Lower Plenum Coefficient (¢/F°)
900	+0.035 ± 0.025	-0.171 ± 0.023
1050	-0.021 ± 0.017	-0.111 ± 0.021
1200	+0.022 ± 0.024	-0.102 ± 0.030

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7568-02124

Figure 26. Overall Fuel-to-Coolant
Heat Transfer Coefficient

3-29-65

7568-02125

Figure 27. Fuel Temperature
Coefficient

- α_f = fuel temperature coefficient ($\text{¢}/\text{F}^\circ$)
- ΔT_f = fuel temperature change (F°)
- α_{up} = upper plenum temperature coefficient ($\text{¢}/\text{F}^\circ$)
- ΔT_o = core outlet coolant (upper plenum) temperature change (F°)
- α_{lp} = lower plenum temperature coefficient ($\text{¢}/\text{F}^\circ$)
- ΔT_i = core inlet coolant (lower plenum) temperature change (F°)
- α_r = reflector temperature coefficient ($\text{¢}/\text{F}^\circ$)
- ΔT_r = reflector temperature change (F°)

Test results indicated that the reflector temperature change (ΔT_r) followed the inlet temperature, and that any long time constant (>20 min) effect was very small. The reflector temperature effect was therefore included in with the lower plenum.

The steady-state reactivity values were measured before and after each power change. The resulting coefficients are given in Table 11.

The overall fuel-to-coolant heat transfer coefficient, UA, must be known in order to completely separate the temperature coefficients. Theoretical values of UA, normalized to the UA determination at 450 kw, 1300° outlet temperature, by noise analysis of flow oscillation tests, are shown in Figure 26.

Values of the fuel temperature coefficient (α_f) as a function of average fuel temperature are shown in Figure 27. The fuel temperature coefficient is the main contributor to the prompt temperature coefficient. Values of the temperature coefficient for the upper and lower plenum are presented in Table 12.

The average upper plenum coefficient, weighted according to statistical variances, was $0.003 \pm 0.012 \text{ ¢}/\text{F}^\circ$. The temperature effect in the upper plenum (expansion of grid plate, core vessel, and beryllium reflector cusps) included both positive and negative effects. However, the overall effect was expected to be slightly negative. The control-drum reflectors were not attached at the top

end of the core and therefore were not greatly affected by its temperature. The three values of the upper grid coefficients were spread uncomfortably far outside the estimated standard deviations for the coefficients. Unaccounted-for error could include systematic error in the hydrogen redistribution correction with temperature. The lower plenum temperature coefficients were much larger than the expected lower grid plate coefficient and evidently include the majority of the reflector temperature coefficient. This was to be expected since the reflector was attached to the lower end of the core vessel.

c. Xenon Poisoning

Reactivity effects of xenon poisoning were determined by monitoring the change in excess reactivity caused by a rapid change in steady-state power levels. The power changes were made at combinations of conditions which tended to minimize other transient reactivity effects caused by hydrogen redistribution, power, and temperature. The excess reactivity was corrected to account for reactivity effects that could not be eliminated by experimental technique. The equilibrium xenon reactivity effects are shown in Figure 28, with an accuracy of $\pm 7\%$, attributable mainly to the inaccuracy in absolute power measurement.

d. Samarium Poisoning

Samarium poisoning reactivity effects were determined by measuring the change in excess reactivity at steady-state conditions with the coolant temperature sufficiently low to effectively eliminate hydrogen loss reactivity effects. The measurements were made at three different times in reactor life* to show the effect of samarium prepoison burnout and samarium poison buildup.

To obtain a corrected rate (shown in Table 13), the measured rates were corrected for minor variations in temperature and power, including xenon and redistribution effects. The corrected rates were adjusted for hydrogen loss and depletion, which includes fuel depletion and buildup of fission products other than xenon and samarium. The adjusted rates then include the combined reactivity effects of samarium buildup and samarium prepoison burnout.

*At nominal conditions of 425 kw and 800°F average temperature

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Figure 28. Equilibrium Xenon Reactivity Effects

TABLE 13
SAMARIUM MEASUREMENTS

	Run 1	Run 2	Run 3
Reactor exposure (kwh)	248,000	775,000	1,245,000
Corrected rate (¢/day)	+0.89	+0.31	-0.01
Depletion rate (¢/day)	-0.40	-0.40	-0.40
Hydrogen loss rate (¢/day)	-0.01	-0.01	-0.02
Samarium rate (¢/day)	+1.30	+0.72	+0.41

The three samarium measurements from Table 13 were converted to the appropriate units and plotted in Figure 29 to determine the slope and the intercept, which were used to produce Figure 30. By adding the fission product poison and fuel depletion reactivity effects to the samarium effect, the long-term reactivity effects can be presented as Figure 31.

e. Hydrogen Loss Reactivity Rates

Determination of hydrogen loss reactivity rates was made, under various conditions, throughout the life of the reactor. Rates were determined at steady-state conditions by measuring the rate of change of excess reactivity, and deducting the long-term reactivity effects of samarium buildup and burnout, fuel depletion, and fission product poison buildup.

Figure 29. Measured Samarium Reactivity Rates

Figure 31. Long-Term Reactivity Effects of Fission Product Poisons, Fuel Depletion, Samarium Prepoison Burnout, and Samarium Buildup

Figure 30. Total Samarium Reactivity Rate

Figure 32. Hydrogen Reactivity Rate

The hydrogen loss reactivity rate is immediately dependent on reactor power level and coolant inlet and outlet temperatures. The rate increased step-wise during reactor lifetime after scrams involving loss of coolant flow and increased significantly after other operations involving less violent thermal cycling. The hydrogen loss reactivity rates observed during the lifetime of the reactor are presented in Figure 32.

f. Hydrogen Distribution Reactivity Effects

Three techniques were utilized for the determination of hydrogen distribution effects during the test program. In the homogenization test, the reactor was operated at 425 kw, 700° F core inlet, and 200° F ΔT to obtain equilibrium xenon concentration and hydrogen redistribution. Reactor conditions were then rapidly changed to 50 kw and 700° F core inlet, and xenon equilibrium was obtained while the hydrogen distribution remained "frozen" at the previous conditions. The temperature was then increased to 1260° F, and the hydrogen homogenization was determined. Redistribution was determined by obtaining equilibrium xenon at 425 kw, 700° F core inlet, and 200 ΔT , and subsequently increasing the temperature to 1100° F core inlet to allow the hydrogen to redistribute. The third technique involved correcting the reactivity data taken after a power or temperature change for variation in temperature, xenon, samarium, and hydrogen loss to determine the hydrogen distribution effects.

Reactivity values obtained for these tests, and the calculated values, are shown in Table 14.

TABLE 14
HYDROGEN DISTRIBUTION
REACTIVITY EFFECTS

Changes in Reactor Operating Conditions		Reactivity Effects	
From	To	Calculated (δ)	Experimental (δ)
425 kw 700° F T_{in} 200° F ΔT	50 kw 1260° F 0° F ΔT	+35.0	+37.0
50 kw 1260° F 0° F ΔT	425 kw 1100° F T_{in} 200° F ΔT	-23.0	-11.2
600 kw 1100° F T_{in} 200° F ΔT	450 kw 1100° F T_{in} 200° F ΔT	+7.0	+5.0
450 kw 1000° F T_{in} 300° F ΔT	450 kw 1100° F T_{in} 200° F ΔT	+2.5	+4.0

The tabulated values were determined by first calculating the axial temperature distribution for an average rod and then computing the resulting hydrogen distribution by assuming a constant zirconium hydride dissociation pressure throughout the rod. The reactivity effect of the hydrogen distribution was then calculated using a one-dimensional Carlson S_n technique.

Corrections of the reactivity measurements required to eliminate effects attributable to variations other than hydrogen redistribution have reduced the definition of the redistribution transients both in magnitude and time constant. Tests have indicated multiple time constants for the redistribution transient. The dominant time constant at 1100°F core inlet temperature has been estimated from experiment to be about 25 hours.

g. Flow Coefficient

The flow coefficient was determined at four different conditions of primary coolant flow from 33 to 122% of full flow by establishing critical control-drum configurations at least four distinct times at each flow condition. The determination confirmed expectations that the flow coefficient was negligible.

h. NaK Coolant Reactivity Effect

The reactivity effect of the NaK coolant was determined from the difference in total excess reactivities for initial dry and wet critical configurations. Excess reactivities were corrected to identical temperatures and compared to obtain the coolant worth of \$0.41.

i. Control Element Calibrations

Control element calibrations were accomplished by three methods; the traditional bump-period method, the drum-swap method, and the drum-drive-out method.

The bump-period method of control drum calibration produced integral and differential drum worth curves as shown in Figures 33 and 34.

The drum-swap method of calibration was initiated by establishing criticality at the minimum source independent power level. The drum being calibrated was withdrawn a small amount, thereby decreasing reactor power level on a negative period. The compensating drums were then inserted to put the

Figure 33. Typical Control Drum Integral Worth

Figure 34. Typical Control Drum Differential Worth

reactor on a positive period. Alternating between negative and positive periods enabled maintenance of the mean power level at the initial condition and was continued until the drum being calibrated was "full out." The power level record was inserted into a reactor kinetics code which calculated reactivity data in relation to drum position.

The drum-drive-out method of calibration was performed and analyzed essentially the same as the drum-swap method except that the drum being calibrated was fully withdrawn in one continuous motion, and no compensation was provided. Shown in Figure 35 are the integral worths of control drum No. 4 obtained by the bump-period, drum-swap, and drum-drive-out methods.

The control drum calibrations are listed in Table 15. Initial control-drum configurations for calibrating control drums were symmetrically identical with those presented in Table 16 (for calibrating Control Drum No. 2). Figure 36 presents the orientation of control drums showing direction of rotation from the "full-in" position. The conditions at which control drums were calibrated are presented in Table 17.

TABLE 15
CONTROL DRUM CALIBRATIONS

Configuration (Table 9)	Condition (Table 10)	Method	Integral Worths (¢)					
			1	2	3	4	5	6
A	1	bump-period	-	329	-	-	-	-
1	1	↓	-	408	-	-	-	-
1	2		432	403	419	414	422	407
1	3		420	396	407	407	420	395
1	5		-	-	-	422	-	-
2	2		331	363	362	403	402	319
3	2		408	397	329	365	324	350
4	2		423	403	412	412	419	405
5	2		348	365	313	376	345	301
6	4		307	-	-	-	-	-
7	5	bump-period	-	-	-	403	364	317
7	6	drum-swap	-	-	-	408	367	321

TABLE 16
CONFIGURATIONS FOR CALIBRATING CONTROL DRUM NO. 2

Configuration	Compensating Drums	Drums Full In	Drums at 60°	Drums Full Out	Drums Banked at Critical
A	4	1 and 5	None	6	3
1	4	5 and 6	None	None	1 and 3
2	6	3 and 5	None	None	1 and 4
3	5	1 and 6	None	None	3 and 4
4	1 and 3	5 and 6	None	None	4
5	5 and 6	None	None	None	1, 3, and 4
6	4	5	None	6	1 and 3
7	4	None	5 and 6	None	1 and 3

TABLE 17
CALIBRATION CONDITIONS

Condition	Date	Core Exposure (kwh)	Core Temperature (° F)	NaK Loaded
1	5-63	0	120	No
2	6-63	0	800	Yes
3	7-63	0	1200	Yes
4	7-63	0	800	Yes
5	5-64	1.4×10^6	800	Yes
6	6-64	1.4×10^6	800	Yes

Figure 35. Comparison of Control Drum Calibration Techniques

Figure 36. Top View of Reactor with Control Drums Numbered

Analysis of the calibrations produced the following conclusions:

- 1) Drums worths are essentially unaffected by variations in average core temperature.
- 2) Drum worths, in configurations in which "adjacent" drums are full-in, are essentially independent of other drum positions.
- 3) Drum worths, in configurations in which adjacent drums are not full-in, vary considerably because of differences in adjacent drum rotation directions.
- 4) Drum worths obtained by the drum-swap method are essentially identical with those obtained by the bump-period method, but the calibration is obtained in about 8 hours less time.
- 5) Drum worths obtained by the drum-drive-out method are accurate only in the region from 0 to \$1.50 subcritical.

j. Excess Reactivity Determinations

The total excess reactivity was monitored during the life of the reactor by summing the reactivities available for insertion with the control drums while critical. The excess reactivity so monitored is presented in Figure 37 as normalized to reactor conditions of 450 kw_t, 1300°F core outlet 200°F ΔT , with xenon concentration at equilibrium.

k. Thermal Calibrations

The core heat loss was determined at various conditions of system temperature and flow by measuring the temperature loss across the core with the reactor shut down. Measured losses agreed well with the predicted losses, thereby allowing use of losses calculated for the nominal reactor operating configuration of three control drums full-in as the losses for correction of indicated-to-actual reactor calorimetric power. The losses are shown in Figure 38.

Figure 37. S8ER Lifetime Excess Reactivity

Figure 38. Core Heat Loss

Calibration factors for the core inlet and outlet pipe thermocouples were determined at various conditions of system temperature and flow by measuring the difference between the pipe thermocouple reading and an immersion thermocouple reading in nearly the same location with the reactor shut down. Factors so determined were used in correcting the core inlet and outlet pipe thermocouple readings for calorimetric power calculations (see Figure 39).

Corrections for the core inlet and outlet pipe thermocouples located just within the primary equipment vault were determined at limited conditions of system temperatures and flows during power operations by comparison of the thermocouple readings with previously established standards. These corrections (Figure 40) and thermocouple readings were used in the latter portion of the test program for calculating reactor calorimetric power level because of immersion thermocouple failures and anomalies at power.

2. Critical Assembly Tests

The fuel and reflector assemblies for S8ER were used in critical assembly tests prior to their installation in the test facility. The results of the experiments on a dry critical assembly are summarized in Table 18. A more complete report on these experiments is contained in Reference 11.

The core without reflector was then installed on the SCA-4B assembly to study the effects of water reflection and immersion. Combinations of void and poison sleeves were used in conjunction with the water immersed core to investigate the means of preventing criticality upon accidental immersion in water. The results of this study are given in Table 19. From this table, it can be seen that a water reflected core of S8ER dimensions would be sub-critical if water reflected but supercritical if the water flooded the core in addition to reflecting it. All of the sleeves were successful at maintaining the reactor subcritical under these conditions. More details on these experiments are contained in Reference 12.

TABLE 18
SUMMARY OF RESULTS - DRY CRITICAL EXPERIMENTS

Experiment	Analytical Prediction	Experimental Result	Significance
Critical fuel loadings			
2-in nominal reflectors	196 rods	213.2 rods	S8DS fuel length increased from 14.0 to ~16.5 in
3-in nominal reflectors	169 rods	185.4 rods	
4-in nominal reflectors	152 rods	172.2 ± 0.1 rods	
Excess reactivity			
3-in nominal reflectors	\$4.14	\$3.24	
4-in nominal reflectors	\$6.88	\$6.00	
5-in nominal reflectors	\$9.51	\$8.46	
Worth of Hydrogen ($N_H - 6.0 \times 10^{22}$ atoms/cm ³) center of core	+41.6¢	+39.6¢	Confirmed calculational technique
1/2 radius of core		+35.0¢	
periphery of core		+15.0¢	
Worth of U ²³⁵ (9.54%)			
Center of core	+18.4¢	+11.5¢	
1/2 radius of core		+10.0¢	
Periphery of core		+14.5¢	
Worth of Sm ₂ O ₃ prepoison			
Total	-\$2.85	-\$2.72	Good agreement prediction based on actual prepoison content
Center element	-\$2.2¢	-\$2.3¢	
Worth of boron relative to void			
Ribbon at center of core		-48¢/gm	
Rod at center of core		-11¢/gm	
Worth of rare earth poison splines			
6 clustered around center rod	-52.5¢	-51.1¢	Average spline worth = -8.8¢
19 uniformly distributed over the core	-146.5¢	-130.5¢	Average spline worth = -7.7¢
Worth of BeO reflectors			
First flat insert removed	-19.5¢	-12¢	Discrepancies attributable to differences between analytical model and experiment
Next flat insert removed	-21.1¢	-15¢	
D insert removed	-8.7¢	-6.3¢	
All three inserts	-47.7¢	-34.7¢	
Determination of β_{eff}/l	1060 sec ⁻¹	950 sec ⁻¹ ± 10%	Adequate agreement for kinetic calculations
Control drum worth			
3-in nominal reflector	\$3.50	\$3.37 ± 10%	Radius of S8DS drums maintained at 4.5 in
4-in nominal reflector		\$3.67 ± 10%	
5-in nominal reflector		\$3.56 ± 10%	
Maximum differential worth			
3-in nominal reflector		5.9¢/degree	Maximum differential worth is essentially independent of nominal thickness Simplifies control requirements
4-in nominal reflector		6.0¢/degree	
5-in nominal reflector		6.0¢/degree	
Shim Worth			
Shim A in 3-in nominal reflector	64.9¢	58.7¢	Discrepancies attributable to assumption that 6 times single-shim worth equals 6-shim worth for which predictions were made
Shim B in 4-in nominal reflector	54.1¢	46¢	
Shim C in 5-in nominal reflector		41¢	
Core Power Distribution	(See Figures 33 and 34)		Determined with U ²³⁵ foils, agreed well with predictions

*See Reference 7

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Figure 39. Corrections for Core Inlet and Outlet Pipe Thermocouples at Core Vessel

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Figure 40. Corrections for Core Inlet and Outlet Pipe Thermocouples in Primary Equipment

3. Operations

a. Preoperational Activities

Critical facilities were utilized to perform a series of dry critical experiments with a prototype reactor core and reflector assembly prior to final assembly of the S8ER core. Measurements were made of:

- 1) Critical fuel loading
- 2) Effects of reflector thickness
- 3) Control element reactivity worths
- 4) Effects of varying fuel and moderator densities
- 5) Reactivity worths of special fuel and absorber rods
- 6) Reactivity worths of the internal reflectors
- 7) β_{eff}/ℓ ratio
- 8) Power density distributions.

The SNAP 2 Experimental Reactor Facility was modified to accommodate the S8ER as follows: The north end of the control room was lengthened 6 ft, and a mezzanine was added. The primary coolant vault was enlarged by raising

TABLE 19
SUMMARY OF RESULTS – WATER IMMERSION EXPERIMENTS

Experiment	Analytical Prediction	Experimental Results	Significance
Critical Fuel Loadings			
Water reflected	Subcritical	>250 rods	Water reflected core is subcritical
Water reflected and flooded	~175 rods	173 rods	Full load excess is 5.87 ± 0.32
Void Sleeve and Poison Sleeve Reactivity Worth			
3-in. void sleeve, core water reflected and flooded		$-\$2.10 \pm 0.45$	Void and poison sleeves are effective in preventing criticality
4-in. void sleeve, core water reflected and flooded		$-\$3.37 \pm 0.53$	
3-in. void sleeve, 1-in. boron sleeve, core water reflected and flooded		$-\$7.50 \pm 1.10$	

*See Reference 6

the upper shielding so that the vault could contain a larger primary coolant system, and a 7-1/2-ton bridge crane was added in the reactor room. Essentially all heat-transfer systems, service systems, instrumentation, and electrical equipment were replaced.

Before the S8ER reactor core was installed, the primary and secondary coolant systems were functionally tested and flushed by circulating high-temperature NaK through them, utilizing temporary piping in place of the uninstalled portion of the primary coolant system within the containment vessel. Tests of all instrumentation and electrical equipment were conducted, and a containment vessel leak rate test was performed. The reactor core vessel and reflector assembly were mounted beneath the shutdown shield (Figure 41), and the shutdown shield installed in the containment vessel.

Fuel-moderator elements were assembled in the SNAP Generalized Critical Facility into a unitized core package on a buildup stand. The core was kept subcritical by poison splines inserted as in Figure 42. The core package was then transported to the S8ER Test Facility (Figure 43), removed from the buildup stand (Figure 44), and inserted into the core vessel, and the poison splines were removed remotely. This unitized core loading procedure presented less risk of fuel element damage than the remote-handling, one-element-at-a-time, fuel loading technique which otherwise would have been used.

b. Operating Experience

Initial nuclear testing in the S8ER Test Facility began May 19, 1963. The reactor power and temperature history is shown in Figure 45. Following dry critical measurements including control element calibration, the shutdown shield center plug was permanently installed above the reactor core, the primary coolant system piping within the containment vessel was connected, and the core was filled with NaK. Final nonnuclear heat transfer system checkout was then conducted.

In preparation for the initial rise to power, a variety of zero-power nuclear tests were conducted at core outlet temperatures ranging from 300 to 1300° F. These tests included: control element calibrations at different configurations, measurement of the isothermal temperature coefficient of reactivity, and measurement of the hydrogen loss rate.

Early operation was frequently interrupted by spurious scrams. Such interruptions became less frequent as sources of these spurious signals were isolated and corrected. Figure 46 shows the scram experience of S8ER.

Prior to the initial rise to power, leaks developed in the containment vessel cooling system which was part of the original facility equipment. Nuclear testing was suspended to repair these leaks and to upgrade other potentially marginal equipment prior to beginning the rise to power. Modifications performed at this time included:

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Figure 41. Reactor Core Vessel and Reflector Assembly Mounted Beneath Shutdown Shield

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Figure 42. Unitized Core Loading

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Figure 43. Transporting Unitized Core to S8ER Test Facility

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Figure 44. Removal of Unitized Core from
Buildup Stand
AI-AEC-13070

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Figure 45. S8ER Operating History

Figure 46. S8ER Scram History

- 1) Provision of a larger, more flexible containment vessel cooling system
- 2) Replacement of the shutdown shield center plug with a plug of higher temperature design
- 3) Provision of increased cooling for certain instrumentation and process systems
- 4) Replacement of certain underground pipe lines and addition of a cathodic protection system
- 5) Addition of a flywheel motor generator set to provide noise-free power to key instrumentation and controls.

The initial rise to power was accomplished using step increases of reactor power and temperature, until a reactor power level of 600 kw with a 1300°F core outlet temperature was achieved on December 11, 1963. System temperature calibrations were performed and heat losses were measured. Measurements of xenon and samarium and hydrogen distribution were interspersed with sustained operation at 450 kw and 1300°F core outlet temperature until April 28,

TABLE 20
S8ER OPERATIONAL HIGHLIGHTS

Modification of Bldg 010 to accommodate S8ER initiated	August 16, 1961	Completed 6 months of continuous operation at 450 kw or greater with a core outlet temperature of 1300°F	March 18, 1965
Critical experiments	September to December 1962		
Water immersion experiments	January to March 1963	Completed 365 days of operation at 400 kw or greater with a nominal core outlet temperature of 1300°F	March 24, 1965
Dry criticality achieved in Bldg 010	May 19, 1963	Completed longest continuous power operation (4542 hours) at a core outlet temperature of 1300°F	
Wet criticality achieved	June 23, 1963	Power levels were 450 kw or greater during this operation	March 26, 1965
Initial significant power operation	November 22, 1963		
Initial operation at 600 kw/1300°F	December 11, 1963	Completed 10 000 hours of operation at 400 kw or greater	April 1, 1965
Exceeded the previous total 497,000 kwh of energy generated by SNAP reactors	February 26, 1964	Completed 12,000 hours of operations	April 11, 1965
Completed 60 days of operation at 450 kw or greater with a core outlet temperature of 1300°F	April 28, 1964	Completed 5,000 hours of continuous operation at 450 kw or greater with a core outlet temperature of 1200°F or greater	April 14, 1965
Completed 1000 hours of continuous operation at 600 kw with a core outlet temperature of 1300°F	September 2, 1964	Completed 5016.5 hours of continuous critical operations, all except 2.2 hours of which were at 450 kw with a core outlet temperature of 1200°F or greater	April 15, 1965
Exceeded longest continuous operation of any other SNAP reactor (1152 hr)	November 5, 1964	Completed 12,080.6 hours of operations	April 15, 1965
Exceeded the AI operating continuity record of 59 days previously held by Piqua Nuclear Power Facility (PNPF)	November 16, 1964	Released a total of 5,154,332 kwh of thermal energy	April 15, 1965
Completed 60 days of continuous operation at 450 kw with a core outlet temperature of 1300°F	November 17, 1964	Achieved a Time Operated Efficiency of 72.03% for the period from noon of the day permission was received from the AEC for operation, May 17, 1963, through final reactor shutdown at 0604 on April 15, 1965	April 15, 1965
Completed 90 days of continuous operation at 450 kw with a core outlet temperature of 1300°F	December 17, 1964		
Completed 10,000 hours of operations	January 18, 1965	Achieved a Time Operated Efficiency of 91.09% for the period from the start of the day of initial significant power operation, November 22, 1963 through final reactor shutdown at 0604 on April 15, 1965	April 15, 1965
Completed a total of 100 days of operation at 600 kw with a core outlet temperature of 1300°F	February 4, 1965		
Exceeded longest known continuous operation of any reactor (147 days set by Yankee Reactor)	February 12, 1965	Achieved a Time Operated Efficiency of 97.91% for the final year of operation from April 15, 1964 through final reactor shutdown at 0604 on April 15, 1965	April 15, 1965
Completed 11, hours of operations	March 1, 1965		
Completed 365 days of critical operation at core outlet temperatures of 1200°F or greater	March 5, 1965		

1964, when the S8ER accumulated a total of 60 days at 450 kwt, 1300°F core outlet temperature, thereby meeting the chief S8ER programmatic objective. Tests were conducted to determine partial power coefficients, control elements were recalibrated, and further xenon and samarium measurements were performed. Throughout the remainder of the program sustained power operation at either 600 kwt or 450 kwt was conducted, while measurements of hydrogen loss and redistribution were performed at various temperature conditions. Highlights of S8ER operations appear in Table 20.

On April 15, 1965, the S8ER exceeded 208 days of continuous operation, surpassing the longest known power operation of a nuclear reactor at that time by 61 days. This record demonstration of operating continuity was made possible by:

- 1) Reliability, upgrading, and growth programs
- 2) Selection and training of operating personnel
- 3) A fortunate record of no major electrical disturbances at the site.

c. Maintenance

S8ER equipment failures were limited to equipment not intended for space use. The following major repairs were performed during the experiment:

- 1) Revision of control-drum-drive wiring to eliminate connectors within the containment vessel which had caused failure of 3 drum-drive circuits.
- 2) Replacement of two failed control-drum-drive motors with motors of higher temperature design.
- 3) Repair of leaks in the primary EM pump end cap. Following the second occurrence of this leak, the cause was identified as an unexpected erosion effect, shown in Figure 47 in oblique and cross-section views. The end-cap design was changed and no further leaks occurred.

Figure 47. Primary EM Pump Discharge Header Cap, Showing Erosion Failure

4. Post-Test Fuel Element Analysis

The S8ER core was delivered to the AI Hot Laboratory on July 28, 1965. For ~6 weeks, the entire hot laboratory facility was devoted to core disassembly and detailed inspection of the 211 fuel elements. The purpose of this inspection was to evaluate the condition of the fuel elements by nondestructive means and to provide a sound basis for the selection of a limited number of elements for destructive examination.

a. Visual and Stereoscopic Examination

Figure 48 shows the core after it was removed from the reactor, as compared with its appearance before installation in the reactor. NaK oxide deposits, which resulted from oxidation of residual NaK, were scattered over the surface of the core, as shown in Figure 49.

Prior to removal from the core, each fuel element was visually identified by number (stamped on end cap), and its orientation with respect to the core centerline was marked on the top end pin. This mark was designated the 0°

Figure 48. S8ER Core

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Figure 49. NaK Oxide Deposits on Core Prior to Cleaning

reference mark on each element. After removal from the core, each element was washed with ethylene glycol n-butyl ether to remove the NaK oxide deposits.

A visual examination, using Kollmorgen periscopes, was performed on each of the fuel elements. The orientation and length of each cladding crack was recorded. Over 90% of the fuel elements were then selected for stereo-microscopic examination, where the cladding surfaces were examined at magnifications up to 22X. Dye-penetrant inspection was used to confirm selectively the results of the visual examination.

Of the 211 fuel elements in the core, 44 were found to be intact and 167 had cracked cladding. Table 21 gives the distribution, by core ring, of intact and cracked elements. A typical cracked fuel element is shown in Figure 50.

TABLE 21
FUEL ELEMENT CONDITION

Ring Number	Total Number of Elements	Number Cracked	Number Intact	Percentage Cracked
I	1	1	0	100
II	6	2	4	33
III	12	11	1	92
IV	18	18	0	100
V	24	24	0	100
VI	30	27	3	90
VII	36	30	6	83
VIII	42	32	10	76
IX	42	22	20	52
Total	211	167	44	79

Figure 51 shows the core configuration and the approximate orientation of the cladding cracks. The majority of the cracks appeared to have occurred in the high-temperature area of the cladding. The cracks in the outer two rings (IX and VIII) generally occurred on the sides of the elements facing the center of the core, their hot sides. Cracking also appeared to be most prevalent on the portions facing adjacent elements, the high-temperature location around the

TOP

CRACK IN CLADDING

7568-02774A

Figure 50. Typical Cracked S8ER Fuel Element (Longitudinal Crack, Element 281)

SYMBOL	DESIGNATION	
	CLADDING CRACK LONGER THAN 5 in.	
	CLADDING CRACKS 0 TO 5 in. long	
	TOP END CAP FAILURE	
	ELEMENT WAS BROKEN INTO 3 PIECES (POSITION III-2 ONLY)	
	SPIRALED CRACK IN CLADDING (POSITION VII-36 ONLY)	

12-2-66

7568-02363B

Figure 51. S8ER Core Configuration, Showing Location and Orientation of Cladding Cracks

12-2-65

7568-0274

a. Circumferential Crack at Top
End Cap (Element 511)

BOTTOM
END PIN

12-6-65

7568-02749

HAIRLINE
CRACK

b. Hairline Crack Over Closure
Plug (Element 470)

Figure 52. Types of Cracks Observed

circumference of an element and the zone expected to exhibit the lowest cladding ductility.

Three general categories of cracks were observed:

- 1) Circumferential cracks in the transition area where the top end cap connected to the cladding (Figure 52a).
- 2) Small hairline longitudinal cladding cracks located in the cladding area supported by the bottom closure cup and plug (Figure 52b).
- 3) Longitudinal cladding cracks of a range of sizes (the most common was shown in Figure 50).

Figure 53 shows the location and character of these cracks.

b. Dimensional Measurement

At the completion of the visual examination, length and diameter measurements were taken of each element. The diameter measurements were taken across the 0 to 180° element orientation, using a profilometer. The profilometer consists of a modified lathe bed with two end stocks to support the object being profiled, and two variable-reluctance, linear-displacement transducers which travel axially along the element. A diametral profile of the element is printed on a strip chart recorder. Using a calibrated diameter standard, the accuracy of these measurements is ± 0.0003 in. From the profiles, the element diameters were determined, and local cladding bulges and distortions were detected.

The profilometer traces revealed considerable diametral changes along the length of the elements. The maximum local cladding diameter increases of intact elements were 0.004 to 0.005 in. ($\sim 0.8\%$); however, some elements showed local diameter decreases of as much as 0.004 in. The highest frequency of peak diameter distortion of all elements occurred from 4 to 7 in. from the tops of the elements (Figure 54). Figure 54 also shows another high frequency of peak distortion near the top end of the elements and a third group peaking at 12-1/2 in. from the top. Approximately 23 fuel elements had cracks resulting from local distortion at the bottoms (12-1/2 in. from the tops) of the elements. In only two cases, Elements 111 (IX-42) and 457 (VII-3), was this cracked cladding at the bottom of the element the sole effect. The local distortion which

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Figure 53. General Location and Character of Cladding Cracks

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Figure 54. Frequency of Peak Diameter Changes

7568-02603

Figure 55. Distribution of Strain in Intact Elements

*MEANS ARE SIGNIFICANTLY DIFFERENT
95% CONFIDENCE LEVEL

1-30-66

7568-02360C

Figure 56. Average Changes in Element Lengths with ± 1 Standard Deviation (68%) Confidence Limits on Individual Elements

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occurred 4 to 7 in. from the tops of the elements was by far the greatest in magnitude, and had the largest number of cracked elements associated with it (i. e., 164 of the 167 cracked elements).

The diametral changes in the intact elements were used to obtain an estimate of the ductility in the Hastelloy N. The average diameter after irradiation for each intact element was determined from the profilometer traces $\left[\text{Average diameter} = (\text{Top} + \text{Center} + \text{Bottom})/3 \right]$. The amount of maximum local distortion ($\Delta D = \text{Maximum Diameter} - \text{Average Diameter}$) was then plotted for all intact elements (Figure 55). The average local distortion ($\bar{X}$) and one standard deviation limit (σ) were obtained. As local distortion, D , can be converted directly into percent strain $\left[(\Delta D / \text{Average Diameter}) \times 100 \right]$, the strain (ductility) in the cladding was obtained as 0.4%. With each element in the core surrounded by six other elements (except in Ring IX), a temperature scalloping effect occurred around the circumference of the element. For a 60°F temperature scalloping, caused by the 600-kwt 1300°F-outlet reactor condition, maximum-to-average creep rates would be in the ratio of 2.3/1.0. Figure 55 would therefore underestimate the strain that occurred in the high-temperature portion of the circumference of the cladding.

Length measurements were taken on each fuel element at three orientations. A dial indicator and calibrated length standard were used to obtain an accuracy of ± 0.001 in. Postirradiation measurement of element lengths revealed changes ranging from -0.015 to +0.077 in. Average element length changes, along with ± 1 standard deviation confidence limits (for individual elements) around the average change, for both intact and cracked elements, are presented by core ring in Figure 56. The plot shows that the largest length increases occurred in Core Rings VI, VII, and VIII. Significance tests at the 95% confidence level indicate a significant difference between the length changes of intact vs cracked elements in Rings IX, VIII, VII, and II.

c. Gamma Activity Determination

Fuel element gamma activity scans were taken by counting the Zr^{95} fission product photopeak as a function of element length. Because Zr^{95} has a half life of 65 days, the gamma scans are representative of the power distribution over only the last 4 to 6 months of reactor operation. Analysis of the axial gamma

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124

Figure 57. Typical Gamma Scan Profile (Element 499)

scan data shows that the axial peak-to-average burnup distribution ranged from 1.34 to 1.41. The value used in the design calculations for the S8ER was 1.40. The axial distributions of fuel rod gamma activity were not symmetrical about the rod centers, but were slightly skewed toward the bottom of the reactor, as shown in Figure 57. Based on the activity scan data, ~54% of the burnup occurred in the lower half of the typical fuel rod, and ~46% occurred in the top half. This shift in burnup was expected, because of hydrogen redistribution toward the cooler region of the fuel elements.

On the basis of predicted end-of-life power profile and actual gamma-scan power profile, the hydrogen content was calculated to match the observed gamma profile. This is shown in Figure 58, and constitutes the calculated hydrogen content (N_H), by ring, from gamma activity data. With Rings IV and V having approximately a 15% hydrogen loss, one can use the reactivity curve for fuel elements (as a function of hydrogen content and gamma data) to show that the loss in power (and thus Zr^{95} inventory) occurred ~4 to 6 months prior to shutdown. This supports the conclusion (Section IV) that a large number, and perhaps a majority, of the elements had cracked by November 1964, 5 months before shutdown.

d. Permeation Tests

Two intact elements, 302 (IX-5) and 182 (IX-11), (high gamma activities), were permeation tested at 1400° F. These elements had length increases of 0.011 and 0.018 in. respectively, and thus interference of the fuel rods with the coated tubes would be expected. This should result in a degraded ceramic hydrogen barrier. Testing proved unsuccessful as the cladding tube was greatly distorted during test rendering the results suspect.

e. Correlation of Observations with As-Fabricated Parameters

In an attempt to determine whether the variations in dimensions, gamma activity, and surface condition of the elements may have been a result of variations in pre-irradiation fabrication parameters, a statistical evaluation of the pre-irradiation data was performed.

Comparison of the as-fabricated variables between the intact and cracked elements of Ring IX revealed no significant differences, as can be seen in

TABLE 22
COMPARISON OF PRE-IRRADIATION VARIABLES FOR CRACKED AND
INTACT ELEMENTS OF RING IX

Variable Tested	Condition of Elements	Average	Standard Deviation	Variance	95% Individual Confidence Limits		Remarks
					Upper	Lower	
Uranium (wt %)	Intact Cracked	9.974 9.956	0.06940	0.004816	10.120	9.828	Cracked have significant (99%) higher variance
Additive (wt %)	Intact Cracked	0.454 0.416	0.1636 0.1820	0.0268 0.0331	0.7980 0.7929	0.1109 0.0394	Cracked have higher variance and lower average value
Hydrogen Content (wt %)	Intact Cracked	1.634 1.649	0.0350 0.0378	0.0012 0.0014	1.707 1.727	1.560 1.571	
Axial Clearance (in.)	Intact Cracked	0.0237 0.0250	0.00191 0.00489	0.0000036 0.0000239	0.0277 0.0351	0.0197 0.0149	Cracked have significant (99%) higher variance
Radial Clearance (in.)	Intact Cracked	0.001279 0.001270	0.000326 0.000496	0.1062×10^{-6} 0.2458×10^{-6}	0.00196 0.00230	0.00059 0.00024	Cracked have significant (95%) higher variance
Average TIR (in.)	Intact Cracked	0.006995 0.006670	0.002872 0.003834	0.8250×10^{-5} 0.1470×10^{-4}	0.01461	-0.00127	
Permeation [cc(STP)/hr at 1400°F]	Intact Cracked	0.5647 0.5857	0.3225 0.3081	0.1040 0.0949	1.2419 1.2234	-0.1124 -0.0521	
Normalized Permeation [cc(STP)/hr at 2 atm internal pressure]	Intact Cracked	0.5044 0.5418	0.1943 0.3357	0.03774 0.1127	0.9124 1.2367	0.0964 -0.1531	Cracked have significant (95%) higher variance
Initial Element Weight (gm)	Intact Cracked	366.8620 367.0505	0.9172 0.7921	0.8412 0.6274	368.7606 368.7140	364.9635 365.3871	
Rod Density (gm/cc)	Intact Cracked	6.0887 6.0836	0.0154 0.0135	0.000236 0.000183	6.1210 6.1116	6.0565 6.0556	
Minimum Ceramic Thickness (mils)	Intact Cracked	1.6737 1.9435	0.6674 0.8366	0.4454 0.6998	3.0752 3.6752	2.7221 2.1179	Average value of cracked is higher (not significant)
Maximum Ceramic Thickness (mils)	Intact Cracked	2.2368 2.4174	0.8738 0.8942	0.7636 0.7997	4.0719 4.2685	4.0181 5.6629	Average value of cracked is higher (not significant)

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Figure 58. Hydrogen Distribution as Calculated to Match End-of-Life Power Profile

Table 22. The elements in this ring were chosen for comparison, since there was a representative number of both cracked and intact elements.

Also, a comparison was made of the as-fabricated parameters for the elements in Sector 2* of the core. These results were compared to those of Sector 3, the sector of highest frequency of cracked elements, and not adjacent to Sector 2. In each instance, there were no statistically significant differences between the averages for the cracked and the intact elements, although the parameters of the cracked elements did have a significantly higher variance in their data than did the same parameters for the intact elements. This was particularly evident for the axial and radial clearances, uranium and additive content, and the permeation leak rates. These and additional tests were performed on those variables which might have affected the element performance. No correlation between as-fabricated variables and cracking frequency was observed.

*Sector__ = The 60° segment of the cross section of the reactor core adjacent to Drum__, enveloping fuel elements in all rings, from the center to the perimeter of the core.

5. Grid Plates and Tie Rods

Measurements of the upper and lower grid plates revealed that they were dish shaped outward from the fuel element cluster. Maximum deformations of the top and bottom grid plates were 0.023 and 0.013 in. respectively. The clearance between the grid plates, at assembly of the core, was 14.52 in. at room temperature. This provided 0.05 in. clearance between the elements and the grid plates in the cold condition. Postirradiation measurements showed that some of the fuel elements were more than 0.020 in. longer than the nominal element-to-grid plate clearance. Additionally, the six elements which experienced circumferential cracking in their top ends (reactor outlet) at their end caps (Figure 52a) were located only in Rings III and IV, and corresponded to positions of near-maximum grid plate deformation. These results indicate axial interference in this section (including Rings III and IV) during reactor operations, particularly during scrams, since this was not the location in the core which corresponded to maximum length increases.

6. Summary and Conclusions

a. Reactivity

The one operating factor that dramatically contrasted the S8ER with all previous reactor tests was the large loss in reactivity (hydrogen loss from the elements during the sustained operation phase) with extended operation. This loss rate is an important operating characteristic of any SNAP reactor. Evaluation of the excess reactivity changes (from the control drum positioning) in the reactor provided the best means of measuring the hydrogen loss rate in the S8ER. The data showed the expected dependence of loss rate on the reactor power level and coolant temperatures.

The reactor coolant outlet temperature was generally maintained at 1300° F, with a 200° F ΔT , during power operations. Various power levels were attained by adjusting NaK flow to maintain the thermal conditions.

The change in the hot excess reactivity of the reactor was a combination of the effects of:

Burnout of the samarium prepoison

Burnout of fuel

Accumulation of fission products

Loss of hydrogen from the fuel elements.

The pre-operation prediction of the total hydrogen reactivity loss was \$2.2, based on out-of-pile permeation test results. The actual loss was \$6.86.

Reactor scrams from equipment associated with this ground test, and severe temperature transients and cyclic conditions associated with nuclear parameter tests, were believed, at the time of operation, to have degraded the interim hydrogen barrier used in these early fuel elements. This damage susceptibility of the ceramic coating was an established effect, proven by many nonnuclear fuel element performance tests.⁽¹³⁾ Examination of fuel elements from previous reactors, the S2ER⁽¹⁴⁾ and S2DR,⁽¹⁵⁾ and a limited number of fuel materials irradiation tests⁽¹⁶⁾ tended to show that the U-ZrH_x fuel was dimensionally stable under the burnup and temperature conditions of the S8ER operation. Computer calculations⁽¹⁷⁾ of factors affecting the performance of the fuel elements predicted difficulties due to the undesirable $\delta \rightarrow \beta$ phase transformation in the fuel and excessive swelling of the fuel with large ($\sim 9\%$, overall) hydrogen losses from the elements. However, reactivity measurements⁽⁴⁾ showed no indication of a step decrease in reactivity due to rapid hydrogen loss from one or more fuel elements (presumed to occur when the fuel was exposed directly to the NaK coolant by cladding fracture) during normal operation or subsequent to any of the reactor scrams.

b. Fission Product Monitoring - Primary Coolant

Samples of primary NaK were taken, at three periods during S8ER operation, for analysis of fission product activity. The results of this sampling are shown in Table 23.

The relatively low Cs¹³⁷ activity in Sample 2, as compared to the Rb⁸⁶ activity of more than ten times the Cs activity, prevented the attainment of a high accuracy in this measurement. The Rb⁸⁶ activity which masked the Cs¹³⁷ measurement is attributed to NaK impurity. Comparing Sample 2 with Sample 1,

TABLE 23
FISSION PRODUCT RELEASE FRACTIONS, BASED ON
Cs¹³⁷ RELEASE

Sample	Sample Date	Core Energy Release (kwh)	Activity (μCi/gm NaK)	Release Fraction (%)
1	12-31-63	0.1 x 10 ⁶	2 x 10 ⁻⁴	3.7 x 10 ⁻⁴
2	4-14-64	1.0 x 10 ⁶	<3.5 x 10 ⁻³	5.8 x 10 ⁻⁴
3	4-15-65	5.1 x 10 ⁶	3.4	8.2 x 10 ⁻²

it is concluded that there was no change in the fuel integrity between the times that these two samples were taken. The release fraction for Sample 3 is a factor of ~200 higher than Samples 1 and 2, indicating that a substantial change in the fuel integrity had occurred.

During the operation of the S8ER, ~750 Ci of Cs¹³⁷ were generated. Of this total, ~0.6 Ci was released to the NaK, representing ~50% of the Cs¹³⁷ that could be released by recoil from all fuel rod surfaces. These results are in reasonable agreement with the number of cladding cracks found during the postirradiation fuel examination. There was no significant change in the Cs¹³⁷ present in the NaK after 4-15-65 (samples were analyzed on 5-24-65 and 8-18-65), indicating that no additional cladding fractures occurred after shutdown.

c. Fission Product Monitoring - Surge Tank Gas

A sample of the surge tank cover gas was taken periodically and analyzed for Xe¹³⁵ by counting the 0.25-Mev γ photopeak (the 0.62-Mev γ was also evident). In some cases, the cover gas was also analyzed for Xe¹³³ by counting the 0.08-Mev γ photopeak. The conclusion drawn from this data is that a significant increase in Xe¹³⁵ activity took place during the period of June to September 1964.

Prior to June 1964, fission product monitoring of the NaK surge tank cover gas indicated a small level of activity, caused either by fuel element surface contamination or a cladding leak (in contrast to gross cracking) in perhaps one element. In June to October 1964 period, following cyclic operation during

temperature and power coefficient measurements and reactor scrams, the fission product activity increased by about three orders of magnitude. Only a small increase in activity levels occurred after October 1964.

To evaluate the xenon data in terms of cladding fracture, it is necessary to assume a release mechanism and also to determine the relationship between the xenon dissolved in the NaK and the xenon in the surge tank atmosphere. Because of the relatively large size of fission product atoms, as compared to the lattice spacing in zirconium hydride, a recoil release model appears reasonable for establishing an upper limit. In this model, a fragment leaves the fuel surface only if the path traversed within the fuel is less than the fragment mean free path. Unfortunately, this model becomes questionable when δ - β phase transformations occur within the lattice. Cycling through this transformation has been observed to result in large releases of fission gases.⁽¹⁸⁾ The fact that β -phase material was present in the S8ER at the end of life increases the uncertainty of the assigned upper limit release value. For the recoil model and 80% of the fuel elements having cracks, ~ 20 Ci of Xe^{135} would be available for release to the coolant. If this xenon were contained solely in the cover gas, a Xe^{135} activity level of $\sim 600 \mu\text{Ci/cc}$ would be observed. This is a factor of 100 more than was actually observed. A review of work performed on the solubility of noble gases in liquid metals resulted in the conclusion that xenon solubility would be sufficient to lower the surge tank activity by the required factor of 100 to bring the xenon data in line with the Cs^{137} results.*

The measurement of xenon in the cover gas and of cesium in the NaK does not provide information on the release mechanism or fission product transport through the system. However, the $\text{Xe}^{135}/\text{Xe}^{133}$ activity ratio does provide information on the age of the measured gas.

The equilibrium activity of a fission product in the core is proportional to its yield and the fission rate (in the case of Xe^{135} , a neutron capture also affects the equilibrium, because of its high absorption cross section, but this is not significant for S8ER fluxes). Since the yields of Xe^{135} and Xe^{133} are nearly the same, the equilibrium ratio of their activities is ~ 1 . If a sample of this equilibrium is isolated, the $\text{Xe}^{135}/\text{Xe}^{133}$ activity ratio will decrease (the

*At the time of reactor operation, calculations that one or two fuel elements may have fractured and released a limited quantity of fission gas. (SNAP fuels release very little fission gas). Solubility effects in the NaK were not accounted for in these calculations.

half life of Xe^{135} is 9.2 hr, while that of Xe^{133} is 5.3 days). The measured $\text{Xe}^{135}/\text{Xe}^{133}$ activity ratios are shown in Table 24.

TABLE 24
 $\text{Xe}^{135}/\text{Xe}^{133}$ ACTIVITY RATIOS IN S8ER
COVER GAS SAMPLES

Date	Ratio	Date	Ratio
8-18-64	0.7	12-1-64	0.76
10-6-64	0.45	12-8-64	0.72
10-13-64	1.00	12-15-64	0.60
10-20-64	1.05	12-22-64	0.56
10-27-64	1.05	12-29-64	0.84
11-2-64	0.72	2-16-65	0.46
11-17-64	0.56	3-2-65	0.85
11-24-64	0.64	4-15-65	0.51

These ratios lie between 1.0 and 0.4, indicating that the surge tank gas was receiving fission products which were <10 hr old. This precludes a diffusion release model, and therefore supports the recoil assumption, since a much longer time would be required.

d. Hot Cell Observations

Core disassembly revealed that a total of 167 fuel elements (out of 211) had cracked claddings. A majority of these cracks were longitudinal, and were located in the central axial zones of the tubes. Thirty-four had longitudinal hairline cracks in their cladding tubes, adjacent to their lower end plugs. Six elements had circumferential cracks in the vicinity of their upper end cap closure welds.

The highest frequency of tube cracks was in the midradius region of the core. These elements were characterized by high cladding temperature during

operation, high hydrogen loss ($\sim 15\%$), reduced gamma activity, and metallurgical evidence of chemical reactions on their fuel surfaces (probably initiated by a NaK - ceramic barrier reaction). A majority of elements in this region had at least one crack, in the range of 0.020 to 0.030 in. in width, implying that these cracks probably occurred relatively early in core life.

Approximately 30% of the cracked elements exhibited characteristics of low hydrogen loss: high gamma activity, minimal swelling, and small crack widths. These elements were located primarily in the central and outer regions of the core, and the cracks probably occurred very late in core life.

e. Fuel Swelling

Previous fuel irradiation experiments and the NAA 115-1 Sublength Fuel Element Experiment,⁽¹⁹⁾ conducted in conjunction with the S8ER operation, showed that hydrides which are operated at the δ - ϵ phase boundaries at some operational mode during life may be somewhat more susceptible to swelling at elevated temperatures than the pure δ or ϵ materials. With the six reactor control drums in the out position during the early stages of core operation (without significant hydrogen loss from the elements), it would be expected that elements from the combined high-temperature, high-irradiation zones (Rings IV, V, and VI) would have undergone a somewhat larger length increase than in other rings of the core. *

After completion of the nuclear power coefficient testing in May 1964, the average core burnup would have been 0.05 metal at. %. It is now fairly well established that this burnup level in the fuel would be sufficient to close the very limited axial clearance of 0.15% (0.022 in.) in the fuel elements.

Temperature cycling (from the reactor scrams) and cyclic conditions (from the samarium runs and power coefficient tests in May 1964) would have aggravated fuel material damage at the δ - ϵ transformation region. The greatest element damage potential was associated with loss-of-flow scrams and primary pump restarts, when NaK at 700° F was injected into a relatively hot core. These reactor scrams and temperature cyclic conditions would also degrade the ceramic hydrogen barrier coating, which was susceptible to cyclic damage,

*The S8ER was orificed for a NaK flow profile matching the neutron flux profile. This results in a binodal fuel temperature distribution across the core, with maxima occurring in Rings IV and V.

particularly in the high-stress bottom closure cup location. The net result was that ~50% of the elements (primarily those from Rings IV, V, and VI) are believed to have had particularly harmful axial interference during the reactor scrams and thermal transients that occurred in the summer of 1964. Support for this deduction is found in the fact that the end caps of six fuel elements were broken (this was found to be the sole effect on one of the elements), in a manner reflecting a condition of axial interference in the hot locations of the core.

Dimensional measurements were made of all the elements from the reactor. The average element length increase was 0.019 in. , indicating considerable axial interference between cladding and fuel. Nine cracked and eight intact elements were selected for detailed destructive examination. A high degree of correspondence was found between fuel growth and the location of cladding bulges and cracks.

Hydrogen analyses and calculated hydrogen distributions both supported the conclusion that, under 600-kwt operating conditions, the undesirable β phase was prominent in the fuel elements from the core midradius zone. During metallurgical examination, high incident of β phase was not as prevalent as had been anticipated. However, hydrogen redistribution and lower fuel temperatures (during the final months of 450-kwt operation) apparently rehydrated much of the β phase back to δ phase. It was not possible to determine, with absolute certainty, whether β -phase formation was a cause of excessive fuel growth or followed after cladding cracks had already been formed. However, deductions made from correlations of $\delta \rightleftharpoons \delta + \epsilon \rightleftharpoons \epsilon$ associated fuel growth and reactor operating history suggest that β -phase formation, in most cases, followed after cladding cracks occurred.

Elements from the outer and inner zones of the core, in contrast to the mid-radius zone, were characterized by lower hydrogen loss and a prominence of $\delta \rightleftharpoons (\delta + \epsilon) \rightleftharpoons \epsilon$ phases under late operating conditions. The fuel growth in these elements was characteristically smaller, as were the widths of the cladding cracks and the proportion of elements having cracks. These observations correlate with reduced temperatures in these zones during the major portion of core life, due to the S8ER NaK flow profile.

f. Cladding Cracks

The investigation considered that the major potential causes of cladding cracks might be:

- 1) Insufficient radial and axial clearance between fuel and cladding to accommodate normal fuel growth associated with $\delta \rightleftharpoons \delta + \epsilon \rightleftharpoons \epsilon$ phase regimes.
- 2) High hydrogen loss, followed by gross fuel swelling due to formation of β phase
- 3) Cladding stresses
- 4) Excessive fuel temperatures or pressures.

Extensive studies in all of these areas resulted in the conclusion that the most likely cause of cracking in the cladding tube area was due to $\delta \rightleftharpoons (\delta + \epsilon) \rightleftharpoons \epsilon$ associated fuel growth, combined with insufficient axial and radial clearances within the fuel element and low cladding ductility. Analyses of residual and operational cladding tube stresses resulted in maximum strain levels, in the absence of fuel growth, in the range of 0.1%. In contrast, the average of the peak strains of intact S8ER elements was 0.4%, indicating that strains resulting from residual and operational stresses could be accommodated without cladding tube fracture.

The cladding fractures in the closure end cap region, on the other hand, appeared to be related to stress levels. The residual shrink fit stresses resulting from the assembly operation, when combined with thermal transient stresses and low cladding ductility, were concluded to be the most likely cause of the observed cracks.

The operational history of the S8ER, as envisioned from postoperation evaluation of the fuel elements, is depicted in Figure 59.

g. Future Design Considerations

The fact that the extensive cracking of fuel element cladding in the S8ER did not result in a gross loss of hydrogen (considering the length of time the fuel was in contact with 1300° F NaK) was a significant and unexpected development. That

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Figure 59. Relationship Between Post Operational Condition of Fuel Elements and Operating History of Reactor

the reactor was able to attain a lifetime of 10,000 hr is, in itself, perhaps the most important consideration in judging its performance and the attractive potential of the zirconium hydride fueled, SNAP reactor concept.

The many studies conducted in the evaluation of S8ER performance have resulted in greater understanding of the design features needed to extend core lifetime and reliability capabilities. In the next generation reactor, S8DR, fuel element design changes were made to provide more axial and radial clearance for the $\delta \rightleftharpoons (\delta + \epsilon) \rightleftharpoons \epsilon$ associated fuel growth. Fuel element and cap designs were modified to reduce operating stress levels. The Hastelloy N cladding material was retained because it has demonstrated that it has sufficient ductility to more than accommodate the operational stresses. The flow profile was modified, based on a better understanding of the role of temperature in fuel growth and cladding ductility.

E. SNAP 8 DEVELOPMENTAL REACTOR TEST PROGRAM

The S8DR was operated to demonstrate operation of the reactor in conjunction with flight system components. It was operated in a vacuum chamber to simulate space environment, and a flight type radiation shield was located immediately beneath the reactor to study the effect of the environment on this component.

The fuel element had been redesigned on the basis of the findings of the S8ER fuel examination. The operation of the S8DR established a better understanding of the mechanism of radiation growth of the fuel. Also, there were periodic motions of clusters of fuel elements which produced small power oscillations indicating hydraulic instability and severe overtemperature conditions due to fuel element clustering.

1. Experimental Results — Pre-operational Power Tests

Fuel loading was initiated June 18, 1968, with the core vessel and reflector assembly mounted on a support stand on the floor of the test vault. The core was initially loaded with a full complement of stainless-steel dummy elements. A loading step consisted of tilting away the reflector halves, removing the 5-curie neutron source and reactor top lid, and replacing a specified group of dummy elements with fuel elements. The lid and source were then reinstalled, reflectors returned, and k_{eff} was measured with all control drums full in.

Criticality was achieved on June 22, 1968, with 169 fuel elements loaded and 42 stainless-steel dummies. Fuel loading continued until 196 elements were loaded, then loading was interrupted while control drums were calibrated. Fuel loading was then continued until the full complement of 211 elements was loaded. Then drum calibrations were repeated.

In late October, the reactor was welded into the primary system, the heat transfer systems were checked out and, in late November, the reflector assembly was installed and dry critical testing in the vacuum chamber was completed. The primary system was loaded with reactor grade NaK, wet critical testing completed, and in mid-December half-rate automatic reactor startup simulations were initiated. Testing was interrupted to perform routine maintenance and repairs on auxiliary equipment, and, in early January 1969, full rate automatic reactor startup simulations were completed.

a. Reactor Fuel Loading

Fuel loading was performed with the reactor located out in the test vault. The reactor vessel rested in a structure which located the bottom of the vessel 8 in. from the vault floor. The entire core was initially filled with stainless-steel dummy elements, which were replaced with the real fuel elements as loading proceeded. Fuel loading of the reactor was begun on June 18, 1968, and was completed on June 28, 1968.

Reactor criticality was achieved at 0126 a. m. on June 22, 1968, with 169 fuel elements and 42 stainless-steel dummies in the core. Extrapolation of the $1/C$ plots from the 164 element loading gave a critical fuel loading of 164.5 fuel elements. The fully loaded reactor was critical with Control Drums 1, 3, and 5 full out, 2 and 4 full in, and 6 at 56.44° . Ambient temperature was 92°F . The measurement was made with 1-curie Pu-Be neutron source located 2 ft above the top of the reactor core. From the last several fuel loading step critical positions, the average worth of an element in the outside ring was estimated to be 19¢.

b. Critical Tests

The reactor, as installed in the vacuum vessel and with coolant drained, was taken critical at 0442 on November 29, 1968. The critical position was with

Control Drums 1, 3 and 5 full out, 2 and 4 full in, and 6, at 67.36°. Reactor temperature was 62°F. The shutdown margin for this condition was \$5.76. Excess reactivity available was \$12.85. Because of the required extrapolation to obtain a value of drum worths with neighbors full in, the excess reactivity value was assigned a possible error of ±\$1.00.

Comparison of critical configurations for the reactor in the vacuum vessel and out on the test vault floor show a reactivity worth contribution of 32¢ for the vacuum vessel pit. NaK coolant worth was found to be 18¢ (see Table 25).

TABLE 25
SUMMARY OF CRITICAL ASSEMBLY TESTS

Parameter	Design Prediction	Experimental Result	Remarks
Total Isothermal Temperature Coefficient of Reactivity			
at 70°F	-0.21¢/°F	-0.17¢/°F	
at 1200°F	-0.23¢/°F	-0.19¢/°F	
Power Coefficient of Reactivity for Constant Mean Temperature	-0.03¢/kwt		
Reactivity Defect to 600 kwt and 1200°F Core Average Temperature	-\$3.00		
NaK Reactivity Worth	+\$0.25	\$0.18	
Clean Wet Excess Reactivity	\$14.40		
Reactivity Inventory (12,000 hr)			After 1000 hr
Hot, End of Life Excess (600 kwt)	\$7.75	\$3.70	
Samarium Burnout	\$2.50		
Fission Product Poisoning			
Xenon	-\$1.05		
Samarium	-\$1.09		
Other	-\$1.10		
Axial Hydrogen Redistribution	\$0.29		
Hydrogen Loss	-0.5¢/deg	0.5¢/deg	Initially
U ²³⁵ Burnup	\$0.62		

Data for the reactor temperature coefficient and the operating temperature reactivity defect are shown in Figures 60 and 61. The temperature coefficient ranged from $-0.17\text{¢}/^{\circ}\text{F}$ at 200°F to $-0.19\text{¢}/^{\circ}\text{F}$ at 1300°F .

c. Control Drum Calibrations

Typical integral and differential control drum calibration curves are shown in Figures 62 and 63. Figure 64 is a graph of total drum worths as a function of neighbor drum position. The total excess reactivity was too large to allow complete calibration of a drum with neighbors full in, so the total values were extrapolated from partial calibrations according to the curve shape from

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Figure 60. S&DR Isothermal Temperature Coefficient of Reactivity

Figure 61. S8DR Reactivity Defect from Temperature Base Point = 204°F

Figure 62. Control Drum 1 Worth Curve

Figure 63. Control Drum 1 Differential Worth Normalized

calibrations with neighbor drums in intermediate positions. The average drum worth with neighbors full in was then determined to be \$4.43. The worth of a drum facing a row of steel dummy elements was found to be 62% of the worth when the drum faces real fuel elements.

After the reactor was put into place in the vacuum vessel and the NaK coolant was loaded, another series of control drum calibrations were performed. The resulting worths were 12 to 14% lower than those from the dry critical calibrations which had been done on the test vault floor.

A final series of drum calibrations were performed after final reactor shutdown. The results had not changed significantly from the beginning of life results. Drum worths for operating configurations are given in Table 26.

d. Initial Approach to Power

The initial approach to full 600-kwt design power was initiated January 17, 1969, following the automatic startup tests. Reactor power was brought to 150 kwt (one-fourth of design power) with primary and secondary NaK loops

6531-5549

Figure 64. Drum Worths As A Function of Neighbor Drum Positions
(Dry Critical Tests)

TABLE 26
CONTROL DRUM WORTHS
(NaK in Core)

Control Drum	Neighbor Positions	Worth Result (¢)	
		Beginning of Life	End of Life
	(°)		
1	68.14	299	303
3	68.14	290	287
5	68.14	321	322
1	35.12	353	358
3	35.12	347	349
5	35.12	376	372
1	out	245	
2	out	248	
4	out	234	
6	out	240	

circulating at full design flow. Flows were then reduced gradually to ~one-fourth of design flow, to achieve 1100° F inlet, 1300° F outlet reactor design temperatures. All system and facility process instrumentation was monitored for off-normal indications, and flow and reactivity oscillation tests were conducted.

After >8 hours, flows were gradually increased to ~one-half of design while reactor power was programmed to 300 kwt to maintain design temperatures. The reactor was then brought to power in steps allowing about 8 hours at each intermediate power level.

e. Transfer Function Analysis

The objectives of the flow and reactivity oscillation tests were to measure the reactor transient response and to determine the reactor partial temperature coefficients and the average fuel-to-coolant transfer coefficient.

The flow oscillation tests and the reactivity oscillation tests were performed at each power step of the first approach to 600 kwt. These tests were

repeated during the extended runs at 600 kw and 1 Mwt. For each condition, several reactor system parameters such as power, flow, inlet temperature, outlet temperature, and in-core temperatures, were recorded on FM magnetic tape and on a brush strip chart recorder for further analysis. The system was perturbed during the recordings by (1) a primary flow driving function, and (2) a reactivity driving function. The perturbations were generated at fixed frequencies and the resulting responses analyzed by time averaging analysis which determines the average response of a variable over many cycles.

The flow oscillation system was connected through a transformer across one phase of the primary pump. An adjustable level bank was used to control the magnitude of the flow perturbations and a signal generator switched the load on and off at the desired frequency. The magnitude of the perturbations was held to a maximum of 3% of indicated flow.

The reactivity oscillations were performed by driving a control drum through the automatic controller inputs to the drum actuator. This allowed the drum to be oscillated when the control mode switch was in "automatic" and also allowed the operator to assume manual control merely by switching to "manual." A dual counter was connected to the "drive in" and "drive out" relays of Control Drum 1, and set to operate at the desired frequency.

The signal averaged results for the flow oscillation tests at the 150, 300, and 450 kw approach to power steps were inconclusive. The small number of cycles collected for the lowest oscillation frequency (0.002 Hz) did not allow a good statistically averaged signal to be obtained for this test. Also, the reactor conditions (i. e., xenon buildup and hydrogen redistribution effects) did not allow these low frequency tests to proceed without making intermediate control drum corrections. During the 0.002 Hz flow oscillation test, the reactor operators would regularly compensate for the effects of a flow step by stepping a control drum a few minutes later. These events made the low frequency data more complex than was intended for the planned analysis.

The intermediate frequency (0.03 Hz) oscillations were simulated with an analog computer model of the reactor. The model included six delayed neutron groups for reactor kinetics, and five axial nodes for fuel and for coolant heat transfer. A fuel temperature coefficient of -0.10 to -0.11% /°F and a UA of

4 Btu/°F was used in the attempt to match the experimental traces. Of particular interest was the apparent rise in power at the decreasing flow step for the 300- and 450-kwt intermediate flow oscillation tests. An attempt to simulate this particular effect was a positive upper plenum temperature coefficient was unsuccessful. Because of the anomalous reactivity events that were occurring during this time and the reactor operators making manual drum corrections, the time-averaged signals had phase errors and amplitude distortion that would mask any positive effect on power. However, a sustained flow coefficient between steady-state conditions was observed.

The power-to-flow transfer function for the approach-to-power tests is shown in Figure 65 along with values from flow oscillations at 600 kwt.

Figure 65. Reactor Transfer Function

Additional drum oscillation tests were performed with the reactor at 750 kwt. The upper curve in Figure 66 shows the transfer function obtained in these tests.

Figure 66. Results of S8DR Drum Oscillation Test at 730 kw

The power transfer function was divided by the zero power transfer function to find the fuel time constant break point. The results of this manipulation are shown as the lower set of points in Figure 66. The break point occurs at 0.029 Hz which gives a fuel time constant of 5.5 sec. The fuel time constant equals fuel heat capacity, MC divided by the fuel heat transfer coefficient UA. The fuel heat capacity value of 32 kwt-sec/°F divided by the derived time constant of 5.5 sec yields a value of 5.9 kwt/°F for UA at the time of the test. The reactor had accumulated 683 Mw-hr energy release at the time of this test.

f. Automatic Startup Tests

The first automatic startup test, denoted by Run A.1 was initiated on December 13, 1968, with system temperature of 200°F and 20% coolant flow. The reactivity insertion rate was 0.00255 \$/min, or approximately one-half of the analytically determined desired startup rate.

TABLE 27
PRE-MERCURY INJECTION STARTUPS

Parameter	Half-Rate Startups									Full-Rate Startups				
	A.1	A.1.a	A.2	A.3.a	A.3.b	A.3.c	A.3.d	A.4	A.5	B.1	B.2.a	B.3	B.4.a	B.5
Date Performed	12-13-68	12-13-68	12-17-68	12-18-68	12-19-68	12-19-68	12-19-68	12-20-68	1-8-69	1-10-69	1-11-69	1-13-69	1-14-69	1-15-69
Time at 50% Subcritical	0612 35	1307	2006	-	-	-	-	-	1130	1951.6	1918.2	0034	1133	1101
Control under which Insertions Made	Automatic	Manual	Manual	Manual	Manual	Manual	Manual	-	Manual	Automatic	Automatic	Automatic	Automatic	Automatic
Initial Temperature (°F)	200	209	200	907	950	1050	1200	400	200	200	200	200	200	200
Initial Insertion Rate (sec/step)	240	-	120	-	-	-	-	-	120	130	130	110	110	110
Final Insertion Rate (sec/step)	-		240	-	-	-	-	-	240,180					
Remarks		Objective was a period of ~100 sec						Primary flow was oscillated with reactor power at 20 kw	Reached 1300° F, first deadband control demonstration	Secondary flow ramped from 20% to 100% when power reached 30 kw				

Results of this first run were obscured by malfunctioning of the automatic controller. However, the first power peak occurred much lower than the predicted 46 kwt, based on the predicted fuel-to-coolant heat transfer coefficient (UA) shown in Figure 67. Run A.1.a was then performed to verify the low value

Figure 67. S8DR Core Fuel-to-Coolant Heat Transfer Coefficient, UA

of UA implied by run A.1. Sufficient reactivity was inserted to put the reactor on a period of approximately 100 sec, then temperature feedback was allowed to turn the power rise over. Power was then held level at one-half the peak power, or 3 kwt, while reactivity feedback from the fuel temperature rise was compensated by drum insertions. The time constant measured during this test was approximately 27 min, and this long-time constant was another indication of low fuel UA. The "predicted" curve shown on Figure 67 was derived from the "theoretical" curve, coupled with the anticipation of a residual hydrogen pressure in the gas gap between fuel and cladding at low temperature.

In an attempt to learn more about UA, additional transients and automatic startups were conducted over a temperature range of 400 to 1200°F. These runs are shown in Table 27. Analysis of the data from these runs resulted in the "estimated" curve shown in Figure 67. The close agreement between

TABLE 28
MERCURY INJECTION SIMULATIONS

Parameter	B.1	B.1.a	B.1.b	B.2	B.2.a	B.3	B.3.a	C.1	C.2	C.2.a	C.3	D.1, D.2, D.3, D.4, and D.5
1969 Date Performed	1-23	1-23	1-23	1-24	1-24	1-24	1-24	1-25	1-25	1-25	1-25	1-25
Primary Flow (lb/hr)												
Initial	24,400	24,400	24,400	24,400	24,400	24,400	24,400	24,400	24,400	24,400	24,400	24,400
Final	24,400	24,400	24,400	24,400	24,400	24,400	24,400	42,200	48,800	48,800	48,800	48,800
Secondary Flow (lb/hr)												
Initial	9,760	9,760	9,760	9,760	9,760	9,760	9,760	9,760	9,760	9,760	9,760	9,760
Final	9,760	9,760	9,760	9,760	9,760	9,760	9,760	29,500	47,000	48,800	29,300	48,800
Initial Outlet Temperature (° F)	1300	1288	1285	1284	1267	1279	1280	1256	1252	1240	1248	1245 to 1275
Initial Reactor Power (kw)	170	157	157	166	148	162	165	180	200	190	200	175 to 190
Initial ABHX Slug (kw-hr)	6.2	3.6	6.2	8.9	11.6	10.7*	16.0*	10.0	10.0	Special run to evaluate ABHX effects; not a simula- tion.	12.5	10.0
Louver Position Afterward	Open	Closed	Closed	Closed	Closed	Closed	Closed	Closed	Closed		Closed	Open
Second ABHX Heat Load Setting (kw)	0	0	0	0	0	0	0	400	40		500	500

*Based on heat removal being proportional to ABHX fan speed; actual cold slug would have been less.

theoretical and estimated values is an indication that essentially all gaseous hydrogen is lost from the gas gap at lower temperatures.

Five automatic full-rate startups were performed during the period January 10 to January 15, 1969. Initial conditions for each startup are listed below:

- 1) Primary and secondary loop coolant flow at 20% rated (9,760 lb/hr) and 200°F isothermal temperature
- 2) Trickle flow through the ABHX; secondary loop heater off
- 3) Initial reactor power over the range 0.1 to 1.5 watts.

All startups were initiated by ramping drums alternately (first the startup drums, then the control drums) at maximum drive rate of 2 1/2 sec/step or 2.08°/min/drum, until startup drums were banked at 68° and control drums were banked at approximately the 50¢ subcritical position. From this configuration, the three control drums were stepped in one at a time at a reduced rate. For the first two startups, the automatic controller was set to step a drum one step each 130 sec, and drum insertions continued until the 1300 ^{+20°F}_{-10°F} deadband was reached. The last three startups were the same, except the automatic controller stepping interval was 110 sec from the 50¢ subcritical condition.

These full-rate startups verified that the SNAP 8 reactor could be automatically brought to power over an eight-decade power range from shutdown, in a well controlled manner, without excessive power overshoot on entry into the useful power range, and without excessive reactor fuel or coolant temperature transients.

g. Mercury Injection Simulations

In late January 1969, a series of mercury injection simulations was performed, as shown in Table 28. The "B" and "C" series were limited simulations for the purpose of verifying system response. The final five simulations, the "D" series, were full simulations performed to duplicate the proposed mercury injection phase of the SNAP 8 startup sequence.

The mercury injection simulations performed on the S8DR verified the capability of the reactor to meet requirements for the proposed automatic startup

of the electrical generating system. The tests were restricted by ground test system component stress considerations to approximations of the desired mercury injection simulation; however, these approximations were close enough to predicted flight conditions to indicate conclusively that the SNAP 8 reactor would respond as required in the flight system.

2. Experimental Results — Power Tests

On January 23, 1969, a series of Hg injection simulations was initiated and, at the conclusion of the final simulation on January 25, 1969, the 500-hour endurance test was initiated at 600 kwt, 1300° F reactor outlet and 200° F reactor ΔT . The endurance test was completed on February 24, 1969, with two short interruptions due to scrams.

On March 20, 1969, the 9000-hour endurance run was initiated at 600 kwt, 1300° F reactor outlet and 200° F reactor ΔT . The run continued through April 10, 1969, when reactor power was increased to the alternate 1-Mwt design power. Outlet temperature was reduced, but because of heat rejection limitations in the airblast heat exchanger, steady-state rejection of 1 Mwt at 1100° F reactor outlet temperature could not be achieved. The 1-Mwt run was conducted at 1150° F outlet temperature and 270° F reactor ΔT .

On April 28, 1969, the reactor was returned to the 600 kwt, 1300° F outlet, 200° F ΔT condition and the 9000 hour endurance run was initiated. On June 1, 1969, the endurance run was interrupted by a loss of flow scram (see Appendix 2). Due to excessive reactivity loss rate, it was decided to continue the endurance run at a lower temperature. The run was continued on June 5, 1969, at 600 kwt, 1200° F outlet and 200° F ΔT . This endurance run was terminated prematurely due to excessive reactivity loss rate resulting from failed fuel cladding. Shutdown from the final endurance run was initiated on November 20, 1969, when reactor power was reduced to 300 kwt while holding 1200° F outlet and 200° F ΔT . After 11 days outlet temperature was reduced to 1140° F, power returned to 600 kwt while holding 200° F ΔT and these conditions were maintained for 7 days. Reactor outlet temperature was then increased to 1200° F for 7 days, then final reactor shutdown was initiated.

Shutdown was accomplished by reducing outlet temperature to 1000° F while holding 600 kwt and 200° F ΔT , then reducing power to collapse outlet temperature

to 800°F while holding inlet temperature at 800°F. The reactor was held critical at 800°F to follow the xenon transient, and on December 21, 1969, temperatures were reduced to 400°F to monitor reactor decay power level.

On January 6, 1970, system temperatures were raised to 750°F and on January 9, 1970, the primary system was drained. Reactor in-core thermocouple temperature dropped steadily and leveled at 180 to 200°F.

A summary of the S8DR operating history is shown in Figure 68. The measured integrated power over the S8DR operating period is shown in Figure 69. The observed excess reactivity of the S8DR over its operational life is shown in Figure 70.

a. Fission Product Release

Fission product monitoring in the S8DR was much more extensive than in the S8ER test. Both the surge tank cover gas and the primary NaK coolant were sampled periodically to supplement the on-line monitoring (gamma spectrometer) of the surge tank cover gas. The results of these measurements over the operating life of the S8DR are shown in the following data. Figure 71 plots the Xe^{135} , Xe^{133} , and Cs^{137} levels detected in the NaK coolant.

The overall picture presented is one of gross cladding failure in the period of May through June 1969, followed by periodic failures of cladding tubes throughout the remaining operational life.

b. Cyclic Outlet Temperature Variation

A temperature instability, characterized by a sudden channel outlet temperature change followed by a slow ramp recovery, was noted in the first month of S8DR steady-state full-power operation. It was postulated that this was due to a clustering-declustering motion of the fuel elements so as to restrict coolant flow in the channel.

A thermal gradient across the core results in differential expansion of the fuel rod and cladding in the direction of the gradient. The unconstrained motion of the rod within the cladding results in greater heat transfer to the cladding on that side, with consequent increases in cladding bowing. All of these effects

70MA22-54-7

Figure 68. S8DR Operating History - 1969

70-J13-003-22

Figure 69. S8DR Operating History
Integrated Power (1969)

70-J6-003-8

Figure 70. S8DR Excess Reactivity, Normalized to 1200° F Temperature Defect

Figure 71. S8DR Operating History, Fission Product Release to Coolant (1969)

LOWER GRID
PLATE,
DOWNSTREAM
SIDE

DR-67

BAFFLE PLATE,
DOWNSTREAM
SIDE

DR-66

FLOW
DISTRIBUTOR

DR-70

70-MA13-51-30

Figure 72. Inlet Plenum Components After Cleaning

reinforce each other so that the elements will bow together and restrict the flow within the channel formed by three adjacent elements.

Were it not for hydrogen redistribution, the process would end with the initial bowing. The effect of hydrogen redistribution, however, is sufficient to reverse the initial bow and introduce a bow in the opposite direction. The initial bowing is complete within a matter of minutes, while the hydrogen redistribution effect requires longer times.

The postulate was investigated both analytically and experimentally, and the direction and magnitude of the effects were confirmed. The effect can be prevented by the incorporation of positive spacing devices such as fins or wire wrap or by segmenting the fuel material into pellets.

3. Post- Test Analysis — Hardware

Examination of the core internals and core support structures (exclusive of the fuel elements) revealed both the inlet plenum hardware (Figure 72) and outlet plenum hardware (Figure 73) to be in excellent condition with no indication of mechanical damage or coolant flow blockages by foreign material. The postulate that the S8DR core behavior was attributable to NaK coolant flow blockage by foreign material being lodged in either the inlet or outlet plenum structures seems obviated by these observations.

Visual examination of the stainless-steel-clad BeO internal reflector pieces failed to indicate any anomalies.

4. Post-Test Analysis — Fuel Elements

a. External Surfaces

One observation made during the course of the core examination was the existence of a thin, highly adherent, metallic film observed to some extent on all of the fuel element external surfaces. Analyses of samples of this film indicated the following:

- 1) The film was magnetic, of FCC structure, and was generally of the composition 40% iron, 40% nickel, and 20% chromium.
- 2) The film was thin, generally being of the order of 0.1 mil thick or less and was highly adherent.

BEFORE CLEANING

DR-16

BEFORE CLEANING

DR-13

DR-8

AFTER CLEANING

DR-10

BEFORE CLEANING

DR-15

AFTER CLEANING

DR-17

70-A1-034-4

Figure 73. S8DR Top Cover — Bulkhead Assembly After Removal

- 3) The constituent composition of the film varied both radially and axially within the core, with the iron content generally decreasing from the center of the core outward while the nickel content correspondingly increased; at the same time, the iron decreased from the inlet to outlet region of the core with the nickel content increasing.

Activation analysis of the film samples indicated that the film had existed on the surface of the fuel elements for an extended period of time with indications being that the film was at least four months old and probably older. The film is thought to be the result of a chemical potential phenomenon wherein the reactor system is apparently seeking a common chemical composition for the surfaces-coolant interaction.

A total of 67 elements were found to have cracked cladding tubes. An additional five elements had fuel and/or cladding profiles characteristic of cracked elements and so were classified as cracked, bringing the total cracked element count in S8DR to 72 (see Figure 74).

Figure 75 illustrates typical appearances of the cladding cracks. In general, the cracks were:

- 1) Short, generally being 1 in. long or less (see Figure 76)
- 2) Tended to occur in the Z/L region from 0.5 to 0.8 (see Figure 77)
- 3) Tended to occur within 15 degrees on either side of the closest approach of the cracked element with its adjoining neighbors (see Figure 78)
- 4) Tended to show no relationship to the direction of fuel element bow (see Figure 79).

The core location of the cracked elements, as previously noted, is shown in Figure 74. It seems that the occurrence of cracked elements tends to favor the so-called horseshoe pattern with the open end of the horseshoe being very nearly diametrically opposite from the coolant inlet nozzle location.

b. Internal Conditions

Observations of the fuel element internals on those elements processed through the disassembly operation during the detailed examination phase showed the following:

70-MA11-51-8B

Figure 74. Core Locations of Cracked Elements
(As Viewed Looking Down to Top of Core)

ELEMENT 6-24

DRS 708-6

ELEMENT 6-22

DRS 705-1

ELEMENT 6-05

DRS 403-4

ELEMENT 7-34

DRS 185-3

ELEMENT 6-20

DRS 149-1

ELEMENT 5-01

DRS 653-1

70-MA13-51-31

Figure 75. Appearance of Some Typical Cracks

70-MA14-51-53D

Figure 76. Length of Crack

70-MA14-51-54D

Figure 77. Z/L Location at Crack Centerline

70-MA-14-51-52C

Figure 78. Orientation of Cracks

70-JU3-034-14B

Figure 79. Angular Relationship Between Direction of Fuel Element Bow and Azimuthal Location of Cladding Cracks

70-AU27-78-28A

Figure 80. Typical Type 1 Element

70-AU27-28-28B

Figure 81. Typical Type 2 Element

70-AU-27-78-28C

Figure 82. Typical Type 3 Element

7759-51199

Figure 83. Typical Type 4 Element

AI-AEC-13070

- 1) On those elements characterized by little or no cladding strain, the fuel rods were generally intact, exhibited a shiny matte appearance, and the hydrogen barrier appeared to be in good condition (Figure 80).
- 2) Cracked elements (Figure 81) indicated typical NaK-fuel-ceramic reaction zones on both the cladding internal surfaces and fuel rod OD as have been seen before in the S8ER. Generally, the fuel rods were cracked transversely with some indicating fine longitudinal hairline cracks on the surfaces of the fuel rods.
- 3) In those elements which were subsequently determined to have sustained a high hydrogen loss (and did, in fact, enter locally beta phase at operating temperatures), a typical observation was that the ceramic had been spalled from the cladding ID and had adhered to the fuel rod OD (see Figure 82). The fuel rods also exhibited numerous longitudinal cracks in the immediate vicinity of the high hydrogen loss zone in the fuel rod.
- 4) Eight elements exhibited longitudinal cracks in the fuel rods in the inlet region of the elements (see Figure 83). The presence of these longitudinal cracks was the key characteristic of such elements.

c. Fuel Element Dimensions

Cladding dimensional data were taken on all (except some of the elements from Ring 9) of the S8DR fuel elements during the course of the screening examination. In addition, a neutron radiography (n-ray) technique was developed to obtain fuel rod OD measurements on the nondisassembled elements during the screening examination. During the course of the detailed examination, it was, of course, possible to obtain direct fuel diameter measurements on some fuel rods — measurements which were subsequently compared and found to agree well with the n-ray measurements previously obtained.

The diameter measurements were obtained using a specially designed profilometer rig at the AI Hot Laboratory (AIHL). This device uses linear voltage differential transformer (LVDT) transducers, horizontally opposed, to obtain continuous readings of the cladding or fuel rod which is rotated within the rig

Figure 84. Typical Profilometer Trace

as the transducers move axially along the length of the specimen. Accuracy of the measurements is $\pm 1/10$ mil. A typical profilometer trace is shown in Figure 84.

From the cladding dimensional traces, an estimate of the cladding deformation can be obtained. In general, cladding OD's increased slightly (~ 1 to 5 mils) while the overall fuel element length decreased slightly (~ 10 to 20 mils).

d. Fuel Densities

Density measurements were taken on selected fuel samples using the Archimedeian principle. Monobromobenzene was used as the immersion fluid with periodic checks being made on suitable NBS standards to assure both the accuracy and precision of the measurements. Accuracy of the measurements is $\pm 0.1\%$.

e. Hydrogen Content

The hydrogen content in the fuel is of interest in determining both the final fuel hydrogen level (and so to determine the total hydrogen loss), and also in determining the end-of-life (EOL) fuel operating temperatures since hydrogen content is directly related to fuel temperature through the ZrH_x phase diagram.

Fuel hydrogen content measurements were made by preparing a special fuel sample by Elox machine techniques. This sample consisted of five approximately $1/16$ -in. OD by $1/4$ -in. long posts (or pegs) standing on a base of fuel material. This sample, cut from a cross-section of the fuel rod at some appropriate axial location, was then used for the hydrogen measurement by breaking off the individual posts and subjecting them to a vacuum extraction technique. The post was heated to about $2500^\circ F$, and the hydrogen which was released from the fuel was collected in a special apparatus which measured the volume of hydrogen released. Accuracy of the measurement was $\pm 0.02 (H/Zr)_{eff}$.

This technique was employed during the course of the S8DR examination for quantitative hydrogen content measurements. Prior to the beginning of the examination, an attempt was made to use the neutron radiography technique to measure quantitatively the hydrogen content within the fuel. While this technique failed to give the desired quantitative results, it did furnish a valuable tool since it could be used to isolate those elements, and indeed the regions

within those elements, which had lost a substantial amount of hydrogen from the fuel rod (and, as subsequently determined, had indeed entered beta phase fuel operation in the local axial region of high hydrogen loss).

f. Fuel Element Permeation

While determination of the gross hydrogen content in the fuel rod by direct vacuum extraction technique as described in the previous section provides an overall measure of the in-reactor performance of the hydrogen barrier, it does not give the local hydrogen retention characteristics of the barrier in regions of the cladding tube end closures nor in the main body of the cladding tube itself. Such data can be obtained by permeation-zone scan test techniques.

The permeation test consists of measuring the hydrogen leakage through the element hydrogen barrier by heating the entire element to an elevated temperature and then measuring with a mass spectrometer the amount of hydrogen gas permeating through the hydrogen barrier-cladding tube composite.

The zone scan rig is essentially a permeation test furnace fitted with a suitable orificing device which provides a means for determining the axial location of hydrogen leakage through the barrier-cladding composite. The total leakage is also measured directly, thereby providing a checkpoint with previous permeation test data on the element which is being zone scanned.

Permeation results were obtained on a total of 22 S8DR elements. Zone scan results were obtained on a total of four S8DR elements.

Three sets of data obtained in the S8DR examination relate directly to the hydrogen barrier performance. These are:

- 1) Hydrogen loss as calculated from the hydrogen content measurements made by vacuum extraction techniques
- 2) Permeation test results
- 3) Zone scan test results.

The hydrogen loss measurements are listed in Table 29 below for the four types of elements. These losses integrated over the number of such elements found in S8DR allow the calculation of an overall hydrogen loss of about 5.5% from the core, which corresponds to a reactivity loss of about \$3.40 due to hydrogen

TABLE 29
HYDROGEN LOSS DATA

Element	Type	Hydrogen Loss (%)
2-06	1/4	1.0
3-09	1	1.4
4-08	1	1.7
5-05	1/4	0.4
6-01	1	1.4
7-02	1	4.8
7-16	1	4.5
7-31	1	5.4
9-20	1	1.2
		Average = 2.4 for Type 1
6-26	2	8.9
8-03	2	4.8
		Average = 6.8 for Type 2's
6-02	3	9.6
6-23	3	10.5
7-30	3	19.4
		Average = 13.2 for Type 3's

leakage from the core. This is in fair agreement with observed reactivity losses due to hydrogen leakage ($\sim 4.9\%$) which are calculated to be \$3.10.

The permeation and zone scan test results from a checkpoint on the hydrogen loss data enable the calculation of the barrier performance history of Type 1 elements in the S8DR core. Such calculations were made using a computer code to process the S8DR permeation and zone scan results.

The results obtained indicate the following:

- 1) If Type 1 elements had behaved in a duplicate manner with ex-pile permeation tests (endurance tests run for 8,000 to 10,000 hours),

calculations indicate that a typical Type 1 element would have lost approximately 0.7% of its hydrogen.

- 2) To obtain the observed average hydrogen loss from a Type 1 element (i. e. , 2.4% — see Table 29), it is necessary to utilize the zone scan results and apply them to a typical Type 1 element operating under the actual power, temperature, and time conditions associated with the S8DR reactor core operation. Results indicate that the prime barrier degradation had to occur early in life to match this observed hydrogen loss and still remain consistent with observed average permeation rates on Type 1 elements (i. e. , about 0.4 cc/hr-sec). The discussion is, of course, based on average Type 1 elements. Some S8DR fuel elements lost only the minimal amount of hydrogen (~0.7%) implying essentially no barrier damage in such elements.
- 3) Zone scan results indicate that the majority of the barrier degradation in Type 1 elements occurred in the tube section of the elements with the end closures sustaining substantially less degradation; these data are used in the computer model to calculate the observed 2.4% hydrogen loss from Type 1 elements.

When the hydrogen loss from Type 1 elements and the permeation rate and zone scan results from Type 1 elements are considered together, the data are found to be consistent and in agreement with the calculational model employed. The early life barrier damage in the tube section of the Type 1 elements is thought to be the result of tube loading stresses (due to fuel element clustering and unclustering early in life) in the barrier resulting in the observed barrier damage. Ex-pile experimental stress analyses tends to support this hypothesis.

The step increases in reactivity loss rate observed in the early May to late July scrams (see Section IV-E-2) are thought to be associated with the formation of Type 3 elements. Visual observations of such elements (Section IV-E-4) suggest that barrier damage resulted from stripping of the barrier (which had adhered to the fuel rod as a result of intimate high stress contact produced by overtemperature fuel growth and/or element-to-element loads) from the cladding substrate due to differential axial motion of the fuel rod and cladding during the scram thermal transient(s).

g. Fuel and Cladding Metallography

Selected fuel rod and cladding tube samples were metallographically examined. Typical observations showed the fuel and cladding material metallurgical structure(s) in accordance with expectations based on previous experience with these irradiated materials.

Independent cladding fractography analyses were performed by both the NASA-Lewis Research Center and Rockwell International Science Center. Analyses of cracked cladding fracture surfaces indicated a stress-rupture failure mode and did not show any evidence of a fatigue mechanism.

h. Element Categorization

The results of the fuel element dimensional measurements, that is, profilometer measurements on the cladding OD and n-ray measurements on the fuel rod OD, coupled with qualitative measurements of high hydrogen loss elements by n-ray (see Figure 85) led to the development of three basic types, or categories, of elements and one subtype which were observed in the S8DR core. The element types and their characteristics are as follows.

- 1) Type 1 elements: Basically intact with diametral cladding strain less than 0.3%; a total of 111 elements in the S8DR core were in this element category - 110 intact, 1 cracked.
- 2) Type 2 elements: Basically cracked elements, with diametral cladding strain generally greater than 0.3%; 69 elements were in this category - 58 cracked, 11 intact.
- 3) Type 3 elements: High hydrogen loss elements (as seen by n-ray) generally exhibiting greater cladding ovality (than the other types of elements) localized in the area of high hydrogen loss in the fuel rod; a total of 31 elements were in this category - 18 intact, 13 cracked.
- 4) Type 4 elements: A subgroup that exhibited longitudinal cracks in the fuel rod in the inlet region of the element, some of which showed some measurable diametral cladding strain in the inlet region; 8 of the S8DR elements were in this subtype - 6 intact, 2 cracked.

a. Characteristic Fuel Rod Profile (n-ray)

b. Typical Associated Cladding Profile(s)

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Figure 85. Categorization of Fuel Element Types

The element categorization, while not based upon an absolute scale but rather upon a set of subjective criteria, nevertheless forms a useful basis for subsequent comparison and discussion of the S8DR examination findings and analysis of the data. Subsequent discussions and analyses will refer to the elements on the basis of this categorization scheme.

i. Fuel Growth Isotropy

Direct measure of the isotropic-anisotropic growth behavior of the fuel is obtained by considering the ratio $\Delta V/V/\Delta D/D$. When this ratio is 3, the fuel growth is isotropic. If the value is between 2 and 3, the fuel growth is preferentially in the radial direction. Values greater than 3 indicate preferential axial growth with respect to radial growth while values less than 2 indicate axial shrinkage.

The mean value of the ratio for delta phase fuel (corrected for local hydrogen content) is 2.8 with a standard deviation of ± 0.4 . The mean value for the epsilon phase fuel material is about 1.6 with the standard deviation being about ± 0.5 .

The degree of anisotropic growth of the delta phase fuel material (i. e. , 2.8) is in reasonably good agreement with previous observations. The small number of samples (5) in the epsilon population makes it difficult to assign much significance to the results.

j. Hydrogen Deduced Temperatures

As previously noted, fuel rod axial temperature distributions can be calculated for a given element if the axial hydrogen distribution of the fuel rod and a normalization temperature (e. g. , coolant inlet temperature) are known. The use of this so-called "hydrogen thermometer" is a result of the unique relationship between hydrogen and temperature reflected in the ZrH_x phase diagram. Using the hydrogen data along with the known element geometry (i. e. , fuel rod OD and cladding OD measurements, measured fission gas release, and known end-of-life (EOL) coolant inlet temperatures, a relatively straightforward calculation of late life fuel element operating conditions can be made.

The TEMPRO code was used to make these calculations. Input data consisted of element geometry, overall EOL fuel rod average hydrogen content, fission gas release fraction, operating power, radial and axial power distributions, coolant inlet temperatures, and appropriate reactor operating time. Output from the code consisted of fuel element and coolant axial temperature profiles, axial hydrogen distribution profile, and calculated coolant exit temperature from the element under consideration.

An iterative type of calculation was used wherein the coolant flow past the element was adjusted to bring the calculated hydrogen distribution into agreement with the measured hydrogen contents at discrete points along the length of the element (see Figure 86). The boundary conditions on the problem were the requirements that the coolant inlet temperature match the EOL (i. e. , late life, 1000° F condition) and that the calculated hydrogen distribution match the observed hydrogen content data points at discrete elevations along the length of the element. A third boundary condition which was used as a final check on the answer was that the calculated coolant exit temperature from the element should match reasonably well with the observed coolant outlet temperature as monitored by the outlet plenum thermocouples in such cases where a thermocouples reading was available in the immediate vicinity of the element under consideration. This third boundary point is shown in the temperature plots of Figure 86.

Having obtained a reasonable fit of the calculated hydrogen distribution to the observed hydrogen data point, the early life temperature conditions were calculated for each element by assuming that the coolant flow conditions along the length of the element at EOL conditions were the same as they were during early life conditions (i. e. , at beginning of life), during the megawatt run, and at the so-called offset conditions* (i. e. , at about 1600 hours accumulated operating time). Having made this assumption, the code inputs were varied suitably to account for actual operating conditions at these various times as illustrated in Table 30. The code output obtained gave operating fuel temperatures at the BOL, megawatt, and offset conditions. Figure 86 shows typical results for BOL, offset conditions, and the megawatt condition. These results coupled

*The offset condition is defined as the point in time associated with saturation of vacancy agglomeration fuel growth.

a. EOL Hydrogen Distribution

b. Temperature Offset - 1600 hr

c. EOL Temperature Distribution

d. BOL Temperature Distribution

e. Temperatures at 1 Mwt

Figure 86. Element 6-01
Thermal History

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with the observed fuel growths from density measurements on the S8DR fuel samples from these elements were used to assess the temperature sensitivity of the fuel growth behavior in S8DR.

TABLE 30
S8DR OPERATING HISTORY SUMMARY

Item	BOL	Megawatt Run	Offset	EOL
Coolant Inlet Temperature ($^{\circ}$ F)	1100	890	1100	1000
Reactor Power (kwt)	600	1000	600	600
Accumulated Operating Time (hrs)	0	1150	1600	7000
Element Operating Power	As defined by element core location and the observed radial and axial power profiles as verified by gamma scans.			
Hydrogen Content	As-built (H/Zr) eff	As-built (H/Zr) eff	As-built (H-Zr) eff	Post-test (H/Zr) eff
Element Geometry	As-built	As-built and post-test (two cases)	Post-test	Post-test

The calculations of operating fuel element temperatures from observed hydrogen distributions lead to apparent experimental confirmation of coolant crossflow in some regions of the S8DR core near the outlet end of the fuel elements. A typical example is shown in Figure 87 which is the EOL calculation for the element from Core Position 7-16. As can be seen, the fuel temperature required to fit the hydrogen data is in good agreement for a coolant temperature profile as shown up to the Z/L location of about 0.8. At this point, if the coolant temperature profile is allowed to continue to increase toward the exit end of the element, the resulting fuel temperature and consequently the hydrogen distribution fails to pass through the measured hydrogen points at Z/L 0.92 and 0.95. The only way that the hydrogen distribution can be made to pass through these points while maintaining the good fit in the Z/L 0.0 to 0.8 is to decrease the coolant temperature as indicated by the dotted line in Figure 87. From Z/L 0.8 to 1.0, the coolant temperature decrease in this region causes the fuel temperature to decrease, again as shown by the dotted line, and consequently the hydrogen distribution to increase in the same region. The coolant exit temperature

7759-51206

a. Hydrogen Distribution

7759-51207

b. Temperature Distribution

Figure 87. Element 7-16 EOL Conditions

from the element then matches the measured temperature (as indicated by the thermocouple directly above the Element 7-16) and forces a good fit on the hydrogen distribution. This coolant temperature decrease in the outlet region of the element would be the result of a coolant cross-flow occurring near the outlet end of this element.

The calculation of operating fuel element temperatures from observed hydrogen distributions also leads to the apparent influence of surge tank gas which was entrained in the primary NaK coolant. Figure 88 shows the calculated EOL fuel and cladding temperature profiles for the element from Core Position 4-08. The fuel element temperature required to fit the observed hydrogen data implies the indicated cladding axial temperature distribution. If one assumes cladding-to-coolant heat transfer coefficients of the magnitude associated with convective NaK cooling, the coolant temperature profile (A) is obtained — a profile which requires substantial local increases in NaK flow along the length of the element. Such local increases in coolant flow are unreasonable.

On the other hand, if one assumes a nominal coolant flow profile along the length of the element, an axial coolant temperature profile of the shape (B) is obtained. Use of the (B) profile with the indicated cladding profile (which is fixed by the fuel temperature profile) implies a lower convective heat transfer coefficient (h) at the surface of the cladding (viz., $\sim 800 \text{ Btu/hr-ft}^2 - ^\circ\text{F}$ vs $2500 \text{ Btu/hr-ft}^2 - ^\circ\text{F}$ for NaK conditions). The lower heat transfer coefficient can be ascribed to the influence of entrained surge tank gas in the NaK coolant.

k. Correlations with Copper Content

Fuel element cracks and fuel element swelling occur preferentially in a core zone identified as the "horseshoe" in Figure 89. A favored zone of low swelling exists in Ring 9 (the "bypass"), and the complementary set is identified in Figure 89 as the "keyhole." Within the family of high swelling fuel elements, there is the expected occurrence of a high incidence of cracked cladding, strained cladding, and broken fuel rods.

Elements in which β phase fuel occurred (Type 3 elements) appeared to have high local ovality and, on decanning of intact elements, were found to have pieces of the hydrogen ceramic barrier sticking to the fuel rod. The occurrence

7759-51208

a. Hydrogen Distribution

7759-51209

b. Temperature Distribution

Figure 88. Element 4-08 EOL Conditions

7759-51200

Figure 89. Temperature Zones for Copper Correlation

Figure 90. Fuel Growth of Temperature Groups vs Copper Content

7759-51210

of larger (~ 2 to 4 mils) than typical epsilon fuel growth in the Type 4 fuel rods was found to correlate with the axial fuel rod cracks in such elements. Fabrication variables examined exhibited no strong correlations with element damage in all cases except for pre-irradiation copper content.

Previous irradiation and reactor experiments had shown fuel swelling to be primarily dependent on fuel temperature. In order to minimize the influence of temperature differences between fuel elements, the three different core zones shown in Figure 89 were each separately analyzed, to identify any correlation between fuel swelling and fuel impurities measured during fuel fabrication. Table 31 shows the decreased fuel swelling observed with increasing copper content for each of the three core zones studied. The results are summarized in Figure 90, which groups elements within a band of copper concentrations for each core zone.

TABLE 31
S8DR FUEL GROWTH AS A FUNCTION OF COPPER CONTENT

	Horseshoe*			Keyhole*			Bypass*		
	Mean (mils)	σ (mils)	Number of Samples	Mean (mils)	σ (mils)	Number of Samples	Mean (mils)	σ (mils)	Number or Samples
High Copper (150 to 200 ppm)	8.1	1.9	15	5.7	1.9	6	3.9	2.2	4
Mid Copper (60 to 100 ppm)	10.4	1.6	29	7.5	1.2	16	4.9	1.4	16
Low Copper (20 to 60 ppm)	10.9	1.4	46	8.4	1.4	29	5.0	1.3	28

*See Figure 89.

Because of the strong correlation between high copper content and low S8DR fuel growth, S8ER fuel element data were analyzed. Fuel growth data on S8ER were very limited, so cracked cladding was taken as the fuel-growth-related parameter. Extensive fuel element damage masked identification of distinct fuel temperature zones, but because S8ER Ring 9 had fewer failures than the rest of the core, it was treated separately.

Results of the analysis are shown in Table 32. A significantly higher mean copper content exists for intact than for cracked elements in both the Ring 9 and Ring 1-8 groupings.

TABLE 32
S8ER FUEL COPPER CONTENT FOR INTACT AND CRACKED CLADDING

	Ring 1-8		Ring 9	
	Intact	Cracked	Intact	Cracked
Average Copper Content (ppm)	365	267	287	191
σ (ppm)	226	237	172	168
Number of Samples	24	134	20	22

The relation between high copper content and low irradiation-induced fuel growth cannot be established without a controlled experiment but the consistency of the correlation between S8DR and S8ER data and the high significance of both S8DR and S8ER correlations makes the copper-fuel swelling relation a very interesting finding. One possible mechanism explaining this effect is the substitution of a higher valence impurity in the lattice. Such an impurity may sweep vacancies from its neighborhood reducing local swelling. In the "horseshoe" (high temperature) region of the S8DR core, high copper elements (>150 ppm — the specified maximum allowable was 500 ppm) grew about 30% less than low copper elements, and in the "bypass" (low temperature) region about 10% less.

e. Summary

In summary, the observations, findings, analyses of data and synthesis of results from the S8DR post-test examination point to overtemperature fuel swelling as being the cause of premature fuel element ruptures. The overtemperature conditions in the core are attributable to the effects of hydraulic maldistributions arising from (1) element bowing and clustering, (2) greater than anticipated coolant bypass flow around the periphery of the core (caused to some extent by restricting of the coolant flow paths by element bowing and clustering), and (3) surge tank cover gas entrainment in the NaK coolant.

The Hastelloy N cladding behaved as expected with respect to strain-to-rupture design characteristics. The hydrogen barrier, while subjected to early life damage, performed to design expectations in several Ring 9 fuel elements.

The S8DR fuel growth data tend to indicate more temperature-sensitive behavior than is supported by prior irradiation test data on the ZrH_x fuel. The increased temperature sensitivity, if indeed it is a fact, would have acted to decrease the fuel growth design margins built into S8DR but of itself cannot be ascribed as a prime contributor to the S8DR failure mechanism.

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V. CONCLUSIONS

The SNAP 8 program carried the design, development, and testing of a compact nuclear reactor up through the testing under simulated space conditions of a flight design configuration. The capability of delivering 600 kw at 1300°F outlet temperature for extended periods of time was demonstrated.

In order to achieve this objective, a number of critical problems had to be solved. These included the development of the fuel element, the reflector control system, the heat transfer system, and the nuclear design. All of these items are covered in detail in SNAP topical reports listed in the references.

A. FUEL ELEMENT

The fuel element development was the most difficult problem. In both S8ER and S8DR, cladding failures were found. These appear to be associated with a loss of ductility of the Hastelloy-N cladding and its consequent inability to accommodate the swelling of the fuel material. After about 1 year of operation, the Hastelloy-N would accommodate less than 1% diametrical strain before failure.

The fuel material itself demonstrated a large activation energy for swelling. Operation with fuel temperatures beyond 1400°F appears to be beyond the state of the art. Temperatures in the vicinity of 1430 to 1450°F were observed to produce a large amount of swelling.

Single-piece fuel elements tended to cluster and decluster in a bistable manner. That is, elements would come together as a result of differential thermal expansion in such a manner as to restrict coolant flow between them. This restriction would enhance the heating and hence reinforce the effect. The effect was cyclic in that periodically the fuel cluster would switch to an opposite configuration.

On the other hand, it was found that fuel elements which did operate in the design environment did not swell, did not cycle, did not exceed 1400°F fuel temperature, and displayed hydrogen leak rates which corresponded to laboratory test results. Cracked fuel elements were of themselves not catastrophic.

Less than 0.1% of the fission products were released to the coolant with no observable deleterious effect.

Hastelloy-X was selected as the reference cladding material because of its better ductility after irradiation. A system of right-and left-hand fins was adopted to improve the cross-flow distribution so as to minimize local hot spots. This change, together with segmenting the fuel rod into pellets, should eliminate the cyclic clustering phenomena. With these changes, it is possible to design a core that will be free from the problems observed in S8ER and S8DR.

B. CONTROL SYSTEM

The rotating drum control system was selected for the SNAP 8 reactor. This led to the need for rotary actuators and bearings capable of operation in the vacuum, temperature, and radiation environment associated with this reactor. This requirement was met, and tests confirmed that the system life requirements could be met. Control drum actuator tests were conducted on three units for in excess of 20,000 hours at 1000° F and a vacuum of 10^{-6} torr.

Five sets of bearings were tested for 12,000 hours at 1150° F in vacuum conditions ranging from 10^{-5} to 10^{-8} torr. One hundred thousand hours of total experience with bearing tests were achieved. Three ground test scram kits were tested from 9000 to 15,000 hours at 10^{-5} torr and 850° F. Twenty times the expected number of scrams were performed.

C. HEAT TRANSFER SYSTEM

Early tests in the SNAP 8 program emphasized the importance of achieving proper coolant flow distribution. Investigations were conducted to establish the proper design of baffles and other flow control devices. The use of a flowthrough surge tank in the S8DR led to difficulties through the entrainment of the system cover gas.

Mass transfer effects within the coolant system were found to be small. In the S8DR it was found that material plated out onto the fuel elements which were Hastelloy in a system that was largely austenitic stainless steel.

D. NUCLEAR DESIGN

The primary nuclear design problem of the SNAP 8 system was the achieving of the necessary reactivity control. Initially, the power and temperature

coefficients of reactivity had to be predicted. The worth of reflector control devices had to be calculated. With the experimental data from test reactor operation the uncertainty in these quantities was essentially eliminated. In the Reference Zirconium Hydride Reactor it was necessary to design a control drum with a poison material backing rather than a void because of the radiation shield which surrounded the reactor. The design of these controls was performed using two-dimension discrete ordinates transports (DOT) code.

In order to minimize the control requirements, burnable poisons were studied. Certain rare earth isotopes were found to have cross sections in the correct range for effective long-term reactivity control. It was also found that these materials had a dramatic effect on the prompt temperature coefficient of the reactor. These poisons were also studied in conjunction with water immersion accidents in an attempt to achieve a reactor that would be safe under such conditions. The calculations for this problem were performed using one-dimension discrete ordinates codes together with burnup codes. Cross sections were prepared by weighting finely structured data with a standard SNAP spectrum.

E. SNAP 8 EXPERIMENTAL REACTOR

The experimental reactor (S8ER) was an engineering test of a prototype core, but did not have flight developed hardware. The developmental reactor (S8DR) was a nuclear system test to verify the operability of the integrated flight configuration.

Operation of the S8ER has demonstrated:

- 1) Sustained power operation capability. The S8ER released more than 5.1×10^6 kwh of energy during 500 days of nuclear operation, including 100 days at 600 kwt and 1300° F outlet temperature and 365 days at more than 400 kwt and 1300° F outlet temperature.
- 2) Static and dynamic stability.
- 3) Acceptable integrity of the interim hydrogen barrier coating on the fuel cladding tubes.

TABLE 33
SNAP 8 EXPERIMENTAL
REACTOR OPERATIONAL SUMMARY

Operation	Date
Initiated modification of Building 010 for use with S8ER	August 16, 1961
S8ER dry critical experiments, Building 373	September-December 1962
S8ER water immersion experiments, Building 012	January-March 1963
Initiated dry critical testing, Building 010	May 19, 1963
Initiated wet critical testing	June 23, 1963
Initial significant power operation	November 22, 1963
Initial operation at 600 kwt/1300° F	December 11, 1963
Completed a total of 60 days of operation at 450 kwt/1300° F to meet chief program endurance objective	April 28, 1964
Completed power test operations, Building 010	April 15, 1965

S8ER OVERALL ACHIEVEMENTS:

Total operating time	500 days (12,000 hours)
Total time above 400 kwt at 1300° F	365 days
Time at 600 kwt, 1300° F	100 days
Longest continuous run (September 18, 1964 to April 15, 1965)	Over 5000 hours
Energy release	Over 5.1×10^6 kwt
Operating efficiency (from first power operation, November 22, 1963, to completion of power operation, April 15, 1965)	91%

4) Capability of tolerating rapid changes in power level. During the power coefficient measurements, the core was subjected to approximately 115 rapid changes in power (100 kw in 1 minute).

5) Verification of SNAP 8 reference design.

Summaries of the S8ER operations are shown in Table 33.

F. SNAP 8 DEVELOPMENTAL REACTOR

The S8DR test established the flight readiness capability of the ex-core components of the reactor assembly. While the fuel elements exhibited failure modes similar to the S8ER elements, the failure rate was significantly reduced. At the time of test termination, the fission gas levels had stabilized at an equilibrium value and the excess reactivity loss rate could be linearly extrapolated to extend beyond design EOL values. There is every reason to expect that continued operation would have allowed the S8DR to fulfill all its design requirements as did the S8ER test.

A summary of the S8DR operating data is shown in Table 34.

TABLE 34
S8DR OPERATING SUMMARY

Critical Time (hr)	7533
Time at 300 kw power (hr)	7023
Time at 1000 kw (hr)	429
Time at nominal 600 kw (hr)	6688
At 1300° F	1670
At 1200° F	5575
At 1100° F	6688
Total energy release (kwht)	4.35×10^6

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