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PROGRESS REPORT
NOVEMBER 1967 - JANUARY 1968
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AEC Research and Development Report

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The previous progress report in this series was NAA-SR-12583, "SNAP Programs Progress Report, August-October 1967." (On January 1, 1968 the number prefix for technical reports on AEC projects was changed from NAA-SR to AI-AEC.)

I. INTRODUCTION

A. PROGRAM OBJECTIVES

The SNAP Reactor Program contains those tasks grouped under the SNAP 8, ZrH Reactor Technology, General Supporting Technology, and Aerospace Safety Programs. These four programs in total comprise the work leading toward the development, test, availability, and application of the zirconium hydride reactor to space power.

The total SNAP Program objectives include:

- 1) Providing a zirconium hydride reactor technology base for application with mercury-Rankine power conversion equipment to a space system with mission capability in the early 1970's.
- 2) Providing the technology base and engineering for an uprated zirconium hydride reactor with flexibility for use with thermoelectric, organic-Rankine, mercury-Rankine, or gas-Brayton-cycle power conversion equipment.
- 3) Continually upgrading the reactor-shield performance as a means of achieving improved power system performance.
- 4) Achieving the reactor system performance within acceptable safety considerations.
- 5) Maintaining the nuclear reactor space power system work directed toward long-range national space objectives.

Specific near-term objectives are:

- 1) Continuation of the existing base technology component program supporting the immediate operation of the S8DR reactor.
- 2) Completing and testing of the S8DR reactor, with operation at 600 kw and 1300°F outlet temperature and 1 Mwt and 1100°F outlet temperature.
- 3) Engineering of an uprated zirconium hydride reactor with a 600 kw, 20,000 hr, 1300°F outlet temperature capability. This reactor will have a 4π shielding capability, utilize an integral primary loop, and

therefore, be compatible with all candidate primary power conversion systems.

- 4) Performance of tests on components including fuel, 4π reflectors, burnable poisons, pumps, bearings, and shields to demonstrate the technical base for the up-rated zirconium hydride reactor.
- 5) Continuation of work with user agencies to incorporate the more significant mission-imposed constraints on the reactor program and to disseminate current knowledge of the program status to the using groups.
- 6) Performance of the required safety analyses and development testing to incorporate appropriate safety requirements into the up-rated reactor.

B. PROGRAM SUMMARY

1. Fuel Development

Endurance testing on the twelve S8DR preproduction fuel elements is continuing, with total time at temperature 3800 hr during this quarter. The "special seven" fuel elements have completed their endurance testing, and post-test examination was underway. This examination has revealed distortions in fuel alloy which have been attributed to the ramp heating cycles after loss of hydrogen. Performance testing of the normal S8DR fuel elements has been started. Forty-two of the 56 fuel elements to be tested in this program have been received and are being placed on test.

The NAA-121-1 experiment has been inserted into the Hanford reactor and was brought to power early in this report period. After stable reactor operation was achieved, all capsules were brought to design cladding temperature levels and profiles.

The preliminary trade study to determine the number, size, and composition of the specimens for the NAA-140-1 experiment has been completed. Final assembly and design work on the NAA-83 experiment has been deferred to next fiscal year.

Eleven 15-mil-clad elements were assembled to compare, directly, the performance of standard elements with those protected against the fuel-coating reaction. Four of these are S8DR-type elements, with the remaining seven having various stainless steel or nickel foils containing gadolinium oxide. Initial leakrates for these elements have been obtained before and after thermal cycling. The previously fabricated double-clad elements have 8760 hr of endurance testing at 1400°F.

Fabrication of the first double-butt-welded fuel elements, using Incoloy 800 cladding, was successfully completed. Five elements have been assembled and are progressing through the preproduction test sequence. In addition, membranes using the butt-weld closure technique were made and tested.

Studies are continuing to define the phase boundaries in the SNAP fuel system. Work this quarter has concentrated on the $\alpha + \delta (\gamma') \rightarrow \delta$ portion of the phase diagram.

Studies are underway to determine appropriate techniques for the addition of burnable poison to the fuel elements. Consideration is being given to vapor deposition of Gd_2O_3 directly on the fuel and to its use in Ni sleeves. The NAA-120-4 irradiation experiment continued to operate satisfactorily during this report period. An unusual number of unscheduled outages has occurred during this period, and leaks have developed in the argon control lines for two of the capsules. These leaks have been remedied and have caused no further problem.

The investigation to determine the environmental effects of NaK and/or hydrogen on mechanical properties of the S8DR fuel element cladding has been completed. Data from the tests have not identified any environmental effects caused by the presence of NaK at the test temperature of 1400°F, and it has been shown that the presence of H_2 did not significantly affect the stress-rupture properties of the cladding.

2. Component Development

The design verification tests of three S8DR actuators of the modified design are underway, with two units completing the initial 55 thermal cycles test to 1125°F and the third unit in the final phase of thermal-cycle testing. Following

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the initial thermal-cycle tests, these units will start the first 1000-hr phase of 12,000-hr endurance testing.

The evaluation of alternate materials for the S8DR bearing to overcome aluminum-oxide coating failures is essentially completed. Test data confirm that Al_2O_3 coating failure is traceable to the lithium-fluoride impregnant in the P5N carbon graphite. The S8DR bearing was redesigned to utilize Al_2O_3 vs plain carbon graphite (P5) as bearing-surface couple.

The design verification tests on the modified-design S8DR position sensors, limit switch, and scram mechanism are progressing on schedule, with all performance well within the S8DR operational requirements.

High-temperature testing on coil conductors indicates that the upper useful temperature of the stainless-steel-clad copper wire is below 1300°F. Nickel-clad silver and beryllium-oxide dispersion-hardened Cu appear to perform satisfactorily at 1300°F.

The advanced-design high-temperature-bearing test at 1500°F and 10^{-8} torr has been started.

The examination of 1020 mild steel, 446 stainless, and Croloy-2-1/4 alloy capsule material after 10,000-hr test in contact with lead at 1000 to 1500°F indicates that Croloy-2-1/4 is recommended up to 1300°F. Development efforts are in progress to determine the range of densities of LiH-ZrH aggregate material for combined neutron-gamma shield, and to evaluate possible hydrogen migration within this material.

Tests of the triple-pass dc conduction pump indicated pump efficiency as high as 30%. The results also show that the performance of this pump can be calculated within acceptable tolerance. A preliminary design of a pump system (30 gpm, $\Delta p = 2.3$ psi) with a lead-telluride Westinghouse-type TE converter and triple-pass pump was completed.

3. SNAP 8 Development Reactor (S8DR)

The work leading toward the S8DR test proceeded on schedule, highlighted by completion of over half the fuel elements, initiation of reflector assembly, and completion of facility preparations for the non-nuclear test. Analyses of the S8DR performance and environment were refined in several important areas.

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Thirty-four percent of the required 215 enriched fuel elements for S8DR and 79% of the fifty-six elements for performance testing were completed, and the first batches were delivered to the Santa Susana Facility. Another 30% of the elements were completed and in acceptance testing.

The effort to develop and deliver the reactor-vessel head with the incore thermocouples was completed.

Several minor changes were made on the reflector-assembly design as a result of experience obtained on the half-reflector design-verification test. The new P5 graphite bearings were designed and fabricated, and the S8DR reflector assembly was completed to the point of installing the wiring harness.

The S8DR test facility was essentially ready for start of non-nuclear testing at the end of the report period. The modifications to larger NaK piping were completed, the NaK systems were insulated, and all facility modifications associated with the new cooling water tower were completed. The core vessel and piping were fitted into the vacuum chamber in preparation for the final welding of the system. All hardware and procedures for the non-nuclear facility check-out were available, and training of the operators for this phase of the test was accomplished.

4. Reactor Studies

Reference design studies for the 4 π -shieldable reactor were initiated. The major objective of these studies is to provide a reactor system preliminary design based on S8DR technology which offers the greatest flexibility of application from the standpoint of mission, power level, thermal conditions, and lifetime. A second objective is to integrate the reactor system with a nominal 25-kwe thermoelectric power-conversion system to obtain maximum performance in terms of reliability, lifetime, and power-to-weight ratio.

Studies were initiated on the 4 π -shieldable 295-element core to determine the fuel operating lifetime capabilities, excess reactivity requirements, reactivity lifetime capabilities, burnable poison selection, control drum materials selection, and the reflector temperature characteristics.

Thermoelectric module performance data were obtained from Westinghouse, and parametric studies were performed to determine the best design point.

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Major variables of the analysis were number of modules, size, operating temperatures, efficiency, converter pressure drop, and radiator area requirements.

5. Aerospace Safety

The preparation of the Safety Analysis Report for the operation of the SNAPTRAN reactor at the SNAP Engineering Test Facility (SETF) was completed. The report has been reviewed and approved by the internal safety review committee, and sent to the AEC for their review and approval. Test plans were completed and test procedures were under review. Work was started on the installation of the SNAPTRAN machine in the SETF.

Nickel foils containing three concentrations of Gd_2O_3 were received and tested. All tests were satisfactorily completed, and the preliminary specification for the foils was prepared.

Analysis of the effect of Gd additions to SNAP reactors continued with an analysis and comparison to past natural-Gd critical experiments.

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II. FUEL DEVELOPMENT

A. INTRODUCTION AND SUMMARY

The primary objective of this program is to develop fuel elements to meet the requirements for the 600-kwt SNAP 8 reactor listed in Table II-1.

TABLE II-1
PERFORMANCE GOALS FOR SNAP 8 REACTOR FUEL ELEMENTS

Lifetime	12,000 hr
Peak power density (at peak central temperature of 1500°F and peak fuel burnup of 0.3 met. at.%)	5.0 kw/ft of fuel rod length
Hydrogen permeation	5% of H ₂ from fuel alloy
Checkouts required of hydrogen-barrier coating and closure seal	Thermal, mechanical, startup transients
Peak cladding temperature	1400°F
Reliability at 50% confidence level	99%

In addition to these requirements, the fuel element cladding must resist corrosion in the NaK environment and maintain dimensional integrity over the life of the core.

The additional objective of the fuel development program is directed toward the upgrading of the present ceramic-barrier metal-clad fuel element design. Improved barriers, closures, and clad materials are under study to improve fuel element performance. The details of the phase diagram for the fuel material are also under study. Irradiation experiments to further explore the behavior of the fuel and cladding materials and confirm fuel element improvements are part of this program.

The investigation of the $\alpha + \delta(+\gamma')/\delta$ phase boundary was completed. Data indicate this boundary extends to about 1.60 H/Zr at room temperature.

The NAA 121-1 irradiation experiment is operating. The temperature profiles at design power are slightly skewed but operation has been good.

The out-of-pile performance testing of normal S8DR fuel elements was initiated. Forty-two of 56 fuel elements were received for the program.

The first Incoloy-800 double-butt-welded fuel elements were assembled. Performance testing of these elements and some membranes was begun.

B. FUEL ELEMENT MATERIALS EVALUATION

1. Cladding and Barrier

a. Hastelloy-N Properties

The investigation to determine the environmental effects of NaK and/or hydrogen on the mechanical properties of the S8DR fuel-element cladding has been completed. A large scatter band of results was found at the 1400°F test temperature. As reported previously,^{(1)*} if an environmental effect exists, it is masked by the large amount of scatter. Rupture life in NaK ranged up to over 1300 hr at a hoop stress of 10,800 psi. Strain-to-failure values in NaK ranged between less than 1-1/2 and more than 3%. Average creep rates in NaK ranged between less than 0.6×10^{-3} and almost 5×10^{-3} %/hr. These values are similar to data (Table II-2) obtained from control tests conducted in helium and vacuum.[†] The presence of hydrogen did not significantly affect the stress rupture properties of the cladding in either chromized or chromized and diffusion-treated condition.

An additional twelve stress rupture samples are now being placed on test, to determine the effect of long-term thermal aging on the creep rupture properties. Four samples are Hastelloy-N from the S8ER Heat Y837, which were vendor chromized, SCB-1 coated, and aged for 20,000 hr at 1400°F as permeation membranes. Eight samples are of AI-D-coated 304 stainless steel which have been aged for 10,000 hr at 1400°F as permeation membranes.

b. Hastelloy-N-Clad Hydrogen-Barrier Evaluation

The objective of this development is to obtain sufficient information on thermal stability of S8DR-processed and coated Hastelloy-N to describe its time-dependent behavior. Major emphasis during this report period has been

*Superscript numbers in parentheses are for References at the back of this report.

†The data cannot be compared directly, since many of the samples tested on NaK failed at welds rather than in the parent metal.

TABLE II-2
STRESS RUPTURE CONTROL SAMPLES
(Hastelloy-N S8DR Fuel Element
Heat 281-4-0143 Chromized,
Diffusion-Treated, and
Coated)

Sample No.	End Configuration	Environment		Hoop Stress (psi)	Rupture Life (hr)	Diametral Strain to Failure (%)	Diametral Creep rate (%/hr)	Rupture Location
		Internal	External					
7221	C	H ₂	Vacuum	10,800	1,604	9.05	5.64×10^{-3}	Blind End Weld
7221	C	H ₂	Vacuum	10,800	1,604	6.79	4.23×10^{-3}	Blind End Weld
5033	D	H ₂	Vacuum	10,800	2,590	8.78	3.39×10^{-3}	Closure End
5069	D	H ₂	Vacuum	10,800	(2,592)	8.40	3.24×10^{-3}	Did Not Fail
7223	C	He	Vacuum	10,800	1,464	6.77	4.62×10^{-3}	Blind End Weld
5072	D	He	Vacuum	10,800	2,144	3.81	1.77×10^{-3}	Closure End

in the area of fabricating and measuring the performance of fuel elements constructed to limit or reduce fuel-barrier reactions. Specimens for accelerated chrome-diffusion studies were placed on test and specimens from aged elements were submitted for x-ray and microprobe examination. These tests have not progressed to a point during this report period to provide meaningful data.

SCB-Coating Hastelloy-N Performance. Eleven 15-mil clad elements were assembled to compare directly the performance of standard elements with those protected against the fuel-coating reaction. Four were of the S8DR type (with the exception of 15-mil thick wall tubing). Three elements contained fuel rods which had been completely clad in 4-mil 304 stainless steel. Two contained rods protected by 4-mil 304 stainless sleeves open at both ends. These two were assembled to determine if simple sleeves would inhibit the fuel-coating reaction the same as complete encapsulation of the fuel. The remaining two elements contained fuel rods protected by sleeves of 2.4-mil Ni foil containing approximately 6.7 mg of Gd₂O₃/in.². These serve the dual purpose of supplementing the other two sleeved rods, and provide preliminary compatibility data for the burnable poison studies.

The initial leakrates of these elements have been obtained and are presented in Table II-3, along with rates obtained after five cycles of thermal input "A".⁽²⁾

TABLE II-3
LEAKRATES, HASTELLOY-N 15-mil-CLAD ELEMENTS

Element No.	Leakrate (cc/hr at 1400°F, 2 atm H ₂)		Type of Element
	Initial*	Post-Input "A"	
8000	0.36	0.30	Standard S8DR type
8001	0.19	0.67	Standard S8DR type
8002	0.40	0.32	Standard S8DR type
8005	0.35	0.30	Standard S8DR type
8006	0.17	0.18	304 SS sleeve
8008	0.42	0.43	304 SS sleeve
8011	0.21	0.18	Ni/Gd foil sleeve
8012	decanned	0.43	Ni/Gd foil sleeve
8013	0.21	0.15	Double clad, 304 SS
8016	0.38	0.12	Double clad, 304 SS
8021	0.32	0.31	Double clad, 304 SS

*100-hour test

No real difference is seen among the four types of elements at this stage of testing. Ten of the elements are now being subjected to the preproduction testing sequence used for the S8DR fuel elements, for direct performance comparison.

The four double-clad elements fabricated previously to evaluate this effect continued on the 1400°F thermal endurance test.⁽¹⁾ An inner cladding of 304 stainless steel physically separates the fuel from the glass hydrogen-barrier coating. This configuration eliminates any reaction between the fuel and the coating. The elements have completed 8,760 hr of endurance testing at 1400°F isothermal. Figure II-1 shows the percent hydrogen lost by the four double-clad elements. For comparison the average percent loss from the "Special 7" elements (early developmental S8DR type) is also shown. At 8,500 hr the average percent hydrogen loss from the double-clad elements was almost a factor of two lower than the Special 7.

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Figure II-1. Hydrogen Losses for Four Double-Clad Elements and Average Loss of Seven Special Elements

Figure II-2 shows the leakrates normalized to 2 atm H_2 for the double-clad elements. Elements 4971 and 4975 exhibit no overall increase in permeability with time, while a slight general increase is noted in the other two. This slight increase is probably due to mechanical damage to the coating caused by the interrupt testing (thermal cycles every 1000 hr) or possibly due to some coating devitrification with time at temperature.

c. Alternate Cladding

Fabrication of the first double-butt-welded (i.e., two blind ends) fuel elements with Incoloy 300 cladding was successfully completed. Five elements were assembled and are now being processed through the S8DR preproduction test sequence. The leakrate data obtained thus far are given in Table II-4. Though there is a fair range in the leakrates, the important fact is that the ability to achieve the lower end of the current production leakrate range has been demonstrated.

In parallel with the fuel-element fabrication three membranes incorporating the butt-weld closure were made and tested. The membranes were

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Figure II-2. Double-Clad Endurance Test Permeation Rates
(normalized to 2 atm)

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TABLE II-4
LEAKRATES, INCOLOY 800 DOUBLE-
BUTT-WELDED ELEMENTS

Element No.	Leakrate* (cc/hr @ 1400° F, 2 atm H ₂)
800-13-2	0.12
800-8-6	0.75
800-14-17	0.77
800-15-5	0.89
800-5-1	1.70

*24-hour test

made by welding coated blind-end caps to coated tubes. The membranes were initially tested at 1400°F and 2 atm H₂. Scanning-type tests were then conducted to determine the permeation rates as a function of distance along the length of the membranes. The results of the scan tests indicate that the ceramic has fused over the welds, since the end-cap areas leaked at essentially the same rate per unit area as the tubes proper. These data are given in Table II-5.

Two of the membranes were then loaded with fuel rods and retested. Membrane 800-9 exhibited an increase in permeation rate typical of that caused by the fuel-coating reaction. Membrane 800-2 exhibited a step increase in permeability, shown in Figure II-3, indicative of mechanical damage due to handling or thermal stresses. Scan tests were performed at the start and the end of an approximate 300-hr test period. These data are also given in Table II-5. While Membrane 800-2 exhibited no preferential degradation of the butt-weld area during testing, 800-9 did exhibit some though not to a gross extent.

Vacuum-melted bar stock and tubing to continue the Incoloy-800 development program was received. End caps and cup plugs are now being machined. The tubing is being processed through Quality Assurance.

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TABLE II-5
MEMBRANE-PERMEATION RATES, INCOLOY 800
BUTT-WELD CLOSURE
(ϕ , cc/hr-cm² x 10⁻³ @ 1400°F, 2 atm H₂)

Membrane No.	Without Fuel		With Fuel			
			Initial		Final	
	Closure End	Membrane Proper	Closure End	Membrane Proper	Closure End	Membrane Proper
800-2	2.1	1.5	6.6	8.5	3.9	8.5
800-9	2.6	2.6	11.6	5.3	19.0	5.5
800-17	4.2	4.6	-	-	-	-

Figure II-3. Hydrogen Permeation vs Time,
Incoloy 800 Butt-Weld End-
Closure Membranes
(1400°F, 2 atm H₂)

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2. Fuela. Phase Studies

An investigation has been conducted to define phase boundaries in the SNAP fuel system (10 wt % U - ZrH_x). The results of δ/ϵ phase-region studies have been reported in previous quarterlies.^(1,2) Work this quarter was concentrated on the $\alpha + \delta(\gamma') \rightarrow \delta$ portion of the phase diagram. Metallography (including hot-stage techniques), dilatometry, and resistance methods were used. Information obtained is described below and summarized in the phase diagram in Figure II-4.

Room-Temperature Metallography. A metallographic examination was performed on nine hydride compositions from 1.26 to 1.60 H/Zr atom ratios; the phases identified at each composition are as follows:

Hydrogen Composition (H/Zr atom ratio)	Phases Identified		
	δ	γ'	α (as $\alpha + \delta$ eutectoid)
1.26	X	X	X
1.30	X	X	X
1.41	X	X	
1.45	X	X	
1.50, 1.51	X	X	
1.55	X	X	
1.58	X	X	
1.60	X	X	

This examination indicates that α phase in $\alpha + \delta$ eutectoid is present only in those compositions cooled through the eutectoid isotherm. Microstructures of hydrides with hydrogen compositions exceeding the eutectoid composition limit, 1.35 H/Zr, contain δ and γ' , exclusively. These observations are consistent with the X-ray diffraction analysis performed by Beck⁽³⁾ on a similar series of compositions. Figure II-5 is a photomicrograph of the 1.60 H/Zr material. Gamma (γ') needles can be seen within the grains and at the grain boundaries of δ phase. Because of the small amount of γ' , this composition is necessarily close to the upper limit of the composition of the $\alpha + \delta(\gamma')$ phase region at room temperature.

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Figure II-4. Segment of Zirconium-Hydrogen Phase Diagram

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Figure II-5. Photomicrograph
of $\text{ZrH}_{1.60}$ - 10 wt% U

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250X

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Hot-Stage Metallography. A time-lapse 16-mm movie camera was employed to record the $\gamma' \rightarrow \alpha$ and $\alpha \rightarrow \delta$ phase transitions in 1.41 and 1.45 H/Zr fuel. A 1.51 H/Zr sample was also examined. The table below summarizes the data obtained. (Reference 1 has photographic stills of the transitions obtained from 1.30, 1.45, and 1.50 H/Zr fuel.)

Hydrogen Compositions H/Zr Atom Ratio	Temperature of Phase Transition (°F)		
	$\gamma' \rightarrow \alpha$	$\alpha \rightarrow \delta$	$\gamma' \rightarrow \delta$
1.41	550	875 - 900	510
1.45	560	800	
1.51			

Dilatometry and Resistance Measurements. The experimental procedures used in dilatometric and electrical resistance examinations have been described in earlier reports.^(4, 5) Tests to date have been limited to a single composition, 1.41 H/Zr. Results obtained are given in Table II-6.

TABLE II-6
DILATOMETRY AND RESISTANCE MEASUREMENTS

Method	Run No.	Transition Temperature (°F)		Heating (↑) or Cooling (↓)	Rate (°F/min)
Dilatometry	1	560	895	↑	0.9
	2a	560		↑	0.9
	2b	540		↑	0.75
	3	560		↑	6.0
Resistance	1	550		↑	Thermal equilibrium. Established before recording data.
	2	590		↓	

The results in Table II-6 suggest that the $\gamma' \rightarrow \delta$ transition is reversible. Some hysteresis in temperature is apparent but this might also be attributed to thermal lag between the sample and the thermocouple.

b. Burnable Poison Studies

The addition of a burnable poison to the fuel elements can reduce the long-term reactivity swing of the reactor and reduce the active control required. The poison provides control by burning out at a rate that compensates for reactivity losses. Studies are being made to determine the feasibility of alternate methods of incorporating the poison into the fuel elements. Application of the poison to the surface of the fuel offers two advantages over inclusion in the cladding or ceramic hydrogen barrier or within an inner metallic sleeve:

- 1) The thickness of the poison can be varied over the length of the fuel element in a controlled manner to yield a poison-concentration profile which is similar to the power profile, thus providing power-shaping.

- 2) The possible reduction in the fuel-barrier reaction could result in lower hydrogen loss rates and increase the reactor lifetime.

The objective of this program is to develop a process technique for the application of from 0.05 to 0.5 gm of gadolinium to each fuel element; the technique should permit a controlled variation of poison content over the length of the element.

The compounds and alloys of gadolinium and the state-of-the-art of all known application methods have been reviewed. The most promising method of applying the poison to fuel is the vapor deposition of gadolinium sesquioxide, Gd_2O_3 , which is vaporized by electron-beam heating. Other methods require heating of the fuel to temperatures at which significant hydrogen losses will occur or are not yet applicable for the thin films (0.0002-in. maximum thickness) required. Other materials are not suitable for one or more of four reasons: (1) a controlled distribution is extremely difficult to attain; (2) the anion of the compound adversely affects the fuel or the cladding; (3) the coefficient of expansion yields a high probability of spallation of the coating; and (4) the anion reduces the effectiveness of the Gd as a poison.

Based on free energies of formation, Gd_2O_3 is thermodynamically more stable than ZrO_2 or any of the components of the ceramic barrier. Hence it should not react with fuel or with the barrier and would be expected to reduce the fuel-barrier reaction by effectively separating the two. The coefficient of thermal expansion of Gd_2O_3 , $5.6 \times 10^{-6}/^{\circ}F$, is nearly equal to that of SNAP fuel so that spallation is unlikely.

Vapor deposition of Gd_2O_3 yields a predictable poison distribution, and normally produces a coating thicker at the center than at the ends of the fuel rod. The distribution can be varied by changing the distance between the fuel and the Gd_2O_3 source, by changing the angle between the longitudinal axis of the fuel rod and the direction of the electron beam, and by the addition of suitable screening. Although it is necessary to heat the fuel to $\sim 550^{\circ}F$ to obtain good adherence of the coating, a maximum loss of only 0.5% of the total hydrogen would be expected during the time required for coating (~ 40 min).

The use of metallic foils containing Gd_2O_3 is being investigated in connection with experiments under the Aerospace Nuclear Safety program and is

described in Section VI of this report. Although this method yields excellent control of the poison concentration, a variable distribution can be obtained only by varying the thickness of foil or by the use of several segments of foils containing different concentrations of poison. These methods would yield a step-wise rather than continuous distribution of poison; however, it has not been established whether or not this would be detrimental to performance.

C. IRRADIATION TESTS

1. NAA-120-4

The NAA-120-4 irradiation experiment is statistically designed to evaluate SNAP fuel performance as a function of temperature and fuel composition at 0.9 met. at.% burnup. The experiment operated satisfactorily during the report period although reactor operational problems caused an unusual number of unscheduled outages. During the first part of the period a leak developed in the argon gas control exit lines in capsule V-1 and/or V-2. The leak was remedied and caused no further problems. At certain times during the experiment it has become impossible to maintain the 1600°F capsule at design operating conditions, and for this reason a krypton gas control system has been installed to increase the range of temperature control. The system has thus far not been used because of reactor operational problems. Seven constant voltage signals were connected to the data logger on spare channels so that if any problems occur in the analog-to-digital-converter they can be noted and corrected immediately.

Heat-generation rates in the experiment have been running typically from 1.40 Q in Capsule 1 to 1.05 Q in Capsule 5, and vary about linearly along the length of the experiment. During a startup considerable data were obtained to describe the fuel-temperature vs heat-generation rate relationships. Figure II-6 shows a typical response of a specimen during a startup. The fuel-temperature curves vary for the different capsules and even differ among the three 1500°F capsules. An attempt will be made to correlate this data with the accommodation coefficient theory. It was noted during this report period that there is an apparent recovery of clad-to-fuel temperature drop which occurs each time the reactor is down or the experiment is operated at pure helium temperatures for an appreciable length of time. This phenomenon is schematically represented by Figure II-7 and could be due to reactor power changes, hydrogen redistribution, and/or both of these in conjunction with other as yet unidentified effects.

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Figure II-6. Typical Fuel Temperature vs Heat-Generation Rate

Figure II-7. ΔT Fluctuations

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2. NAA-121-1

The objective of this experiment is to determine the inpile performance of SNAP 8 fuel elements. The test is designed to realistically simulate the center fuel element in the S8DR reactor core at 600, 660, and 720 kwt total reactor power. The four NAA-121-1 fuel elements are essentially identical to current S8DR production elements with the following exceptions:

- a) The Numbers 1, 2, and 3 Capsule elements have radial hydrogen gaps ~0.5 mils less than current S8DR elements;
- b) The Number 4 Capsule fuel rod has a $5.61 N_H$ compared to the nominal S8DR N_H of 6.0;
- c) The current chem-milling process was not used on NAA-121-1 fuel rods; and
- d) The blind-end cap and cup-plug geometries are flat-ended rather than hemispherical and dished as in the S8DR.

The experiment was inserted into the K-East Hanford production reactor and brought to power early in this report period. During initial operations on pure helium control gas it was determined that the experiment was operating 30% below design power due to the existing reactor loading. All instruments were checked and found to be operating satisfactorily with the exception of the Number 1 Capsule ADIT flux monitor which exhibited a severely degraded output.

After stable reactor operations had been achieved and satisfactory experiment performance verified, all capsules were brought to design cladding temperature level and the profiles compared to the NAA-121 design predictions. Upon completion of the design temperature checks, the Number 4 Capsule was lowered back to pure helium operating conditions, at which temperatures it will be maintained until near the end of the irradiation. The other three capsules are being positively controlled to maintain the cladding temperature ($Z/L = 0.9$) at $1300 \pm 20^\circ\text{F}$.

Since initial operations the experiment power has been raised to 100% by a change in the reactor loading. Figures II-8, -9, and -10 show the current fuel and cladding temperatures compared to the latest S8DR reference design

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Figure II-8. Capsule 1 Design Temperature Profiles

Figure II-9. Capsule 12 Design Temperature Profiles

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Figure II-10. Capsule 3 Design Temperature Profiles

conditions. The end peaking and corresponding midsection temperature droop exhibited by the capsules is caused by a design miscalculation which resulted in a uniform 1-mil undersizing of the control-gas gap. Due to the control-gap shaping (20-mil ends and 10-mil center) the midsection temperatures are 50°F lower than the end temperatures in the original design.

The operation of Number 4 Capsule at design power is shown in Figure II-11, as estimated from the initial design-temperature check at 70% power. Also shown in this figure are the initial fuel centerline and cladding profiles at pure helium conditions.

Since the initial startup, the operating condition and performance of the experiment have remained relatively unchanged with the exception of the Number 4 Capsule. The cladding profile of this capsule has exhibited unusual fluctuations as shown in Figure II-12. This phenomenon is being investigated.

The data reduction codes for processing the NAA 121-1 digital data tapes have been completed and routine processing has been initiated. To facilitate the evaluation of this data a continuous calibration system has been installed in the data logger which produces a 6-point logger calibration for each recorder scan.

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Figure II-11. Capsule 4 Design Temperature Profiles

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Figure II-12. Capsule 4 Cladding Profiles

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The NAA-121-1 fuel elements have diametral gaps of 6 mils. The low-temperature helium operation after hydrogen redistribution is computed to put the highest temperature region of the fuel in or near the δ/ϵ phase boundary. Similar operation in the S8ER fuel — NAA-115-1, NAA-115-2, and NAA-117-1 — followed by operation in the 1400°F range resulted in up to 6- or 7-mil growth on similar-sized fuel rods. Therefore it is expected that some interference with the cladding may occur in the NAA-121-1 elements because of their smaller than optimum (S8DR) gap. Previous irradiation-experiment capsules with interference in this range did not suffer rupture. Irradiated Hastelloy-N data would also indicate that the non-"hot-line" cladding temperature profile of the NAA-121-1 operation should readily accommodate such low interference without rupture. Therefore the possible interference is not expected to jeopardize the operation of the experiment, or invalidate postoperational data.

3. NAA-140-1

The primary objective of the NAA-140-1 experiment is to obtain data on parameters that are of interest for "long-life" fuel elements. This experiment will be designed to operate for 20,000 hr at design power and operating temperatures. The major task accomplished this quarter was the completion of a preliminary trade study to determine the number, size, and composition of the specimens. The experiment length was limited to about 240 in., the approximate length of usable flux in the test site. It was determined that the diameter of the test site limited the number of subcapsules to a maximum of six. From previous experience the close tolerances required on gas-control annuli restrict the length of capsules to about 32.0 in.; the fueled section should not exceed 30. Within this envelope it was recommended that each capsule contain a full-length fuel element or three 8-in.-long specimens. The results of this study are being reviewed and will form the necessary input for the experiment specification. In addition to the number, size, and composition of the specimens, items included in the trade study were: type of cladding material, radial fuel-cladding gap size, type of end closure, burnable poison and method of application, end-of-life mechanism, individual specimen temperature distributions, and candidate specimens for pressure monitoring. The configuration currently being considered for preliminary design effort contains both full and sublength elements.

4. NAA-83

Final assembly and designwork on NAA-83 was deferred to GFY 1969. Purchased components are still on order for Capsules 1 through 4; otherwise, all activities have been deferred.

D. FUEL-ELEMENT TESTING

1. Performance Tests

Extended 1400°F endurance tests are continuing on 12 S8DR preproduction fuel elements. These 12 were selected from the 20 subjected to the preproduction test sequence, the results of which were reported in a previous progress report.⁽²⁾ To date, the 12 elements have logged over 3800 hr at temperature, with 3000 accrued in these extended tests. A downward trend in hydrogen NaK rate is indicated by the data from these extended tests with the average normalized leakrate (1400°F and 2 atm) going from 0.40 to 0.35 cc(STP)/hr during the 3000 hours. This average rate at 3000 hr is about the same as the average rate of the initial acceptance test in the preproduction sequence. Figure II-13 shows graphically the average normalized permeation rate for the 12 elements as a function of time.

2. "Special 7" Fuel Elements

The post-test examination of the "Special 7" fuel elements was begun. Three (4285, 4261, and 4308) exhibited visual evidence of ridges near the blind ends. These were in the form of smooth circumferential ridges approximately 1 in. from the blind ends. Upon decanning, the ridges were found to be due to localized growth of the fuel.

Examination of the fuel rods disclosed that five of the seven exhibited growth at points approximately 1 in. from the blind ends (see Figure II-14). Table II-7 gives the calculated hydrogen loss, the end-of-life H/Zr, and the visual condition of the rods. Longitudinal metallographic sections of the blind-end areas of the rods were examined. The "as-polished" surfaces of these specimens revealed symmetrical patterns of voids. Figure II-15 shows examples of this condition. (The distribution of fine white spots are voids; the reversal in image contrast resulted from the bright as-polished condition of the surface.) This void effect was absent in Fuel Rods 352-AI (Element 4122)

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Figure II-13. Average Leakrate (Normalized) for 10 Preproduction S8DR Fuel Elements in Extended 1400°F Endurance Tests

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Figure II-14. "Special 7" Fuel Rods, Distortion Near Blind End
(taken at about 2X)

TABLE II-7
POST-TEST CONDITION, "SPECIAL 7" FUEL RODS

Element	Rod No.	% Loss Hydrogen*	Final H/Zr†	Condition of Rod
4122	352-A1	8.2	1.60	Good
4131	374-A2	8.2	1.59	Good
4257	374-A1	10.6	1.56	End broken by decanning
4261	374-C3	10.1	1.55	Swelling near blind end
4308	374-A4	10.7	1.55	Swelling near blind end; also cracked
4302	374-C2	10.8	1.55	Swelling near blind end
4285	357-A4	17.2	1.49	Swelling near blind end

*Based upon the integrated leakrate

†Determined by isochore measurements

Figure II-15. As-Polished Longitudinal Sections, Showing Symmetrical Void Patterns Typical of the Distorted Fuel Rods

and 374-A2 (4131). The photomicrograph in Figure II-16 was taken at an area near the end of 357-A4 (4285), which represents the largest voids found in any of the distorted areas of the rods. The shape of the void patterns suggests that they were formed during thermal-transient conditions, probably resulting from the ramp-heating phases of testing. With one exception the final distribution of hydrogen (based upon phase identification) within the seven specimens was found to be uniform, as would be expected after the final testing under isothermal conditions. The only exception, 357-A4 (4285), was about 1.35 H/Zr as evidenced by the presence of isolated areas of $\alpha + \delta$ eutectoid and γ' phases (Figure II-16), while the final isochore value of the major portion of the rod showed an H/Zr of 1.49. Predicted hydrogen compositions based upon the metallurgical examinations agreed with the final isochore H/Zr values in all other cases.

Figure II-16. Largest Voids
Found: Rod 357-A4 (4285)

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(100X)

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Despite the final uniform distribution of hydrogen in the fuel, microstructural changes indicate the probable cause of the distortion. Figure II-17 shows photomicrographs taken at an interface between a structure typical of as-hydrided material and a material containing small equiaxed grains. The directionality of the normal structure is the result of nucleation of δ grains in a β -phase matrix at the surface and growing toward the center of the rod during the hydriding process. The formation of small equiaxed δ grains in the distorted ends of the fuel probably occurred during rehydridding of β -phase material during 1400°F isothermal testing. At this temperature, diffusion of hydrogen from δ phase (≥ 1.45 H/Zr) to β phase (≤ 0.9 H/Zr) would occur predominantly by bulk diffusion since the driving force for hydridding via the gas annulus in the element would be about 0.1 atm. The final position of the suspected prior β/δ interface did not appear to be associated with the void formations. This further suggests that the voids had formed during the ramp heating cycles and that the β/δ phase separation was not necessarily sufficient to cause the void formation.

Therefore, the following is proposed as the cause for the distorted fuel:

- 1) The absence of distortion in Rods 352-A1 (Element 4122) and 374-A2 (4131) indicates that hydrogen loss must exceed 8.2% before distortion can occur; therefore the voids formed late in the testing program.
- 2) When the hydrogen loss exceeded a point between 8.2 and 10.1%, ramp heating to about 1450°F caused the blind end, under probable temperature gradient conditions, to transform to β phase.
- 3) Because of the lower specific volume ($1/\rho$) of β phase (versus δ), together with the temperature gradients in the fuel, stresses at the moving $\beta + \delta$ interface were sufficient to cause void formation and subsequent localized swelling.

These voids are not of real importance to the consideration of core operation since β phase (about 5% hydrogen loss) is the design limit for hydrogen loss. However, out-of-pile endurance tests are performed at isothermal temperatures which produce leakrates approximately three times greater than will occur under reactor operating conditions. Total integrated hydrogen loss will be watched closely in out-of-pile performance tests as an aid in identifying elements which may undergo this effect.

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Figure II-17. Suspected Prior $\beta + \delta$ Interfaces

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The cladding tubes from the seven elements were destructively examined. Evidence of the fuel-coating reaction, in the form of the typical iridescent film, was found to varying degrees. The extent of the film ranged from slight to gross flakes. Coating adherence tests of samples from each of the seven tubes produced numbers of less than 1 in all cases, i.e., adherence was practically nil. Microprobe scans of the chromized layers on samples from each of the seven tubes have been taken and the data are now being reduced. Samples for x-ray diffraction analyses of the coating were also prepared.

3. S8DR Performance Tests

The non-nuclear performance testing of S8DR fuel elements was started with the delivery of 22 normal uranium fuel elements, representative of elements assembled at the start of the S8DR core production. The S8DR testing program has been designed to evaluate: (1) consistency in fuel-element fabrication during the production of the core; (2) extended lifetime performance (up to 20,000 hr at temperature); (3) long-term performance at a subnormal test temperature; (4) effects of thermal shock and cycling which interrupt the extended tests; and (5) effects of vibration, shock, and acceleration on the S8DR fuel element.

A total of 56 fuel elements will be involved in the test program. They will be divided into five separate groups and designated as follows:

Number of Elements	Designation	Description
12	Phase I	Representative of start-of-core production-fuel-elements
12	Phase II	Representative of midcore production-fuel-elements
12	Phase III	Representative of end-of-core production-fuel-elements
10	Phase IV	Mechanical test elements
10	Phase V	Thermal test elements

The basic test sequence consists of two cycles of thermal input "H" (shown in Figure II-18) followed by 2000 hr of endurance test at 1400°F. The thermal input is preceded and followed by a standard permeation test as a measure of

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Figure II-18. S8DR Performance Test Thermal Input "H"

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the effect of the thermal input. The thermal and endurance combined input is repeated a total of ten times for an accumulation of 20,000 hr in endurance and 20 thermal input cycles.

For elements in Phases I, II, and III only four of each group will complete the entire sequence, as groups of two elements will be removed from test each 4000 hr. These elements, after removal, will be given selected tests and destructively examined.

The Phase-IV fuel elements will be initially subjected to vibration, shock and acceleration inputs, as listed in Table II-8. Afterward they will go through the same thermal-test sequence as Phases I, II, and III.

The Phase-V fuel elements will go through the same test sequence as Phases I through III except that the test temperature during endurance will be 1300°F instead of 1400.

To date, all fuel elements except those for Phase III have been received. Phase I has progressed into the first 2000-hr endurance test. The data from these elements up to the endurance testing are shown in Table II-9.

Six of the Phase-I elements were selected for zone scan testing before start of the performance tests. These six elements will be among those later removed from the test sequence for comparative scan tests and destructive examination. The scan-test results are listed in Table II-10. Because the Phase -II and -V fuel elements have just been received, no test data are available. Three of the Phase-IV fuel elements have completed the mechanical inputs but no post-test data are yet available.

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TABLE II-8
SHOCK, VIBRATION, AND ACCELERATION TEST INPUTS

a. Shock Test			
Amplitude	Duration	Axis	Number of Shocks
20 g (half sine-wave)	10 milliseconds	+ Longitudinal	3
		- Longitudinal	3
		+ Lateral	3
		- Lateral	3
		+ Normal	3
		- Normal	3
b. Vibration (three mutually perpendicular axes)			
1. Sinusoidal			
Frequency (cps)	Amplitude	Sweep rate	
5 to 16	0.368-in. double amplitude 5g (peak)	1 octave/min	
15 to 2000			
2000 to 16	5g (peak)		
16 to 5	0.368-in. double amplitude		
2. Random			
Frequency (cps)	Spectral Density	Time	
10 to 80	+ 3 db/oct 0.06 g ² /cps constant level - 3 db/oct	5 min. duration	
80 to 400			
c. Acceleration			
Amplitude (g)	Duration (min.)	Axis	
6	5	+ Longitudinal	
3	5	- Longitudinal	
2	5	+ Lateral	
2	5	- Lateral	
2	5	+ Normal	
2	5	- Normal	

TABLE II-9
 PHASE-I S8DR PERFORMANCE-TEST DATA, NORMALIZED
 HYDROGEN PERMEATION (1400°F, 2 atm, cc[STP]/hr)

Element No.	Initial Acceptance	Final Acceptance	Post-Acceptance	Post-Thermal Input "H"
027	0.59	0.41	0.67	0.43
032	0.32	0.28	0.30	0.37
035	0.73	0.70	0.92	0.77
042	0.22	0.31	0.47	0.24
045	0.31	0.41	0.66	0.27
047	0.54	0.18	0.20	0.20
053	0.45	0.47	0.45	0.47
057	0.24	0.31	0.26	0.29
065	0.45	0.44	0.63	0.42
068	0.41	0.43	0.70	0.95
075	0.31	0.44	0.92	0.91
077	0.56	0.60	0.38	0.69
Average	0.43	0.42	0.55	0.50

TABLE II-10
 PHASE-I S8DR FUEL-ELEMENT ZONE-SCAN RESULTS

Element No.	Permeation Rate, L (cc[STP]/hr at 1400°F)						
	Blind End (1-1/4 in.)		Center (14-7/8 in.)		Closure End (1-1/4 in.)		Total Leakage
	L	%	L	%	L	%	%
027	0.16	47.1	0.04	11.7	0.14	41.2	0.34
035	0.14	26.4	0.19	35.8	0.20	37.7	0.53
042	0.06	26.1	0.07	30.4	0.10	43.5	0.23
045	0.07	18.4	0.13	34.2	0.18	47.4	0.38
047	0.04	25.0	0.06	37.5	0.06	37.5	0.16
077	0.06	14.3	0.12	28.6	0.24	57.1	0.42
Averages	0.09	26.2	0.10	29.7	0.15	44.1	0.34

III. COMPONENT DEVELOPMENT

A. INTRODUCTION AND SUMMARY

Design verification tests to demonstrate reliable long-term performance are being conducted on S8DR components. These tests are being made in S8DR test-vacuum environment and at or above S8DR design temperature. Developmental efforts applicable to advanced zirconium-hydride reactors are being concentrated on high-temperature bearings, actuator technology, shielding materials, reflectors for 4 π -shielded reactors and dc conduction pumps.

The design verification test of three S8DR actuators of the modified design are well underway. Two units have completed the initial 55 thermal cycles to 1125°F and are now in the first 1000-hr phase of the 12,000-hr operational endurance at 1125°F. The other unit has completed 42 of the initial 55 thermal cycles.

The evaluation of alternate materials for the S8DR bearing to overcome aluminum oxide coating failures is essentially completed. Test data confirm that Al₂O₃ coating failure is traceable to the lithium-fluoride impregnant in the P5N carbon graphite and also that LiF is not required to obtain favorable friction characteristics. The S8DR bearing was redesigned to utilize Al₂O₃ vs plain carbon graphite (P5) as bearing-surface couple. Titanium-carbide coating vs P5N was selected as the backup design.

The design verification tests on the modified-design S8DR position sensors, limit switch, and scram mechanism are progressing on schedule, with all performance well within the S8DR operational requirements.

High-temperature testing on coil conductors indicates that the upper useful temperature of the stainless-steel-clad copper wire is below 1300°F. Nickel-clad silver and beryllium-oxide dispersion-hardened Cu appear to perform satisfactorily at 1300°F. Testing also indicates that a less brittle iron-based magnetic material can be used in place of high-cobalt alloy at temperatures up to 1100°F at lower flux densities.

The advanced-design high-temperature-bearing test at 1500°F and 10⁻⁸ torr has been started.

The examination of 1020 mild steel, 446 stainless, and Croloy-2-1/4 alloy capsule material after 10,000 hr test in contact with lead at 1000 to 1500°F indicates that Croloy 2-1/4 is recommended up to 1300°F. Development efforts are in progress to determine the range of densities of LiH-ZrH aggregate material for combined neutron-gamma shield, and to evaluate possible hydrogen migration within this material.

Tests of the triple-pass dc conduction pump indicated pump efficiency as high as 30%. The results also show that the performance of this pump can be calculated within acceptable tolerance. A preliminary design of a pump system (30 gpm, $\Delta p = 2.3$ psi) with a lead-telluride Westinghouse-type TE converter and triple-pass pump was completed. The calculated weight of this system for 1-ft separation between the pump and the converter is 44 lb.

B. CONTROL-DRUM ACTUATORS

1. S8DR

The control drums of the SNAP 8 Reactor are rotated in 0.26° increments through a gear driven by variable-reluctance stepping motors. Pulses of direct current provide rotating torque depending on demand signal. An integral brake prevents drum rotation during the period when the stepping motor is not energized.

The 12,000-hr design-verification test on three modified actuators was initiated. These units are identical and fabricated concurrently to the actuators installed on S8DR. The design improvements including brake and phase coils, wound with stainless-steel-clad copper wire encapsulated in AI-1 cement, and the integral lead cables were described previously.⁽⁶⁾ A completed actuator assembly is shown in Figure III-1.

The test schedule consists of an initial 55-thermal-cycle phase (400 to 1125°F) including 27 simulated startups, followed by periods of actuator operation and static dwells at 1125°F. During the actuator operational sequences the actuator is stepped at 1125 and 400°F. The present status of testing is given in Table III-1 while Table III-2 lists test data obtained to date.

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Figure III-1. Modified Design S8DR Actuator

TABLE III-1
STATUS OF ACTUATOR TESTING

Serial No.	Initial 55-Thermal-Cycle Testing	1000-hr Post-Thermal-Cycle Testing
011	Completed, including 209 hr at 1125°F	250 hr +5 thermal cycles
013	Completed, including 207 hr at 1125°F	180 hr +2 thermal cycles
010	42 thermal cycles completed, including 158 hr at 1125°F	Not started

TABLE III-2
ACTUATOR PERFORMANCE DATA AT 1125° F

	Serial No.	Brake Current (amp)		Stepping* Current (amp)		Insulation Resistance (ohms)	
		Pull-In	Release	Clockwise	Counter-Clockwise	Brake	Minimum Phase
Start of test	011	1.55	0.35	2.9	2.9	7M	300k
Latest data		1.52	0.31	3.0	2.9	3.5M	1M
Start of test	013	1.44	0.68	3.3	2.8	7.5M	5M
Latest data		1.48	0.62	2.8	2.7	3M	7M
Start of test	010	1.28	0.34	3.0	3.2	10M	200k
Latest data		1.24	0.36	2.8	3.1	6M	1.1M

*To step against 40 in.-oz load

All units appear to be operating satisfactorily. The insulation resistance is running somewhat higher than in tests on the originally designed units, where resistances as low as 50 k-ohms for uncontaminated units were encountered.

2. Actuator Technology Development

The effort in this program is directed towards material technology for the development of advanced actuators.

a. Coil Reliability

As reported earlier,⁽¹⁾ tests are being made to evaluate potential high-temperature high-reliability conductor materials for actuator coils. These units are being performed on coils wet-wound with AI-1 cement on rectangular-shaped alumina bobbins. Three different tests are being conducted.

The coils under test were wound of three conductor materials, nickel-silver, Cube alloy, and Oxalloy. The Ni-Ag conductor consists of a fine Ag core swaged in a Ni cladding. The composite wire is 80% Ag by cross-sectional area. Cube-alloy conductor is a copper base which has been dispersion-hardened by the addition of fine particles of beryllia. Oxalloy conductor consists of a Cu core swaged in chrome-iron alloy (stainless-steel) cladding. The composite wire is 72% Cu by cross-sectional area.

The first test was intended to produce failure in the conductor material by thermal cycling to 1300°F. The conductors evaluated were Oxalloy, Ni-clad Ag, and Cube alloy. After the completion of 405 thermal cycles to 1300°F and 338 hr at 1300, no catastrophic coil failure was apparent from external electrical measurements of the 12 coils (four of each type) in the test chamber. External resistance measurements indicated that the resistance values of the Ni-Ag coils and the Cube-alloy coils remained constant while those of the Oxalloy coils increased by approximately 56%. The data scatter of the measured data was approximately 10%.

The visual inspection showed: (1) the Ni-clad-Ag coils were unaffected by the thermal-cycle testing; (2) the Cube-alloy coils were stained by what appeared to be Cu migration within the coil; and (3) the Oxalloy coils were badly distorted at their ends by conductor growth. This growth caused the coil to pull free from the bobbin at the coil ends, producing a distorted end and increasing the coil length by approximately 1/2 in. This was an increase of conductor length of approximately 17.5% (see Figures III-2, -3, and -4).

Because the wire volume is constant, an increase of length must decrease the area of the coil. A 25% change in length with resulting area reduction could account for the 56% change in resistance noted in testing. More accurate before-and-after coil wire-length measurements might disclose that the coil conductor did, in fact, increase in length by 25%.

Preliminary microscopic examination of the coils and lead wire indicated that the No. 30 AWG Oxalloy lead-wire strands and No. 24 AWG Oxalloy conductor wire were misshapen and grooved similar to that previously reported.⁽¹⁾ The surface of the Ni-Ag conductor, where it was carried out as the coil lead wire, was pitted and marked with valleys. This wire was very flexible, bending easily to the touch. The surface of the Cube-alloy conductor, used for both coil and lead wire, looked excellent, with a slight diffused appearance as though the surface had been lightly sandblasted. This wire was very stiff to the touch. One lead broke in handling the coil during removal from the test chamber. The break was square across the wire without any appearance of elongation.

Closeup external views of the various wire materials are shown in Figures III-5, -6, and -7.

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Figure III-2. 1300°F Thermal Cycle Test, Side View

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Figure III-3. 1300°F Thermal Cycle Test, Side View Showing Staining

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Figure III-4. 1300°F Thermal Cycle Test, Top View

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Figure III-5. Oxalloy Conductor After
405 Thermal Cycles to 1300°F

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Figure III-6. Nickel-Silver Conductor Leadwire
After 405 Thermal Cycles to 1300°F

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Figure III-7. Cube Alloy Conductor After
405 Thermal Cycles to 1300°F

This 1300°F "failure test" has been converted to a 1400°F steady-temperature test, as thermal cycling does not appear to be an operational mode of failure of the Ni-clad-Ag and the Cube-alloy conductor materials. The 400 thermal cycles are far in excess of the expected system requirements. All Oxalloy coils have been removed from the test chamber as well as one each of the Cube-alloy and Ni-clad-Ag coils which were subjected to preliminary microscopic examination. The latter two types will be examined in detail to determine if any changes have occurred in the material as a result of testing to date.

A second test consisted of thermal cycling coils of Ni-clad-Ag and Cube-alloy conductor to 1200°F. The coils (six of each type) were fabricated exactly as the 1300°F-test specimens. After the completion of 407 thermal cycles to 1200°F and 339 hr at 1200, this test chamber was opened up for visual inspection. This inspection (Figures III-8 and -9) again showed the Ni-clad-Ag coils to be unaffected by the thermal-cycle testing, but the Cube-alloy coils showed a heavy stain of what appeared to be Cu migration from within the coils. One sample of each type of coil was removed from this test, and the test converted

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Figure III-8. 1200°F Thermal Cycle Test

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Figure III-9. 1200°F Thermal Cycle Test Showing Copper Stain

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to a 1200°F steady-temperature test to obtain reliability data on coil endurance at temperature.

Preliminary microscopic examination of the coil conductor showed the Ni-Ag conductor to be bright, with slight markings. This wire was very flexible, bending easily to the touch. The Cube-alloy conductor had a dull surface and was very stiff to the touch. Both conductor materials appeared to be in excellent condition. Magnified views of these conductors are shown in Figures III-10 and -11.

A third test is being performed with coils wound with Oxalloy conductors on rectangular-shaped alumina bobbins mounted in an S8DR actuator housing. The purpose of this test is to obtain long-term (12,000-hr) high-fatigue-loading (500 thermal cycles) testing of Oxalloy wire at 1100°F, which is considered as the most useful temperature for this material. These coils were previously subjected to 200 thermal cycles and 350 hr at 1125°F. In the current test the coils have accumulated an additional 54 thermal cycles and 1404 hr at 1100°F.

b. Magnetic Materials

As previously reported,⁽¹⁾ ring samples of Hiperco-27 and Armco iron were tested at ambient temperature. Armco iron is a pure iron (99.9 +%) while Hiperco 27 contains approximately 27% cobalt. These test results showed good correlation with published magnetic properties.

Testing has since been performed at temperatures of 800, 1000, and 1200°F as the sample temperature was initially being raised to 1200. These tests were repeated after 115, 208, and 610 hr at 1200°F. Based on the data analyzed to date (Figures II-12 through -17), it appears Armco magnetic iron is a promising high-temperature magnetic material, and can be used to replace the somewhat brittle Hiperco-27, at magnetic flux densities of 12 kilogauss or less in temperatures up to 1200°F. Neither material has exhibited any tendency to be adversely affected by time at 1200°F, although Armco magnetic iron shows its decreasing ability to carry flux as the temperature is increased to 1200°F. Hiperco 27 test data show an increase in performance before the 115-hr test point, which was attributed to annealing of the test sample. For temperatures higher than 1200°F and/or flux densities greater than 12 kilogauss, Hiperco-27 will probably be the preferred magnetic material.

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Figure III-10. Cube Alloy Conductor After
407 Thermal Cycles to 1200°F

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Figure III-11. Nickel-Silver Conductor After
407 Thermal Cycles to 1200°F

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Figure III-12. Hiperco-27 Magnetic Iron Test, Ambient Temperature

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Figure III-13. Armco Magnetic Iron Test, Ambient Temperature

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Figure III-14. Ambient Temperature Magnetization

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Figure III-15. Hiperco-27 Magnetic Iron Tested at 1200°F After
610 Hours at 1200°F

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Figure III-16. Armco Magnetic Iron Tested at 1200°F After 610 Hours at 1200°F

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Figure III-17. 1200°F Magnetization
After 610 Hours at 1200°F

C. BEARINGS

1. S8DR

The reference and backup designs of the S8DR control-drum bearings have been selected. The reference design uses a solid bearing ball fabricated from a nonimpregnated carbon graphite (Grade P5) dimensioned to fit with the existing Al_2O_3 -coated Inconel-X shafts and socket assembly. The backup design substitutes a TiC coated shaft for the existing Al_2O_3 shaft. The existing bearing ball with its LiF-impregnated carbon graphite bushings is retained. The selection of these designs was based upon the evaluation of the sliding friction, material compatibility, and bearing tests made on samples with Cr_3C_2 , TiC, WC, chromium, and several variations of Al_2O_3 coatings. The selection of these coatings for evaluation and their preliminary test results were discussed in previous reports.^(1,7) Additional evaluation of these tests is given below:

a. Sliding Friction Tests

Sliding friction tests were made at 1250°F and 10^{-7} torr to compare the frictional characteristics of the various coatings against carbon graphite, both

P5N and P5. The test data are summarized on Table III-3. All of the carbide coatings exhibited erratic friction properties. Coatings with Cr had a tendency to transfer it to the carbon graphite surface. The behavior of the Al_2O_3 coating against both P5 and P5N was similar.

TABLE III-3
SLIDING FRICTION TEST COEFFICIENTS
(at 1250°F, 10^{-7} torr)

Bearing Couple	Hours at 1250°F	Maximum Friction Coefficient		Remarks
		At 1250°F	At Ambient	
Al_2O_3 vs P5	403	0.12	0.46	Test terminated due to leak in vacuum system
Al_2O_3 vs P5	950	0.29	0.69	Continuing to 2000 hr
Al_2O_3^* vs P5N	1010	0.28	0.69	
Cr vs P5N	192	0.32	0.64	Terminated due to leak; Cr transfer to P5N (heavy)
K-162B [†] vs P5N	980	0.35	0.50	
TiC vs P5N	1010	0.42	0.78	
TiC vs P5N	1068	0.62	0.65*	*850°F, $\mu > 1.0$ at ambient
WC vs P5N	1080	0.39	0.50	Cr transfer to P5N (slight)
WC vs P5N	1005	1.0	2.0	Cr transfer to P5N (slight)
Cr_3C_2 vs P5N	1195	0.85	0.68	Cr transfer to P5N (moderate)
Cr_3C_2 vs P5N	-	-	-	Seized during heatup
Haynes-90 vs P5N	170	-	-	Seized after 170 hr at 1250°F

*Control sample for comparison of frictional characteristics.

[†]TiC-Ni cemet

b. Material Compatibility Test

Based on analysis of compatibility specimens tested for 500 hr, inspection of 1000- and 2000-hr specimens was concentrated on the carbide coatings

(TiC, Cr_3C_2 , and WC) and the Al_2O_3 coatings. Specimens tested at 1250, 1350, and 1450°F were examined by metallurgical and X-ray diffraction techniques.

Al_2O_3 Coatings. Twelve 1000-hr exposure specimens were examined. Nine were tested in contact with P5N carbon graphite, one with P5 carbon graphite, and two unexposed to graphite. All nine against P5N had areas of separation between the Al_2O_3 and the substrate (Inconel-X, Inconel-600, Ta-10% W). Examination of the Al_2O_3 showed a definite change in the microstructure and X-ray diffraction inspection showed a corresponding change in the Al_2O_3 from γ to α phase. The Al_2O_3 against P5 and the Al_2O_3 unexposed to carbon graphite showed no separation between Al_2O_3 and the Inconel-X substrate, no change in the Al_2O_3 microstructure, and no change from γ to α phase.

Four specimens from the 2000-hr tests were examined, one Al_2O_3 in contact with P5 and one Al_2O_3 in contact with P5N from both the 1350 and 1450°F tests. Again, both specimens in contact with P5N showed failure and separation between the Al_2O_3 and the Inconel-X substrate. Change in microstructure and phase change of the Al_2O_3 was again found. No separation, or microstructure or phase change occurred with the two specimens in contact with P5. Photomicrographs of these four specimens are shown as Figure III-18. In all cases, the only known difference between failed and unfailed specimens was the presence of the LiF impregnant in the P5N carbon graphite. The unchanged structure of the Al_2O_3 and the excellent bond to the substrate of the sample tested in contact with the P5 carbon graphite is attributed to the absence of LiF. Inspection of the samples now on 5000-hr test will be conducted to confirm these conclusions.

Carbide Coatings. Examination of the 1000-hr carbide-coated specimens substantiated the 500-hr test results. No samples showed any signs of separation or failure of the coating to substrate bond. There was considerable transfer of Cr from the Cr_3C_2 specimens to the surface of the P5N carbon graphite. The WC specimen also exhibited this transfer, but to a lesser degree. Comparison of the 500- and 1000-hr samples indicated that the Cr transfer was dependent upon both time and temperature. The TiC-coated specimen had no sign of Cr transfer, as expected, since this coating did not contain any metallic binders. The diffusion zone between the coating and the substrate of the TiC was much smaller than for either Cr_3C_2 or WC.

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a. AT 1350°F AGAINST P5 GRAPHITE

b. AT 1450°F AGAINST P5 GRAPHITE

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Figure III-18, a and b. Alumina (LA-2) Coatings on Inconel-X Substrate Exposed for 2000 Hours (taken at 250X: reduced 20%)
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c. AT 1350°F AGAINST P5N GRAPHITE

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d. AT 1450°F AGAINST P5N GRAPHITE

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Figure III-18, c and d. Alumina (LA-2) Coatings on Inconel-X Substrate Exposed for 2000 Hours (taken at 250X; reduced 20%)
(Sheet 2 of 2)

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Examination of the 2000-hr specimens was limited to TiC, the Cr_3C_2 and WC coating being eliminated for further evaluation because of the Cr transfer. Again, no indications of coating failure or separation were noted. The surface of the TiC as prepared for metallographic examination appeared to be more porous after the tests than was the untested sample. As shown in Table III-4 there is no definite relation between this apparent porosity and either test time or temperature. It is believed that the apparent porosity is due to loss of TiC material during the preparation of the sample for the examination. One conclusion is that coating is weakened during initial exposure above some threshold temperature. The amount of material pullout from the weakened coating then depends upon mechanics of the sample preparation.

TABLE III-4
APPARENT POROSITY OF TiC COATINGS

Exposure Temperature (°F)	Porosity (%) [*]			
	As-Received	At 500 hr	At 1000 hr	At 2000 hr
1250	10.8	-	13.5	40.3
1350	-	-	24.8	30.5
1450	-	35.7	22.4	24.4
1450 (no graphite)	-	-	23.3	13.7

*Porosity is an estimate made through visual observation; it is not absolute values.

Examination of the 1450°F 2000-hr specimens show carbide precipitates in the grain boundaries of the Inconel-X. This condition exists only very close to the TiC interface and within the grains adjacent to the TiC. The specimen in contact with P5N carbon/graphite shows this condition to a greater extent than the specimen not in contact with any carbon graphite (Figure III-19). Additional examinations will be made of specimens being tested to 5000 hr at 1250 and 1350°F.

a. IN CONTACT WITH P5N GRAPHITE

b. WITHOUT GRAPHITE

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Figure III-19. Carbide Precipitates in Grain Boundaries and in Grains of Inconel-X Close to the TiC Interface, 2000 Hours at 1450°F (500X, etched with Marbles' Reagent)

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c. Prototype Shaft Test

One TiC-coated shaft set has completed 1800 hr of testing at 1250°F; a second set, two Cr₃C₂, two WC, and one Al₂O₃-over-Ni shaft sets have completed up to 900 hr at 1250. Each bearing set completed 50 thermal cycles in addition to the 900-hr operation tests. Visual examination of each set after testing showed no signs of failure or separation of the coatings. No metallurgical examination of test parts has been made. A summary of the friction torque for each bearing set is given in Table III-5.

TABLE III-5
PROTOTYPE SHAFT TESTS, BEARING FRICTION TORQUE

Bearing Set	Total Time (hr)	Maximum Friction Torque (in.-lb)					Remarks
		Thermal Cycles		1250°F Dwell Test		Operational Test (1250°F)	
		at 450°F	at 1250°F	Pre-Test	Post-Test		
TiC vs P5N-1	1181	3.1	2.7	2.3	2.7	2.5	{ Same bearing set, balls pinned in socket after 1181 hr Balls pinned in socket
-1 (cont.)	2000	4.1	1.9	1.4	2.6	2.8	
-2	1261	2.8	3.2	3.2	4.1	5.7	
Cr ₃ C ₂ vs P5N-1	1463	3.6	3.0	2.8	3.6	3.7	
-2	1415	2.6	2.4	2.3	3.6	4.8	
WC vs P5N-1	729	3.0	2.8	2.6	7.0	5.5	Test terminated after 400-hr operational testing due to high friction
-2	1220	2.6	1.6	1.3	2.6	3.7	
Al ₂ O ₃ vs P5N-1	694	4.0	2.5	2.0	2.5	2.5	Test terminated after 300-hr operation testing

It was concluded from the evaluation of these test results that none of the alternate coatings performed as well as the Al₂O₃. The increase in the apparent porosity of TiC coating, which performed the best, led to some doubts about its performance for 12,000 hr. Better evaluation of the TiC coating can be made after the examination of the 5000-hr-material compatibility samples. The results also confirmed the previous indications that the failure of the Al₂O₃ coatings was attributable to the LiF impregnant in the P5N carbon graphite bushings. While gross coating failures could be repeated when Al₂O₃ was tested in contact with P5N, no evidence of failure was present when tested in contact with P5 carbon graphite. Furthermore Al₂O₃ exhibited the same favorable frictional characteristics against P5 as it did against P5N.

By substituting a solid P5 carbon-graphite bearing ball it was possible to utilize the new bearing material couple while salvaging most of the existing bearing components. In addition, the change introduces the material couple at the bearing-ball-to-socket interface. This combination is superior to the Al_2O_3 -vs-dry-film-lubricated Al_2O_3 couple of the original design. The backup design substitutes the TiC coating in place of the existing Al_2O_3 coating on the shaft and can be effected by making a new shaft. The composite Al_2O_3 coated Inconel-X ball with the P5N bushings will be used with this backup since no reaction due to the LiF was noted or expected from the thermodynamic consideration.

d. Design Verification Tests

Design-verification testing set up to establish operational conditions very similar to S8DR operations during the early phase, is being planned on four bearing sets of the modified design and on one of the backup design. The latter phase of the test will include more operations and thermal cycles and higher test temperature to demonstrate design margin. The following will be performed on each bearing set:

- 1) 255 equivalent full-in to full-out drum rotation (5 reactor lifetimes).
- 2) 200 thermal cycles (4 reactor lifetimes).
- 3) 3000 hr of dwell at temperature.
- 4) 8500 hr of operation, with 3000 at 1250°F, 100 above the maximum temperature expected for 600 kwt-operation of the S8DR.

Bearing balls of solid P5 carbon graphite have been fabricated and the first three tests are being set up. Bearing shafts with TiC coating are being completed and the test on the backup design will be started in the near future.

2. High-Temperature

The objective of the high-temperature bearing program is to develop bearings for use in deep space at temperatures up to 1500°F for a design life of 20,000 hr. The high-temperature bearing program consists of three tasks:

- 1) High-temperature thermal-vacuum endurance test of the reference bearing.

2) Compatibility tests of alternate bearing couple materials.

3) Sliding friction tests of alternate bearing materials.

a. High-Temperature Test of Reference Bearing

The fabrication of the reference test bearing, shown in Figure III-20, was completed. The test bearing set was installed in the test fixture for calibration and bench checkout. Some modifications of the fixture were required to complete this phase. The bearing-test fixture, shown in Figures III-21 and -22, was installed in a large ion-pumped ultrahigh-vacuum test chamber, and the room-temperature operation tests were run. The test system is being baked out and heated to achieve the test conditions of 1500°F at the bearings in a vacuum of 10^{-8} torr. The bearing set will be initially operated for 2000 hr under these conditions.

b. Material Compatibility and Sliding-Friction Tests

The basis for selecting five material combinations for both the compatibility and friction tests was discussed previously.⁽¹⁾ The test samples of these materials are being purchased or fabricated. The test fixtures have been designed and are also being fabricated.

D. INSTRUMENTATION

1. Position Sensors

The control-drum position-sensing system includes: a rotary differential transformer shaft position-sensor; a demodulator, which conditions the position-signal from the sensor; and a display system driven by the conditioned position-signal.

Four developmental position-sensors of the modified design are undergoing design-verification tests. Two of these have been thermally cycled to 800°F 200 times, and have subsequently completed 4745-hr exposure to 800°F at 10^{-6} torr, of a 5000-hr thermal-endurance test.

The other two sensors have completed 6660 hr and 31 thermal cycles of a combined 55-thermal-cycle 12,000-hr endurance test to 800°F. A review of the test data made at the completion of 5000 hr shows these two units to have

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Figure III-20. High-Temperature Test Bearing

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Figure III-21. Fixture Components Located External to the Vacuum Chamber

Figure III-22. Components of Bearing Test Fixture Within the Vacuum Chamber

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excellent insulation resistance (72 megohms minimum at 800°F) with an increasing trend. The output shift with temperature and the data scatter with time are similar to that seen on the originally designed units, and represent approximately 9 to 10° of rotation. Average sensitivity for both units is approximately 12.5 mv/° rotation and remains quite constant with temperature and time. On some data runs groups of points deviated from the average slope, reducing the slope in the vicinity of the points. The aberrant points and the general data scatter are most likely caused by translational forces imparted to the sensor rotor shaft by the test fixture. Because these forces should be greatly reduced on the S8DR, the output effects should be correspondingly diminished.

2. Limit Switches

Long-term (12,000-hr) testing is in progress on nine switches selected from previously run short-term static and dynamic tests. The new test has accumulated 2070 hr of test time, 1717 switch-operation cycles, and 13 thermal cycles from room temperature to the 1000°F test temperature. The latest test data and a description of the test items are listed in Table III-6.

TABLE III-6
LIMIT-SWITCH TEST DATA

Serial No.	Description	Applied Voltage (vdc)	Previous Test Time (hr)	Contact Resistance (Ω)	Insulation Resistance (Ω)*
001	Production unit	1	5518	0.430	5 k
003	Alumina insert	24	2771	1.060	6.3 M
005	Alumina insert	24	2771	0.383	31 M
006	Development unit	24	15518	0.446	830
032	Production unit	1	5518	0.380	25 k
065	Vent holes drilled	1	2771	1.188	250 k
066	Vent holes drilled	1	2771	0.480	5 M
068	Production unit	1	2771	0.381	50 k
069	Production unit	1	2771	0.365	17 k

*Resistance measured between pin and case; lower value of the two shown.

The smoothed-out curves of insulation resistance vs total integrated test time for the switches are shown on Figure III-23. According to the data in Table III-6 and Figure III-23, the trends discussed previously⁽¹⁾ have continued. Production switches S/N-001, -032, -068, and -069, with 1 vdc applied, continue to degrade with time. Development switch S/N-006, which partially recovered from a low value of insulation resistance at the beginning of its second dynamic test, has again decreased in resistance in this test and has appeared to stabilize at approximately 1000 Ω , compared to the 600- Ω resistance level reached previously at the end of 10,000 hr.

The two switches with vent holes drilled near their bases, S/N-065 and -066, although exhibiting some degradation with time, have shown clearly superior performance over unvented switches. The lowest resistance reached for either unit has exceeded 200,000 Ω .

The two experimental units with the commercial receptacles replaced with alumina inserts have shown the best performance of all the switches, and no definite degradation trend with time is apparent. Thus far it would appear that the use of an alumina insert is the approach which should be followed in switches for future development for advanced flight application. However, since the minimum insulation-resistance requirement of the S8DR system is less than 100 Ω , the test data continues to indicate that the present production switches should meet all S8DR system requirements. To provide added assurance, the redundant reflector switches will not have a voltage applied during the time they serve as installed spares. Previous tests have shown that insulation-resistance decay occurs only when potential is applied.

3. Temperature-Sensing System

The automatic control system of the SNAP 8 nuclear system is based upon maintaining the core-outlet temperature within predetermined limits. The temperature-sensor for this system consists of a platinum resistance-temperature-detector immersed in the coolant line. It is electronically coupled to a temperature switch which provides the signal that controls the movement of the control drums.

The initial three-point calibration at the ice point, and at the melting points of aluminum and zinc, was completed on the six new temperature detectors, and

Figure III-23. S8DR Limit Switches Smoothed Curves of Insulation Resistance vs Time (combined test)

the first 500 hr of the 1000-hr stabilization test before long-term endurance testing were completed.

The long-term (12,000-hr) drift test on two other detectors has accumulated 2503 hr. The thermal-cycle testing of two additional units between room temperature and 1340°F, at rates in excess of 300°F/min, has completed 90 and 60 cycles respectively.

E. SCRAM MECHANISM

A scram mechanism was designed to shut down the S8DR rapidly in the case of emergency during operation. Tests to verify the performance capability and margin of the scram mechanism were made on two test assemblies with the vital components heated to design temperatures. A third was similarly tested except that the scram clutch was operated at 100°F above design throughout the 10,000 hr of testing. The test history on this unit was described in the previous report.⁽¹⁾ In the interim this unit was utilized as a test fixture to conduct high-temperature acceptance tests on the modified scram snubber for the S8DR. Upon completion, the scram mechanism will be disassembled and inspected.

F. SHIELD-MATERIALS DEVELOPMENT

1. Liquid-Lead Studies

The objective of the lead gamma-shielding program is to investigate liquid lead and lead-bismuth eutectic alloy, which offer a potentially lower-weight shield than uranium or tungsten because of their lower secondary-gamma production and their relatively high densities, 11.3 and 10.5 gm/cc respectively.

Test capsules of 1020 carbon steel, Croloy 2-1/4, and Type 446 stainless steel loaded with Pb or Pb-Bi alloy were heated at 1000, 1300, and 1500°F for 625, 2500, and 10,000 hr. The 30 capsules which had been exposed for 10,000 hr (18 contained Pb, 12 Pb-Bi) were given a preliminary examination.⁽¹⁾ Eighteen of these plus untested control capsules were examined metallographically. The results of the evaluation are as follows:

a. Untested Control Capsules

Although the exact process history of the raw tube stock is not known, the microstructures indicated that the tubes from which the capsules were fabricated had been drawn and annealed (Figure III-24). The deep gouges and irregularities on the ID of the Type 446 stainless steel capsule were believed caused by the drawing operation.

After one end of each capsule was Heliarc-welded closed, the capsules were pickled to remove surface oxides, dried, and sealed in plastic bags until needed for loading with Pb or Pb-Bi. When the bags were opened in preparation for loading, a large number of the capsules were found to be rusted, probably from improper drying. Other capsules were found to have whitish deposits on the surface from improper rinsing after the pickling. The pickling operation was repeated in the laboratory to remove the rust and deposits. Consequently, the pitting of the capsule surfaces, as shown in Figure III-24 (photomicrographs of the untested controls and the later-described tested specimens), was believed to have been present before the corrosion test started.

b. 1020 Carbon Steel

Metallographic examination revealed that the 1000°F exposure to liquid Pb for 10,000 hr did not appear to have any significant effect (Figure III-25). The microstructure remained fine-grained and the recovery was slight. The carbides were in a spheroidized form, and there was little if any pearlite remaining.

The 1300°F exposure showed more noticeable carbon loss by the steel (Figure III-26). The microstructure was predominantly ferrite with some spheroidized carbides. The grain structure was enlarged. Some difference was found between the microstructures of the steel exposed to Pb with zirconium and magnesium corrosion-inhibiting additives and that without additives. With additives the recovery of the steel was insignificant and the spheroidized carbides were dispersed within the ferrite grains. Without additives the ferrite grain became equiaxed and the carbides were precipitated at the grain boundaries.

For both 1000 and 1300°F exposures a thin reaction layer, possibly an oxide, appeared to be on the steel surface in the capsules without additives. The exposed surfaces of the capsules with additive were irregular and well defined. Sporadic leaching of grain-boundary carbides also was observed.

Figure III-24. Microstructure of Untested Control Capsules

Figure III-25. Photomicrographs of 1020 Carbon-Steel Capsules
Exposed to Liquid Lead for 10,000 Hours
(taken at 250X, reduced 30%)

Figure III-26. Photomicrographs of Croloy-2-1/4-Steel Capsules
Exposed to Liquid Lead for 10,000 Hours
(taken at 250X, reduced 30%)

The 1500°F exposure (Figure III-25c) resulted in partial austenitization of the steel (the microstructure of the control capsule was a mixture of ferrite and pearlite). A decarburized zone of less than 0.005 in. was found on the Pb-exposed surfaces. However, the surfaces were relatively smooth.

c. Croloy 2-1/4 Steel

The general behavior of the Croloy 2-1/4 steel was similar to 1020 carbon steel, except that the Croloy 2-1/4 was more resistant to grain growth (Figure III-26). The samples exposed at 1500°F were completely pearlitic and showed a decarburized layer similar to that in the 1020 steel capsules.

d. Type-446 Stainless Steel

The microstructure of the Type 446 stainless steel after 1000°F exposure very much resembled sensitized austenitic stainless steel. The grain boundaries were heavy and formed a complete network (Figure III-27a). The 1300°F exposure resulted in the precipitation of carbide phases and possibly some sigma phase. Formation of σ phase was rather massive in the 1500°F exposure. Sporadic and localized decarburization also was observed (Figure III-27b and c).

The effect of the additives on the stainless steel was negligible. The only significant effect of the exposure appeared to be structural changes in the alloy. This effect was believed to be basically temperature- and time-produced.

e. Conclusions

The results of the Pb-corrosion tests indicated that the only significant effect on the 1020 carbon steel, the Croloy 2-1/4, and the Type 446 stainless steel was decarburization. It was slight at 1000°F, more noticeable at 1300, and pronounced at 1500. Since decarburization in a stagnant system depends on C activity in Pb, this problem could be readily overcome by adding an appropriate amount of C to the Pb to neutralize its decarburization potential.

Based on these tests and assuming a 1300°F maximum liquid-Pb operating temperature, the Croloy 2-1/4 steel appears to be the most attractive alloy for the following reasons:

- 1) 1020 carbon steel has a low creep-strength at 1300°F,
- 2) Type 446 stainless steel has a tendency to form σ ferrite during prolonged exposures between 1000 and 1600°F.

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a. AT 1000°F

8070-14-7

8070-15-7

b. AT 1300°F

8070-16-7

8070-17-7 WITH ZIRCONIUM AND MAGNESIUM ADDITIVES

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c. AT 1500°F

WITHOUT ADDITIVES

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Figure III-27. Photomicrographs of Type 446 Stainless Steel Capsules Exposed to Liquid Lead for 10,000 Hours (Taken at 250X, Reduced 30%)

Above 1300°F Croloy 2-1/4 does not appear to be useful, because the resulting phase transformation could cause dimensional instability. A super alloy or a refractory metal such as niobium may be required if the gamma-shield operating temperature exceeds the 1300°F limit.

The 12 capsules containing Pb-Bi which were exposed for 10,000 hr at 1000, 1300, and 1500°F are being prepared for examination.

2. Aggregate Neutron-Gamma-Shield Materials

The objective of the aggregate shield-materials program is to investigate the characteristics of mixtures of lithium-hydride and zirconium-hydride, and lithium hydride and tungsten, which offer the versatility of tailoring the amount of neutron and gamma shielding to fit the specific application. These mixtures are formed by casting LiH into an aggregate of cheaper more-available granular or powdered material. The current program efforts are to investigate the range of densities obtainable by using ZrH of different particle sizes and shapes, and to determine the extent of H-redistribution in a ZrH-LiH composite under a temperature gradient.

a. Density Particle-Size Study

About 50 lb of commercial-grade zirconium rod was hydrided in the laboratory to an N_H of about 6.3×10^{22} H atoms/cc and then crushed into approximately 1-in. diam (maximum) pieces. The crushed ZrH was sieved into the following particle-size ranges:

<u>Sieve Sizes</u>	<u>Particle-Size Range (in.)</u>
+10 mesh	+0.079
-10, +20 mesh	0.033 to 0.079
-20, +80 mesh	0.007 to 0.033
-80, +100 mesh	0.006 to 0.007
-100 mesh	-0.006

Combinations of particle sizes will be used to vary the density of cast LiH-ZrH samples.

b. H-Redistribution in ZrH-LiH

One-in.-diam test rods of ZrH-LiH were cast in the laboratory for use in the H-redistribution test. Three-in.-long test specimens were cut from the rods and loaded into capsules (see Figure III-28) which were evacuated and sealed. The six capsules were uniformly heated for 100 hr at 1000°F to equilibrate the specimens, then were cooled to establish the "cold"-end temperatures of 500 and 800°F (3 capsules each). The other end of each capsule was then heated to obtain a "hot"-end temperature of 1000°F.

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Figure III-28. Schematic of Test Capsule

The two sets of capsules will be heated at gradients of 500 to 1000 and 800 to 1000°F, for 1000, 4000, and 10,000 hr. At the end of the test period the hydrogen content of the specimens will be measured and compared with the values obtained from untested samples to determine any possible migration within each capsule.

3. Combined Neutron-Gamma-Shield Compounds and Alternate Neutron-Shield Materials

The objectives of these two efforts are to (a) identify chemical compounds which contain both H and a high-atomic-weight element for potential future use as materials to serve as dual neutron-gamma shielding and (b) identify compounds having a higher ratio of removal cross-section (Σ_r) to density (ρ) than LiH which will provide potential weight savings in future neutron-shield designs. A literature survey is currently underway to identify compounds meeting these requirements.

G. PUMP

1. Multipass dc Conduction Pump Test

The testing of the triple-pass dc conduction pump⁽¹⁾ was completed. The pump design is shown in Figure III-29. Figure III-30 is a photograph of the pump.

The testing covered four subjects: magnet system, hydraulic loss, pump resistance, and pump performance.

a. Magnet System

The purpose of this test was to determine the flux density which could be reached in the magnet system supplied. The calculated maximum flux was 16,160 lines/in.² in the center throat and 8,080 in the outer throats. The calculated values are based on a magnet temperature of 540°F, which corresponds to a NaK temperature of 1000°F. A comparison between the calculated values and experimental data, adjusted for 1000°F NaK, is shown in Figure III-31. The experimental data were obtained at room temperature. The adjusted experimental values vary from 93.5 to 133% of the calculated values. In general the center-throat values show better agreement than those of the outer throat. The outer-throat values are uniformly higher than the calculated values.

It appears that the magnets were operating at a somewhat higher flux level than was anticipated and that there was a higher flux leakage along the sides of the center throat than was accounted for in the calculations.

The variation in the measured flux from end to end of each channel is, no doubt, due to the extreme sensitivity of flux to position in a region of rapidly decreasing flux.

b. Hydraulic Loss

The results of the hydraulic-loss test are shown in Figure III-32. Test values are approximately 50% higher than the calculated values. The discrepancy could be due to such things as excessive weld droptrough. In any event the hydraulic loss of the pump is small and quite reasonable when compared to the net output of the pump.

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Figure III-29. Triple-Pass Pump, Diagram

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Figure III-30. Triple-Pass DC Conduction Pump

Figure III-31. Magnet Flux Test

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Figure III-32. Triple-Pass Pump Hydraulic Loss

c. Pump Resistance

The purpose of the pump-resistance test was to verify calculation of the pump resistance. This is best done at zero flow where the effect of flow terms in the performance equation are zero. Ideally this test would be performed at design operating temperature (1000°F); however, in practice it is difficult due to the likelihood of hot spots occurring near loop heaters and cold spots occurring in the pump throat at zero flow. Consequently the test was performed at room temperature (65°F).

The resulting data are shown in Figure III-33. Agreement between the experimental and calculated values is evident. The calculated zero-flow pressure is about 10% higher than the experimental values and the calculated wall voltage is about 5% higher than the experimental values. This indicates that the calculated NaK resistance was 5% low and the calculated shunt resistance was 10% high.

d. Pump Performance

The pump-performance test is the most important of the tests performed, since it shows what the pump will do when installed in a hydraulic system. The results of this test are shown in Figures III-34 and -35.

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Figure III-33. Triple-Pass Pump Pressure and Wall Voltage at Zero Flow

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Figure III-34. Triple-Pass Pump Wall Voltage

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Figure III-35. Triple-Pass Pump Performance

The pressure-flow characteristic is shown in Figure III-35 as a function of the pump current. The experimental values are within 10% of the calculated values, confirming that the experimental shunt resistance is 10% lower than the calculated value.

Figure III-34 shows the wall voltage as a function of flowrate and current. The calculated values are about 5 to 7% higher than the experimental values, again, supporting the claim that the actual NaK resistance is 5% higher than calculated and the shunt resistance 10% lower than calculated.

The results of extrapolating the data of Figures III-34 and -35 back to zero-flow and plotting the pressure rise and wall voltage as a function of current are shown in Figure III-36. Comparing this to Figure III-33 we see the same agreement between the measured and calculated values of pressure rise and wall voltage for the case of 1000°F-NaK as for room temperature.

The pump efficiency is shown in Figure III-37 as a function of NaK flowrate and pump current. The calculated efficiency is about 5% higher than the measured values. The highest pump efficiency measured was about 30%. For single-pass dc pumps which have been designed for maximum efficiency, efficiencies

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Figure III-36. Triple-Pass Pump Zero-Flow Performance

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Figure III-37. Triple-Pass Pump Efficiency

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in the order of 40% are typical. The results of this test indicate that this figure can be achieved with multipass pump designs even though the latter would be somewhat less efficient than single-pass pumps due to higher hydraulic- and current-leakage losses in the multipass.

From the results presented here it is clear that the performance of multipass pumps can be calculated to well within acceptable limits of accuracy. Normally, for a single-pass dc conduction pump the performance is within 5% of the calculated value. For the multipass pump tested the experimental performance was within 5 to 10% of the calculated values.

2. Separate Source-Pump Design Using Lead-Telluride Converter

A preliminary pump system has been designed that utilizes a PbTe thermoelectric converter. It was designed by a subcontractor, Westinghouse Corp., for the following conditions:

Flowrate	30 gpm
Pressure rise	2.3 psi
Hydraulic power	30 watts
Hot-NaK temperature	1200°F
Cold-NaK temperature	500°F

The pump used in the system is a triple-pass dc conduction pump similar to the one in Figure III-29. The magnet assumed is Alnico-V. Higher-energy product materials such as Alnico-IX could be used with reduced magnet weights. The three throats of the pump are connected by a Cu bus which is brazed to the throats. The predicted performance for this type of pump has been confirmed experimentally (see Section III-G-1).

The assumed converter configuration is shown in Figure III-38. It consists of two PbTe couples connected by an iron-plated Cu strap. The use of Cu for the strap material is a departure from the Westinghouse design and is not a confirmed design approach. If substitution of another material for the Cu, such as solid iron, is found to be necessary, appropriate changes will have to be made to accommodate the increased strap resistance. Also, the method used in the current design to bring out the current leads will need to be revised because of the need to carry large currents.

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Figure III-38. Thermoelectric Module

The performance characteristics of the PbTe TE converter are:

Average hot-clad temperature, °F	1200
Average cold-clad temperature, °F	500
Load voltage, mv	188
Current, amp	500
Efficiency, %	5.2
Number of couples	2

The distance between the pump and converter was assumed to be 12 in., making the total bus length 24. The bus diameter would be 0.70 in., the bus material copper.

The calculated pump-system weight in lb is:

Pump	30.0
Bus	2.8
Converter	5.0
Total (including 15% contingency)	44.0

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The overall system efficiencies in % are:

Pump efficiency	43.8
Bus efficiency	73.5
Converter efficiency	5.2
Overall efficiency	1.7

Although the converter design in Figure III-38 is not to be construed as the only workable converter design for use with dc conduction pumps, it does represent the design oriented toward a minimum-weight pump system. It might, however, be designed with additional current passes to reduce the total current required, and the converter internal strap losses, thus making smaller strap sizes feasible. The number of couples would be increased to provide the electromagnetic force necessary to drive the current through the additional pump-throat passes.

Additionally, it is not necessary to have the couples, or even the elements of a single couple, installed in a single converter shell; they could be connected electrically external to the converter, thus reducing size of internal straps.

In any event the design shown provides a basis from which a study can start concerning the feasibility of using a PbTe converter to power a dc conduction pump.

As the number of passes is increased the total pump-system weight decreases until about three passes are reached. Then the total weight increases with further increase in the number of passes. The primary reason for the decrease is a large reduction in the weight of the bus and in the size and therefore weight of the interconnecting straps required in the converter. The rise in the total weight after more than three passes is due in part to the increased hydraulic loss of the pump and the need to build the pump larger to compensate for this loss. The converter weight also increases, since a larger converter is needed to supply the power to the larger pump, and after three passes there is no significant increase in converter efficiency.

Selection of TE module and pump designs depends on further definition of the fabricability limitations of the module as compared to the weight penalty, and on difficulties involved in fabricating a pump with a large number of passes.

IV. SNAP 8 DEVELOPMENT REACTOR (S8DR)

A. SUMMARY

The work leading towards the S8DR test proceeded on schedule, highlighted by completion of over half the fuel elements, initiation of reflector assembly, and completion of facility preparations for the non-nuclear test. Analyses of the S8DR performance and environment were refined in several important areas.

Thirty-four percent of the required 215 enriched fuel elements for S8DR and 79% of the fifty-six elements for performance testing were completed and the first batches were delivered to the Santa Susana Facility. Another 30% of the elements were completed and in acceptance testing. A few minor changes were made in the fabrication process to eliminate items causing rejection, and the production yield was approaching a very good level.

The effort to develop and deliver the reactor-vessel head with the incore thermocouples was completed. Tests were completed demonstrating the adequacy of the brazed bulkhead design, and the bulkheads for the non-nuclear test and S8DR core vessels were completed.

Several minor changes were made on the reflector-assembly design as a result of experience obtained on the half-reflector design-verification test. The new P5 graphite bearings were designed and fabricated, based on the work described in Section III, and the S8DR reflector assembly was completed to the point of installing the wiring harness.

The S8DR test facility was essentially ready for start of non-nuclear testing at the end of the report period. The modifications to larger NaK piping were completed, the NaK systems were insulated, and all facility modifications associated with the new cooling water tower were completed. The core vessel and piping were fitted into the vacuum chamber in preparation for the final welding of the system. All hardware and procedures for the non-nuclear facility checkout were available, and training of the operators for this phase of the test was accomplished.

B. DESIGN AND ANALYSIS

1. Nuclear

The rate of reactivity loss in S8DR as a result of fission-product buildup and fuel burnout has been calculated as a function of reactor operating lifetime. The calculations were performed with the PABUC computer code.

The analysis indicated that the rotation of the control drums inward decreased the worth of the fission products and burnup. The decrease resulted from the flattening of the radial-power profile. It was also found that the worth of the fission products and fuel burnout increased with time for constant drum position. The fuel was depleted and the fission products accumulated preferentially in the high-flux high-worth region of the core.

The two effects offset each other in S8DR. Consequently, a single value is satisfactory for use for the reactivity-loss coefficient. For core operation from startup to 20 months (at 600 kw) the reactivity-loss coefficient for fission-product buildup and fuel burnout is 0.0175 ± 0.0002 ϵ /kw-month.

a. Fuel-Growth Correlation

The data from the NAA-115-2 and the NAA-120-4 fuel-irradiation experiments were compared to the previous fuel-growth data for ZrH_x -U fuel material. The combined data are shown in Figure IV-1 in the form of the accommodation-growth-corrected volume change ($\% \Delta V/V - 1.5$ met. at. $\%$ burnup) plotted as a function of reciprocal beginning-of-life temperature. The new data are seen to fall well within the trend of previous $\delta/\delta + \epsilon/\epsilon$ fuel-growth data. The mean of all $\delta/\delta + \epsilon/\epsilon$ data at $1400^\circ F$ is changed by the addition of the 115-2 data to $2.5\% \Delta V/V$ from the earlier all-carbon value of $2.48\% \Delta V/V$, and the variance at $1400^\circ F$ is changed to $0.86\% \Delta V/V$ from $0.79\% \Delta V/V$. Neither change is considered significant. Analysis of the data indicates, however, that a carbon separation is no longer supportable, and that an all-carbon model is the best correlation. The NAA-120 data shown are current deductions from temperature-change measurements.

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SEPARATION BY DOMINANT PARAMETER

CONFIDENTIAL RESTRICTED DATA (GP-1)

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Figure IV-1. Total Zr-10 U-Hydride Fuel-Growth Data, Separation by Dominant Parameter

2. Thermal

a. Reflector-Component Temperatures

Analytic models of the S8DR actuators and other reflector-mounted components have been prepared, and operating temperatures at 600 kw and 1 Mwt have been determined.

As shown in Table IV-1, significant reductions in actuator temperatures are achieved by the addition of a heat shield between the actuators and the top of the core vessel. The use of the shield is calculated to result in a top-head effective emissivity of 0.09 and a maximum actuator temperature slightly less than that reported in the table.

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TABLE IV-1
REDUCTION OF ACTUATOR TEMPERATURES BY
ADDITION OF HEAT SHIELD

	Maximum Actuator Temperature (°F)
Original calculation	1005
Revised with corrected core-vessel temperature profile	995
With heat shield between actuator and core vessel, ϵ_{ff} (vessel to actuator) = 0.1	955

Temperatures of other major components are listed in Table IV-2 for reflector emissivities of 0.7, 0.8, and 0.9. The higher emissivities are representative of BOL conditions while 0.7 is a minimum, EOL value.

TABLE IV-2
COMPONENT TEMPERATURE VARIATIONS WITH
REFLECTOR EMISSIVITY

Operation	Emissivity	Temperature (°F)			
		Upper Bearing	Scram Clutch Coil	Scram-Kit Bearing	Drive-Gear Housing Bearing
600 kwt, 1300°F Outlet Temperature					
	0.7	1063	883	719	804
	0.8	1043	881	716	801
	0.9	1024	881	715	798
1 Mwt, 1100°F Outlet Temperature					
	0.7	1134	903	726	809
	0.8	1114	902	723	805
	0.9	1089	901	722	802

b. Overall Reactor Heat-Transfer Coefficient, UA

The average thermal performance of a reactor is usually expressed in terms of an overall reactor heat-transfer coefficient, UA, defined as $UA = P/\Delta T$, where P = reactor thermal power (kw) and ΔT = average temperature difference in core, fuel to coolant. The UA for S8DR is important for three purposes:

- 1) To determine steady-state average fuel temperatures used to obtain macroscopic core changes during lifetime (e.g., hydrogen leakage, fuel growth, etc.)
- 2) To calculate the reactor response to transient operation.
- 3) To estimate the fuel-reactivity coefficient α_f since only the value of α_f/UA can be determined experimentally with reasonable accuracy.

Although incore instrumentation will provide experimental measurements of selected individual channel outlet-coolant temperatures and, therefore, indirect measurements of the coolant-temperature distribution within the core, there is no direct way of measuring local fuel temperatures. These must be inferred from analysis and experimental determination of core average performance as defined by UA.

The variation in the S8DR overall heat-transfer coefficient with temperature at BOL is shown in Figure IV-2. The primary variable affecting the coefficient is the amount of H_2 gas in the radial gap between the fuel and the cladding; more specifically, by whether or not a gas-continuum exists.

The gas-gap thermal resistance is a function of gap width and gas conductivity. As long as a continuum exists in the gap, the conductivity is dependent only on temperature. (The conductivity of H_2 varies as the absolute temperature to approximately the 0.58 power.) When the gas pressure falls below approximately 1 atm (for nominal S8DR gap-widths) free-molecular effects appear at the fuel and ceramic surfaces. These take the form of additional temperature drops that can be interpreted as increases in the effective gap-width.

This thermal-jump effect can be evaluated theoretically and is discussed in standard works on kinetic theory of gases.⁽⁸⁾ The fictitious additional gap width at a solid-gas interface can be expressed as:

Figure IV-2. Variation in Heat-Transfer Coefficient, Beginning-of-Life

$$\Delta w = \frac{2 - a}{a} \left(\frac{2}{\gamma + 1} \right) \frac{k}{\mu C_v} L ,$$

where

Δw = added gap-width,

a = accommodation coefficient,

γ = specific-heat ratio (1.4 for H_2),

L = mean-free-path length (4.63×10^{-3} mils for H_2 at 1 atm and $15^\circ C$),

k = gas thermal-conductivity,

μ = gas viscosity, and

C_v = gas specific-heat at constant volume.

For the calculations reported herein, an accommodation coefficient of 0.36 was assumed both for hydrogen on ceramic and for hydrogen on ZrH. The resulting effective coefficient (for the two surfaces combined) is 0.2. These values are in reasonable agreement with those given in the literature for hydrogen on glass and on metals.

At low temperatures a minimum residual hydrogen pressure-level is reached. This is a consequence of the extremely long-time constants for H_2 diffusion at low temperatures. All S8DR fuel elements before insertion in the reactor are permeation-tested at $1400^\circ F$ to check the hydrogen leakrate through the ceramic barrier. When the elements are cooled, a residual of hydrogen gas remains in the gap because not all the gas released by the fuel at $1400^\circ F$ is reabsorbed when the rod cools. Residual pressures have not been measured on S8DR elements. Two SNAP 10A fuel elements were tested in 1963 by welding a pressure probe to one end of each element and subjecting them to a typical 10A permeation test at $1200^\circ F$. The resulting residual pressures were 3.9 and 14.4-mm Hg. Similar pressure variations are expected for S8DR elements, as the cooldown rate, not the permeation temperature, is the principal variable determining the residual pressure.

The nominal UA curve plotted in Figure IV-2 assumes 5-mm-Hg residual pressure. The minimum curve is for 1 and the maximum is for 15-mm Hg.

The relationship between UA and average fuel temperature at EOL condition is shown in Figure IV-3. The analysis assumes that 12,000 hr of operation occurs at the nominal 600-kwt $1300^\circ F$ outlet condition.

Figure IV-3. Variation in Heat-Transfer Coefficient, End-of-Life

c. Core Pressure Drop

S8DR pressure-drops at the nominal 48,800 lb/hr NaK flowrate are listed in Table IV-3. The 4.38-psi calculated pressure-drop is in good agreement with the hydraulic test total pressure-drop of 4.6 ± 0.3 psi (converted to NaK flow by Reynolds Number analogy).

TABLE IV-3
CALCULATED S8DR PRESSURE
LOSSES, psi

Inlet nozzle	0.67
Flow distributor	0.45
Orifice plate	0.97
Lower grid plate	0.20
Coolant channel:	
Inlet	0.03
Channel	1.82
Upper grid plate	0.18
Exit nozzle	0.06*
Total calculated	4.38

*For hydraulic-test configuration. For instrumented bulkhead, exit nozzle $\Delta P = 0.52$ psi.

Pressure-drops at flowrates (W) other than the design value ($\bar{W}$) can be calculated from the equation

$$\Delta P = 1.82 \left(\frac{W}{\bar{W}} \right)^{1.8} + 2.56 \left(\frac{W}{\bar{W}} \right)^{2.0}$$

The first term represents the frictional losses along the core central channel, the second represents all other reactor losses (nozzles, orifice, plate, etc.).

Pressure losses were experimentally measured at off-design conditions in the hydraulic tests. When converted to NaK flow they indicate 1.3- and 7.0-psi drop across the reactor at 0.5 and 1.25 times S8DR nominal flow. The

corresponding calculated pressure losses are 1.16 and 6.7 psi. The 60,200-lb/hr flow required for 1-Mwt operation is calculated to result in 6.6-psi reactor-pressure loss.

As indicated in Table IV-3 the exit nozzle pressure-drop of 0.06 is for the hydraulic test mockup (and the flight configuration). The S8DR design outlet is a considerably higher pressure-drop device; the outlet flow is up a 5-in.-diam pipe to the bottom of a thermocouple bulkhead and out at right angles through a 2-in.-diam pipe. The nominal pressure-drop for this exit is calculated to be 0.52 psi. It can be assumed to ratio as the flow squared.

3. Stress

a. S8DR Internal Reflectors

Because of the cracks observed in the S8ER airfoil-shaped internal reflector-cladding, the D-shaped S8ER and S8DR reflector-claddings were analyzed and compared to the airfoil cladding to determine relative design margins. Although the airfoil-shaped cladding was cracked, the D-shaped cladding did not crack in S8ER. At the beginning of the operational life of the reflectors the cladding is subject to external pressure. Due to thermal and radiation expansion and helium release by the BeO inserts the internal pressure builds up, and at the end of the operational life of the reactor there is a net internal pressure on the cladding. The critical-loading conditions for the cladding are the initial external pressure at the start and the increasing internal pressure at EOL. The stresses in the cladding were calculated from the predicted pressure using Castigliano's theorem.

The calculated initial stress in the airfoil-shaped S8ER cladding was above the yield strength of Hastelloy-N which indicates that the cladding collapsed around the BeO insert. Since the BeO insert limits the cladding deformation, the internal pressure case is critical. The calculated internal pressure at EOL and the resultant cladding stress are shown in Table IV-4 for S8ER and S8DR.

The stress-to-cause-rupture is based on an analytic extrapolation of constant-stress vs time-to-rupture data to the case of linearly increasing stress vs time-to-rupture. Note that the S8ER airfoil stresses are more than an order of magnitude greater than the stress-to-cause-rupture. The S8ER

TABLE IV-4
CLADDING STRESS

	S8ER Airfoil	S8ER D	S8DR D
Operational life, hr	10,000	10,000	12,000
Temperature, °F	1,300	1,300	1,350
Net internal pressure at EOL, psi	26	28	38
Cladding thickness, in.	0.01	0.01	0.03
Maximum EOL stress, psi*	255,000	30,500	3,700
Stress-to-cause-rupture, psi	11,400	11,400	8,700

*Calculated, assuming that the material remains in the elastic region for all stresses

D-shaped stresses are calculated to be greater than the rupture stress even though the cladding did not actually rupture. This is a measure of the conservatism in the estimate of internal pressure and cladding strength. The calculated stresses in the internal reflector cladding for S8DR are a factor of eight less than the stress in the S8ER D-shaped reflectors (which did not crack), and they are also less than the predicted rupture stress. It is therefore concluded that there is an adequate design margin on the operating life of the S8DR internal-reflector cladding.

C. ASSEMBLY AND ACCEPTANCE TESTING

1. Fuel Elements

The fabrication of S8DR normal-uranium fuel elements for performance testing and enriched-uranium fuel elements for the core is 64% completed. Sixty-three enriched, 34% of the required 215, and 44 normal elements, 79% of the required 56, have been final released. Another 30% of the core have been assembled and are now in acceptance testing. The overall acceptance of batches which have completed acceptance testing is 67% (released elements/number assembled). This is somewhat less than the estimated yield of 80% established before fabrication. Table IV-5 is a summary of the S8DR fuel-element status and Figure IV-4 is a plot of fuel elements assembled and elements accepted by batch.

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TABLE IV-5
S8DR FUEL-ELEMENT ASSEMBLY STATUS

Elements Assembled			Acceptance Permeation Tests		Released		Element Yield (%) (Release vs Assembled)
Date	Quantity	Type*	Number Assembled	Number Accepted	Normal	Enriched	
Nov							
7	12	N	11	8	8		
8	12	N	12	8	8		
9	6	N	6	6	6		
17	12	E	10	10		7	
22	12	E	12	11 [†]		9	
28	12	E	7	4		4	
30	12	E	10	8		6	
Dec							
5	12	E	9	8 [§]		7	
6	12	E	11	10 [§]		9	
13	12	E	9	2 ^{**}			
14	12	E	11	10		9	
18	12	N	11	8	8		
20	12	E	12	12		10(61) ^{††}	
26	12	N	11	9	9		
27	6	N	6	5 [†]	5(44) ^{††}		66.7%
Jan							
2	12	E	11				
4	11	E	10				
9	12	E	12				
10	12	E	12				
15	12	E					
16	12	E					
19	12	E					

*N = normal; E = enriched

[†]One additional element had not completed inspection.

[§]One additional element was in for retest.

**Two elements had not completed inspection.

^{††}The cumulative number released is shown in parentheses, i.e. total number of normal and enriched elements released to date.

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Figure IV-4. S8DR Fuel-Element Fabrication Yield-Status

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Each batch of elements was carefully monitored during assembly and acceptance testing to evaluate causes for rejection. From data obtained on elements through the batch assembled on Dec 14, successful corrective action has been taken in three areas identified as contributing to fuel-element rejection. Figure IV-5 illustrates the areas which have been identified as causes for fuel-element rejection and compares the number rejected for the first 11 assembly lots (before corrective action) to the second 11 assembly lots (after corrective action).

Seven elements in the first 11 assembly batches were rejected for assembly fitup at the final electron-beam weld joint. The rejections were traced to a combined problem of insufficient accuracy of the measurement of the open end of the coated tube assembly and the specified dimension on the weld-flange of the cup-plug closure-seal. The diameter of the cup-plug weld-flange was increased ~ 0.002 -in. to eliminate mismatch of the tube OD to the OD of the cup-plug weld-flange. Also, the method of measuring the open ends of the tubes was changed. Gage pins of 0.0001-in. increments are now used to measure the inside of the tube. The previous technique of averaging the maximum and minimum inside diameters measured with a two-point inside micrometer proved to be inaccurate when tube ovality before assembly was beyond 0.0005 in. No rejections for this cause have occurred in the next 11 batches since these corrective actions were taken.

Eleven elements (Figure IV-5) were rejected from the first 11 assembly batches due to the closure weld. The cause for these rejections was traced to the 12-barrel indexing and drive-fixture which positions and rotates the fuel elements during welding. Minor fixture modifications were made to improve the drive train, and to reduce the rotational friction and longitudinal tolerances in the bearings, to position the elements better under the electron beam. Since this correction was made, the yield of acceptable elements has increased to 97% over the next 11 assembly batches. The last five batches of this group have actually had a 100% yield.

The rejection rate due to permeation tests has run somewhat lower than initially estimated, as expected. The estimated rejection due to the first permeation test is larger than that experienced for the first 11 batches of fuel elements (see Figure IV-5). This is due to the fact that a larger number of fuel

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Figure IV-5. Causes for Rejection, S8DR Fuel Elements

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Figure IV-6. Leakrate Distributions, S8DR Fuel Elements, as of 1-18-68

elements will reach the first permeation test than the latter, not because lower yield-rate is expected at this test. The average final-acceptance permeation rate of the fuel elements is running approximately 0.33 cc/hr normalized to 2 atm H₂. This is well below the required core average of 0.5 cc/hr. Figure IV-6 shows the distribution of fuel-element permeation.

The last cause for rejection of fuel elements was due to larger than specified diametral envelope requirements. This was traced to the amount of bow (TIR) in the coated tubes, which was not being reduced during the thermal-acceptance testing program as previously experienced. To eliminate this rejection a tighter restriction on tube bow before assembly was made, as were improvements in the coated-tube firing racks and firing procedures to reduce the coated-tube bow.

The status of S8DR fuel-element hardware is given in Table IV-6. About 50% of the tubing and cup-plugs have been coated and all other hardware is completed. All hardware acceptance yields are above 80% with the exception of coated tubing which is 60%. Flex-test results for 328 coated tube-end samples show an average of 12 flexes. All tubes coated to date have at least 50% of the coating remaining after one flex. Figure IV-7 is a plot of the average

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Figure IV-7. Average Flex vs Lot Number for Cladding Tubes Coated for S8DR Fuel Elements (average 8 tubes per lot)

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TABLE IV-6
S8DR HARDWARE STATUS

Item	Number Fabricated	Number Accepted	Yield (%)
End caps	741	737	99.5
Blind-end weld tubing	736	655	89
Coated tubing	296	177	60
Cup-plugs	629	581	92.5
Coated cup-plugs	291	246	84.5
Fuel rods (normal + enriched)	361	305	84.5

flex for the eight tubes in the lot vs the lot number. The average flex for one lot of tubing dropped to three flexes and it was observed that the tube-coating firing-furnace was running about 10°F lower than normal. After the furnace temperature was corrected, the average flex returned to normal range.

2. Core and Reflector

a. Incore Instrumentation

The work during the quarter was concentrated on the assembly, brazing, and testing of bulkheads. A total of four bulkheads was assembled as listed below.

USE PLAN FOR BRAZED-BULKHEAD ASSEMBLIES

Bulkhead Assembly No.	Use
1	Final qualification of procedures for brazing and spacer-segment welding on S8DR hardware
2	S8DR thermal mockup (non-nuclear) test
3	Thermal-cycling tests
4	S8DR

The thermal-cycling test was run to demonstrate the durability of the brazed joints. The arrangement of the bulkhead in this test is illustrated in Figure IV-8. The testing was performed by (1) inserting the bulkhead into a

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Figure IV-8. Thermal-Cycle Testing Bulkhead Assembly Arrangement

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cold muffle furnace, (2) raising the temperature to approximately 1250°F, (3) withdrawing the bulkhead from the furnace, and (4) directing a compressed-air jet against the center of the upper bulkhead (at the location where Thermocouple 7158 was attached). The velocity of the air jet and its duration were adjusted to produce temperature gradients that would simulate those expected during transients in reactor operation. The resulting temperatures indicated by the attached thermocouples are illustrated in Figure IV-9. The calculated braze-joint stresses in this test were on the order of twice those expected in S8DR. The temperature traces shown are typical of those obtained in each of the 55 cycles to which the bulkhead assembly was subjected.

The bulkhead used for thermal-cycle testing was also used as the sample for final qualification of the welding procedures. All of the welding requirements were successfully met by this sample, with the exception of suckback of the underside of the bead. However, metallographic examination of weld cross-sections indicated that in no case was thinning of the bulkhead wall such that functional reliability of the assembly would be in question. The welding procedure was approved by project management. Brazed joints from the thermal-cycled bulkhead were metallographically examined. An example of the microstructure of typical brazed joints is illustrated in Figure IV-10. Nine brazed joints were examined, five from the upper bulkhead and four from the lower. Those from the upper included the central gas tube, the most highly stressed joint in the assembly. No cracking was found in the main body of the braze. In some cases small cracks were observed in the fillet area of the brazed joints. These were similar to cracks previously observed in other bulkheads that were not subjected to thermal-cycle treatments. In no case was any evidence of crack propagation found that could be attributed to thermal cycling.

b. Reflector Assembly and Controls

Various minor design modifications of the reflector assembly were incorporated. These were designed to eliminate deficiencies and marginal performance encountered during the previously reported⁽¹⁾ design-verification test on the first reflector-half assembly.

Figure IV-9. Test Temperatures for Cycle No. 51 in Thermal-Cycle Testing of Brazed S8DR Qualification Thermocouple Bulkhead. (Locations of the thermocouples are shown in Figure IV-8. Note that the time scale is reversed from the conventional presentation for convenient use of the recorder-strip chart sections.)

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(45X)

a. TYPICAL OF MINIMUM FILLET OBTAINED

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(45X)

b. TYPICAL OF AVERAGE FILLET AND CLEARANCE

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Figure IV-10, a and b. Photomicrographs of Thermocouple-Bulkhead Brazed Joints
Illustrating Features Found After 55 Thermal Cycles
(Sheet 1 of 2)

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(45X)

c. TYPICAL OF CRACKS RESULTING FROM ACCIDENTAL
BENDING OF THERMOCOUPLES

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8165-4-3

(45X)

d. TYPICAL OF OCCASIONAL CRACKS CAUSED BY NON-
UNIFORM SHRINKAGE OF THE INHOMOGENEOUS
ALLOY IN THE FILLET AREA

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Figure IV-10, c and d. Photomicrographs of Thermocouple-Bulkhead Brazed Joints
Illustrating Features Found After 55 Thermal Cycles
(Sheet 2 of 2)

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Retainers were added to position the snubber-shaft bushings firmly within the snubber housing. The snubber-shaft-to-bushing clearance was also increased to minimize possible seizure. The modified snubbers were acceptance-tested by subjecting each unit to 10 control-drum scrams from the full-in position while the snubber was heated to 950°F. The snubbing action and the latching of the snubber shaft in the depressed position were observed during each scram cycle.

Securing of the cam which operated the limit switches at the end of the scram-spring rewind cycle was modified to eliminate any possible interference with the switches. The control-drum full-out limit-switch actuating lever was reworked to maintain the switches in the ON state throughout the drum rotation in the snubbing zone. The beyond-full-out limit switch on each drum was changed to make it a redundant full-out limit switch. (The previous intent of the switch was to signal that the snubber shaft had started to depress during the snubbing portion of the scram cycle.) Only one of these redundant switches and the end-of-the-scram-spring-rewind will be electrically connected initially when the S8DR is installed in the Ground Prototype Test Facility. The unconnected spare limit-switch will be truly redundant because development tests have demonstrated that the degradation of the limit-switch insulation-resistance occurs only under electrical potential.

To utilize the P5 carbon-graphite vs Al_2O_3 bearing-material couple recommended in Section III, a new control-drum bearing-ball was designed and fabricated. Because of the difference in the thermal-expansion characteristics between the P5 carbon-graphite and the Inconel-X of the socket and shaft, it was necessary to increase the overall diametral maximum clearance of the bearing from 0.004 to 0.0122 in. As can be seen on Figure IV-11, this increased clearance is a compromise which retains the 0.0015-in. minimum clearance at both the shaft-to-ball and ball-to-socket interface. A new set of control-drum bearing-shafts coated with TiC is also being fabricated for backup. The bearing clearance of this backup design is the same as the original design.

The automatic controller was redesigned to eliminate the mechanical relays in the output stage which malfunctioned several times during the previous test. The automatic controller will now operate in conjunction with the S8DR

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Figure IV-11. Modified S8DR Control-Drum Clearances

manual controller to take advantage of both their current-limiting actuator power-supplies and the solid-state output switches. The integrated design is shown schematically on Figure IV-12. The current limits on the actuator power-supplies are required to prevent excess power to the coils of the modified actuator during low-temperature operation. The solid-state output stage-switches are not only more reliable but also give clean positive switching. The latter feature is important to the proper operation of the actuator step-counting precision position-indicator of the control drum. Some malfunctions of the step-counter noted during the last test were traced to contact bounce of the mechanical relays used as the output stage-switches in the automatic controller. Other improvements have been incorporated into the precision position-indicator to improve its reliability and performance.

The final assembly of the reflector assembly in the clean room was initiated. Most of the mechanical components have been installed as can be seen in Figure IV-13.

D. FACILITY AND TEST PREPARATIONS

1. Test-System Installation and Checkout

The insulation of the primary and secondary heat-transfer system was completed. Following completion of the insulation the final termination of all primary and secondary system-instrumentation was accomplished. Checkout and calibration of the process-system controls and instrumentation were started. This work was in the final stages of completion at the end of the report period.

The piping, valving, and support structure for the remote NaK-sample station was installed.

The emissivity coating of the vacuum vessel was completed in preparation for installation of reflector harness-wiring and the mockup reactor vessel for the non-nuclear test. The -Z and the inlet-outlet thermocouple harnesses were installed and checked out. The core-vessel reflector-assembly and 3-in. piping inside the vacuum vessel were fitted up. Welding of the 3-in. piping was completed. Welder and weld-procedure qualifications were completed for the final closure weld between the reactor head and vessel.

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Figure IV-12. Automatic-Manual Controller Integration

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Figure IV-13. Zirconium-Hydride Reactor (S8DR) Reflector Assemblies

The facility cooling-water-tower modification was completed. This modification replaces the open-loop system with a closed-loop distilled-water system. This eliminates the collection of dirt in the cooling-water system and will increase facility reliability.

2. Remote-Handling and Viewing Systems

The modifications to the Building 059 tool-transfer-lock required to accommodate the NaK-sampling system were completed. These modifications included the installation of a valve which will allow maintenance of vault atmosphere while removing a NaK sample. One vault shield-plug was also modified to

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permit easier access to the vault for the remote NaK-sample system. A shielded transfer container for the NaK sample-chamber was also fabricated.

The television console was installed in Building 059 and checkout of the TV system was started.

3. Preoperations

Draft versions of the system and facility operations-manuals were issued for review and use during the non-nuclear checkout. Phase I of the operator-training program was completed. This phase was concerned with preparing operators for operation of the non-nuclear test.

4. Test Planning and Procedures

The non-nuclear test plan was approved and issued. Drafting of the nuclear test plan was started. Test procedures for the non-nuclear test were prepared.

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V. REACTOR STUDIES: THERMOELECTRIC SYSTEMS

A. SUMMARY

A major objective of this task is to provide a reactor system based on S8DR technology which offers the greatest flexibility of application from the standpoint of mission, power level, thermal conditions, and lifetime. A second objective is to integrate the reactor system with a nominal 25-kwe thermoelectric power-conversion system to obtain maximum performance in terms of reliability, lifetime, and power-to-weight ratio.

In the reactor system area the following key design and analytical areas for a 4π shieldable 295-element core were identified:

- 1) Determination of the drum-controlled reactivity requirements, including the use of an incore burnable poison.
- 2) Selection of a reflector, drum, and core design to meet the control requirements.
- 3) Temperature control of the reflector and drum assembly in the 4π shield environment.
- 4) Temperature control of the 4π shield.
- 5) Characterization of the reactor core performance under varied operating conditions.

Results of work in these areas follow.

Thermoelectric module performance data were obtained from Westinghouse and parametric studies performed to determine the best design point. Major variables of the analysis were number of modules, size, operating temperatures, efficiency, converter pressure drop, and radiator-area requirements. Detailed results are presented in Section V-D.

B. CORE ANALYSIS

The principal activity during this reporting period was the definition of the capabilities and characteristics of the 295-element reference design zirconium-hydride reactor. The items specifically investigated were:

- 1) Operating-lifetime capability.
- 2) Excess-reactivity requirements.
- 3) Burnable-poison specification.
- 4) Reactivity-lifetime behavior
- 5) Control-drum material selection

1. Operating-Lifetime Capability

The design lifetime for the reference design reactor has been defined as the time at which the core is calculated to reach the fuel-phase-change limit, the fuel-growth limit, or the reactivity limit, whichever occurs first. The diametral fuel-to-cladding gas gap within the fuel element has been sized to provide for the simultaneous achievement of the fuel-growth fuel-phase-change limits. The reactivity limit has then been examined, and the system design specified to provide a reactivity limit comparable to the fuel-growth fuel-phase-change limit.

The fuel-growth limit was based on the calculated probability of 0.5% that a fuel element would fail (cladding crack) within the core during the operational lifetime. The fuel-growth correlation used was that revised by Birney for ZrH_x -U fuel, as follows:

$$\% \frac{\Delta V}{V} = 2b + (2.2 \pm 0.60) - e^{-18,000(1/T - 1/1860)}$$

While it was recognized that this equation was not the final correlation, it was expected that the final fuel-growth correlation would predict approximately the same magnitude mean growth with a reduced uncertainty.

The growth-limit calculations assumed the use of Hastelloy-N cladding. The strain-to-failure value used for the Hastelloy-N took into account the NaK and irradiation environment and the localized temperature distribution in the cladding.

The fuel-phase-change limit (first appearance of β -phase ZrH_x) was based on the calculated occurrence of phase change in 0.5% or less of the elements in the core at the lifetime limit. The hydrogen-leakage model used in the analysis was based on the hydrogen-leakage data from the preproduction qualification

testing of 20 S8DR fuel elements. The data indicated superior hydrogen-retention properties when compared with the previously used data from the Special Seven S8DS fuel elements. However, the 20-element data involved only approximately 1000 hr of endurance testing upon which to base the time variation of K_D .

The phase-change calculations were made using the K_D correlation based on inclusion of 99.5% of the fuel elements at the 90% confidence level. The correlation based on the mean value of K_D was used for the reactivity-loss calculations. It was assumed that the time-dependence was the same for both cases. A comparison of these two models with the previously used model based on the mean of the Special Seven is shown in Figure V-1.

The calculated fuel-phase-change fuel-growth lifetime limits as a function of power level and inlet temperature are shown in Figure V-2. The flow profile had been optimized for 600 kw 1100°F-inlet 1300°F-outlet operation. The diametral gas gap was optimized to provide for simultaneous occurrence of the phase-change and growth limits. The optimized diametral gas gap varied from approximately 7 to 10 mils, depending on the power level and the coolant temperature.

2. Excess-Reactivity Requirements

The relatively large size of the 295-element core assures that adequate unpoisoned excess reactivity exists to more than compensate for the reactivity losses during operation to the phase-change fuel-growth limits. However, the relatively large size of the core also has the effect of decreasing the control-drum worth. It thus became evident very early in the study that the use of a burnable neutron prepoin material in the core was required to reduce the control requirements to obtain sufficient design and operational margins. The cold unpoisoned excess reactivity for a typical case was calculated to be \$18.00 for a core with a 4-1/2-in.-thick reflector with drums composed of 3 in. of BeO and 1-1/2 of B_4C as a poison backing.

Reactivity losses which occur during operation are divided into two categories for calculational convenience. They are: short-term reactivity losses, including the temperature and power defects, xenon buildup, and hydrogen redistribution; and the long-term losses including buildup of samarium-149 and

Figure V-1. K_D vs Time

Figure V-2. Phase-Change Fuel-Growth Lifetime Limits

other fission products, fuel burnout, and hydrogen loss. The short-term losses occur during the initial phases of operation and are generally complete after about 3 days of operation at power with 1300°F-outlet coolant. The long-term losses occur throughout the operating lifetime with the exception of the Sm-149 which reaches approximately 95% of its equilibrium concentration after about 15 months. Typical calculated losses for operation at 600 kwt, 1100°F-inlet, and 1300°F outlet are \$4.20 for the short-term losses and \$7.95 hot-operating loss to the fuel-phase-change fuel-growth limit. Thus total operating reactivity losses are \$12.15.

Finally, provision must be made for some reactivity contingency. For this reactor provision was to be made to allow for operation with one control drum in the least reactive (poison-in) position.

3. Burnable-Poison Specification

It was desirable to use a poison material which burns out of the core at a rate as nearly equal as possible to that of the long-term reactivity loss. This was required to minimize the reactivity swing during lifetime. An additional consideration was to use a poison which would result in a larger negative worth when the core was water-flooded than when it was in the operational mode. (That constraint was imposed because of the desirability of intrinsic water-immersion subcriticality before operation.) The selection of gadolinium-155 as the burnable poison was acceptable from the standpoint of both the selection criteria. Natural Gd was not acceptable because of the large absorption cross-section of Gd-157.

The effective cross-section for the Gd-155 is a function of the localized Gd concentration. Since the Gd will almost certainly be included in the core in localized areas of the fuel elements (e.g., in a thin foil between the fuel and the cladding), it is necessary to consider its self-shielding effect. The effective absorption cross-section is linear with Gd-155 worth for a 2.5-mil-thick foil between the fuel and the cladding. The relationship is:

$$\sigma_{\text{eff}} (\text{Gd-155}) = 590 - 16\rho \quad ,$$

where σ is the absorption cross-section in barns and ρ is the Gd-155 worth in dollars. The expression is valid for Gd-155 loadings up to \$15.

As the initial Gd-155 prepoison loading burns out during reactor operation, the effective σ_a increases and the Gd-155 concentration decreases faster than it would have if the cross-section has remained constant at its initial value. However the Gd-155 worth decreases at a slower rate than would be expected based on the assumption of an unchanging σ_a . The Gd-155 worth is proportional to the product of the Gd concentration and the effective cross-section. Although the burnout rate is continually increasing as the Gd is decreased, the Gd worth per atom is increasing because of the increasing absorption cross-section. Consequently, the rate of change of the prepoison worth is decreased (see Figure V-3).

Two additional items must be considered as a result of the choice of Gd as the prepoison material. Both occur as a result of the neutron-spectrum shift with Gd in the core.⁽¹⁾ First, the initial temperature defect is reduced because of the presence of Gd-155 in the core. Second, the worth of the Gd in the core is reduced when going to operating temperature. Both effects must be considered when calculating the reactivity swing during the operating lifetime.

The initial Gd loading was selected to provide the smallest practical reactivity swing during the operating lifetime of the reactor while staying within the limits of the reactivity bounds. Study showed that a hot Gd-155 loading of \$10.00 would serve very well over a variety of power and temperature ranges.

4. Reactivity-Lifetime Behavior

The swing in the reactor excess reactivity as a function of operating time was investigated for a number of power- and operating-temperature conditions. A hot Gd loading of \$10.00 (\$12.05 cold worth) and an initial cold-poisoned excess reactivity of \$5.25 were assumed for all the cases. It was also assumed that ten poison-backed control drums were used, providing a total control worth of \$12.00 and a maximum stuck-drum worth (due to drum interaction effects) \$1.50. The results of the study are shown in Figures V-4 through -6.

The effect of the hot-to-cold-reactivity swing and its increasing magnitude as the Gd prepoison burns out is shown in Figure V-7. It is significant to note that the point of minimum cold-shutdown margin would occur at about 25 months operation for the case under consideration rather than at the beginning of operation. The difference of approximately \$2.75 in the cold-shutdown margin could

Figure V-3. Effect of Variable σ on Gd-155

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Figure V-4. Reactor Excess Reactivity, $T_{IN} = 1100^\circ\text{F}$

Figure V-5. Reactor Excess Reactivity, $T_{IN} = 1000^\circ\text{F}$

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Figure V-6. Reactor Excess Reactivity, $T_{IN} = 900^\circ F$

Figure V-7. Reactor Excess Reactivity, $T_{IN} = 900^\circ F$
 $P = 900 \text{ kw}$

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be significant and must be considered when shimming the initial excess reactivity to obtain the desired shutdown margin.

5. Control-Drum Material Selection

Preliminary analysis of reactivity requirements indicated that a 3-in. -thick beryllium oxide reflector would provide sufficient excess reactivity for successful operation of the reference design zirconium-hydride reactor.

Previous analyses and experiments have shown that the maximum poison-backed control-drum worth results with a reflector-material thickness drum radius (T/R) ratio of 4/3 (i.e., 3-in. reflector and 1-1/2-in. poison).

A 4-1/2-in. total reflector thickness permits up to ten poison-backed control drums to be placed around the reactor core vessel. Extrapolation of critical experiment data and analyses with one-dimensional neutron-transport theory both indicate that the worth of ten 4-1/2-in. -thick BeO control drums with 50% dense boron-carbide backing was $\$12.00 \pm 2.00$. Poison-backed control drums were chosen for analysis because the reflector was designed to be used for both 4π - shielded and shadow-shielded applications. Void-backed control drums are not effective inside a 4π shield.

A survey was made to determine the poison backed control-drum worth for various poison materials on a BeO drum. Three potential poison materials were evaluated in the survey:

- 1) Boron, natural and enriched
- 2) Europium oxide
- 3) Tantalum-10 wt % tungsten (Ta-10W).

Other potential poison materials were rejected because of poor physical properties at the high temperatures encountered in the 4π configuration.

Table V-1 shows the relative worth of ten poison-backed control drums for various poison-backed materials. The table is normalized to the 50%-dense B_4C -backed drum worth of $\$12.00$. The tabulation is for ten control drums with BeO T/R = 4/3.

The table shows that enriched B^{10} in graphite provides the largest drum worth. Boron in graphite and B_4C have a significant disadvantage in their

TABLE V-1
SUMMARY OF CALCULATED WORTH OF CONTROL DRUMS
WITH VARIOUS POISON-BACKING MATERIALS

Poison-Backing Material (T/R = 2/3)	Worth of Ten Control Drums (\$)*
100%-dense B ₄ C	12.55
40%- boron in graphite	10.50
40%-enriched (92% B ¹⁰) boron in graphite	15.10
93%-dense Eu ₂ O ₃	11.75
60% Eu ₂ O ₃ in stainless steel	10.15
60% Eu ₂ O ₃ in nickel	10.05
60% Eu ₂ O ₃ in stainless steel with 25% hole fraction, holes filled with 93%-dense Eu ₂ O ₃	10.56
Ta-10W	10.70
Ta-10W with 25% hole fraction, holes filled with 93%-dense Eu ₂ O ₃	10.96

*Normalized to \$12.00 worth for 50%-dense B₄C backing, for drums 4.5 in. in diameter,
T/R = 4/3

production of helium gas through the (n, α) reaction. Gas production presents a potential fuel-growth problem and at the least requires venting of the gas from the control-drum cladding can.

Ta-10W is a very dense material and is considerably heavier than the other poison material candidates.

Eu₂O₃ compacted with Ni or stainless steel offers structural strength at high temperatures and absence of gas production. Accordingly it was chosen for the control-drum poison backing. B₄C was chosen as the backup material on the basis of its greater worth, if the problems of gas production can be overcome.

Control-drum worth increases as the reflector thickness increases. Table V-2 shows that each integral number of control drums corresponds to a

TABLE V-2
CONTROL-DRUM WORTH FOR VARIOUS NUMBERS
OF DRUMS AND DRUM DIAMETERS

Number of Drums	Maximum Drum Diameter (in.)	Total Drum- Worth Factor	Available Drum- Worth Factor*
10	4.5	1.00	1.00
9	5.3	1.14	1.11
8	6.5	1.27	1.19

*Assumes one drum stuck full-out, one full-in.

specific reflector thickness. Total drum-worth increases significantly for the 9- and 8-drum reflectors. However, the consequence of control drums sticking at any position becomes more critical when the number of drums is reduced.

Assuming that one control drum is stuck full-out and another stuck full-in; an "available" drum-worth factor was calculated, as is shown in the table. The maximum drum worth available with 8 is 19% greater than that with 10.

Further analysis of the effects of poison-material shape on the drum, reflector cusp-piece shape, and axial streaming in reflector voids is required before fixing the reference reflector design.

C. REFLECTOR ANALYSIS

Analyses were conducted to evaluate temperature conditions that exist in poison-backed control drums. Heat-generation rates were first determined, then used to calculate temperature distributions.

1. Heat-Generation Rates

Heat-generation rates were calculated based upon the model shown in Figure V-8. Radiation from both primary and secondary sources was included. The primary sources include the core gamma rays (prompt fission, delayed fission-product, and neutron-capture) and core neutrons; the secondary sources include gamma rays resulting from neutron capture in the Eu_2O_3 poison and W shield. The results of these calculations are given in Figures V-9 and -10,

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Figure V-8. Heat-Generation Model

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Figure V-9. Energy Absorption Rates in BeO and Eu₂O₃, Reflector-In (600 kwt)

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Figure V-10. Energy Absorption Rates in BeO and Eu₂O₃, Reflector-Out (600 kw)

which show the energy-absorption rates in the BeO and Eu_2O_3 as a function of thickness for drums-in and drums-out, respectively. These values are at the reactor midplane, for a thermal power at 600 kw.

2. Temperature Distribution

Based upon the energy-absorption rates in Figures V-9 and -10 steady-state temperatures in the control drums were calculated with the TAP-4 code. Two basic cooling configurations were analyzed, Figure V-11, each using ten control drums. Also shown in Figure V-11 is the assumed control-drum geometry. Temperature calculations were performed for both the reflector-in (BeO) and reflector-out cases. The results of these cases are given in Table V-3.

TABLE V-3
REFLECTOR TEMPERATURES, CORE MIDPLANE

Power Level (kw)	Cooling Concept	Drum Position	Maximum Temperature (°F)		
			Beryllium Oxide	Poison	Can
600	Annular	Reflector in	1445	1388	1318
600	Fin-tube	Reflector in	1446	1391	1330
1200	Annular	Reflector in	1700	1635	1476
1200	Fin-tube	Reflector in	1770	1637	1483
600	Annular	Reflector out	1485	1582	1293
600	Fin-tube	Reflector out	1479	1567	1358
1200	Annular	Reflector out	1757	1879	1432
1200	Fin-tube	Reflector out	1731	1851	1543

Temperatures are for heat-generation rates at the core midplane and do not assume any axial heat conduction. It can be noted from Table V-3 that comparable temperatures exist in both the configurations analyzed. The fabrication and development problems associated with the annular-cooling configuration were considered to be less difficult; this concept, therefore, was selected for additional evaluation.

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Figure V-11. Control Drum Cooling Thermal Model

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D. THERMOELECTRIC-SYSTEM ANALYSIS

The primary purpose of this task is to investigate system and subsystem designs covering a variety of applications. This includes the following specific objectives:

- 1) Establish reference converter-module design.
- 2) Evaluate various system hydraulic and electrical configurations.
- 3) Generate component parametric data.
- 4) Establish reference-system designs for several missions.

During this phase primary emphasis was placed on the selection of a reference-module design. Because module performance directly affects system size, weight, efficiency, and reliability, selection of the best module design must be based on system as well as converter considerations. Therefore an analytical model typical of a 25-kwe reactor thermoelectric system was used in a parametric study to determine the influence of converter-module design on system design. Figure V-12 is a schematic of the reference system, which has a single primary loop, four converter sections, and four heat-rejection loops (HRL's). Four across-the-line pumps provide necessary NaK flow. Table V-4 defines the parameters investigated and their range of variation. The 5-kwe difference between the 30-kwe (beginning-of-life) and 25-kwe (end-of-life) power accounts for system degradation and power-conditioning losses.

Converter parametric data were generated by Westinghouse, the converter subcontractor, for the range of parameters shown in Table V-4. Figure V-13 shows how module weight varies with outer and inner diameters. The weights shown include end-closure weights but do not include NaK, NaK containment, or manifolding.

From Figure V-13, a module having a 3/4-in. ID and 1.5-in. OD weighs 5.2 lb. Adding the liquid-metal component weight increases the module weight to approximately 8.0 lb.

Figure V-12. Reference System Schematic

TABLE V-4
REFERENCE-SYSTEM PARAMETERS

Power, kwe: gross	30 (beginning-of-life)
net	25 (end-of-life)
Converter temperatures, °F, average	
Hot junction, T_H	1000, 1100, and 1200
Cold junction, T_C	400, 500, 600, and 700
Module dimensions, in.	
Outer diameter*	1.2, 1.5, 2.0, and 2.5
Inner diameter	3/8, 1/2, and 3/4
Active length	15

*Excluding NaK enclosure pipe

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Figure V-13. Parametric Weights of Tubular Modules

Figure V-14 shows converter performance for a 0.75-in. ID module at $T_H = 1100^\circ\text{F}$, as a function of module OD and T_C . Similar data were also provided at $T_H = 1000$ and 1100°F and ID = 3/4 and 1/2 in. The lower limit on OD for a given ID is dictated primarily by fabrication. As module OD increases, the power output decreases and the efficiency increases (see Figure V-14).

These converter data were combined with other component parametric data and used to determine system weight, size, and performance. Figure V-15 shows a typical curve of unshielded system weight as a function of area for a range of T_C 's and T_H 's at an OD = 1.5 in. and ID = 0.75 in. Similar curves were also obtained at the other ID's and OD's investigated. For each ID, OD, and T_H , there is a minimum weight point. Figure V-16 shows a plot of these minimum weight points as a function of module OD and T_H for a constant ID of 0.75 in. Figures V-17, -18, and -19 show area, efficiency, reactor power, and module quantities as a function of module OD. Area, weight, and module quantities are minimum at minimum module OD. Using minimum OD as the optimum, the results shown in Figures V-20 through -23 were obtained.

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Figure V-14. Converter Performance

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Figure V-15. Influence of Radiator Area on Unshielded System Weight

Figure V-16. Influence of Outer Diameter on Unshielded System Weight (minimum weight)

Figure V-17. Influence of Module Outer Diameter on Radiator Area

Figure V-18. Reactor Power and System Efficiency

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Figure V-19. Module Quantities
(minimum weight)

Figure V-20. Unshielded System
Weight (minimum weight, opti-
mum outer diameter)

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Figure V-21. System Radiator Area (minimum weight, optimum outer diameter)

Figure V-22. Reactor Power and System End-of-Life Efficiency (minimum weight, optimum outer diameter)

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Figure V-23. Module Quantities (minimum weight, optimum outer diameter)

Figures V-20 through -22 show that system weight, area, and efficiency are reasonably insensitive to module ID variations for minimum weight and optimum OD. However, Figure V-23 shows that module quantities are reduced as ID increases.

Figure V-24 is a summary curve which shows the influence of module OD and ID on minimum weight system characteristics for $T_H = 1100^\circ\text{F}$. Minimum unshielded system weight is nearly the same for all three ID's; however, it occurs at different OD's. For a given ID the minimum weight occurs at the minimum OD. Minimum radiator area occurs at ID = 3/8 in. and minimum OD. However, the difference in area between ID = 3/8 and 3/4 in. is only 100 ft². The most significant influence of converter design is reflected in module quantities. The minimum number of modules is required for an ID of 3/4 in. and an OD of 1.5. The 3/4-in. ID requires half the modules required at 3/8 and 30% fewer than the 1/2. The real significance of fewer modules is that fewer NaK piping welds are required and manifolding problems are reduced. This should result in a significant gain in reliability. An efficiency penalty, however, associated with large ID's and small OD's is not too important except in those applications where the system is reactor-power limited.

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Figure V-24. System Characteristics (minimum weight, $T_H = 1100^\circ\text{F}$)

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Based on these results, a reference-module preliminary design was selected having the characteristics shown in Table V-5. The maximum clad temperature of 1200°F was selected considering overall power-system performance, converter development and test experience to date, and reactor-lifetime capabilities.

TABLE V-5
REFERENCE-MODULE CHARACTERISTICS

Temperatures, °F	
Average hot clad	1125
Hot-side coolant ΔT	150
Average cold clad	570
Cold-side coolant ΔT	150
Dimensions, in.	
Active length	15.0
Outer-clad OD	1.50
Inner-clad ID	0.75
Voltage, volts	14.0
Power (electrical BOL), watts	260.0*
Efficiency, % (BOL minimum)	4.7*
Thermal power, kwt	5.54*
Carnot efficiency, % (based on average clad temperatures)	35
Weight (wet), lb (including jacket)	8.0

*Including end losses

VI. AEROSPACE SAFETY

A. INTRINSIC-SUBCRITICALITY ANALYSIS

Preliminary calculations of gadolinium-155 worth in the SNAPTRAN-1 reactor core were reported in the previous SNAP Reactor Progress Report.⁽¹⁾ The calculations showed that the prompt fuel coefficient would become positive when the 2200 m/sec $\Sigma_a t$ of Gd-155 exceeded 0.27. The calculations were for 2-mil foils wrapped around SNAPTRAN fuel elements. A specification has been written which calls for foils to be prepared with Gd contents as follows:

Foil No.	$\Sigma_a t$ of Gd-155 (2200 m/s)	Gd ₂ O ₃ /cm ² (gm)	Gd ₂ O ₃ /in. ² (gm)
1	0.00	0.00	0.00
2	0.05	2.8×10^{-4}	1.8×10^{-3}
3	0.10	5.6×10^{-4}	3.6×10^{-3}
4	0.20	1.12×10^{-3}	7.2×10^{-3}

These foils will be tack-welded into sleeves which can be slipped on and off the fuel elements. They will be used only one set at a time (no laminating). Foil overlap for welding will be about 0.15 in. Foil length will be 12.40 ± 0.05 in.

A verification of the accuracy of the analytical methods used to calculate Gd worth has been made. An experimental measurement of the worth of natural Gd (essentially Gd-157) was made in 1965. The Gd was applied to tape which was then glued onto SNAP 10A fuel elements. Worth measurements were made in the SCA-4A critical assembly (dry) and in the SCA-4B critical assembly (water-flooded and -reflected). An analysis of these experiments was recently made using the same techniques, models, and nuclear cross-sections which have been used to predict Gd-155 worth in the current experimental program. The calculated and experimentally measured Gd worths are compared in Figure VI-1. The operational-configuration calculated Gd worth was within 7.5% of the measured worth. The calculated water-flooded Gd worth at $\Sigma_a t = 0.1$ was within 2% of the experimental worth. The experimental water-flooded worth, which was measured at $\Sigma_a t = 0.2$ is believed to be in error for three reasons:

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Figure VI-1. Comparison of Calculated Gadolinium Worth to SNAP Critical Assembly Experiments

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- 1) The excess reactivity of the assembly was measured by determining the worth of poison splines inserted into the coolant channels. The composition of the core was therefore changed (by reduction in the number of splines) as Gd was added to the fuel. The error introduced by this technique was a maximum in the $\Sigma_a t = 0.2$ case because nearly all the splines were removed to make the critical measurement.
- 2) The measured worths show no indication of self-shielding effects, whereas two independent methods of analysis (and the operational-configuration worth measurements) indicate that self-shielding should be expected. Self-shielding results in a reduction in worth-per-atom as concentration is increased.
- 3) The change in Gd worth due to the change in neutron-energy spectrum resulting from water-flooding should be nearly independent of concentration. The experimental worth at $\Sigma_a t = 0.1$ changes by 42%, while the measured-worth change at $\Sigma_a t = 0.2$ was 67%. The calculated-worth change due to water-flooding was 30%, and was essentially independent of concentration, as anticipated. It was concluded that the current analytical methods are satisfactory for prediction of experimental results.

Establishment of the isotopic content of the Gd-155 which will be purchased from Oak Ridge for use in the experimental program has been made. The at. % of each Gd isotope is shown in Table VI-1. The absorption cross-section of Gd isotopes other than -155 and -157 is negligible. The percentage of parasitic absorptions which are expected from Gd-157 is only 4.8%. The unidentified

TABLE VI-1
COMPOSITION OF GADOLINIUM

Isotope	Atomic Percent	Absorption Percent	Isotope	Atomic Percent	Absorption Percent
Gd-152	<0.05	0.0	Gd-157	1.13	4.8
-154	0.61	0.0	-158	0.94	0.0
-155	91.77	95.2	-160	<0.05	0.0
-156	5.14	0.0	Other	~0.31	Unknown

0.31 at. % of the Gd is most probably uncertainty in the content of the primary isotopes Gd-155, -156, -157, and -158. It is possible that Sm-154, Eu-153, and Dy-156 may be included in the "other," of Table VI-1, but they all have relatively small absorption cross-sections compared to Gd-155. It was concluded that impurities in the Oak Ridge Gd-155 will not adversely affect the spectral changes anticipated in the experiments. The Gd-157 effect on worth will be accounted for in analysis.

B. INTRINSIC-SUBCRITICALITY EXPERIMENTS

The preparation of the Safety Analysis Report for the operation of the SNAP-TRAN reactor at the SNAP Engineering Test Facility (SETF) was completed.⁽⁹⁾ The report was reviewed and approved by the internal safety committee and was published and sent to the AEC for their review. The test plans for the water-immersion and high-temperature experiments were prepared, approved, and distributed. Test procedures for these experiments were prepared and are being reviewed at this time.

Work was begun on the installation of the SNAPTRAN reactor in the SETF. The electrical-wiring diagrams are being prepared and work on the procedures for the mechanical installation and checkout was started. Dimensional checks were made on the core vessel and fuel elements for the SNAPTRAN reactor. The excessive bow and out-of-roundness of the fuel elements dictate an increase in fuel-element lattice-spacing from 1.260 to 1.270 in. The core vessel is also out of round and must be corrected to accept the fuel elements.

C. GADOLINIUM

The three foils ordered to provide material for development of foil-attachment procedures were measured for dimensional uniformity and checked for distribution of Gd_2O_3 content by metallographic and x-ray emission techniques. The foils, including a roll of pure Ni foil, were found to vary from 2.39 ± 0.21 to 2.47 ± 0.13 mils in average thickness and from 3.9986 ± 0.0031 to 4.0035 ± 0.0018 in. in width. Samples of the foil taken parallel as well as perpendicular to the axis of rolling showed the Gd_2O_3 particles to be uniformly dispersed in the Ni as shown in Figure VI-2. Analyses of the foils by solution x-ray emission showed the Gd_2O_3 content to be 33 to 59% higher than target

Figure VI-2a. Microstructure of Ni and Ni-Gd₂O Foils
(microphotographs taken parallel to roll)

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Figure VI-2b. Microstructure of Ni and Ni-Gd₂O Foils
(microphotographs taken perpendicular to roll)

values. This is in part due to the foils being 15 to 30% thicker than specified and the vendor's calculations being incorrectly based on additions of Gd metal instead of Gd_2O_3 . The vendor claims a capability of holding a specified Gd_2O_3 content within ± 5 wt % and specified thickness to within ± 0.0002 in., a capability yet undemonstrated.

A technique was developed for fabricating foil sleeves using a graphite mandrel (1.260 in. in diam) and spot welding. Nine sleeves were fabricated to demonstrate the technique, using pure Ni and Ni foils bearing 0.0011 and 0.00442 gm Gd_2O_3 /in.² These sleeves were slipped on and off five SNAPTRAN fuel elements with no difficulty. The dimensions of these elements were representative of those anticipated in the reactor core.

D. SHIELD ENGINEERING ANALYSIS

The effect of calculational uncertainties on SNAP reactor shield weight was estimated. The purpose of this study was to quantitatively assess the potential weight savings associated with reductions in current analytical uncertainties. The study covered both the manrated shadow (directional protection) and 4π (omnidirectional) shield configurations shown in Figure VI-3. The assumed mission profile is given in Table VI-2.

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Figure VI-3. Shield Configurations Studied

TABLE VI-2
SNAP REACTOR MANNED-MISSION PROFILE

Shield Type	Shadow Shield	4π Shield
Reactor power, kwt	600	600
Reactor envelope at core midplane, in.	20	20
Reactor fuel length, in.	20	20
Gallery thickness, in.	18	18
Doseplane diameter, ft	60	60
Doseplane separation distance, ft	150	150
Doserate at doseplane, mrem/hr	3	3
Doserate at 100 ft, radially, rem/hr	-	100

Tables VI-3 and 4 present current appraisals of uncertainty in the major sources of radiation. In many instances these data are no more than intelligent estimates. Better definitions of these probable uncertainties were purposely deferred, however, since the objective of the study was to indicate which of the areas of uncertainty deserved more attention. Thus it was decided to postpone a more precise evaluation of these uncertainties until the results indicated that a significant weight reduction could be realized.

The normal curve was used to represent the frequency (logarithmically) of the measured doserate about the design doserate. The latter was determined by the probability that the measured doserate is no greater than the specified radiation level. (This probability is 50% for the case in which the design doserate is set equal to the specified value.)

Figures VI-4, -5, and -6 show the resulting estimates of the shield-weight penalty required to increase this doserate penalty above the 50% level. Individual curves represent the differential weight increase due to each source of radiation. The "knees" in the secondary-gamma curves of Figures VI-4 and -5 correspond to the points at which the thickness of the first gamma shield (T_1) adjacent to the core vanishes. (The portion of the secondary-gamma curve to the left of the knee is associated with $T_1 = 0$.)

Figures VI-7 and -8 illustrate the effect on shield weight of different values of uncertainty in the secondary-gamma contribution to the total doserate. These

TABLE VI-3
ESTIMATED UNCERTAINTY IN RADIATION TRANSMISSION,
SHADOW SHIELD OR AXIAL PORTION OF 4π SHIELD

Radiation Source	Calculation Factor	Uncertainty Factor (one- σ correspondence)	Remarks
Neutrons	Relative biological effectiveness	3.0	Reflects uncertainties in biological damage effects
	Material attenuation	2.7	Corresponds to σ 's of 0.01 cm^{-1} in removal cross-section and 0.01 gm/cc in LiH density
	Geometry	1.5	Includes effect of angular distribution of reactor neutron-leakage
	Source strength	1.2	Includes consideration of neutron-scattering source in radial reflector
	Total	4.7	Based on logarithmic-probability distribution
Reactor Gammas	Material attenuation	1.9	Corresponds to σ of 0.04 cm^{-1} in effective attenuation coefficient of uranium
	Geometry	1.5	Includes angular-distribution effects
	Source strength	1.4	Includes uncertainty in reactor-leakage fraction
	Total	2.3	
Activated NaK	Material attenuation	1.5	Corresponds to σ of 0.04 cm^{-1} in effective attenuation coefficient of uranium
	Geometry	1.5	Includes uncertainty of physical distribution of NaK components in gallery
	Source strength	1.5	Reflects uncertainties in NaK activity, NaK inventory, and components' shielding of source
	Total	2.0	
Secondary Gammas	Material attenuation	1.4	Primarily reflects uncertainty in calculation of neutron distribution in shield
	Geometry	1.5	
	Source strength	4.0	
	Total	4.4	

TABLE VI-4
ESTIMATED UNCERTAINTY IN RADIATION TRANSMISSION,
RADIAL PORTION OF 4π SHIELD

Radiation Source	Calculation Factor	Uncertainty Factor (one- σ correspondence)
Neutrons	Relative biological effectiveness	3.0
	Material attenuation	1.4
	Geometrical attenuation	1.2
	Source strength	1.1
	Total	3.2
Reactor Gammas	Material attenuation	1.4
	Geometrical attenuation	1.5
	Source strength	1.2
	Total	1.7
Secondary Gammas	Material attenuation	1.1
	Geometrical attenuation	1.2
	Source strength	2.0
	Total	2.1

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Figure VI-4. Shield Weight vs Dose Rate Probability,
Shadow-Shield Design

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Figure VI-5. 4π -Shield Weight vs Dose-rate Probability
Axial Shield Radiation Transmission

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Figure VI-6. 4π -Shield Weight vs Dose-rate Uncertainty
Radial Shield Radiation Transmission

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Figure VI-7. Shadow-Shield Weight vs Doserate Uncertainty
Secondary-Gamma Doserate

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Figure VI-8. 4π -Shield Weight vs Doserate
Uncertainty Secondary-Gamma Trans-
mission Through Radial Shield

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curves provide an indication of the potential savings in shield weight which may be realized by reducing calculational uncertainties.

Evaluation of results leads to the conclusion that the largest potential weight savings in manned SNAP-reactor shields can be realized by reducing the uncertainty in the calculation of secondary-gamma doserates. For example, consider the effect of a reduction of such uncertainty to 50% of the nominal value. This results in a 10% weight savings for a shadow shield which has been designed for a 99% probability of satisfying the specified doserate. A similar reduction in the secondary-gamma transmission through the radial portion of a 4π shield results in a 40% weight savings.

Earlier comparisons⁽¹⁰⁾ of analytical and experimental data indicate that the major source of inaccuracy in secondary-gamma doserate calculations is in the determination of the source strength. The prime requirements for a better secondary-gamma source-strength description appear to be improved calculations of the neutron-flux distribution within the shield and more comprehensive compilations of cross-sections and gamma spectra for secondary-gamma production. It is recommended that these earlier correlations of analytical predictions and existing secondary-gamma measurements be extended.

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