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SNAP PROGRAM
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PROGRESS REPORT
NOVEMBER 1970 - JANUARY 1971
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I. INTRODUCTION

A. PROGRAM SCOPE AND OBJECTIVES

The SNAP Program activities covered in this report include the following major tasks:

- 1) SNAP 8 Developmental Reactor (S8DR) Test
- 2) Reference Zirconium Hydride Reactor Engineering
- 3) Reactor Core Test
- 4) Reactor - Thermoelectric System Engineering
- 5) Compact Thermoelectric Converter Technology
- 6) Component Development
- 7) Fuel Development
- 8) Aerospace Nuclear Safety.

The S8DR Reactor Test effort includes all activities necessary for the installation, checkout, operation, test support, disassembly, and examination of the S8DR. The GFY 1971 objective consists primarily of completion of the post-test analysis of the S8DR.

The Reference ZrH Reactor Engineering effort provides preliminary engineering for a reference space reactor capable of broad range performance. The design must also be suitable for use with thermoelectric (TE), Brayton, or organic Rankine cycle power-conversion systems, and must be capable of operation in either a shadow-shielded or a 4π -shielded configuration.

The Reactor Core Test is being designed to demonstrate the satisfactory performance of the reference ZrH reactor core configuration. The objective will be to eliminate the observed S8DR problems of power/temperature oscillations, fuel element clustering, excessive fuel swelling, and hydraulic instabilities. In addition, the core will have a performance goal of five-year operation at 600 kw with a 1200°F outlet coolant temperature. The Reactor Core Test program includes those experimental and developmental efforts directly related to the design. The preliminary design is scheduled for completion in the last quarter of GFY 1971. Fabrication is planned for completion in mid-GFY 1973.

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The Reference Reactor - TE System Engineering effort includes a preliminary system definition and related application and integration studies.

The Compact Thermoelectric Converter Technology program is being carried out by the Astronuclear Laboratory of Westinghouse Electric Corporation under subcontract to Atomics International. This program is intended to develop the TE power-conversion subsystem to the extent of combining it with a ZrH reactor-TE space-power system.

The Component Development tasks include the development and testing of reflector components, shielding, and liquid-metal components in support of the S8DR, the reference ZrH reactor, and the Reactor-TE system development programs.

The Fuel Development tasks provide for basic understanding, substantiation, and improvements in the technology of ZrH-type fuel-moderator elements in support of the S8DR and ZrH reactor programs.

The primary effort under the Aerospace Nuclear Safety program is directed toward obtaining a preliminary parametric evaluation of the post-operational safety aspects of reactor power systems for typical unmanned space applications which can have mission lifetimes up to five years.

B. SUMMARY OF PROGRESS

Detailed examination of the S8DR core was continued during the quarter with profilometer measurements being completed on all the fuel rods selected for destructive examination. The profiles gave relatively good agreement with the n-ray data on intact elements; however, on cracked clad elements, the n-ray data were low when compared to those of the actual fuel diameter. Density analyses were completed on 22 selected fuel rods and comparison of the data with the diameter profile indicates that the fuel did swell isotropically. The hydrogen permeation system for measuring the leak rate of intact elements was fabricated, and checked out in the mockup area of AIHL.

A two-dimensional transport code, DOT II, was used to calculate the control worth of the ten-drum flight reactor. The total worth of drums containing natural B_4C cylindrical fillers was \$9.66 and a change to enriched boron-10 resulted in an increase in drum worth of $\sim 15\%$.

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The preliminary design of the core test reactor and the associated test facility was initiated. Detailed analytical modeling of the two-hydraulic-zone core with right, left, and nonfinned or "neutral" elements (RLN) assembled on a 0.670-in. pitch triangular array was performed. The effect of the induced cross flow caused by the RLN configuration is a 45% reduction of the θ ripple in the central core zone and a 25% reduction in the outer zone.

The reactor mechanical design activity concentrated on requirements related to the step-by-step assembly of the core test in the test fixture. Poison splines of B_4C in the coolant channels are used to maintain the necessary safety margin against inadvertent criticality of the preassembled core during assembly of the test. Based on temperature data information considerations, locations were selected for 40 thermocouples located at the coolant channel exits and 20 thermocouples within the fins of the central zone elements.

A reactor - thermoelectric power system capable of delivering 33-kwe gross electrical power was designed in support of the Space Station studies. The major constraint in the studies was that the system be capable of fitting into the 20,000-lb payload and 14-ft diameter by 50-ft-long envelope of the Advanced Logistics Shuttle (ALS). Also, the radiation dose rate received by the station crew from the reactor was not to exceed 30 mrem/day at a separation distance of 200 ft and dose plane diameter of 100 ft. As a result of these constraints, the reactor - thermoelectric power system was designed in two sections for separate ALS launches. One section contained the reactor, shield, and thermoelectric converters; the other section contained the radiator. In order to keep the weight of the reactor section within the launch weight capability of the ALS, a water-augmented neutron shield was required. Gross overall efficiency of the power system was $\sim 6\%$.

During the past quarter, primary emphasis was placed on the listing and evaluation of data from two identical reactor modules, TEM-X S/N -3A and -3B, fabricated with refractory inner claddings and thermoelectric circuits designed to maximize performance stability. The first module successfully completed 1167 hr of steady-state testing and 50 scheduled thermal cycles with no measurable effect on performance. Subsequently, this module was removed from test for post-cyclic test destructive examination. Module TEM-X S/N-3B

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has accrued 1900 hr of steady-state testing during which power output data have remained constant within 281 ± 2 watts. Two additional TEM-X S/N 3 series modules, designated C and D, were fabricated during the quarter. Module TEM-13G S/N-2 remained on test in the liquid-metal loop for over 11,500 accrued hours with the performance characteristics unchanged from previously reported trends. Pump modules TEM-14A S/N-1 and -2 continue on test with S/N-1 approaching 18,000 hr. Module S/N-3 was terminated after 7700 hr of endurance testing and fifty controlled thermal cycles with little change in performance. The rate of change in efficiency per 10,000 hr for the latter designed S/N-2 and -3 was between -0.4 and -1.6. Thermal conductivity measurements on the ternary P-type thermoelectric materials agree with previous literature values reinforcing the possibility that the use of this material will result in significantly improved module performance.

In the component development area, heaters are being fabricated for the 19-element flowing NaK heat transfer study, and modification of the test loop was completed. All actuators and coil tests were shut down in Building 5 and are to be reassembled in Building 013 at Santa Susana. The total test time on the actuator was 25,931 hr; 22,922 hr were at 1125°F , during which, operation remained normal. A high-temperature bearing consisting of a carbon graphite ball and alumina-coated Ta-10W sockets has successfully completed 16,350 hr at 1500°F and 10^{-8} torr. Fabrication of the drive train components and test fixture was completed. The drive system consists of the control drum and supporting bearings, a mechanical mockup of the actuator with locking cam and drive gears, and the drive shaft with flexible couplings.

Destructive examination of the large-diameter, thin, pancake shield was started in the disassembly dry box. Manufacturing drawings for the LiH annular shield were completed, and fabrication has begun.

Fuel element developmental efforts have indicated that a "reverse drain" coating application process results in a reduction of the hydrogen barrier coating taper inherent with the standard coating process, and approaches a uniform coating thickness of 2 mils. The vibration suppressor in the fuel element was relocated at the closure end of the element to allow the removal of hydride fuel away from the blend area. This was required to reduce heating and dehydriding

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of the fuel during the shrink-fit blend cycle where the resulting hydrogen pressure prevented a satisfactory closure seal. Seven 15-mil Incoloy-800 clad elements continued on test and have now accrued over 17,600 hr at 1400°F, with an average hydrogen loss of 7.8%.

Tubing from three new vacuum-melted heats of Incoloy-800 was received and is being evaluated in tensile, creep, and stress rupture tests. The creep strength of the high-carbon, high-titanium heat is significantly higher than those of the other two heats. Biaxial stress rupture properties of SCB-coated Incoloy-800 from the initial commercial heats (8105358 and HH7531A) have been measured over a wide range of conditions. Irradiated material from the 810535 heat exhibited diametral ductilities averaging 4% at 1300°F. There appears to be little loss in rupture strength or ductility for Incoloy-800 in flowing NaK.

A sandwich-type burnable poison film consisting of a base layer of Y_2O_3 , overlaid with co-deposited Eu_2O_3 , Sm_2O_3 , and Y_2O_3 , which was overlaid with a top layer of Y_2O_3 , was tested for compatibility with the glass hydrogen barrier and hydride fuel. These Y_2O_3 -stabilized, vapor-deposited films showed little change after 1350 hours at 1500°F.

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Figure II-1. Fuel Rod Profilometer Scan for Intact Element 389 (Core Position 2-6)

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II. SNAP 8 DEVELOPMENT REACTOR

A. DETAILED S8DR EXAMINATION

Examination activities at the Atomics International Hot Laboratory (AIHL) were continued during this report period. Fuel profilometer measurements were completed. Fuel sampling was started on selected fuel rods for density analyses, hydrogen analyses, and burnup samples. Permeation test equipment was designed, fabricated, and checked out prior to in-cell installation. Following is a summary of the activities completed during this reporting period.

1. Fuel Profilometer Measurements

Profilometer measurements were completed on all the fuel rods selected for destructive detailed examination. The profiles continued to give relatively good agreement between the n-ray data which is sensitive to hydrogen concentration and the fuel profiles on the intact elements. On the cracked clad elements, the n-ray data were low compared to those of the actual fuel diameter. A few fuel rods, such as those from core positions 2-6 and 5-1, indicated large diameter changes in the bottom 4 in. of the rods as shown in Figure II-1. However, visual observation of the fuel in this area revealed the presence of longitudinal fuel rod cracks which could result in an abnormally large diameter when measured on the profilometer.

2. Fuel Rod Sampling

Sampling plans were completed on the fuel rods selected for detailed sectioning for density, hydrogen, burnup, and metallography analyses. Density sampling has been completed on all the selected fuel rods. Fuel sampling for hydrogen and burnup samples has been completed on ten fuel rods. Elox coring of fuel samples for hydrogen density analysis has been completed on fuel from seven elements.

All the density samples were 1-1/2 in. in length with a few exceptions. The samples were selected from locations of interest, but away from: cracked areas, NaK reaction zones, dehydrided zones, and fuel rod breaks. Also, regions were excluded where ceramic was adhered to the fuel. The hydrogen samples were cut 5/8 in. in length with cores 3/8 in. long being Eloxed from these samples.

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The hydrogen cores are located around the sample at 90° orientations with a fifth core in the center of the rod. Normally, three cores are analyzed from each sample.

3. Fuel Density Analyses

Density analyses were completed on all 22 selected fuel rods. Fuel density measurements were taken by the Archimedian liquid displacement method with monobromobenzene used as the immersion liquid. Prior to analyzing irradiated samples, six archive fuel specimens and one standard fuel specimen were analyzed. The standard fuel sample consisted of a 2-in. long piece of unirradiated fuel which was measured by the AI Mass Standards Laboratory and AIHL. A comparison of the two values obtained are as follows:

Mass Standards Laboratory	6.093 gm/cc
AIHL	6.095 gm/cc

Density measurements on irradiated specimens are accurate to 0.1% (0.006 gm/cc) or better.

Density analyses are used to determine if the fuel swelled isotropically as predicted. This is done by comparing the observed density change in percent, $\Delta\rho/\rho$ (post-irradiation), which is about equal to the volumetric change, to the observed diameter change when converted to percent. On all the samples, good agreement was noted between the percent density change and the diameter data indicating the fuel did swell isotropically.

Figures II-2 and II-3 show typical density profiles of fuel rods from core positions 3-9 and 6-1, respectively. Good agreement with the diameter profile can be seen with the density profile being the inverse of the diameter profile.

4. NASA Clad Samples

Four 13-in. long half-round sections of cladding were shipped to NASA Lewis, Sandusky, Ohio, for fractography analysis. The samples sent for analysis were representative of small cladding cracks, cracks near the bottom of an element, multiple cracked elements, and long single cracked elements. Typical photographs and copies of the cladding profilometer traces were transmitted to NASA Lewis for their information to assist in evaluation of the fractography.

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Figure II-2. Fuel Density and Hydrogen Data for Intact Element 536 (Core Position 3-9)

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Figure II-3. Fuel Density and Hydrogen Data for Cracked Element 386 (Core Position 6-1)

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5. Fuel Hydrogen Analysis

Hydrogen analysis was completed on archive elements 335 and 564. Three fuel samples were cut from each of the archive rods near the top, center, and bottom of the rods. Sample cores were Eloxed from each sample. A total of ten samples were analyzed from rod 335 and nine samples were analyzed from rod 564.

An average of the analyses obtained is as follows:

<u>Archive No.</u>	<u>AIHL Data (wt %)</u>	<u>Pre-Irradiation Data (wt %)</u>
335	1.66	1.66
564	1.63	1.65

In addition to the unirradiated samples, analyses were completed on the fuel rods from core positions 7-31 and 7-30. The fuel rod in position 7-31 was an intact Type 1 rod while 7-30 was a dehydrided Type 3 rod. Three analyses were performed at each elevation of the rod. No significant radial hydrogen gradients were observed except in the dehydrided regions from 7-30. In fuel rod 7-30 at 0.79 Z/L, a dehydrided zone existed. According to the n-ray data, this zone extended across the diameter of the rod. The hydrogen concentration in this zone was 0.79 H/Zr_{eff}, indicating a loss of 0.95 in H/Zr from the pre-irradiation value. At 0.50 Z/L, a second dehydrided zone existed; however, the zone was tear-drop-shaped surrounded by a fuel zone of higher hydrogen content.

6. Permeation Testing

The permeation system was completely fabricated and checked out in the mockup area of AIHL. Two furnace systems were fabricated so that two elements can be permeation tested simultaneously in-cell. Furnace temperature profiling was completed during this period. The furnace temperature profiles were set up to simulate the axial fuel temperature profiles that were calculated to be in existence in the fuel during reactor operation. This will minimize hydrogen redistribution in the elements that would occur if the elements were tested isothermally at 1400° F as the pre-irradiation acceptance tests were

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Figure II-4. Categorization of Fuel Element Types,
Based on N-Ray Observations

performed. Hydrogen redistribution could cause cladding deformation and possible barrier damage on the irradiated elements. The basic temperature profile established for the irradiated elements increases linearly from 1100 to 1400°F at 0.7 Z/L (12 in. from the top), tapering down to ~1350°F at 1.0 Z/L. At present, unirradiated archive elements are being tested to establish standard permeation leak rates at the various test conditions.

B. EVALUATION TO DATE

1. Element Categorization

The fuel rod diameters on all 46 elements disassembled for the detailed examination phase were compared to those obtained from the n-ray measurements taken during the screening examination. Table II-1 shows the comparisons of the profilometer to n-ray measurements, by element type, for these 46 disassembled elements to date. Figures II-4 through -11 and Table II-1 define the four types of characterization of the fuel elements that have been established and their positions in the reactor.

TABLE II-1
COMPARISON OF FUEL ROD DIAMETERS AS MEASURED
BY N-RAY AND PROFILOMETER

Element Type*	Z/L											All Z/L
	0	0.1	0.2	0.3	0.4	0.5	0.6	0.7	0.8	0.9	1.0	
1 $\dagger\Delta$	0.5	0.5	0.3	0.3	1.2	0.9	0.2	0.7	1.1	0.9	0.1	0.6
σ	1.0	0.8	1.4	1.4	0.8	1.1	1.8	0.9	1.0	1.1	1.3	1.2
2 Δ	1.4	0.7	1.9	1.3	1.9	2.4	3.9	2.7	3.0	3.5	1.6	2.2
σ	1.1	1.2	1.3	0.9	0.9	1.4	2.7	1.7	1.6	1.4	0.9	1.7
3 Δ	0.4	0.2	0.4	1.0	1.4	1.8	2.0	2.5	3.0	2.0	0.5	1.4
σ	1.6	0.8	0.7	1.1	1.2	1.6	1.7	2.0	2.9	2.2	2.7	2.0
4 Δ	-0.1	0.6	0.6	0.2	0.3	0.8	1.2	1.8	1.1	0.8	0.6	0.7
σ	1.4	0.9	1.2	1.1	2.1	0.4	1.1	0.9	1.5	1.3	1.2	1.3

*17 Type 1's, 11 Type 2's, 12 Type 3's, 6 Type 4's

$\dagger\Delta$ = Profilometer measurement minus average of 0° and 90° n-ray measurement in mils

σ = Standard deviation

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Figure II-5. Core Location of Type 1 Fuel Element

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Figure II-6. Core Location of Type 2 Fuel Element

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Figure II-7. Core Location of Intact Type 2 Fuel Element

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Figure II-8. Core Location of Type 3 Fuel Element

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Figure II-9. Core Location of Intact Type 3 Fuel Element

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Figure II-10. Core Location of Type 4 Fuel Element

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Figure II-11. Core Location of Cracked Fuel Element

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As can be seen from Table II-1, agreement between the profilometer and the n-ray measurements were generally good for the Type 1 and Type 4 elements with the discrepancy between the profilometer and n-ray generally running <1 mil (the profilometer measurement being higher than the n-ray measurement). Agreement was not quite as good for the Type 2 and Type 3 elements with the discrepancy for the Type 2 elements generally running in the 2 to 4 mil range in the region of peak fuel growth (i. e., between 0.5 to 0.9 Z/L). Again, the profilometer measurements were higher than the n-ray measurements. Agreement on the Type 3 elements was about the same as on the Type 2 elements.

The discrepancy between the profilometer and n-ray on Type 2 elements is thought to be the result of a thin dehydrided ring around the periphery of the fuel rod due to NaK-fuel reactions in the (generally) cracked Type 2 elements. The discrepancy on the Type 3 elements seems to be more the result of slight misalignments in locations of readings by n-ray. The Type 3 elements exhibited substantial changes in fuel rod profile along the length of the elements and any slight error of actual reading location could cause the discrepancy seen.

The categorization of the S8DR fuel elements is under constant reassessment as new data become available. Such a reassessment was made of the prior element categorization based upon the profilometer data made available during the reporting period and based upon redefinition of Type 4 elements (i. e., bottom growth elements). The Type 4 elements were redefined as those elements exhibiting 4 mils or greater fuel growth in the Z/L region between 0.0 and 0.2.

The general fuel rod cladding shapes associated with Types 1, 2, 3, and 4 elements are shown in Figure II-4. The reassessment of element categorization has resulted in the element listings shown in Table II-2. Locations of the Type 1, 2, 3, and 4 elements are shown respectively in Figures II-5 through -10.

It appears that over half the elements in the S8DR core have sustained some measure of damage during the course of reactor operation. The total cracked fuel element count for S8DR now stands at 73, as shown in Table II-3. The cracked fuel element locations are seen in Figure II-11. An additional 30 elements show beta-phase zones in the fuel rods and 11 elements have been reclassified as Type 4 elements with apparent excessive inlet end fuel growth.

TABLE II-2
FUEL ELEMENT CATEGORIZATION

<u>Type 1:</u>	Basically Intact: $0 \pm 0.3\%$ Cladding Strain
	109 Intact
	1 Cracked
<u>Type 2:</u>	Basically Cracked: Clad Strain $>0.3\%$
	11 Intact (all may be low growth Type 1)
	59 Cracked
<u>Type 3:</u>	Beta-Phase Elements:
	18 Intact
	13 Cracked
<u>Type 4:</u>	Large Cold End Growth: Most Have Longitudinal Rod Cracks
	9 Intact (2 may not have abnormal growth)
	2 Cracked

TABLE II-3
SUMMARY - CRACKED ELEMENTS

<u>Number Cracked</u>	
By Observation	67
By Deduction, n-ray and Cladding Profiles	4
By Deduction, n-ray Profile Only	<u>2</u>
Total	73
<u>Number Cracked Elements With</u>	
Cladding Strain at Crack Location	65
Bottom Cracks and Longitudinally Split Fuel Rods	2
High Cladding Ovality in Crack Region	3
Bottom Crack Without Split Rod	1
N-ray Characterization Only	2

2. Hydrogen Results to Date

One postulate which has been made regarding the failure mode of the S8DR core is that a hydraulic perturbation was caused within the core due to fuel rod bowing/clustering, which reduced the coolant flow to the interior ring fuel elements, increased the bypass flow around the core, with the bypass flow subsequently re-entering the coolant exiting from the core just upstream of the upper grid plate. This resulted in higher than anticipated fuel temperatures within the core and, at the same time, quenched the coolant issuing from this hotter region of the core, giving erroneously low coolant temperature readings on the thermocouples in the outlet plenum. Such bypass flow giving a quenching action could be the cause of the abnormal fuel growth drop-off from $Z/L = 0.8$ to the outlet end of some elements, as seen typically in element 7-31 (see Figure II-12). This element happens to be the highest strain, intact element from the S8DR core.

The hydrogen data obtained from element 7-31 are shown in Figure II-13. TEMPRO calculations of the hydrogen distribution profile in element 7-31 were made for two conditions, namely, 800°F inlet operation and 1000°F inlet operation (recall that at EOL conditions, the coolant inlet temperature in S8DR was 1000°F prior to shutdown with a subsequent reduction to 800°F inlet at full power and then an isothermal soak at 800°F coolant condition subsequent to reactor shutdown for ~80 hr). If, indeed, flow restrictions were encountered as postulated above, the inlet-to-peak coolant temperature rise in the region of element 7-31 could have been more the order of 300°F rather than the nominal core ΔT of 200°F. For this reason, the coolant ΔT cases of both 200 and 300°F were calculated by TEMPRO for both the 800 and 1000°F operation cases. From Figure II-13, it can be seen that the hydrogen profile predicted by TEMPRO for the 800°F operation is not representative of the data trend. The profiles predicted by TEMPRO for the 1000°F operation are in much better agreement with the data obtained and consequently, the hydrogen distributions are considered characteristic of the 1000°F inlet, 300°F ΔT , and 600 kwt thermal operation of S8DR immediately prior to shutdown.

The axial temperature distribution for element 7-31 deduced from the observed hydrogen profile is shown in Figure II-14. The fuel temperature is deduced from the hydrogen profile by normalizing the fuel temperature to the

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Figure II-12. Cladding and Fuel Rod Diameters for Element 7-31
(Intact Type 1)

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Figure II-13. Hydrogen Distribution for Element 7-31

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Figure II-14. Axial Temperature Distribution Deduced From Hydrogen Distribution for Element 7-31 (1000° F Inlet Operation)

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observed hydrogen content in the inlet region of the rod (where reasonable accuracy of calculated temperatures may be assumed) and then determining the fuel temperatures for the rest of the fuel rod from the observed hydrogen profile. Having obtained the fuel temperature from the hydrogen distribution, and knowing the EOL fuel element geometry (see Figure II-12), a calculation of the coolant temperature associated with the fuel temperature is made. Figure II-14 shows the coolant temperature which would have been required to produce the fuel temperature deduced from the hydrogen distribution. The decrease in coolant temperature from 0.8 to 1.0 Z/L is indicative of the existence of bypass coolant cross flow into the region of element 7-31 previously mentioned.

Having obtained the EOL coolant temperature, one can proceed to deduce the fuel temperatures during early life conditions in S8DR (i. e., during 1100°F inlet operation). Such a deduction is shown in Figure II-15, where the coolant profile has been normalized for 1100°F inlet operation with the resulting BOL and offset fuel temperatures shown. Also shown in Figure II-15 is the fuel temperature which is calculated from the observed fuel growth in element 7-31 using the present fuel swelling correlation. The discrepancy between the temperature obtained from fuel growth and the fuel temperature at offset calculated from the hydrogen distribution is seen to be of the order of 60°F. The BOL fuel temperatures, on the other hand, are within ~20° of those predicted at the peak swelling point by the fuel growth in element 7-31.

Element 7-30, an intact, high ovality, Type 3 element which was an immediate neighbor of element 7-31, was also processed for hydrogen content data during the reporting period. The cladding and fuel rod diameter profiles for element 7-30 are shown in Figure II-16, along with the axial locations of two beta regions (as seen by n-ray).

Since element 7-30 was adjacent to element 7-31, it is assumed that the coolant temperature profile along the length of the element should be about the same. Using this assumption and the fuel element geometry as shown in Figure II-16, it is possible to calculate a fuel temperature profile as shown in Figure II-17. Hydrogen analysis from five axial locations on element 7-30 are also shown to be in reasonably good agreement between the hydrogen data (temperatures deduced therefrom) and the calculated fuel temperature profile based on the coolant temperature profile from element 7-31. This agreement appears

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Figure II-15.
Axial Temperature Distribu-
tions Deduced From
Hydrogen and Fuel
Growth for
Element 7-31
(1100°F Inlet Operation)

to verify the existence of bypass coolant cross flow in the region of these elements with the resulting drop in the coolant temperature profile near the outlet end of the elements.

The post-test averages $(H/Zr)_{eff}$ for elements 7-30 and 7-31 were 1.40 and 1.66, respectively. Comparing these to the pre-test hydrogen contents in these elements, it appears that element 7-30, an intact Type 3 element, lost ~20% of its hydrogen during the course of the S8DR operation, while element 7-31, an intact, high strain Type 1 element, lost ~5% of its hydrogen. It is also interesting to note that the hydrogen analyses confirm the high hydrogen loss region(s) as seen by n-ray in element 7-30 and that these regions are, in fact, beta-fuel material.

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Figure II-16. Cladding and Fuel Rod Diameters for Element 7-30
(Intact Type 3)

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Figure II-17.
Axial Temperature Distribution for Element 7-30
(1000°F Inlet Operation)

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3. Failure Mode Analysis

a. Overtemperature Fuel Swelling

A noticeable coolant bypass cross flow near the outlet region of the reactor appears to exist, based upon the admittedly sparse hydrogen data from elements 7-31 and 7-30. This hydraulic condition could have produced higher than expected fuel temperatures in certain regions within the core. For example, the offset fuel temperature in element 7-31 is found to be the order of 1460°F from hydrogen data results whereas the predicted ring 7 nominal fuel temperature at offset determined during the design of S8DR was more the order of 1390°F. The overall temperature level at offset, however, is still $\sim 60^\circ$ lower than that predicted from the present fuel swelling correlation when the observed growth in element 7-31 is used to infer operating offset fuel temperatures.

Fuel swelling observed by profilometry measurements of de clad, Type 1, fuel elements in the AIHL was compared with the ZrH fuel swelling correlation. The comparison was made at Z/L 0.35, an axial location on the S8DR fuel elements which should be in δ -phase and for which the fuel temperature uncertainty because of fuel element clustering should be minimal. Figure II-18 shows the comparison, using analytically predicted fuel temperatures based on the measured outlet coolant profile. The difference in fuel swelling between that observed and the correlation is equivalent to $<30^\circ\text{F}$ temperature bias or 0.5-mil diametral difference. This excellent agreement adds confidence to the current swelling correlation in the 1250°F temperature range.

b. Operationally Induced Cladding Stresses

Stress analysis of S8DR cladding indicates that the cladding behaved about as expected in its environment. Fatigue damage was shown as a probable cause for weakening the cladding, but probably not in itself sufficient to cause cladding failure. Maximum stresses to 15° from the hot line due to circumferential load spreading were calculated to exist, matching the angular distribution of observed cracks. Load distributions consistent with the multiple cracks found on some elements were considered as plausible. For cases in which hydrogen loss left cladding relatively unsupported by the fuel, creep collapse due to element-to-element loading may have been sufficient to cause multicracks.

Figure II-18. Zirconium Hydride Fuel Swelling Correlation Compared With S8DR Observations

c. Type 3 Elements

Visual examination of the 12 Type 3 elements, disassembled during the detailed examination, has shown the barrier to be stripped from the cladding and adhering to the fuel rod (Figure II-19). Some general observations of the ceramic in these Type 3 elements are as follows:

- 1) The ceramic is generally missing in the region of $Z/L = 0.7$, around the entire inside of the cladding tube, being more prevalent at 60° intervals (generally heavier losses in those regions adjacent to neighboring fuel elements).
- 2) The amount of ceramic missing varies from ~ 10 to 50% of that originally present in the affected regions.
- 3) The ceramic is generally missing adjacent to a beta zone; in those elements exhibiting two beta zones in the same fuel rod, the ceramic is missing opposite the lower zone only.
- 4) The ceramic which has been removed from the cladding is adhered to the fuel and in one case (element 7-28), a piece of fuel is physically adhered to the ceramic/cladding.

From examining the adhered ceramic on the fuel rods in Type 3 elements (Figure II-19), it appeared as though the ceramic had been scraped off the cladding and then stuck on to the fuel. It was postulated that the operating mechanism was a differential movement between the fuel and the cladding. During reactor scrams, the fuel was in intimate contact with the barrier, and the cladding tended to move axially in a larger amount than the fuel, thereby producing a scraping or abrasion at the barrier-fuel interface.

An out-of-pile test rig was built to test this hypothesis. The test rig, designed for operation in a standard hydride furnace, applied a uniform load between a fuel segment and a half-round section of coated Hastelloy N tubing which was supported on a half-round of Hastelloy N bar stock to simulate an adjacent element loading. Differential movement between the fuel and cladding sample was produced by a lever arrangement designed into the test rig. Four test runs were made at 1400°F using SCB-coated Hastelloy N cladding samples loaded by a fuel segment with an H/Zr of ~ 1.73 . The fuel/barrier/cladding

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Figure II-19. Element 5-07

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sample was soaked at 1400°F for ~120 hr prior to conducting differential movement between the fuel and barrier. Interfacial loads between the fuel and barrier were 15 lb/in. in two of the tests and 80 lb/in. in the other two.

In the final test, at 80 lb/in. load, 1400°F conditions, two small pieces of ceramic were spalled from the cladding and remained adhered to the fuel segment. The fuel/barrier interface is being metallographically examined for subsequent comparison with samples from the S8DR core.

d. Type 4 Elements

The eleven Type 4 elements with the "epsilon bump" (>4 mils) at 0.1 Z/L are different from elements with the normal, 2 ± 1 mil, growth at 0.1 Z/L in that they have longitudinal (watermelon type) cracks in the fuel rod extending through the growth zone. This 0.1 Z/L location lies just below the calculated epsilon boundary during 600-kwt operation, and corresponds to the structural zone adjacent to the phase boundary which has a rapid reduction of lattice volume with hydrogen addition. This results in the epsilon material having an effective large increase in its thermal expansion coefficient with hydrogen redistribution in contrast to the delta phase behavior in which hydrogen redistribution results in a reduction to an effectively negative thermal expansion coefficient. The epsilon material just below (or to the cool side) of the boundary, therefore, has large equal tangential and axial tensile stresses at the surface, with the tangential stress larger than the axial stresses interior to the surface. Stresses at 0.1 Z/L are computed to be ~5000 psi after hydrogen redistribution at 600°kwt, or roughly half the ultimate tensile strength. Stresses during the megawatt operation are about 100% higher because of the increased temperature gradients due to the higher power and higher elevation of the δ/ϵ boundary on the fuel rod. Transient stresses during axial hydrogen redistribution are higher also because the 0.1 Z/L zone is one in which hydrogen is added through the surface after leaving the hot zone (i. e., local H/Zr is about 1.80 H/Zr after redistribution relative to an initial 1.74 H/Zr concentration). It is thought that these fuel rods therefore cracked longitudinally by exceeding their tensile strength as a result of (1) operation at a megawatt, (2) additional stresses due to local fuel bow contact loads, (3) excessive local hydrogen redistribution rates as a result of local hot spots, (4) fabrication variables (two of the largest Type 4 rods are the only

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survivors of a 16-rod hydriding run in which 14 were rejected for cracking), or (5) a combination of these. It might be pointed out that a similar longitudinal crack occurred in the fuel rod used in the hydriding/rehydriding experiment conducted in ex-pile in the laboratory. Metallurgical studies in the post-irradiation examinations and further fabrication variable correlations are aimed at attempting to identify the variable or combination of variables responsible for this low population family.

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III. REFERENCE ZrH REACTOR ENGINEERING

A. TWO-DIMENSIONAL DRUM WORTH CALCULATIONS

The two-dimensional transport theory code, DOT II, was used to calculate the control worth of the ten-drum flight reactor configuration (Core E) shown in Figure III-1. The technique used is the same as that used to calculate the SCA-4A test drum worths reported last quarter. This technique involves a two-dimensional representation of the reactor in polar coordinates as shown in Figure III-2. The DOT calculations were then made in the S_4 , Po transport approximation using the standard 28 energy group cross section library.

Three evaluations of drum worth were made to investigate changes to the drum and fixed reflector which might enhance drum worth. The change to the fixed reflector was the substitution of cusped filler pieces for the cylindrical pieces. This change gave a slight increase ($\sim 2\%$) in drum worth. The change to the drum was the use of B_4C enriched to $67\% B^{10}$ rather than natural B_4C . This change increased the drum worth about 15% .

The three values of drum worth obtained are given in Table III-1. Also shown is the results of one-dimensional calculations made with the APC II code. The one-dimensional calculations are 5 to 8% high.

B. NUCLEAR LIFETIME STUDIES

The determination of reactivity loss and nuclear lifetime in a ZrH core requires consideration of the following time-dependent effects:

- 1) Depletion of the U^{235} fuel
- 2) Production of fission products and U^{236}
- 3) Depletion of Burnable prepoisons and their daughter products
- 4) Loss of hydrogen moderator

Specialized thermal-hydraulic codes already are in use for the determination of hydrogen loss (e. g., LOAFER II, TEMPRO). Items 1 through 3, the "burnup" effects, must be determined separately by a neutronics-oriented code or methodology.

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Figure III-1. Reference ZrH Reactor - Core E (0.670-in. Pitch, 295 Fuel Element Core, 10 Control Drum Layout)

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Figure III-2. Nuclear Analysis ZrH Reactor Model

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TABLE III-1
ZrH REACTOR DRUM WORTH

Configuration	Worth (\$)		
	2D-DOT	1D-APC	Δ
Natural B ₄ C Cylindrical Fillers	9.66	10.23	+0.57
Natural B ₄ C Cusped Fillers	9.83	10.33	+0.50
Enriched B ₄ C Cylindrical Fillers	11.14	12.07	+0.93

The basic requirements for this calculation are:

- 1) The production-depletion chains must be solved with at least a 2-dimensional spatial model.
- 2) Nuclear criticality must be determined at various times throughout the reactor lifetime. This must include:
 - a) A transport theory eigenvalue calculation
 - b) The inclusion of spectral changes due to nuclide density changes
 - c) The effect of the spatial distribution of their various burnup effects on their reactivity worth
 - d) The interaction between hydrogen loss and other effects
 - e) The interaction between reflector changes and other effects
 - f) The capability to use the standard 28-group cross section set
 - g) The capability to solve a typical problem having 4 eigenvalue calculations in ~ 15 min or less on an IBM System/360 Model 85L.
- 3) Data handling must be automated to the highest practical degree, and input data requirements must be compatible with other neutronics codes in common usage.

The APC II consistent leakage RZ transport theory code was chosen for the neutronics calculation. This code, in common usage at AI for ZrH calculations, uses a leakage-iteration method of solution. It lacks the sophistication of a true 2-D calculation, but is comparatively economical. The method has been

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extensively compared with true 2-D calculations such as DOT II, and its range of applicability is well understood.

Another existing code, FUZE, was chosen to perform the production-depletion calculation. FUZE, developed by AI, is used as a subordinate module in the 1-D PABUC burnup code.

An interface package was made to synthesize 2-D fluxes and average cross sections from the pseudo 2-D APC II results, to transfer this information to FUZE, and to transfer the "burned" material densities from FUZE to APC II. FUZE was capable of 2-D burnup without change.

This system, called BUFRN, has been checked out and compared with other results. It meets the criteria established for its operation, and is expected to see considerable use in future ZrH development activities.

C. REACTOR TRANSIENT ANALYSIS

The STAC code, a collection of programs used to simulate, transfer, and analyze reactor transient data, was restored to successful operation after a year's inactivity. The data synthesis section had become incompatible with the rest of the code in previous evolutionary changes, and its correct operation was restored and tested. This system is now ready to analyze the SNAPTRAN transient temperature coefficient experiments to be run in the next quarter.

D. TEMPERATURE COEFFICIENT CALCULATIONS

The temperature coefficient effort during the report period was directed toward producing new cross section sets. The calculations reported in the April-June 1970 progress report showed that our previous cross section set failed to calculate the effect of burnable poisons on the prompt temperature coefficient of reactivity. The trouble was suspected to be the use of obsolete thermalization data for zirconium hydride.

Since that time, the FLANGE II code has been placed into operation at AI. This is used to produce 70-group cross section and scattering probability data covering the energy range 0.001 to 1.0 ev, based on the ENDF/B library. The GT 100 code, used in previous SNAP and ZrH cross section work, was modified to accept this data, calculate a thermal spectrum for a given temperature and

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nuclide mixture, and to produce 13-group thermal scattering data compatible with the existing 28-group library. The previously used ALC code then integrates the thermal data with fast cross sections produced by the GRISM code, and adds the new data to the existing 28-group library. The entire processing of the FLANGE output is automatic and runs as a single computer job.

Data for H and Zr bound in zirconium hydride have been produced at eight temperatures using this system. U^{235} and U^{238} data have also been produced at these temperatures to allow calculation of the Doppler component of the coefficient. The new library data set is ready for use, and is presently being tested.

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IV. REACTOR CORE TEST

A. INTRODUCTION

The preliminary design of the core and related core test installation was initiated. More detailed analytical modeling of the two hydraulic zone cores with right, left, and nonfinned or "neutral" elements (R-L-N configuration) assembled on a 0.670-in. pitch triangular array was performed. The GEOM thermal/hydraulic code which treats a 30° core segment was revised to conform to the detailed cross flow pattern corresponding to a R-L-N array. The effect of the induced cross flow on the characteristic six theta temperature ripple on the element cladding in a close packed array was evaluated analytically. Results indicate a 45% reduction of the ripple in the central zone and 25% in the outer zone.

A preliminary study of creep relaxation and creep collapse analysis of the fuel element was performed using available element and six theta thermal data. An initial maximum element-to-element contact force of 17.9 lb was generated which relaxes to 13 lb in 100 hr and to <6 lb in 5 yr. The relaxation of the element under operating conditions results in a reversal of forces under shutdown conditions. This results in an additional stress component which must be considered for shutdown and restart operation. Detailed stress analysis was performed and evaluation made on the final closure end cap and the collar button launch suppression device.

Initial checkout calculations were performed with the multi-element, thermal/mechanical structural analysis code, SAS3D. Conversion of this code to the IBM 360 computer neared completion at the end of the report period.

In the nuclear area, a study of power flattening by a number of methods was studied to determine if incentive existed to pursue this technique. It was concluded that none of the methods of flattening the fundamental radial power shape was sufficiently attractive to warrant more detailed consideration. The use of two zones of different element diameter (two zones of coolant mass flow) to approximately match the fundamental power shape was retained as the most attractive reference design approach.

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Figure IV-1. Reflector Configuration for a 60° Segment of the Core

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A correlation of all pertinent test data on ZrH fuel growth in the δ/ϵ -phase boundary regime was performed. Under the action of the fuel rod thermal gradients with associated hydrogen redistribution, the δ/ϵ composition occurs along a specific isotherm in the fuel. At the current initial hydrogen content of $6.3 N_H$, the δ/ϵ composition occurs at about one third the length from the inlet end of the core and again near the exit end. The boundary at the exit end disappears in the normal course of hydrogen loss from the fuel. Major progress was made on a theoretical model to supplement the current correlation work.

The reactor system mechanical design activity concentrated on requirements related to the step-by-step assembly. A key feature is the method of installing of a pre-assembled core unit into the reactor vessel and reflector system. As in S8ER, poison splines are used in the coolant channels to maintain the necessary safety margin against inadvertent criticality during the assembly operation.

Based on temperature data information considerations, locations were selected for 40 thermocouples, located at the coolant channel exits, and for 20 TCs within the fins of the central zone elements. Evaluation continued on the additional use of other in-core instrumentation, especially flux probes to permit core power mapping and monitoring.

B. CORE THERMAL AND HYDRAULIC ANALYSIS

The major areas of effort during the report period involved investigation of the core edge effect of bypass flow reflector heat generation and radiation to the containment vessel and the proper assessment of the mixing characteristics of the R-L-N fin configuration. Analysis of the core edge effects required formulation of a new computer code, NØDREF, and the R-L-N mixing assessment was accomplished by updating the existing code, GEØM2.

1. Reactor Vessel and Reflector Temperatures

Figure IV-1 shows the reflector configuration for a 60° segment of the core. This segment is repeated six times to form the total core. This configuration leads to a ripple in both power distributions and temperatures around the core edge. In order to assess these effects, a new computer code, NØDREF (nodal reflector), is being developed. This code is currently in the checkout phase.

Figure IV-2. Geometry of 180° Fuel Segment Included in Heat Transfer Model

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2. Six Theta Temperature Analysis

A detailed finite difference model of a 180° segment of a fuel cell has been set up for the digital computer. The geometry of the model is shown in Figure IV-2. The model includes a 180° -section of a horizontal slice through a fuel slug, gas gap, and cladding. Also included is the $1/3$ of each coolant channel which is associated with the fuel segment. The model was used to determine temperature profiles in the fuel, cladding, and coolant for various reactor powers and geometrical situations. Of particular interest was the magnitude of the circumferential temperature ripple caused by the close approach of elements in a close packed array. The calculation of volumetric averaged temperatures for the coolant and the cladding surface allowed determination of the average NaK film coefficient, h , for use in other heat transfer calculations which consider the overall reactor core instead of just one small segment. Use of a 180° segment allows treatment of some nonsymmetrical effects, such as, an off-center fuel slug inside the cladding and an element located preferentially toward one neighbor element.

Results of temperature ripple calculations are shown in Figure IV-3, -4, and -5. Figure IV-3 shows the ripple with 0.015-in. and with 0.030-in. element-to-element spacing for the power density (heat flux) at the test reactor core center. Figure IV-4 shows the ripple with 0.015-in. spacing and 1.98 kw/ft, as occurs at the zone boundary in the test core. The results seen on both figures are based on the assumption of a slug flow coolant velocity profile, i. e., equal axial velocity at all points with the tricuspid coolant stream. Figure IV-4 also shows the reduced ripple for a 20%/in. channel-to-channel mixing which is caused by the fins on the elements. The effect of mixing on temperature ripple reduction has been found to be linear out to the 30%/in. mixing quantities calculated. Figure IV-5 is a comparison for the same conditions as in Figure IV-4, but assumes that the coolant has developed a turbulent velocity profile instead of acting as slug flow. It is evident that the ripple magnitude increases about 25% over the slug flow case. This is a result of the low coolant velocity and resulting reduction in heat transfer at the locations of close approach of the fuel elements. Because of the disturbing effects of the fuel element fins, the actual velocity profile is probably somewhere between the two cases considered.

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Figure IV-3. Test Core Temperature Ripple on Cladding OD,
Heat Flux: 2.37 kw/ft (Core Center)

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Figure IV-4. Test Core Temperature Ripple on Cladding OD,
Heat Flux: 1.98 kw/ft (Zone Boundary)

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Figure IV-5. Test Core Temperature Ripple on Cladding OD,
Heat Flux: 1.98 kw/ft (Zone Boundary)

Preliminary results of the effect of mixing on the average heat transfer coefficient show a linear increase from 3520 Btu/hr-ft²-°F to 6090 Btu/hr-ft²-°F for a cross flow increase from 0 to 30%/in. These results are for 0.015-in. spacing and 0.032-in.-thick cladding. Larger spacings or cladding thicknesses result in larger values of h .

3. GEOM Results With R-L-N Model

The use of the R-L-N fin configuration described previously necessitated revision of the GEOM2 code to accommodate the distinct flow patterns characteristic of the system. Figure IV-6 shows this flow pattern for a 30° sector of the core. Adjacent 30° sectors would exhibit mirror image flow patterns to that shown. The cross flow balance is attained by considering the channel conditions surrounding each element and thereby determining a cross flow factor for each element. Where two adjacent elements are finned, the cross flow is the sum of both elements. The cross flow between a finned and neutral element is of course that due only to the finned element.

Figure IV-7 shows the results of a GEOM2 run considering axial and radial variation of fin height in a symmetric core. The axial fin height variation is used to provide clearance for element motion caused by thermal gradients. Figure IV-8 shows the thermal conditions which would exist if no mixing was developed. Figures IV-7 and -8 show quite well the effect of R-L-N mixing in radial flattening of coolant temperatures across the core.

C. STRESS ANALYSIS

1. Fuel Element Analysis

a. Tentative Creep Law for Incoloy 800

An analytical expression for creep rate was developed to perform the creep analyses. Uniaxial data obtained at Oak Ridge National Laboratory and AI information for an environment of 1200°F were reviewed. Looking at Figure IV-9, it is obvious that a large primary creep effect (~ 0.27 strain) occurred for Incoloy 800. This figure shows the primary creep rate, slope from point (1) to (2); intercept between primary and secondary creep rate, point (2); and the secondary creep rate, slope from point (2) to (3).

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Figure IV-6. R-L-N Cross Flow Distribution

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Figure IV-7. Core Coolant Outlet and Fuel Temperature Distributions With R-L-N Coolant Mixing

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Figure IV-8. Core Coolant Outlet and Peak Fuel Temperature Distribution - Assuming No Interchannel Coolant Mixing

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Figure IV-9. Typical Creep Curve

The available data for the uniaxial secondary creep rate were plotted in Figure VI-10. These data include a variety of heats and processes and are considered to be very tentative for the actual alloy and heat selected. The primary creep rate as a function of the secondary creep rate is shown in Figure IV-11. It can be seen that the primary creep rate is ~ 2.3 times the secondary rate. The strain intercept, point (2) on Figure IV-9 is shown in Figure IV-12.

The equations developed for uniaxial and primary creep rates, and strain intercept are:

secondary creep rate

$$\dot{\epsilon}_2 = 4.3369 \times 10^{-11} \sigma^{7.51322} \exp^{-93783.5(T)} ;$$

primary creep rate

$$\dot{\epsilon}_1 = 2.3\dot{\epsilon}_2 ;$$

strain intercept

$$\epsilon_i = 0.0528 \dot{\epsilon}_2^{0.22} ;$$

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Figure IV-10. Secondary Creep Rate, Incoloy 800, 1200°F

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Figure IV-11. Relationship Between Primary and Secondary Strain Rates, Incoloy 800, 1200°F

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Figure IV-12. Strain intercept for Incoloy 800 at 1200°F

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Figure IV-13. Thermal Distribution for Worst Case Fuel Element

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where

$\dot{\epsilon}_1$ = primary uniaxial creep rate (in. /in. -hr)

$\dot{\epsilon}_2$ = secondary uniaxial creep rate (in. /in. -hr)

ϵ_i = strain intercept (in. /in.)

σ = stress (psi)

$T(^{\circ}R)$ = temperature ($^{\circ}R$) .

It must be remembered that this data analysis is tentative until more data become available and that a large data scatter occurs leading to large deviations from the analytical expressions.

b. Beam Creep Analysis

A creep analysis computer program was written and utilized for a beam tube pinned at each end with a concentrated load applied 8 in. from one end. This simulates a fuel element contacting its adjacent neighbor. The contact load is applied to create a beam bending moment and bending stresses which were superimposed on the six theta stresses. These stresses were then allowed to relax using an incremental time, strain hardening rule, creep analysis. This relaxation reduced the stresses in addition to reducing the contact force which in turn reduced the beam bending moment and bending stresses.

A worst case fuel element environment was assumed and the loading computed on the element. It assumed a seven-element cluster across the zone boundary with asymmetric fuel oriented to give the worst transelement temperature gradients. A description of the temperatures is given in Figure IV-13 showing the hot line maximum and cold line minimum axial temperature profiles and the circumferential gradients at Z/L of 0.7. Element 17 (Figure IV-13) is the stress critical element with a maximum transelement cladding temperature gradient of 55°F and a cross element coolant channel temperature of 32°F. The stresses for this condition are shown in Figure IV-14.

The severity of the six theta stresses was questioned and it is thought that if conduction around the periphery is considered and a better hydraulic mixing model were used, the stresses, in the limit, should reduce. This reduction in thermal environment is assumed to cause, in the limit, an elimination of the six

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Figure IV-14. Combined Stresses for Worst Fuel Element Case

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theta temperatures and an equivalent cross element ΔT of 32°F. This is termed, the lower bound case.

The analysis method considers a beam made up of ten sections each 1.85 in. long with a circular cross section divided into 60 elements. The beam (or fuel

element contact) load is known from other analysis and is the result of a thermal gradient causing the element to bow and contact its neighbor. The contact load defines the bending moment, and therefore, the bending stress. The bending stress in each element, j (Figure IV-15), of station, i , is

$$\sigma(i, j) = \frac{Mc}{I} = \frac{M(i)}{\pi R^2 t} \cos \left[\left(\frac{\pi}{30} \right) (j - 1) \right] .$$

In addition the six theta stresses (computed from another program) are input and added to the bending stress for each element, j , at each station, i . Having defined the basic stress levels, the logic of the program is as follows (Figure IV-16).

- 1) The creep strain at each element j of each station, i is derived from the creep curve and strain intercept equations computed previously.
- 2) The fictitious load at each element, defined by the creep strain at each element, j , times the modulus of elasticity times the distance from the neutral axis is

$$M_F(i) = \sum_{j=1}^{j=60} \epsilon_c(i, j) \cdot E \cdot A_j \cdot R \cos \left[\frac{\pi}{30} (j - 1) \right]$$

Figure IV-15. Typical Cross Section Circumferential Station, i

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Figure IV-16. Moment Diagrams

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- 3) The change in deflection at the load point due to the fictitious moment distribution of step 2) is computed on the principal of vertical work and applying a unit moment at the load point,

$$\delta = \int_{i=1}^{i=11} \frac{M(i)m ds}{EI} ,$$

where

M = moment from step 2)

m = virtual moment

s = beam length

E = modulus of elasticity

i = moment of inertia

A_j = area of element j

R = mean radius

c = distance from neutron axis

- 4) The real load, or load relaxation, which will cause the same deflection as in step 3) is

$$\delta = \frac{Pab}{6EI} (2l - 2b^2) = 127.5 P/EI .$$

- 5) A new moment diagram is drawn which is the sum of
- a) the moment for the previous step
 - b) less the moment relaxation computed in step 2)
 - c) less the moment caused by the load relaxation computed in step 4)
 - d) plus the moment of step 2), redistributed in a linear manner.
- 6) The stress at each element is then computed as described above.

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$$\sigma = \frac{M(i)}{\pi R_t^2} \cos \left[\frac{\pi}{30} (j - 1) \right] .$$

- 7) The creep relaxation value is established by assuming a time increment and computing the strain, using the creep rate equations.

$$\epsilon_c = 4.3369 \times 10^{-11} \sigma^{7.51322} \exp(-93783.5/T) \Delta T .$$

If the creep strain is below $0.0528 (\epsilon_c / \Delta T)^{0.22}$, then the creep strain rate is assumed to be 2.3 times the above equation.

- 8) This completes one cycle and then, the next cycle repeats from step 2).

The results are shown in Figures IV-17 and IV-18 and in Table IV-1. The load relaxation for the upper bound case is from 17.93 to 5.23 lb in 100,000 hr, while the corresponding stress relaxation is from -14,364 to -2,077 psi. Another aspect of the relaxation to be considered is when the reactor is shut down and the thermal environment neutralizes. Then, if the stresses are completely relaxed, the room temperature strains will be equal and opposite to the initial operating strains. Since the stresses are not completely relaxed, the residuals are not totally equal to the equal and opposite phenomena.

The initial stresses were derived from a 0.006-in. initial circular bow of the element due to manufacturing tolerances and a bow dictated by the thermal environment less the 0.001-in. clearance between elements. Then the six theta stresses were added.

The residual stresses listed in Table IV-1 were computed as follows:

- 1) The room temperature contact load was determined by:
 - a) subtracting the bow caused by the existing operating contact load from the initial thermal bow. This defines the free bow in the opposite direction.
 - b) subtracting out a 0.001-in. clearance.
 - c) ascertaining the room temperature load strains necessary to deflect the element through the movement of steps a) and b).

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Figure IV-17. Creep Relaxation - Upper Bound Case for Worst Element

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Figure IV-18. Creep Relaxation - Lower Bound Case for Worst Element

TABLE IV-1
CREEP ANALYSIS SUMMARY FOR WORST ELEMENT

Time (hr)	Load (lb)	Compressive Stress (psi)	Residual Tensile Stress (psi)	Tensile Stress (psi)	Residual Compressive Stress (psi)	Maximum Creep Strain (%)
0	$\frac{17.93}{13.9}$	$\frac{-14,364}{-6,316}$	0	$\frac{9,248}{6,282}$	0	0
1	$\frac{17.05}{13.87}$	$\frac{-9,663}{-6,283}$	$\frac{4,295}{0}$	$\frac{8,801}{6,251}$	$\frac{-41}{0}$	$\frac{-0.023}{-0.0002}$
100	$\frac{12.97}{12.64}$	$\frac{-5,231}{-6,148}$	$\frac{9,093}{0}$	$\frac{5,218}{5,144}$	$\frac{-3,990}{0}$	$\frac{-0.049}{-0.009}$
1,000	$\frac{10.00}{10.22}$	$\frac{-3,837}{-3,915}$	$\frac{9,767}{300}$	$\frac{3,825}{3,913}$	$\frac{-4,663}{-300}$	$\frac{-0.061}{-0.021}$
10,000	$\frac{6.98}{7.20}$	$\frac{-2,781}{-2,684}$	$\frac{11,223}{2,200}$	$\frac{2,600}{2,683}$	$\frac{-6,300}{-2,200}$	$\frac{-0.071}{-0.036}$
50,000	$\frac{5.68}{5.82}$	$\frac{-2,324}{-2,160}$	$\frac{12,040}{3,000}$	$\frac{2,261}{2,160}$	$\frac{-7,190}{-3,000}$	$\frac{-0.076}{-0.042}$
100,000	$\frac{5.23}{5.37}$	$\frac{-2,077}{-2,060}$	$\frac{12,347}{3,300}$	$\frac{2,058}{2,060}$	$\frac{-7,250}{-3,300}$	$\frac{-0.077}{-0.044}$

- 2) The bending stresses were determined from calculations defined by the load strains of step 1 c).
- 3) The six theta and redistributed bending components of the residual stresses were calculated from:
 - a) computing the bending stress relaxation (at temperature) by finding the moment and stresses due to the difference in load from time zero to the time in question.
 - b) subtracting this stress from the stress at time zero. The remainder is the initial six theta plus the remaining bending.
 - c) subtracting out the remaining stress in each element to obtain the "residual operating temperature" stress and dividing this by E to find the strain.

d) multiplying the strains of step c) by the room temperature modulus to find the six theta and redistributed bending residual stress.

4) The total residual stress is found by combining steps 3 d) and 2).

c. Preliminary Creep Collapse Analysis

An approximation to the collapse of the fuel element cylinder is made using a ring, plane strain, model. This is conservative in the sense that no membrane axial restraint exists.

The case in Section IV-B is used for analysis. The contact load drops from 17.93 lb initially to 12.97 in 100 hr and to 10 lb in 1000 hr. At the end of 100,000 hr the load is 5.23 lb. The analysis was generated for constant loads and also for the load time variation.

A 18.38-lb load is seen to collapse the ring in 16,250 hr, while a 12-lb load doesn't cause ring collapse in 100,000 hr. The load deflection curves are shown in Figure IV-19 for 18.38, 12, and 9 lb.

Comparing the load relaxation to the collapse load curves, it becomes obvious that the tube won't collapse, especially considering the conservatism of the analysis.

d. Fuel Element Vibration Suppressor

The development of the column buckling device (CBD) evolved from the investigation of a launch vibration suppressor. Recent analysis and correlation to experimental collapse data have led to the recommendation of a CBD with a pre-kinked column as shown here.

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Figure IV-19. Creep Collapse-Ring Analysis for Worst Element Upper Bound Case

A typical CBD load-deformation curve is shown in Figure IV-20. Data correlation has proven that the elastic load limit (design load for no plastic response during launch vibration) can be calculated directly by using the secant formulation. The latter provides for short column abnormalities by assigning an effective load eccentricity,

$$\sigma_{ys} = \frac{P}{\pi R^2} \left[1 + (Ecc) \secant \left(\frac{L}{2R^2} \sqrt{\frac{P}{\pi E}} \right) \right],$$

where

σ_{ys} = material yield strength (0.2% offset)

P = elastic load

R = column radius

L = column length

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Figure IV-20. Typical Column Buckling Device Load-Deflection Curve (Type 405 Stainless Steel) Straight Column, 0.053-in. -diam, 0.45-in. Length

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E = modulus of elasticity

Ecc = effective eccentricity = 0.25.

However, there is an additional loading from a short column strain-hardening phenomenon, $P_{\text{strain hardening}}$ (Figure IV-20). This latter effect results from bending stresses superposed on the average column compressive stress. As the average stress reaches the material yield strength, abnormalities which exist in the column give way to a slight buckling effect which in turn sets up bending stresses across the column.

Thus, the concave side of the buckling column is experiencing a total compressive stress above the proportional limit of the material. The concave side of the member is therefore being hardened through straining, and tends to resist further buckling. The maximum loading limit, P_{max} , exists when the total axial deformation, ΔL collapse, has initiated sufficient buckling to negate the strain hardening effects. This corresponds to a maximum deflection for the buckled column of $\sim 0.75 \times$ column radius.

It would therefore be desirable to minimize the effects of strain hardening i.e., reduce ΔL and ΔP strain hardenings. In this light, the effects of pre-kinking the column were investigated. By incorporating analytical techniques for bowed columns which were based on secant formulism, theoretical compatibility between calculated results was assured. It was found that a pre-kinked column (with a maximum kink deflection of $\sim 0.50 \times$ column radius) which was designed to the same elastic load as a "straight" column would experience less strain hardening.

$$\Delta L_{\text{strain hardening, prekinked}} \approx 0.50 \times \Delta L_{\text{strain hardening, straight}} ;$$

and

$$\Delta P_{\text{strain hardening, prekinked}} \approx 0.25 \times \Delta P_{\text{strain hardening, straight}} .$$

The prekinked column is therefore superior to the "straight" design in that less axial deformation is required to initiate collapse at a reduction in total loading.

e. Multi-Element Stress Code

Development of the cluster computer program "SAS3D" is nearly complete. This code determines the clustering response of a 31-element bundle, including 14 active elements, to an arbitrary initial thermal and mechanical environment. The output of the program includes the set of forces required at initial element interference points to achieve mechanical interelement compatibility. Given further is a 3-dimensional definition of all element centerlines. The physical arrangement of the fuel element bundle considered by SAS3D is shown in Figure IV-21.

Figure IV-21. Cluster Code Development (SAS3D)

Using the indicated computer program, the response of this fuel element bundle to initial conditions representing the reference core test system was calculated. These calculations were based on a nominal inter-element or fin-to-fin clearance of 0.002 in., pinned element ends and the element contact plane at $Z/L = 0.55$. For the calculations, the boundary condition of no thermal bow of

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Figure IV-22. Calculated Force Pattern After 1st Mechanical Iteration Cluster Using "SAS3D" Program With Straight Elements

the outer rows of "reaction elements" was employed. Using fixed coolant temperature as a boundary condition is also a possible option of the program. The clustering and force pattern calculated for elements with zero manufacturing bow is shown in Figure IV-22.

Another set of calculations was performed with all active elements assumed to show an initial manufacturing bow of 0.006 in. The orientation of the bow was specified so as to increase the contact forces associated with straight elements. The results for elements with initial bows are given by Figure IV-23.

The effect of clustering upon initial (straight elements) channel temperatures has been assessed and is shown in Figure IV-24. For conditions with initial manufacturing bow results for both no mixing effect and some mixing are given. These results suggest that clustering with finned tubes, for the conditions analyzed, may increase nominal flow channel temperatures by $\sim 7^\circ\text{F}$ in the region of primary elements. Flow channel temperatures at the core periphery may increase by up to $\sim 10^\circ\text{F}$.

The results presented are based on mechanical (4-times) but not on thermal iteration. However, final results, i. e., after 2 to 3 thermal iteration passes, are not expected to change these results drastically.

g. Flow Induced Vibration

A preliminary study of flow induced fuel element vibration was made. Lateral element vibration due to axial flow is vanishingly small. However, cross flow may cause a lateral dynamic element excitation force of ≈ 0.25 lb with an oscillatory lateral element response of ≈ 0.0004 in. at ~ 26 cps. From the fatigue standpoint, this is considered inconsequential.

D. POWER FLATTENING STUDIES

An evaluation of flattening the radial power shape in the ZrH reactor was completed. A flat radial power profile can smooth the radial fuel temperature distribution which will prevent excessive hydrogen loss or fuel element swelling during the lifetime of the reactor. Similar results may be achieved by adjusting flow channel areas zonewise across the reactor core, as it is presently designed in the reference ZrH reactor; flattening of the power profile utilizing only compositional adjustment, however, has the advantage of mechanical simplicity.

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Figure IV-24. Clustering Effect on Flow Channel Temperatures for Finned Tubes

Four schemes for altering the power distribution were investigated:

- 1) Two radial zones of hydrogen
- 2) Two radial zones of uranium
- 3) Two radial zones of a Eu-Gd poison
- 4) Two radial zones of combined uranium and Eu-Gd.

In each case the position of the zone boundary as well as the interzone compositional ratio affects the peak-to-average power ratio in each zone and therefore, several zone boundary positions were used in each of the schemes in order to extrapolate to a theoretical "best" power flattening. Several representative power distributions are given in Figure IV-25. Both uranium and poison zoning produce a second peak in the outer zone which can be made equal to the central peak by adjusting the position of the zone boundary.

The four schemes were compared against the reference flight reactor with respect to available excess reactivity, radial fuel temperature distribution, calculated loss of hydrogen, and fuel swelling over a 5-yr and 600-kw reactor

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Figure IV-25. Radial Power Distributions

operation, and available control drum worth. In general, the fuel swelling and hydrogen reactivity loss characteristics for the best flattened cases of the four schemes were nearly identical to those predicted for the reference flight reactor. Therefore, the evaluation of "flattening" was based on the following results:

- 1) Hydrogen zoning has a very limited spatial effect upon the radial power profile, as may be seen in Figure IV-25. A geometrically homogeneous core does allow larger fuel elements in the reactor central zone; therefore, the calculated excess reactivity is potentially greater than for the flight reactor. However, power flattening requires reduced hydrogen concentration in the central zone and increases the probability that hydrogen loss during operation will cause a transition to beta-phase fuel.
- 2) Uranium zoning always produces a power discontinuity at the zone interface, as may be seen in Figure IV-25. Reduction of the power peaks in the central zone can be more than compensated for by the consequent increase in the second zone peak. Even after adjusting the zone boundary for the optimum equal peaking, it was found that the uranium composition in at least one zone would have to deviate significantly from the nominal 10.5 wt % in order to give peak-to-average power ratios that were competitive with the alternate scheme using distributed Eu-Gd poison. The principal disadvantage to using uranium zoning is that the majority of fuel swelling information on ZrH fuel is for a nominal 10.5 wt % fuel composition.
- 3) Eu-Gd poison zoning shows the smoothest power variation across the reactor core. It was found that since all the poison could be put into a relatively small central zone, an optimum low peak-to-average power ratio of 1.140 could be calculated. In addition, a series of control drum worth calculations showed that such a poison-flattened core makes the reflector more important and potentially increases the control drum worth by over \$2. However, Eu-Gd poison burns out preferentially as compared to uranium. A calculation of beginning-of-life and end-of-life power profiles showed that the poison-flattened reactor "unflattens" itself which causes the potential control drum enhancement to be lost over the lifetime, creating a drastic change in power shape.

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Figure IV-26. Reactor Core Test Installation

- 4) The use of combined poison-uranium zoning was found to give lower peak-to-average power ratios than either scheme alone; however, the disadvantages of non-nominal uranium composition and preferential poison burnout remain.

It was concluded, therefore, that none of the four schemes could show a considerable superiority to the reference flight reactor configuration. In summary, the keyword disadvantages of the nuclear zoning schemes are:

- 1) hydrogen: small flattening effect
- 2) uranium: reduced confidence in swelling characteristics
- 3) poison: lifetime power shape change.

E. MECHANICAL DESIGN AND ASSEMBLY

1. System Design

The current configuration for the reactor core test installation is illustrated by Figure IV-26. The changes incorporated in the design during the quarter, based on the recommendations that were made in the conceptual design review conducted early in the quarter, are itemized as follows:

- 1) Core detached from shield plug
- 2) One piece reactor vessel installed upward through intermediate shield
- 3) Increased outlet plenum height by lowering reactor core
- 4) Two outlet NaK pipes
- 5) Control drum drives located in upper dome area — shielded from NaK pipes
- 6) Drain line integrated into NaK inlet pipe.

In order to proceed logically into the preliminary design of the system components, a definition of the sequence for assembly and installation was required. The assembly and installation are divided into four phases as follows:

- 1) System assembly in preparation for core installation
- 2) Core assembly

3) Core installation and assembly completion

4) Vacuum chamber preparation and system installation.

Phase 1), illustrated by Figure IV-27, will be accomplished in an assembly building (Bldg. 13, Santa Susana). The unit will then be transported to the test facility (Bldg. 59) and lowered to the floor of the test vault. Cheater cards will be connected between the control drives on the system and the facility control wiring at the vacuum chamber feedthrough ring and the entire control system will be checked out prior to core installation.

Phase 2), core assembly, will also be performed in Bldg. 13. This is currently planned as a noncritical loading, using poison splines in the coolant channels to insure subcriticality. The core will then be moved to the critical facility in Bldg. 12 where it will be used to measure the characteristics of the reference ZrH reactor poison-backed control drums. The mechanical sequences of core assembly have not been determined because details of the core design, specifically those of core boundary constraint, have not been finalized.

All operations of Phase 3) will be performed on the test vault floor. The sequence is illustrated and outlined on Figure IV-28.

Steps 1 through 7 of Phase 2), illustrated and outlined on Figure IV-29, will be performed concurrently with Phases 1) and 2) and must be completed prior to the start of Phase 3). Steps 8 through 12 of Phase 4) then follow the completion of Phase 3).

2. Poison Spline Study

The conceptual unitized core loading sequence requires the passing of the fully assembled core through the upper shield plug. The possibility of reactor criticality necessitates poisoning the core during this operation. A scheme similar to that of the S8ER loading has been considered in which poison splines are placed in the core coolant channels during insertion through the shield plug.

A spline outer diameter of 0.080 in. for both core hydraulic zones was chosen on a geometric basis. B_4C was chosen as the poisoning material. Vibrational compaction of the B_4C powder into ~ 0.125 -in. tubes and subsequent swaging of the 0.005-in.-thick wall tubes was demonstrated and established the fabrication capability of splines with 80% of B_4C theoretical density. The results

1. PLACE INTERMEDIATE SHIELD ON ASSEMBLY STAND
2. ASSEMBLE THERMAL INSULATION ONTO GAMMA SHIELD
3. COMPLETE GAMMA SHIELD ASSEMBLY
4. INSTALL GAMMA SHIELD ASSEMBLY
5. INSERT REACTOR VESSEL UPWARD THROUGH INTER-MEDIATE SHIELD
6. INSTALL SPLIT RING RETAINER TO SECURE REACTOR VESSEL

7. INSTALL REFLECTIVE INSULATION ON NaK PIPES – SHIELD AREA ONLY
8. INSTALL NaK PIPES, WELD, X-RAY, AND LEAK TEST

9. INSTALL FIXED REFLECTOR AND CONTROL DRUM ASSEMBLIES ON REACTOR VESSEL
10. INSTALL CONTROL DRUM DRIVES
PERFORM CONTROL DRIVE – REFLECTOR DRUM FUNCTIONAL TESTS (AMBIENT TEMPERATURE)
TRANSPORT SYSTEM ASSEMBLY TO BLDG. 095 TEST VAULT

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Figure IV-27. Core Test Reactor Assembly Sequence

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Figure IV-28. Sequence of Operations for Phase C

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Figure IV-29. Sequence of Operations Prior to Phase C

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below, however, indicate that simple vibration compaction to 50% density may be acceptable.

The effective spline worth in the core was calculated, using P_{OS_4} transport theory with a 28-group neutron library. Cell weighting techniques were used to include the self-shielding effect. A homogenous spline loading was assumed across the active core region. The results of worth calculations using varying total spline loadings and varying B_4C densities are presented in Figure IV-30.

Further transport calculations showed the unsplined core to be \$0.50 critical (normalized, cold, clean, poisoned) in the shield plug. Thus a nominal loading of 150 poison splines at 0.50 fractional theoretical density (worth -\$13.50, Figure IV-30) provides a \$13.00 subcritical core in the plug.

These calculated B_4C spline worths can be compared to boron spline worths from the SCA-4B critical experiments.* An average SCA-4B spline (1.34 gm B/spline) provides a relative worth of -\$1.00 per gm B/kg U^{235} . The comparison is made with a 0.50 fractional density B_4C spline (1.129 gm B spline) which has a calculated (250 splines) average worth of -\$0.085. Then,

$$\frac{-\$1.00}{\text{gm B/kg } U^{235}} \times \frac{1.129 \text{ gm B per spline}}{12.93 \text{ kg } U^{235} \text{ in core}} = -\$0.087 \text{ per spline}$$

Thus, theory and experiment are in excellent agreement.

3. Core and Reflector Design

A horizontal cross section of the core and reflector is shown on Figure IV-31. The six-drum reflector configuration was selected over the seven-drum configuration using the 4.50-in. radius S8DR drums. The preliminary design of the reflector was initiated with a design concept in which each of the six fixed reflector tri-cusps and each of the drum assemblies are individually mounted onto the reactor vessel.

Figure IV-31 also shows an enlarged cross section of a sector of the core. During the quarter, various design configurations for the internal reflectors were evaluated, and the design shown was selected for detailed study.

*B. J. Dray et al, "Reactivity Effects of Gadolinium-155 on a SNAP Reactor," AI-AEC-12909 (April 15, 1970)

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Figure IV-30. Core Test Reactor Cross Section

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Figure IV-31. B₄C Spline Worth
(0.080-in. OD, 0.005-in.-wall splines)

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Figure IV-32.
Internal Reflector Support for
Core Test Reactor

The fuel elements, internal reflectors and the top and bottom grid plates make up a unitized core assembly which will be inserted in the core vessel. The core is supported by seating the top grid plate on a step in the vessel ID. Three long bolts pass through side reflectors and screw into vessel lugs below the lower grid plate for core holddown. The seating of the top grid plate stops flow from bypassing the core through the clearance (~ 0.030 in.) between the internal reflectors and the vessel. The clearance space therefore has a static pressure equal to that at core inlet and the internal reflectors are loaded inward by a pressure differential; zero at the inlet end and equal to the core pressure drop of 1 psi at the outlet end. This inward pressure holds the internal reflectors against the outer fuel elements and maintains the boundary flow channel areas at their design values.

The design of the core liner currently under study is shown on Figure IV-32. The basic function of the liner is to prevent inward leakage of coolant through the gaps between adjacent internal reflectors into the core boundary coolant channels. They also provide a small supplementary inward force on the internal reflectors to maintain their position under low flow operation.

4. In-Core Instrumentation

The in-core instrumentation in S8DR provided much useful data which greatly facilitated the explanation of the fuel element failures. Thus, the decision was made early in the planning for the reactor core test to utilize more in-core instrumentation than had been used in S8DR. This instrumentation would provide the on-line operational data to verify the predicted core performance. The instrumentation to be employed in the reactor core test, in addition to providing the desired data, had to meet two additional criteria. It had to be "off-the-shelf" hardware which would require minimal development to include it in the core, and its inclusion in the core could not jeopardize the long term operational capability of the reactor.

Within these guidelines, various types of in-core instrumentation were evaluated which would provide information regarding fuel temperatures, cladding temperatures, coolant temperatures locally within the core and plenum regions, flow rates within the core, local power/flux levels, mechanical vibration in the core, and core position of any elements which developed cladding cracks. Evaluation of the various types of instrumentation which could provide this information

Figure IV-33. Proposed Thermocouple Locations in Core Test Reactor

while satisfying the above-mentioned criteria narrowed the instrumentation types to thermocouples in the outlet plenum (similar to S8DR) and thermocouples in the fuel element cladding fins. An additional type still receiving evaluation is in-core flux probes. Other types of instrumentation which would potentially provide the desired information failed to meet one or both the above-mentioned criteria.

Placement of the thermocouples in the plenum and the fins in such a manner as to provide the maximum useful information was the next consideration. S8DR experience had shown the high desirability of placing additional thermocouples in-core such that more complete mapping of the core temperatures was available. Core bypass flow had been shown to be highly significant in S8DR but very little temperature data had been available from the region of the core edge. Further, analysis of the core test configuration had shown its region of largest temperature uncertainty (other than the core edge) existed at the hydraulic zone boundary. With this experience and these considerations in mind, the tentative placement of the plenum and fin thermocouples in the reactor core test preliminary design was specified and is shown in Figure IV-33. While the pattern at first glance resembles a "shotgun" pattern, overlaying the thermocouple locations on a 60° repeating segment of the core shows that excellent coverage of representative coolant channels is afforded. This is shown by Figure IV-34.

The data provided in Table IV-2 illustrate the types of data sought and the number of thermocouples which will provide the information with the proposed thermocouple placement. Cross element ΔT measurements will be useful in assessing the local cladding stresses. Correlating plenum thermocouples with fin thermocouples on the same coolant channel will allow a better correlation of inferred fuel temperatures based on the plenum thermocouple readings. The overall temperature mapping of the plenum will provide a direct indication of the flow uniformity in the core.

The location of thermocouples in the cladding fins is limited to the inner hydraulic zone of the core. Fins in that zone are 30 mils high while fins in the outer zone of the core are only 15 mils high. The current plan is to machine a groove in the fin and attach a flattened thermocouple in the groove with its junction being at the desired axial location. Since the flattened thermocouples are 18 mils high, their use in 15-mil-high fins was rejected. It is noted from

Figure IV-34. 60° Overlay of Thermocouple Locations
in Core Test Reactor

TABLE IV-2
IN-CORE THERMOCOUPLES - REACTOR CORE TEST

TC Location	Measurement	Core Location	Number	Comments
Outlet Plenum	Cross element ΔT	Toward hex flats	9	15 in S8DR
		Toward hex corners	16	15 in S8DR
	Correlate with fin TC Temperature mapping	Inner zone	8	-
		Zone boundary ± 1 channel	9	-
		Core edge + 1 channel (Influence of bypass)	8	5 in S8DR
		Inner zone (Other)	3	-
		Outer zone (Other)	3	-
		Total plenum TC	40	27 in S8DR
Element Fins	Cross element ΔT and cladding/channel temperature mapping at $Z/L = 0.7$	Toward hex flats	7	No fin TC in S8DR
		Toward hex corners	5	
	Axial temperature gradients	2 channels, each at 2 Z/L 's	4	
		8 channels in inner zone	10	
	Correlate with plenum TC			
	Other cladding/chan- nel temperature mapping	Selected channels	2	
		Total fin TC	20	

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Figure IV-34 that some of the elements in the inner zone of the 30° segment do not have fin thermocouples. These elements are "neutral" elements which do not have fins.

Fin thermocouples will provide a more accurate means of extrapolating to the fuel temperatures than afforded by plenum thermocouples and will allow direct measurement of the cross element ΔT in the region of largest fuel element operating stresses. Fin thermocouples are also used to determine the axial temperature gradient along the elements. As previously mentioned, they will also be used to correlate with plenum thermocouple readings and to provide additional core temperature mapping. The total number of thermocouples inside the core vessel was determined as being a reasonable balance between (1) providing "complete" core mapping, and (2) the number of instrumentation leads which could be installed in the core without causing undue difficulty.

5. Shield Design

Preliminary layout drawings of the intermediate shield plug and the center shield plug were made. Some modifications of these drawings may be required when the shield cooling analysis is completed. Shield cooling analysis is proceeding on the basis of using pressurized nitrogen in a closed loop, with water being considered as an alternate coolant for the intermediate and the ring shield plugs. The selection involves a tradeoff between the relatively high equipment costs for a nitrogen cooling system and the safety requirement for triple barrier separation from NaK with water cooling.

V. REACTOR THERMOELECTRIC SYSTEM ENGINEERING

A reactor thermoelectric power system capable of delivering 33.0-kwe gross electrical power has been designed for North American Rockwell Corporation, Space Division, in support of their Space Station studies. This design follows that of a reactor-Brayton cycle study made earlier for the Space Division and is based on similar ground rules and concepts.* It also draws on work done on a reactor thermoelectric system for the McDonnell-Douglas Corporation.†

A major constraint in these studies was that the Advanced Logistics Shuttle (ALS) was limited to a maximum payload of 20,000 lb per launch and that the payload must be no larger than 14 ft in diameter and 58 ft long.

A second major constraint was that the dose rate received by the crew from the reactor was not to exceed 30 mrem/day for a reactor-crew separation distance of 200 ft and a primary dose plane diameter of 100 ft. A secondary dose plane diameter of 200 ft was established in which the allowable dose rate was 150 mrem/day. As a result of these constraints, the reactor thermoelectric power system was designed in two sections for two separate ALS launches. The upper section including the reactor, shield, thermoelectric converters, and NaK lines was designated R-1 and the lower section including the radiator and coolant lines was designated R-2.

Because of the low dose rate and the large dose plane diameter, the radiation shield was too heavy to stay within the 20,000-lb ALS payload limit. Therefore it was decided that a water augmented neutron shield be installed below the gallery. With this shield concept, water would be piped into the R-1 shield tank from the R-2 and other sources to produce the acceptable dry launch weight for the R-1.

While the Brayton power system had a pressurized compartment where a crew member could perform maintenance of the power conversion in a shirt-sleeve environment, the thermoelectric system does not have this feature. The

*SD70-168, Nuclear Reactor Powered Space Station Definition and Preliminary Design, Volume III, Electrical Power Subsystem (to be released March 1971)

†MDC-GO750, Space Station Reactor Power System Design Document, Volumes I and II, December 1970

thermoelectric system requires very minimal maintenance, and any maintenance can be accomplished by a crew member in a space suit.

Table V-1 gives the performance for conditions after 5 yr when the reactor outlet temperature has gradually risen, offsetting degradation, to the design value of 1200° F. It is assumed by this time that six of the 120 thermoelectric modules have failed and that the remaining 114 modules have undergone a performance degradation amounting to ~9% loss in electrical power. The power system at its design point delivers 25.0 kwe to the electrical loads and outputs 33.0 kwe at the patchboard to provide for transmission and power conditioning losses. At the thermoelectric modules, 34.2 kwe is generated since there are losses in pumping the radiator coolant and in transmitting to the patchboard.

To arrive at the performance shown in Table V-1, a trade study was conducted in which the reactor outlet temperature, the reactor thermal power, the number of thermoelectric modules, and the radiator area were varied. This revealed that if the entire available area on the R-2 module were used for the radiator and since the R-1 did not have a radiator, the overall system weight would be about minimal and, more importantly, the R-1 weight would be at minimum.

The R-1 weight is 19,700 lb and the R-2 weight is 19,800 lb. The R-1 shield tank can hold 19,200 lb of water of which 9,000 lb is included in the basic R-2 weight. With only water from the R-2 in the shield tank, the dose rate is 150 mrem/day — the nominal guideline dose, the reactor can be operated. The addition of 10,200 lb of water from a later ALS flight lowers the dose rate to the conservative 30 mrem/day value, which allows for continuous operation in an orbit having the severest natural radiation.

The reference zirconium hydride reactor generates 547 kwt which is carried by NaK coolant to the gallery where the intermediate heat exchangers transfer the thermal power to the intermediate NaK coolant. The latter transports the power to the 120 thermoelectric modules located in an annular array at the base of the R-1 module. Figure V-1 depicts the reactor, shield and gallery, and Figure V-2 shows the main features of the complete R-1 module.

<u>Electrical Power Utilization</u>		<u>NaK Loop 3</u>	
Power at patchboard (kwe)	33.0	NaK flow (lb/sec)	11.4
Organic pump power (kwe)	0.5	Loop ΔP (psi)	3.30
Power loss TE-PB (kwe)	0.7	Hydraulic power (kw)	0.153
Gross TE power (kwe)	34.2	NaK maximum temperature ($^{\circ}F$)	533
Converter pair voltage (volts)	5.66	NaK minimum temperature ($^{\circ}F$)	333
		Loop thermal power (kwt)	506
<u>NaK Loop 1</u>		<u>Organic Loop (A or B)</u>	
NaK flow (lb/sec)	15.6	Organic flow (lb/sec)	5.07
Loop ΔP (psi)	2.11	Loop ΔP (psi)	10.0
Hydraulic power (kw)	0.134	Hydraulic power (kw)	0.206
Reactor inlet temperature ($^{\circ}F$)	1042	Organic maximum temperature ($^{\circ}F$)	518
Reactor outlet temperature ($^{\circ}F$)	1200	Organic minimum temperature ($^{\circ}F$)	318
Reactor power (kwt)	547	Loop thermal power (kwt)	506
<u>NaK Loop 2</u>		<u>Radiator (Shared fin)</u>	
NaK flow (lb/sec)	12.3	Area (ft ²)	2476
Loop ΔP (psi)	3.06	Alternate-tube spacing (in.)	3.5
Hydraulic power (kw)	0.153	Average effective temperature ($^{\circ}F$)	395
NaK maximum temperature ($^{\circ}F$)	1195	Effectiveness (Ω)	0.85
NaK minimum temperature ($^{\circ}F$)	995	<u>Thermoelectric Pumps</u>	
Loop thermal power (kwt)	547	Thermal power input (kwt)	55
<u>Thermoelectric Modules</u>		Total hydraulic power (kw)	0.440
Average hot clad temperature ($^{\circ}F$)	1083	Overall pump efficiency (%)	0.80
Average cold clad temperature ($^{\circ}F$)	436		
Thermal power input (kwt)	492		
Gross electrical power (kwe)	34.2		
Converter efficiency (%)	6.95		
Modules operating	114		

Table V-1.
Thermoelectric System Performance
(After 5 yr of Operation, 6 Modules
Disconnected at Patchboard)

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Figure V-1. Reactor, Gallery, and Shield Assembly - Reactor TE Power System

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Figure V-2. R-1 Module - Reactor TE Power System

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The thermoelectric modules are cooled by NaK heat rejection loops that carry the heat to four NaK organic heat exchangers located just above the thermoelectric modules in R-1. The heat is transferred to an organic fluid and travels to the heat rejection radiator in R-2 (Figure V-3). The organic fluid (such as Dow-Corning 200, Dowtherm A, or MIPB) is not suitable for cooling the thermoelectric modules directly because of the high heat flux and resultant large film drop. This fluid, however, is more suitable for disconnect junctions such as are needed between the R-1 and R-2 modules. Also, organic radiator fluids may obviate the need for a thermal shroud over the radiator during non-operating periods. Two redundant organic circuits — one active and one standby — are used in the heat exchanger, lines, and radiator. This is desirable because the R-2 module is designed for a 10-yr life rather than the 5-yr life of the R-1 package.

Each of the four series of primary-intermediate-heat rejection NaK loops is pumped by a three-throat direct current electromagnetic pump located in the gallery. Each pump receives electrical power from 4 special thermoelectric modules operating at low voltage and high current. The pump system is located in the gallery because the primary NaK in each pump is radioactive.

The organic fluid is pumped by canned rotor pumps using main electrical power. There are four series-parallel pumps in each of the two circuits.

The main thermoelectric modules are connected in pairs to deliver 5.66 volts. Since this voltage is too low for efficient transmission over long distance, the thermoelectric module pairs are connected in series in four strings. The patchboard is used to electrically isolate any defective pair so that the net power loss per pair is only $\sim 1.7\%$ rather than losing one string out of four or nearly 25% power.

The structure of the R-1 section is an open truss type to increase the ballistic coefficient. In the R-2, a semi-monocoque structure is used. In R-2 are two toroidal water tanks which can contain the 19,200 lb H_2O needed for the gallery shield. This allows for storage of the water while R-1 modules are being exchanged.

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Figure V-3. R-2 Module - Reactor
TE Power System

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VI. THERMOELECTRIC CONVERTER DEVELOPMENT

(Prepared by Westinghouse Astronuclear Laboratory)

A. REACTOR POWER MODULES

During the past quarter primary emphasis was placed on the testing and evaluation of data from two identical reactor modules, TEM-X S/N-3A and -3B, fabricated with refractory inner clads and thermoelectric circuits designed to maximize performance stability. Initial $\bar{T}_H = 1085^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$ performance data from these modules correlated very well with performance predictions. The first module successfully completed 1167 hr of steady-state testing and 50 scheduled thermal cycles with no measurable effect on performance. Subsequently, this module was removed from test for post-cyclic test destructive examination. TEM-X S/N-3B has accrued 1900 hr of steady-state testing during which power output data have remained constant within the band 281 ± 2 watts.

Two additional TEM-X S/N-3 series modules, designated C and D, were fabricated, processed, and prepared for test during the past quarter.

One TEM-13G series reactor module remained on test in the liquid metal loop system at Atomics International with performance characteristics unchanged from previously reported trends. This module, TEM-13G S/N-2, has accrued over 11,500 hr of successful operation at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ test conditions.

1. Refractory Clad Modules

Two TEM-X S/N-3 series modules, designated S/N-3A and S/N-3B, were placed on test during the past quarter at the design matched load operating conditions of $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$. The basic features of the TEM-X S/N-3 series module design include:

- 1) A refractory Ta - 10W inner cladding incorporated to provide stable cyclic operation.
- 2) Thermoelectric washer axial thicknesses of 0.188 in. to reduce the effects of intercouple diffusion, should it occur. This thickness is approximately twice the thickness used in the TEM-13G and TEM-X S/N-2 designs.

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TABLE VI-1
A COMPARISON OF CALCULATED AND EXPERIMENTAL DATA
FROM TEM-X S/N-3 SERIES MODULES

ROOM TEMPERATURE			B.O.L. $\bar{T}_H = 1085^\circ\text{F}$, $\bar{T}_C = 507^\circ\text{F}$					
	R (Milliohms) Circuit Module		R Circuit (Milliohms)	R Module (Milliohms)	V_{oc} (Volts)	P_e (Watts)	Q_{in} (Watts)	η_o (Pct)
TEM-X S/N-3A	2.7	3.0	24.9	24.9	5.24	276	5847	4.73
TEM-X S/N-3B	2.7	*	*	25.0	5.29	280	5933	4.72
Calculated	2.7	3.0	23.8	24.9	5.38	290	6010	4.63

* Pin Circuit Resistance Measurement Not Made on TEM-X S/N-3B.

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- 3) The number of conductor pins increased from four to eight to reduce the overall module resistance. TEM-X S/N-3 series modules have a circuit resistance approximately one-fourth that of TEM-13G series modules. In addition, the pins were threaded into the internal collector rings to assure positive retention of the pins.
- 4) Mica insulator thickness increased to 0.010 in. (two 0.005-in. insulators) to minimize intercouple diffusion.
- 5) 0.0005-in. tungsten foil washers extending radially outward from the couple hot junction (lead telluride/inner conductor rings interface) to a point midway between the hot junction and cold junction (lead telluride/outer conductor ring interface). The foil, "sandwiched" between the two 0.005-in. mica washers, was included to provide an absolute diffusion barrier in the region most susceptible to intercouple contamination resulting from diffusion of tellurium through the mica insulators.

Startup of the two TEM-X S/N-3 series modules in static test stands was accomplished without incident. Table VI-1 presents a comparison of the beginning-of-life calculated and the corresponding experimental parameters for TEM-X S/N-3A and S/N-3B. The module-to-module reproducibility is excellent and, in general, the correlation of experimental data with the calculated parameters is considered good. Although the measured beginning-of-life open circuit voltage was $\sim 2\%$ lower than calculated, the deviation is within the standard $\pm 3\%$ range. Variations in lead telluride material properties from module to module and experimental uncertainties (primarily in average clad temperatures) result in minor performance variations of this magnitude. The experimental data indicate that the heat input data from the two modules fall from 80 to 160 watts below the level calculated for the TEM-X S/N-3 design. The cause for this deviation is felt to be a result of two factors: experimental uncertainties can account for a large portion of the variation, the remainder is felt to stem from the conservative approach used to calculate heat transferred through the module in the calculational model, TEMOD.

Axial temperature variations within each couple of a module are assumed negligible in the calculational model. This is a good assumption for a non-barrier module in which there are no gross variations in thermal conductivity

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TABLE VI-2
TEM-X S/N-3A DATA SUMMARY

Time (Hours)	P _e (Watts)	R _g (Milliohms)	V _{oc} (Volts)	Q _{in} (Watts)
0.0	276	24.86	5.24	5847
11.0	276	25.07	5.26	5830
36.7	278	24.63	5.24	5841
107.1	278	24.88	5.26	5814
109.1	279	24.74	5.25	5858
111.1	281	24.79	5.28	5872
FIFTY 30°F/MINUTE THERMAL CYCLES PERFORMED				
541.6	276	24.89	5.24	5834
613.0	276	25.02	5.26	5820
642.9	277	24.88	5.25	5831
685.1	277	24.86	5.25	5836
781.0	278	24.82	5.26	5857
833.2	277	24.91	5.25	5842
1002.6	278	24.82	5.26	5837
1045.9	278	24.87	5.26	5819
1166.4	277	25.10	5.28	5872

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in the axial direction. Inclusion of the tungsten foil barrier as in the TEM-X S/N-3 design, however, produces a sharp discontinuity in local conductivity and a temperature variation results. The temperature of the hot junction in the vicinity of tungsten foil is lower than the "average" hot junction temperature. The inverse occurs at the outer extremities of the tungsten foil. As a result, the radial temperature drop across the tungsten is considerably less than the "average" temperature drop. Consequently the model, which uses the average temperatures to calculate heat fluxes, over-estimates the total heat flow.

This axial temperature variation, which is not accounted for in the code, could also explain the over-estimated voltages. Since the hot junction temperature is depressed locally at the point of nearest proximity between adjacent thermoelectric washers, the voltage will be slightly reduced.

After a brief steady-state operating period at design operating conditions, $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$, TEM-X S/N-3A was subjected to a series of controlled thermal cycles. Each controlled cycle consisted of a cooldown from design operating conditions to an intermediate $\bar{T}_H = 500^\circ\text{F}$, a 1-hr steady-state soak period, and a return to design operating conditions followed by another 1-hr soak. All transient operations were conducted at average hot clad temperature ramp rates of $30^\circ\text{F}/\text{min}$ in accordance with the module design specifications. In addition to 49 controlled thermal cycles, the module experienced one rapid shutdown as a result of a laboratory power failure. The power interruption occurred as the module was operating at the design conditions, resulting in a maximum cooldown temperature ramp rate of $\sim 460^\circ\text{F}/\text{min}$. Following the unscheduled shutdown, the module was returned to design operating conditions with a $30^\circ\text{F}/\text{min}$ heatup rate, and the controlled cyclic program continued.

TEM-X S/N-3A completed the thermal cyclic test with no measurable degradation in performance. Table VI-2 presents a summary of performance data recorded before and after cyclic testing of TEM-X S/N-3A. Module power output measured at the design operating conditions of $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$ remained constant at 279 ± 3 watts. This test was conducted to demonstrate the cyclic insensitivity of a module fabricated with a refractory inner clad in which total thermal expansion of the inner and outer clad are matched. The data show quite clearly that cyclic operation had no measurable effect on module performance. Similar results were achieved in a similar cyclic experiment performed

on a previous reactor power module, TEM-X S/N-2, also designed with a refractory inner clad.

Figures VI-1 through -4 present performance parameters (overall efficiency, electrical power output, internal resistance, and open circuit voltage), recorded during testing of the two TEM-X S/N-3 series modules. The data are plotted as a function of steady-state testing time. TEM-X S/N-3B is a steady-state test module which has experienced only one thermal cycle as a result of a laboratory power failure. The data presented in these figures indicate both the high degree of reproducibility of performance between identical modules and the complete lack of module degradation. No previous reactor length module, cycled or uncycled, has experienced this degree of performance stability.

After completion of thermal cyclic testing and an additional 600 hr of steady-state $\bar{T}_H = 1085^\circ\text{F}$ and $\bar{T}_C = 507^\circ\text{F}$ operation, TEM-X S/N-3A was cooled to room temperature to determine the cause of a sudden loss of the two uppermost cold clad thermocouples. Examination of the module revealed that liquid NaK had penetrated through the inner clad into the end closure region of the module. Subsequently the NaK had attacked the boron nitride electrical pin insulators and escaped to the outside of the module, attacking the cold side thermocouples. At the time of the thermocouple failures, module performance data gave no indication that NaK had attacked the circuit.

The preliminary findings of the destructive examination of TEM-X S/N-3A are discussed in a subsequent section of this report.

Based on the successful performance of the initial TEM-X S/N-3 series modules, two additional modules (designated S/N-3C and S/N-3D) were fabricated, processed, and prepared for test. These modules are identical to TEM-X S/N-3B except that inner conductor rings were selected from within the existing tolerance band to ensure an axial interference between the tungsten inserts and the adjacent mica insulators. This was done to prevent tellurium diffusion around the large ID mica washers during processing. Subsequent TEM-15 module experiments, discussed in Section VI-C of this report, are being designed to incorporate this "tungsten pinch."

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Figure VI-1. Overall Efficiency vs Elapsed Time for Two Refractory Clad Reactor Power Modules

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Figure VI-2. Electrical Power Output vs Elapsed Time for Two Refractory Clad Reactor Power Modules

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Figure VI-3. Internal Resistance vs Elapsed Time for Two Refractory Clad Reactor Power Modules

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Figure VI-4. Open Circuit Voltage vs Elapsed Time for Two Refractory Clad Reactor Power Modules

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2. Prototype 5.25-Volt Reactor Power Modules

TEM-13G S/N-2, a first generation 5.25-volt reactor power module fabricated with an Inconel inner clad, remained on test in a two-loop liquid metal system at Atomics International during the past Quarter. This module has accumulated a total of 11,373 hr of operation at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ operating conditions. Overall efficiency and electrical power output data are presented in Figures VI-5 and -6 as functions of elapsed time for TEM-13G S/N-2. Power output and overall efficiency degradation rates of 16 and 13%, respectively, have been determined for this module which has been thermally cycled 5 times. These data are in close agreement with similar data from TEM-13G S/N-4 which was previously tested at the same temperatures in a static test stand at WANL.

It is significant to note that the rate of degradation increased after each thermal cycle. This increased rate of degradation is caused by accelerated tellurium flow through mica cracks produced by component distortions during transient operation. More recent reactor power modules, viz. the TEM-X series incorporating refractory inner claddings, have exhibited greatly improved performance stability in comparison to these TEM-13G data by virtue of the reduced mica deformation. In addition, test data from TEM-X S/N-3A which was thermally cycled 50 times, showed that the improved performance stability is not altered by cyclic operation.

Previously reported performance trends for TEM-13G S/N-6, -7, -8, and -9, four modules incorporated into Converter Module S/N-1, continued during the past reporting period. Testing of Converter Module S/N-1 was interrupted on November 15, 1970 due to a heater power supply controller malfunction. The test will not be restarted since it was scheduled to be shut down on November 21, 1970. Approximately 7040 test hours at nominal $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ test conditions were accumulated on the Converter Module. Individual module degradation rates, both efficiency and power, ranged from 24 to 29%/10,000 hr in the four modules. The increased degradation is felt to be a result of increased component distortion resulting from the transmission of forces in the converter module assembly.

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Figure VI-5. Overall Efficiency vs Elapsed Time for TEM-13G S/N-2

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Figure VI-6. Electrical Power Output vs Elapsed Time for TEM-13G S/N-2

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TABLE VI-3
PUMP MODULE PERFORMANCE STABILITY SUMMARY

Module	Rate of Change: % Increase Per 10,000 Hours					Thermal Cycles		
	Testing Time (Hours)	R _g	V _{oc}	P _e	η ₁	Uncontrolled Number	Controlled	
							No.	Rate(°F/Min)
TEM-14A S/N-1	17542	**	**	-4.7	-6.2	10	0	-
TEM-14A S/N-2	14584	8.4	2.5	-3.2	-1.6	7	0	-
TEM-14A S/N-3*	7727	4.5	3.0	1.3	-0.4	2	50	30

* Testing Terminated

** No V_{oc} or R_g measurements made on TEM-14A S/N-1

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B. PUMP TUBULAR POWER MODULES

1. Performance Summary of Prototype Pump Modules

Two tubular modules designed to deliver power to an electromagnetic pump continued on test at WANL during the past Quarter. These prototype pump modules, designated TEM-14A S/N-1 and S/N-2, are being endurance tested at nominal average cladding temperatures of $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$.

Electrical current and electrical power output data from all TEM-14A series modules are presented in Figures VI-7 and -8, respectively. Data from TEM-14A S/N-3 which was removed from test for destructive examination after completing fifty thermal cycles with no measurable degradation, are included for comparison. Performance levels achieved in S/N-2 and S/N-3, with the number of nickel electrical pins increased from 6 to 12 to minimize electrical resistance, are in excellent agreement with the design levels (110 watts at 500 amp).

Table VI-3 presents a summary of performance stability data from the EM pump modules. The improved power output and efficiency stability exhibited by S/N-2 and -3 are felt to be a result of the stepped inner conductor ring design incorporated in the latter modules.

The long-term stable performance capabilities of the tubular module incorporating lead telluride thermoelectric materials are clearly demonstrated by the performance data from the TEM-14 series pump modules. Successful performance following the fifty controlled thermal cycles on S/N-3 (Figures VI-7 and -8) and the various uncontrolled shutdowns of all three modules indicate that the TEM-14 design is insensitive to thermal cycling.

C. SUPPORTING TECHNOLOGY DEVELOPMENT

1. Sublength Modules

The TEM-15 series modules were designed as a part of the support technology development phase of the Compact Thermoelectric Converter System Technology Program. These modules were designed with radial dimensions similar to those of the TEM-13G reactor power modules, but with circuit lengths reduced from 15.0 to 4.0 in. The purpose for fabricating and testing these modules is to evaluate the effects of design, processing, and fabrication modifications on

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Figure VI-7. Electrical Current Data From Three Prototype Pump Power Modules

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Figure VI-8. Power Output Data From Three Prototype Pump Power Modules

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TABLE VI-4
TEM-15 PERFORMANCE ANALYSIS SUMMARY

Module	Testing Time	Rate of Change: % Per 10,000 Hours				Thermal Cycles		
		R_g	V_{oc}	P_e	η	Uncontrolled No.	Controlled No.	Rate ($^{\circ}$ F/min)
TEM-15A S/N-1	9161	30	3	-19	-15	6	0	-
TEM-15A S/N-4*	2349	30	4	-22	- 8	2	0	-
TEM-15A S/N-4A	Not Tested	--	-	---	---	-	-	-
TEM-15A S/N-4B*	2174	30	6	-17	-20	0	50	2.5
TEM-15A S/N-4C	875	**	**	**	**	2	50	30
TEM-15A S/N-4D	9518	30	4	-17	-13	4	0	-
TEM-15A S/N-4E	4407	30	7	-16	-13	3	0	-
TEM-15A S/N-5	8688	28	3	-17	-12	7	0	-
TEM-15A S/N-8*	2442	29	2	-23	-13	2	50	60
TEM-15B S/N-4*	3637	12	6	0	- 2	2	0	-
TEM-15B S/N-4A	3498	15	5	- 3	- 1	1	0	-
TEM-15C S/N-4*	2753	29	7	-14	-14	2	0	-
TEM-15C S/N-8	6928	26	3	-17	-12	4	0	-
TEM-15D S/N-4	4857	18	3	-11	- 7	3	0	-

* Testing Terminated.

** Insufficient Testing Time to Allow Meaningful Performance Analysis.

All Modules Tested with $\bar{T}_H = 1125^{\circ}$ F, $\bar{T}_C = 570^{\circ}$ F.

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module performance. All TEM-15 series modules have been tested at nominal $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$ cladding temperatures in static test facilities at WANL. A list of all TEM-15 series modules fabricated to date is presented along with testing times in Table VI-4 and Process/Design Feature shown in Table VI-5. A discussion of the performance stability data is presented below.

a. TEM-15A Series Modules

TEM-15A series modules were designed to investigate the influence of minor component dimensional variations and the effects of the hydrogen purge operation on module performance stability, the influence of weld shrinkage on circuit alignment and mica deformation, and the influence of axial pre-load on mica deformation and circuit alignment in a module incorporating standard 0.005-in. thick mica insulators. No diffusion barriers have been incorporated in any of these modules.

Testing of four TEM-15A series modules, S/N-1, -4D, -4E, and -5, continued during the past month. Excellent long-term performance reproducibility has been achieved in all TEM-15A series modules. Electrical power output and overall efficiency data are presented in Figures VI-9 and -10, respectively, as a function of elapsed time for these four TEM-15A series modules. As can be seen in the figures, the TEM-15A data fall in a very narrow band, indicating that none of the above-mentioned design processing or fabrication modifications has had a gross effect on module performance.

Table VI-4 summarizes results of the most recent degradation rate analysis of all TEM-15 series modules having steady-state testing times in excess of 1000 hr (a minimum of 1000 hr is required to establish performance trends). Degradation rates for four parameters (resistance, open circuit voltage, power output, and overall efficiency, all adjusted to account for minor testing temperature variations) are reported.

Degradation rate data from the four long-term TEM-15A series modules remained essentially unchanged during the past Quarter, i. e., no changes in previously established performance stability trends were observed. These data indicate that degradation performance stability is relatively insensitive to the processing and dimensional variables under investigation in the TEM-15A series of modules.

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TABLE VI-5
PROCESS AND DESIGN FEATURES OF TEM-15 MODULES

Module	Process and Design Innovation
15A-1	50-mil washer; 5-mil mica; 1-mil mica radial clearance; EB welded; low axial preload
15A-4	baseline: same as above but TIG weld with biaxial preload
15A-4A	same as 15A-4
15A-4B	same as 15A-4
15A-4C	same as 15A-4
15A-4D	same as 15A-4
15A-4E	same as 15A-4 but no hydrogen purge
15A-5	same as 15A-4 but low axial preload, and EB weld, and 10-mil mica clearance
15A-8	same as 15A-4 but 10-mil mica clearance
15B-4	same as 15A-4 but two 4.5-mil mica pieces with 1/2-mil W foil full radius
15B-4A	same as 15A-4 but two 4.5-mil mica pieces with 1/2-mil W foil 1/2 radius
15C-4	same as 15A-4 but thin film molybdenum barrier between two 2-1/2-mil micas
15C-8	same as 15A-8 but thin film molybdenum barrier between two 2-1/2-mil micas
15D-4	same as 15A-4 but 10-mil mica

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Figure VI-9. A Comparison of Power Output Data From TEM-15A Series Modules

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Figure VI-10. A Comparison of Overall Efficiency Data
From TEM-15A Series Modules

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TEM-15A S/N-4E, the first TEM-15 series module in which the hydrogen purge step was eliminated, is exhibiting a degradation pattern identical to that of other TEM-15A series modules, indicating that performance stability is unaffected by the hydrogen purge. Electrical power and overall efficiency data from TEM-15A S/N-4E can be compared in Figures VI-9 and -10 to similar data from other TEM-15A series modules. Based on these data, it appears that the elimination of the hydrogen purge has no adverse effect on the BOL level of the performance parameters. This is important since TEM-X series modules fabricated with tantalum claddings are not being hydrogen purged to avoid the formation of hydrides.

Recently an additional TEM-15A series module, designated S/N-4F, was identified. This module is identical in design to all previous TEM-15A S/N-4 type modules except that minor component dimensional variations have been incorporated to ensure an interference between the tungsten inner conductor ring insert and the adjacent mica insulators at fabrication. This modification will eliminate tellurium flow around the inner extremity of the large mica insulator during purging operations where the module is subjected to temperatures up to 750° F prior to processing in which the assembly clearances are eliminated. Evidence of this tellurium flow has been observed during destructive examination of modules which were never tested, i. e., TEM-X S/N-3. Fabrication of TEM-15A S/N-4F has been initiated and testing should commence in early March.

b. TEM-15B Series Modules

All TEM-15B series modules utilize tungsten foil diffusion barriers positioned between two mica electrical insulators. The latest module of this series to be placed on test, TEM-15B S/N-4A, incorporates 0.0004 ± 0.0001 -in. tungsten foil extending radially outward from the module hot junction to a point midway between the hot and cold junction and sandwiched between two 0.005-in. mica washers. This is similar to the barrier design of TEM-X S/N-3 series modules discussed in Section VI-A of this report. The number of thermoelectric couples was reduced to 33 from 36 in TEM-15B S/N-4A to accommodate the increased mica thickness within the 4.0-in. circuit length.

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Figure VI-11. A Comparison of Power Output Data From TEM-15 Series Barrier Modules

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Electrical power output and overall efficiency data from TEM-15B S/N-4A are presented in Figures VI-11 and -12, respectively. Similar data from two other modules currently on test are discussed here. In addition, data from TEM-15B S/N-4, a module incorporating tungsten foil barriers extending radially outward from the hot junction to the cold junction, are presented. TEM-15B S/N-4 module was destructively examined after 3637 hr of $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$ degradation-free operation.

From the TEM-15B S/N-4A power output and overall efficiency data it is evident that its performance is higher than that of TEM-15B S/N-4. The improved performance is a result of the reduced tungsten radial thickness, which lessens shunt heat losses through the barrier. Overall degradation of TEM-15B S/N-4A has been virtually unmeasurable after 3500 hr of operation. Statistical analysis of the power output and overall efficiency data from this module (Table VI-4), however, indicates degradation rates of 3 and 1%/10,000 hr, respectively.

The performance of TEM-15B S/N-4 was the most stable recorded for a TEM-15 series module. As is shown in Table VI-4, the 12%/10,000-hr increase in resistance was completely offset by a 6%/10,000-hr increase in voltage. Thus, no degradation of power output was observed. The destructive examination of this module, however, did reveal that a greatly reduced, but measurable, contamination of the n-leg washer had occurred in the vicinity of the large diameter mica washer/inner conductor ring interface. It is felt that this contamination was primarily responsible for the increased resistance observed in TEM-15B S/N-4 and TEM-15B S/N-4A.

It is currently postulated that the tellurium diffuses from the p-type washer to the n-type washer around the ID of the mica-tungsten foil barrier assembly during processing. Figure VI-13 shows the schematic of the couple assembly in the TEM-15B S/N-4 design. The module components were dimensioned such that a potential 0.0035-in. axial gap existed between each tungsten insert and the adjacent mica washers. During purging and processing, a diffusion path around the mica washers may have initiated the contamination process, which continued during testing.

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Figure VI-12. A Comparison of Overall Efficiency Data From TEM-15 Series Barrier Modules

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Figure VI-13. Couple Schematics for TEM-15B Series Modules

An additional TEM-15B S/N-4 type module was identified during the past Quarter to eliminate this mode of degradation. This module, TEM-15B S/N-4B, is identical in design to TEM-15B S/N-4 except that inner conductor rings were resized to ensure a 0.001 to 0.002 in. interference between the tungsten inserts and the mica insulators. This modification should eliminate tellurium diffusion purging and processing. In addition, the outer diameter of the tungsten foil insulating barrier was reduced by 0.020 in. to prevent shorting of the foil to the outer conductor ring. Both modifications are shown in Figure VI-13. Fabrication of TEM-15B S/N-4B has been initiated and testing of this module should begin in early March.

c. TEM-15C Series Modules

TEM-15C series modules are defined as experiments in which thin films of molybdenum or tungsten formed by electron beam deposition are used to retard the transport of tellurium between adjacent thermoelectric washers. Two such modules have been fabricated and tested to date, TEM-15C S/N-4 and S/N-8, using 0.0025-in. thick mica sheets upon which 1000 to 2000 Å of molybdenum was deposited. Two washers are used at each mica position (molybdenum-coated

faces together) so that the total barrier thickness is 2000 to 4000 Å at each position. Since the total electron-beam deposited film is only ~3% as thick as the tungsten barrier used in the mica washers of TEM-15B S/N-4 shunt heat losses in TEM-15C series modules are negligible.

TEM-15C S/N-8 remained on test throughout the past Quarter. Power output and overall efficiency data from this module are compared to similar data from other barrier modules in Figures VI-11 and -12, respectively. The thin barriers have negligible shunt heat losses, resulting in substantially improved BOL performance in TEM-15C S/N-8. However, statistical analysis of performance data from both TEM-15C series modules presented in Table VI-4, indicates that the molybdenum barrier has been ineffective in reducing module degradation rates.

During the past month, an additional TEM-15C series module, S/N-4A, was identified. This module will incorporate a tungsten vapor deposited diffusion barrier. Details of the barrier are contingent upon success of a materials program currently underway. This module, however, will incorporate the component dimension variations included in the TEM-15A S/N-4F design, discussed above.

d. TEM-15D Series Modules

The TEM-15D designation applies to modules designed with 0.010-in. mica insulators which do not contain diffusion barrier mechanisms. Like TEM-15B S/N-4A, TEM-15D S/N-4 contains 33 thermoelectric couples as opposed to 36 used in all previously fabricated TEM-15 series modules. The reduction in the number of couples was required to accommodate the increased mica thickness within the 4.0-in. (nominal) circuit length.

TEM-15D S/N-4 has accrued 4857 hr of life testing at matched load static test conditions at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$. Power output and overall efficiency data from this module are presented in Figures VI-11 and -12. Reduction in BOL levels of both parameters (compared to TEM-15C S/N-8 and TEM-15A series module data) is the result of replacing a portion of the lead telluride with mica in this design.

The performance stability data from TEM-15D S/N-4, shown in Table VI-4, indicate that increased mica thickness does minimize degradation. However, the performance stability exhibited by TEM-15B S/N-4, designed with only 0.005-in. mica but, including a 0.0004-in. tungsten diffusion barrier, is significantly better than that of TEM-15D S/N-4.

e. Additional TEM-15 Module Experiments

During the past Quarter, a group of additional TEM-15 module experiments were identified. Three of these experiments involve minor variations from previous experiments. The modules incorporating these experiments, TEM-15A S/N-4F, TEM-15B S/N-4B, and TEM-15C S/N-4A, are described above. The remainder of the experiments incorporate basic component substitutions; thus the modules incorporating these experiments have been identified with new TEM-15 letter designations.

Recently, major emphasis in the Compact Thermoelectric Converter Systems Technology Program has been directed toward developing a lead telluride base ternary alloy (p-type PbTe-PbSe and N-type PbTe-GeTe) for incorporation into the tubular module. The goal of this program is to substantially improve module performance. Identification of the optimum dopant levels for these materials, discussed in this and previous progress reports, is imminent, and a series of TEM-15 series modules into which the new thermoelectric materials will be incorporated has been identified.

Since all newly defined modules incorporate the standard TIG clad-to-retainer weld, axial preload, and mica insulator dimensions, all are designated as S/N-4 modules consistent with designations used in all previously fabricated TEM-15 series modules. TEM-15 letter designations (i. e., 15A, 15B, etc.) are used primarily to distinguish between modules with alternate barrier mechanisms and alternate thermoelectric material combinations.

Table VI-6 summarizes the basic design features of all TEM-15 series modules, including the new experiments. As can be seen, E, F, and G series designations have been applied to modules in which various combinations of ternary lead tellurides (referred to as ANX and APX) are to be incorporated. All these modules will be fabricated with component dimensions sized to ensure an interference between the tungsten inner conductor ring inserts and adjacent mica insulators.

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Figure VI-14. TEM-15H S/N-4 Couple Assembly

Figure VI-15. Schematic of Inner Conductor Ring/Mica Junction Showing Probe Area

TABLE VI-6
TEM-15 MODULE BASIC DESIGN FEATURES

TEM Designation	P-leg	N-leg	Barrier	N-leg Inner Conductor
15A	TEGS-2P	GE-nl	None	Mo
15B	TEGS-2P	GE-nl	W foil	Mo
15C	TEGS-2P	GE-nl	W or Mo film	Mo
15D	TEGS-2P	GE-nl	Thick mica	Mo
15E	APX-4	GE-nl	None	Mo
15F	TEGS-2P	ANX	None	Mo
15G	APX	ANX	None	Mo
15H	TEGS-2P	GE-nl	None	W

The final module listed in Table VI-6 differs from TEM-15A series modules only in the design of the inner conductor ring assembly. The first module of this type, TEM-15H S/N-4, is designed with a couple assembly (Figure VI-14) having tungsten inserts under both lead telluride washers. This modification was made to eliminate a possible surface reaction between molybdenum and n-type lead telluride. Parts for this experiment have been obtained and the module is currently being fabricated. Testing should commence in mid-March.

2. Module Examination

Final destructive evaluation of the TEM-15B S/N-4 (a barrier module containing tungsten foil between mica washers) and TEM-15C S/N-4 was carried out via the electron beam microprobe. Both modules were examined at points where the large diameter mica meets the stepped inner conductor ring and also at the junction formed by the small diameter mica and the inner conductor ring. Figure VI-15 is a schematic of the typical areas where contaminants were found by the electron microprobe.

a. Microprobe Trace of
BSE + Te

b. Microprobe Trace of
BSE + Pb

Figure VI-16. TEM-15B S/N-4, Microprobe
Scans on Typical Area Shown Schematically
in Figure VI-15

Figure VI-16 is a set of photographs of probe traces taken on TEM-15B S/N-4. Figure VI-16a shows a back-scattered electron scan on which the probe trace for tellurium has been superimposed. Note the tellurium-rich areas indicated near the right top and bottom of the tungsten insert. Figure VI-16b shows the same area, but in this case a probe trace for lead was superimposed on the back-scattered electron scan. A comparison of these figures revealed that the Te-rich area at the top of Figure VI-16a was lead telluride between the tungsten inner ring and the mica. The Te-rich area at the lower edge of Figure VI-16a, however, contains no lead. A probe for molybdenum over this same area revealed that the tellurium at the bottom of the tungsten insert was combined with molybdenum to form a Mo_xTe_y compound.

It is postulated that the excess tellurium from the p-type washer had moved along the tungsten/mica interface until it contacted the molybdenum inner conductor ring. No Mo_xTe_y compound formation was detected at any point to the right (n-side) of the tungsten foil barrier. The Mo_xTe_y formation was detected only at the junctions similar to that shown in Figures VI-16a and -16b. Note that the location of the Mo-Te compound is the first point where excess tellurium atoms from the p-type washer have an opportunity to contact a material (molybdenum) with which it reacts.

E. B. probe examination of the junction formed by the small diameter mica and the inner conductor rings revealed no detectable tellurium or tellurium compounds in these areas. Interpreting the foregoing findings explains why contamination of the n-type washer has only been observed near the large mica/conductor ring junction. Fabrication procedures have previously ensured stack-up on the stepped conductor ring. As a result, the tungsten insert has been allowed to "float," resulting in a decreased impedance to tellurium migration in the large mica region. The small mica, on the other hand has been adequately pinched to prevent tellurium migration. The technique used to ensure positive pinch along the entire conductor/insert stack-up in modules currently being fabricated is described above. Providing the proper dimensional stack-up should prevent tellurium migration in both the large and small mica washer regions.

a. TEM-14A S/N-3

Additional examination of TEM-14A S/N-3 was conducted during this reporting period.

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Sections were cut from the module end and center, and disassembled to examine the interface formed by the inner conductor ring and the lead telluride washers. Samples were removed by scraping the outer diameters of the inner conductor rings and the inner diameters of the lead telluride washers. The results of the examination are as follows:

- 1) No evidence of compound formation or reaction products was found in samples scraped from the tungsten inner conductor rings nor from the inside diameter of the p-type washer.
- 2) Scrapings from the first inner conductor ring at the negative end of the module showed a strong PbMoO_4 pattern via x-ray diffraction. No such pattern was seen on films of the scrapings taken from the inside diameter of the matching lead telluride washer.
- 3) Scrapings from the module center n-type washer and inner conductor rings showed very weak patterns of PbMoO_4 , with the weaker pattern being from the sample scraped from the lead telluride washer. Previous samples scraped from another portion of the center ring and washer revealed no diffraction lines for PbMoO_4 .

Indications are that a slight reaction occurs between molybdenum and n-type lead telluride in the presence of a limited amount of oxygen. Tungsten, however, does not seem to exhibit the same phenomenon. The effect of the above reaction on module performance is not known, but the replacement of molybdenum by tungsten appears necessary to eliminate any possible deleterious reactions that could increase module degradation. It should be noted that despite this minimal surface contamination, no changes were noted in the Seebeck coefficient values of the n- and p-type washers, even near the conductor ring/thermoelectric junction region. In fact, there was no measured power degradation of this module after testing for 7727 hr at the $\bar{T}_H = 1125^\circ\text{F}$ (Table VI-3).

b. TEM-X S/N-3A

The performance and subsequent necessity for destructive examination of TEM-X S/N-3A were mentioned previously. The source of the NaK leak was traced to attack of the Ta - 10 W cladding, just inboard of the transition region on the outer diameter of the cladding. Subsequent ultrasonic testing showed the transition bond still intact. Metallographic examination of the cracked region

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disclosed that the cracks were initiated at the outer diameter of the cladding, i. e., the failure mode was not a consequence of NaK attack from the inner diameter. The negative end of the module, which was maintained at 1100°F during testing of the module (100°F higher than the positive end), was still intact. The preliminary analysis is that the failure was caused by the development of an air leak where the power pins emerge through the BN insulators at the end of the module. As a result, oxidation attack of the Ta - 10 W occurred which caused the cladding to crack. Further evaluation, including the possibility of an Alsimag 222/Ta - 10 W reaction, is being performed. It should be noted that TEM-X S/N-3B, a comparable module, has been on test for 1900 hr without any indication of inner cladding problems.

3. Mica Evaluation

Two series of EXC capsules containing high density (~96% of theoretical) lead telluride pellets have completed tests at 1202°F. The first pair of couples were on test for 1000 hr at temperature; the second pair for 2000 hr. In one capsule of each pair, one-half of the outer BN liner was replaced by a stainless steel sleeve to examine the effects of having the mica terminated in BN as opposed to metal.

The capsules have been checked for reduction of diameters after autoclaving. They are also being examined metallographically, and Seebeck probe traces are being taken along the capsule axis and across the pellet faces.

The original capsule diameter was nominally 0.690 in. In the capsules without the high density pellets, the diameter was reduced by a mean value of 0.025 in. The diameters of the high density material capsules were reduced by values of 0.015 in. In the region of the capsule where the stainless steel liner was used, the reduction was ~0.011 in. There was a large reduction across the stainless steel-BN interface where the diameter changed by ~0.015 in. Furthermore, the pellets of both sleeved capsules had significant deformation (cupped interfaces) in the non-sleeved half. In all of the capsules the mica barriers were cracked.

Seebeck probes were taken of the pellets of the two capsules tested for 1000 hr. The metal/BN sleeved capsule showed severe interface reactions (n-to-p transformation) and a significant depth penetration of tellurium in the BN

Figure VI-17. Effect of Reducing Excess Te:Na Ratio on the Seebeck Coefficient of P-Type $\text{PbTe}_{0.96}\text{Se}_{0.05}\text{Na}_{0.01}$

sleeved end. The interfaces under the metal sleeved end of the capsule had essentially no reaction. The other capsule, all BN outer sleeve, also had interface reactions but no significant depth penetration. The results suggest lower mica deformation is related to reduced contamination.

The two 2000-hr capsule tests have been concluded and these are in the process of being examined in a similar manner to the others in this series.

4. Improved Thermoelectric Materials

a. P-Type $\text{PbTe}_{0.95}\text{Se}_{0.05}$ Alloys

Preparation, fabrication, and testing of a series of specimens designed to help determine the minimum overall tellurium concentration at which adequate sodium solubility can be achieved were completed during this period. Ingots having the compositions,

were melted and converted to powder using standard techniques. Rectangular test bars were fabricated and processed using 650°C hydrogen heat treatments for both the powder and the cold pressed bars. Standard Seebeck coefficient and electrical resistivity measurements were then made over the temperature range 300 to 850°K.

Results of the above tests, together with Seebeck and resistivity results for a comparable specimen of the $\text{PbTe}_{0.96}\text{Se}_{0.05}\text{Na}_{0.01}$ "optimum" composition, are plotted in Figures VI-17 and -18. For purposes of the remarks to follow, we define the "excess Te" content of a particular composition to be the quantity

$$\frac{\text{Atom Fraction Te} + \text{Atom Fraction Se}}{\text{Atom Fraction Pb}} - 1.0 \quad .$$

Then we see that the four compositions under discussion contain excess Te:Na ratios as follows:

Figure VI-18. Effect of Reducing Excess Te:Na Ratio on the Electrical Resistivity of P-Type $\text{PbTe}_{0.96}\text{Se}_{0.05}\text{Na}_{0.01}$

<u>Composition</u>	<u>Designation</u>	<u>Excess Te:Na</u>
PbTe _{0.9600} Se _{0.05} Na _{0.01}	APX 4	1:1
PbTe _{0.9575} Se _{0.05} Na _{0.01}	APX 7	0.75:1
PbTe _{0.9550} Se _{0.05} Na _{0.01}	APX 8	0.50:1
PbTe _{0.9525} Se _{0.05} Na _{0.01}	APX 9	0.25:1

Figures VI-17 and -18 show that both the Seebeck and resistivity are increased as the excess Te:Na ratio is decreased below 1:1. The increases in these properties are due to a decreasing carrier concentration, which in turn is evidently due to a decreasing sodium solubility. The conclusion from just the above results is that a ratio >0.75:1 (and possibly even >1:1) is required to achieve complete sodium solubility at the 0.01 sodium level.

Further insight into the tellurium concentration - sodium solubility relationship was obtained from a second series of specimens having the compositions:

<u>Composition</u>	<u>Designation</u>	<u>Excess Te:Na</u>
PbTe _{1.010} Na _{0.008}	WP 1	1.25:1
PbTe _{1.008} Na _{0.008}	WP 2	1:1
PbTe _{1.006} Na _{0.008}	WP 3	0.75:1
PbTe _{1.004} Na _{0.008}	WP 4	0.50:1
PbTe _{1.002} Na _{0.008}	WP 5	0.25:1

These are variations on what is essentially the 3M Company TEGS-2P PbTe specimens, composed of PbTe_{1.01}Na_{0.008}, which were processed and tested similarly to the Se-bearing alloys previously described, except that the standard TEGS-2P processing temperatures of 550°C (powder) and 575°C (pressed test specimens) were used.

The results of testing these specimens are plotted in Figures VI-19 and -20. Again, it is seen that both Seebeck and resistivity are increased as the excess Te:Na ratio is decreased below 1:1, indicating (as before) a decreasing carrier concentration due to a decreasing sodium solubility. In this series, however,

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Figure VI-19. Effect of Reducing Excess Te:Na Ratio on the Seebeck Coefficient of P-Type $\text{PbTe}_{1.010}\text{Na}_{0.008}$

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Figure VI-20. Effect of Reducing Excess Te:Na Ratio on the Electrical Resistivity of P-Type $\text{PbTe}_{1.010}\text{Na}_{0.008}$

Figure VI-21. Thermal Conductivity of Ternary P-Type Thermoelectric Materials

we can see that increasing the ratio from 1:1 to 1.25:1 has no significant effect on the properties. (The small difference in Seebeck coefficients seen in Figure VI-19 is within the range of normal experimental deviations.)

The most significant indication of the above results is that a 1:1 ratio of excess Te:Na is adequate to ensure complete sodium solubility in either a binary p-type PbTe material or in the ternary $\text{Pb}_{0.95}\text{Se}_{0.05}\text{Te}$ - base compositions. Since (based on chemical analysis previously performed) the excess Te:Na ratio in the currently standard TEGS-2P PbTe module material is of order 1.4:1, it would appear that a module containing a 1:1 ratio material (binary or ternary) should be considerably superior to a TEGS-2P module with respect to tellurium transport degradation.

Thermal conductivity measurements over the temperature range 300 to 850 °K have been made by Resalab, Incorporated on a specimen of $\text{PbTe}_{0.96}\text{Se}_{0.05}\text{Na}_{0.01}$, the currently designated optimum composition in the series of PbTe-PbSe alloys. The results of these measurements are plotted in Figure VI-21 together with the RCA reference data for this $\text{PbTe}_{0.95}\text{Se}_{0.05}\text{Na}_{0.007}$ alloy. The correspondence between the two data sets is quite good, reinforcing the belief that use of the ternary p-type material will result in significantly improved module performance.

b. N-Type $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}$ and $\text{Pb}_{0.97}\text{Ge}_{0.03}\text{Te}$ Alloys

Continued evaluation of the five compositions

which gave virtually indistinguishable results in the optimization evaluation of cold-pressed specimens processed exclusively at 750 °C, was completed during this period. A series of 0.625-in.-diam pellets was cold pressed to 91% of theoretical density from powder which had been hydrogen heat treated at 750 °C.

Figure VI-22. Figure of Merit of n-Type Thermoelectric Materials

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After hydrogen heat treating the pellets at 750°C, each was loaded in a graphite-lined molybdenum die and hot pressed in helium for 30 min at 700°C and 6500 psi, giving a final density >99% of theoretical.

Seebeck and resistivity measurements were made on each pellet, after which estimated values for thermal conductivity and figure of merit were calculated. As a result of these calculations, the composition $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$ (WANL designation ANX 6) was designated as the optimum PbTe-GeTe alloy for module use in the 880 to 572°K operating temperature range. Figure VI-22 is a plot of the estimated figure of merit for this composition, together with the RCA reference data for their $\text{Pb}_{0.951}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.001}$ composition and also the best available data for GE-NL n-type PbTe, the currently standard module material. The higher figure of merit is due to an extremely low electrical resistance after hot pressing.

A large batch of material having this optimum composition is now being prepared. It will be used to make washers for an experimental module, TEM-15F S/N-4, and also to make thermal conductivity specimens for measurements at Resalab, Incorporated.

5. Deposition of Thin-Film Tellurium Barriers

Electron beam vapor deposition (EBVD) experiments concerned with application of tungsten films to mica sheet stock were conducted at both WANL and at Westinghouse Baltimore Systems Development Division during this period. In these experiments, the best results were obtained by (1) minimizing substrate temperature (a rather unusual technique in thin-film deposition work), and (2) cleaning the substrate surface by argon glow discharge bombardment prior to deposition.

Application of tungsten films to mica sheet was also investigated in several RF sputtering experiments performed at the Westinghouse Research and Development Center. Attempts were made to deposit tungsten directly onto mica and also an intermediate sputtered layer of aluminum oxide (Al_2O_3). No completely successful results were obtained, but a promising film was deposited onto a substrate which had been glow-discharge cleaned.

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In other EBVD experiments performed at Westinghouse Baltimore, preliminary investigations were made on deposition of tungsten on p- and n-type PbTe, iron on n-type PbTe, and iron on boron nitride. Promising results were obtained on each of these combinations, all of which have potential applicability to the various concepts for eliminating tellurium transport degradation in modules.

Installation and setup of a new WANL vacuum chamber, electron gun, and extensive fixturing (including argon-glow discharge apparatus) for performing EBVD experiments were completed during this period. An initial evaluation run, using an iron deposition source, was made without major problems. Experimental runs with this will shortly be initiated and continued on a regular basis.

6. Duplex Cladding Process

The bimetal inner cladding used in TEM-X series modules was redesigned in a joint configuration providing maximum strength. The bimetal joints used up to the present have had a joint bond length of 0.750 to 1.0 in. The new redesigned joint has a bond length of ~ 2 in. The processing temperatures were also adjusted to provide a minimum thickness diffusion zone between the dissimilar materials. Metallographic examination of three fabricated joints permitted the selection of the processing parameters for optimum joint configuration.

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VII. COMPONENT DEVELOPMENT

A. INTRODUCTION AND SUMMARY

Current component development efforts comprise the fuel element and core heat transfer and hydraulic studies and the component technology development. Reactor components of interest are the reactor control system, including the drum drive, actuator, bearing, and the 47 shieldable control drum, and reactor shielding system.

Nearly all the fuel element heater assemblies for the first planned heat transfer test have been fabricated and are being readied for installation. The remaining electrical heater assemblies are being completed. Tests on the selected heater assemblies, including one unit with nine thermocouples attached to the inner wall of the sheath, were performed in a water pool and are being conducted in a flowing temperature controlled NaK environment. The early test data indicate that the deviation of the thermocouple readouts, with the heater isothermal, are within the allowable tolerance. The modification and operational checkout of the NaK loop for conducting the heat transfer test was completed. Preliminary planning for conducting a similar test on finned elements, representative of the latest ZrH Reactor core design, was initiated.

It was concluded that continued hydraulic characteristics testing of the full core model of the S8DR was not warranted and all testing was terminated. The preliminary series of hydraulic tests on the 19-element model to evaluate the effects of the element pitch, fin height, and fin lead on the mixing of the flowing coolant between channels were also completed. The previously selected R-L-N (Right Hand-Left Hand-No Fin) configuration apparently provides acceptable mixing.

One S8DR design actuator successfully completed its extended life test with 25,931 hr and 299 thermal cycles to 1125°F. Similarly two sets of prototype actuator coils and an advanced bearing have completed ~16,000 hr of endurance testing at 1300 and 1500°F, respectively. These tests were temporarily halted to relocate them into recently renovated AEC test facilities at the Santa Susana Field Laboratory. The alumina sliding surfaces, without any dry film lubricant, exhibited frictional characteristics acceptable for control drum bearing use during the 5200 hr of testing at 1500°F.

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The development drum assembly, which experienced a failure of one of the six BeO reflectors during the thermal cycle and vibration testing, was rebuilt with the revised BeO retaining band and its attachment. This drum will be used for the thermal cycle and operational demonstration tests of the completed drum and drive system. The test assembly, which includes the mechanical mockup of the actuator, drive gears with the drum launch locking device, flexible couplings, drum assembly and the drum bearings, is being installed for the initial test.

The second development drum assembly, which incorporates the B_4C inserts in the Ta - 10 W absorber to increase the drum control worth and reduce weight, was also completed. This drum will be checked for control worth in the SCA-4A (SNAP Critical Assembly) and then be instrumented for the thermal test of the drum assembly. The drum assembly will be subjected to the expected operational thermal and nuclear heating by conducting the thermal test adjacent to the core of the STIR (Shield Test and Irradiation Reactor).

Material studies combined with the nuclear and thermal analysis have indicated that the CVD (Chemical Vapor Deposited) tungsten coating on the Nb - 1 Zr cladding around the BeO reflector and the plasma gun applied tungsten coating on the Ta - 10 W absorber may have high enough thermal emittances to maintain the temperatures at acceptable level. The respective tungsten coating is required to inhibit the oxygen absorption by the thin Nb - 1 Zr cladding and to minimize oxygen embrittlement of the heavier Ta - 10 W. The 12,200-hr test at 1800°F indicates that no compatibility problems are expected with any of the basic drum materials.

The shield examination dry box and the installation of the equipment for the disassembly examination of shields were completed. Preliminary operation indicated that the planned partial removal of the containment vessel of the LiH checkout shield could be accomplished and the examination was started. The design of the prototypical annular shield, which surrounds the reactor vessel in the 4 π shielded configuration, was completed and the manufacture of the containment was initiated.

B. FUEL ELEMENT HEAT TRANSFER STUDIES

Studies are in progress to generate empirical data on the heat transfer characteristics of NaK flow through channels formed by closely packed fuel

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elements, which are representative of the ZrH reactor core. Data from these tests will provide the basis for determining the local and average film coefficients between the flowing NaK and the fuel element cladding under simulated operational conditions of the reactor. In turn, more accurate prediction of the cladding and fuel temperatures can be made. Initial tests are scoped to provide data which will aid the post-test evaluation of the S8DR core and fuel elements. Similar studies are also planned to assess the effects of the spacer fins of the reference design ZrH reactor on these heat transfer characteristics.

1. S8DR Design Core and Fuel Elements

Effort is proceeding on the fabrication of simulated fuel element heaters which will be assembled in the test section in a close packed array (pitch/diameter = 1.00). This configuration will provide a reference for subsequent testing of finned elements of higher P/D's and will provide data for verification of analytical predictions of six theta temperature ripple vs heat flux for the test condition P/D and cladding thickness.

All but three of the heater assemblies required for the 19-element array have been fabricated. The remaining three are expected to be completed shortly. After receipt of the heater elements, extension wire must be joined to each lead of the 0.020-in. thermocouples. This operation is in progress for the elements previously received. Figure VII-1 shows a completed straight center element and a completed outer element containing an offset bend.

Modification of the NaK test loop in which the test section containing the heater elements is to be installed has been completed, and the system has been checked out with a pipe spool substituting for the test sections. The final item in upgrading the loop was the addition of a cold trap and economizer.

A production type straight center heater containing nine sheath thermocouples (one TC inoperative) and one thermocouple located in the center of the heater coil has been tested in water prior to testing in NaK. The purpose of the testing was to determine the cumulative effects of heater coil-to-sheath eccentricity, MgO nonhomogeneity, and internal sheath thermocouple installation irregularities on sheath thermocouple readings under isothermal conditions. Table VIII-1 illustrates typical results. It is seen that the maximum deviation in reading of any of the eight sheath thermocouples is 8° at the maximum power level of 5 kw.

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Figure VII-1. Heat Transfer Test Heater Assemblies

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TABLE VII-1
RESULTS OF HEATER COIL-TO-SHEATH TESTS

Heater PWR	H ₂ O Temperature	Sheath TC Readings (°F)								Maximum Deviation (°F)
		1	2	3	4	5	6	7	8	
1010	209	248	246	247	245	248	247	246	245	3
2035	210	265	264	265	262	266	262	263	263	4
3030	209	277	278	277	274	280	275	276	276	6
3994	209	287	290	289	285	292	285	287	288	7
5003	209	298	302	299	297	304	296	297	301	8

Calibration testing of this same straight heater section in NaK has begun and data are being taken. As shown in Figure VII-2 the heater is installed in a 2-in. section of pipe, and located within a 4-in. section of pipe. The purpose of the larger section of pipe is to facilitate bringing out the heater power and the TC leads. In the photograph, the test section is shown prior to installing insulation.

2. Reference ZrH Reactor Core With Finned Fuel Elements

A study of the requirements for testing the finned elements has begun. Of particular interest in this test is the experimental verification of the reduction in six theta temperature ripple of the fuel cladding by the addition of the fins. It appears that the existing test section can be utilized. The same general instrumentation scheme as presently utilized will be adopted, viz internal sheath thermocouples located on one plane and external NaK channel thermocouples. Only one fin configuration is planned to be tested.

C. FUEL ELEMENT AND CORE HYDRAULICS

Hydraulic studies are being conducted to characterize the coolant flow thru the S8DR and the Reference ZrH reactor cores. The parameters of interest for the S8DR core are the pressure variation in the inlet plenum and its effect on the individual channel flow, and pressure drop thru the core and thru the interfuel element flow channels, and for the Reference ZrH core configurations, inter-channel mixing and bundle pressure drop using finned tubes. Of particular interest are the affects of entrained gases on the coolant flow thru the S8DR core.

Figure VII-2. NaK Environment Heater Checkout Installation

Data based on water tests will be analyzed to calculate the NaK coolant flow characteristics. The results of these studies will provide significant information pertinent to the post-test evaluation of the S8DR and the basis for selecting the design and predicting the performance of the ZrH core.

1. S8DR Core Hydraulic Studies

The hydraulic studies previously reported in AI-AEC-12982 on the full core model had established the existence of twin vortices in the inlet plenum and some of the effects. Fair correlation between measured pressure distribution at the inlet flow orifices with the original design model had also been established.

2. ZrH Reactor Core Hydraulic Studies

The testing effort previously described was continued. In addition to the three configurations already tested, five additional configurations were tested (Table VII-2). The two fuel bundle configurations which were tested with unfinned rod and twisted ribbons in the flow channels (Figure VII-3) were not as

TABLE VII-2
CONFIGURATIONS FOR ZrH 19-ELEMENT MIXING STUDIES

Configuration	Fins per Element	Fin Wrap Direction	Fin Thickness (in.)	Fin Pitch (in.)	Fin Width (in.)	Element OD (in.)	Element Pitch (in.)
1	4	All same Direction	0.020	4	0.050	0.560	0.600
2	0	-	-	-	-		
3	3	R-N-L	0.030	4	0.050		
4*	0		0.010*	2*	0.140*		
5†	0		0.010†	2†	0.140†		
6	3	R-N-L	0.030	8	0.050		0.600
7	3	R-N-L	0.018	8	0.050		0.580
8	3	R-N-L	0.018	4	0.050	0.560	0.580

*Right-and-left-hand twisted fins (ribbons) were inserted in bundle flow channels with adjacent channels having fins of opposite twist.

†Left-hand twisted fins (ribbons) were inserted in every other flow channel with adjacent channels left open.

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Figure VII-3. Hydraulic Mixing Test Section With Twisted Ribbons in Flow Channels

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effective in promoting mixing as the previously tested configuration having elements with alternating fin-wrap orientation adjacent to an unfinned element - R-L-N arrangement. Therefore, the balance of the test effort was devoted to running configurations which would provide parametric data for the reference design R-L-N configuration (Figures VII-4, -5, and -6). Parameters varied with fin pitches of 4- and 8-in., fuel rod spacings of 0.040- and 0.020-in., and corresponding fin heights of 0.030- to 0.018 in.

Data recorded during these tests consisted of fuel bundle pressure drops as measured by a mercury manometer and a printed record of the variations of conductivity bridge voltages. Bridge voltage variations were produced by an injected salt stream moving past the conductivity probes mounted in selected fuel rods and facing into a flow channel.

During the test program several changes were made in the equipment and operating procedure. For Configurations 6 thru 8, the number of conductivity probes monitoring each flow channel of interest was increased to seven from four. For Configurations 7 and 8, entrance tubes were soldered over the flow holes feeding the two injected channels of interest (Figure VII-7). The salt injector was relocated to allow injection at the entrance to these tubes rather than at the bottom of the inlet grid plate. These modifications were designed to increase the homogenization of the flowing water stream as it entered the flow channel. During the testing of Configuration 8, the external salt injection system was revised to provide a manual valve which could be adjusted to decrease variation in the injection system flow reading. Also attempts were made to time the injection duration so that a mass balance assumption could be used to calculate the system volume and thus, by agreement of calculated volumes, provide a check on the indicated injected salt solution flowrate and concentration.

Pressure drop data for all configurations tested have been reduced and adjusted for the measured pressure drops of the inlet and outlet grid plates (Figures VII-8 and -9). The reduction methods of the conductivity data recorded during each run into the mixing coefficient have been a subject of considerable study. Methods utilized to date include the best fit of the data for each probe position of a channel to families of mixing curves generated from the theoretical model, and two methods of solving the theoretical equation to determine the mixing at each probe position in an individual channel, and then averaging these results.

Figure VII-4. Typical Right-Left-Neutral Bundle and Instrumentation Arrangement Used in Hydraulic Mixing Studies

Figure VII-5. Instrumented Elements With R-L-N Fins Used in Hydraulic Mixing Studies With Water-Salt Solutions

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Figure VII-6. Hydraulic Mixing Test Section With
R-L-N Finned Elements

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Figure VII-7. Modified Salt Solution Injection System Used in Mixing Studies

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Figure VII-8. Pressure Loss Across Test Section for Configurations 1, 2, 3, 4, and 6

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Figure VII-9. Pressure Loss Across Test Section for Configurations 7 and 8

The inferred interchannel mixing coefficients derived from these reduction methods continue to have relatively large uncertainties. Additional studies to improve the data reduction methods are being continued. The mixing coefficients, based on the selected results from these methods for Configurations 3, 6, and 7, are shown on Table VII-3. These data indicate that mixing for a given configuration is relatively unaffected by flow velocity within the range of interest.

TABLE VII-3
MIXING COEFFICIENTS AS DETERMINED IN WATER-SALT
SOLUTION TESTS

<u>Configuration 3 - 4-in. Fin Lead, 3-Fin, 40-mil Gap</u>			
Velocity	5 fps	10 fps	15 fps
Average	0.1470/in.	0.1708/in.	0.1912/in.
<u>Configuration 6 - 8-in. Fin Lead, 3-Fin, 40-mil Gap</u>			
Velocity	5 fps	10 fps	15 fps
Average	0.1544/in.	0.1420/in.	0.1494/in.
<u>Configuration 7 - 8-in. Fin Lead, 3-Fin, 20-mil Gap</u>			
Flow Velocity	5 fps	10 fps	15 fps
Average	0.090/in.	0.087/in.	0.089/in.

D. CONTROL DRUM ACTUATOR

The effort in this program is directed toward the development of advanced actuators for ZrH reactors.

1. Component Testing

Tests are being conducted to evaluate potentially high temperature, high reliability conductor materials for actuator coils. These tests are being performed on coils wet-wound with AI-1 cement on rectangular shaped alumina bobbins. Three different conductor materials have been tested or are under test as candidate material for the coils. Nickel clad silver and Cube Alloy are presently under test; Oxalloy has been previously tested. The nickel clad silver conductor consists of a fine silver core swaged in "A" nickel cladding.

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The composite wire is 80% silver by cross-sectional area. The Cube Alloy conductor is a copper base or matrix, dispersion hardened by addition of fine particles of beryllia. The Oxalloy conductor consists of an OFHC copper core swaged in a chrome-iron alloy (Type 430 stainless steel) cladding. The composite wire is 72% copper by cross-sectional area.

A performance test is underway on prototype actuator coils, built to the specifications of the bobbin shaped configuration and material, conductor size and number of turns, lead wire termination and fabrication process, representative of the advanced actuator design. The test items originally consisted of six coils wound with Cube Alloy, and six coils wound with nickel clad silver. After 40 thermal cycles and 2276 test hours of which 2063 hr were at the test temperature of 1300°F, two Cube Alloy coils failed. The coil failures were due to brittle fracture of the conductor near the termination to the lead wire. This area of conductor is subject to mechanical flexing during the coil fabrication process. The inherent stiffness of this conductor probably results in localized high stresses. This area of high stress is more subject to corrosion by the acidic solvent of encapsulant during the winding and curing cycles. After 103 thermal cycles and 12,458 test hours of which 11,509 hr were at the test temperature of 1300°F, a nickel-clad silver coil failed by open circuiting. Failure investigation of this coil was delayed until after the scheduled shutdown on December 16, 1970. The shutdown was necessary in order to move the system to its new location at the Santa Susana Field Laboratory. At the time of shutdown, the test history included 124 thermal cycles and 16,043 test hours, of which 14,944 hr were at 1300°F. Failure investigation to date has revealed that the conductor in the region of termination to the lead wire had lost its silver core by sublimation through cracks in the nickel cladding. This is the area subjected to cold working during the termination process. These cracks either developed or propagated as the test progressed. All the wire damage was in 1/2-in. of wire length. The break was similar to that observed in the 1400° conductor test.* Visual observation of the coils indicated some axial coil growth similar to that observed on the Oxalloy coils. This growth has not been observed in rapid thermal cycle testing nor in steady-state testing, but only in cycle dwell testing. In this mode of testing, the coils soak at 1300°F for ~120 hr, after which

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the temperature is reduced to 300°F for a short dwell period, then the coils are again soaked for 120 hr at 1300°F.

2. Actuator Performance

After completion of the S8DR design verification testing, one actuator, S/N Dev. 013, was continued in test to demonstrate extended life actuator performance at temperature. The total test time on this actuator is now 25,931 hr, 22,922 hr of which were at 1125°F. A total of 299 thermal cycles has been accumulated, 151 of which have been startup cycles. Startup cycling, conducted in conjunction with alternate thermal cycling, consists of operating the actuator continuously for 25 min. at rate, speed, and current, prior to thermal ramp, and after the maximum temperature is attained. These hours do not include the 5,340 hr of testing at ~1025°F, conducted on the actuator's mechanical parts before it was rebuilt to the latest S8DR design. Performance of this actuator remains normal. This test was also shut down for relocation to the Santa Susana Field Laboratory on December 19, 1970.

The extended life test consists of the following sequences subsequent to the 12,000-hr design verification test.

- 1) 3000-hr endurance run at 1125°F (completed)
- 2) 2000-hr dwell at 1125°F (completed)
- 3) 3000-hr endurance run at 1125°F (completed)
- 4) 2000-hr endurance run at 1125°F (completed)

3. Actuator Design

The design effort is continuing at a minimal level. The previously described mockup actuator, designed to provide an interface for the control drum drive train test discussed in Section VI-E, has been fabricated and assembled in the control drum drive system test fixture.

E. CONTROL DRUM BEARINGS

1. Developmental Bearing Test

A bearing simulated life test is being conducted to demonstrate operational capability of 20,000 hr at 1500°F and 10^{-8} torr. The bearing, with carbon

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graphite vs Al_2O_3 plated Ta - 10 W friction surfaces, has completed 16,350 hr at temperature. Testing has included two 1000-hr dwell periods and one for 500-hr, in addition to 12,000 hr with periodic bearing rotation. As shown on Figure VII-10, the friction torque at 1500°F has been in the 4.0 to 7.0 in.-lb range, well below the bearing specification limit of 18.0 in.-lb at 1350°F. The test was interrupted for relocation at the AEC field laboratory, Santa Susana. The friction torque, prior to the test interruption, was 6.6 in.-lb at 1500°F, 14.2 in.-lb at 800°F, and 21.4 in.-lb at ambient temperature. Friction-torque of 6.6 in.-lb is equivalent to a friction coefficient of 0.23 at the primary function surface.

2. Backup Bearing Materials Study

The friction evaluation test, of solid Al_2O_3 against Al_2O_3 plated Ta - 10 W as possible bearing couple, has been completed. The accumulated test time was 5200 hr in a vacuum 10^{-8} torr, of which 4832 hr of testing were at 1500°F. Just prior to completion of the test, the friction coefficients were as follows: 0.49 at 1500°F, 0.71 at 800°F, and 0.99 at ambient temperature for Test 1; and 0.34 at 1500°F, 0.44 at 800°F, and 0.48 at ambient temperature for Test 2.

Static friction of the two samples recurred during the test and is shown in Figure VII-11. The maximum friction coefficient of 0.54 would result in a friction torque of 15.4 in.-lb on the bearing of the configuration undergoing life test, which is well within bearing specification.

3. Bearing Performance in Controlled Environment

Studies to evaluate the effects of a partial oxidizing, high temperature vacuum environment on the carbon-graphite control drum bearings are planned. This environment simulates the reference ZrH reactor operational conditions of the bearings under which irradiation of the BeO will produce oxygen in the drum dry well. The test is designed to determine the relationship between friction and wear of carbon graphite as a function of its oxidation when operated as a bearing surface in contact with Al_2O_3 . Fabrication of test hardware and test fixturing is nearing completion.

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Figure VII-10. Advanced ZrH Bearing Simulated Life Test, Friction Torque at 1500°F, 10⁻⁸ torr

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Figure VII-11. Advanced ZrH Bearing Material Evaluation Test, Static Friction Coefficient at 1500°F, 10⁻⁸ torr

Figure VII-12. Poison Backed Development Drum No. 2

F. 4- π SHIELDABLE REFLECTOR

The objective of this project is to develop a 4- π shieldable reflector control drum for the ZrH reactor. This program currently consists of two main tasks:

- 1) Control Drum Development
- 2) Material Studies

1. Development Drums

The replacement BeO reflector and the new design BeO retaining bands were fabricated for Development Drum No. 1. This drum, reassembled without the cladding, will be tested as part of the drum drive system (Section VII-G). The cladding will be installed after completion of initial prelaunch thermal cycle launch simulation vibration tests, and prior to the long-term operational test.

All detail parts (Figures VII-12 and -13) for Development Drum No. 2 assembly have been completed. This drum is scheduled to be temporarily assembled for the critical test. The B₄C inserts in the absorber will be placed into the absorber holes and contained by mounting temporary end plates. The BeO reflectors will be placed on the absorber, located on their respective dowel pins and secured by wire wrapping. This will provide a drum configuration for measuring drum worth on the SCA-4A (SNAP Critical Assembly). After completion of critical tests, the drum will be disassembled for rework required for thermocouple installations and reassembly for the thermal gradient test in the STIR irradiation environment (Section VII-F-1).

a. Drum Rivet Studies

The design of banding for BeO block to the control drums was redesigned following fracture of one BeO block on Development Drum No. 1. The modifications include a thicker band to reduce the bearing stress at the attachment rivets and expands into undercut of the rivet hole for locking and shank expands to completely fill the rivet hole in the base absorber and the band. Examination of sectioned riveted samples indicated that these rivets meet all design requirements.

Test assembly of a typical BeO reflector attached to absorber simulating Ta - 10 W block with the revised banding design was built. This assembly will be subjected to 10 thermal cycles up to 1300°F, and then to launch load simulating random vibration.

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Figure VII-13. End View of Poison Backed Development Drum No. 2

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During Development Drum No. 1 test loosening of the temporary stainless steel cladding rivets were noted. A test program to demonstrate the reliability of the Ta - 10 W cladding rivets was initiated. Rivet installation samples have been prepared for simulated prelaunch thermal cycling; subsequently a pull test will be conducted to determine the force required to remove the rivet.

b. Drum Thermal Gradient Test

A test to evaluate the combined thermal and nuclear heating effects on a control drum assembly in a capsule placed near the face of the STIR (Shield Testing and Irradiation Reactor) is being prepared. Drum temperature profiles and drum bowing will be measured during testing.

A detailed layout drawing of the test capsule was completed. The detail drawings of the instrumented drum assembly and the test assembly are being made.

Development Drum No. 2 is to be modified to accept thermocouples for this test. Most of these thermocouples are to be placed in the drum's upper three BeO blocks, leaving the lower three intact for post-test examination. The temperature profiles measured by these thermocouples will be used for comparison to and refinement of the thermal model of the drum.

The nuclear model for the test predicts a heat generating rate of 2.8 kw for the drum with its poison turned in toward the STIR reactor when the reactor is operating at 1 Mwt. A 2.1-kw power output is predicted with the drum's BeO reflector turned toward the reactor. The three-dimensional thermal model of the control drum has been extended to include the lower bearings.

The procurement of the long lead items, the thermocouples and heat pipe assembly, has been started.

2. Material Studies

The material study effort consists of: (1) Material Selection, (2) Material Compatibility Testing, (3) Emittance Measurements, and (4) Coating Development and Testing.

a. Material Selection

The material and coating selections that remain unchanged are:

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Figure VII-14. Compatibility Stack After 12,211 hr
at 1800°F and 10^{-7} torr

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Absorber — Ta - 10 W with possibility at plasma sprayed tungsten and an outer high emittance coat of plasma sprayed iron titanate. B₄C slugs (1.047-in. diam) are inserted into Ta - 10 W.

Band — Cb - 1 Zr coated with tungsten by a chemical vapor deposition process (CVD)

Cladding — CVD tungsten applied to Cb - 1 Zr followed possibility by a thin coat of plasma sprayed tungsten and an outer coat of plasma sprayed iron titanate.

Dry Well — Type 316 stainless steel with inner surface oxidized for high emittance.

A re-evaluation of the emittance coating needs is underway. The original requirement was for surface emittances in excess of 0.60, but with more accurate data on heat generation in the drum materials becoming available and with a lowering of the power level of the reference reactor, the emittance may not need to be that high. The re-evaluation consists of having emittance measurements made on the minimum coating which is chemical-vapor deposited-tungsten on the Cb - 1 Zr bands and cladding, and the bare Ta - 10 W absorber. A thermal analysis will be made with these emittances and, if temperatures are at an acceptable level, the iron titanate outer coat and plasma-sprayed tungsten will be deleted.

b. Compatibility Test

The bare sample compatibility test was continued to 12,211 hr at 1800°F in a low 10⁻⁷ torr pressure and then, shut down. Visual examination shows a severe reaction between Hastelloy-N and Ta - 10 W at two locations in the stack (Figure VII-14). This is similar to the appearance of the stack that was removed and examined* after 4000 hr wherein the extruded portions of material were binary systems of Ta - Mo, Ni - Ta, and Ni - Mo. Hastelloy-N was deleted from consideration for use in the drum at that time. No reactions are visually evident at interfaces representative of reference drum material couples (Ta - 10 W - BeO, Ta - 10 W - Cb, or BeO - Cb).

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Figure VII-15. Drum Drive Train Test Assembly

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c. Emittance Measurements

The emittances of Type 316 stainless steel samples that had been oxidized by a wet hydrogen process were measured and found to be 0.90 at 1000°F, and 0.93 at 1100 and 1200°F.

d. Coating Development and Testing

The CVD-W oxygen barrier will be required on the bands and cladding in any eventuality and development of full cladding coating has proceeded. Four claddings were CVD-W coated. Two were sound in all respects and two had slight blistering of 2 to 4 sq in. on the surface. A fifth cladding had poor adherence; a dark film developed on the substrate and on the backside of the flaked coating. The cladding was stripped and an attempt to recoat gave the same results. Small coupons were coated successfully by the same process after the unsuccessful cladding attempts. A review of the materials and process steps will be made to determine the cause of the poor adherence.

G. CONTROL DRUM DRIVE SYSTEM

The control drive system for the ZrH reactor will be tested under simulated launch loading and under simulated reactor thermal vacuum conditions. The test drive system consists of the rebuilt control Drum No. 1 and supporting bearings, a mechanical mockup of the actuator with locking cam and drive gears, and the drive shaft with flexible couplings as shown on Figure VII-15.

All test parts and components for the test, including application of dry film lubricant to the drive gear and rebuilding of the control drum, have been completed. Fabrication of the thermal test fixture has been completed, and assembly of the test hardware and fixturing is in progress.

H. SHIELD FABRICATION DEVELOPMENT

1. LiH Checkout Shield

A large diameter, thin pancake, shield assembly has been designed, fabricated, and filled with LiH by a casting method. The large shield examination dry box was prepared to conduct the final disassembly examination of the LiH checkout shield. The shield interior will be examined by cutting into and removing, the dome and the sides of the stainless steel container. The large positioner

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Figure VII-16. Shield Positioner in the Examination Dry Box

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(Figure VII-16) which will be used to move the shield to various positions during the examination was installed within the box. The shield, mounted on the table, can be remotely rotated, tilted, and moved vertically and horizontally. The large radial saw, capable of driving various cutting wheels, including a 22-in. diamond wheel used to cut the LiH, was also installed.

The 82-in.-diam checkout shield was mounted to a large aluminum adapter plate and attached to the positioner table.

A series of trial cuts were made on the stainless steel dome using an abrasive wheel to assure that all areas could be reached easily. These trial cuttings provided the assurance that all operations necessary for the planned disassembled examination could be carried out from outside the box. Disassembly of the shield assembly in the dry box was started. The result of this examination should establish the fabrication feasibility of this shield configuration.

2. LiH Annular Shield

The design and fabrication of other typical shield configurations are being developed. One configuration is an annular LiH shield assembly, which surrounds the reactor vessel assembly.

The manufacturing drawings for the LiH annular shield previously discussed^{*} were completed. A layout drawing of the LiH annular shield is shown in Figure VII-17.

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VIII. FUEL DEVELOPMENT

This program has the primary objective of producing a fuel element which meets the design requirements of both the Test Reactor and the Advanced ZrH Reactor. Inherent in this objective is the generation of the basic material properties required for fuel-element design and analyses, and the improvement of fabrication processes to ensure that the design goals are achieved. To meet this broad objective, variations in Incoloy 800 composition are being investigated. This work includes both the coating-processing parameters and the resultant performance in simulated reactor environments. Similar work is being conducted on backup cladding material but at a lower level of effort. Techniques for manufacture of integral finned cladding are also under investigation. A vapor-deposition process is being developed as the technique for incorporating burnable poison in the fuel element. Revisions in the design and fuel-element assembly processes are being made to meet new design requirements such as segmented fuel, vibration suppression, and helium backfill.

A. FUEL-ELEMENT MATERIALS EVALUATION

1. Cladding Processing

a. Finned Cladding

Inspection of twelve 25-mil wall finned tubes, the second group to be machined by hobbing, was completed. Direct destructive measurements were taken to support Vidigage wall thickness determinations. A rather consistent 2-1/2 mil variation in the wall thickness between fins was found, with the heavier area centered between the fins. This condition did not exist in the previous group of 15-mil wall finned tubes; it is attributed to a slight offset in the angular setting of the cutter with respect to the tube. Aside from this consistent variation, the greatest wall variation within a tube was 3 mils, with an average of ~ 1.5 mils. Variation of the wall between tubes averaged 2.1 mils. Assuming misalignment of the cutter was responsible for the 2.5-mil variation between fins, the dimensional consistency of these tubes was very good. A follow-on order has been placed with the vendor for 45 additional tubes.

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Approximately 1500 ft of 0.540-ID by 0.050-in. wall Incoloy 800 tubing was received and processed through Receiving Inspection. This tubing was fabricated from the three new heats of Incoloy 800 (89339, 89630, and 89676), with 500 ft being drawn from each heat.

b. Coating

To reduce the stresses in Incoloy 800 cladding, it has been determined that the hydrogen barrier thickness must be controlled to a maximum thickness of 2.5 mils. All fuel element test data to date have been based on a 1-mil minimum coating thickness. In the current coating application process, each of two coats is drained from the open end of a blind-end tube assembly before the firing operation. The result is a tapered coating thickness ranging from a nominal 1.2 mils at the blind end to 3 mils or greater at the open (closure) end. The 2.5-mil maximum thickness can be achieved by reducing the slip viscosity. However, tubes coated in this manner have exhibited significant areas at the blind end with <1-mil coating thickness.

Developmental efforts to date have indicated that a "reverse drain" coating application process may be a major improvement. This technique involves coating an open ended tube, butt welding on a coated end cap, and then applying a second coat which is drained the reverse direction from the first coat. It appears to provide a reduction of the coating taper inherent with the standard coating process, and approaches a coating thickness of 2 mils. Initial data on tubes coated by this process were reported last quarter. An additional group of five tubes has been coated, of which one has been evaluated. A summary of the measurements obtained to date is given below.

Tube No.	Coating Thickness (mils)					
	Open End		Center		Blind End	
	Average	Range	Average	Range	Average	Range
1	1.7	1.3 to 2.0	1.7	1.5 to 1.9	1.5	1.2 to 1.8
2	1.6	1.4 to 1.9	1.8	1.6 to 2.0	1.4	1.2 to 1.7
3	1.5	1.1 to 1.9	1.8	1.6 to 2.0	1.5	1.1 to 1.7
4	1.6	1.3 to 2.0	1.8	1.7 to 2.0	1.3	1.1 to 1.5

2. Fuel Processing

a. Burnable Poison

The current reference ZrH reactor core design includes \$12 worth of ^{155}Gd and ^{151}Eu mixture. The reference form of the burnable poison is a thin film (1 to 2μ) of stabilized rare earth oxides, vapor deposited onto the cylindrical surface of each U-Zr-H fuel rod. The reference method for depositing this burnable poison film is bent electron beam bombardment heating, evaporating each constituent oxide from its own crucible and allowing the oxides to condense on the fuel rod surface.

Four 4-in. fuel rods coated with a sandwich-type burnable poison film were placed on test during the previous quarter. This sandwich-type film consisted of a $0.3\text{-}\mu$ base layer of Y_2O_3 , overlaid with a $0.9\text{-}\mu$ co-deposited layer of Eu_2O_3 , Sm_2O_3 and Y_2O_3 , which was then overlaid with a $0.4\text{-}\mu$ top layer of Y_2O_3 . Each of the four test elements also contained a companion uncoated fuel rod segment and two spacers, to simplify the evaluation of coating performance. Hydrogen barrier regions in contact with the burnable poison could be compared with those barrier regions in contact with the uncoated fuel or those barrier regions prevented, by the spacers, from any contact. Three of the four elements were removed from test during the present quarter. The Y_2O_3 stabilized films in all three elements showed little or no change in appearance through 1352 hr at 1500°F and 1880 hr at 1400°F . There was no change apparent in the barrier adjacent to the Y_2O_3 stabilized poison film in the 1400°F test and only a slight change in the reflectivity of the adjacent barriers in the 1500°F test. The usual fuel-barrier reaction was visible only on the barrier surfaces adjacent to the uncoated fuel. The Y_2O_3 appears to stabilize the Eu_2O_3 rich film and prevent the fuel-barrier reaction.

A series of coated and uncoated Type 316 stainless steel and molybdenum foils which could possibly be used as carriers for the burnable poisons were placed in contact (nominal 500 psi) with Y_2O_3 coated and uncoated fuel rod segments and with glass barrier coated cladding wafers. These tests were terminated during the present quarter after 1000 hr at 1500°F and 2283 hr at 1430°F . The nickel in the Type 316 stainless steel foils that were not protected by Y_2O_3 films had reacted extensively with free uranium in the adjacent fuel slugs (melting or incipient melting of large areas of the foil). The Type 316 stainless steel

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foils protected by Y_2O_3 films showed no change in appearance. The Y_2O_3 coating seems to prevent reaction between the fuel and the stainless steel. It should be noted that in earlier tests with Type 316 stainless steel foils which were in slight contact with the fuel, there had been no visible reaction between fuel and foil although one had been expected. Apparently, in the earlier tests, the lack of continued intimate contact between stainless steel and fuel was sufficient to prevent the reaction. There was no visible reaction of the molybdenum foils with either the fuel or the glass barrier, therefore molybdenum may be considered acceptable as a compatible poison carrier.

Coating of fuel rod segments at a vendor facility was completed. Gd/Eu atom ratios of 6/3, 4/6, and 2/9 were employed, with and without the addition of Y_2O_3 as a stabilizer for the Eu_2O_3 . The fuel rods were cut into 1-in. segments and assembled into test elements. Each element contains three uncoated segments and fourteen different coated segments. The elements were placed on test at temperatures of 1300, 1400, and 1500°F. Fuel discs which were coated at the same time as the fuel rods were assembled in two compatibility capsules and placed on test at 1430°F.

One-inch coated fuel segments from the fourteen vendor coating runs were tested for coating adherence by re-hydriding the coated segments to a higher hydrogen content, thereby increasing the fuel element size. The fourteen segments were increased from 1.2 to 1.8 mils in diameter (a circumferential growth of 4 to 6 mils (0.2 to 0.3%). No change in coating and adherence or continuity was noted, but some of the high Eu_2O_3 coatings (Gd/Eu $\approx$ 2/9) did change in color and texture.

Approvals were obtained, and an order has been placed for bent beam gun vapor coating equipment to be delivered next quarter.

3. Fuel Element Assembly and Test

a. Developmental Element Tests

A series of developmental elements was assembled during the report period which contained segmented fuel (four slugs 4.5-in. long), helium backfill (70 mm) and the vibration suppressor inside the blind end. Difficulty was experienced during the shrink-fit and blend-cycle phase of assembly and, almost without exception, these elements exhibited excessively high leak rates. It was observed,

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during the shrink-fit/blending cycle heating, that the fuel was being heated sufficiently to cause a significant hydrogen dissociation pressure. This was evidenced by the pressure within the assembly retort essentially doubling during the closure operations. Hydrogen flow out of the closure area probably prevented complete closure seal. A series of elements was then assembled without the vibration suppressor, which lowered the fuel essentially out of the heated closure zone. Acceptable leak rates were then obtained, as illustrated by the data in Table VIII-1. It must be concluded that having the fuel in the heated zone is not compatible with the cup plug closure technique. As a consequence, the vibration suppressor will be redesigned and located at the closure end.

TABLE VIII-1
15-MIL INCOLOY CLAD "REFERENCE" ELEMENTS WITH FUEL
IN/OUT OF CLOSURE ZONE

Type Element	Element No.	Normalized Leak Rate, cc(STP)/hr	
		(1300°F, 2.1 atm)	(1400°F, 2 atm)
"Reference" - segmented fuel, helium backfill, vibration suppressor (Fuel in closure zone)	9531	1.97	2.78
	9532	2.77	3.90
	9533	1.02	1.46
	9534	2.06	2.91
"Reference" - less vibration suppressor (Fuel out of closure zone)	9530	0.53	0.79
	9535	0.00	0.01
	9538	0.31	0.47

Nine 25-mil wall Incoloy clad elements were assembled and tested. The cladding was integral finned Incoloy 800 tubing, fabricated by hobbing, with four 20-mil high fins. Five tubes were coated using the reverse drain technique and four were coated by the standard technique. In both cases, the applied coating weight was 30% less than standard in an attempt to reduce the maximum coating thickness to 2 mils and minimize the overall axial coating taper. The elements were assembled with segmented fuel and the helium backfill. Vibration suppressors were not included to keep the fuel out of the closure zone.

TABLE VIII-2
FINNED INCOLOY CLAD ELEMENT PERFORMANCE

Coating Method	Element No.	"As-Built" Permeation Rates, dc(STP)/hr			Post Thermal Cycles Permeation Rates, cc(STP)/hr		
		As-Measured		Normalized to 2 atm at 1400° F	As-Measured		Normalized to 2 atm at 1400° F
		1300° F	1400° F		1300° F	1400° F	
Reverse Drain With 30% Reduced Coating Weight	F-28	0.07	0.17	0.30	0.11	0.18	0.29
	F-29	0.07	0.35	0.40	0.18	0.42	0.48
	F-30	0.07	0.25	0.35	0.10	0.26	0.38
	F-31	0.09	0.28	0.38	0.11	0.34	0.38
	F-32*	0.80	2.31	2.21	-	-	-
Standard With 30% Reduction in Applied Coating Weight	F-24	1.17	-	3.13			
	F-25	0.74	-	2.78			
	F-26	0.84	-	2.91			
	F-27	1.35	-	3.80			

*Fuel located partially up into the closure zone.

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The leak rate history of these elements is given in Table VIII-2. The "as-built" leak rates of the reverse drained elements were quite acceptable, with the exception of F-32. This element was assembled with the fuel partially up into the closure zone, with the results substantiating the conclusions drawn previously. The four good elements were then subjected to five "A ramp" thermal cycles and then retested. No significant change was observed. These elements are now being subjected to 1000 hr of endurance at 1400°F isothermal. The "standard" coated elements exhibited leak rates which were excessive. Although these elements have not been decanned, it is believed that the high leak rates are due to excessively thin SCB coatings in the blind-end cap area. This is based upon the destructive examination of tubes more recently coated in the same manner. It was found that thin coating in the blind end area is not readily detectable with the usual boroscopic inspection. Zone permeation scanning of these elements is in progress. The results obtained from F-24 tend to substantiate this theory, inasmuch as 79% of the leakage was from 1-1/4 in. of the blind end.

The straightness of the nine finned 25-mil cladding tubes was measured at various processing steps. No attempt was made to straighten the tubes at any time. These data are summarized in Table VIII-3 and indicate excellent straightness stability, confirming earlier data obtained from 15-mil wall finned tubes.*

The seven 15-mil Incoloy 800 clad developmental elements (four double-blind-end and three cup-plug type) continued on test and have now accrued over 17,600 hr at 1400°F with an average hydrogen loss of 7.8%. The four double blind end elements have lost an average of 7.6% and the three cup-plug type an average of 8.1%. Curves of the average normalized leak rate and cumulative hydrogen loss are shown in Figures VIII-1 and -2, respectively.

The four double blind end type elements containing enriched uranium fuel rods have now accrued 16,900 hr at 1400°F. The average normalized leak rate and cumulative hydrogen loss of this group are shown in Figures VIII-3 and -4, respectively. The comparatively poor performance, when compared to the group of normal U elements, is primarily due to one high leaking element. Excluding this element, the remaining three have an average normalized leak rate of 0.61 cc(STP)/hr, and an average cumulative hydrogen loss of 7.4%.

*SNAP Reactor Programs Progress Report, May-July 1970, AI-AEC-12975

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Figure VIII-1. Average Normalized Leak Rate of the Seven Incoloy 800-Clad Developmental Fuel Elements (Four Double-Blind-End Type and Three Cup-Plug)

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Figure VIII-2. Cumulative Average Hydrogen Loss for the Seven Incoloy 800-Clad Developmental Fuel Elements

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Figure VIII-3. Average Normalized Leak Rate of the Four Enriched Uranium Double-Blind-End Incoloy 800-Clad Developmental Elements

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Figure VIII-4. Cumulative Average Hydrogen Loss for Four Enriched Uranium Double-Blind-End Incoloy 800-Clad Developmental Elements

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TABLE VIII-3
STRAIGHTNESS OF 25-MIL WALL FINNED INCOLOY CLADDING

Tube/Element Number	TIR (mils)			
	As- Hobbed	After Coating	After Element Tests	Approximate Hours at Temperature
F-24	10	15	12	120
F-25	12	12	8	264
F-26	11	15	-	264
F-27	13	12	8	120
F-28	12	12	11	532*
F-29	12	12	11	532*
F-30	17	11	12	420*
F-31	12	12	15	444*
F-32	<u>13</u>	<u>15</u>	<u>-</u>	192
Average	12	13	11	

*Includes five "A-Ramp" thermal cycles

b. Special Tests

The allowable stress in the hydrogen barrier is an important criterion affecting the design of the Test Reactor and Advanced ZrH Reactor. Special tests are being conducted to determine the SCB hydrogen retention capability as a function of temperature and stress. These tests and the data obtained are discussed below.

(1) Barrier Creep - Permeation Tests

Six furnace systems have been modified in order to test Incoloy 800 fuel elements under uniaxial creep conditions while monitoring the hydrogen leak rates. An initial group of elements was fabricated for the test program. The initial short term tests will be performed at 1200 and 1300°F with loads producing a strain rate of 10^{-3} in./in./hr in secondary creep. The complete test matrix will be developed after analysis of the short term test data.

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(2) Fuel Element Constant Deflection

Work was initiated on a test to evaluate fuel element performance when subjected to a constant deflection such as could be experienced in a fin-on-element contact load situation. The objectives are to determine the load-carrying capability of the element without damaging the hydrogen barrier, and to obtain a quantitative measure of the load relaxation experienced at a constant deflection as a function of time and temperature. The test apparatus was designed and component fabrication was initiated. The test equipment will be capable of continuous measurement of hydrogen leak rates under various conditions of temperature, amount and location of deflection, and load relaxation.

4. Cladding Properties

An investigation of the mechanical properties of Incoloy 800 is being performed to evaluate its potential as a SNAP reactor cladding material. Five heats of Incoloy 800 are being evaluated in this study. Their chemistries are given in Table VIII-4.

TABLE VIII-4
CHEMICAL ANALYSIS OF THE INCOLOY 800 HEATS
AVAILABLE FOR TESTING

Element	Heat No.				
	810535	HH7531A	K89676*	K89339*	K89630*
Carbon	0.04	0.04	0.01	0.03	0.09
Titanium	0.37	0.17	0.10	0.25	0.47
Aluminum	0.38	0.17	0.19	~0.10	0.21
Chromium	19.74	18.24	21.27	21.44	21.35
Nickel	34.14	33.02	33.09	33.54	33.39
Manganese	0.55	1.02	0.69	0.61	0.64
Silicon	0.41	0.27	0.32	0.29	0.27
Phosphorus	0.011	?	0.012	0.010	0.014
Sulfur	0.014	0.007	0.006	0.005	0.005
Molybdenum	?	?	0.10	0.06	0.07
Copper	0.17	0.17	0.02	0.04	0.03
Cobalt	0.09	?	0.02	0.04	0.04
Boron	<0.0005	0.0001	<0.001	<0.001	<0.001
Nitrogen	?	0.015	0.020	0.011	0.011
Iron	Balance	Balance	Balance	Balance	Balance

*Designated K-76, K-39, and K-30 for simplicity

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Figure VIII-5. Biaxial Creep Ductility Data for Heat No. 810535 at 1300°F

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Figure VIII-6. Biaxial Creep Ductility Data for Incoloy 800 at 1100, 1200, and 1400°F (Heat No. HH7531A)

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The biaxial stress rupture properties of SCB coated Incoloy 800 tubing from two of these heats (810535 and HH7531A) have been measured over a wide range of conditions. Irradiated material from the first heat (810535) exhibited diametral ductilities ranging from 1.3 to 11.7% with an average value of $\sim 4\%$ at 1300°F (Figure VIII-5). Whereas material from the second heat (HH7531A) yielded several data points below 1% $\Delta D/D$ at 1100, 1200, and 1400°F (Figure VIII-6). These two sets of data indicate (1) a possible heat-to-heat variation, and (2) that Heat 810535 has "reasonable" biaxial ductility over widely varying strain rates in the irradiated condition.

Stress rupture data recently obtained for fully processed Incoloy 800 in 1300°F flowing and static NaK are given in Figures VIII-7 and -8. Some loss in rupture strength appears to occur in flowing NaK although the biaxial ductility comparison of the flowing NaK and helium control data doesn't indicate a significant effect. However, the trend in the stress vs rupture life plot indicates, at least at long times, some loss in ductility can be expected, i. e., the divergence of the NaK curve from the helium control curve is probably indicative of loss of third stage creep.

The scope of the test program has changed because of the low ductilities observed for SCB coated Heat HH7531A and the evaluations of three new heats (89339, 89630, and 89676). The revised program will be aimed at determining if the low ductility mentioned above is related to alloy chemistry, precipitate morphology (stringers), the coating processing effects, or to the surface defects on these particular specimens. A detailed metallographic examination is currently in progress on biaxial stress rupture tested specimens to help answer these questions. The potential of the new heats will be observed in tensile tests on simulated processed, aged and unaged material in uniaxial creep testing, and in biaxial stress rupture testing on simulated processed material at 1300°F. Specimens for these tests are $\sim 50\%$ complete.

a. Metallographic Examination

Preliminary metallographic examination of tubing specimens has shown that significant differences exist in their microstructures, dependent both internal and external reactions. The internal reactions are due to normal aging occurring in Incoloy 800. The external reaction is due to the processing or test environment.

Figure VIII-7. Biaxial Creep Rupture Strength of SCB Coated Incoloy 800 at 1300°F Flowing and Static NaK (Heat No. HH7531A)

Figure VIII-8. Biaxial Ductility of Incoloy 800 at 1300°F in NaK (Heat No. HH7531A)

Two slightly varying coating processes were used in preparation of biaxial stress rupture specimens. The first process had stagnant air at the outside diameter of the tube during the intermediate temperature oxidation step. This resulted in a gradient of precipitates and duplexing of the grain structure along the length of the as-coated tube. This process was used exclusively in preparation of samples from Heat 810535. In the second process, the outside diameter of the tube was protected with flowing helium during the oxidation step but not during the high temperature (2100°F) firing cycle. This process was used in preparing samples from Heat HH7531A. The resulting microstructure was improved but some of the duplexing and precipitates were still in evidence. The latter effect is probably the result of nitriding the outside diameter during the coating firing cycles (stagnant air in shroud tube) to a depth of ~ 7 mils of the 15-mil wall tubes. It has also been observed that the microstructure plays an important role in determining the deformation mode of the biaxial creep test specimen. Examination of specimens tested to date indicates differences in grain boundary appearance, dependent upon thermomechanical history. Identification of and reasons for these differences are being established on a systematic basis.

(1) Specimens From Heat 810535

Light microscopy on an as-coated sample revealed fine grains on the outside diameter and very coarse grains elsewhere. A band of precipitates begins at the outside diameter and extends ~ 0.001 to 0.002 in. into the tube. No difference in hardness was evident from the outside diameter to the center of the tube wall.

A coated unaged sample that ruptured after 4871 hr at 1400°F (in helium) was examined by light and replica-electron microscopy. A zone, relatively free of precipitates, appears at the outside diameter and extends inward for $\sim 20 \mu\text{m}$. Then a zone of precipitates begins which extends ~ 0.007 -in. into the tube. Two sizes of precipitates are present. 0.1 to $1 \mu\text{m}$ and $500 \text{ \AA}$ as seen in Figure VIII-9. An excellent bond at the coating-metal interface is also shown in Figure VIII-10.

Figure VIII-9. Electronmicrograph of Biaxial Stress Rupture Tested Incoloy 800 Specimen
Near Outside Diameter (Heat No. 810535, Rupture Life - 4871 hr, 1400°F, ~5% $\Delta D/D$)

Figure VIII-10. Same Sample as in Figure VIII-5 With Metal-Glass Interface

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Figure VIII-11. Sample From Incoloy 800 (Heat HH7531A),
Ruptured in 530 hr, 1300°F Helium, 15-mil Wall

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(2) Specimens From Heat HH7531A

Light microscopy of an as-coated sample (Figure VIII-11) revealed fine grains at the outside diameter and a zone of precipitates which extend from the outside diameter to ~ 0.005 in. into the tube. The tube is harder at the outside diameter; a value of 216DPH was found there as compared to 170DPH at the center of the tube wall.

Examination by light microscopy of a sample stressed to rupture in 530 hr at 1300°F in helium (unaged) showed considerably more precipitation and a widening of the precipitation zone.

Light and replica electron microscopy of a sample aged 1285 hr at 1100°F and stressed to rupture at 1100°F in helium (786 hr) revealed $0.5 \mu\text{m}$ precipitates almost up to the outside diameter and $0.1 \mu\text{m}$ precipitates farther away from the outside diameter. Intergranular voids and cracks were present.

In summary, the coating processes are responsible for the dissolution of nitrogen and/or oxygen into the tube wall. This results in some precipitation of finely distributed particles near the outside diameter on cooling from the coating temperature, and additional precipitation during high temperature testing. The influence of these reactions on properties is still under investigation. Steps are being initiated to protect the outside diameter of the Incoloy tubes with an inert environment during the high temperature glass firing cycle.

b. Mechanical Properties of New Heats

Tensile properties measured for all six heats at 75 and 1200°F in the simulated processed condition are listed in Table VIII-5. As was expected, the heat containing high carbon and titanium (89630) is the only one that is markedly different from the others.

The creep strengths of 0.15-in. thick strip samples of the three new heats were measured at 1200°F and 8000 psi. These samples were subjected to the time and temperature of the SCB coating process, but in a vacuum environment. The creep curves are shown in Figure VIII-12. Again the high carbon and titanium bearing Heat 89630 is considerably stronger than the others.

TABLE VIII-5
INCOLOY 800 TENSILE DATA
(Simulated Coating Process)

Heat No.	Chemistry (wt %)			Temperature (°F)	0.2% Yield	
	Ti	Al	C		Strength (ksi)	Elongation (%)
810535	0.38	0.37	0.04	RT	27	36
				1200	17.5	24
HH7531A	0.17	0.17	0.03	RT	24	44
				1200	16.5	26
K89676	0.10	0.19	0.01	RT	24.5	40
				1200	24.5	40
K89339	0.25	0.04	0.03	RT	24.2	44
				1200	13.2	36
K89630	0.47	0.21	0.09	RT	28.0	41
				1200	19.3	36

Figure VIII-12.
Creep Strength Comparison for
Three New Heats
of Incoloy 800