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PROGRESS REPORT
AUGUST-OCTOBER 1970
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AEC Research and Development Report

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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) Thermoelectric converter development
- 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 a broad range of performance. The design must also be suitable for use with thermoelectric (TE), mercury-Rankine, Brayton, or organic 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 years 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 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 provide the TE power-conversion subsystem for use 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 Safety Program is to perform 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

The detailed examination of the S8DR core was started during the quarter. A total of 73 fuel elements have been found to have cracked cladding based on the visual examination, leak checks, and neutron radiography. Forty-six fuel elements have been disassembled and have undergone detailed visual and dimensional examination. The fuel rod diameter measurements obtained from neutron radiography and profilometry were found to fall into four types of profiles. The Type 1 fuel elements (101) were generally intact and showed an epsilon phase fuel swelling bump at the inlet end and characteristic delta phase swelling in the remainder of the rod. The Type 2 elements (76) were generally cracked, showed large cladding strains, but did not show the delta swelling profile. Type 3 elements (30) had regions of very high hydrogen loss with associated fuel shrinkage. The Type 4 elements (4) have an exaggerated epsilon fuel swelling bump. Back calculations from the fuel swelling data would indicate the fuel in S8DR operated about 50° F higher than had been predicted from the outlet coolant measurements.

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A study was performed to determine the prepoison requirements for the reference ZrH reactor. A mixed loading of \$12.00 of Gd¹⁵⁵ and Eu¹⁵¹ was found to minimize the reactivity mismatch for five-year operation at 600 kw and 1200°F. Temperature coefficient measurements will be made to establish the optimum ratio of Gd and Eu.

The conceptual design of the reactor core test was completed. The reactor core contains 295 fuel elements in a 0.670-in. triangular array. The elements have 0.032-in.-thick Incoloy 800 cladding and are arranged in two zones with different element diameters to provide a match between the flow rate and power generation. The spacing between the elements is assured by means of spiral fins which enhance the channel-to-channel mixing, in addition to preventing excessive temperatures due to fuel element clustering as was observed in S8DR. The design is also configured to minimize cladding stresses. The reactor will be operated in the vacuum chamber previously used for S8DR. The control drum assembly is a simple, void-backed beryllium design which makes maximum use of off-the-shelf drive and position sensing components.

System studies and design layouts have been prepared for reactor-thermo-electric power systems for use on manned space stations. A 29-kwe (net) system was designed which was compatible with the weight and size restraints of the space shuttle. An upgraded design was prepared which was capable of producing 41.5 kwe (net) with a small (~12%) weight penalty.

During the quarter, the first thermoelectric converter module incorporating a refractory metal inner cladding was fabricated and tested. The testing, which included 50 thermal cycles, showed that this inner cladding redesign was completely successful in eliminating the circuit distortion which had been observed in previous converters with Inconel inner claddings. Based on the success of this test, three additional refractory clad modules were fabricated and placed on test. In addition, six power modules and two pump modules continued on test.

The test section for conducting the S8DR fuel element heat transfer studies was completed and the fabrication of instrumented heaters was under development. The results of preliminary tests conducted with the 19-element hydraulic test section showed that a fin on wall, right-left-neutral (RLN) fin configuration

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provided very significant ($\sim 25\%$ per inch) channel-to-channel mixing. The extended endurance testing of an S8DR actuator has exceeded 24,000 hr at 1125°F and an advanced bearing has been tested for 15,000 hr at 1500°F . The large diameter LiH checkout shield was successfully cast in the new shield casting facility. Even though the shield was over 7 ft in diameter and contained nearly 1900 lb of LiH, it was one of the soundest castings ever produced with a void volume of only 5%.

Finned fuel element cladding tubes have been fabricated by hobbing (a specialized milling technique). The initial tubes had wall thickness variations of 3 to 6 mils, but improved tooling should alleviate this problem. A matrix of fuel specimens is being coated with various ratios of the burnable poison materials; Gd_2O_3 and Eu_2O_3 . Films of Y_2O_3 are also included to stabilize the Eu_2O_3 . Eleven SCB-coated 15-mil Incoloy 800 clad fuel elements were tested to determine the short-time stress/strain capability of the hydrogen barrier at elevated temperature. Failure of the hydrogen barrier was found to occur at about the 0.2% yield point of the material. The evaluation of cladding and barrier material properties continued during the quarter. The thermal expansion characteristics of the barrier were determined. Continuing tests on Incoloy 800 include stress rupture tests in He and NaK over a range of temperatures, both in-pile and unirradiated.

A preliminary parametric evaluation of the post-operational safety aspects of reactor power systems for typical unmanned space applications is being conducted for mission lifetimes up to 5 yr.

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II. SNAP 8 DEVELOPMENT REACTOR

A. DETAILED S8DR EXAMINATION

The detailed examination activities were started during this period. The scope of the detailed examination includes both destructive and nondestructive operations to be performed on selected elements as determined from the data obtained during the screening examination. Additional clad profilometer measurements, gamma scanning, leak checking, and permeation tests are the principal nondestructive tests to be performed. Primary destructive tests include fission gas analysis, element disassembly, visual examination, fuel dimensional measurements, density measurements, fuel hydrogen analysis, clad and fuel metallography, and other selected materials tests.

A summary follows of the activities completed during this reporting period.

1. Gamma Scanning

Additional gamma activity profiles were taken on all the elements selected for detailed examination which were not scanned during the screening examination. This resulted in scans being taken on about 73 additional elements. All scans were normalized to the same standard element used throughout the examination.

Since the start of the examination, a total of 165 elements have been gamma scanned. These are shown on the core map in Figure II-1.

2. Film Analysis

Film samples were taken from the cladding surface of three additional elements to determine whether the film composition was a function of core location. The samples were analyzed by x-ray fluorescence techniques. The analyses of the additional film samples have been completed and are reported in Table II-1. The analyses indicated a decreasing iron content in the film from top to bottom of the core with a corresponding increase in the nickel content. The chrome concentration indicated a slight increase at the bottom of the elements. The iron concentration also increased from the edge to center of the core with a decrease in the nickel concentration.

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Figure II-1. Gamma Scanned Fuel Elements

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TABLE II-1
CLAD FILM SAMPLE ANALYSIS

Element No. :	398	398	398	702	228	228	228
Core Position:	3-7	3-7	3-7	6-19	8-27	8-27	8-27
Sample Location:	Top	Center	Bottom	Center	Top	Center	Bottom
Elemental Composition: (wt%)							
Fe	30	45	55	30	20	25	40
Cr	10	10	15	10	10	10	15
Ni	50	35	20	50	60	60	40
Mn	1	2	*	2	1	*	*
Mo	2	1	*	2	4	3	*

*Sample was too small to report the element concentration with any accuracy although the composition was estimated to be similar to the other samples analyzed.

3. Element Leak Check

All the intact elements were given a helium leak test to insure that all elements with cracked cladding were found. Since many cracks were found during the screening examination which were less than 1/4 in. long, it was felt that some may have escaped detection. Also, correlations made between clad profilometer profiles and cracked elements indicated that some elements were suspected of having small undetected cracks.

The leak check testing consisted basically of a pressure chamber for forcing helium into any cracks in the elements, a vacuum chamber connected to a mass spectrometer for detecting helium leaks, and a bubble check bath to visually observe the location of any cracks found by helium leak checking.

All intact elements which did not have any cracks, based on the visual examination performed during the screening examination, were leak checked. The additional elements found to be cracked during leak testing, which were originally listed as intact based on visual examination, are as follows:

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<u>Core Position</u>	<u>Element No.</u>
3-2	720
4-10	730
4-13	131
5-18	391
5-22	402
6-16	252
7-27	175
8-9	484
8-42	234
7-7	428

In addition, 6 elements were found to be cracked during fission gas puncturing or disassembly based on the presence of NaK inside the element. They are listed below.

<u>Core Position</u>	<u>Element No.</u>
5-21	383
6-1	386
6-26	133
7-17	711
8-23	365
8-41	274

Two elements (7-05 and 9-43) classified as cracked based on visual observations during the screening examination could not be confirmed by leak check. Element 9-43 had long been questioned, and review of n-ray and He cladding diameter scan indicates that it is probably intact and so 9-43 is no longer classified as cracked. Element 7-05, on the other hand, shows high cladding strain and a characteristic Type 2 fuel rod profile as seen by n-ray (see Section II-B-2) and is therefore retained as a cracked element.

An additional 6 elements are classified as cracked based on their cladding/n-ray fuel profile shapes (see Section II-B-2). The total cracked element count is then 73 elements (51 found during the screening examination, exclusive of 9-43, 16 identified during the detailed examination, and an additional 6 classified as cracked based on cladding/n-ray shapes).

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The core map shown in Figure II-2 shows the final tabulation of cracked elements. A total of 66 elements had cladding cracks which extended through the wall. Elements in core position 7-5 and 9-43 are not included in this tabulation.

4. Fission Gas Analysis

A total of 18 S8DR fuel elements were sampled for fission gas. Fourteen of these elements were intact and the remaining 4 had cracked cladding, determined either by helium leak and bubble check or by observed NaK inside the fission gas puncture hole.

The fission gas was collected by placing the element in a container that was connected to a vacuum system. The container was evacuated to approximately 0.5μ , and the element was punctured in the void space at the top end of the element. The released gas was pumped through dry ice acetone cold traps to remove any condensable gases. The remaining Kr^{85} gas was then passed through two activated charcoal filled glass vials. The first trap was normally greater than 99% efficient in collecting the gas. The second trap which collected the remaining gas normally contained less fission gas than the first by a factor of 100 or greater.

Table II-2 gives the basic history of the elements punctured and the amount of fission gas released. The only marked difference observable in the data was that the intact dehydrided elements (Class B in Table II-2) had greater amounts of fission gas released than the other intact elements (Class C in Table II-2) by a factor of between 2.5 and 40.

5. S8DR Element Disassembly

The fuel elements were initially disassembled by cutting both end caps off using a tube cutter. The cladding was then slit at the 0° and 180° orientations using an electrical discharge machine (Elox). The cladding was then spread open and the fuel rod removed.

A change in the method of disassembly was decided upon after several fuel rods were broken at the top and due to pressure exerted by the tube cutter. The remainder of the elements were disassembled by making a saw cut at the top end, a tube cut at the bottom end, and Elox slitting the cladding at 0° only.

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Figure II-2. Final Core Map of Elements With Cracked Cladding

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TABLE II-2
FISSION GAS ANALYSIS RESULTS

Element No.	Core Position	Dehydrided	Cracked Fuel	Cladding Intact	Observed NaK	% Kr ⁸⁵ Released x 10 ⁻¹³	Class
133	6-26	No	Yes	No	Yes	1	A
386	6-1	No	No	No*	Yes	0.36	
131	4-13	No	No	No†	Yes?	3.25	
391	5-18	Yes	No	No†	No	0.50	
110	4-4	Yes	Yes	Yes	No	46.6	B
454	4-12	Yes	Yes	Yes	No	19.6	
142	7-28	Yes	Yes	Yes	No	120.0	
631	5-7	Yes	Yes	Yes	No	105.0	
111	6-2	Yes	Yes	Yes	No	8.27	
368	7-30	Yes	Yes	Yes	No	40.0	
156	8-12	Yes	Yes	Yes	No	60.9	C
536	3-9	No	No	Yes	No	2.82	
144	5-5	No	No	Yes	No	3.25	
701	4-1	No	No	Yes	No	1.86	
195	7-16	No	No	Yes	No	1.60	
412	4-5	No	Yes	Yes	No	2.08	
268	7-31	No	No	Yes	No	2.11	
472	5-19	No	No	Yes	No	2.55	

A = Cladding not intact

B = Cladding intact; dehydrided, broken fuel

C = Cladding intact; not dehydrided

* = Passed helium leak and bubble check; not intact on basis of observed NaK

† = Originally thought intact; crack found during leak check

The Elox cut is deep enough to mark a 0° orientation on the fuel rod. The fuel rod was then slid from the sprung open cladding tube. This method maintains a more accurate fuel orientation, especially if the fuel rod is broken in several pieces. The cladding was then returned to the Elox equipment to be split at the 180° orientation. The 46 irradiated and 2 unirradiated archive elements which were disassembled are listed in Table II-3.

TABLE II-3
DISASSEMBLED S8DR ELEMENTS

Core Position	Element No.	Element Condition	Core Position	Element No.	Element Condition
-	335	Archive	6-14	463	Cracked
-	564	Archive	6-23	657	Cracked
2-6	389	Intact	6-26	133	Cracked
3-1	396	Intact	6-29	128	Cracked
3-5	103	Intact	7-2	140	Intact
3-6	397	Intact	7-6	267	Cracked
3-9	536	Intact	7-16	195	Intact
4-1	701	Intact	7-17	711	Cracked
4-4	110	Intact	7-18	199	Intact
4-5	412	Intact	7-25	132	Intact
4-8	416	Intact	7-28	142	Intact
4-12	454	Intact	7-30	368	Intact
4-13	131	Cracked	7-31	268	Intact
5-1	653	Cracked	7-33	426	Intact
5-5	144	Intact	7-34	185	Cracked
5-6	673	Cracked	8-3	723	Cracked
5-7	631	Intact	8-12	156	Intact
5-17	302	Cracked	8-15	230	Intact
5-18	391	Cracked	8-19	526	Intact
5-19	472	Intact	8-23	365	Cracked
5-21	383	Cracked	8-29	308	Intact
6-1	386	Cracked	8-32	571	Cracked
6-2	111	Intact	8-41	274	Cracked
6-6	531	Cracked			

6. Detailed Visual Examination

A detailed Kollmorgen and stereo visual examination was performed on the fuel rod and cladding surfaces after disassembly of each fuel element.

The majority of the intact elements which did not have a dehydrided zone in the fuel rod were similar in appearance to the archive ceramic coating and fuel rod. The coating was adherent to the cladding and was dark green. The fuel rods were either shiny metallic silver, or dull grey. On a few of the intact elements, the ceramic had an iridescent color which is typical of a fuel/ceramic reaction observed in out-of-pile tests. Figure II-3 shows the condition of a typical intact element. A few elements, such as Element 144 (core position 5-5), had longitudinal fuel cracks near the bottom end. Generally, the fuel rods which indicated greater than normal growth near the bottom exhibited one or more fuel rod longitudinal cracks in this region.

All of the intact elements disassembled which had fuel dehydrided zones based on examination of the n-ray negatives had ceramic barrier damage in the vicinity of the dehydrided zones. The ceramic was pulled away from the cladding and attached to the fuel rod. Element 110 from core position 4-4, shown in Figure II-4, is a typical example of this phenomenon. The ceramic coating in the remainder of the element was usually in good condition. N-ray dimensional measurements in the region of the dehydrided zones indicated fuel decreases with large post-test diametral gaps. The ceramic on the fuel rod indicated that at some time during reactor operation the fuel and ceramic were in contact. In the dehydrided regions of the fuel rods, both longitudinal and radial cracks were noted. These ranged from about 4 in. long to short hairline cracks.

The cracked elements exhibited a generally poor ceramic condition which is the result of a NaK reaction. The typical result of the effect of a NaK reaction is shown in Figure II-5.

7. Length Measurements

Fuel rod length measurements were obtained using a dial indicator accurate to ± 0.001 in. A measurement was taken on a known length standard, three measurements of the fuel rod, and followed by another length standard reading. The fuel rod length was taken as the average of the three measurements.

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TYPICAL OF THE INTERNAL CLADDING SURFACES

4.3X

BOTTOM

TOP

DRKD-195-1 to 195-8

TYPICAL OF THE FUEL ROD SURFACE

4.3X

Figure II-3. Element 195 in
Core Position 7-16
After Disassembly

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THE FUEL ROD SURFACE 11 in. FROM THE BOTTOM AND SHOWING FUEL CRACKS AND CERAMIC ADHERING TO THE SURFACE

DRSK-110-13 to 110-16

DRKD-110-1 to 110-7b

DRSK-110-8 to 110-12

Figure II-4. Element 110 in Core Position 4-4 After Disassembly

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DRKD-302-1 to 302-7

Figure II-5. Element 302 in
Core Position 5-17
After Disassembly

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Of the 46 elements disassembled, only 13 rods were intact at the length measuring station. Evaluation of the data indicated that the n-ray length measurement data taken during the screening examination was biased on the high side by an average of 0.022 in. in all but one fuel rod measured (see Table II-4). Neglecting element 396 in core position 3-1, the standard deviation of the n-ray data about the average difference between the n-ray and actual measurement was ± 0.0054 in. Assuming the indicator measured fuel rods are representative of the entire core, the n-ray data should be corrected by subtracting 0.022 in. from all measurements taken during the screening examination on the intact fuel element.

8. Profilometer Measurements

Fuel profilometer measurements have been completed on 39 irradiated and 2 archive elements. Generally, good agreement was noted between the n-ray data and the fuel profiles on the intact elements. On the cracked clad elements, the n-ray data was low compared to the actual fuel diameter. The majority of the differences occurred in the upper portion of the fuel rods. The NaK-ceramic-fuel reaction zones on these rods could account for some of the discrepancy. Surface dehydriding of the fuel could also reduce the apparent diameter as measured from the n-ray negatives.

On the intact fuel rods, profile scans were taken to establish the bow. Based on the 13 rods measured, the bow ranged from zero bow to a maximum of 0.028 in. The axial location of the bow occurred at a Z/L of 0.7 to 0.8.

B. DETAILED EXAMINATION EVALUATION

1. N-Ray Observations

The results of the n-ray fuel rod diameter measurements were reviewed during the reporting period. As a result of this review, the various fuel rod profiles were categorized as indicated by the n-ray results. Four types of fuel rod profiles were found to recur in the S8DR fuel elements. These four types of fuel rod profiles are shown in Figures II-6, -7, -8, and -9.

A Type 1 fuel rod profile (Figure II-6) shows the characteristic epsilon phase bump in the inlet region of the fuel rod and the characteristic delta phase fuel swelling shape in the remainder of the rod. The Type 2 fuel rod profile

TABLE II-4
FUEL ROD LENGTH MEASUREMENTS

Core Position	Element	Pre-Irradiation (in.)	Post-Irradiation		Deviation (N-Ray-Actual) (in.)	L (Post-Pre) (in.)
			Dial Indicator (in.)	N-Ray (in.)		
3-1	396	16.824	16.873	16.861	-0.012	+0.049
3-5	103	16.825	16.837	16.856	+0.019	+0.012
3-6	397	16.822	16.862	16.882	+0.020	+0.040
4-8	416	16.827	16.863	16.884	+0.021	+0.036
4-13	131*	16.825	16.835	16.857	+0.022	+0.010
5-17	302*	16.824	16.821	16.837	+0.016	-0.003
7-16	195	16.826	16.885	16.908	+0.023	+0.059
7-18	199	16.826	16.919	16.940	+0.021	+0.093
7-31	268	16.827	16.938	16.960	+0.022	+0.111
7-33	426	16.824	16.889	16.911	+0.022	+0.065
8-15	230	16.823	16.842	16.857	+0.015	+0.019
8-19	526	16.824	16.856	16.892	+0.036	+0.032
8-29	308	16.823	16.859	16.886	+0.027	+0.036

*Cracked cladding

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Figure II-6. Type 1 Fuel Rod Profile, Element 4-05

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CONFIDENTIAL RESTRICTED DATA (G-1)

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Figure II-7. Type 2 Fuel Rod Profile, Element 4-10

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Figure II-8. Type 3 Fuel Rod Profile, Element 6-14

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~~CONFIDENTIAL RESTRICTED DATA (G-1)~~

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Figure II-9. Type 4 Fuel Rod Profile, Element 5-01

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(Figure II-7) also indicates the characteristic epsilon bump in the inlet region of the rod, but the characteristic delta phase fuel swelling profile is absent in the remainder of the rod. The Type 3 fuel rod profile (Figure II-8) is characteristic of elements which indicated high hydrogen loss regions in the fuel rod (as seen by n-ray) with the fuel rod profile showing an exaggerated depression in the dehydrided zone. The Type 4 fuel rod profiles indicated an excessive epsilon bump in the inlet region of the rod and, like the Type 2 elements, lacked the characteristic delta phase fuel swelling profile in the remainder of the rod. The Type 4 profile is shown in Figure II-9.

2. Characteristic Profiles

Also associated with the fuel rod profiles, as defined for Type 1, 2, 3, and 4 elements, are characteristic cladding profiles as indicated by the cladding spiral scan traces. Figure II-10 shows the general coupling between the characteristic fuel rod profiles and the typical associated cladding profiles. The Type 1 fuel rod profile characteristically shows only a minimal amount of diametral cladding strain. Generally, these elements are intact elements. The Type 2 elements, on the other hand, characteristically show larger cladding diametral strains than the Type 1 profiles; and, generally, these elements are cracked. The Type 3 profiles, on the other hand, tend to be associated with high ovality cladding, and roughly a 40-60 split between cracked and intact cladding condition. The Type 4 elements generally show large cladding strain in the inlet region of the rod where the exaggerated epsilon fuel swelling bump occurs; in these elements the split is 50-50 between cracked and intact cladding condition.

All elements in the S8DR core were categorized according to the definitions of Types 1, 2, 3, or 4 fuel elements in accordance with the characteristic shapes shown in Figure II-10. The locations of the Type 1 elements are shown in Figure II-11, the Type 2's in Figure II-12, the Type 3's in Figure II-13, and the Type 4's in Figure II-14. A total of 101 S8DR elements were classified as being Type 1 elements, 76 as Type 2 elements, 30 as Type 3 elements, and 4 as Type 4 elements.

The final cracked element count for the S8DR core is 73 elements. A total of 51 cracked elements were found during the course of the screening examination (excluding 9-43); and an additional 16 were located during the first phase of

~~CONFIDENTIAL RESTRICTED DATA (G-1)~~

CHARACTERISTIC FUEL ROD PROFILE (n-ray)

TYPICAL ASSOCIATED CLADDING PROFILE(S)

CONFIDENTIAL RESTRICTED DATA (G-1)

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Figure II-10. Categorization of Fuel Element Types,
Based on N-Ray Observations

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Figure II-11. Core Locations of Type 1 Elements

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Figure II-12. Core Locations of Type 2 Elements

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Figure II-13. Core Locations of Type 3 Elements

FUEL ELEMENT LOCATION

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Figure II-14. Core Locations of Type 4 Elements

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the detailed examination, either through leak checking or visual observation of decanned elements. An additional 6 elements are considered to be cracked based upon the n-ray data and cladding strain data, although the actual existence of cracks in these elements has not been physically confirmed. Of these 73 cracked elements, 1 element is a Type 1 element, 58 are Type 2 elements, 12 are Type 3 elements, and 2 are Type 4 elements.

3. Inferences Regarding Fuel Swelling

Profilometer measurements of the fuel rod OD were becoming available throughout the course of the reporting period. Generally, it was found that the fuel rod profiles for Types 1 and 3 fuel elements, as measured with the profilometer, agreed quite well with the fuel rod profiles as generated from the n-ray data (see Figures II-15 and -16). The agreement between the n-ray fuel rod profiles and the profilometer data for Type 2 fuel rods was not as good with the profilometer data indicating that the fuel rod OD was indeed larger than that indicated by the n-ray data (see Figure II-17). The profilometer data on the Type 4 elements confirmed the n-ray observations of an exaggerated epsilon fuel bump in the inlet region of the Type 4 elements.

Another interesting observation based upon fuel rod profilometer data is the general correspondence of the fuel rod shape to the observed cladding shape even though the fuel rod is not in contact with the cladding at EOL conditions. This may be seen by referring to Figure II-17 where the maximum fuel rod diametral growth region occurs very near the maximum cladding diametral strain region, suggesting that the cladding diametral strain may have been the result of fuel growth induced forces.

A fuel growth map, as shown in Figure II-18, can be constructed if the hypothesis is accepted that the cladding strain indeed was the result of fuel growth and uses the fuel growth characteristics for unstrained cladding as indicated by n-ray results. (Generally for unstrained cladding, the fuel rods are of the Type 1 category which shows good agreement between profilometer and n-ray data.) Using the Bonzer-Swenson fuel swelling correlation (excluding the burnup term as being negligible for this first analysis), a temperature map can be inferred for the core based upon the assumed fuel growth characteristics observed. Such a temperature map is illustrated in Figure II-19, where elements in the core have been selected and lumped together based upon similar growth characteristics.

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CONFIDENTIAL RESTRICTED DATA (G-1)

Figure II-15. Comparison of N-Ray Data and Profilometer Data on Fuel Rod Diameter Measurements, Element 3-09

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CONFIDENTIAL RESTRICTED DATA (G-1)

Figure II-16. Comparison of N-Ray Data and Profilometer Data
on Fuel Rod Diameter Measurements, Element 6-14

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~~CONFIDENTIAL RESTRICTED DATA (G-1)~~

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Figure II-17. Comparison of N-Ray Data and Profilometer Data on Fuel Rod Diameter Measurements, Element 6-29

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Figure II-18. Maximum Fuel Rod Diametral Growths Based Upon N-Ray Data for Unstrained Elements and Cladding Strain Levels for Strained Elements

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Figure II-19. Fuel Temperatures Inferred From Fuel Growths
(Figure II-18) as Calculated by the Bonzer-Swenson
Fuel Swelling Correlation

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The temperature map defined in Figure II-19 shows reasonable agreement, shape-wise, to that indicated by the coolant outlet thermocouple instrumentation during the early life operation of S8DR. The overall temperature levels, however, are somewhat in excess of those previously calculated for the core, being on the order of about 50° high (overall).

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Figure III-1. ZrH Reactor Reactivity and Control Requirements vs Eu Percent (of \$12 Prepoison)

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III. REFERENCE ZrH REACTOR ENGINEERING

A. PREPOISON SELECTION FOR THE ZrH REACTOR

A study has been performed to determine prepoison requirements for the ZrH reactor. The effects of mixtures of Gd¹⁵⁵ or Sm¹⁴⁹ with Eu¹⁵¹ were determined for a design mission of 5-year operation at 600 kw.

A minimum mismatch (reactivity gain and/or loss throughout lifetime) over the design mission was obtained with a prepoison loading of approximately \$12. (See also Section IV-D.) Figure III-1 presents the minimum control and reactivity requirements as a function of Eu in a Gd/Eu prepoison loading of \$12. The Sm/Eu prepoison effect on reactivity and control is also included. For the case of a Gd/Eu prepoison loading, both control and reactivity requirements to achieve mission goals were slightly influenced by the prepoison split. However, control requirements varied with Eu in the case of a Sm/Eu prepoison mixture. Additionally, little or no gain in prepoison effect was observed for Sm/Eu in preference to Gd/Eu mixtures. (This is contrary to the observed effect in shorter missions, in which a Sm/Eu prepoison is preferable for high Eu fraction prepoisons.) Accordingly, Sm/Eu prepoison loadings have been excluded from further consideration as a burnable poison candidate. This decision is acceptable from a fabrication standpoint, as poison deposition using Sm/Eu prepoisons is more difficult to achieve than with the reference Gd/Eu prepoisons.

B. SNAPTRAN EXPERIMENTS

A series of experiments has been planned at the SNAPTRAN in support of the prepoison selection. This program will provide data necessary to establish the Gd/Eu concentration ratio for the reference reactor.

Gd and Eu affect the prompt fuel temperature coefficient of the ZrH reactor. Gd makes the coefficient more positive, while Eu makes it more negative. As a result, neither of these poisons can be used alone. The experimental program is designed to determine the appropriate mixture of these poisons so that the unpoisoned temperature coefficient is not significantly disturbed.

Additional experiments will be performed to determine the worths of the prepoisons as well as to evaluate the worth of the U²³⁶ poison by-product produced in the operating reactor.

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Figure III-2. SCA-4A Test Drum Configuration

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C. CONTROL DRUM EVALUATION

The DOT two-dimensional computer code was successfully applied to the calculation of control drum worth. This has been part of the program devised to determine accurately the control worth of the ZrH reactor.

A set of poison-back drum worth measurements were made last Fiscal Year in the SCA-4A Facility. The SCA-4A critical assembly is a uranium 235 fueled, zirconium-hydride moderated, beryllium reflected reactor. The core contains 37 SNAP 10 fuel elements. The standard reflector of the SCA-4A contains four 7-in. -diameter void-back drums. For this test, 1 drum was replaced by a 5-1/2-in. -diameter drum with a beryllium thickness of 3-1/2 in. A sketch of this configuration is shown in Figure III-2. The back of the drum was fitted with an aluminum can into which plates of tantalum or boron carbide powder were placed.

Three poison-back drum configurations have been calculated. A sketch of these drum configurations is shown in Figure III-3. A two-dimensional model was set up in polar coordinates for the DOT calculations. A 28-group cross section library containing 17 groups of upscatter was used. Table III-1 presents the comparison between calculation and measurement.

The technique will be extended to evaluate the ZrH drums in a flight configuration now that the method has been verified by comparison to experiment.

TABLE III-1
DRUM WORTH CALCULATION USING DOT

Poison Back Configuration	Calculation* (\$)	Measurement (\$)
B ₄ C	1.97	2.03
B ₄ C + Ta	1.84	1.82
Ta	1.80	1.71

*Assumes $\beta_{\text{eff}} = 0.008$

BORON BACK

TANTALUM BACK

COMPOSITE

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Figure III-3. Poison Back Drum Configurations

IV. REACTOR CORE TEST

A. INTRODUCTION

A plan to conduct a nuclear test of the reference ZrH core has become a major objective of the SNAP Reactor Program. The test is to be performed in the S8DR vacuum chamber. Except for the inclusion of core diagnostic instrumentation, the reactor core will be an exact duplicate of the flight core. The core will be insertable into and removable from the pressure vessel/control drum assembly from the vault floor. The control drum assembly is a simple void-back beryllium drum design making maximum use of off-the-shelf drive and position sensing components. This control system design philosophy is basically that used successfully in the S2ER and S8ER reactor systems.

The system installation schematic is shown in Figure IV-1. Further description of the system mechanical design is contained in Section IV-3 which follows.

The principal effort during the past quarter was on the completion of the core and system conceptual design. Emphasis was placed on fuel element cladding design, fuel element support, core thermal and hydraulic design, and the overall assembly concept.

The conceptual ZrH reactor core uses 295 fuel elements with 0.032-in. Incoloy 800 cladding. The core lattice pitch is constant throughout with coolant flow/power matching being accomplished by two zones of differing fuel element diameter. The inner zone of 127 elements has a fuel element OD of 0.640 in. and the outer zone, 0.655 OD. The fuel elements are supported somewhat above the core midplane by means of the integral spiral fins on 2 out of 3 elements. The finned elements alternately are right-hand and left-hand pitch and when combined in a repeating pattern with the nonfinned or "neutral" elements forms a nested fin-on-wall assembly. The resulting fin orientations are such that the coolant flow paths are everywhere confluent and a high degree of interchannel mixing is attained. Initial hydraulic tests indicate that the interchannel coolant exchange will range from 15%/in. in the outer core zone to 30%/in. in the inner zone.

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Figure IV-1 Reactor Core Test System Installation

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Significant progress was made in the structural design of the fuel element. Analytical and experimental study of the SCB coating on the Incoloy substrate resulted in considerable advancement in identifying and quantifying the design controlling factors. Preliminary structural margin analysis was performed on the cladding stresses both in the cold assembled mode and the hot operating mode.

B. CORE THERMAL HYDRAULIC ANALYSIS

As a result of parametric lattice spacing trade studies, a 2-zone core using variable clad and fuel diameter for spacing control has been selected as the core test conceptual model. The GEOM2 computer code was used to select the zone boundary with consideration given to peak fuel temperature, maximum cross element temperature gradient, and fuel area fraction. Further GEOM2 runs were then made to ascertain the effects of clustering and fuel asymmetry in 3- and 7-element groups. Runs were also made at off nominal power and temperature levels to determine temperature swings including a run at 1300°F outlet. Finally, runs were made for the reference flight reactor to determine the edge treatment required to obtain feasible temperature conditions and to examine design techniques to realize the required flows.

Two important variables in the selection of the zone boundary location in the 2-zone core were the thermal and nuclear effects. Thermal studies showed that a boundary at either Ring 7 or Ring 8 resulted in near optimum peak fuel temperature profiles and lower cross element thermal gradients than at other locations. A reactivity increase of about 50¢ results from a zone boundary location at Ring 7 rather than Ring 8. Hence the preliminary design selection was made at Ring 7 with 127 elements in the inner zone and 168 elements in the outer.

Core thermal studies have shown that considerable benefit is derived from the channel-to-channel mixing afforded by the element fins. Mixing reduces thermal gradients at the zone boundaries and minimizes the effects of spacing variations, element clustering, and asymmetry effects due to off-center location of the fuel rod within the cladding tube. Initial hydraulic studies were

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RIGHT LEFT-NEUTRAL ORIENTATION (FLOWS ASSIST)

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Figure IV-2. Right-Left-Neutral Mixing

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based on an assumed attainment of 50% of the ideal mixing values dictated by the fin geometry. The initial design was fin-on-fin with all elements having the fins spiraling in the same direction.

Early mixing test results of the fin-on-fin design showed the flow opposition characteristics of this design resulted in much lower interchannel mixing than had been used for design analysis. In order to attain the desired mixing, a right-left-neutral (R-L-N) fin configuration as shown in Figure IV-2 was substituted. This configuration showed very good mixing, 20 to 30%/in., and has been selected as the design.

The limiting factor in spacing had been a minimum fin height based upon mixing considerations. The R-L-N configuration permits consideration of a fin-on-wall or a fin-on-fin arrangement with the fin-on-wall being preferred from the standpoint of approximately twice the fin height for a given spacing. The fin-on-fin approach, however, offers the advantage of constant fin envelope diameter in both core zones with no mismatch at the zone boundary. Figure IV-2 illustrates the geometry for the fin-on-wall design and the ideal mixing it affords. As discussed in Section VII, the mixing tests to date have shown attainment of about 50% of ideal mixing efficiency. Table IV-2 in Section IV-D provides the characteristics of the current 2-zone core design. Figure IV-3 presents nominal peak fuel temperatures and coolant channel exit temperatures for the design. The peak fuel temperatures are seen to fall within a 75°F band and the coolant temperatures within 30°F. These must be regarded as minimum variations as the effects of asymmetrical location of the fuel slug within the cladding, and element clustering to the extent allowed by assembly clearances, must be included. These detailed studies are currently in progress.

C. FUEL ELEMENT STRESS ANALYSIS

1. Fuel Element Support Trade Study

This trade study evaluated the effects of various and support schemes for attachment of the fuel element to the upper and lower grid plates. Also the fin-to-fin spacing between elements was evaluated. The effect of controlling the element-to-element axial contact location was also studied. The basic geometry studied was a finned element 17-1/2 in. long. It should be noted that

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Figure IV-3. Nominal Temperatures in a 2-Zone Core

as this study proceeded, the fuel element geometry varied, trans-element temperature gradients varied, and the core axial temperature profile was reduced from $200^{\circ}\text{F } \Delta T$ to $135^{\circ}\text{F } \Delta T$. Conclusions were drawn using similar environments and analysis methods.

The support methods studied (Figure (IV-4) include:

- 1) Both ends pinned
- 2) Hot end fixed
- 3) Cold end fixed
- 4) Two intermediate supports
- 5) Pinned cold end, one intermediate support, loose hot end
- 6) Fixed cold end, one intermediate support, loose hot end
- 7) Completely restrained element
- 8) Flexible end supports

Fixed end refers to an element fixed against both translation and rotation whereas a pinned end refers to an element free to rotate but not translate.

Two basic analyses were used. The first considers a 2-element cluster with equal and opposite trans-element temperature gradients. Deflections were computed and the resulting hot channel effects were estimated. These were then added to the original trans-element temperature gradient and the fuel element resulting shears and moments caused by adjacent element restraint were computed. From these moments and associated element-to-element contact loads, the bending and local stresses were computed. A 30-lb tensile force was added to account for the vibration suppressor as were the 6 theta stresses.

The second analytical method uses the output of the GEOM2 Heat Transfer computer code. The cluster pattern is assumed in the GEOM Analysis and axial and trans-element temperature gradients computed. The 6 theta stresses are unique to a specific element as is the bending caused by element-to-element constraint and the thermal gradients.

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Figure IV-4. Fuel Element Support Schemes

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The types of stresses considered are for the operating environment only but include the effects of the residual stresses caused by having an element which is not perfectly straight inserted into a relatively tightly packed core. A summary of these stress modes is given in Figure IV-5.

The 6 theta stresses and the equivalent trans-element temperature gradient are computed for the asymmetrical gradients caused by element clusters. The equivalent trans-element temperature gradient was separated from the 6 theta components at several stations along the length of the element. This trans-element thermal gradient causes the element to bow in addition to the initial bow of a manufactured rod. This element bowing is assumed restrained by the adjacent elements causing beam bending stresses. The local stresses due to contact forces were computed using the previously reported analysis method.

The preliminary fuel element support screening analysis was performed and it had several simplifying assumptions. Six theta stresses were assumed to be 3500 psi at the midplane and tapering to 900 psi at the ends. To these results the tensile stresses due to a 30-lb vibration suppression load were added. Bending stresses were based on the assumption that two adjacent elements are equal and opposite in their actions. A point contact is assumed to constrain the element to an envelope defined by one half the lattice pitch. The computer code used is iterative in that it computes the deflection and places the constraining load at the point of peak deflection. The new hot channel temperature is computed and this process is repeated until the load point and maximum deflection converge. Stresses include the effect of the 0.010 free bow (due to manufacturing tolerances), the initial temperature gradient, and the hot channel effect. The hot channel effects were assumed based on the equations given in AI-AEC-12975 using a core axial thermal gradient of 200°F compared to the current design of 135°F and a mixing factor reducing trans-element hot channel ΔT to 60% of the unmixed value.

The results of the analyses are shown in Figure IV-6. These stresses from preliminary screening indicate the ranking of supports as follows:

STRESS MODES

PINNED SUPPORT
FREE TO ROTATE

HOT ELEMENT
(INTERNAL LOADS)

THERMAL
BOW

ADJACENT ELEMENT
RESTRAINT

LOAD

SIX THETA HOT-COLD
LINE TEMPERATURE
VARIANCE

HOT
COLD

STRESS COMPONENTS

INTERNAL
LOADING

COMPRESSION
TENSION
BENDING

LOCAL CONTACT
(HOOP AND AXIAL)

SIX THETA

HOT LINE
COLD LINE

Figure IV-5. Fuel Element Stresses

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Figure IV-6. Conceptual Comparison of Alternate Fuel Element Support Concepts

- 1) Fixed cold end, one intermediate support and loose hot end.
- 2) Pinned cold end, one intermediate support and loose hot end.
- 3) Fixed hot end, pinned cold end, 0.002 spacing.
- 4) Both ends pinned.
- 5) Completely restrained element.
- 6) Two intermediate supports, close spacing.

It became apparent that the majority of the bowing took place near the hot end. To restrain this at a point near the hot end caused high local contact stresses. The element also must be held in place so that it can withstand vibration and must be oriented to maintain a fin-on-fin contact. It became obvious that there should be no restraint at the hot end if stresses were to be minimized and that a support should be placed at some distance from the hot end. One of the major advantages of this concept is that clustering producing a hot channel causes the outlet end to bow away from the hot channel which tends to neutralize the thermal gradient.

The fixing of either end against rotation and translation was considered impractical because of the stress levels imposed and the normality requirements between the grid plates and the fuel element surfaces. A forced rotation of one degree causes approximately 27,000 psi stress on assembly.

$$\theta = \frac{Ml}{3EI} \quad \sigma = \frac{MC}{I} = \frac{MR}{I} = \frac{3RE}{l}$$

$$\sigma = \frac{3 \times 0.302 \times 30 \times 10^6 \times 1/57.3}{17.5} = 27,000 \text{ psi}$$

The completely restrained element is not practical because of the need to accommodate tolerance buildup within the core. This screening analysis also makes the completely constrained element falsely attractive. This resulted from less conservatism used in the hot channel conditions assumed for this design.

From these considerations, the fuel element supports considered for further analysis were:

- 1) Pinned cold end, one intermediate support and loose hot end.
- 2) Pinned at both ends.

Because of the core mechanical stability requirements, the configuration which is pinned at both ends was adopted as the overall more desirable.

The axial location of element-to-element contact was varied for the support method of both ends pinned. These studies were run utilizing a computer program with the temperature gradient with a cosine distribution, with and without a hot channel correction. Two initial trans-element thermal gradients were looked at, 20°F and 60°F. The hot channel correction in this case was based on a core axial gradient of 135°F and a 60% factor reducing the hot channel effect because of mixing.

The results shown in Figure IV-7 indicate that the support location should be between 6 and 8 in. The deflected mode shape is shown for a trans-element thermal gradient of 60° in Figure IV-8. This indicates that the fins should be undercut 0.006 in. at the upper end and 0.004 in. at the lower end (fin-on-fin design) to prevent contact at more than one controlled point. This is accomplished by shaving the fins on a hexagonal pattern so as not to completely destroy the fin height and mixing capability.

As a result of this study the following conclusions were:

- 1) The fuel element is best pinned at the cold end, with a few mils of clearance between the hot end grid plate hole and the fuel element end pin.
- 2) The second best support scheme is to pin the element at both ends, that is to allow the element to rotate with respect to the grid plate but not translate.
- 3) The intermediate support or element -to-element contact should be controlled and located between 6 and 8 in. from the hot end.
- 4) The fin-to-fin spacing at the contact point should be minimized, preferably in the range of 0.002 to 0.004 in.

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Figure IV-7. Effects of Contact Location

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Figure IV-8. Deflection Mode Shape $\sim 60^\circ \Delta T$

- 5) The fins should be shaved in a hexagonal pattern to provide clearance for relative movement from the intermediate support points and the end pins.

2. As-Fabricated Stresses

During the element manufacturing process, the inside diameter is coated with SCB coating and fired at approximately 2100°F. The coating is very viscous between 2100°F and its softening point of approximately 1500°F. When the element is cooled below the softening point the coating adheres to the metal and, because it has a lower coefficient of expansion than the metal, it causes compression in the coating and tension in the tubing. This residual stress is biaxial with the hoop and axial stress components being equal.

Considering the tubing and the coating as two separate cylinders the compatibility and equilibrium equations are set up.

$$\Delta R_{SCB} = \alpha_{SCB} \Delta T + \frac{\sigma_{SCB}}{E_{SCB}} (1 - \mu)$$

$$\Delta R_{Tube} = \alpha_{Tube} \Delta T + \frac{\sigma_{Tube}}{E_{Tube}} (1 - \mu)$$

$$\Delta R_{Tube} = \Delta R_{SCB}$$

$$\sigma_{Tube} t_{tube} = - \sigma_{SCB} t_{SCB}$$

Combining these

$$\sigma_{Tube} = \frac{(\alpha_{SCB} - \alpha_{Tube}) \Delta T}{\frac{(1 - \mu_{Tube})}{E_{Tube}} + \frac{t_{Tube}}{t_{SCB}} \frac{(1 - \mu_{SCB})}{E_{SCB}}}$$

where

α = coefficient of thermal expansion

E = modulus of elasticity

μ = Poisson's ratio

R = change in radius of tube or SCB

T = cool down temperature range

t = thickness

Of these values the greatest unknown was the coefficient of expansion of the SCB coating. A split ring test was used to measure the coefficient of expansion with respect to the tube coefficient of expansion.

The rings were cut into approximately 0.75-in. lengths and then split. When these segments were split, they would open creating a significant gap. This opened segment of a tube was then put into a furnace and the gap recorded as a function of temperature. Equations were derived, and programmed on the GE 430 timeshare computer, to equate the gap to the coefficient of expansion of the SCB coating. The effect of radius change due to the gap opening and due to the shift in neutral axis, from the centroidal axis, because of the curved section, was accounted for. Section properties are based on average thicknesses. The resulting equation for evaluation of the coefficient of expansion is

$$(\alpha_{SCB} - \alpha_{Tube})\Delta T = \frac{\delta E_T I}{\pi R^2 t_{SCB} (t_{Tube} \pm t_{SCB})} \left[\frac{t_{SCB}}{t_{Tube}} \frac{(1 - \mu_{Tube})}{E_{Tube}} + \frac{(1 - \mu_{SCB})}{E_{SCB}} \right]$$

where α , E, μ , and ΔT are as described above and

I = moment of inertia of equivalent section

R = radius of curvature

δ = gap opening.

The reduction of this data is shown in Figures IV-9 and -10 for 0.015-coated and chromized Hastelloy N and various thicknesses of coated Incoloy 800 specimens. Evaluation and refined processing of this data is currently being accomplished.

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Figure IV-9. Experimental Results - $\alpha \Delta T$ Coating on Incoloy

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Figure IV-10. Experimental Results - $\alpha \Delta T$ Coating On Hastelloy N

The lower bound of this data was then applied to the fuel element to compute the residual stresses. The SCB coating maximum is considered to be 2.5 mils and the tube 0.032-in. -thick Incoloy 800. This produces a stress of 8700 psi. A finite element computer program was utilized to estimate the effect of eccentricity of both the tube and the SCB coating. The stresses for the reference configuration are referenced below including the combination of the biaxial field using a Von Mises yield criterion:

Processing Thermal Stress (psi)	8900
SCB Eccentricity (psi)	1300
Tube Eccentricity (psi)	300
Total Stresses	
Hoop (psi)	10,300
Axial (psi)	9230
Equivalent (psi)	9800
Proportional Limit at Room Temperature (psi)	15,500
Margin of Safety (%)	0.58

The 58% margin of safety is that referred to the prospect of thermal ratcheting of the tubing caused by the SCB coating.

3. Core Assembly Stresses

When the elements are assembled into the core, the elements are assumed to be initially bowed 0.006 in. in a circular arc. Two elements are then assembled back to back, causing a bending and local contact stress in the element to straighten it to its envelope. A summary of these stresses are presented below:

As Fabricated (psi)	Hoop		
Bending (psi)	Axial $\pm$ 1610		
Contact (psi)	Hoop +1830	} Inside	-2600 } Outside
	Axial +720		
Equivalent Stress (psi)	Contact side		10,800
	Opposite side		10,600

Comparing this equivalent stress to the proportional limit the margin of safety against thermal ratcheting is 43%.

A parametric analysis was made evaluating the effect of SCB coating thickness and tubing thickness on Fabrication and Core Assembly stresses. This is presented in Figure IV-11.

4. Operational Stresses

The operational stresses are computed on the compression side of the tube which is compared to the yield strength and on the tension side of the beam which is compared to the onset of increased hydrogen permeation. (See Section VIII.)

The components considered included:

- 1) The core assembly bending and load contact stresses.
- 2) The bending and local contact stresses caused by the imposed trans-element thermal gradient.
- 3) The 6 theta stresses.
- 4) The load of the vibration suppressor.

These stress equivalents are shown in Figures IV-12 and -13 for both sides of the beam. The calculated thermal gradient maximum at the present time is 28°F. The corresponding maximum equivalent compressive stress is 11,000 psi which corresponds to a margin of safety of 50% with respect to yield. The tensile stress in the tube at the glass interface is 7900 psi maximum and this is compared with the stress which causes increased hydrogen permeation. The margin of safety for this case is 138%.

5. Material and Geometric Alternates

A corresponding analysis was generated using Hastelloy N and Hastelloy X as a cladding material. The tube thicknesses for these alloys was 0.015 compared to the 0.032 Incoloy 800. Both systems are based on a fin on tube contact for core loading and operational stresses.

A Stress Summary is presented in Figure IV-14 of the three materials.

D. NUCLEAR ANALYSIS

1. Core Configuration

The primary emphasis during the current reporting period was directed toward defining a reactor core configuration which would meet the design

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Figure IV-11. Parametric Analysis - 0.012 in TIR

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Figure IV-12. Operational Stresses -
Equivalent - 0.012 in TIR

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Figure IV-13. Operational Stresses -
Tensile - 0.012 in TIR

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	AS FABRICATED	CORE AS ASSEMBLED	CORE OPERATIONAL
INCOLOY (t = 0.032)			
SCB COATING STRESS	9,800 psi	9,800 psi	
CORE RESTRAINT			
BENDING		$\pm 1,610$	
LOCAL CONTACT		1,830 HOOP 720 AXIAL	
TOTAL		10,800	
OPERATIONAL (28°ΔT)			7,900 psi
TENSILE			11,000
EQUIPMENT			1.38 (BARRIER)
MARGIN OF SAFETY	0.58	0.43	0.50 (YIELD)
HASTELLOY N (t = 0.015)			
SCB COATING STRESS	9,900 psi	9,900 psi	
CORE RESTRAINT			
BENDING		$\pm 1,780$	
LOCAL CONTACT		3,350 HOOP 502 AXIAL	
TOTAL		11,300	
OPERATIONAL (28°ΔT)			8,915
TENSILE			18,050
EQUIVALENT			2.41 (BARRIER)
MARGIN OF SAFETY	2.13	1.74	0.77 (YIELD)
HASTELLOY X (t = 0.015)			
SCB COATING STRESS	16,100 psi	16,100 psi	
CORE RESTRAINT			
BENDING		$\pm 1,790$	
LOCAL CONTACT		3,370 HOOP 505 AXIAL	
TOTAL		17,700	
OPERATIONAL			8,335
TENSILE			16,167
EQUIVALENT			3.86 (BARRIER)
MARGIN OF SAFETY	1.24	1.03	1.00 (YIELD)

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Figure IV-14. Stress Summary

operation goal of 3 to 5 years operation at 600 kw and 1200°F outlet. The flight system configuration, as discussed in the following section, was used to define the requirements on the core excess, reactivity control, and pre-poison loading. A number of configurations were evaluated to determine their adequacy from the standpoint of excess reactivity and control worth. This comparison is shown in Table IV-1. The first configuration shown, with 0.0650-in. element to element pitch, 25-mil-thick Incoloy 800 cladding, and "uniform" fin spiral, was the core chosen as the base point for the core evaluation studies. As the analysis of this core proceeded and experimental data from the hydraulic tests and cladding properties tests became available, it was apparent that the base core would not provide the desired operating margins. Specifically, it was necessary to increase the cladding thickness to increase the stress margin and change the fin arrangement to promote better coolant mixing. Several alternate designs considered are also shown in Table IV-1. All employ the right-left-neutral (R-L-N) fin spiral and increased cladding thickness. All the designs appeared capable of meeting the excess reactivity and control requirements. The reference configuration which evolved from the design requirements dictated by the stress and thermal/hydraulic considerations is shown in Table IV-2. The core capability was then checked with the core test reflector configuration and found to be adequate also.

2. Flight Reactor Configuration

The flight reactor configuration with its 4π shieldable, poison-backed control drums provides less reactivity control than is required for a 5-year operating lifetime. Hence, burnable poisons are used to compensate for a portion of the reactivity loss. The prepoison material and loading become important parameters in minimizing the reactivity swing during lifetime and thus reducing the control worth requirement of the control drums.

The prepoison materials have tentatively been selected as a mixture of Eu^{151} and Gd^{155} with a cold loading worth of \$9 Gd^{155} and \$3 Eu^{151} . This ratio gives a reactivity mismatch (difference between most reactive and least reactive configuration, hot operating) of approximately \$2. The determination of the precise loading of the prepoisons must await the results of SNAPTRAN transient experiments during the next reporting period. These experiments will allow the poison loading to be tailored to obtain the desired fuel temperature coefficient.

TABLE IV-1
COMPARISON OF AVAILABLE EXCESS AND CONTROL REQUIREMENTS
FOR FLIGHT CONFIGURATION ZrH CORE

Element Pitch (in.)	0.650	0.670	0.670	0.670	0.670
Cladding Material	Incoloy 800	Incoloy 800	Incoloy 800	Incoloy 800	Incoloy 800
Cladding Thickness (mils)	25	30	32	32	35
Fin Height (mils)	10/20	15/30	15/30	15/30	15/30
Fin Spiral	Uniform	R-L-N	R-L-N	R-L-N	R-L-N
Uranium (wt %)	10.5	10.5	10.5	11.5	11.5
Available Excess (\$)	17.46 (17.16)	18.52 (18.22)	17.44 (17.14)	18.70 (18.40)	17.08 (16.78)
Available Control* (\$)	10.80	10.29	10.48	10.48	10.77
Reactivity Requirements					
Recoverable Losses (\$)	3.65	3.65	3.65	3.65	3.65
Fission Products and Burnup (\$)	8.23	7.73	7.83	7.24	7.37
Hydrogen Loss (\$)	2.75	2.49	2.40	2.40	2.30
EOL Prepoison (\$)	1.33	1.33	1.33	1.33	1.33
EOL Margin (\$)	<u>2.00</u>	<u>2.00</u>	<u>2.00</u>	<u>2.00</u>	<u>2.00</u>
Total (\$)	17.96	17.20	17.24	16.62	16.65
Control Requirements					
Recoverable Losses (\$)	3.65	3.65	3.65	3.65	3.65
Reactivity Mismatch (\$)	2.00	1.80	1.70	1.75	1.75
MOL Shutdown Margin (\$)	2.00	2.00	2.00	2.00	2.00
EOL Margin (\$)	<u>2.00</u>	<u>2.00</u>	<u>2.00</u>	<u>2.00</u>	<u>2.00</u>
Minimum Total (\$)	9.65	9.45	9.35	9.40	9.40

*With fully enriched B₄¹⁰C inserts in Ta/10W poison backing

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TABLE IV-2

ZrH FLIGHT REACTOR CONCEPTUAL MODEL DESIGN SUMMARY

Design	
Nominal Thermal Power (kw)	600
Number of Elements	295
Lattice Pitch (in.)	0.670
Inlet Temperature (°F)	1065
Outlet Temperature (°F)	1200 ±20
Reflector Type	Poison Backed BeO
Inner Reflector Type	Clad BeO
Core Diameter (in.)	12.6
Core Length (Active Fuel) (in.)	18
Fuel Rod Diameter (Inner/Outer) (in.)	0.560/0.575
Fuel Cladding	0.032-in. Incoloy 800
Barrier	SCB
Gap (mils)	6 (Radial)
Fin Height (mils)	30/15
Fuel Element Spacing - Nominal (Inner/Outer) (mils)	30/15
Poison	Eu ¹⁵¹ + Gd ¹⁵⁵
Fuel	ZrH + 10.5 wt % U ²³⁵ , N _H - 6.3
Fuel Element Dimensions	
I. D. (Inner Zone) (in.)	0.576
(Outer Zone) (in.)	0.591
O. D. Neutral Elements (Inner Zone) (in.)	0.640
(Outer Zone) (in.)	0.655
Fin OD. (Inner Zone) (in.)	0.685
(Outer Zone) (in.)	0.700
Fin Length (in.)	15.5
Fin Spiral	Right-Left-Neutral
Fin Pitch (in. /turn)	5.5
Fin Relief Cut	
Hot End 6.5 in. (mils)	12
Center 2.0 in. (mils)	0
Cold End 7.0 in. (mils)	

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Figure IV-15. Mismatch vs Prepoison Loading for Several $\text{Eu}^{151}/\text{Gd}^{155}$ Mixes

Figure IV-16. Control Worth vs Cold Clean Excess Reactivity

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The effect on mismatch of changing the prepoison mix for the reference materials is shown in Figure IV-15 as a function of prepoison loading. It is seen that the additional mismatch penalty is relatively small over a broad range of prepoison mixes.

Typical requirements for the flight configuration systems have shown a zero margin, cold, unpoisoned reactivity requirement of \$15.50 to \$16.00. When \$2 beginning of life (BOL) or end of life (EOL) reactivity margin is added, the minimum cold, unpoisoned excess reactivity requirement becomes \$17.50 to \$18.00 for a typical prepoison loading of \$12 $\text{Eu}^{151}/\text{Gd}^{155}$. The corresponding minimum control requirement with \$2 minimum cold shutdown margin is \$9.50 to \$10.00. The current design for the 2-zone core shown in Table IV-2 meets both these objectives.

3. Core Test Configuration

When the parametric thermal, stress, and nuclear studies of the flight reactor configuration indicated that a viable conceptual core design had evolved, performance studies of the selected core in the ground test reflector configuration were begun. The ground test configuration will employ void-back control drums similar to those used in previous ground tests such as S8DR and S8ER. However, the larger core size of the Core Test Reactor, compared to the previous tests, with the attendant decreased neutron leakage, causes a markedly lower reflector and control drum worth for the Core Test Reactor than for either S8DR or S8ER.

Proper choice of the void-backed reflector configuration provides more reactivity control than is afforded by the poison-backed control drums of the flight configuration as well as more initial excess reactivity. This, when coupled with the prepoison loading of the flight configuration, gives larger margins on both excess reactivity and the allowable reactivity swing than are available with the flight configuration. The larger margins are necessary to cover the larger performance uncertainties that will accompany and be quantified by the Core Test operation. Operating experience from the Core Test will allow use of smaller operating margins on excess reactivity and reactor shutdown for the flight configuration reactor.

The reflector configuration selected for the Core Test configuration uses 6 void-backed control drums having a radius of 6.25 in. Use of these large-size drums also provides the capability to adjust the reactivity of the as-built reactor (shim) with relatively small penalty in control worth. This is an important feature, since the prepoison loading and initial excess reactivity must be matched to provide the desired reactivity swing during reactor operation. The trade between excess reactivity and control worth is shown in Figure IV-16 for the Core Test reactor.

E. REACTOR SYSTEM INSTALLATION

The conceptual design of the reactor system was developed as shown on Figure IV-1. This system, to be installed in the existing S8DR vacuum vessel in Building 059 Santa Susana, is similar in concept to the S8ER reactor that was operated in Building 010 at Santa Susana. The reactor, consisting of the core, reactor vessel, and beryllium reflector with void back control drums, is located within the existing vessel. The shielding over the reactor is designed to permit access to its upper surface 1 week after shutdown, and to limit the operating radiation dose received by equipment located above the shield, such as control drum drive motors, to acceptable levels. The reactor and shielding are arranged to permit integral core replacement without appreciably affecting other parts of the system such as the reactor vessel, piping, and reflector. The feedthrough ring attached to the top end of the existing vacuum vessel, plus a removable cover dome, provide complete containment in the event of a loss of coolant accident.

The major studies and analytical efforts completed in support of the conceptual design are summarized as follows:

1. Containment Atmosphere Selection

A high vacuum (10^{-5} torr or less) environment, maintained by the existing vacuum system, was selected. This will permit the use of S8DR type bearings for the control drums.

2. Reactor Shielding Analysis

The reactor shielding will consist of a 2-in. -thick steel gamma shield, followed by 48 in. of normal density (150 lb/ft^3) borated concrete.

3. Shield Cooling

Shield cooling requirements were established and the cooling system was defined based on the use of pressurized nitrogen in a recirculating loop for heat removal and rejection.

4. Reflector Control Drum Selection

Six void-back control drums, with a radius of 6.25 in. and 18 in. long, were selected as the configuration for the reflector-control assembly.

5. Control Drum Drives

The control drum drive requirements were defined and the conceptual design of the drives was completed. A dc-stepper motor which rotates the drum in one-eighth degree increments, is operated at different stepping rates to cover all control modes; startup, normal control, and scram.

6. Fuel Element Removal

The feasibility of providing for removal of individual fuel elements at intervals during the core test was evaluated and determined to be inconsistent with the basic objectives of the core test reactor. The risk of inducing anomalous fuel element damage was considered too high.

7. Core Instrumentation

An extensive study of core instrumentation was carried on throughout the conceptual design. At the completion of the conceptual design the tentative list of instruments to be installed included 20 fuel element cladding thermocouples (TC's) and 40 coolant channel outlet TC's.

- LENGTH 55 FT
- DIAMETER 15 FT

Figure V-1. Reactor Thermoelectric System

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V. REACTOR THERMOELECTRIC SYSTEM ENGINEERING

A. SPACE STATION SYSTEM CONSIDERATIONS

System studies and design layouts have continued to be prepared for reactor-thermoelectric power systems for use on the manned space station. The nominal power requirement is for 29.0 kwe delivered continuously to the loads which is equivalent to 38.5 kwe at the thermoelectric (TE) modules since there are losses in power transmission and conditioning and a need for battery charging. The replaceable reactor-TE package is 15 ft in diameter and 55 ft long. This size is set by the Advance Logistic Shuttle (ALS) configuration. The main components of the reactor-TE power system are the reactor, shield, TE modules, radiators, coolant system, and controls.

Figure V-1 shows the nuclear reactor module (NRM). It is configured with the reactor and shield shown at the left and the power conversion equipment in the opposite end near the docking interface. The system size is established by the large cylindrical radiator. An insulated and passively cooled compartment is located adjacent to the docking interface for the maintainable equipment and end-of-life disposal rockets. This area is accessible during operation and has an environment acceptable for the rockets and electronic gear.

A main consideration in designing the power system was the incorporation of a high degree of operational flexibility. Since the output of a reactor system is controllable and since its weight and configuration are not highly sensitive to power level, it was decided to investigate the impact of incorporating 50% excess power capability (to 43.5 kwt net). With this capability the nominal required power (38.5 kwe gross) could be supplied at a somewhat derated condition; but, more importantly, NASA management would have the flexibility to accommodate necessary or desirable changes in the power requirement without a program change.

A basic thermoelectric system was selected with approximately 15% more tubular modules than necessary and with the heat transfer components oversized for a total weight penalty of about 1000 lb (4%). While this system has only about 10% excess power capability, the 50% excess power capability can be obtained by

TABLE V-1
REACTOR POWER SYSTEM CHARACTERISTICS

Characteristics	Thermoelectric System			
	Basic System		With Supplemental Radiator	
	Nominal	Growth	Nominal	Growth
Performance:				
Net Conditioned Power (kwe)	29.0	31.0	29.0	41.5
Gross Power (kwe)	38.5	41.0	38.5	55.0
Reactor Power (kwt)	680	712	668	845
Reactor Outlet Temperature (°F)	1150	1200	990	1200
Gross Efficiency (%)	5.7	5.7	5.8	6.5
Gross Specific Area (ft ² /kwe)	49	46	99	69
Average Hot Clad Temperature (°F)	1041	1073	872	1076
Average Cold Clad Temperature (°F)	510	515	390	428
Description:				
Length (ft)	55		+ 38*	
Diameter (ft)	15		16*	
Weight (basic) (lb)	24,520		+2900*	
Weight (w/EOL disposal) (lb)	27,770		+2900*	
Radiator Area (ft ²)	1,890		+1900*	

*Supplemental Radiator Only

the addition of a 1900-ft², 2900-lb radiator on the boom section adjacent to the NRM. The extra modules on the basic system permit better utilization of this extra radiator. Using this supplemental radiator has the added advantage of allowing full power operation at a reactor outlet temperature below 1000°F.

The shield on the system is a shaped 4π shield. A primary shield shadow cone encompasses the Space Station, while higher radiation levels are allowed to the side of the reactor. The requirements were established after a thorough review of the mission. The radiation dose to the astronauts in a worst case (assuming that the astronauts spend 6 months in the compartment nearest the reactor) is only 25 rem. The rendezvous dose is only about 1 additional day's dose. With the reactor located 200 ft from the Space Station, the diameter of the primary shielded cone is 130 ft or greater at all docking ports.

Operation of the power plant is expected to be completely automatic, and only minimal crew involvement is required for periodic diagnostic checks or possibly infrequent replacement of failed components.

B. SYSTEM PERFORMANCE CHARACTERISTICS

Table V-1 gives the performance characteristics for nominal power operation and for growth power operation at 1200°F with and without the supplemental radiator. All of the cases are based on 6 of the 192 modules being electrically disconnected but still drawing thermal power. Also, all of the cases refer to conditions at the end of 5 years during which time the TE modules have degraded according to their time-temperature history at constant power.

The Basic System delivers its 38.5 kwe (29 kwe net) at a thermal power of 680 kw and a reactor outlet temperature of 1150°F. If the operating temperature is raised to 1200°F, the end-of-life electrical power goes up to 41.0 kwe.

A supplemental radiator placed on the Space Station boom may be connected to the Basic System to increase the efficiency or power capability. With the supplemental radiator, the 38.5 kwe is obtained at the somewhat lower power of 668 kw and an outlet temperature just under 1000°F. Continuous operation at 1200°F will provide an end-of-life output of 55.0 kwe. If the system is operated at the nominal operating conditions, the degradation would be reduced such that the 50% over-power (58 kwe) capability would be attainable at 1200°F at any time in life.

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● THERMAL POWER – FOR FULL SYSTEM

● ELECTRICAL POWER – AFTER 5 yr WITH 6 MODULES
NOT PRODUCING POWER

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Figure V-2. Reactor Thermoelectric System Schematic
(One Quadrant)

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The TE module voltage varies somewhat with operating conditions. At the nominal operating conditions, the Basic System puts out 4.50 volts per module pair or 108 volts when all the modules in one quadrant are connected in series.

Figures V-2 and -3 show schematically the component relationships in the thermoelectric system without and with the supplemental radiator, respectively. The operating parameters shown are for the 100% power point in the case without the supplemental radiator and for the growth power at 1200°F with the supplemental radiator.

C. SYSTEM DESCRIPTION

Four separate primary loops carry the reactor NaK to four separate heat exchangers in the gallery and return it to the reactor. Also, in the gallery are the thermoelectric electromagnetic (EM) pumps which pump not only the primary NaK loops but the intermediate and heat rejection NaK loops as well. These EM pumps consist of a low voltage TE source, heavy copper buses, heavy permanent magnets, and the pump throats, which are electrically in series.

Four intermediate loops transport hot NaK from the heat exchangers in the gallery to the thermoelectric modules located at the base of the NRM. Losing heat in the modules, this NaK returns to the heat exchangers in the gallery. The intermediate NaK is nonradioactive so that shielding of the lines and TE modules is not required.

The 192 TE modules are cooled by the NaK in four heat rejection loops that circulate the NaK between the modules and the radiator system. In Figure V-2, the NaK radiator of the basic system is shown schematically including the heat exchangers and connections needed for attachment of the supplemental radiator.

The supplemental radiator loop serves all four basic heat rejection loops in parallel (see Figure V-3). This minimizes the power loss in the event of failure of a loop in one of the basic system quadrants since the whole supplemental radiator would still be available to the remaining three quadrants. Failure of the supplemental loop itself is overcome by switching to the redundant standby loop.

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Figure V-3. Reactor Thermoelectric System Schematic (One Quadrant)

The small cone at the left end of the system layout (Figure V-1) contains the reactor/shield/primary loop assembly. This assembly is shown in Figure V-4. The shield is in two parts and is shown in phantom in Figure V-4. The upper cylindrical part is immediately adjacent to the reactor and reduces the side dose to levels acceptable for rendezvous and reduces the neutron dose sufficiently to prevent activation of the NaK in the intermediate loops. The lower part of the shield is designed to reduce the radiation levels from the reactor and the radioactive primary loop NaK to acceptable levels in the Space Station. It also allows manned access to the aft end of the NRM during operation.

The spiral form of the primary loop lines is employed to avoid direct radiation paths as well as accommodate thermal expansion. The 4 U-shaped intermediate heat exchangers are clustered at the center of the gallery. The 4 triple-throat pumps drive the NaK in all 12 loops.

The base of the lower shield assembly is fastened to the upper plane of a truncated transition cone which structurally ties the shield to the cylindrical radiator structure. This basic structure is of the semimonocoque type with a stressed skin, frames, and longitudinal stiffeners, and is fabricated from a titanium alloy. Titanium is required because of the relatively high radiator temperatures. The structure is designed to carry its own weight and to support the reactor and shield during operation in space including the firing of the EOL disposal rockets. Launch loads in both the integral Saturn V launch and ALS launch will be carried by external structure attached to the base of the transition cone. In the integral launch this would be the boom structure and in the other it would be tie points in the ALS hold.

Figure V-1 shows the TE module installation, the patchboard, the EOL disposal package and, other portions of the aft end of the NRM. The TE tubular modules have their axes parallel to that of the radiator and are grouped in 4 annular rings inside the radiator shell. Forty-eight of the 192 TE modules are connected hydraulically in parallel in each quadrant. One of these assemblies is pictured in Figure V-5. Electrically, the modules are in series pairs and each module pair delivers its power independently to the patchboard.

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Figure V-4. Thermoelectric System Reactor/Shield/Primary Loop

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Figure V-5. Thermoelectric Converter Assembly
(One Quadrant)

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Figure V-6. System Sensitivity to Pump Performance

The patchboard is designed to be operated by the crew on occasions when 1 or more module pairs need to be electrically disconnected from the system. It is a device for achieving a desirably high voltage from many modules in series without risking a large fraction of the total power capability should 1 module fail.

The patchboard is 50 ft from the reactor in the most heavily shielded zone. At the patchboard, a crew member would receive gamma radiation at the rate of 0.07 rem/hr and neutron radiation at the rate of 0.02 rem/hr. Since the time of occupancy is brief and infrequent, these dose rates are acceptable.

The radiator is attached by flexible brackets to the outside of the titanium cylindrical structure. It is possible for the NaK in the radiator to freeze under some operating conditions so a thermal shroud is used to reduce the heat loss. This is an array of shutters which fits within the 15-ft-diameter constraint when closed. When the shutters are open, the radiator rejects heat at its normal rate. During nonoperating periods, such as the prestart period, the vanes are closed. This changes the emissivity and absorptivity from that of the radiator fins to that of the vanes reducing the heat loss rate and increasing the solar heating rate. The vanes are grouped so that any single failure would prevent only a portion of the radiator from being exposed. If this occurred during the initial, unmanned start, a large percentage of power would still be available. The shutter drive system can be maintained so, when convenient, an astronaut could overcome the problem.

The supplemental radiator is permanently mounted to the boom of the Space Station. It is 50 ft long and 16 ft in diameter. The fluid is pumped by canned rotor pumps that are located at the opposite end of the radiator from the reactor so that basic NRM can be retracted into the supporting boom section. Flexible lines are located in the end near the docking interface for connection to the heat exchangers on the NRM.

D. PUMP PERFORMANCE SENSITIVITY

Pump sensitivity studies were conducted to determine the effect on system performance if the predicted pump performance is not attained. Pump performance was characterized in terms of the ratio of the pump thermal power to the reactor thermal power. Parametric evaluation of this parameter is presented in Figure V-6. This figure gives the pump to reactor thermal power

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- 29-kwe NET POWER AT 5 YEARS
- 192 MODULES EXCEPT AS NOTED

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Figure V-7. Thermoelectric System Sensitivity to Module Efficiency

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ratio as a function of the pump parameter ($\Sigma \Delta P / \eta_{\text{throat}} \Delta T$) for various values of the converter efficiency time bus efficiency.

The bus efficiency is a function of the power delivered, current, and bus dimensions. For the reference pump, the bus efficiency is 77%. Efficiency can be increased at the expense of bus weight.

The pump converter efficiency is a function of the operating conditions. The pump converter efficiency is lower than the power converter efficiency because of the higher currents and slightly different design. At the reference temperature conditions for the basic power plant ($T_{\text{hot}} = 1041$; $T_{\text{cold}} = 510^\circ\text{F}$), the pump converter efficiency is 4.62%. This value corresponds to a BOL power converter efficiency of 6.95%.

The pump parameter in Figure V-6 is a function of the pump throat efficiency. This efficiency is a complex function and is calculated using a computer program. The computer program is based upon past experience in the design and operation of the dc pumps, and gives slightly conservative efficiency when compared to actual test data. For the reference pump design, the predicted efficiency for the primary and intermediate loop throats is about 23% while the heat rejection loop throat is about 25%. It can be noted that if the pump parameter were to increase by 25% due to system unknowns and the converter efficiency decreased by 25%, the pump thermal power would still be only 12% of the reactor power. Such increases cannot practically be anticipated; however, it shows the insensitiveness of the overall system performance to the pumping requirements.

E. EFFECT OF THERMOELECTRIC MODULE DEGRADATION

The performance predictions used for the thermoelectric power system assumed the successful development of a TE module utilizing (1) a refractory metal inner clad to reduce distortion, (2) a diffusion barrier to reduce degradation, (3) the ternary materials to increase performance, and (4) a redesigned module to reduce parasitic thermal losses. To evaluate the effects of these improvements on the power system, an analysis was performed to characterize the thermoelectric system should the development program goals not be achieved. Figure V-7 shows the key system parameters as a function of thermoelectric efficiency for three different radiator areas. The efficiency is normalized to the value used in the study.

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It can be noted from Figure V-7 that the basic system with the supplemental radiator (300 ft²) could provide the required 29 kwe with no improvements over the current performance at an outlet temperature of only 1120°F. With the geometric design changes only in the module and no ternary material, the basic 1900 ft² system could provide the required power at 1250°F with only a 3000-lb weight penalty.

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VI. THERMOELECTRIC CONVERTER DEVELOPMENT (Prepared by Westinghouse Astronuclear Laboratory)

A. REACTOR POWER MODULES

During the past quarter the first module designed with a refractory inner cladding was fabricated and tested. Results of this experiment, which included 50 thermal cycles, showed that the inner cladding redesign was completely successful in eliminating circuit distortion observed in previous reactor modules fabricated with Inconel inner claddings. Based on the success of this experiment, three additional TEM-X series modules with refractory claddings were fabricated during the quarter.

In addition to the testing of refractory clad modules, 6 reactor power modules with Inconel inner claddings are currently on test.

1. Refractory Clad Modules

Cyclic testing of TEM-X S/N-2, the first module incorporating the bimetal Ta-10W/SS 316 inner cladding designed to eliminate circuit distortion, was completed during the past quarter. Experimental data from the test and post-test destructive examination of the module indicate complete success of the experiment.

TEM-X S/N-2 was identical in design to the TEM-13G series with one exception. TEM-X S/N-2 incorporates a refractory (Ta-10W) inner clad section under the thermoelectric circuit, which was selected and sized to qualize the total inner and outer cladding expansions during heatup from room temperature to operating conditions. The refractory Ta-10W clad section was bonded to 316 stainless steel on each end by a co-extrusion process. Previous reactor power modules incorporated single-piece Inconel 718 inner claddings.

Effective Seebeck coefficient, internal resistance, and thermal impedance data recorded during heatup of TEM-X S/N-2 are plotted as functions of average module temperature in Figures VI-1 through -3, respectively. Similar data recorded during heatup of TEM-13G S/N-10 are presented for comparison along with calculated curves for the TEM-X S/N-2 and TEM-13G designs. Note that the incorporation of the refractory cladding has a noticeable effect on calculated

Figure VI-1. Comparison of Calculated and Experimental Effective Seebeck Coefficient Data Recorded During Heatup of TEM-X S/N-2 and TEM-13G S/N-10

Figure VI-2. Comparison of Calculated and Experimental Internal Resistance Data Recorded During Heatup of TEM-X S/N-2 and TEM-13G S/N-10

Figure VI-3. Comparison of Calculated and Experimental Thermal Impedance Data Recorded During Heatup of TEM-X S/N-2 and TEM-13G S/N-10

and experimental data from these modules. Because of the higher thermal conductivity of Ta-10W, the voltage (hence effective Seebeck coefficient) of the TEM-X S/N-2 module is higher than that of the TEM-13G module, resulting in improved performance.

Experimental data from both modules can be seen to agree very well with the corresponding calculated curves over the entire temperature range. It is evident from the TEM-X S/N-2 data that proper compaction was achieved during autoclaving of the refractory clad module.

A cyclic test program was specified for TEM-X S/N-2 to determine the success of the cladding substitution in reducing or eliminating circuit distortions observed in previous reactor-size (15-in.) modules. After a brief steady-state operating period at the design operating conditions, $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$, the module was subjected to a series of 50 controlled thermal cycles. Each 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 $\bar{T}_H$ ramp rates of $30^\circ\text{F}/\text{min}$. This was a duplication of the TEM-13G S/N-10 test discussed in a previous quarterly report, AI-AEC-12953. After a short steady-state operating period following completion of thermal cycling tests, the module was removed from test for destructive examination.

Table VI-1 presents a summary of performance data recorded during testing of TEM-X S/N-2. During cyclic operation of the module, data were recorded during the steady-state soak period at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$. The data show quite clearly that cyclic operation had no measurable effect on module performance. By contrast, TEM-13G S/N-10, subjected to identical cyclic operation, suffered a 21-watt decrease in power output during testing. The success of the redesigned refractory inner clad concept is demonstrated by these data. This is more emphatically brought out in Figure VI-4 which presents temperature and load adjusted electrical power output for these 2 modules as a function of the number of thermal cycles completed.

Upon completion of static testing, TEM-X S/N-2 was cooled from the design operating conditions to room temperature at a $30^\circ\text{F}/\text{min}$ transient ramp

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TABLE VI-1
TEM-X S/N-2 DATA SUMMARY

Data Point No	Hours	Total Cycles	P _e (Watts)	R _g (Milliohms)	V _{oc} (Volts)	Q _{in} (Watts)
1	0	0	272	110.8	10.99	5662
2	1	0	272	110.8	10.98	5665
3	19.2	0	272	110.8	10.98	5683
5	39.8	0	272	110.6	10.97	5676
6	43.3	1	272	110.5	10.97	5571
7	46.0	2	271	110.9	10.96	5572
8	64.0	8	273	110.5	10.99	5553
9	70.3	10	274	110.6	11.00	5608
10	88.0	16	270	110.5	10.93	5581
11	94.6	18	270	110.8	10.95	5576
12	112.3	24	271	110.3	10.94	5612
13	118.8	26	271	110.2	10.93	5568
14	137.1	32	271	110.6	10.94	5560
15	143.4	34	270	110.6	10.94	5549
17	167.8	42	270	110.5	10.92	5593
18	191.8	50	269	110.5	10.91	5594
19	192.7	50	270	110.6	10.92	5611
20	207.3	50	270	110.7	10.93	5610
21	214.5	50	270	110.4	10.92	5617
Calculated			276	107.8	10.92	5910

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Figure VI-4. Comparison of Electrical Power Output Data as a Function of Thermal Cycles for TEM-X S/N-2 and TEM-13G S/N-2

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rate. After completion of the cooldown procedure, a final measurement indicated a post-testing room temperature module internal resistance of 11.4 milliohms. This level of internal resistance agrees well with both calculations and experimental data from the TEM-13G series modules. This agreement indicates that the substitution of Ta-10W for Inconel 718 has not resulted in reduced interface pressures after cyclic operation.

Experimental data from TEM-X S/N-2 indicates that the incorporation of the duplex refractory inner cladding has been a complete success. A discussion of the results of the post-test destructive examination is presented in Section VI-C of this report.

Three additional TEM-X series modules containing refractory inner clads were fabricated and processed during the past quarter. The design details of these identical modules, designated as the TEM-X S/N-3 series, are discussed in the previous Quarterly Progress Report. A comparison of the room temperature internal resistance data taken after processing with the corresponding calculated resistance indicated that proper compaction had been achieved in the autoclaving cycle for each module. Table VI-2 presents a summary of the room temperature circuit resistance data along with the calculated value. The first of the 3 new modules, TEM-X S/N-3, was destructively examined immediately after processing to ensure proper alignment of components after completion of the compaction process. The results of this examination are discussed in another section of this report.

Both TEM-X S/N-3A and -3B were heated to $\bar{T}_H = 1085^\circ\text{F}$, $T_C = 507^\circ\text{F}$ test conditions near the end of this reporting period. Beginning-of-life performance parameters from both modules correlated very well with the corresponding calculated parameters. The first module, S/N-3A, is currently being thermally cycled in the same manner at TEM-X S/N-2. Completely stable performance has been observed in data generated during the first 31 thermal cycles. TEM-X S/N-3B will be endurance tested under steady-state operating conditions to determine the long-term performance stability associated with this module design.

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TABLE VI-2
COMPARISON OF ROOM TEMPERATURE CIRCUIT
RESISTANCE DATA FROM TEM-X S/N-3
SERIES MODULES

Module	Circuit Resistance (Milliohms)
TEM-X S/N-3	2.70
TEM-X S/N-3A	2.70
TEM-X S/N-3B	2.65
Calculated *	2.63

* Calculated circuit resistance assuming electrical contact resistances negligible.

A test assembly for evaluating the compatibility of the Ta-10W/316 SS bimetal clad with NaK in a liquid metal loop has been designed. Retaining-ring closures, of the type used in modules, are joined by a 16-in. -long stainless steel jacket having a 2.0-in. OD and a 0.25-in. wall. The stretch-neck sections in the retaining rings accommodate differential thermal expansion between the clad and the jacket. Provisions are made for evacuating the chamber between the jacket and cladding OD. This chamber was backfilled with an inert gas to protect the refractory cladding during loop testing. All hardware has been assembled and shipped to Atomics International for testing.

2. Performance Calculations for TEM-X S/N-3 Module Designs Incorporating 0.005-in. Mica

An analysis to determine the performance of a TEM-X S/N-3 type module incorporating 0.005-in. mica, rather than 0.010 in. as specified in the TEM-X S/N-3 design, has been completed. Performance calculations were performed using TEMØD, thermoelectric module performance code.

Table VI-3 summarizes the results of the calculations. The TEM-X S/N-3 calculated parameters are compared to those of three variations of the TEM-X S/N-3 design in which 0.005-in. mica insulators were incorporated. Parameters

TABLE VI-3
CALCULATED PERFORMANCE PARAMETERS FOR TEM-X S/N-3 AND
THREE VARIATIONS OF 5-mil MICA MODULES

	TEM-X S/N-3		Variation 1		Variation 2		Variation 3	
Mica thickness (in.)	0.010		0.005		0.005		0.005	
P-leg thickness (in.)	0.188		0.188		0.193		0.193	
N-leg thickness (in.)	0.188		0.188		0.188		0.193	
Test Conditions*	A	B	A	B	A	B	A	B
Power Output (Watts)	253	290	254	291	256	294	260	298
Voltage (Volts)	2.62	2.69	2.62	2.70	2.62	2.70	2.62	2.70
Current (Amps)	96.8	107.8	96.9	107.9	98.0	109.2	99.2	110.5
Internal Resistance (Milliohms)	27.0	25.0	27.0	25.0	26.7	24.7	26.4	24.4
Heat Input (Watts)	6084	6261	5901	6070	5969	6138	6036	6207
Circuit Efficiency (%)	4.38	4.88	4.53	5.05	4.52	5.05	4.53	5.05
Overall Efficiency (%)	4.16	4.63	4.30	4.79	4.28	4.80	4.31	4.80

* Test Condition A: $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$ (Matched Load)
Test Condition B: $\bar{T}_H = 1084^\circ\text{F}$, $\bar{T}_C = 507^\circ\text{F}$ (Matched Load)

are presented for two operating conditions: $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$; and $\bar{T}_H = 1084^\circ\text{F}$, $\bar{T}_C = 507^\circ\text{F}$.

Variation 1 consists of a module which is identical to TEM-X S/N-3 except that the mica thickness has been reduced to 0.005 in. from 0.010 in. thus reducing the overall length of the circuit by 0.38 in. This configuration would require modified inner and outer conductor rings. In addition, the electrical heater length would be reduced by 0.38 in. to maintain testing configuration consistency with other reactor length modules. The attached table shows that although all electrical parameters are essentially identical to those of TEM-X S/N-3, a reduction in heat input is achieved by reducing mica shunt losses.

Variation 2 consists of a module in which the p-leg thermoelectric washer thickness is increased by 0.005 in. from the TEM-X S/N-3 dimension. The design allows use of the existing TEM-X S/N-3 molybdenum conductor rings, but requires new tungsten conductor ring inserts and an electrical heater reduced by 0.190 in. to compensate for the shorter thermoelectric circuit. Power output in this configuration is somewhat increased over that of TEM-X S/N-3 as a result of the increased amount of lead telluride. Heat throughput is somewhat reduced as a result of the decreased mica shunt heat losses.

Variation 3 consists of a total circuit length identical to that of TEM-X S/N-3. This is achieved by increasing both the n- and p-leg thermoelectric washer thicknesses by 0.005 in. to compensate for the reduced mica thickness. Although both the inner and outer conductor lengths must both be modified in this configuration, the electrical heater would be identical to that of TEM-X S/N-3. A 2.8% increase in power output accompanied by a small reduction in heat input rate can be achieved by altering the TEM-X S/N-3 design in this manner.

Table VI-3 shows that the reduced mica thickness will result in a 3% increase in efficiency in all of the proposed variations. Variation 3 allows the most direct comparison of the performance of a 0.005-in. mica module with that of the TEM-X S/N-3 design.

3. 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 cladding, remained on test in a two-loop liquid metal

system at Atomics International. This module has accumulated a total of 9242 hr of operation at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ operating conditions.

TEM-13G S/N-2 was tested for 504 hr in a static test stand at WANL prior to shipment to Atomics International for additional endurance testing in the liquid metal loop system. During the first 3256 hr of loop testing, in which the module experienced only 1 thermal cycle, a power output degradation rate of 10%/10,000 hr was observed. During subsequent testing, four additional thermal cycles have occurred and the overall degradation rate, determined statistically, has increased to 17%/10,000 hr.

Although there has been no gross indication of intercouple shorting, there is evidence that the degradation rate has been accelerated by cyclic operation. The relationship between operational stability and cyclic operation is felt to be caused by the increased cracking of the mica insulating washer during cyclic operation, which has resulted in accelerated diffusion of tellurium.

Performance trends of TEM-13G S/N-4, being tested at $\bar{T}_H = 1025^\circ\text{F}$ and $\bar{T}_C = 538^\circ\text{F}$ operating conditions in a static test stand at WANL, have closely duplicated those of TEM-13G S/N-2. Statistical analysis indicated that degradation of the module was 9%/10,000 hr during the first 4132 hr of testing. During this period, the module experienced 7 thermal cycles. Analysis of subsequent data through 7990 hr of testing and 3 additional thermal cycles indicates the degradation rate has increased to 19%/10,000 hr.

Elimination of relative component motion during cyclic operation, through the use of a refractory inner cladding as successfully demonstrated in the TEM-x S/N-2 test above, should substantially improve the performance stability of a TEM-13G type module. Initial stability should be improved due to decreased mica cracking during the initial heatup, and long-term stability should not be affected by thermal cycling.

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.

B. 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 $\bar{T}_C = 570^\circ\text{F}$.

Testing of an additional prototype pump module, TEM-14A S/N-3, has been completed. During 7727 hr of life testing, this module was subjected to 5 sets of 10 thermal cycles performed with $30^\circ\text{F}/\text{min}$ average hot clad temperature ramp rates. None of the sets of thermal cycles produced any deleterious effects on module performance. There was absolutely no degradation in electrical power output for this module. TEM-14A S/N-3 has been removed from test and destructively examined. The results of this examination were discussed in the July Quarterly Progress Report.

Electrical current and electrical power output data recorded from all TEM-14A series modules are presented in Figures VI-5 and -6, respectively. The BOL data from TEM-14A S/N-2 and S/N-3 agree to within 1% of the design levels. Excellent performance stability has been realized in the TEM-14 series pump modules.

Table VI-4 presents a summary of performance stability data from the EM pump modules. Degradation rates for four parameters were determined from a statistical analysis of experimental data from these 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 50 controlled thermal cycles on S/N-3 (see Figures VI-4 and -5) and the various uncontrolled shutdowns of all 3 modules indicates that the TEM-14 design is insensitive to thermal cycling.

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Figure VI-5. Electrical Current Data from Three Prototype Pump Power Modules

Figure VI-6. Power Output Data from Three Prototype Pump Power Modules

TABLE VI-4
PUMP MODULE PERFORMANCE STABILITY SUMMARY

Module	Testing Time (Hours)	Rate of Change: % Per 10,000 Hours				No. Cycles
		R _g	V _{oc}	P _e	η	
TEM-14A S/N-1	16078	**	**	-4.8	-6.4	9
TEM-14A S/N-2	12304	8.9	3.2	-2.3	-0.9	6
TEM-14A S/N-3*	7727	4.5	3.0	+1.3	-0.4	52

* Testing Terminated

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

C. SUPPORTING TECHNOLOGY DEVELOPMENT

1. Sub-Length Module

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 varying processing techniques, assembly clearances, and intercouple insulating methods on module performance.

a. TEM-15A Series Modules

All TEM-15A series modules utilize a single piece 0.005-in. -thick mica insulator between adjacent thermoelectric washers. These modules were designed to investigate the influence of mica radial clearance on mica deformation during processing, the influence of weld shrinkage on circuit alignment and mica deformation, and the influence of axial preload on mica deformation and circuit alignment in a module incorporating standard 0.005-in. -thick mica insulators.

Testing of 4 TEM-15A series modules, S/N-1, -4D, 4E, and -5, continued during the past quarter. All of these modules were tested under steady-state conditions of nominal $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$ cladding temperatures in static test facilities at WANL. Excellent long-term reproducibility of performance has been achieved in TEM-15A series modules. Electrical power output and overall efficiency data are presented in Figures VI-7 and -8, 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.

TEM-15A S/N-4E, the most recently fabricated module of this series, is identical in design to the baseline TEM-15A S/N-4 module. The TEM-15A S/N-4E module, however, was not hydrogen purged during its processing. The hydrogen purge step was eliminated from the processing sequence to evaluate its effect on module performance stability. This was done in anticipation of modules currently being designed with refractory claddings which cannot be hydrogen purged due to the formation of hydrides.

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Figure VI-7. Comparison of Power Output Data from TEM-15A Series Modules

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Figure VI-8. Comparison of Overall Efficiency Data from TEM-15A Series Modules

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Figure VI-9. Comparison of Power Output Data from TEM-15 Series Barrier Modules

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Electrical power and overall efficiency data from this module can be compared to Figures VI-6 and -7 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. Performance stability analyses, presented below, indicate that the elimination of the hydrogen purge has no effect on module degradation.

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, designated TEM-15B S/N-4A, was placed on test this quarter. This module 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, similar to the design of TEM-X S/N-3 series modules.

Electrical power output and overall efficiency data from TEM-15B S/N-4A are presented in Figures VI-9 and -10, respectively. Similar data from two other modules currently on test are also presented. 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. This module was destructively examined after a statistical analysis indicated no degradation of power output after 3637 hr of $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_H = 570^\circ\text{F}$ operation.

The TEM-15B S/N-4A power output and overall efficiency data can be seen to fall above the TEM-15B S/N-4 data, as expected. The improved performance results from the reduced tungsten radial thickness, thereby reducing shunt heat losses through the barrier.

c. TEM-15C Series Modules

TEM-15C series modules are designed with 2-piece mica washers coated with thin films of molybdenum formed by electron beam deposition. The washers used in TEM-15C S/N-8 were made from 0.0025-in. -thick mica sheets upon which 1000 to 2000 Å of Mo was deposited. Two washers are used at each mica position (Mo-coated faces together) so that the total barrier thickness is 2000 to

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Figure VI-10. Comparison of Overall Efficiency Data from TEM-15 Series Barrier Modules

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to 4000 Å at each position. The design of TEM-15C S/N-8, with the exception of the molybdenum coated mica and an increased clearance between the large mica insulator OD and outer boron nitride ID, is identical to that of the reference TEM-15A series module (S/N-4).

The purpose of the molybdenum barrier was to eliminate the diffusion of tellurium between adjacent lead telluride washers. Since the total electron-beam-deposited film is approximately 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.

Power output and overall efficiency data from TEM-15C S/N-8 are presented in Figures VI-9 and -10, respectively. Power output and overall efficiency data from the module are substantially better than data from the other barrier modules. The use of thin barriers essentially eliminates the shunt heat losses observed in modules incorporating foil. However, statistical analysis of these data indicates that the molybdenum has been ineffective in reducing module degradation rates.

d. TEM-15D Series Modules

The TEM-15D designation applies to modules designed with 0.010-in. mica insulators containing no additional diffusion barrier mechanisms. 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 2576 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-8 and -9. A reduction in the BOL level of both parameters (compared to TEM-15C S/N-8 data) is the result of replacing a portion of the lead telluride with mica in this design.

e. TEM-15 Performance Stability Analysis

Table VI-5 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).

TABLE VI-5
TEM-15 PERFORMANCE ANALYSIS SUMMARY

Module	Testing Time (Hours)	Rate of Change: % Per 10,000 Hours				
		R _g	V _{oc}	P _e	η	No. Cycles
TEM-15A S/N-1	6880	29	3	-19	-14	5
TEM-15A S/N-4*	2349	29	4	-19	- 6	2
TEM-15A S/N-4B*	2174	30	6	-17	-20	50
TEM-15A S/N-4D	7238	30	5	-16	-11	3
TEM-15A S/N-4E	2586	29	7	-14	- 9	2
TEM-15A S/N-5	6407	28	4	-17	-10	6
TEM-15A S/N-8*	2442	29	2	-23	-13	52
TEM-15B S/N-4*	3637	12	6	0	- 2	2
TEM-15B S/N-4A	1651	16	7	- 2	- 2	0
TEM-15C S/N-4*	2753	29	7	-14	-14	2
TEM-15C S/N-8	4646	28	3	-19	-12	3
TEM-15D S/N-4	2576	19	4	-11	- 7	2

* Testing Terminated

$\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$

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Degradation rates for four parameters (resistance, open circuit voltage, power output, and overall efficiency all adjusted to account for minor testing temperature variations) are presented.

Degradation rate data from the 4 long-term TEM-15 series modules remained essentially unchanged during the past quarter; i. e., no changes in previous established performance stability trends were observed. TEM-15A S/N-4E, the 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. This is important since TEM-X series modules fabricated with refractory claddings are not being hydrogen purged.

TEM-15D S/N-4, the module fabricated with mica thickness increased to 0.010 in. from 0.005 in., is exhibiting a somewhat lower degradation rate than the TEM-15A series modules. It appears that increasing the mica insulator thickness reduces degradation to a small, but measurable, extent.

The high degree of reproducibility in performance stability data from TEM-15 modules of a given design is apparent in Table VI-5. This reproducibility establishes confidence in the ability to determine the effect of various design modifications incorporated into technology modules.

2. Module Examination

a. TEM-15B S/N-4 and TEM-15C S/N-4

The TEM-15B S/N-4 module incorporated 0.0005-in. tungsten foil between mica washers to act as a diffusion barrier to n-type contamination by the p-type lead telluride. The module was removed for destructive examination after 3637 hr at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$.

Electrical measurements on n-type washers from a section that had operated at 1150°F showed the foil was completely successful in preventing n-type contamination across the small mica washer. However, the contamination was considerably less than observed in previous n-type washers that were obtained from modules which did not contain barriers.

Figure VI-11. Negative Ends of Thermoelectric Circuits from TEM-X S/N-2 and TEM-13G S/N-10. Each Module Given 50 Thermal Cycles 30°F/min Ramp Rates

Figure VI-12. Positive Ends of Thermoelectric Circuits from TEM-13G S/N-10. Each Module Given 50 Thermal Cycles 30°F/min Ramp Rate

TEM-15C S/N-4 utilized a barrier of EBVD coating on the mica washers. The module was removed from test after 2753 hr at $\bar{T}_H = 1125^\circ\text{F}$, $\bar{T}_C = 570^\circ\text{F}$. Destructive examination of this module yielded results imilar to those described above for the TEM-15B S/N-4.

Electron beam microprobe examination of the inner conductor ring/mica/lead telluride junction region is currently in process on samples from both of these modules.

b. TEM-14A S/N-3

The TEM-14A S/N-3, pump power module, was examined after 7727 hr at $\bar{T}_H = 1125^\circ\text{F}$ and $\bar{T}_C = 570^\circ\text{F}$. Seebeck probe traces revealed: (1) a slight contamination problem in the n-type washer similar to that found in the 2 modules described above, and (2) no bulk property changes in either n- or p-type washers in regions removed from the mica/conductor ring/thermoelectric region

As a result of the above investigations it has been decided to further alter the joint at the large mica interface. The data show, that, once this modification is successful both n- and p-type lead telluride can operate without changes in electrical properties at $T_H = 1125^\circ\text{F}$.

c. TEM-9A S/N-3 and TEM-9M S/N-2

The modules were material experiments designed to test the behavior of n- and p-type lead telluride for long periods of time at $\bar{T}_H \geq 1200^\circ\text{F}$. Destructive analysis is now in progress.

d. TEM-X S/N-2

TEM-X S/N-2 was fabricated to demonstrate the use of a low thermal expansion inner cladding to eliminate circuit distortion during module thermal cycling.

The module was tested for 207 hr at reactor conditions. During the test, the module was subjected to 50 controlled thermal cycles with a ramp rate of $30^\circ\text{F}/\text{min}$. After 207 hr, the module was removed from test and sectioned to expose the thermoelectric circuit. The destructive examination revealed that a major improvement in maintaining circuit alignment had been achieved. Figures VI-11 and -12 show the cross sections of TEM-X S/N-2 compared to those

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Figure VI-13. Relative Displacement of Inner Conductor Rings Versus Conductor Ring Axial Position for TEM-13G S/N-10 and TEM-X S/N-2

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of TEM-13G S/N-10. The success of the duplex clad is evidenced by comparison of the cross sections of these two modules, which were tested under identical conditions.

The positive and negative ends of the module were also examined to determine the extent of movement of the inner conductor rings with respect to the outer conductor rings. Figure VI-13 is a plot of the relative displacement between the inner and outer conductor rings versus conductor ring location along the module axis. Also included on this plot are similar measurements taken on TEM-13G S/N-10. TEM-13G S/N-10 was subjected to the same cyclic history as TEM-X S/N-2. The improvement in circuit distortion is obvious.

The results from TEM-X S/N-2 show conclusive proof that the reason for circuit distortion in thermally cycled modules was the mismatch between the thermal expansion of the inner and outer claddings.

The entire length of the inner and outer diameter of the duplex cladding was examined visually and metallographically. There was no evidence of NaK corrosion or oxidation observed at any point along the clad axis. The center section of the cladding, where the 2 extruded bimetal joints were electron beam welded was removed and metallographically prepared for examination via light microscopy. Figure VI-14 is a photomicrograph of the weld zone from TEM-X S/N-2. The microscopic examination revealed the joint had no indications of impurities, porosity, or inclusions.

The electron beam welding was done at a pressure in the 10^{-5} torr range. Sufficient work has been done to show that EB welding of refractory materials at pressures less than 10^{-3} torr results in oxygen levels at the weld zone which are equal to or less than the oxygen content in the base material. Consequently, essentially oxygen-free welds are assured at the center of the Ta-10W cladding by proper control requirements during EB welding.

The tandem transition joint between the 316 stainless steel ends and the Ta-10W center portion was removed, mounted separately, and examined via light microscopy and the electron beam microprobe. Figure VI-15 is a photomicrograph of a portion of the joint area. The photo is representative of the

*NASA Contract Report No. 54088, Quarterly Report, March, 1964, "Determination of the Weldability and Elevated Temperature Stability of Refractory Metal Alloys."

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Figure VI-14. Electron Beam Weld Zone from
TEM-X S/N-2 Inner Cladding

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Figure VI-15. Photomicrograph of Bond from
TEM-X S/N-2 Inner Cladding Showing
Intermetallic Diffusion Zone

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Figure VI-16. Photomicrograph of Positive End Section of TEM-X S/N-3

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entire bond area examined. No separation of the two materials was evident. The diffusion bond reaction is characterized by the thin layer of intermetallic formation (0.0001 to 0.0002 in.) at the Ta-10W/stainless steel interface. EB probe traces across the joint bond area showed the intermetallic layer was composed of nickel, chromium, and tantalum.

The width of the intermetallic layer can be controlled during the fabrication process by varying the processing temperature. A lower extrusion temperature would produce a two-fold benefit. Not only would the intermetallic layer be reduced in thickness but the bond would be strengthened because the strength ratio of the stainless steel to the Ta-10W would increase. There is also evidence that a stronger joint would result if the bond length, i. e. , the tapered transition region composed of Ta-10W and stainless, were increased. The joint from TEM-X S/N-2 had a bond length which varied from 1/2 to 1 in. A slight change in the billet preforms would result in a joint with a longer bond length and a stronger joint. These changes will be implemented in future claddings.

Destructive examination was initiated on TEM-X S/N-3 during this reporting period. The design features of this module are covered in a previous section. The module was examined to determine the effect of the processing cycle on circuit alignment. The positive end of the module was cut, sectioned, and cast in epoxy. An axial cut through the end section exposed the circuit for visual examination. Figure VI-16 is a photomicrograph of the section from TEM-X S/N-3. No deformation of the circuit was noted. Examination of the negative end showed the same results, i. e. , excellent circuit/conductor ring alignment.

3. Mica Evaluation

Two series of sleeved capsules containing 10-mil mica barriers and alternating pellets of p- and n-type lead telluride were removed from testing after 1000 and 2000 hr at 1202°F. Seebeck probe examination is currently in progress and the data are being evaluated.

Data from previously tested sleeved capsules indicated there was a possible correlation between the degree of compaction and barrier effectiveness. Accordingly, a new series of EXC sleeved capsule has been fabricated using alternating pellets of n-type and p-type lead telluride that were pressed to a higher density (~96% of theoretical) than previously pressed pellets (91% of theoretical).

The pellets are separated by 5-mil mica barriers. The capsules will be tested at 1202°F and compared with previously tested sleeved capsules having similar barriers.

A second series of high density lead telluride capsules was fabricated with two different terminations for the mica barrier. In one case the mica edge terminates at a boron nitride sleeve and in the other case at a stainless steel sleeve. One capsule from each series was placed on test for 1000 and 2000 hr, respectively. After completion of the test times, the capsules will be examined destructively to determine the effect of the reduced deformation of the mica on its effectiveness as an intercouple contamination barrier.

4. Improved Thermoelectric Materials

a. Electrical Resistivity Measurements

To increase the level of confidence in the accuracy of WANL measurement of the electrical resistivity of TE material specimens as a function of temperature, a dc measurement system was set up to operate concurrently with the standard ac system. Concurrent dc and ac measurements were then made on 6 experimental specimens over the temperature range 300 to 850°K. The correspondence of results obtained by the two methods was excellent, and it is considered that the adequacy and accuracy of the standard ac measurement system and technique have been established beyond question.

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

An ingot of composition $\text{PbTe}_{0.9585}\text{Se}_{0.05}\text{Na}_{0.0085}$ was melted and processed, and standard specimens were fabricated and their Seebeck coefficient and electrical resistivity measured. As expected, these properties fall midway between the corresponding properties for the 0.01 Na and the 0.007 Na compositions discussed in the previous quarterly.

Analyses of Seebeck coefficient (α) and electrical resistivity (ρ) measurements, together with calculated power number (α^2/ρ) and estimated figure of merit ($\alpha^2/\rho k$) values for the 6 p-type compositions investigated to date, have indicated that the highest "doped" compositions, i. e., $\text{PbTe}_{0.96}\text{Se}_{0.05}\text{Na}_{0.01}$ and $\text{PbTe}_{0.9643}\text{Se}_{0.05}\text{Na}_{0.01}$, have the highest figure of merit in the temperature range of interest. Continued work in the immediate future will therefore be concentrated on materials having these compositions.

To permit measuring the true electrical resistivities of the compositions, test specimens having near-theoretical densities are required to eliminate the effect of specimen porosity on the values obtained. Due to the hard, brittle nature of the p-type compositions, it has not been possible to fabricate acceptable high density specimens by cold pressing techniques. Accordingly, a hot pressing technique has been developed for fabricating cylindrical pellet specimens having high densities. After resolving some initial difficulties, a suitable process was worked out which involves the use of a graphite-lined molybdenum die, a helium working atmosphere, and pressing parameters of 650°C - 7000 psi - 1 hr. Fabrication of 5/8-in. -diameter specimens having densities greater than 98% of theoretical is now being routinely performed.

The WANL Seebeck - resistivity test apparatuses, originally set up to measure standard rectangular test bars, were modified to accommodate the cylindrical pellet specimens. Testing of high density hot pressed pellets was then initiated, but problems were encountered due to specimen fracture in the test apparatus. The fixture used to make voltage probe contact with the cylindrical pellets applied too high a stress to the hard, brittle specimens. Precision fixtures were designed and procured, and recent test runs indicate that the problem is now resolved.

Based on the first results obtained, it appears that the figure of merit (Z) calculated from α and ρ measurements on a hot pressed high density pellet is significantly higher than the corresponding Z value calculated from α and ρ measurements on a standard cold pressed specimen of the same composition. This is a very encouraging result because the physical condition of a hot pressed pellet is more representative of a TE washer in a fabricated module than is a cold pressed standard specimen. Specifically, the Z versus T curve calculated for a hot pressed $\text{PbTe}_{0.96}\text{Se}_{0.05}\text{Na}_{0.01}$ specimen (having a density 98.8% of theoretical) is quite comparable to the reference curve derived from the RCA data.*

The Z calculations made to date for p-type $\text{PbTe}_{0.95}\text{Se}_{0.05}$ alloys have all been based on calculated thermal conductivity values. Measurement of thermal conductivity over the temperature range of interest has awaited selection of the optimum composition and fabrication of specimens. Since $\text{PbTe}_{0.96}\text{Se}_{0.05}\text{Na}_{0.01}$

*"Improved Compatible Materials for Thermoelectric Power Generation,"
NYO-3886-2 November (1968)

TABLE VI-6
THERMAL CONDUCTIVITY OF P-TYPE TE MATERIAL SPECIMENS

Identification	Composition	T (°K)	Thermal Conductivity Measurements By (or Data From) K (Watts/cm - K°)		
			3M	RCA (3)	Ⓜ R&D
TEGS - 2P	~ PbTe _{1.01} Na _{.008}	302	.0373		.0431
		380	.0275		.0338
PX	Ternary PbTe - PbSe	302		.0349 (1)	.0325 (2)
		383		.0251 (1)	.0259 (2)

(1) RCA Composition PbTe_{.95} Se_{.05} Na_{.007}

(2) WANL Composition PbTe_{.957} Se_{.05} Na_{.007}

(3) RCA Data Corrected by WANL for Temperature Dependence of Specific Heat

is the present choice for optimum composition, a 5/8-in. -diameter specimen of this material (together with a similar specimen of TEGS-2P PbTe for control purposes) was sent to Resalab, Incorporated, for measurement of k over the temperature range 300 to 850°K.

Prior to making the optimum composition selection, and while awaiting the results for the Resalab measurements, some measurements were made on p-type specimens of the Westinghouse Research and Development Center in order to reinforce the belief supported by the RCA work* that the dilute alloys possess substantially lower thermal conductivity through the effect of alloying on the lattice contribution. The results of these measurements are presented in Table VI-6, where they are compared with the RCA data for their reported $\text{PbTe}_{0.95}\text{Se}_{0.05}\text{Na}_{0.007}$ composition and with the 3M Company data for TEGS-2P PbTe.

From Table VI-6 it is seen that the agreement between the RCA results and the Westinghouse Research and Development results on the ternary specimens is quite good, whereas the Westinghouse-3M agreement for TEGS-2P is rather poor. In the latter case, it should be noted that the 3M data were received in 1965, and the measurements were probably made in 1964; it is believed[†] that the TEGS-2P material currently being supplied by 3M has a somewhat higher carrier concentration than their 1964-1965 vintage material, which would mean that the newer material has a higher Weidemann-Franz thermal conductivity contribution and hence a higher total thermal conductivity, as indicated by the Westinghouse Research and Development measurements.

Thus, comparing the Westinghouse/RCA measurements on the ternary material with the Westinghouse measurements (or even with some compromise between the Westinghouse and 3M Company measurements) on TEGS-2P, we conclude that there is a substantial lowering of the thermal conductivity in the dilute-alloy ternary compositions..

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

Testing has been completed of specimens made from the 12 experimental compositions listed in Table VI-7. For each composition, at least one specimen

*Improved Compatible Materials for Thermoelectric Power Generation,"
NYO-3886-2 November (1968)

†Based on electrical resistivity measurements

TABLE VI-7
EXPERIMENTAL N-TYPE COMPOSITIONS

Base Compound	$\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}$	$\text{Pb}_{0.97}\text{Ge}_{0.03}\text{Te}$
Derived Experimental Compositions	$\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.00035}$	$\text{Pb}_{0.97}\text{Ge}_{0.03}\text{Te}(\text{PbI}_2)_{0.00035}$
	$\text{Pb}_{0.95035}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.00035}$	$\text{Pb}_{0.97035}\text{Ge}_{0.03}\text{Te}(\text{PbI}_2)_{0.00035}$
	$\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$	$\text{Pb}_{0.97}\text{Ge}_{0.03}\text{Te}(\text{PbI}_2)_{0.0007}$
	$\text{Pb}_{0.9507}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.0007}$	$\text{Pb}_{0.9707}\text{Ge}_{0.03}\text{Te}(\text{PbI}_2)_{0.0007}$
	$\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.001}$	$\text{Pb}_{0.97}\text{Ge}_{0.03}\text{Te}(\text{PbI}_2)_{0.001}$
	$\text{Pb}_{0.951}\text{Ge}_{0.05}\text{Te}(\text{PbI}_2)_{0.001}$	$\text{Pb}_{0.971}\text{Ge}_{0.03}\text{Te}(\text{PbI}_2)_{0.001}$

was fabricated using each of the hydrogen heat treating temperatures 650°C and 750°C. The first series of analyses was completed of the Seebeck and resistivity measurements, calculated power number, and estimated Figure of Merit values for all of these specimens. The various material/process temperature combinations were compared to one another on the following bases:

- 1) Graphical evaluation of $\int Zdt$ over temperature ranges currently of interest in module technology.
- 2) TEMØD calculation of overall efficiency of fictitious modules containing each of the n-type cases in combination with TEGS-2P p-type PbTe.

It appears that, on these bases, the following compositions give virtually indistinguishable results:

Thus, further considerations will have to be used to select the one or two optimum compositions for further investigations; these will be principally concerned with maximum Seebeck coefficient and minimum excess Pb content.

As in the case of the p-type materials discussed in the previous section, the above analyses and comparisons were all based on calculated thermal conductivity values. To firmly establish a satisfactory technique/apparatus combination for making definitive thermal conductivity measurements on the n-type PbTe/GeTe alloys, several measurements have been made on specimens of GE-NL n-type PbTe. Measurements of k versus T made on two GE-NL specimens by Resalab, Incorporated are plotted in Figure VI-17. For reference purposes the most recent data available from 3M Company on their TEGS-2N material, as well as the RCA reference data for a ternary $\text{Pb}_{0.95}\text{Ge}_{0.05}\text{Te}$ alloy, are also plotted in this figure. Finally, the WANL thermal conductivity apparatus was set up and made operational, and a series of measurements made on a GE-NL specimen; these data are also plotted in Figure VI-16. While the agreement between the Resalab and WANL results is not outstanding, particularly above 750°K, it is considered sufficiently good to justify initiating measurements on ternary materials at Resalab (as discussed previously, this has actually been done in the case of the p-type ternary materials).

5. Electron Beam Vapor Deposition (EBVD)

Work is in progress on setting up a new vacuum chamber and electron gun in order to increase the time available for deposition work.

Six experimental deposition runs were made at WANL during this period. Although some success was achieved using a pedestal-type heat sink, "good" coatings continue to be very thin and considerable run-to-run variability continues to be experienced. "Powdering" of the deposit when the film thickness exceeds a few hundred Angstroms is a regular occurrence.

Figure VI-17. Thermal Conductivity of n-Type Thermoelectric Materials

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In addition to the above, four deposition runs were made at another facility using an Airco-Temesca system designed specifically for EBVD work. Again, "powdering" of the deposit was the inevitable characteristic of films greater than a few hundred Angstroms thick. It appears that this problem is associated with the mica itself, rather than with the tungsten or the equipment system, and work in the immediate future will be directed toward investigating this possibility.

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VII. COMPONENT DEVELOPMENT

A. INTRODUCTION AND SUMMARY

Current component development efforts include heat transfer and hydraulic studies and component technology development. Reactor components of interest are the reactor control system, including the drum drive, actuator, bearing, and 4 π shieldable control drum, and the reactor shielding system.

The test section for conducting the fuel element heat transfer studies of the S8DR design core was fabricated. The methods for adapting existing techniques for fabricating electrical heater assemblies with multiple thermocouples on the inner wall of the cladding are being developed. A surplus NaK test loop was modified to mate with the test section and updated to provide the operational performance required for the tests. Analytical computer models for calculating heat transfer parameters from empirical test data for various fuel element spacings and clustering configurations are being prepared.

The hydraulic tests on the full core model of the S8DR have confirmed that the twin vortices in the inlet plenum tend to accumulate any gases in the flow stream and preferentially feed the gases into selected coolant flow channels. The gases generally remain in the injected flow channels as they traverse through the core. The core model is being assembled with instrumented elements to measure the size and velocity of the gas bubbles, as a function of gas injection and total flow rates.

Results of the preliminary tests conducted on the 19-element hydraulic test section indicated that the interchannel mixing of the original fin on fin fuel element design is minimal. A fin on wall, right-left-neutral (R-L-N) fin configuration yielded much greater mixing. Parametric tests to optimize the fin configuration are in progress. The fin configuration of the preliminary design fuel elements will be based on the results of these parametric tests.

One S8DR design actuator successfully continued its extended life test with 24,548 hr and 283 thermal cycles to 1125°F to date. Similarly, two sets of prototype actuator coils and an advanced bearing have completed approximately 15,000 hr of endurance testing at 1300°F and 1500°F, respectively. These test

conditions are more severe than expected on the ZrH reactor. After 4000 hr of test at 1500°F, the alumina surfaces without any dry film lubricant continue to exhibit friction characteristics satisfactory for control drum use.

The first developmental 4π shieldable control drum, which experienced failure of 1 BeO reflector block during the thermal and vibration testing, is being rebuilt to incorporate design improvements. The rebuilt drum will be utilized for the thermal, vibrational, and operational tests of the drum drive system. The fabrication of the second developmental drum, which includes B_4C inserts in the Ta-10W absorber to decrease mass and improve drum worth, is being completed. The relative control worth of this drum will be checked out in the SCA-4A facility prior to its final use in the thermal test of the drum assembly in a nuclear environment. The layout design of this irradiation test to be conducted in the STIR facility was completed. A heat pipe is utilized in the test fixture to provide simulation of the isothermal 1100°F drum drywell wall of the ZrH reactor.

The presence of oxides at the interface of the iron-titanate emittance coating and the tungsten undercoat was tentatively identified as the cause of the spalling of the emittance coating from one side of the Ta-10W samples subjected to thermal cycles. Oxidation of the tungsten undercoat apparently occurs during the plasma spray application of the iron-titanate (under an argon cover gas) on the opposite side of the material. Thus the coating failure takes place on the second side to be coated with iron-titanate. Equipment for providing a controlled high purity argon environment during the automated plasma spraying process is being built.

The large diameter LiH checkout shield was successfully cast in the large new LiH shield casting facility. Only minor problems were encountered with the facility during the casting operation. These have been corrected. No significant distortion of the shield casing resulted from the casting cycle. The shield is very dense with a small void volume and minimal cracks and fissures. The disassembly examination of the shield is pending the completion of the large examination dry box.

Results of the corrosion tests conducted at 1300 and 1500°F for periods up to 10,000 hr on Nb-1Zr capsules partially filled with lead show that this material is an excellent candidate from the structural and containment consideration.

The liquid lead gamma test shield assembly was cast and subjected to several thermal cycles to 1400°F. The causes for the unexpected distortion resulting from these thermal cycles are being investigated.

The design of the prototypical annular LiH shield surrounding the reactor vessel in the 4 π shielded configuration was completed. This shield will be fabricated, cast with LiH, and tested under thermal and vibration environments to evaluate the design concept and fabrication methods.

B. FUEL ELEMENT HEAT TRANSFER STUDIES

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

Preparations are under way to conduct heat transfer tests on four positional configurations of the instrumented heaters in the previously described 19-element test section.* The first configuration will have all heaters in a close-packed array (Pitch/Diameter = 1.00) to provide a reference. The heaters on the balance of the tests will be spaced to give an average Pitch/Diameter (P/D) ratio of 1.018. In one, the heaters will be uniformly spaced, while in the other two, the center heater will be displaced to contact one or two adjacent heaters. The latter 3 configurations simulate the S8DR core with and without clustering.

Models for computer analysis of both the uniformly spaced close-packed and non-close-packed fuel element configurations have been developed and checked out. These models will accept the test data, including the local inner

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wall temperatures of the heater cladding, and calculate the local and average heat flux, outer wall temperatures, and film coefficients. Additional models for the cases of asymmetric center heater are being developed.

The test section, Figure VII-1, has been fabricated up to the installation of the heater assemblies. Two inner shroud assemblies, Figure VII-2, were built to permit one to be assembled with heaters and thermocouples while the other is being installed and tested. These shroud assemblies will accommodate all four planned test configurations. The half round outside filler pieces, shown on Figure VII-3, will be modified to adjust for the change in the overall heater bundle diameter between the close-packed and non-close-packed configurations. Methods of attaching thin rings to the heater assemblies to positively position them in the nonclustered position were developed. The axial locations of these rings are selected for minimal perturbation of the NaK flow over the instrumented heater section.

The methods for fabricating the instrumented heater assemblies are being developed. Initially, 0.020-in. -diameter thermocouples are accurately located and attached by TIG welding to the inner wall near one end of a stainless steel tube. After inspection and checkout, a tubular extension is added to the thermocoupled end by EB welding to make up the instrumented heater cladding assembly. The helically coiled heater element and the MgO insulation are then placed within the cladding and the total assembly is swaged to compress the MgO insulation to a controlled density. This is the same technique used for fabricating standard cartridge type heaters. For the heat transfer test purpose, the central location of the heater coil and the uniformity of the coil pitch are critical.

Three preproduction heater assemblies have been built and 2 were performance tested in a heated NaK environment. Under isothermal conditions without any power to the heater, the temperature indicated by the thermocouples attached to the inner wall were within acceptable tolerance. With power applied to the heater the variation of the thermocouple readings was greater. Improved methods of centrally locating the heater coil are being introduced. The thermal conductivity of the MgO insulation computed from the test data is approximately one-half of the value originally expected. Lower thermal conductivity of the MgO is beneficial for test purposes because the local cladding temperature is

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Figure VII-1. Heat Transfer Test Section

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Figure VII-2. Shroud Assembly, Heat Transfer Test Section

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Figure VII-3 Shroud Assembly Showing Half Round Fillers

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less influenced by the heater temperature. Original plans were to conduct the heat transfer tests at 1200°F NaK inlet temperature to take advantage of the decreasing thermal conductivity of MgO with temperature. The lower thermal conductivity indicated from the heater performance tests will permit these heat transfer tests to be conducted with NaK inlet temperatures of approximately 600°F.

The existing NaK test loop, which had been recently moved into the test facility, was modified extensively to upgrade its capabilities as required for the heat transfer tests. The significant improvements included:

- 1) Addition of an ac EM pump to increase the pressure capacity to 8 psi at 30 gpm flow rate.
- 2) Addition of flow-through cold trap and an economizer to minimize the oxide level of the NaK. Oxides can affect the film coefficients.
- 3) Installation of larger fill and dump tanks.
- 4) Rerouting of piping and valves to accommodate the test section.
- 5) Addition of necessary instrumentation.
- 6) Revision of the operational control system.

The preliminary operational checkout of the modified test loop, Figure VII-4, was conducted to assure the necessary operational performance.

2. ZrH Reactor Core With Finned Fuel Elements

This activity will be initiated when the preliminary configuration of the ZrH Reactor core with finned fuel elements is defined. This selection will be based upon the results of the current core hydraulic studies and core engineering and design efforts. Initial investigations indicate that the S8DR test section previously described should be usable with the potential finned element core configurations.

C. FUEL ELEMENT AND CORE HYDRAULICS

Hydraulic studies are being conducted to characterize the coolant flow through the S8DR and the ZrH reactor cores. The parameters of interest are the pressure variation in the inlet plenum and its effect on the individual channel

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Figure VII-4. Heat Transfer Test NaK Loop

flow, pressure drop through the core and interchannel mixing. Of particular interest are the affects of entrained gases on the coolant flow through the S8DR core. 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

Hydraulic model of the complete S8DR core with adequate simulation of the inlet piping is being studied with the following objectives:

- 1) Determine coolant flow patterns in the inlet plenum with and without gas in the liquid.
- 2) Photographically record, if possible, the flow behavior in the core coolant channels using transparent "dummy" fuel rods.
- 3) Photograph flow behavior above the outlet grid plate.
- 4) Determine effects of gas in the coolant on the pressure distribution below the inlet grid plate.
- 5) Determine flow void fractions in the core flow channels.
- 6) Determine the coolant flow distribution in the core flow channels with and without gas entrance into the coolant.
- 7) Qualitatively determine the mixing characteristics of the flow beneath the upper grid plate.

The design of the full core model and the results of the preliminary tests were reported previously.* Additional tests at water flow rates ranging from 40 gpm (equivalent to 30% of the rated NaK flow) to 170 gpm (130%) with helium gas injection equivalent to 0 to 6% of the total flow were conducted. The flow pattern and the bubble distribution in the inlet plenum below the inlet orifice plate, through the inter-element flow channels and in the outlet plenum above the upper grid plate were observed visually and photographically. The previously described twin vortices in the inlet plenum, Figure VII-5, were evident in all cases. Each of the vortices tended to concentrate the injected gas bubbles

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Figure VII-5. Twin Vortices in S8DR Core Model Inlet Plenum

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and feed them intermittently to one of the adjacent holes in the inlet orifice plate. The gas normally remained within the flow channels to which it was injected as evidenced by the location of the bubbles when exiting from the holes in the upper grid plate.

Tests were also conducted to map the pressure distribution at the holes in the inlet orifice plate. The installation of the pressure probes is illustrated in Figure VII-6 and can be seen in the photograph of the lower plenum, Figure VII-7. Tests were conducted with the probes located in two different orientations within the core as indicated in Figures VII-8 and -9. The recorded pressure data were converted to velocities and compared to original data from the hydraulic tests conducted during the design of the S8DR core. These comparisons are shown on Figures VII-10 and -11. The velocity data from the original tests were extrapolated from the measurements made within the flow channel at several elevations. In an attempt to determine whether the presence of the gas bubble might induce acoustic noise in the core which may increase the stresses in the fuel element, a probe was placed in the upper plenum close to the grid plate. Acoustic noise with frequency of 35,000 to 40,000 Hz could be detected with the probe in contact with the metal outlet grid plate. The amplitude of the noise increased as the volume of gas injected increased. The noise pickup was approximately coincidental with the initial entry of the bubble into the inlet plenum. The presence of noise was less discernable with probe in the flow stream.

2. ZrH Reactor Core Hydraulic Studies

The initial efforts on the ZrH core studies are concentrated on generating empirical interchannel cross mixing and pressure drop data as functions of the configuration parameters of the spiral fins on the fuel element. These fins are expected to promote cross channel coolant mixing to reduce local cladding hot spots as well as providing physical support to minimize clustering. Initial tests are necessary to provide empirical data, which are nonexistent for the proposed fin configuration, to confirm the validity of the analytical models being utilized for the core design.

The previously described 19-element model* was completed and preliminary mixing and pressure drop tests were conducted. In these tests, the mixing

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Figure VII-6. Pressure Probe Position

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Figure VII-7. View of S8DR Core Model Plenum

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Figure VII-8 Pressure Probe Locations, Configuration 1

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Figure VII-9. Pressure Probe Locations, Configuration 2

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Figure VII-10. Typical Hydraulic Test Velocity Profile, Probe Configuration 1

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Figure VII-11. Hydraulic Test Velocity Profile, Probe Configuration 2

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Figure VII-12. 19-Element Mixing, Top View, Configuration 1

(cross channel flow) of the water is determined by injecting a salt solution of known concentration into a selected flow channel and monitoring the change of electrical resistance of the water as it traverses the flow channel. The electrical resistance of the water is measured by 4 conductivity probes located at 4-in. intervals along an instrumented element adjacent to the flow channel. The resistance variation at each measuring point is determined by statistical analysis of the recorded data. The flow mixing, ratio of lateral to axial flows, is calculated by the analysis of the data from all of the instrumented elements. The pressure drop is measured by pressure taps in the inlet and outlet plenums.

The fin configurations tested included the original reference design of identical fins on all elements, no fins (to establish a datum), and elements with fins of alternating fin-wrap orientation adjacent to unfinned elements (right-left-neutral, R-L-N). The unfinned elements are required to facilitate assembly of the elements into the core. The tops of the fins on adjacent elements of the first configuration are in contact. In the last configuration a nominal 0.010-in. radial clearance exists between the top of the fins and the walls of the adjacent element. The element configuration and arrangement of the elements in the test section for each are listed in Figures VII-12, -13, and -14 and Table VII-1.

As expected, very limited cross flow and minimum pressure drop were evident with unfinned elements. Fins with identical wrap orientation on all elements resulted in less than expected mixing. The low mixing can be probably attributed to the fact that the liquid flow in the helical channel around each element tends to oppose the similar flow in the adjacent element at the interface, as can be visualized on Figure VII-12. The R-L-N fin arrangement increased the mixing to the range utilized for the Reactor core hydraulic and thermal analysis. The flow pattern tending to increase cross flow can also be visualized on Figure VII-14. The pressure drop for the R-L-N arrangement was slightly less than the other finned case. The mixing and pressure drop data are summarized on Table VII-1.

Additional tests with the R-L-N fins are being planned to optimize the configuration.

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Figure VII-13. 19-Element Mixing, Top View, Configuration 2

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Figure VII-14. 19-Element Mixing, Configuration 3

TABLE VII-1
ZrH 19-ELEMENT HYDRAULIC MIXING STUDIES

Configur- ation	Fins Per Element	Fin Thickness (mils)	Fin Pitch (in.)	Fin Width (mils)	Element OD (in.)	Element Pitch (in.)	Mixing $\frac{\dot{W}_{\text{lateral}}}{\dot{W}_{\text{axial/in.}}}$	Pressure Drop (psi)*
1	4	20	4	50	0.560	0.600	3%	6.8
2	0	0	0	0	0.560	0.600	0.3%	3.7
3	3	30	4	50	0.560	0.600	25%	6.5

*The pressure drop is indicated for a water flow of 10 ft/sec through the flow channels.

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 potential high temperature, high reliability conductor materials for actuator coils. These tests are being performed on coils wet-wound with Al-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, Cube Alloy, and Oxalloy. The Nickel Clad Silver conductor consists of a fine silver core swaged in "A" nickel cladding. The composite wire is 80% silver by cross-sectional area. Cube Alloy conductor is a copper base or matrix, dispersion hardened by addition of fine particles of beryllia. Oxalloy conductor consists of an OFHC copper core swaged in a chrome-iron alloy (430 stainless steel) cladding. The composite wire is 72% copper by cross-sectional area.

A performance test is under way of prototype actuator coils, built to the configuration, bobbin shape 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 6 coils wound with Cube Alloy, and 6 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 this failure investigation will be delayed until the next scheduled test shutdown. Testing is continuing and the remaining coils have accumulated 116 thermal cycles and 13,601 hr at 1300°F in a total test time of 14,642 hr of a scheduled 20,000 hr.

2. Actuator Performance

After completion of the S8DR Design Verification Testing, one actuator was continued on test to demonstrate extended life actuator performance at temperature. The total test time on this actuator is now 24,568 hr, of which 21,704 hr have been at 1125°F. A total of 283 thermal cycles has been accumulated, of which 143 have been startup cycles. The startup cycle consists of operating the actuator continuously for 25 min in conjunction with a thermal ramp. These hours do not include the 5340 hr of test at ~1025°F, conducted on mechanical parts prior to the rebuild to the latest S8DR design. Performance of this actuator is normal.

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)
- 5) Extended endurance run (current status)

3. Actuator Design

The design effort, targeted towards the completion and review of the design layout near the end of this fiscal year, is continuing at a minimal level. The

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Figure VII-15. Advanced ZrH Bearing Simulated Life Test Friction Torque at 1500°F,
 10^{-8} Torr Al_2O_3 vs P5 Carbon-Graphite

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previously described* mockup actuator, designed to provide an interface for the control drum drive train test discussed in Section VII-E, has been fabricated. This unit is scheduled to be tested in the control drum drive system test (Section VII-G).

E. CONTROL DRUM BEARINGS

A simulated life test of a bearing is being conducted to demonstrate operational capability of 20,000 hr at 1500°F and 10^{-8} torr. The bearing, with carbon-graphite vs Al_2O_3 plated Ta-10W friction surfaces, has completed 15,400 hr at temperature. Testing has included two 1000-hr dwell periods and 12,000 hr with periodic bearing rotation. A third 1000-hr dwell period is currently in progress. A summary of bearing friction-torque is given in Figure VII-15. Maximum friction-torque has been 7.7 in. -lb, equivalent to a friction coefficient of 0.27, and occurred immediately prior to the third dwell period. The average friction torque has been 5.6 in. -lb in comparison to the bearing specification limit of 18.0 in. -lb at 1350°F. Following the current 1000-hr dwell, a series of thermal cycles between 1500°F and 450°F will be conducted with additional testing at 1500°F to a total test duration of 20,000 hr.

A material evaluation test of solid Al_2O_3 against Al_2O_3 plated on Ta-10W is continuing at 1500°F and 10^{-8} torr. Two test couples are being evaluated with 4000 hr at temperature accumulated to date. Static friction coefficients of the two couples are shown in Figure VII-16. Prior to visual inspection of the test specimens after 2900 hr at test, friction coefficients appeared to be stabilized around a friction coefficient of 0.23 to 0.26. Following inspection, the friction has risen to about 0.43 for couple 1 and 0.32 for couple 2. A second dwell is currently in progress which will be followed by additional friction testing. The maximum friction coefficient of 0.43 would result in a friction-torque of 12.2 in. -lb on the bearing of the configuration undergoing life test. This friction-torque would be well within bearing specification limits of 18.0 in. -lb.

Studies to evaluate the effects of partially oxidizing vacuum environment on the carbon-graphite control drum bearings was initiated. This environment simulates operational conditions of the bearings in the reference reactor dry

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Figure VII-16. Advanced ZrH Bearing Material Evaluation Test Friction Coefficient vs Time at 1500°F, 10^{-8} Torr Solid Al_2O_3 vs Al_2O_3 on Ta-10W

well where the irradiation of BeO causes the production of oxygen. The purpose of the test is to evaluate the relationship of friction and wear to carbon-graphite oxidation. The test fixture was designed and is being fabricated.

F. ~~4~~ SHIELDABLE REFLECTOR

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

1) Control Drum Development

2) Material Studies

1. Control Drum Development

a. Development Drum No. 1 and No. 2

Development Drum No 1, which had a BeO block fracture during thermal or vibration testing,* was disassembled. The residual tension on the bands of the other 5 BeO blocks was measured during disassembly using strain gages. The tension varied from 1.5 to 20 lb as opposed to the installed tension of 70 lb. This verified the original conclusion that the band tension had released primarily due to deformation of the band at the rivets as a result of loads produced by differential thermal expansion of the band material and the BeO. The failure of the rivets to completely expand in the holes in the band at installation could also have contributed to the relaxation.

Development Drum No. 1 is being rebuilt with the redesigned band and rivets (same as described previously* for Development Drum No. 2). The rebuilt drum will be tested as part of the Drum Drive System. (See Section VII-G.)

Development Drum No. 2 is being readied for assembly into the critical test configuration with only the BeO blocks awaiting final machining. The B₄C slugs will be slipped into the absorber holes and restrained by mounting the end plates. The BeO blocks will be mounted on the absorber and held by wire wrapping. The cladding will also be held by wire wrapping. This will provide a true reference drum configuration for measuring drum worth on the SCA-4A (SNAP Critical Assembly). After the critical test the drum parts will be fitted with thermocouples and permanently assembled for use in the Thermal Gradient Test, described subsequently.

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Figure VII-17. ZrH Reactor Control Drum Irradiation Test

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b. Drum Rivet Studies

The redesign of the BeO attachment to the developmental drums included use of a larger rivet (1/4 in. vs 5/32 in. diameter). These have been received and test specimens in maximum and minimum tolerance holes demonstrated good expansion of the rivet into the undercut in the base and complete filling of the hole in the band.

A test program to prove the band redesign has been started. A BeO block will be banded by reference techniques using the redesigned band to a Ta-10W block. The assembly will be thermal cycled, vibrated, and examined for residual tension to verify redesign calculations.

All parts are now available for a similar evaluation test of the refractory of metal cladding rivets. Sample cladding material will be rivetted to a base, the assembly will be thermal cycled, and then the force to pull the rivets out will be measured.

c. Drum Thermal Gradient Test

A test of the control drum assembly to evaluate the combined thermal and nuclear heating effects is in the design phase.

Layout of the test capsule (shown schematically in Figure VII-17) for conducting this test in the Shield Test and Irradiation Reactor (STIR) facility was completed. A heat pipe to provide a simulated dry well with an isothermal 1100°F temperature was selected after other schemes resulted in large temperature fluctuation on the periphery of the well. Cartridge heaters will provide control of the pipe temperature. The stainless steel wall of the heat pipe will be oxidized to provide equivalent emissivity of the reactor dry well. A contract to design and fabricate the heat pipe assembly is being negotiated with an experienced vendor.

The design includes 89 thermocouples in the BeO, end plates, B₄C, bearing and on the cladding to provide temperature profiles during the thermal test. Linear transducers are provided to measure bowing of the drum. The capsule will be evacuated during operation and provision for positioning the drum over 180° of arc are included. A test plan covering test objectives, requirements, and technical approach was issued to aid in coordination of the design, analysis, and test operations efforts.

The nuclear model for the test has been completed and computer runs are being made to determine the heat generation rates with the drum under the STIR radiation spectrum. The original 2-dimensional thermal model of the control drum was expanded to a 3-dimensional model. This model will be utilized to predict the temperature profile of the various drum components when the heat generation rates become available from the nuclear analysis.

2. Material Studies

The material study effort consists of 7 subtasks: (1) Material Selection, (2) Material Compatibility Tests, (3) Emittance Test, (4) Coating Development and Tests, (5) BeO Modulus of Rupture Tests, (6) Thermal Expansion Tests, and (7) Dry Well Emissivity Development.

a. Material Selection

The materials and coatings selections remain unchanged and are:

Absorber — Ta-10W with plasma sprayed tungsten and an outer high emittance coat of plasma sprayed iron titanate. B_4C slugs (1.047 in. diameter) are inserted into Ta-10W.

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

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

Dry Well — 316 SS with inner surface oxidized for high emittance.

b. Compatibility Test

The bare sample compatibility continues at 1800°F in a low 10^{-7} torr pressure. Over 11,000 hr have been accumulated.

c. Emittance Test

No additional tests have been run but samples of Cb-1Zr with CVD tungsten coating are being prepared for emittance measurement.

d. Coating Development and Test

As reported previously,* the plasma sprayed iron titanate was found to adhere to plasma sprayed tungsten but not CVD tungsten. Samples of Cb-1Zr are being CVD coated with tungsten and will be plasma spray coated with tungsten and iron titanate for further evaluation of the coating. The loss of the reference coating on 1/8-in. -thick Ta-10W from one side during thermal cycling was evaluated. Analysis indicated presence of oxides at the flaking interface which may have formed during the iron titanate coating of the opposite side under an argon blanket. It was concluded that the plasma coating process must be performed in an oxygen free environment to preclude this formation of the oxides.

An existing AEC vacuum-dry box is being modified to provide cooling and filtering of the controlled high purity argon environment. Modification includes mechanization of the spraying process for better process control.

Coupons of Ta-10W plasma sprayed with tungsten and iron titanate in a supplier's semicontrolled glove box and coupons coated under an argon cover bath have been prepared. These will be thermal cycled to provide a preliminary comparative evaluation of the processes.

A full-size Nb-1Zr cladding piece (approximately 19 in. long by 4.5 in. diameter) have been successfully coated on both sides with CVD tungsten.

e. BeO Modulus of Rupture Tests

Samples of the BeO (UOX + 0.5% MgO) have been prepared by core drilling BeO block and are ready for modulus of rupture testing.

f. Thermal Expansion Tests

Samples of annealed Ta-10W, recrystallized and stress relieved Cb-1Zr and BeO (UOX + 0.5% MgO) in normal and radial direction have been prepared for measuring thermal expansion.

g. Dry Well Oxidation Development

The initial samples of 316 SS oxidized by a wet hydrogen process were prepared. A range of temperatures and time and two cleaning processes were used. Evaluation methods are being formulated.

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G. CONTROL DRUM DRIVE SYSTEM

The control drive system for the ZrH reactor consists of the rebuilt control drum No. 1, its supporting bearings, mechanical mockup of the actuator with locking cam and drive gears, and the drive shaft with flexible couplings.* The actuator, drum bearings, and drive shaft have been fabricated. The application of the dry film lubrication to the drive gear and the rebuild of the control drum are in progress.

The drive system will be tested under simulated launch loading and under simulated reactor thermal vacuum conditions. Fit-up and preliminary evaluation of the system will be conducted on the existing vibration test fixture. The thermal test fixture is about 90% fabricated.

H. SHIELD FABRICATION DEVELOPMENT

The design and fabrication technologies applicable to flight type shield systems are being developed. The evaluation of gamma shielding materials also is being conducted.

1. Materials Studies

An objective of the liquid lead (Pb) shielding program is to investigate the materials problems associated with the use of liquid Pb as a high temperature gamma shield for the SNAP Reactor Shield system. Earlier studies on the effects of molten Pb on selected iron-base alloys revealed that the Croloy 2-1/4 steel was a satisfactory material for use up to 1500°F based upon its corrosion resistance although, because of metallurgical characteristics, the alloy is not recommended for temperatures significantly above 1300°F.

A program was initiated in 1969 to determine the corrosion resistance of the niobium-zirconium alloy at 1300 and 1500°F for periods up to 10,000 hr.

The same type of test capsule was chosen for this test as was used in the previous Pb corrosion tests; that is, a tubular capsule pinched and welded at one end, half-filled with Pb, pinched and welded at the other end under a vacuum of about 10^{-5} torr, and enclosed in an evacuated quartz tube. The capsules were heated in the horizontal position in a large box furnace.

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Test capsules were removed from the furnaces after 2500, 5000, and 10,000 hr of heating at 1300 and 1500°F and visually examined. In all instances, the quartz was unclouded and transparent; and the Nb-1Zr alloy was as shiny as when the capsules were originally sealed. Several empty control capsules also were prepared.

One end of each quartz tube was broken and the metal capsules removed. One end of each Nb-1Zr capsule was cut off for metallographic examination of the Pb-Nb-1Zr interface. The other end was cut from the 10,000-hr capsules for examination of the weld area. A sample of Pb was removed from each capsule for chemical analysis.

The interior surface of the empty control capsule heated for 2500 hr at 1500°F (no 1300°F controls were prepared) was found to be smooth and the grain structure of the metal was typical of the aged alloy. The two capsules containing Pb, heated at 1300 and 1500°F, were found to have a thin, porous, spongy layer (about 1 mil thick) on the inner surface of the tube apparently from some intergranular penetration by the Pb. The same appearance was noted in the Nb-1Zr exposed only to Pb vapor at 1500°F, but not at 1300°F.

After 5000 and 10,000 hr of heating in contact with Pb, the Nb-1Zr alloy appeared to have suffered much less attack than after 2500 hr, indicating a possible initial early dissolution of surface metal until the Pb was saturated.

Chemical analysis of the lead samples, cut perpendicular to the length of the capsule to provide a cross-sectional profile, revealed the lead to contain 50 ppm or less Nb and less than 20 ppm Zr. No effect of temperature or length of exposure was present.

Metallographic examination of the welded Nb-1Zr revealed that the welded alloy had a corrosion resistance similar to the parent metal.

Based upon these limited studies, it was concluded that the Nb-1Zr alloy was superior to the iron-base alloys, especially at the 1500°F test condition, and it was concluded further that the Nb-1Zr alloy was an excellent candidate for a structural/container material at temperature up to 1500°F for long periods of time as may be required for future space reactor gamma shields.

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Figure VII-18. Shield Assembly - Liquid Lead

2. Design and Fabrication Development

a. Liquid Lead Shield

The fabrication of the Croloy 2-1/4 steel vessel, lead (Pb) filling hardware, and the test fixture was completed. Figure VII-18 schematically shows the liquid lead shield, and Figure VII-19 shows the top of the Croloy 2-1/4 vessel resting in a holding fixture.

The Pb filling hardware was mated with the vessel (Figure VII-20), and the assembly installed on the base of the 48-in. -diameter retort in Building T003 at the Nuclear Field Laboratory. About 900 lb of high purity (99.9 + % Pb) lead bricks were loaded into the upper vessel. The surface of each brick was cleaned using a wire brush to remove surface oxides, etc., prior to loading.

The retort was placed on the base, evacuated, and backfilled with argon gas to 1.6 psig pressure. The furnace was placed over the retort and the contents heated to $1450 \pm 5^\circ\text{F}$. The filling hardware was designed to function in the following manner:

- 1) As the lead melts, it collects in a pool in the bottom until the liquid level reaches a height determined by the level control loop.
- 2) The lead flows into the shield vessel through the filling tube.
- 3) The shield vessel fills to the level of the overflow tube which was adjusted to equal the highest point of the vessel.
- 4) The lead continues to flow into the vessel, overflowing into an overflow vessel until the lead in the reservoir vessel drops to the level of the level control loop.
- 5) Siphoning of the lead from the upper vessel is prevented by the anti-siphon tube, retaining the lighter impurities in the lead in the reservoir.

The retort and its contents were cooled to room temperature, and the retort unsealed and removed. The filling tube and the vent tube were cut off and Croloy 2-1/4 steel discs welded into the shield vessel. The shield vessel was evacuated to 20μ pressure through the overflow tube, the tube pinched shut in two places, and the tube cut at the outer pinch and welded shut.

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Figure VII-19. Top View of Croloy 2-1/4 Steel Vessel

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Figure VII-20. Shield Vessel and Pb Filling Hardware

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Figure VII-21. Lead Shield During Assembly into the Test Fixture

The weight of the Pb in the shield vessel was calculated to be 624.5 lb by subtracting the weight remaining from the weight of Pb loaded into the upper vessel.

The Pb shield was assembled into the thermal cycling test fixture (Figure VII-21). The dish which simulated the bottom of the reactor vessel was placed in the fixture. The inner surface of the dish was coated with iron titanate to improve the heat transfer from the heater to the top of the shield vessel. The heater consisted of 5 Kanthal wire coils on alumina rods. Each heater was connected to individual power control units.

In addition to the 12 Chromel-Alumel thermocouples inside the shield vessel, thermocouples were installed on the outer surface of the dish and the bottom of the shield vessel. Five strain gages also were installed on the side and bottom of the shield vessel. A thick layer of insulation on the bottom of the shield vessel completed the assembly.

The assembly was placed in the vacuum retort (Figure VII-22) in Building T062 at the Nuclear Field Laboratory, and the heaters, thermocouples, and strain gages connected to the vacuum feed-throughs of the retort. The top of the retort was installed and the retort evacuated to 2×10^{-5} torr. The power to the heaters was turned on to begin the thermal cycle:

- 1) Heat dish to 1400°F in 2 hr
- 2) Hold dish at 1400°F until lead is melted
- 3) Increase dish temperature to about 1650°F to raise lead temperature to 1400°F
- 4) Turn off heaters and allow assembly to cool.

The first heating cycle was aborted when several heaters burned out. The test was shut down and the retort opened. It was found that the heater wire had vaporized in the 2×10^{-5} torr vacuum.

The heaters were rewound and oxidized by heating in air for 30 min. The retort was reinstalled and the test restarted under the 10 to 20 μ pressure maintained by operating only the mechanical roughing vacuum pump.

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Figure VII-22. Test Fixture Installed in the Vacuum Retort

The dish was heated to 1400°F during the first 2 hr of the test, and the 1400°F temperature was maintained for 1-3/4 hr until all the lead was melted. The dish temperature was slowly increased to 1650°F to bring the lead temperature to 1400°F. It was found that the 1650°F dish temperature was incapable of heating the lead to 1400°F, and the dish temperature was slowly increased to about 1900°F to obtain a lead temperature in the vessel of $1400 \pm 18^\circ\text{F}$. Two of the five heaters malfunctioned during the test — one from an open circuit in the heater and the other from a failure of the power control unit. Figure VII-23 shows the temperatures of the assembly immediately prior to shutdown of the power for cooldown to room temperature.

The retort was opened and the heater assembly and dish removed to examine the top of the lead shield. The depressed center of the top of the shield was found to have bulged upwards, apparently until it was in contact with the heated dish as indicated by several depressions in the Croloy 2-1/4 steel which matched the size and location of the sheathed thermocouples on the bottom of the dish (Figure VII-24). The bulging was not found to be uniform, but in a four-leaf clover pattern.

Examination of the heater assembly revealed all the heaters to be intact. The open circuit was caused by the heater lead shorting against the stainless steel reflector plates.

The test setup was disassembled and dimensional measurements were made of the shield and the fixture. Figure VII-25 shows the dimensional changes after the first thermal cycle.

Several reasons were postulated for the distortion, but none were conclusive. It was decided to proceed to the second of the three planned cycles to obtain additional data.

Several minor modifications were made to the assembly:

- 1) Addition of thermocouples to the top of the shield vessel
- 2) The dish was raised to provide more clearance between the dish and the vessel

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Figure VII-24. Bottom of Heated Disk After First Thermal Cycle to 1400°F

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NOTES: UNLESS OTHERWISE SPECIFIED ON THIS DRAWING

1. ALL CHANGES SHOWN ARE CHANGES FROM THE INITIAL MEASUREMENT. THE UPPER NUMBER IS CHANGE FROM TEST ONE, THE LOWER FROM TEST TWO.

2. ☐ ARE THERMOCOUPLE NUMBERS. ☐ 7 THRU ☐ 5 ALSO HAD STRAIN GAGES WITH THE SAME NUMBER.

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Figure VII-25.
Dimensional Changes of the Lead
Shield After the First and Second
Thermal Cycle to 1400°F

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- 3) The threads on the top of the bolts tying the insulation can to the shield vessel were removed to minimize strain produced during the thermal cycle.
- 4) Insulation was added to the top and sides of the stainless steel heat reflectors to reduce the lateral and vertical heat loss from the heaters.

The test was reassembled with the shield and rotated 90° from the horizontal. The dish was heated to about 1100° to melt the lead, the heaters turned off, and the assembly cooled to freeze the lead with void located at a point on the periphery of the shield.

The heaters were turned on and the same heat-up rate was applied as in the first cycle. Figure VII-23 shows the temperatures at the completion of the heatup to 1400°F. Because of the added insulation on the stainless steel reflectors, it was necessary to raise the heated dish only to $1588 \pm 32^\circ\text{F}$ during this cycle.

The heaters were deactivated, the system allowed to cool to room temperature, and retort opened.

The lead shield was visually examined and the depressed head of the vessel was found to have bulged upwards even more than after the first thermal cycle (Figure VII-26). Dimensional measurements were again made (Figure VII-25). These distortions are being analyzed in an effort to explain the behavior, although no simple answer is attainable. The most reasonable answer is that the shield has a small amount of residual gas which was introduced accidentally during the final closure.

Since the lead gamma shield has been deemphasized in favor of a T - 10 wt % W gamma shield, only a minimal effort will be conducted to ascertain the reasons for the deformation.

b. LiH Checkout Shield

The fabrication of the Checkout Shield, shown schematically in Figure VII-27, and the casting hardware were completed and the assembly (Figure VII-28) was shipped to the new Building T042, Large Shield Casting Facility at the Nuclear Field Laboratory.

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Figure VII-26. Side View of Liquid Lead Shield After
Second Thermal Cycle to 1400°F

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Figure VII-27. LiH Neutron Shield Fabrication Development
(The Checkout Shield)

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Figure VII-28. View of Checkout Shield Removed from the Safety Container

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Figure VII-29 shows a flow diagram of the LiH loading and casting process. The shield casting hardware assembly was mated with the LiH Loading Dry Box (Figure VII-30) and the box and shield assembly purged with nitrogen until less than 3% oxygen was present. The 15- to 20-lb bags of LiH were removed from the shipping drums, weighed, and transferred into the antechamber of the dry box. The antechamber was purged to less than 3% oxygen, the inner door opened, the bags opened, and the LiH poured through the rubber sleeve connecting the dry box with the shield assembly (Figure VII-31).

About 1900 lb of LiH were loaded into the assembly, the dry box sealed with the inner floor plate, the rubber sleeve removed, and the top of the reservoir sealed with a gasketed plate held with "C" clamps.

The assembly was transferred to the retort base (Figure VII-32) and the thermocouples and heater leads connected. The retort was lowered over the assembly, the retort sealed, evacuated to about 10μ pressure, and backfilled with hydrogen to 1.6 psig pressure. The furnace was raised (Figure VII-33), rolled into place over the retort, and lowered onto the retort.

The power to the four temperature control zones of the furnace was turned on and the retort and contents heated to about 1450°F. The temperatures of the shield and casting hardware, the gas flow and pressure, and oxygen content of the hydrogen were monitored throughout the cycle. About 30 hr were required to melt the 1900-lb charge of LiH and raise the melt temperature to about 1450°F.

Cooling was accomplished by raising the furnace upwards stepwise and adjusting the furnace temperature downwards to permit the LiH to freeze inwards radially and axially upwards. The LiH in the reservoir and in the bellows-filler tube was kept hotter than the LiH in the shield by use of the auxiliary heaters on the bottom of the reservoir and around the filler tube. To freeze the LiH in the shield required 75 hr.

When the assembly had cooled to about room ambient, the hydrogen was evacuated from the retort, the retort filled with argon, and the retort unsealed and removed. Figure VII-34 shows the shield-casting hardware after the retort was removed. The cast shield was lifted from the safety container and photographed in the same positions as before the casting cycle (Figure VII-35). Visually the shield did not appear to have been distorted in any significant manner by the casting cycle.

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Figure VII-29. LiH Shield Filling and Casting Steps

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Figure VII-30. Shield-Casting Hardware Assembly Mated with the
LiH Loading Dry Box

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Figure VII-31. Transferring of Bags of LiH into the Dry Box

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Figure VII-32. Loaded Shield-Casting Hardware Assembly
Installed on Retort Base

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Figure VII-33. Furnace Raised in Preparation to
Rolling into Position Over the Retort

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Figure VII-34. Shield-Casting Hardware Assembly After the Casting Cycle

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Figure VII-35. Closeup View of the Top of the Checkout Shield After the Casting Cycle

The shield was prepared for radiography and 28 exposures were made covering the entire cross-sectional area of the shield. Evaluation of the radiographs revealed that the shield was the soundest ever produced, exhibiting less than average of the normally occurring small cracks and fissures between crystal masses.

The filler tube-bellows assembly was severed from the shield and a temporary closure of the filler tube stub made. Figure VII-36 shows the normal series of steps in a shield evaluation and examination, but, since this is not a prototype shield, the thermal cycle and vibration testing will not be conducted. A series of void volume measurements were made by evacuating the shield, then filling it to atmospheric pressure with helium from a tank of known volume and pressure, and calculating the per cent void using the Perfect Gas Law. A void volume of 5% was obtained whereas the average void volume for previous shields was about 6%. This measurement confirmed the visual appraisal from the radiographs.

The LiH Shield Examination Dry Box is currently being prepared to receive the Checkout Shield for the final, destructive examination wherein the stainless steel shell will be removed and the LiH exposed for further examination.

c. LiH Annular Shield

Current plans included the designing, fabricating, and testing of one or more of the family of LiH neutron shields associated with the Reference ZrH Reactor-Shield system (Figure VII-37). The shield selected for design and fabrication this year is the LiH Annular Shield which surrounds the sides of the reactor.

The design of the shield has been completed and the fabrication drawings are being prepared.

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Figure VII-36. LiH Shield Evaluation and Examination Steps

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Figure VII-37. Reference ZrH Reactor and Shield

VIII. FUEL DEVELOPMENT

This program has the primary objective of producing a fuel element which meets the design requirements of the reactor core test (and the Reference 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

Dimensional measurements were obtained on the initial group of 5 finned tubes fabricated by hobbing (a specialized milling technique). The major discrepancy was found to be variation in wall thickness. The variation ranged from 3 to 6 mils and thus did not meet the print allowance of 2 mils. This is believed to be primarily due to straightness and wall thickness variation in the 50-mil wall starting material. A second problem is the machining off of the fins at the ends of the tubes. Tubes machined to date have a step in the wall in making the transition from finned to non-finned areas. These steps varied from 0 to 4 mils. Some variation in surface finish in the hobbled area due to cutter chatter was also noted. To correct the wall thickness discrepancy, new support tooling for the hobbing machine was designed and installed to improve the tube stability and centerline retention during machining. A group of 12 tubes with a 25-mil wall and 20-mil-high fins were subsequently machined and are now being evaluated.

The tubing vendor completed the reduction of 27 draw shells (9 each from Incoloy 800 Heats 89339, 89630, and 89676) into approximately 1800 ft of 0.540-in. -ID by 50-mil wall tubing. This material will be used as starting material for experimental finned tubing.

Three 20-in. -long Incoloy 800 tubes were coated by the reverse drain technique using a coating target weight 30% less than standard. 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. This was done to investigate: (1) a possible reduction of the coating taper inherent with the standard coating process, and (2) the feasibility of obtaining a target coating thickness of 2 mils. A taper of 1 to 1-1/4 mils was obtained on the first coat and a composite 1/2 to 1 mil taper after the second coat (as determined by ID air gage traces. Coating thicknesses were measured on sections of the tubes and showed that the maximum occurred in the approximate axial center of the tubes, ranging from 1.9 to 2.0 mils. The end-to-end variation in thickness was 0.3 to 0.5 mil, with a circumferential variation of ± 0.3 mil. These data are encouraging but qualification of the process will require a sufficient number of tubes to establish reproducibility. In addition, elements must be fabricated from cladding processed in this manner to demonstrate that acceptable permeation rates can be achieved.

The coating thicknesses noted above were obtained destructively. The usual method of determining the SCB coating thickness (eddy current lift-off technique) has proven unreliable for Incoloy 800. Apparently, the electrical conductivity of the Incoloy substrate changes and to varying degrees during the coating firing cycles. These changes shift the zero reference point of the eddy current test. Experiments with a new instrument have shown some improvement, but wandering of the zero reference point remains a problem. An alternate method, beta backscatter, is being investigated. The basic test equipment is on hand, and a suitable beta source, ^{204}Tl , has been ordered. Feasibility will be checked on SCB coated flat specimens and/or cup plugs. A special probe will be required to make measurements inside coated tubes. Once feasibility has been demonstrated, a special ID probe may be obtained.

TABLE VIII-1
BURNABLE POISON TEST STATUS

Test No.	No. Specimens	Specimen Type*	Poison Film Composition (Absorber Atom Ratio 4Eu/1Sm) Oxides	Temperature (°F)	Test Time (hr)
F-1	1	V. C. 4-in. fuel rod	Y-Y/RE-Y [§]	1400	1150**
F-2	1	V. C. 4-in. fuel rod	Y-Y/RE-Y [§]	1400	1150**
F-3	1	V. C. 4-in. fuel rod	Y-Y/RE-Y [§]	1500	680**
F-4	1	V. C. 4-in. fuel rod	Y-Y/RE-Y [§]	1500	680**
1	4	2 V. C. fuel discs 3 sintered RE pellets	Y/RE Ti or Zr or Y/RE [†]	1460	1000
2	4	2 V. C. fuel discs 3 sintered RE pellets	Y/RE Ti or Zr or Y/RE [†]	1500	1013
3	37	16 V. C. fuel discs 21 sintered RE pellets	Y/RE and Y-Y/RE-Y Ti or Zr or Y/RE	1430	1968
4	37	15 V. C. fuel discs 21 sintered RE pellets	Y/RE and Y-Y/RE-Y	1500	1000
5	8	V. C. foil	RE, Y/RE, and Y/RE-Y	1430	1293**
6	8	V. C. foil	RE, Y/RE, and Y/RE-Y	1500	1000

*V. C. - vapor coated

†RE - rare earth oxide

§Sandwich type laminate

**Testing continues

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2. Fuel Processing

a. Burnable Poison

During the previous reporting period, three SNAP fuel vapor deposited burnable poison compatibility tests were initiated (1430, 1460, and 1500°F). Two of the tests were terminated late in the previous report period and the third was terminated early in the present report period. Seven more burnable poison compatibility tests were initiated during the present report period and two of these were removed from test at the end of the report period. Test conditions, special types, and times at temperature for all tests to date are presented in Table VIII-1. X-ray diffraction and fluorescence examinations were performed for selected compatibility test specimens from each of the five terminated tests. The diffraction and fluorescence data are proving difficult to analyze but qualitatively it appears that the higher the europium concentration in the film, the poorer the chemical stability of the film. This behavior was anticipated. It also appears that Y_2O_3 films 0.2 to 0.3 μ in average thickness provides protection of the hydrogen barrier and the fuel from interaction with Eu oxides. Thicker or better Y_2O_3 films may be required for long time protection.

Early in this quarter, the reference burnable poison was changed from \$7 (Eu + Sm) to \$12 (Gd + Eu). This change is advantageous from a materials standpoint since clad SNAP fuel rods with vapor deposited (Gd_2O_3) thin films on the fuel surface have demonstrated excellent compatibility/adherence for nominal periods of 3900 hr at 1500°F and 5000 hr at 1400°F. Good stability is expected for vapor deposited burnable poison oxide films with high Gd/Eu atom ratios. Vapor deposition of representative Gd-Eu film compositions began at a vendor facility in October. The equipment at the vendor facility has proved quite satisfactory for the simultaneous deposition of two or three refractory rare earth oxides on both fuel rods and fuel discs, and for the deposition of as many as four refractory dielectric oxides, to total thicknesses of 5 μ and greater in a single run. A proposed test matrix covering the expected vapor deposition parameters for the new reference composition is presented in Table VIII-2, and will be largely completed in the current series of vendor tests. Depending on the observed characteristics of the films, a second series of composition refinement tests may be necessary.

TABLE VIII-2
VAPOR DEPOSITED BURNABLE POISON TEST MATRIX

Test No.	Oxide Film Thickness (μ)					
	Gd	Eu	Sm	Y	Zr	Y*
1	0.76		-	-	-	-
2	0.5	1.0	-	-	-	-
3	0.5	1.0	-	1.0	-	-
4	0.5	1.0	-	-	1.0	-
5	0.76	0.4	-	-	-	0.4
6	0.5	1.0	-	-	-	0.4
7	0.76	0.4	-	-	0.4	-
8	0.76	0.4	-	-	0.4	0.4
9	0.5	1.0	-	-	1.0	0.4
10	0.5	1.0	-	1.0	-	0.4
11 [†]	-	0.6	0.2	-	0.8	0.4
12 [†]	-	0.6	0.2	0.8	-	0.4
13	-	0.6	-	-	0.6	-

*Yttrium thickness applied both at fuel surface and as cover coat; other oxides codeposited.

[†]Samarium included for correlation with prior work.

3. Fuel Element Assembly and Tests

a. Developmental Element Tests

During this report period, approximately 15 Incoloy 800 clad developmental elements were assembled. These elements incorporated various combinations of design factors which are part of the next generation fuel element; i. e., segmented fuel, helium backfill, vibration suppressor, and finned cladding. As the initial permeation test data began to accumulate, it became apparent that the leak rates were consistently higher than anticipated, averaging about 1.0 cc(STP)/hr. This was the case even with the standard S8DR configuration. Upon decanning, micro-cracks were found in the SCB coating immediately below the cup-plug closures. Generally, the extent of the cracking was in direct proportion to the

magnitude of the measured leak rates. A screening test, involving the various assembly procedures, was conducted to establish the cause of the cracking. The data obtained indicated that the SCB cracking was initiated by the thermal cycle of the shrink-fit operation, independent of the interference of the tube-plug fit-up, although the latter appeared to propagate the cracking. The cracking started in a random pattern in the coating in a band approximately 1/83 in. wide immediately below the zone stripped of coating for weld preparation. A typical interference fit of 3 mils caused some of the cracks to propagate in an axial direction along and beyond the cup-plug zone. Interferences of 1 mil or less appeared to eliminate such propagation. Tests were also conducted with reduced heating and cooling rates for both the shrink fit and blending cycle. With slower heating and cooling cycles, no cracking was observed.

Subsequently, three elements were assembled with helium backfill, single-piece fuel, interference fit-ups limited to 1 mil and the slower heating and cooling rates. The elements were permeation tested, subjected to the "A ramp" thermal inputs, and then permeation tested again. The results were quite positive, and are summarized as follows:

Element	As Measured Leak Rate [cc(STP)/hr at 1400°F]	
	As-Built	Post "A" Ramps
9510	0.24	0.23
9511	0.26	0.38
9513	0.20	0.28

The elements will now be decanned to verify the elimination of the cracking as inferred from the leak rates

The seven 15-mil Incoloy 800 clad developmental elements (4 double-blind-end and 3 cup-plug type) continued on test and have now accrued slightly over 15,000 hr at 1400°F, with an average hydrogen loss of 6.9%. The 4 double-blind-end elements have lost an average of 6.6% and the 3 cup-plug type an average of 7.3%. Curves of the average normalized leak rate and cumulative hydrogen loss are shown in Figures VIII-1 and -2, respectively.

Figure VIII-1. Average Normalized Leak Rate of the 7 Incoloy 800 Clad Developmental Fuel Elements

Figure VIII-2. Cumulative Average Hydrogen Loss for the 7 Incoloy 800 Clad Developmental Fuel Elements

The 4 double-blind-end Incoloy 800 clad elements containing enriched uranium rods have now accrued 14,500 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 7 normal uranium elements is primarily due to one high leaking element. Excluding this element, the remaining 3 have an average normalized leak rate of 0.55 cc(STP)/hr and an average cumulative hydrogen loss of 6.7%

b. Special Tests

The allowable stress in the hydrogen barrier is an important criterion affecting the design of the Test Reactor and Advance ZrH Reactor. Special tests were conducted to develop data from which residual fabrication stresses in the SCB enamel-cladding composite could be calculated. Additional special tests were 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) Uniaxial Barrier Strain Tests

Eleven SCB coated 15-mil Incoloy 800 clad fuel elements were tested to determine the short-time stress/strain capability of the SCB hydrogen barrier. The tests were conducted in a permeation test system which had been modified for vacuum feed-throughs and allowed axial loading of the element externally. Loads were applied incrementally until a definite increase in hydrogen permeation rate was measured at which time the test was terminated. To avoid any creep effects, the loads were allowed to remain on the element for ~10 sec only at each load level. The load would then be removed and the permeation rate monitored to determine if the SCB barrier had been damaged at the preceding load level.

Three elements were tested at each of 3 temperatures, 1200, 1300, and 1400°F. One element was tested at 1100°F and 1 additional element was tested at 1400°F after thermal aging at 1400°F for 100 hr.

Absolute strain measurements were obtained from gage marks which had been lightly scribed along the length of the elements at 1-in. intervals prior to the test. After testing, the elements were disassembled, the SCB removed

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Figure VIII-3. Average Normalized Leak Rate of the 4 Enriched Uranium Double-Blind-End Incoloy 800 Clad Developmental Elements

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

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from the cladding and tube wall thickness measurements taken for use in stress calculations. The values for average wall thickness, initial and final leak rates, temperatures, and strains are summarized in Table VIII-3. The peak temperatures shown in this table are the measured peak temperatures of the elements during the test. The peak temperatures occurred near the axial center of the elements with the center 8 in. of the elements being within 25°F of this value.

The data from these tests are plotted versus temperature in Figure VIII-5. Included in the figure is the 0.2% yield strength of SCB coated tubes as determined from uniaxial tensile tests on tubes from the same heat of Incoloy 800 (HH7531A). This close correspondence between yield strength and cladding stress at barrier failure had been observed previously on Hastelloy N.

(2) Split Ring Test

A standard split ring test was selected to obtain data from which stress-strain in the SCB/cladding composite could be calculated at ambient and elevated temperatures. The test specimens consisted of rings cut from cladding which was coated by the current process. (Rings from 10-mil Hastelloy N and 15- to 30-mil Incoloy 800 cladding were used.) The rings were split at 1 circumferential location with a saw. The saw cut width (gap) was established and changes in gap width were then measured as a function of different conditions.

Strain is induced in the SCB-cladding composite as a result of the mismatch in thermal expansion coefficients of the two materials. Data on the strain in the metal was obtained by stripping the SCB off a ring, splitting the ring, and measuring the change in saw cut gap (springback). Data on the strain in the composite was obtained by splitting a coated ring, measuring the gap, stripping the SCB, and then remeasuring the gap. A third test was conducted to obtain data on the effect of temperature. Rings were split and then cycled to the SCB softening temperature and back to room temperature. Gap width was measured over the entire range. The gap change data were analytically reduced to determine SCB thermal expansion coefficient, strain in the composite at room temperature, and composite strain as a function of temperature (see Section IV-C).

TABLE VIII-3
SUMMARY OF INCOLOY 800 UNIAXIAL FUEL ELEMENT STRAIN TESTS

Test No.	Element No.	Peak Temperature (°F)	Average Wall Thickness (mils)	Tensile Stress at Barrier Failure (psi)	Initial Leak Rate (cc/hr)	Final Leak Rate (cc/hr)	Maximum Residual Axial Strain (%)	Average Residual Axial Strain (%)
7	9479	1400	15.2	15,550	1.67	7.74	0.5	0.17
8	9477	1384	15.2	15,650	1.77	2.50	0.3	0.08
9	9506	1380	14.9	16,600	0.83	1.40	0.3	0.09
10	9485	1281	14.7	16,850	0.14	0.36	0.2	0.09
11	9481	1300	14.7	16,100	0.20	0.80	0.3	0.15
12	9488	1300	14.9	17,350	0.22	1.58	0.2	0.07
13	9482	1200	15.1	19,450	0.09	0.43	0.3	0.12
14	9484	1197	15.0	18,750	0.17	0.48	0.3	0.11
15	9494	1181	14.9	18,950	0.49	3.10	1.0	0.27
16	9495	1409	14.4	17,200	1.20	41.9	0.8	0.32
17	9500	1125	14.9	20,450	0.13	0.46	1.0	0.38

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Figure VIII-5. Tensile Stress at Barrier Failure vs Temperature

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Additional data obtained directly from this test include the SCB enamel softening temperature or upper no-strain point and the lower no-strain temperature. The latter is that temperature at which the stresses the enamel and metal are equal. It therefore varies as a function of the SCB enamel thickness and with the differential thermal expansion of the composite. The data obtained on these 2 zero strain points are shown in Table VIII-4 for SCB coated Hastelloy N and Incoloy 800.

(3) Thermal Cycle Tests

As an adjunct to the split ring tests, a number of 4-in. -long coated tube sections were subjected to thermal cycles to gain data with respect to dimensional stability in a tube configuration. Initial tests involved a 10-mil Hastelloy N tube and a 15-mil Incoloy 800 tube, both coated; and an uncoated 15-mil Incoloy 800 specimen. The test consisted of 20 thermal cycles from room temperature to 1400°F, 5 min at temperature followed by an air quench. Profilometer data were taken after every 5 cycles. The Hastelloy N and uncoated Incoloy specimens were essentially constant in average diameter and ovality. The coated Incoloy 800 tube exhibited a significant increase in ovality and a slight increase in average diameter. Several heavier wall, 23- to 45-mil, Incoloy specimens were then given the same test. The data indicate improved dimensional stability, as expected. No measurable change in diameter occurred and ovality changes were minimal.

TABLE VIII-4
UPPER AND LOWER NO-STRAIN TEMPERATURES FOR
SCB COATED HASTELLOY N AND INCOLOY 800

SCB Coating	SCB Thickness (mils)	No Strain Temperature	
		Lower (°F)	Upper* (°F)
Hastelloy N	1.6-1.8	1330	1460
	2.3-2.4	1270	1470
	2.6	1250	1510
Incoloy 800	1.2-1.5	1170	1475
	2.3-2.5	1065	1450
	2.8-2.9	920	1470

*Upper no strain temperature is the softening point of the enamel

4. Cladding Properties

The Incoloy 800 program is being carried out to evaluate the potential of this material as fuel cladding for ZrH reactors. This is being accomplished by measuring strength and ductility characteristics in simulated reactor environments. The program encompasses: stress-rupture tests in helium of specimens irradiated at different temperatures, in-pile stress-rupture tests both in He and NaK, out-of-pile stress-rupture tests of controls in both He and NaK (flowing and static), and tensile tests of unirradiated specimens. Stress rupture test results of the 1300, 1400, 1200, and 1100°F stress-rupture irradiation experiments (NAR 83-1, -2, -3, and -4, respectively) are summarized in Figures VIII-6, -7, and -8. (Note the shift in abscissas when comparing Figures VIII-6 and -8.)

As reported in the preceding quarterly report,* a significant effect on the strength and ductility of Incoloy 800 samples exposed to flowing NaK at 1300 and 1400°F has been observed. Also, it was reported that the presence of sodium-chromite ($\text{Na}_2\text{O}-\text{Cr}_2\text{O}_3$) as a thin (~ 0.3 mil) layer at the OD surface and in the grain boundaries to a depth of ~ 0.4 mil may have, in part, accounted for this "NaK effect." In each case, a thin metal layer, ~ 0.2 mil thick, was also found to have been deposited on the surface of the sodium-chromite. An example of the typical deposits found is shown in Figure VIII-9. During this report period, a short-term test (~ 100 hr) was performed in which about the same amount of sodium-chromite was formed as occurred in the 300- and 1500-hr tests. The thin metal layer also formed on these samples. Microprobe analysis of the surface of these specimens indicates the metal layer to have a composition very close to the composition of the 316 SS loop material except that its chromium content was low. This is not surprising since these loops have been operated well over 10,000 hr at varying oxygen contents. The thickness of both layers, sodium-chromite and the thin metal deposit, was the same in the 100-hr samples as on the 300- and 1500-hr samples. Thus, the sodium-chromite must have formed early in the test. Also, the oxygen level may have been higher during loop startup than measured in the first NaK-oxygen sample taken after 50 to 100 hr of loop operation.

*SNAP Reactor Programs Progress Report, May-July 1970, AI-AEC-12975, September 15, 1970

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Figure VIII-6. Biaxial Stress Rupture of SCB Coated Incoloy 800 at 1800°F — Irradiated and Aged Specimens

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Figure VIII-7. Ductility of SCB Coated Incoloy 800 Obtained from Biaxially Stressed Tubes at 1300°F

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Figure VIII-8. Biaxial Stress Rupture of SCB Coated Incoloy 800 Irradiated and Aged Specimens

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Figure VIII-9. OD of Incoloy 800 Biaxial Stress Rupture
Samples Exposed for 1500 hr to 1300°F NaK
in an Isothermal Loop
(Lightly Etched, Electrolytically, in 10% Oxide Solution)

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Figure VIII-10. Biaxial Stress Rupture of SCB Coated Incoloy 800 in 1300°F Flowing and Static NaK

A new loop is being employed in a 1300°F stress rupture test. This loop was equipped with a larger cold finger and was operated for 100 hr at 600°F prior to heating to the test temperature to avoid the probable initial oxygen excursion mentioned above. The oxygen level of the NaK was measured to be < 15 ppm after this low temperature operation. A significant improvement in the strength properties measured in this test is indicated in Figure VIII-10. However, an offset from the control samples tested in helium is still evident.

A similar loss in strength and ductility has also been observed in Incoloy 800 tubes filled with static sodium and tested uniaxially.* An attempt was made to explain this observation on the basis of a surface tension or energy related phenomenon.* To explore this possibility, a static NaK retort stress rupture test was initiated. This test consists of a 2-in. -ID 316 SS retort in which 8 stress rupture specimens are immersed in 1300°F NaK. A NaK sampling line is provided for routine oxygen analysis. During the first 1000 hr of testing the O₂ concentration has been measured to be <10 ppm. The stress rupture data obtained in this test is also shown in Figure VIII-10. It is not possible at this point to evaluate the relative effects of static and flowing NaK.

The unirradiated short-term tensile properties of SCB coated and simulated processed tubing for Incoloy 800 heats HH7531A and 810535 are shown in Figures VIII-11, -12, and -13. No significant difference can be seen between the two heats.

*H. Bohm and H. Schneider, J. Nucl. Matls. 24 (1967) p 188

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Figure VIII-11. Tensile Properties for Simulated Processed Incoloy 800 Tubing

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Figure VIII-12. Tensile Properties for Coated 15-mil Incoloy 800 Tubing

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Figure VIII-13. Short-Term Elongation

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IX. AEROSPACE SAFETY

The primary effort under the Aerospace Safety Program is to perform 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 5 years.

The reference reactor system chosen for this evaluation is a relatively low-powered, undermoderated 85 element ZrH core with sliding beryllium control segments to adjust the neutron leakage (see Figure IX-1). The power conversion system is comprised of thermoelectric units utilizing liquid NaK as the heat transfer medium.

The same core was used for all cases studied. The reactivity and heat rejection requirements required for the various initial power levels were met by adjusting the radiator area and control segment thickness. Two reference unmanned missions, designated Mission A and Mission B, were selected for evaluation. The system parameters associated with each mission are shown in Table IX-1 below.

TABLE IX-1
SYSTEM PARAMETERS FOR MISSIONS A AND B

Mission Parameters	Mission A	Mission B
Spacecraft size (L x D) (ft)	60 x 4	10 x 4
Spacecraft weight (lb)	7000	1225
Orbit altitude (n. m.)	225	800
Orbital inclination (degrees)	polar	polar
Mission lifetime (years)	2	5

Table IX-2 shows additional parameters which were varied in the study to determine the effects on both their individual interactions and the total effect on the overall risk associated with the mission.

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LOW POWER REACTOR

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Figure IX-1. Unmanned Reactor

TABLE IX-2

UNMANNED MISSION – PARAMETERS FOR EVALUATION

A. Reactor Thermal Power

1. 100 kw
2. 50 kw
3. 15 kw

B. Post-Operational Disposal Mode

1. Random reentry
2. Deboost into deep ocean disposal site
3. Orbital boost to enhance orbital decay time

C. Tritium Loadings as an Inherent Shutdown Mechanism

1. \$5
2. \$11
3. \$17

D. Orbital Decay Time after End-of-Life

1. Short-lived orbits <10 yr
 2. Long-lived orbits >10³ yr
-

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A brief summary of the current status of the study to date is presented below:

A. ORBITAL DECAY ANALYSIS

An orbital decay analysis was performed to determine the orbital lifetime associated with various configurations of the spacecrafts and reactor system as a function of orbital altitude. Gravity gradient ballistic coefficients have been determined for the various system configurations with and without the spacecraft for altitudes above 100 n.m., the free-molecular flow regime. The ballistic coefficients determined range between 13 and 18 lb/ft², which are directly related to orbit lifetime. The effect of sunspot activity on atmosphere density and hence orbit lifetime as a function altitude was also included.

Figure IX-2 shows the decay time as a function of orbital altitude for a ballistic coefficient of 18 lb/ft² (representative of a gravity gradient orientation of the spacecraft). The multi-valued portion of the curve at lower altitudes reflects the effect of the extremes in sunspot activity on orbital decay time. The upper curve represents minimum solar activity and hence a longer time prior to reentry from a given altitude. The maximum and minimum solar activity years in the 11-year-solar cycle are represented by 1980 and 1974.

B. REENTRY ANALYSIS

In order to study possible ablation of the reactor-shield assembly it was necessary to establish a realistic heat pulse by determining ballistic coefficients in the continuum flow regime. The maximum ballistic coefficient which also corresponds to the maximum heat pulse for nose-on reentry, is 390 lb/ft² for the reactor shield assembly.

Impact velocities were determined for all 4 systems (missions A and B, with and without the spacecraft) in nose-first, tail-first, and tumbling attitudes. The nose-first or tail-first impact velocities range from 550 to 670 ft/sec while the tumbling impact velocities range from 350 to 440 ft/sec.

Ablation studies were performed to determine the condition of the reactor, reflector assembly, and LiH shield. For the unprotected reactor, about 0.5 in. of fuel ablate for nose-on reentry. The core vessel lid and part of the grid plate will melt for the tumbling case but no fuel will ablate. Reflector assembly

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Figure IX-2. Natural Decay From Orbit Altitude

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ablation will be less than 0.1 in. while LiH shield ablation will range from 0.7 in. for the tumbling case to 2.6 in. for the unlikely aft-first stable case.

The reactor was studied with and without a protective cover over the core vessel dome. For low altitude missions where the orbital decay time is short, such as Mission A, it may be necessary to cover the core vessel dome to prevent the release of fission products to the atmosphere. High altitude missions, such as Mission B, where the orbital decay times are very long, allow the fission products to decay to levels where their release does not represent a hazard. A protective cover consisting of a 1.6-in. -thick inner layer of reinforced pyrolytic graphite and an 0.25 to 0.50-in. -thick outer layer of ATJ graphite is capable of keeping the core vessel lid below 2200°F.

C. IMPACT ANALYSIS

An impact analysis of the unmanned reactor power system into various earth media was conducted to determine the post earth-impact configuration of the reactor. The analytical methods used to predict the post-impact configurations were based on the SNAP 10A impact tests and analysis, recent impact tests conducted with reactor component materials and geometries, and the energy balance analytical approach in which the energy absorption capabilities of the reactor and target are compared to the impact kinetic energy. The depth of penetration of the reactor system into various earth media was also predicted.

The most likely reentry condition is the tumbling reentry at an impact velocity of 440 ft/sec. To determine the worst case post-land impact configuration for the tumbling reentry condition, energy balances were done for a reactor-first, side-on, and LiH shield-first tumbling impact on a granite target. The results of the analysis indicate that for a tumbling shield-on or side-on impact the reflectors and the LiH shield would be shattered and disassembled from the core vessel, and the core vessel would be deformed but in all probability the core vessel would remain "intact," i. e., in a condition such that fuel elements are not readily available for handling. For a reactor-first tumbling impact, the core vessel may or may not remain intact depending on the detail design of the reactor system.

The energy method assumes the impacting specimen is a monolithic structure and as it varies from this, the confidence in this method for determining the post-impact configuration decreases. The energy absorption capacity of the reactor system for the tumbling side-on and shield-on impact is considerably greater than the impact kinetic energy. Therefore, even though the reactor system does not behave as a strictly monolithic structure, there is enough margin to expect that the core vessel will remain intact. For the tumbling reactor-first impact, however, the energy absorbing capacity is only slightly greater than the impact kinetic energy so whether the core remains intact or disassembles depends on the detail reactor design.

Stable reactor-first impact at 670 ft/sec, stable shield-first impact at 615 ft/sec, and stable side-first impact at 355 ft/sec were also investigated even though the likelihood of these cases ever occurring is remote. For the stable shield-first and side-first impacts, the reflectors and the LiH shield would be shattered and disassembled from the core; but the core vessel would, in all probability, remain intact. For the stable reactor-first reentry, the reactor system would be completely disassembled and components scattered over the impact area if the impact occurred on a media that was more dense than sand.

The similarity of the subject reactor and the SNAP 10A reactor in conjunction with the SNAP 10A water impact test results were used to predict the water impact post-impact configuration. The reflectors and the LiH shield would be shattered and disassembled from the core vessel and the core vessel would be deformed but intact for all of the anticipated reentry conditions and impact attitudes.

The calculated depth of penetrations of the reactor into various earth media for the tumbling condition varies from 2.9 in. for a side-on impact into sandstone to 135 in. for a reactor or shield-first impact into soft soil.

D. POWER HISTORIES

The power histories of the reactor system for the 100-, 50-, and 15-kwt power levels has been determined. These power histories were also determined for tritium loadings of \$5, \$11, and \$17 at these power levels. Figure IX-3 presents these histories for the normal situation of reactor shutdown at the end

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Figure IX-3. Reference Reactor Power Histories

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Figure IX-4. Gross Fission Product Inventories for 50 kwt Operation

of mission life and also for no shutdown. The difference in the power decay slopes for the various initial power level after no shutdown reflects the difference in heat rejection capabilities of the radiator due to the difference in size for the same initial core loading. Figure IX-4 shows the radioactive materials inventory corresponding to the various power histories at 50 kw level for both the normal shutdown situation and accident situations where no shutdown results.

E. RADIOLOGICAL EXPOSURE MODES

Once the reactor system starts reentry into the earth's atmosphere, the potential for interactions between man and any residual radioactive inventory in the system becomes an active rather than a passive situation. Figure IX-5 identifies a number of potential radiological exposure modes resulting from reentry of the system. Analytical models were developed to evaluate both the magnitude and number of people which could be affected on a probabilistic basis for the potential exposure modes as a function of the radioactive materials inventory. Of the exposure modes, only the following, from both the probabilistic viewpoint and potential numbers of people involved, have been determined to represent an exposure of significance: (1) "intact" core, (2) fuel element, (3) criticality exposure, and (4) potable water contamination. The analytical exposure models are used to determine the conditional probabilities of exposing various numbers of people at or above a guideline value. These conditional probabilities, when combined with the sequence of event probabilities leading to these potential exposure modes, yields the total risk to a particular mission.

F. SHUTDOWN RELIABILITY ANALYSIS

Shutdown control reliability for the reactor system in the unmanned station has been determined for the proposed control system without redundancy. To improve shutdown reliability, the use of control circuit redundancy in active parallel or standby redundancy was considered assuming the use of isolation and switching logic circuitry for the redundant assemblies. Reliability estimates with and without redundancy are shown in Table IX-3.

The estimates shown in Table IX-3 are based on the use of discrete solid state components. The use of integrated circuits for portions of the control system would be expected to improve reliably.

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Figure IX-5. Event Sequences Leading to Potential Radiological Exposures

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TABLE IX-3
REACTOR SHUTDOWN RELIABILITY

Mission Time	No Redundancy		Circuit Redundancy	
	2 yr	5 yr	2 yr	5 yr
Command Telemetry	0.9_6^+	9.9_6^+	9.9_6^+	9.9_6^+
Shutdown Signal	0.9_3	0.9_3	0.9_3^8	0.9_3^5
Pulse Generator	0.9_{11}	0.792	0.9_5^{3*}	0.9_3^{6*}
Control Logic	0.998	0.995	$0.9_5^{6\dagger}$	$0.9_4^{8\dagger}$
Actuator Selection and Sequence	0.982	0.957	$0.9_5^{5§}$	$0.9_4^{2§}$
Actuator (2 of 4)	0.9_5^{00}	0.9_3^8	0.9_5^0	0.9_3^8
Shutdown Reliability	0.893	0.754	0.9_3^8	0.999

*5 units in active parallel redundancy

†2 units in active parallel redundancy

§3 units in active parallel redundancy

$$(0.9_6^+ = 0.999999^+)$$

G. DISPOSAL SYSTEM

The preliminary design of a propulsion system for utilization as a system disposal package was completed that utilizes a solid fuel main propulsion system with a bi-propellant reaction control system. The disposal package weighs 400 to 500 pounds depending on its particular configuration. The use of the disposal package results in a 27 to 35% weight increase for the power system. The disposal package would provide a velocity increment of 750 ft/sec for either a boost or deboost disposal. This velocity increment is adequate to perform either mode of disposal thereby adding increased flexibility to the mission by allowing a choice to be made at the time of disposal.

June 30, 1971

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TO
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In Table II-2, the seventh column heading should read:

$\% \text{Kr}^{85} \text{ Released} \times 10^{-3}$

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