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SNAP REACTOR IMPROVEMENT PROGRAM
JANUARY - MARCH 1966
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SNAP REACTOR IMPROVEMENT PROGRAM
JANUARY - MARCH 1966
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L. M. MAKI

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I. PROGRAM OBJECTIVES

The primary objective of the SNAP Reactor Improvement Program is advancement of the performance capabilities of uranium-zirconium hydride reactors. Improved reactor performance may provide either higher powers and longer lifetimes or may be utilized to provide greater design margins and enhanced reliability at present reactor operating conditions. Analytical and developmental studies are in progress to evaluate methods of improving technology areas that most affect reactor performance. These studies will define developmental areas necessary to achieve these improvements.

In order to meet the overall objectives of the SNAP Reactor Improvement Program, analytical tasks are established to determine the reactor performance limits within the scope of existing technology. The results of these analyses will guide the fuel and component development tasks in the program. Data from the developmental program will provide continual improvement in the knowledge of reactor performance.

Engineering analyses are in progress to determine the limits of performance of the present SNAP reactors and the gains to be achieved by changes in configuration or extension of the limits of technology. Key problem areas limiting the performance of the present reactors are being identified and potential increases in performance will be evaluated in terms of increased reliability, increased lifetime, and reduced system weight. The results of these analytical studies will be used to guide the development effort carried on in the program.

The fuel development program is directed toward the optimization of the present ceramic barrier nonrefractory metal clad fuel element design. Improved barriers, closures, and clad materials are under study, in order to improve fuel element performance. Indications are that an operating life in excess of 20,000 hours at SNAP 8 conditions is attainable.

Irradiation experiments are being prepared to determine the behavior of the fuel and cladding materials.

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The component development effort is directed toward those items which will most improve SNAP reactor performance. The development of a high-torque control drum actuator is being initiated. Studies of advanced electronic logic circuits and high-temperature bearings are also underway.

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II. PROGRESS SUMMARY

A. REACTOR ENGINEERING

A parametric survey was made of the effect of various design parameters on SNAP reactor lifetime. Parameters considered were fuel swelling rate, hydrogen gas gap size, hydrogen retention capability, and cladding ductility. It was found that the design lifetime was very sensitive to the capability of the fuel to retain hydrogen.

A study was performed to evaluate the ultimate lifetime capabilities of SNAP reactors. The normal design lifetime limit, for example, is based on no interference between the fuel and cladding due to swelling. These studies evaluated the effects of operation beyond the very conservative design limits. In general, it was found that the ultimate reactor lifetime greatly exceeds the design values used in parametric studies.

A summary of all available fuel irradiation growth data was compiled. The initial attempts at obtaining an overall correlation appear to indicate that there is virtually no temperature dependence associated with fuel swelling and that the data fall into two families.

A study was initiated to reevaluate the hydrogen redistribution effects in SNAP reactor cores. This study showed that the diffusion constant for hydrogen redistribution should be a function of the hydrogen content of the fuel (H/Zr). Using this revised diffusion coefficient, good agreement was obtained in the calculation of the SNAP 10A and S8ER hydrogen redistribution transients.

B. FUEL DEVELOPMENT

The hot cell examination of the NAA-82-1, SNAP 2 fuel element irradiation experiment, continued during the report period. Metallographic examination of the cladding showed no evidence of damage and less intergranular precipitation than was observed in the S8ER cladding. The average hydrogen loss was 4%, with a maximum of 7.7%. This hydrogen loss was insufficient to cause any $\beta + \delta$ phase transformations.

The fabrication of the NAA-120 fuel material irradiation experiment is continuing. The method of assembly of the fuel specimens was changed to eliminate the shrink-fit design, which potentially could have caused problems during rapid reactor transients.

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A modified SNAP fuel alloy developed by Denver Research Institute was evaluated to confirm the reportedly lower hydrogen dissociation pressures. These reported results were not confirmed by dissociation pressure and hydriding studies performed at AI.

Of the three alternate hydrogen barrier materials selected for study during the fiscal year, only SCB-1 + 10% Al_2O_3 was found to have promise and all development work on the others was terminated. This hydrogen barrier material exhibits good hydrogen retention properties and greater resistance to devitrification than SCB-1.

Coating studies were performed on alternate cladding materials. Included in these studies were Hastelloy N, Hastelloy X, 304 Stainless Steel and 316 Stainless Steel. Hastelloy N requires a chromizing process in order to achieve good adherence of the hydrogen barrier. Hastelloy X was found to provide excellent adherence with no chromizing or pre-oxidation treatment. The stainless steels had poor adherence with SCB-1, which is fired at 2150°F, but exhibited excellent adherence with the AI-D barrier, which is fired at 1900°F.

C. COMPONENT DEVELOPMENT

The design of a prototype high-temperature bearing was completed. This bearing will use a solid carbon-graphite ball in a Ta-10W housing. A versatile bearing test fixture is being designed which will be capable of testing the bearing over a wide range of temperatures and loads.

Envelope studies were performed to determine the allowable control-drum actuator size for advanced SNAP 8 reflector configurations. These allowable envelopes were compared with the results of parametric torque vs size studies performed for various actuator concepts. These studies provided an allowable design range for the parametric actuator and brake design studies which are in progress.

A test rig was designed and fabricated to study the effects on output performance of changes in actuator tooth and slot geometries.

An improved coil encapsulant was developed which greatly reduces the attack on the conductor wires. Testing to date indicates that the encapsulant has long-term electrical resistance properties equal to those of the present commercial encapsulants.

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III. REACTOR ENGINEERING

A. THERMAL ANALYSIS

1. Parametric Survey of Design Lifetime Variations

The effect of various design parameters on the lifetime of SNAP reactors has been evaluated. On the basis of preliminary studies, the four most significant parameters were selected for detailed investigation. These parameters were:

- 1) Ceramic defect constant, which governs hydrogen permeation rate,
- 2) Fuel swelling rate,
- 3) Cladding percent strain to failure,
- 4) Radial gas gap width.

The S8DS reactor (600 kw, 1100°F inlet, 1300°F outlet), with a nominal lifetime of 36 months, was selected as the reference case. Other reactors operating in the same power and temperature range will exhibit similar variations, although the nominal lifetimes may differ from that of S8DS.

Variations in lifetime due to the ceramic defect constant K_{BLD} are shown in Figure 1. Experimental observations of hydrogen leak rates have shown that the rate during operation can be greater than that observed during acceptance testing. The acceptance level K_{BLD} is 1.6 and the nominal K_{BLD} is set at 4.8 for reactor performance predictions. Loss of hydrogen results in lowering of the fuel N_H and eventually a fuel structural change from δ to β phase which may be accompanied by excessive swelling. Therefore, the first appearance of β -phase fuel within the reactor core is considered a design limitation. Figure 1 illustrates the strong dependence of the time of phase change on the defect constant. For values of K_{BLD} less than 5.6, the nominal fuel swelling limit is encountered prior to the phase-change limit. Fuel swelling is considered independent of N_H (other than during the δ - β phase change) and, thus, of K_{BLD} .

Another important parameter which affects lifetime is the fuel swelling rate. Fuel irradiation swelling is nominally assumed to obey the Miller correlation:

$$\frac{\Delta V}{V_{\text{initial}}} (\%) = (1.2 \times 10^6) b^2 e^{-22600/T_{\text{max}}} + 1.0 b \quad ,$$

Figure 1. S8DS Lifetime vs Defect Constant, K_{BLD}

Figure 2. S8DS Lifetime vs Fuel Swelling Rate

Figure 3. S8DS Lifetime vs Cladding Strain to Failure

Figure 4. S8DS Lifetime vs Radial Gas Gap Width

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where the burnup b and the averaged peak fuel temperature $T_{\max}$ are in units of met. at. % and °R, respectively. Lifetime variations with fuel swelling, normalized to the Miller correlation rate, are shown in Figure 2. The swelling limit is designated as that time at which the fuel swells to just touch the cladding.

This definition of the nominal fuel swelling limit implies that the cladding cannot undergo any strain without failure. Some preliminary data are available on the percent strain to failure of aged and irradiated SNAP cladding. Out-of-pile creep-rupture and short-term tensile tests indicate 0.5 to 2.0% strain to failure. Preliminary analysis of S8ER elements indicates somewhat lower strains to failure. The additional lifetime provided by allowable cladding strain is shown in Figure 3.

The effect of the initial gap width on S8DS lifetime is shown in Figure 4. An increase in the time to reach the fuel swelling limit can be achieved by increasing the radial gap width from the nominal 3.2 mils. However, this gain may be offset by the phase-change limit. As the defect constant K_{BLD} increases, the optimum gap width decreases. A reduction in the initial gap size for the S8DS design would result in increased capability, for leak rates corresponding to K_{BLD} greater than 7. For the expected leak rate of approximately 3 times the acceptance level, the present S8DS gap size is near-optimum. It should be noted that this calculation did not consider fission product gas accumulation within the gap.

2. Analytical Methods

The thermal model of the LOAFER code, which calculates long-term reactor performance, has been revised to consider separately radial power and flow profiles and to include the effect of fission gas accumulation in a variable gas gap conductivity term. These revisions result in a more realistic calculation of fuel swelling and hydrogen loss and permit an evaluation of the effect of flow shaping on long-term performance and reliability.

The original LOAFER model assumed that all coolant channels had the same temperature rise. This is equivalent to assuming matched radial power and flow profiles. The revised model considers the radial profiles as separate terms in the calculation, thus increasing the generality and utility of the code.

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A second serious deficiency of the original model was that variations of gap conductivity, with changes in gas composition due to fuel burnup, were not included. Three or more gases may be present in the gap between the fuel and cladding: (1) hydrogen released from the uranium-zirconium hydride fuel; (2) fission gases, largely xenon, produced within the fuel and released to the gap; and (3) residual gases associated with the closure weld on the element. (For example, helium is present in S8ER elements because of the final TIG weld. S8DS elements have no residual gas because their closures are electron-beam welded in a vacuum.) The gap conductivity is a function of the relative proportions of these gases and will change during the operational life of the reactor.

As an example of the importance of fission gas buildup, consider S8ER after 10^4 hr of operation at 450 kw. The mole percentages of the various gases in the gap of an average element at 1300°F are:

Hydrogen	44%
Helium	28%
Fission gases	28%.

The overall heat transfer coefficient (UA) associated with this gas composition at 10^4 hr is 4.85 kw/°F. If only helium and fission gases exist in the gap (a "worst case" corresponding to essentially complete hydrogen loss or β -phase fuel), UA would be 4.18 kw/°F. For comparison, UA at the beginning of life, when fission gases are negligible, is 5.7 kw/°F.

Although these changes have been incorporated into the LOAFER thermal model, no studies have been made using the revised code. It is anticipated that the first use of the revised code will be an evaluation of lifetime gains associated with flow shaping.

3. Ultimate Lifetime Limitations

A study has been initiated to investigate the various failure modes of SNAP reactor cores and thus to improve estimates of the reliability attainable under various operating conditions. The design limit criteria currently employed are extremely conservative and place excessively severe limits on the lifetime reliability of SNAP reactors. There is a need for more realistic definitions of

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reactor failure modes consistent with the fact that the ultimate limitation is always loss of reactivity sufficient to drop power and/or temperature below a minimum acceptable level.

The current limit analysis assumes that the entire core has reached its design lifetime prior to the time when one fuel element cracks or ruptures. In reality, an element's hydrogen leak rate increases 40- to 50-fold when the element ruptures and the reactor reactivity loss rate is accelerated. The time of ultimate core failure may be many months after the initial rupture.

The hydrogen loss rate from an element, at a given temperature and pressure, is determined by the element's defect constant. This factor is not constant but, rather, proportional to the permeability of the hydrogen barrier. The defect constant of an individual element would be expected to increase slowly during reactor operation due to natural degradation and aging effects. Sudden increases might occur as the result of thermal or mechanical shocks. The defect constant would greatly increase upon rupture of the element.

Analysis of barrier permeation and hydrogen loss appears feasible if a meaningful value can be assigned to the average defect constant of a ruptured element. This value would be assumed as maximum for all elements. With the initial defect constant specified by the acceptance level and a maximum defect constant fixed, probability models could be generated for the various loss-inducing mechanisms.

A maximum defect constant for the AI-8763D ceramic can be estimated from available S8ER data. At the beginning of life, the defect constant was somewhat lower than the acceptance level of $1.6 \text{ cc(STP)/hr-cm}^2\text{-atm}^{1/2}$. After 5×10^6 kwh of operation, the core average defect constant had increased to 30 or 40 times the acceptance level. Following shutdown, 167 of the 211 elements were found to be cracked. If it is assumed that each of the 44 intact elements still had an acceptance level defect constant, the average cracked element constant is $65 \text{ cc(STP)/hr-cm}^2\text{-atm}^{1/2}$.

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TABLE 1
FUEL MATERIAL IRRADIATION EXPERIMENTS

Experiment	Temperature (°F)	Burnup (met. at.%)	Status	Significance
NAA-26-1, 2, 3, 4	1165 to 1300	0.027 to 0.084	Complete	Demonstrated the stability of U-ZrH _x fuel material at low burnups and SNAP 10A/2 temperatures.
NAA-43-3, 5, 6	730 to 1065	1.62 to 3.35	Complete	Irradiation of low hydrogen (H/Zr = 1.0) fuel to high burnups at moderate temperatures. Indicated moderately good stability (0-3% diametral changes). Data subject to question due to inaccuracies in post-irradiation measurements and hydrogen loss from the samples.
NAA-53-1, 2, 3	1210 to 1515	0.3 to 1.03	Complete	Indicated that fuel swelling would be important in the SNAP 8 operating regime. Data analysis gave rise to Miller correlation. S8DS fuel element design changed from S8ER design based on NAA-53 results. Actually a subscale fuel element. Problems in data interpretation pointed out the need for more carefully controlled experiments. Hydrogen loss was not taken into account.
NAA-75-1, 2	900 to 1075	0.5 to 2.15	Complete	Indicated that low temperature irradiation swelling of fuel is proportional to burnup. Fuel was constrained radially by the cladding and showed a growth of about 0.9% ΔD/D per metal atom percent burnup.
NAA-116-1, 2	300 to 600	0.18 to 2.0	Re-evaluation of data	Shows greater swelling than would be predicted by Miller correlation. Problems in evaluation due to uncertainties in temperature and some dimensional measurements. Appears to indicate that unrestrained irradiation swelling is ~1.5 to 3% ΔD/D per metal atom percent burnup. Effects of annealing are as yet unresolved.
NAA-120-3, 4	1300 to 1600	0.6 to 0.9	Fabrication	A statistical experiment designed to separate the effects of temperature, burnup and phase change on irradiation swelling. Attempts to eliminate temperature gradients, cladding restraint, etc.
NAA-120-1, 2	1300 to 1600	0.2 to 0.4	Design	Same as NAA-120-3, 4 but at lower burnups.
NAA-XXX-1	Above 1600	~0.4	Conceptual	Extend the temperature range covered by the NAA-120 series to establish design margins.
NAA-116-3, 4	200 to ?	~0.2 to 2.0	Conceptual	Statistical set of experiments to investigate the effects of temperature, burnup, restraint and fuel composition in the low-temperature regime.

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B. NUCLEAR ANALYSIS

1. Fuel Irradiation Damage

The basic objective of the SNAP reactor improvement program is to maintain a continually upgraded technology capable of designing a reactor at any time a specific requirement occurs. One aspect of this process is the determination of effects of fuel irradiation damage on reactor performance.

a. Listing of Experiments

A detailed review of the overall fuel irradiation program was initiated in conjunction with the SNAP 8 Project. A listing of all pertinent irradiation experiments, either planned or completed, is presented in Tables 1 to 5. The temperature, burnup, status, and significance of each experiment has been noted. The experiments include irradiation of fuel material, non-fuel material, subscale fuel elements, and SNAP reactor assemblies.

b. Summary of Fuel Growth Data

The parameter of interest for reactor design is the diametral change of the fuel during irradiation. A survey of References 1 through 12 was made, and the results have been compiled. See Figure 5, which shows percent diametral change vs metal atom percent burnup for the various irradiation tests studied. The time-averaged centerline fuel temperature has been noted with each point. Uncertainty bands have been indicated where such information was available.

An attempt to correlate the data resulted in the plot shown in Figure 6. The plot does not indicate good correlation, and two families appear to be separated. The only point that obviously does not fit is 53-1U. A wider range of data for the NAA-53 capsules is presented in Reference 13 (see Page 31). The uncertainty range of these measurements is shown in Figure 7. If the point at the center of the uncertainty band for 53-1U is used, the location on Figure 6 would be at a burnup of 0.45 met. at. % and a met. at. %/ $\Delta V/V$ of 0.19. This would fit the trend established by the major portion of the data.

The data obtained from irradiation of capsules 43, 53, and 75 have been plotted in Figure 8. The bands show the result of including both the calculated and mass spectrograph burnup information. They also indicate the change in $\Delta V/V$ when determined by diametral, length, and density measurements. Within the bands drawn for these particular data a correlation appears reasonable.

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TABLE 2
NON-FUEL MATERIAL IRRADIATION EXPERIMENTS

Experiment	Material	Temperature (°F)	Irradiation (nvt)	Status	Significance
NAA-68-1	AI-8763D barrier	1000 to 1200	1.0×10^{21} (fast)	Complete	Demonstrated the stability of AI-D coating on Hastelloy N at SNAP 2 temperatures.
NAA-77-1, 2, 3, 4	SCB-1 barrier	1300 to 1700	1.5×10^{21} (fast)	Complete	Demonstrated the stability of SCB-1 coating on Hastelloy N at SNAP 8 conditions. Hydrogen permeation rates remained within S8DS specification values. Showed an increase in permeation rate after about 400 hr which is attributed to devitrification of the barrier. Accelerated creep of the cladding was observed.
NAA-83-1, 2, 3	Hastelloy N	1450	1.5×10^{21} (fast)	Design	Will measure in-pile creep rate (and creep to rupture) as a function of stress at SNAP 8 conditions. Effect of irradiation on ductility will be determined.
NAA-77-5, 6, 7	Improved H ₂ barrier	-	-	Conceptual	Evaluation of irradiation behavior of improved barrier material.
NAA-83-4, 5, 6	Improved cladding	-	-	Conceptual	Evaluation of irradiation behavior of improved cladding material.

TABLE 3
SUBSCALE FUEL ELEMENT IRRADIATION EXPERIMENTS

Experiment	Temperature (°F)	Burnup (met. at. %)	Status	Significance
NAA-115-1	1300 to 1600	0.4	Hot-cell examination	Subscale S8ER fuel elements. Preliminary fuel swelling data agrees well with the Miller correlation.
NAA-115-2	1300 to 1600	0.8	In-pile	Subscale S8DS fuel elements.
NAA-117	1300 to 1500	0.4 to 0.8	In-pile	Similar to NAA-115-1 but at accelerated burnup rates.

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TABLE 4
FULL-SCALE FUEL ELEMENT
IRRADIATION EXPERIMENTS

Experiment	Temperature (°F)	Burnup (met. at. %)	Status	Significance
NAA-20-1,2	-		Complete	Inconclusive due to operational problems.
NAA-82-1	1500	0.09	Hot-cell examination	SNAP 2 fuel element operated successfully for the equivalent of one year at 125 kw. Preliminary data indicate that the fuel swelling was greater than would be predicted by the Miller correlation.
NAA-121-1	1450 to 1550	0.28 to 0.35	Final design	Demonstrate irradiation behavior of S8DS fuel elements at design conditions.

Figure 5. Diametral Growth vs Burnup for
NAA Irradiation Tests and the S8ER

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TABLE 5
SNAP REACTOR IRRADIATION EXPERIMENTS

Experiment	Coolant Temperature (°F)	Power (kwt)	Energy (kwh)	Status	Significance
SER	1200	50	2.25×10^5	Complete	Demonstrated feasibility of U-ZrH _x reactor concept. High hydrogen loss rates indicate need for additional development in the area of hydrogen retention.
S2DR	1200	65	2.73×10^5	Complete	Demonstrated successful operation of SNAP 10A/2 type reactor. Operational hydrogen loss rates coupled with out-of-pile testing showed need for improved hydrogen barrier at SNAP 8 conditions.
S8ER	1300	600	51×10^5	Hot-cell examination	Operated at SNAP 8 conditions using an interim hydrogen barrier. Performance of the reactor was better than expected. Hot cell examination revealed many cracked cladding tubes. Fuel swelling associated with cracked elements is greater than would be predicted from Miller correlation. Evaluation is continuing.
FS-3	970 to 1010	38	3.8×10^5	Shut down after 10,000 hr	Successful operation.
FS-4	1030	43	0.41×10^5	Shut down in orbit	Fuel elements apparently withstood Atlas-Agena launch without damage.
S8DS	1300	600	72×10^5 Expected	Fabrication	Long-term operation of reference SNAP 8 design. Changes incorporated in the fuel element design as a result of the NAA-53 data and barrier development effort should preclude a repetition of the S8ER cladding cracks.

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Figure 6. Fuel Growth Data from NAA Irradiation Tests and the S8ER

Figure 7. Fuel Growth - NAA-53 Capsule

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Figure 8. Fuel Growth—NAA-45, -53, and -75 Capsules

c. Fuel Growth Data from SNAP 8 Experimental Reactor (S8ER)

Operation of the S8ER was ended after producing 51×10^5 kilowatt hours of energy. The elements were then removed from the core and examined. The location of cladding cracks was noted for the 167 elements which had failed during the experiment. The locations of bulges on the cladding was noted for all the elements and the magnitude of the diametral change for each bulge was recorded for the 44 elements which remained intact. Of a total of 211 elements comprising the S8ER core, 15 have been declad for further study. Of these 15, 8 were elements with cracked cladding and 7 were elements with intact cladding.

Data obtained from the 15 fuel slugs was used to calculate both the maximum and average diametral fuel-clad interference based on the "as-built" diametral gap and the initial clad ID. The results, presented in Table 6, represent fuel

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growth data after 5.1×10^6 kwh of operation. The anisotropy of the observed fuel growth is shown by the results given in Table 7 and Figure 9. For isotropic swelling, $\% \Delta D/D$ equals $\% \Delta L/L$, as shown by the line on Figure 9.

The data for 39 of the intact elements, pertaining to cladding bulges, was used to infer values for $\% \Delta D/D$; the results are shown in Figure 10. The plot shows both the number of elements and percent of the elements with a given $\% \Delta D/D$ and indicates the element number and axial position of the maximum diametral change. The peak $\% \Delta D/D$ observed was 0.93 and the most probable value was 0.38. A comparison of the cladding strain based on fuel slug data and cladding bulge data was made for the 7 intact elements which were declad. The results for Element 432 are inconsistent, and may represent an indication of the uncertainty in the as-built diametral gap. The axial position of the maximum diametral change for the cladding generally occurs at the position of maximum diametral change for the fuel. Results are shown in Table 8.

TABLE 6
FUEL GROWTH AND CALCULATED CLADDING STRAIN
FOR 15 S8ER DECLAD ELEMENTS

Element Number	Ring Position	As-Built Diametral Gap (mils)	Maximum ΔD (mils)	Maximum $\% \Delta D/D$	Maximum Interference (mils)	Maximum Cladding Strain (%)	Average ΔD (mils)	Average $\% \Delta D/D$	Average Interference (mils)	Average Cladding Strain (%)
342*	I-1	5.0	9.0	1.69	4.0	0.71	6.7	1.26	1.7	0.30
476	II-4	4.2	4.3	0.81	0.1	0.02	3.8	0.71	-	-
519*	IV-1	4.3	8.9	1.67	4.6	0.82	5.4	1.02	3.3	0.59
398*	IV-13	5.4	9.0	1.69	3.6	0.64	1.4	0.26	-	-
309*	V-5	3.6	13.0	2.44	9.4	1.68	8.4	1.58	4.8	0.86
232	VI-24	3.6	6.2	1.16	2.6	0.46	3.5	0.66	-	-
476*	VII-19	3.0	2.7	0.51	-	-	0.6	0.11	-	-
525	VII-28	3.2	5.4	1.02	2.2	0.39	3.2	0.60	-	-
188	VIII-6	2.7	3.1	0.58	0.4	0.07	1.0	0.19	-	-
332*	VIII-25	3.2	6.5	1.22	3.3	0.59	-3.1	-0.58	-	-
432	VIII-29	3.0	7.4	1.39	4.4	0.78	4.4	0.83	1.4	0.25
206	IX-15	2.0	2.4	0.45	0.4	0.07	0.5	0.09	-	-
378*	IX-28	2.4	10.6	1.99	8.2	1.46	6.9	1.30	4.5	0.80
293	IX-29	2.5	3.1	0.58	0.6	0.11	1.2	0.22	-	-
111*	IX-42	1.8	6.1	1.15	4.3	0.77	1.9	0.36	0.1	0.02

*Indicates cracked cladding

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Figure 9. Comparison of Axial with Diametral Growth of Selected S8ER Rods

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Figure 10. S8ER Intact Rods - Frequency of Observation vs Maximum Cladding $\Delta D/D$

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TABLE 7
FUEL GROWTH OBSERVED IN 15 S8ER DECLAD ELEMENTS

Element Number	Ring Position	Average Values			As-Built [†] End Gap (mils)	X-Ray [†] End Gap (mils)
		% $\Delta D/D$	% $\Delta L/L$	ΔL (mils)		
342	I-1	1.26	0.26	36	23	14
474	II-4	0.71	0.34	47	24	27
519	IV-1	1.01	0.16	23	23	20
398	IV-13	0.26	0.30	42	21	-
309	V-5	1.58	0.42	59	25	37
232	VI-24	0.66	0.27	38	23	28
476	VII-19	0.11	0.59	82	26	23
525	VII-28	0.60	0.23	32	30	-
188	VIII-6	0.19	*	*	21	29
332	VIII-25	-0.58	*	*	23	-
432	VIII-29	0.83	0.44	61	22	20
206	IX-15	0.09	0.16	23	22	-
378	IX-28	1.30	0.38	53	23	-
293	IX-29	0.23	0.20	28	25	5
111	IX-42	0.36	0.10	14	25	22

* ΔL not available.

[†]Two methods of determining the axial clearance at beginning of life.

TABLE 8
S8DS/SRIP FUEL IRRADIATION DAMAGE

Element Number	Ring Position	As-Built Diametral Gap (mils)	Fuel Slug Data from Intact Elements								Cladding Bulge Data		
			Fuel ΔD @ Axial Position Noted (mils)					Interference @ Axial Position Noted (mils)			ΔD (mils)	Axial Position (in.)	
			10-in. §	11-in.	12-in.	13-in.	14-in.	10-in.	11-in.	12-in.			13-in.
474	II-4	4.2	3.8	4.0	3.9	3.3	*	-	-	-	-	-	†
232	VI-24	3.6	3.3	3.3	3.3	2.9	*	-	-	-	-	-	†
525	VII-28	3.2	4.1	4.8	4.8	3.7	*	0.9	1.6	1.6	0.5	-	†
188	VIII-6	2.7	1.2	2.5	2.8	2.9	*	-	-	0.1	0.2	1.4	12-1/4
432	VIII-29	3.0	2.5	2.8	2.6	2.3	*	-	-	-	-	0.7	11
206	IX-15	2.0	1.3	2.3	1.7	1.3	*	-	0.3	-	-	1.7	11-1/4
293	IX-29	2.5	1.4	1.6	2.1	3.0	*	-	-	-	0.5	2.7	14

*Not available.

†No cladding bulges in bottom 4-in. section.

§From top.

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2. Reactivity Effects of Alternate Fuel Element Cladding Materials in SNAP Reactors

As a part of the SNAP Reactor Improvement Program, alternate fuel cladding materials are evaluated when they appear to offer an advantage over the cladding material presently in use (Hastelloy N). One of the major considerations in the evaluation of an alternate cladding material is the effect on the excess reactivity of the assembly.

To facilitate the calculation of the reactivity effects of any prospective cladding material in SNAP reactors, a method was devised so that only the numbers listed in Table 9 (cladding density, weight fraction of each element, and the cladding thickness) are necessary for estimating the effect on core excess reactivity. This eliminates the need for a separate computer calculation for each prospective cladding material.

TABLE 9
REACTIVITY EFFECTS OF FUEL
CLADDING MATERIALS

Element	Reactivity Worth* $\{ \rho / [(\text{gm/cc}) - \text{mil} - \text{wt \%}] \}$
Nickel	3.16
Molybdenum	7.42
Iron	1.42
Chromium	2.08
Manganese	12.92
Boron	20250.
Cobalt	47.32
Tungsten	47.46
Titanium	3.59

*per cladding density, mil of cladding thickness, and weight fraction of element

Given the chemical composition of a prospective material, density, and wall thickness, the reactivity worth of the material may be determined. The constants in the table are based on the SNAP 8 core configuration but could be normalized for any U-ZrH_x reactor.

3. Hydrogen Redistribution and Loss Study

Mass transport of hydrogen in zirconium was reexamined in some detail. Two methods of calculation are currently employed at AI to estimate hydrogen distribution in fuel rods:

Method 1 — Solution of the usual mass transport equation:

$$J = -D \left(\frac{\partial N_H}{\partial r} + \frac{k_T}{T} \frac{\partial T}{\partial r} \right) , \quad \dots (1)$$

where

- J = mass flux vector,
- D = thermal diffusion coefficient,
- N_H = hydrogen concentration,
- r = direction of diffusion,
- k_T = thermal diffusion ratio,
- T = absolute temperature.

This method is used in the HYTRAN Code.

Method 2 — Iterative solution for hydrogen distribution assuming constant momentum at equilibrium. This is the method used in the HYREP Code.

Method 1 permits solution for the hydrogen distribution at any point in time (transient), but requires knowledge of the thermal diffusion coefficient and thermal diffusion ratio as a function of both concentration and temperature. Method 2 permits solution for the equilibrium hydrogen distribution only, and requires knowledge of hydrogen dissociation pressure as a function of both concentration and temperature. Both methods require a precise description of temperature distribution in the fuel rod.

The initial calculation of hydrogen redistribution transients in SNAP 10A fuel elements was performed with the HYTRAN digital computer code. A logic error

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was later discovered in HYTRAN; this has been corrected. The code has recently been used to calculate the time constant for hydrogen redistribution in SNAP 10A (results were in excellent agreement with S10FS-3 and -4 experience). HYTRAN is being modified to permit the calculation of transient hydrogen redistribution in any fuel rod. The "heat of transport," or " Q ," used in HYTRAN is proportional to k_T (the thermal diffusion ratio) shown in Equation 1. The quantity, Q^* , has been experimentally measured,⁽¹³⁾ and has been analytically checked by comparing HYTRAN calculated hydrogen distributions with experimental values and also those calculated with HYREP (Method 2).

The problem of determining the appropriate thermal diffusion coefficient has been extensively examined by experimenters (as summarized in Reference 14), but no agreement has been obtained. Several investigators have stated that the diffusion coefficients they observed in beta-phase zirconium hydride were independent of hydrogen concentration. This observation was probably justified for the low H/Zr ranges studied. In theory, one would expect to observe some concentration dependence for H/Zr ratios which approach saturation (~ 1.97). Accordingly, the diffusion data for delta and epsilon hydrides were reanalyzed and definite concentration dependence was observed, as shown in Figure 11. A semiempirical relationship expressing diffusion coefficient dependence on hydrogen concentration and temperature is being derived. Introduction of the hydrogen concentration dependence has resulted in very good agreement of most experimental data. Data for beta-phase ZrH has not yet been thoroughly examined for concentration dependence, although theory implies that it should exist.

The new diffusion coefficient model has been used in a preliminary check of redistribution time constant in SNAP 10A and S8ER fuel and resulted in good agreement with experimental results. It is expected that a significant improvement in analytical accuracy will result when the new model is applied to SRIP analysis of both hydrogen redistribution and hydrogen loss.

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Figure 11.
Concentration Dependence of Thermal
Diffusion Coefficient of Hydrogen in
 ϵ - and δ -Phase Zirconium Hydride

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IV. FUEL DEVELOPMENT

A. NAA-82-1 IRRADIATION EXPERIMENT

1. Summary

The NAA-82-1 irradiation experiment was designed to evaluate full-scale-production SNAP 2-type fuel element performance at 125-kwt reactor conditions of temperature and burnup.* The experiment successfully completed its scheduled 23 reactor cycles of irradiation in the Materials Testing Reactor (MTR), and is currently undergoing post-irradiation examination at the AI hot laboratories. The fuel element, both cladding and fuel rod, survived the irradiation intact and in excellent condition.

2. Status

The NAA-82-1 fuel element (both fuel rod and cladding) survived the rigors of one year's operation at 125-kwt reactor conditions intact and in excellent condition. Summaries of reactor operations and fuel dimensions which were given in the last quarterly progress report† are repeated here for clarity, Tables 10 and 11. (Note the slight changes.)

Figure 12 shows the fuel element before and after the thermocouples were removed. All 16 of the thermocouples were functioning normally at the end of the irradiation. The cladding was verified to be intact by a close visual examination, by fission gas analysis, cladding metallography, and by the absence of NaK or any NaK reaction with the ceramic coating on the inside of the cladding. Figure 13 shows the fuel rod and the cladding as it appeared cut open and with the end caps removed. The fuel rod is in excellent condition with no cracks, blisters, or other indication of gross irradiation damage. The pre-irradiation machining marks are still clearly visible. The adherence of the ceramic coating to the cladding appears to be very good; only minimal chipping occurred where the cladding cuts were made. The fuel rod was in intimate contact with the ceramic coating over virtually all of its surface. This can be seen in

*Principal design features were reported in "SNAP 2 Quarterly Progress Report, July-September 1963," NAA-SR-8990 (SRD), November 15, 1963

†L. M. Maki, "Progress Report SNAP Reactor Improvement Program, October-December 1965," NAA-SR-11754 (CRD), February 15, 1965

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COMPOSITE PHOTOGRAPH OF NAA-82-1 FUEL ELEMENT SHOWING CLADDING THERMOCOUPLES AND BANDS. SOME OF THE THERMOCOUPLES WERE DISPLACED DURING CAPSULE DISASSEMBLY.

COMPOSITE PHOTOGRAPH SHOWING FUEL ELEMENT CLADDING INTACT. NOTE THE OUTLINES OF EACH THERMOCOUPLE AND BAND. THE OXIDE LAYER DUE TO PRE-IRRADIATION PERMEATION TESTING HAS BEEN REMOVED BY THE NaK FROM THE HIGH-TEMPERATURE REGION OF THE CLADDING BUT IT IS STILL PRESENT AT THE ENDS, WHICH OPERATED AT RELATIVELY COOL TEMPERATURE.

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Figure 12. NAA-82-1 Post-Irradiation Fuel Element

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BOTTOM END OF
FUEL ELEMENT

TOP END OF
FUEL ELEMENT

COMPOSITE PHOTOGRAPH SHOWING FUEL ROD INTACT AND IN EXCELLENT CONDITION. NOTE THE MACHINE MARKS FROM THE PRE-IRRADIATION TURNING AND GRINDING OPERATIONS. SCRATCHES WERE CAUSED BY SLAVE FINGERS.

FISSION GAS
PUNCTURE HOLE

COMPOSITE PHOTOGRAPH SHOWING THE INSIDE OF THE CLADDING TUBE. THE ABSENCE OF CHIPPING OF THE CERAMIC WHERE THE CLADDING CUTS WERE MADE INDICATES GOOD COATING ADHERENCE. NOTE THAT THE MACHINE MARKS ON THE FUEL ROD HAVE BEEN IMPRESSED INTO THE SURFACE OF THE CERAMIC, INDICATING INTIMATE FUEL-CLADDING CONTACT. SCRATCHES WERE CAUSED BY SLAVE FINGERS.

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Figure 13. NAA-82-1 Post-Irradiation Fuel Rod and Inside View of Cladding

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TABLE 10
NAA-82-1 REACTOR OPERATION SUMMARY

Peak Fuel Temperature (design)	1510°F
Peak Clad Temperature (design)	1300°F
Peak Burnup (design)	0.08 met. at. %
Insertion Date	April 10, 1964
Removal Date	April 16, 1965
Time at Power	1 year
Thermal Cycles	>100

TABLE 11
NAA-82-1 PHYSICAL MEASUREMENT SUMMARY

Measurement	Pre-Irradiation		Post-Irradiation	
	Fuel Element	Fuel Rod	Fuel Element	Fuel Rod
Average Length (in.)	12.449	12.250	12.457	12.262
Average Diameter (in.)	1.2506	1.2102	1.2527	1.2168
Maximum Diameter Increase (in.)	-	-	0.006	0.0092
Maximum $\Delta D/D$ (%)	-	-	0.48	0.76
Average Diameter Increase (in.)	-	-	0.0023	0.0066
Average $\Delta D/D$ (%)	-	-	0.18	0.54
Length Increase (in.)	-	-	0.008	0.012
$\Delta L/L$ (%)	-	-	0.064	0.098
Calculated $\Delta V/V$ (%)	-	-	0.80*	1.24*
Average $\Delta \rho/\rho$ (%)	-	-	-	~1.02

*These values were calculated from the average diameter and length increases of the fuel rod and fuel element.

Figure 13 by the pattern of machine marks which have been impressed into the surface of the ceramic and also by a comparison of pre- and post-irradiation measurements. Despite the close contact, the ceramic coating did not stick to the fuel rod except at a small area in the end caps (see Figure 14).

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The fuel rod was sectioned for analysis according to the plan shown in Figure 15. A total of 8 wafers were cut for hydrogen analysis to establish the axial hydrogen redistribution profile. In addition, 3 of these wafers were analyzed for radial hydrogen distribution. Wafers for mass-spectrographic burnup analysis were taken at 3 locations. Density measurements were made on the 7 large remaining pieces; these were then sectioned to give 3 metallography samples and 3 additional burnup wafers for chemical analysis.

Photomicrographs of the three fuel metallography samples all show an epsilon-phase zirconium hydride structure with typical pronounced banding. No microcracks or increase in porosity over the pre-irradiation condition could be detected at 500X. The metallography samples were taken from three different locations in the fuel rod and included one from the peak-temperature, peak-burnup position. Figure 16 shows a comparison of pre-irradiation and post-irradiation metallography.

VIEW OF BOTTOM END OF NAA 82-1 FUEL ROD. THE DARK RING IS CERAMIC WHICH HAS ADHERED TO THE FUEL ROD AND PULLED OFF FROM THE CLADDING. NOTE THAT THE PRE-IRRADIATION ID NUMBER IS STILL CLEARLY VISIBLE.

VIEW LOOKING INTO THE BOTTOM END CAP SHOWING THE CONDITION OF THE CERAMIC COATING. NOTE THE CIRCULAR AREA WHERE THE COATING HAS PULLED OFF. THE VERY LIGHT AREA IS BARE METAL.

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Figure 14. NAA-82-1 Post-Irradiation View — Bottom End of Fuel Element and Respective End Cap

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Figure 15. NAA-82-1 Fuel Rod Segmenting Diagram

CARBIDE
PARTICLES
(LIGHT)

URANIUM
PRECIPITATES
(DARK)

EPSILON
ZIRCONIUM
HYDRIDE
GRAINS
(BANDED)

PRE-IRRADIATION METALLOGRAPHY FROM THE END OF THE FUEL ROD BEFORE MACHINING, SHOWING EPSILON ZIRCONIUM HYDRIDE STRUCTURE AT 500X. THE RELATIVELY SMALL GRAIN SIZE IS PROBABLY DUE TO HYDRIDING END EFFECTS. THIS SAMPLE WAS ETCHED.

POST-IRRADIATION METALLOGRAPHY TAKEN FROM THE CENTER OF THE FUEL, 6 in. FROM THE TOP END. THE HYDROGEN LEVEL AT THIS POINT IS 1.63 wt %, WHICH IS EQUIVALENT TO THE MAXIMUM LOSS OF 7.75%. THE OPERATING TEMPERATURE WAS $\sim 1500^{\circ}\text{F}$. THE PRESENCE OF THE TYPICAL BANDED EPSILON HYDRIDE VERIFIES THAT δ - β PHASE CHANGE DID NOT OCCUR. NOTE THAT THERE IS ESSENTIALLY NO DIFFERENCE FROM THE PRE- TO POST-IRRADIATION PHOTOMICROGRAPHS EXCEPT FOR THE LARGER GRAIN SIZE. THIS SAMPLE WAS ETCHED.

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Figure 16. NAA-82-1 Pre- and Post-Irradiation Metallography

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Figure 17. NAA-82-1 Hydrogen Distribution

Figure 18. NAA-82-1 Axial Temperature Profile in the Materials Testing Reactor

Hydrogen analyses have been performed on 21 samples taken from 8 fuel wafers. A summary of these data is given in Table 12, and shows the effects of hydrogen redistribution under the influence of the axial temperature gradient. This can be clearly seen by comparing Figures 17 and 18. The lowest hydrogen concentration (maximum hydrogen loss) was found at the center of the fuel rod where the maximum temperatures occurred. The top of the fuel, which operated at a relatively cool temperature, actually gained hydrogen.

TABLE 12
NAA-82-1 POST-IRRADIATION HYDROGEN ANALYSES SUMMARY
(Pre-irradiation wt % Hydrogen = 1.767)

Hydrogen Wafer No.	Weight Percent Hydrogen		Percent Change
	Centerline	Radial Average	
1	1.82		+3.00
2	1.78		+0.70
3	1.68	1.70	-3.86
4	1.61	1.64	-7.20
5	1.63	1.63	-7.75
6	1.64		-7.20
7	1.74		-1.50
8	1.74		-1.50
Volume Average	1.695		-4.07

The average hydrogen loss was 4.07% and the maximum loss was 7.75%. These low hydrogen loss values were not sufficient to cause the fuel to undergo a δ - β phase transformation even at the peak fuel operating temperature of 1500°F. This is verified by the observed epsilon-phase zirconium hydride composition of the metallography specimens.

Density measurements have been taken on 7 large fuel samples. The observed density changes, with comparison to the diameter measurements, are shown graphically in Figure 19. The anisotropic swelling behavior of the fuel in this experiment can be seen from the empirical relationship $\Delta\rho/\rho \cong < -3 \Delta D/D$. For pure isotropic swelling, this relationship would be $\Delta\rho/\rho = -3 \Delta D/D$. The

Figure 19. NAA-82-1 Post-Irradiation Measurements

relatively large density change at the top of the fuel is probably due to axial growth, whereas the relatively small density change at the bottom of the fuel rod may reflect some axial compression.

The burnup analysis derives from several sources: heat transfer calculations based on in-pile temperature measurements, mass-spectrographic burnup analyses, chemical burnup analyses, axial gamma scans taken on the intact fuel element, radial gamma scans taken on the burnup wafers, and autoradiographs taken on the metallography samples. Of these methods, only the first 3 give absolute burnup numbers; gamma scans and autoradiographs give relative values. The heat transfer calculation gives a peak burnup of 0.08 met. at. %, which is also the design value. Two of the mass-spectrographic burnup samples verified these calculations. However, the third burnup sample gave an unexpected high value. For comparison, the gamma scan data and the autoradiograph readings have been normalized to give 0.08 met. at. % peak burnup and are shown in Figure 20, together with the mass spectrographic data and the calculated value. The agreement shown by these data presents a very strong case against the validity of the single high burnup measurement. The chemical burnup determination will provide an additional check on this.

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Figure 20. NAA-82-1 Burnup Data

Photomicrographs taken of the cladding metallography samples (see Figure 21) show no cracks or observable damage at 500X. Both the cladding and ceramic coating appear to be in good condition. The Hastelloy grain size is somewhat larger than that seen in the NAA-115-1 experiment, and there was pronounced twinning. There appeared to be less intergranular precipitation than was present in the S8ER cladding. The absence of any reaction on the ceramic indicates that there was no NaK present inside the clad and provides additional verification of the integrity of the cladding. Microhardness measurements taken on the three cladding metallography samples show no significant difference from preirradiation values.

In conclusion, it can be stated that the NAA-82-1 was a model irradiation experiment. The overall excellent condition of this fuel element, including the fuel rod, cladding, and ceramic coating, amply demonstrate the ability of this type of full-scale fuel element to survive the rigors of 125-kwt SNAP 2 reactor conditions of temperature and burnup.

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CERAMIC CHROMIZED LAYER HASTELLOY N CLADDING CHROMIZED LAYER

CROSS-SECTION OF CLADDING TAKEN AT 200X. THE CLADDING APPEARS LARGE GRAINED WITH PRONOUNCED TWINNING. THERE IS BOTH INTERGRANULAR AND INTRAGRANULAR PRECIPITATION. THE CERAMIC IS INTACT AND ADHERENCE APPEARS TO BE GOOD. THIS SAMPLE WAS ETCHED.

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HASTELLOY N CLADDING CHROMIZED ZONE

CLOSEUP OF OUTSIDE EDGE OF THE TUBE SHOWING THE REACTION OF CHROMIZED ZONE AND SHOWING PRECIPITATES. SCRATCHES ARE EVIDENT. THIS SAMPLE WAS ETCHED.

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Figure 21. NAA-82-1 Post-Irradiation Cladding Metallography

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B. NAA-120-3 AND -4

1. Summary

The NAA-120 irradiation experiments are statistically designed to evaluate SNAP fuel performance as functions of temperature, burnup, and fuel composition. The detailed design features have been reported in previous Quarterly Progress Reports.*

The construction of this experiment is proceeding satisfactorily. A rather significant change in assembly technique has been introduced, however, in order to achieve greater in-pile reliability.

Considerable progress has also been made in the following additional areas:

- 1) Identification of the poor quality of reworked welds,
- 2) Improved tungsten coating adherence to the fuel supports through the use of a nickel flash, and
- 3) Development of qualification tests to insure high product reliability.

2. Status

The original procedure for assembling the fuel material was to shrink-fit or press-fit the enriched center and guard sleeves over the support rod. Stress calculations indicated that the stresses introduced by the interference fit coupled with thermal gradient stresses could exceed the matrix strength of the fuel material. If cracking occurred, temperature perturbations would result which are inconsistent with the design objectives. Consequently, thermal cycling tests were conducted which clearly demonstrated the crack-resistant superiority of fuel material which did not contain "interference fit" stresses. Fuel specimens assembled by an interference-fit and a slip-fit process were subjected to heating and cooling rates in excess of those encountered in reactor startups and shutdowns. The results confirmed the advisability of employing a slip-fit assembly procedure.

*Principal design features of the NAA-120 irradiation experiments were reported in "SNAP Reactor Improvement Program Quarterly Progress Report, July-September 1965," NAA-SR-11690 (CRD), November 22, 1965.

Figure 22. Hot-Short Cracks in Repair Welds
(Electron-beam welds on Hastelloy X)

Figure 23. Sound Single-Pass Welds
(Electron-beam welds on Hastelloy X)

Adoption of a slip-fit assembly procedure introduced two new problems:

- 1) Rotating of the fuel sleeves during final machining, and
- 2) Lateral movement of the fuel sleeves resulting in misalignment and skewing of the axial temperature profile.

Rotating of the fuel sleeves was eliminated by the use of specially designed collets and a hardened mandrel. To prevent lateral movement, a deformable — 10% dense — fibre metal body is being evaluated. Since stresses less than 1000 psi are needed to deform this material, lateral movement of the fuel sleeves during handling is prevented; however, irradiation-induced fuel swelling is not affected.

Examination of the cladding tube to end cap welds revealed that in some cases the welds contained tungsten inclusions. Attempts to repair these welds using an electron-beam welder resulted in "hot-short" cracks (see Figure 22). These cracks were not present in single-pass welds, i. e., in welds that did not require repair. This can be seen in Figure 23, a typical sound weld. In view of these results, all welds in the NAA-120 experiment will be single-pass welds; no further attempts to repair defective welds will be made.

During the preliminary design of the NAA-120-3 and -4 experiments, it was found that the Hastelloy-X fuel supports could possibly adhere to the glass hydrogen barrier resulting in restraint of the fuel during irradiation. Compatibility tests indicated that tungsten was compatible with the hydrogen barrier in the temperature range of interest. Consequently, tungsten was vapor-deposited on the Hastelloy-X fuel supports. Problems of adherence arose, and the tungsten coating chipped during machining. It was learned that a nickel flash applied to the supports prior to vapor deposition of the tungsten imparted excellent adherence. The nickel flash has been incorporated as a routine step in the manufacture of the fuel supports, and no further chipping difficulties have been encountered.

Very careful attention is being given to quality assurance during the construction of the NAA-120-4 experiment. Various testing and qualification techniques have been developed to insure the highest finished product quality. Measurement tests using combinations of air gages, dial indicators, go-no-go gages, and electronic internal measuring devices have been developed to establish acceptance

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and rejection criteria. These tests were necessitated by the tight design tolerances. Permeation testing equipment have also been developed for the NAA-120 fuel specimens with capabilities of simulating the in-pile temperature profile. In addition to these tests, high-temperature compatibility studies are being initiated to determine if there is a compatibility problem with SNAP fuel and the deformable fibre metal body previously mentioned.

Sufficient cladding tubes and end caps for the NAA-120-4 experiment, including adequate backup, have been machined and welded up. They are presently in the process of being enameled with the hydrogen barrier material.

Of the fuel assemblies, the fuel sleeves, guard rings, and support rods are complete, and the tungsten-coated fuel end supports are about 40% complete.

C. DESIGN AND EVALUATION

1. Summary

The design and evaluation task serves three basic needs: (1) provision of direct analytical support for the other fuel development tasks; (2) provision of technical liaison to assure that experimental data and the results of analytical studies performed under the 14000 accounts are incorporated into the fuel development effort; and (3) performance of inexpensive experimental tasks which are not funded under another fuel development subaccount, or are needed to evaluate the feasibility of a particular concept. During the last quarter, the tasks briefly summarized in the following four paragraphs were performed:

The thermocouple comparison test, designed to compare the performance of Chromel-Alumel and Geminol thermocouples in three different hydrogen-containing environments, was terminated prematurely when NaK leaked from the test assembly. A slight upward drift ($\sim 2^\circ\text{F}$) in calibration had been found during the 380 hours at temperature prior to test termination.

A modified SNAP fuel alloy, developed by Denver Research Institute, was evaluated to confirm the reported lower hydrogen dissociation pressure. The reported results were not confirmed by dissociation pressure and hydriding studies at AI.

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A modest development effort was initiated to apply an yttrium coating to the exterior surfaces of SNAP fuel cladding assemblies as part of an in-core hydrogen getter concept. The initial phase of this work, involving application to flat coupons, proved successful and efforts are now directed toward coating tubular specimens.

Efforts were initiated during this quarter on development of a new sophisticated digital computing code for data reduction of the NAA-120 irradiation experiments. The new code, written in FORTRAN IV, has been appropriately designated the NAA-120 code and was approximately 50% completed at the end of the quarter.

2. Status

a. Thermocouple Comparison Test

The Chromel-Alumel vs Geminol thermocouple comparison test was terminated prematurely. After approximately 380 hours at temperature (1300°F) a small hole developed in the microbrazed joint on the lower bulkhead, which allowed the NaK to escape into the furnace retort. A detailed description of the experiment and equipment is given in the previous quarterly progress report.* Up to the time of test shutdown, a slight upward drift (about 2°F) in thermocouple output was measured, as shown in Table 13. Anticipated test results were rapid

TABLE 13
RESULTS OF THERMOCOUPLE COMPARISON TEST

Number of Thermocouples	Thermocouple Type	Location	Average Change in Output After 380 Hours (°F)
3	Chromel-Alumel	In-Fuel	+1.8
3	Geminol	In-Fuel	+1.7
4	Chromel-Alumel	NaK	+1.5
6	Geminol	NaK	+1.4
3	Chromel-Alumel	Vacuum	-0.7
2	Geminol	Vacuum	+1.5

*L. M. Maki, "Progress Report SNAP Reactor Improvement Program, October-December 1965," NAA-SR-11754 (CRD), February 15, 1965.

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failure of the Chromel-Alumel in-fuel thermocouples; this did not, however, materialize and 21 of the 23 thermocouples used in the test performed well.

One of the two malfunctioning thermocouples was broken during assembly due to hydrogen embrittlement during microbrazing. The other bad thermocouple, which was located in the NaK, failed on test startup — the cause is unknown. Plans are in progress to initiate a similar test with a somewhat wider scope of in-fuel thermocouple evaluation.

b. Denver Research Institute Alloy

Studies have been completed on an alloy hydride developed by Denver Research Institute (DRI), which was reported to reduce the hydrogen dissociation pressure by approximately 40% over that of unalloyed zirconium hydride. The alloy consists of a 0.50 wt % scandium addition and a 0.30 wt % beryllium addition to the 90 wt % Zr-10 wt % U alloy hydride. The beryllium is reported to provide grain refinement, in much the same way as does the carbon addition to the SNAP fuel, while the scandium addition is responsible for lowering the dissociation pressure of the hydride.

To evaluate the merits of this alloy, two unhydrided samples were obtained from DRI. Density measurements of 6.964 gm/cc and 6.894 gm/cc were obtained for the two samples. The more dense sample was chosen for study, since its density was comparable to the unhydrided SNAP fuel alloy. This sample was hydrided along with sections of the SNAP fuel alloy. The dissociation pressure isochore, determined by DRI for a H/Zr (total) ratio of 1.85, was used to control the hydride composition during the hydriding process. The resulting hydride composition, as determined by weight gain measurement, was 1.76 H/Zr. This resultant hydride composition was not expected because the reported 1.85 isochore was used. The hydride composition of the SNAP fuel alloy, however, was 1.78 H/Zr total; this composition was expected because the DRI, 1.85 H/Zr isochore, and the unmodified SNAP fuel 1.78 H/Zr isochore are almost identical. This fact indicates that the proper isochore was used during hydriding and that the DRI alloy did not respond to hydriding in the reported manner.

To substantiate the weight gain analysis used for hydrogen content determinations, equilibrium dissociation pressure measurements were obtained on the

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hydrided sample of the DRI alloy. Results from this test are presented in Figure 24. As indicated, the experimentally determined isochore for the 1.76 H/Zr total, DRI alloy hydride falls very close to the isochore used during hydriding. This indicates that equilibrium conditions did exist during hydriding. Because these results do not confirm the findings reported by DRI, an extensive quantitative chemical analysis was conducted to verify the chemical composition of the hydrided DRI sample. The original chemical composition of this alloy as published by DRI is presented in Table 14.

Figure 24. Comparison of AI and DRI Isochore Curves for the Same Alloy

TABLE 14
CHEMICAL COMPOSITION OF THE DRI ALLOY AS
REPORTED BY DENVER RESEARCH INSTITUTE
(Elements in wt %)

Zr	U	Sc	Be
89.2	10.0	0.5	0.3

These values were used to calculate total H/Zr of the hydrided sample by weight gain measurements (1.76 H/Zr) and any deviation from this composition could also influence the total H/Zr value.

The results of the quantitative chemical analysis conducted at AI, along with the method of analysis used, are presented in Table 15.

TABLE 15
CHEMICAL COMPOSITION OF DRI ALLOY
AS DETERMINED BY AI
(Elements in wt %)

Zr	U	Sc	Be	H
84.81	12.24 ± 0.05 Volumetric	1.2 ± 0.05 Colorimetric	0.08 ± 0.005 Colorimetric	1.67 ± 0.02 Vac. Extraction

Using these values to calculate total H/Zr, we have the following expression:

$$\text{H/Zr}_{\text{Total}} = \frac{90.496 (\text{wt \% H})}{100 - (\text{wt \% H} + \text{wt \% U} + \text{wt \% Be} + \text{wt \% Sc})}$$

$$\text{H/Zr}_{\text{Total}} = \frac{90.496(1.67)}{100 - (1.67 + 12.24 + 0.08 + 1.2)}$$

$$\text{H/Zr}_{\text{Total}} = \frac{151}{84.81}$$

$$\text{H/Zr}_{\text{Total}} = 1.78$$

This value of 1.78 H/Zr total is identical to the value obtained for the SNAP fuel alloy, and, thus, further supports the earlier data.

In conclusion, it is felt that the results obtained from this analysis indicate the DRI alloy to exhibit essentially the same hydrogen dissociation pressure as the unmodified SNAP fuel alloy. Although the composition of the tested alloy varies from that reported by DRI, the higher scandium content would be expected to have even more influence on reducing the dissociation pressure.

c. Yttrium Coating

A small development effort has been initiated to apply an 8-mil coating of metallic yttrium to the exterior surfaces of SNAP fuel elements. The objective of this work is to study the feasibility of an in-core hydrogen getter concept. The application and processing work has been subcontracted to Temescal Metallurgical Corporation, Berkeley, California. Temescal uses large electron-beam

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guns to melt and vaporize metallic materials within large high-vacuum chambers. The vaporized material streams in a direct line-of-sight path and deposits on the component to be coated. By bending the electron beam, they are able to mount the electron gun to one side of the crucible, thereby permitting an unobstructed path for the vaporized particles. This feature, together with large working areas within the vacuum chamber, permits considerable versatility in fixturing devices for handling complicated substrate geometries.

Phase I of the program involved the application of yttrium to flat coupons of chromized Hastelloy N. This phase has been completed and four samples, each representing a different substrate temperature, were submitted for evaluation. The samples were examined metallographically, and, as shown in Figure 25, the yttrium coating was dense and oxide-free. The sample which was subjected to a substrate temperature of 1320°F during the yttrium application was judged to possess the best metallurgical bond between the yttrium and Hastelloy-N substrate. Samples of the yttrium-coated Hastelloy N were also exposed to hydrogen at 1650°F and hydrided to about 1.7 H/Y. However, it was found that at this temperature (1650°F) serious diffusion of the yttrium into the Hastelloy N occurred.

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Figure 25. Yttrium Layer on Chromized Hastelloy N
Applied at a Substrate Temperature
of 1320°F — Cross-Sectional View

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This diffusion prevented a detailed evaluation of the hydriding results, but the hydride appeared sound in spots where diffusion did not occur. This diffusion of the yttrium into the Hastelloy N is not expected at temperatures of 1300 to 1400°F.

Based on evaluation of the coupons, a decision was made to continue with Phase II of the work. This will involve the application of yttrium to short lengths of SNAP 8 fuel cladding tubes.

d. NAA 120 Data Reduction Code

As previously mentioned, efforts were initiated during this quarter toward the development of a new, sophisticated, digital computing code for data reduction of the NAA-120 series irradiation capsule experiments.* The new code (written in Fortran IV) has been designated NAA 120; it was approximately 50% completed at the end of the quarter. The design objectives for the NAA-120 data reduction code are as follows:

- 1) To permit operation of one or two NAA-120 irradiation assemblies on a single 100-channel punched paper tape instrumentation recording console.
- 2) To provide means whereby any instrument can be on any channel at any time.
- 3) To provide means whereby treatment of bad intervals of readings of instruments can be accomplished either by input of data cards or by internal program logic.
- 4) To provide internal program logic to compute values for channels 51 to 100 at times when the instrumentation console is in an alarm mode condition and rapidly scanning only channels 1 to 50.
- 5) To supply a complete data tabulation for each run of a punched paper tape to indicate hookup of all instruments with recording channels and the readings of all channels; to place an asterisk after all values obtained by computation, i. e., because of treatment of bad data, or instruments being on channels 51 to 100 and instrumentation system in the alarm mode.

*See Section IV-B of this report for the status of the NAA-120 irradiation experiments.

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6) To furnish CRTs for each run describing the detailed temperature operation of each individual fuel specimen.

7) To provide means whereby generation of from 0 to 10 updated CRT histogram summaries may be obtained for any run of a given punched paper tape, the selection of the thermocouples being left to the user of the code.

8) To provide means for obtaining individual calibration constants by internal program logic for the various flux monitors (utilizing appropriate capsule ΔT 's), and utilizing these monitors to compute accumulative burnup of all individual fuel specimens; to yield a printed output tabulation at the end of each run giving current flux monitor calibration constants and computed accumulative burnup for the individual fuel specimens.

9) To provide means for computing in-pile fuel swelling for those fuel specimens containing internal fuel thermocouples; additionally, to compute the predicted swelling for all individual fuel specimens utilizing the current fuel swelling correlating equation; to give as printed output at the end of each run the accumulative fuel swelling computed by both techniques for the fuel specimens having internal fuel thermocouples, and also to give the predicted fuel swelling for the other fuel specimens obtained using the swelling correlating equation.

10) To condense all data for all instruments for a given run of a punched paper tape; to transfer the condensation as a particular file to magnetic tape such that the condensation for run (n) immediately follows that for run (n-1), (n+1) immediately follows (n), etc.; to compute the physical length of all records, record gaps, and end of file marks written on such a magnetic tape; to give as printed output at the end of each run the total length of tape consumed so that a user of the code will know when to initiate the next successive tape for the tape condensed output file.

The primary objective of the NAA 120 code is attainment of the condensed output file of magnetic tapes. The technical approach in order to obtain both an understanding and good correlation of fuel irradiation performance is utilization of the computer, starting with pre-irradiation data of individual fuel specimens, and tracing through in-pile performance, making use of the condensed magnetic

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tape history files. Hydrogen loss from hot-cell post-irradiation data must be programmed as time functions for the individual fuel specimens. Hot-cell burnup measurements must be checked against burnup computed by the NAA 120 code and, if necessary, modifications must be made to the means by which the code computes calibration constants for the various flux monitors. Then, the condensed history files can be re-run through the computer using a special code, still to be written, which permits use of various fuel swelling correlations to obtain computed fuel dimensions at the end of irradiation, these to be directly compared with hot-cell measurements.

Progress in development of the NAA 120 code throughout the quarter has been excellent. Two successive dummy-punched paper tapes generated by the actual NAA-120 instrumentation console at the Hanford reactor site have been used to permit checkout of the code in its various stages of development. These tapes were obtained by utilizing various millivolt signal inputs to the 100 recording channels of the instrumentation console.

To demonstrate a small portion of the output of the current NAA 120 code, a sample page of tabular data is presented in Figure 26. Figure 26 contains the tabular data for eight successive input readings (converted to temperature or relative flux) of Channels 1 to 14. Shown are the day, hour, and minute for data taken in each of the channels, together with the accumulative hours for the eight sets of data listed. The numbers 4-1-2 and (5) above Channel 1 (for example) are: NAA-120-4 irradiation assembly, Experiment No. 1 and Fuel Specimen No. 2 (a zero in place of the 2 would indicate a flux monitor reading for experiment No. 1); the 5 in parentheses denotes the instrument number assigned to Channel 1. Should an asterisk follow any value, it would indicate that the data point had been calculated by the computer code.

					4-1-2	4-1-3	4-1-4	4-1-2	4-1-1	4-1-1	4-1-5	4-2-0	4-2-2	4-1-4	4-1-5	4-2-3	4-2-3	4-2-4
					(5)	(8)	(11)	(6)	(1)	(2)	(14)	(62)	(21)	(12)	(15)	(24)	(26)	(27)
(1)	DAY	HR	MIN	ACC HRS	1	2	3	4	5	6	7	8	9	10	11	12	13	14
1	111	13	20	0.00	66	64	64	64	64	66	68	66	68	66	68	66	68	70
2	111	14	10	0.83	66	64	68	64	68	64	68	66	66	66	64	70	68	70
3	111	14	25	1.08	62	64	62	66	64	64	66	66	66	64	4	84	66	68
4	111	23	48	10.47	64	64	68	68	66	66	66	66	68	66	66	68	68	68
5	111	23	48	10.47	70	64	62	62	64	62	62	62	64	62	64	66	64	64
6	111	23	48	10.47	64	64	66	66	64	64	64	64	66	64	64	64	64	68
7	111	23	49	10.48	66	62	62	64	68	64	64	66	64	68	68	70	66	66
8	111	23	49	10.48	64	64	64	66	64	64	66	66	68	64	66	68	68	70

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Figure 26. NAA-120 Output Data Format
(14 of 100 channels are shown)

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Typical CRT displays indicating detailed fuel specimen performance are presented in Figure 27. The major heading of such a CRT defines the experiment and particular band width in use. Minor headings are utilized to indicate the particular fuel specimens. To the right of each grid are identifications of the various plotting symbols used. Note that all grids on a given page of CRT output would have the same time scale, but that the temperature scale could differ in that each grid in this respect has the best attainable scale for the range of temperatures being plotted. Note that the time period spanned on a given set of grids has taken into consideration the necessity of having resolution between points. The graph in Figure 27-a spans the time period from 0.0 to 10.47 hours. The next successive graph (Figure 27-b) spans the time period from 10.47 to 10.53 hours, the initial time points on the latter graph being the last plotted points on the previous graph.

D. IMPROVED BARRIER DEVELOPMENT

1. Summary

In Fiscal Year 1965, the SNAP 8 program developed three different modifications of the SCB hydrogen barrier. The reference S8DS hydrogen barrier, SCB-1, undergoes devitrification as a function of time and temperature, and thereby its hydrogen retention capabilities decrease by a factor of approximately 2 or 3. A task was established to evaluate these new barrier materials — designated SCB-1 plus 10% Al_2O_3 , SCB-2, and SCB-23 — relative to SCB-1 for future SNAP reactors. During this report period, the three modifications of the SCB-1 hydrogen barrier were applied to Hastelloy X-285 and Hastelloy X substrates. Of these three coatings, only SCB-1 plus 10% Al_2O_3 proved satisfactory.

Coating of Hastelloy X-285 required preoxidation and was therefore discontinued. Hastelloy X, which can be used without pretreatment, was substituted.

Initial permeation data of Hastelloy X-285 coated with SCB-1 plus 10% Al_2O_3 showed this barrier material to be at least equal in hydrogen retention of SCB-1. Additional permeation tests at 1600°F indicate a slight increase in resistance to devitrification compared with SCB-1.

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NAA-120-4, EXPERIMENT 2, BAND = 10

a. Time Period from 0 to 10.47 hours

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b. Time Period from 10.47 to 10.53 hours

Figure 27. NAA-120 Data Reduction Code CRD Display

2. Status

The three experimental coatings, SCB-1 plus 10% Al_2O_3 , SCB-2, and SCB-23, were applied to Hastelloy X-285 membranes. In all cases, the substrate had to be preoxidized to achieve any degree of adherence. This was accomplished by exposing the membranes to 2000°F for 1 hour while circulating dry air through a quartz tube inserted in the membrane. This treatment resulted in a black, tightly adherent oxide layer on the metal. The glass coatings were then fired at 2125°F. SCB-1 plus 10% Al_2O_3 produced a coating of excellent adherence. The SCB-2 and SCB-23 barrier materials, on the other hand, showed, under identical coating conditions, pits, blisters, and discolorations. Both also had poor adherence. At firing temperatures of 2200°F, the adherence of SCB-2 improved, but the adherence of SCB-23 remained poor. Due to the poor adherence of SCB-2 and SCB-23 to the substrate, no membranes with these coatings were made, and no permeation tests conducted. Because of the limited funds in this subaccount and because SCB-2 and SCB-23 do not at this time indicate potential improvement for future SNAP reactors, all future FY 1966 efforts under the improved barrier subaccount will be directed toward evaluating SCB-1 plus 10% Al_2O_3 .

Hastelloy X was found not to require preoxidation in order to develop excellent adherence with SCB-1 plus 10% Al_2O_3 coating. For this reason, and because Hastelloy X-285 proved difficult to weld, it was decided to discontinue use of the latter and to use Hastelloy X.

The adherence of the coating was measured by flexing 3/4-in.-long lengthwise-split tube sections over a mandrel of the same diameter. Less than 50% of the coating removed by 23 flexes is considered excellent adherence.

Permeation tests were conducted on four Hastelloy X-285 membranes coated with SCB-1 plus 10% Al_2O_3 . Preliminary values are shown in Table 16.

These values compare very well with an average value of 1.6×10^{-3} cc(STP)/hr-cm² at 1500°F and 1 atm hydrogen pressure for SCB-1 on chromized Hastelloy-N.

Changes in permeation rates and in the activation energy for hydrogen permeation through the SCB-1 barrier have, in the past, been used as guidelines for determining crystallization kinetics. In general, the permeation rate at 1500°F

TABLE 16
HYDROGEN PERMEATION RATE OF FOUR HASTELLOY-X-285
MEMBRANES COATED WITH SCB-1 PLUS 10% Al_2O_3

Membrane Number	Hydrogen Permeation Rate at 1500°F and 1 atm H_2 [cc(STP)/hr-cm ²]
XX-2	1.35×10^{-3}
XX-5	1.0×10^{-3}
X37-2	1.2×10^{-3}
X23-5	1.0×10^{-3}
Average	1.16×10^{-3}

and 1 atm H_2 increases due to crystallization by a factor to 2 to 3. Also, the activation energy decreases due to crystallization of SCB-1 from a nominal value of 27,000 cal/mole to about 15,000 cal/mole.

A Hastelloy-X membrane coated with SCB-1 plus 10% Al_2O_3 was tested in a manner similar to that used in previous SCB-1 membrane tests, with the activation energy being measured after various inputs. Table 17 shows the results.

TABLE 17
HYDROGEN PERMEATION RATE AND ACTIVATION ENERGY DATA
FROM A HASTELLOY-X-285 MEMBRANE COATED WITH
SCB-1 PLUS 10% Al_2O_3

Time (hr)	Hydrogen Permeation Rate at 1600°F and 1 atm H_2 [cc(STP)/hr-cm ²]	Activation Energy* (cal/mole)
0	1.5×10^{-3}	24,300
19	1.5×10^{-3}	24,300
36	1.5×10^{-3}	24,300
103	$1.3 \times 10^{-3}\dagger$	19,700
690	3.9×10^{-3}	19,700

*Measured between 1600°F and 1300°F

†Pressure dropped below 1 atm H_2

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The crystallization of SCB-1 at 1600°F usually takes about 24 hours, as compared to approximately 100 hours for the SCB-1 plus 10% Al₂O₃ coating. Since crystallization is an exponential function of temperature, the small time increments (100 - 24 = 76 hr) at 1600°F will prove very beneficial at 1400°F. Therefore, the preliminary indications are that SCB-1 plus 10% Al₂O₃ should resist devitrification at 1400°F far better than standard SCB-1.

E. IMPROVED PROCESS DEVELOPMENT

1. Summary

Major efforts during the reporting period were directed towards evaluating the reverse-drain coating method and the quality of the plug-type seal. Twelve tubes were processed using this technique;* plug-type seals were assembled at one end and the membranes were submitted for permeation testing. Detailed dimensions obtained during processing showed that the reverse-drain method was capable of reducing the coating taper by a factor of approximately 3 times that produced by the conventional coating procedure. Extensive work was performed on various measurement techniques for the inside diameters of tubing. It appears that a marked improvement in measuring capability, accuracy, and reproducibility, will result from these efforts. Specifically, methods were developed which provide precise measurements for use in calculating the amount of interference fit with the plug-type seal. Past experimental data has shown that the proper amount of interference fit is important in achieving a sound hydrogen barrier at the end closure seal. The improved measurement methods for determination of interference fit are directly applicable to the SNAP 8 process development program.

2. Status

Twelve full-length (20-in.) open-end tubes were coated, assembled with plug-type seals, and submitted for permeation testing. Six of the tubes were chromized Hastelloy N and six were pre-oxidized Hastelloy X-285. Half of each group was plugged after the first coat and half after the second coating application.

*Details of this process and program objectives are given in the last quarterly progress report, L. M. Maki, "Progress Report, SNAP Reactor Improvement Program, October-December 1965," NAA-SR-11754 (CRD), February 15, 1965

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The tube assemblies were inspected for inside diameter measurements in the uncoated, first-coated and second-coated stage. Dimensions were obtained using a conventional six-jet air gage assembly. Difficulties encountered in attaining reproducibility in the dimensional data resulted in a detailed investigation of inside diameter measurement techniques. Attempts to duplicate measurements using the same gage assembly produced variations up to 0.001 inch. Comparison of averaged readings made by two different gages showed differences of as much as 0.0012 inch.

An internal comparator was used as the reference for verifying the accuracy of the various available conventional six-jet air gage assemblies. Data were obtained using gage assemblies designed for an overlapping series of inside diameter ranges with various plug diameters. Analysis of the data showed that selection of the gage with the largest practical plug diameter was of primary importance to accurate and reproducible measurements. It was established that optimum selection of the gage range and the plug diameter produced readings within 0.0001 inch of those determined by the reference internal comparator. Sufficient data were obtained to calculate correction factors for the various gage assembly combinations. The corrected dimensions were used in all subsequent evaluation of measurements.

The taper of the reverse-drain coated tubes was compared to that obtained from the conventional unidirectional drainage process. The taper — defined as the difference between the maximum and minimum inside diameter — normally experienced in the conventional method averages about 0.004 inch. The amount of taper in the twelve reverse-drain coated tubes is summarized thus:

Minimum (in.)	0.0009
Maximum (in.)	0.0020
Average (in.)	0.0014

A series of measurements was taken at the open end of the coated tubes for calculating the interference fit for assembly of the plug. Two six-jet air gage assemblies and the internal comparator were used to measure the inside diameters of the tubing. Confirming earlier findings, six of the tubes measured by an air gage with a close fitting plug showed good agreement with data obtained

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from the reference internal comparator method. The tubes measured with the loose-fitting plug gage showed a lack of agreement in measurement from 0.0004 to 0.0012 inch. These differences can be considered significant because the interference fit between the tube and plug is specified as 0.003 to 0.004 inch. Historical data has indicated that maintaining the required degree of interference is of major importance to the subsequent element or membrane leak rate and could possibly be a primary reason for the relatively high leak rates from the blend end of fuel elements.

In order to evaluate the effect of the plug-tube interference fit on closure leakage, the plugs were assembled using 0.003 to 0.004-in. interference calculated from conventional air gage measurements. The actual interferences, based on internal comparator data, ranged from 0.0026 to 0.0044 inch. The assembled membranes were submitted for hydrogen leak testing with the closure portion of the assembly at 1400°F and 2 atmospheres of hydrogen pressure.

F. IMPROVED CLADDING DEVELOPMENT

1. Summary

The objective of the subject program is the development of an improved cladding material for SNAP reactor fuel elements. The cladding materials presently being studied are Hastelloy N, Hastelloy X, type 304 stainless steel, and type 316 stainless steel. The major effort during the quarter was expended on determining the enameling properties of these materials. Hastelloy N requires a special chromizing treatment to obtain excellent adherence with SCB-1 barrier material. Hastelloy X develops excellent adherence with this material during the SCB-1 firing cycle. However, stainless steels types 304 and 316 did not exhibit acceptable adherence with SCB-1 but did develop excellent adherence with AI-8763D barrier material, which is fired at 1900°F. An oxidation kinetics theory has evolved from these tests to qualitatively predict the adherence of the glass enamel materials to metallic substrates. Based on this theory, good adherence can be obtained when the firing temperature of the glass enamel is near the maximum temperature at which the alloy substrate exhibits good oxidation resistance.

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In addition to coatability characteristics, ductility is of substantial importance in the development and evaluation of advanced cladding materials. In order to obtain information on the ductility of "aged" Hastelloy-N cladding tubes, creep-rupture deformation measurements were made on 10 S8ER-type elements which had been aged for 10,000 hours at temperatures of 1300°F or 1400°F in the SNAP 8 fuel element endurance testing program. The tubes were creep-rupture tested at 12,000 psi and 8,000 psi. Metallographic examination showed that the membranes with the highest ductility had significantly lower numbers of precipitates in the grain boundaries than the membranes with the lowest ductility. In addition, larger numbers of precipitates were found in the samples aged at 1300°F than in those aged at 1400°F. This accounts for the difference observed in the deformation to failure for the two groups of tubes.

2. Status

A review of the future requirements for SNAP claddings revealed: (1) the cladding material should be capable of withstanding a temperature of 1600°F for 20,000 hours and still retain a ductility of 1% strain-to-fracture, (2) the material surface composition must be coatable and provide a high coating-cladding adherence without affecting the high-temperature properties, (3) the material should have a low thermal expansion to minimize the annulus between the fuel and cladding, and (4) the material must be compatible with NaK. The cladding materials being studied are Hastelloy N, Hastelloy X, and types 304 and 316 stainless steels. Specification of nuclear-grade low-cobalt Hastelloy X-280 was found to be unnecessary because neutron absorption by normal Hastelloy X in a SNAP reactor spectrum is quite tolerable. In addition, Hastelloy X-285 (a special heat of Hastelloy X-280) was eliminated from the program because it requires a preoxidation treatment in order to achieve acceptable adherence of SCB-1, whereas Hastelloy X material develops excellent adherence without this special pretreatment.

The major effort this quarter was spent on identifying the materials characteristics which promote adherence. Experiments performed to determine the coatability of stainless steels types 304 and 316 have shown SCB-1 to have poor adherence to these alloy substrates, whereas AI-8763D was shown to have excellent adherence. To determine whether the difference in adherence was caused

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by the difference in chemistry of the barrier materials, the stainless steel tubes were enameled with the first coat of AI-8763D and the second coat of SCB-1. These tubes also exhibited poor adherence, the reason for this behavior being probably associated with the oxidation resistance of the substrate materials. Both of these stainless steels have good oxidation resistance in air at temperatures up to 1700°F, but scaling of the oxide will occur at markedly higher temperatures, or when the atmosphere becomes strongly oxidizing. The poor adherence of SCB-1 to these stainless steels is probably due to the oxidation characteristics of the alloys at the firing temperature of SCB-1 (2150°F). The excellent adherence of AI-8763D is obtained because of the improved oxidation condition at 1900°F, the firing temperature of AI-8763D.

The processing history of Hastelloy N also illustrates the same relationship between adherence and oxidation condition. Hastelloy N has good oxidation resistance at temperatures to about 1800°F. Chromizing the surface should increase the oxidation resistance temperature. The amount of chromium applied would determine the maximum oxidation temperature. Changing the hydrogen barrier material from AI-8763D to SCB-1 necessitates a change in the enamel firing temperature from 1900 to 2150°F. This increase in firing temperature led to a special chromizing requirement. The chromizing process raised the oxidation resistance temperature of Hastelloy N to a level compatible with the firing requirements for the SCB-1 enamel. Likewise, the excellent adherence that develops during the firing cycle of SCB-1 on Hastelloy X is the result of the oxidation properties of Hastelloy X at 2150°F.

In applying this concept to find a stainless steel that would be compatible with the SCB-1 firing cycle and would not need a special surface treatment, selection would be made of those stainless steels with oxidation resistance temperatures above 2000°F. The high chromium-high nickel stainless steels such as types 309 and 310 are in a favorable position with regard to this property and therefore should exhibit excellent adherence characteristics with SCB-1 glass enamels.

In addition to the tests to determine the enameling characteristics of various selected alloy substrates, a series of 10 specification-approved S8ER-type fuel elements were obtained from the SNAP 8 fuel element endurance testing program. These elements were exposed to various endurance testing inputs for 10,000 hours

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at test temperatures of 1300°F or 1400°F. Biaxial creep rupture tests were initiated on these tubes in order to obtain some preliminary information on the effect of thermal aging on the creep rupture characteristics of S8ER-type cladding.

After removing the fuel rods from the cladding tubes, modified end plugs with attached pressure capillary tubes were fitted to the unfueled cladding. Five of the ten tubes were creep-rupture tested at 12,000-psi calculated hoop stress, while the remaining five were tested at 8,000-psi hoop stress. Hydrogen gas was used to produce these stresses; the external environment was vacuum. Both the external pressure and the internal tube pressure were monitored continuously. This technique allowed tube ruptures to be detected at the instant of failure. Pre- and post-test diametral measurements were obtained on each tube and these values, along with the testing time, were the basis for determining rupture deformation and average diametral creep rate data.

The results of these tests, together with results obtained from similar tests on "as-processed" cladding tubes, are presented in Table 18. As shown, the rupture ductility and rupture time for the tubes tested at a stress level of 12,000 psi are significantly reduced due to thermal aging.

TABLE 18
SUMMARY OF BIAxIAL CREEP RUPTURE DATA OBTAINED FOR
AGED S8ER-TYPE CLADDING TUBES

Sample No.	Hoop Stress (psi)	Pre-test Conditions	Average Diametral Deformation (%)	Average Diametral Creep Rate (% hr x 10 ⁻³)	Time to Rupture (hr)	Type of Rupture
4 unnumbered samples	12,000	As-Processed	3.5 to 4.5	6.00 to 7.00	500 to 800	Longitudinal slit
814	12,000	10,000 hr at 1400°F	2.10	7.20	288	Longitudinal slit
884	12,000	10,000 hr at 1400°F	1.07	3.80	278	Longitudinal slit
940	12,000	10,000 hr at 1300°F	1.55	4.76	325	Longitudinal slit
993	12,000	10,000 hr at 1300°F	0.87	4.80	180	Longitudinal slit
996	12,000	10,000 hr at 1300°F	0.92	5.10	180	Pin hole
870	8,000	10,000 hr at 1400°F	2.08	1.80	1150	Pin hole
918	8,000	10,000 hr at 1400°F	0.75	0.55	1350	Longitudinal slit
1008	8,000	10,000 hr at 1300°F	0.49	0.34	1410	Pin hole
990	8,000	10,000 hr at 1300°F	0.97	0.59	1640	Pin hole
1033	8,000	10,000 hr at 1300°F	0.89	0.47	1890	Several longitudinal slits

Note: All tubes whether aged or as-processed were fabricated from heat Y-8437 and were coated with AI-8763D ceramic barrier material.

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Average diametral creep rate values obtained for the aged tubes at 12,000-psi stress are nominally the same as the values reported for tubing in the "as-processed" condition. As indicated in Table 18, two tubes had a rupture deformation of about 2%, which is significantly higher than that exhibited by the remaining aged tubes in the series. Metallographic examination revealed that the number of precipitates found in the grain boundaries of these two tubes was significantly less than for those tubes exhibiting lower rupture ductilities. These data also show the rupture ductility of the tubes aged at 1300°F to be lower than that obtained for similar tubes aged at 1400°F. Metallographic analysis of these tubes revealed generally larger amounts of precipitate in the grain boundaries of the tubes aged at 1300°F, compared to tubes that were aged at 1400°F. It is important to note, however, that these observations are qualitative, and more tests are needed to determine quantitatively the effect of thermal aging time and temperature and various endurance test inputs on the biaxial creep rupture properties of SNAP cladding materials. A typical example of the precipitates formed in the grain boundaries of Hastelloy N during aging is shown in Figure 28.

Figure 28. Typical Photomicrograph Showing Precipitates Found in Hastelloy-N Cladding After Aging (This sample, No. 918, was aged 10,000 hours at 1400°F)

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G. FUEL-BARRIER INTERACTION STUDIES

1. Summary

During the course of fuel element permeation studies in FY 1965 under the SNAP 8 program, it was observed that the presence of a zirconium hydride fuel rod in the cladding caused an increase in the rate of hydrogen leakage from the elements as compared with that for the same cladding tested as a membrane. The increase was found to be reversible (removing the fuel rod from the cladding resulted in return of the leakage rate to approximately that of the cladding without a fuel rod) and appeared to be as large as at least a factor of three to five in total hydrogen loss rate. Based on this and other related observations, the present program was initiated (1) to study and understand the cause for the fuel-barrier interaction, and (2) to devise methods for suppression of the interaction and its related effect.

Because of the work performed in FY 1966 under the SNAP Reactor Improvement Program, a shift in the emphasis for this study from Phase 1 to Phase 2 has occurred. This shift has resulted from the generation of information supporting the oxygen transport theory as the cause for the fuel-barrier interaction. According to this theory, zirconium in the fuel rod is a very large sink for oxygen. The very dry hydrogen gas from the fuel rod reacts with the silicate glass enamel to form water vapor. The water vapor formed as the result of this reaction migrates in the vapor phase to the fuel rod, where it is decomposed and the oxygen is absorbed by the zirconium. The presence of the oxygen sink prevents an equilibrium water vapor concentration from being reached. It is believed that this mechanism results in the removal of small amounts of oxygen from the glass and causes a reduction in its hydrogen retention properties.

The remedy being studied under the second phase of this program is the isolation of the fuel from the barrier by a "molecular barrier" to prevent the oxygen transport phenomenon from occurring. Two experiments are in progress toward this end. The first involves five fuel elements with the fuel rod "canned" in 0.010-in.-wall type 304 stainless steel. These elements will be endurance-tested at 1400°F for up to 20,000 hours. The second experiment is designed to determine the kinetics of the fuel-barrier interaction by insertion and withdrawal of

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the fuel rod from the test membrane without disassembly of the test sample. Various modifications of the fuel rod such as canning, electroplating, and/or oxidizing the fuel surface will also be studied as required.

2. Status

The sliding fuel equipment was modified to permit study of the kinetics of the fuel-barrier interaction and to determine the worth of a molecular barrier. The fuel rod was canned in a 10-mil-wall, stainless steel tube. A test membrane was fabricated using SCB-1 hydrogen barrier material applied to an oxidized Hastelloy X-285 tube. The test sequence was:

- 1) Determine the permeation rate of the membrane without fuel, using bottled hydrogen;
- 2) Determine the permeation rate with the stainless steel clad fuel inserted in the membrane;
- 3) Determine the rate of increase of the permeation rate as a function of time with bare fuel in the membrane; and
- 4) Determine the decrease in permeation rate as a function of time after the fuel rod was withdrawn and as a function of the amount of oxygen admitted to the system.

The permeation flow rate was 0.1 cc/hr at 1400°F and 2 atm hydrogen pressure when no fuel rod was present in the membrane. The flow continued at this rate during testing even after the canned fuel rod was inserted in the membrane. The rate increased to 5 cc/hr after the canned fuel rod was replaced with a normal bare fuel rod, as shown in Figure 29. The bare fuel rod was withdrawn from the hot zone of the system and air was permitted to enter the membrane while at temperature. The permeation rate decreased after each introduction of air to the membrane until a permeation rate of 0.7 cc/hr was measured, thus indicating a significant healing of the ceramic barrier to have taken place. At this time, pure oxygen was admitted to, and left in the system over the weekend. Following this treatment, the permeation rate was measured at 0.5 cc/hr. An additional oxygen treatment did not change the permeation rate.

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Figure 29. Hydrogen Permeation Increase Caused by Fuel-Barrier Interaction

During these experiments, measurements were made to determine the pressure dependence of hydrogen permeation. These data indicate molecular diffusion of hydrogen for virgin and healed barrier coating and partial atomic diffusion for the barrier after the fuel-barrier reaction has occurred. Additional tests are in progress to determine the change in the activation energy for permeation resulting from the fuel-barrier interaction.

Another experiment which is being performed under the second phase of this program will utilize a "duplex" cladding configuration and will determine the long-time permeation changes as the result of the molecular barrier for a complete fuel element. Five fuel elements are being built with the fuel rod clad in a 0.010-in.-wall, type 304 stainless steel tube which has an outside diameter of 0.540 inch. Three fuel rods will be canned in the bare SS tubing; the remaining two fuel rods will be canned in stainless-steel tubes which have been coated with AI-8763D hydrogen barrier material. These tubes are being coated to prevent possible reaction between the fuel rod and the stainless-steel molecular barrier in these two elements. These stainless-steel clad fuel rods will then be canned in a reference S8DS Hastelloy-N membrane. At present, this experiment has been temporarily delayed because problems in the chromizing of Hastelloy N limited the availability of the external cladding assemblies.

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V. COMPONENT DEVELOPMENT

A. IMPROVED ELECTRONICS

1. Introduction

Work continued on the development of improved electronics for SNAP reactor instrumentation and control applications. Preliminary development plans were formulated last quarter for improvement in controller technology, control parameter sensor development, and improvement in temperature sensor technology. These development plans define the tasks leading to development of the technology for improved reactor control systems utilizing the basic SNAP 8 reactor design for operation at 1300°F with a lifetime of five years. Development schedules are based on demonstration of the adequacy of the technology by application to a specific system design in about three to four years.

Study of the tasks defined by this effort has been utilized to formulate the following specific objectives for the two programs funded under the FY 1966 SNAP Reactor Improvement Project:

- 1) Core-Semiconductor Circuit Development
- 2) Power Switch Circuit Development

The SNAP 10A controller utilized solid-state transistor logic circuits to develop the control-drum actuator stepping sequence signals necessary for reactor startup and temperature control. Output stages of the controller utilized electro-mechanical relays as the final switching device. The present SNAP 8 design controller utilizes core-transistor logic circuits to obtain increased reliability by reduction in the number of active components subject to radiation-induced degradation. Utilization of semiconductors in circuits such as this minimizes the effect of component degradation.

The SNAP 8 controller continues to utilize relays to perform the output power switching function. These electromechanical elements of the controller are the fundamental components determining the reliability of the controller and the controller lifetime. Alternative power switching circuits which employ solid-state circuit elements are subject to radiation damage. For some reactor applications,

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however, a reduced radiation environment is provided at the location of the controller so that completely static controller switching circuits may be employed. For this reason considerable emphasis has been placed on the evaluation of these alternative circuits for reactor control applications.

The specific objectives and status of the two programs active in FY 1966 are described in the previous quarterly progress report.*

2. Status

A review of the results of radiation test data on prototype modules and components was completed during the reporting period. The results of the review indicate that the basic logic modules are quite resistant to the effect of radiation up to the SNAP 8 design levels. The pulse-former network following the oscillator module was the first circuit to fail at a total integrated dose of approximately 10^{14} nvt of fast neutrons. A concurrent review of new developments in power transistors indicated that suitable thin-base power transistors are now available from several manufacturers with nuclear radiation tolerances in the controller switching application which approach the tolerances of the remainder of the previously radiation hardened modules. The design of power switching circuits utilizing thin-base power transistors was initiated and a purchase order was placed for sufficient transistors to fabricate power switching circuits for a controller.

A design review of existing module circuits was initiated to determine where improved designs are necessary to enhance module performance and design margins.

B. HIGH-TEMPERATURE BEARINGS

The high-temperature bearing program is scoped to develop bearings for operation at 70 to 1500°F in deep space for as long as five years. The most demanding applications are those for self-aligning journal bearings which operate at slow speeds and endure long dwell periods. The technology gained from this development will guide design for the bearing needs of future generations of SNAP reactor control systems.

*L. M. Maki, "Progress Report, SNAP Reactor Improvement Program, October-December 1965," NAA-SR-11754 (CRD), February 15, 1966

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The test bearing design described previously* has been completed. The friction couple of the bearing is alumina vs carbon-graphite, the substrate for the alumina coating being tantalum-based 10% tungsten (Ta-10% W) alloy. These materials were selected on the basis of previous SNAP development and with consideration of 1500°F environments. Two 1-in.-diameter shaft samples of Ta-10% W were successfully coated with alumina using the detonation-gun application process. The alumina coating will be ground to the design thickness, and high-temperature testing of these samples will be accomplished before fabrication of the actual bearings.

The bearing test fixture is presently being designed. The test will operate in an existing 2400 liter/sec ion pump system which has a chamber working area 20 in. in diameter by 16 in. long. This system operates free from oil and obviates oil contamination from chamber or pumps. A kinematic schematic of the test fixture is shown in Figure 30.

The test system requirements and schematic description are as follows:

Test time (hr)	10,000 (continuous operation)
Bearing temperature (°F)	70 to 1600
Thrust load (lb)	0 to 140
Radial load (lb)	0 to 50
Misalignment angle (°)	0 or 2.5
Environmental pressure (torr)	10^{-8} or less

Schematic Description [○ symbols refer to Figure 30]

- 1) Bearing thrust loads up to 140 lb are provided by the variable dead weight ①.
- 2) The fixture radial-thrust bearing ② and the fixture radial bearing ④ have material couples of Purebon P-5N vs Al_2O_3 (Linde LA-2).
- 3) The test bearing housing ③ has a rigid vertical arm for measuring the bearing reaction torque. The housing material is Ta-10% W.

*Ibid.

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Figure 30. SRIP Bearing Test Fixture Schematic

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4) The coupling (5) is stainless steel. The test shaft is Ta-10% W and has a rigid coupling at (14). The magnetic drive shaft is stainless steel. The magnetic drive (6) is a Varion 954-5039, with modified bearings per S8DS fixture design. Torque rating of the Varion drive is 72 lb-in.

5) The load bellows feedthroughs (7) and (16) are bidirectionally constrained to prevent collapsing of the bellows by differential pressure in case of load loss, and to prevent pullout in case of cable failure inside the chamber. This bellows design has proven successful on long vacuum tests.

6) The torque pickup lever (8) is stainless steel in the bellows (9) area to facilitate weldment. The ball bearing bellows trunion rotation center is at the bellows convolution midlength point.

7) The (3) (8) (9) and (10) lever ratios are optimized for transducer-bearing torque accuracy. Maximum design bearing torque is 40 lb-in. below 800°F, and 20 lb-in. above 800°F. Normal operation will be approximately 3 in.-lb above 800°F. Transducers or transducer linkages are changeable to give midscale operation.

8) The radial load, up to 50 lb, is provided by the variable dead weight (11) adjusted for atmosphere bellows force.

9) The thrust load harness (12) consists of three wires 120° apart leading from the bearing housing to a plate. The three wires are as close to the shaft axis as is practical.

10) The radial load linkage (15) is a single-tree arrangement of bands.

11) Tantalum or Ta-10% W is stress relieved where very close accuracies are required.

12) The heater (13) is designed to maintain the test fixture at temperatures up to 1600°F.

13) Thermocouples are tantalum sheathed.

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C. HIGH-TORQUE ACTUATOR

The control of SNAP reactor systems, accomplished by varying the neutrons that are allowed to escape through the external reflector, is in the present SNAP 2, 10, or 8 systems effected by rotating drums, each coupled to an electromechanical drive system.

The actuator is the prime mover of this drive system, taking the electrical signal from the controller and transforming it into mechanical motion to move the reactor control elements. Previous actuators have been developed to meet the needs of the individual system as these needs were defined. A high-torque actuator (advanced actuator) program is now underway to develop a standardized unit which will meet the needs of the existing SNAP systems, and of the future systems presently visualized, as well.

Present systems require drum motion of $1/4^\circ$ arc increment, with torques from 10 to 17.5 lb-in. at the drums, and temperature capabilities to 1025°F . The dynamic performance parameters are obtained by an intermediate transformation device (simple gears) through which the actuator supplies the torque and angular motion. Performance of the actuators has been excellent throughout the stringent environments imposed.

1. Advanced Actuator

Future reactor systems may require performance values far exceeding the capabilities of existing actuators, and, in addition, may impose environments even more severe. The new prime movers needed cannot be made available solely as extensions of the past work, new configurations and approaches are necessary to meet the requirements. These requirements have been consolidated into an actuator performance specification which establishes the environmental and performance requirements for an electromechanical control drum actuator to be used as a high-torque stepper motor for the stepping, positioning, and holding of compact reactor control drums. A summary of these requirements was furnished in the previous quarter progress report.*

*Ibid.

a. Envelope Limit Study

The previous progress report* discussed a comparison of the performance of three classes of actuators. It was necessary to bracket the possible performances in order to determine the distance from target and, from this, to plan an effective program for a suitable actuator. The envelope chosen for this early study was arrived at as a result of a preliminary analysis. As reported, a rigid analysis was necessary and had been started; this study has now been completed.

The scope of the above study was to determine the maximum practical and permissible magnitudes for actuator diameter and length based on a layout of advanced compact reactor systems. The system layout used for this study is designated to be the reference design for a shadow-shielded manrated nuclear system (Figures 31 and 32). This system imposes severe restrictions upon the maximum allowable actuator envelope.

Further, actuator dimensions depend to a considerable extent on the type of coupling provided between the drive shafts of the actuator and the control drum. Seven ways of coupling and mounting an actuator have been considered. Although other alternatives regarding actuator integration may exist, the seven variants listed below may be considered preferred practical solutions. Coupling and mounting variants considered were:

Variant 1 — In-line with drum, direct mounting, see Figure 33.

Variant 2 — Actuator parallel to shadow cone with flex coupling, Figure 33.

Variant 3 — Actuator inclination intermediate to Variants 1 and 2 above with flex coupling, Figure 33.

Variant 4 — Actuator centerline offset from drum centerline, gear drive, see Figure 34.

Variant 4a — Same as Variant 4, but with actuator centerline parallel to shadow cone, gear drive, Figure 34.

Variant 5 — Same as Variant 4, but double-deck, Figure 34.

Variant 5a — Same as Variant 5, but with actuator centerline parallel to shadow cone, Figure 34.

*Ibid.

SCALE (INCHES)

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Figure 31. Manrated SNAP Reactor System with Shadow Shield (Sheet 1)

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SCALE (INCHES) 5 10 15 20 25 30 35 40

SECTION E

SECTION D

SECTION C

SECTION B
(SHOWN WITH CONTROL
DRUM ACTUATORS REMOVED)

SECTION A

SECTION F

SECTION G

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Figure 32. Manrated SNAP Reactor System with Shadow Shield (Sheet 2)

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Figure 33. Limits of Actuator Envelopes (Variants 1, 2, 3)

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Figure 34. Limits of Actuator Envelopes (Variants 4, 4a; 5, 5a)

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Figure 35. Envelope Limits
(8-Drum Systems)

Figure 36. Envelope Limits
(6-Drum Systems)

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Subsequent development of the actuator's diameter vs length relation for each of the above variants is based on a conservative core vessel closure as indicated in Figures 33 and 34. Space provision for a separate drum override device has been made throughout.

The detailed approach taken in developing compatible actuator dimensions for each of the seven variants is shown in Figures 33 and 34. Derived from this is the maximum permissible diameter for a given actuator length. Based on the preliminary electromagnetic studies, only Variants 4 and 5 indicate envelope configurations compatible with performance requirements. Table 19 shows dimensional results for Variants 4 and 5 (as scaled from Figures 33 and 34), together with associated magnitudes of shadow-cone diameter at the end of the actuator and approximate center distances for variants involving coupling by gears.

TABLE 19
ACTUATOR DIMENSIONS ESTABLISHED FROM VARIANTS 4 AND 5
(in.)

	Actuator Length, L	Maximum Actuator Diameter, D		Shadow Cone Diameter, D, at End of Actuator		Gear Center Distance, A (approximate)			
		8 Drums	6 Drums			8 Drums		6 Drums	
Variant 4 (Actuator center-line offset from drum centerline gear drive, see Figure 34)	2	4.12	4.93	15.2		0.60		1.00	
	4	3.91	4.68	14.5		0.71		1.09	
	6	3.70	4.43	13.7		0.79		1.17	
	8	3.48	4.17	12.9		0.85		1.20	
	10	3.30	3.95	12.1		0.92		1.27	
Variant 5 (Same as Variant 4, except double-decked)*	2	5.81	6.52	(D ₁ -Lower)	(D ₂ -Upper)	(A ₂ -Upper Set)	(A ₁ -Lower Set)	(A ₂ -Upper Set)	(A ₁ -Lower Set)
				15.24	14.26	1.70	1.47	2.07	1.83
				14.86	13.50	1.74	1.41	2.09	1.75
				14.48	12.74	1.78	1.36	2.11	1.63
				14.04	12.00	1.84	1.28	2.13	1.58

*Assumptions:

- 1) 1-in. clearance between OD's of adjoining actuators for lower set at point of minimum distance.
- 2) 1/2-in. clearance between nominal actuator end faces.

Figures 35 and 36 graphically represent the results for all mounting variants studied based on nuclear systems with 8 and 6 control drums respectively.

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The envelope study was conducted for both 6- and 8-drum systems. Since all advanced SNAP-type nuclear systems are planned on the basis of 8 reflector drums, however, the continuation of this effort concerning the actuator analysis is based on the 8-drum system only.

b. Envelope Application Study

The envelope limit study provided actuator envelopes as a function of space allotment, coupling, and general mounting concepts. The ultimate objective was to select the best actuator concept within the limits defined by the study and to include refinements for an optimum design. To establish the basis for this choice, it was necessary to try to fit the three types of actuators in the envelope limits and evaluate their performance.

This was done by first calculating the performance of the three types of actuators for one envelope and then, by the ratio method, obtaining a range of envelopes. These were superimposed on the results illustrated in Figures 35 and 36. An actuator whose superimposed area lay to the left of a variant line was considered a contender. The greatest flexibility possible was thus preserved regarding coupling and mounting of the advanced actuator onto a reflector system.

The following presents the results of the electromagnetic performance study made to evaluate the classes of reluctance-type actuators which could fit into one or more of the variants for the 8-drum system, and outlines their associated restrictions.

Figure 37 shows the results of the study for the heteropolar model. These were obtained by the ratio method from the tested performance data of the S8DS actuator, also a heteropolar device. Thus, the actuator concerned for this case roughly represents a scaled model of the S8DS actuator. No points within the limits of Variants 3 and 1 are shown, past experience and preliminary calculations having demonstrated the impracticality of designing an actuator in the small diameters (less than 3-1/4 in.) demanded by these two variants. Further, 1 and 3 are variants calling for direct drives. Applying the established study ground rules, a step size of 0.250° must be achieved with a minimum tooth and gap width of 0.020. This cannot be achieved for any of the actuator concepts within the diameter limits imposed by Variants 1 and 3. Variant 2 offers no advantage over Variant 4 and is thus eliminated.

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Figure 37. Minimum Heteropolar (S8DS) Actuator Dimensions to Meet Step Error Requirements

The curves for Variant 4 show that only with relatively high mmf's is it possible to use the heteropolar actuator. Two hundred (200) ampere turns/pole is insufficient to meet any of the variants. A minimum of 300 ampere turns/pole or 600 ampere turns/phase qualifies the heteropolar for use under Variant 4. For comparison, the S8DS actuator operates at 370 ampere turns/phase.

At the present time, it appears that a brake approximately the size of that of the S8DS actuator might possibly be used to fulfill the braking function, with some upgrading. It can be shown that the actuator should be as large as possible in diameter in Variant 4. Hence, the ideal design point would be a diameter of about 3.75 in. and its corresponding length about 5.4 in. (this point is shown by the large arrow). The brake can be scaled to a 3-in. diameter or one slightly larger, leaving ample length for design margin. In this region a 3-to-1 gear ratio is required.

For diameters smaller than 3.5 in. a 4-to-1 gear ratio is required. However, the curve shown (Figure 37) is approaching a vertical slope, indicating the approach of the lower limit of diameter. In addition, the length of the brake would tend to be limited here.

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Figure 38. Minimum Homopolar Actuator Dimensions to Meet Step Error Requirements

Figure 39. Minimum Modified Homopolar Actuator Dimensions to Meet Step Error Requirements

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Figure 38 shows study results for the homopolar actuator. These were obtained by basic calculation and checked by ratio method from the $1/2^\circ$ stepper test results. Essentially the same size as the heteropolar is called for, the diameter being 3.75 in. and the length 5.7 in.

A minimum of 500 ampere turns/phase is necessary to produce the desired operating results. The brake has ample length, as in the heteropolar device. The large arrow denotes what appears to be a good design point.

The homopolar model has an advantage over the heteropolar in the area shown (Variant 4), in that the target gear ratio of 2-to-1 can be achieved. This is due to the fact that in the same diameter as the heteropolar machine the teeth are closer to the periphery, thus making for smaller teeth and step size. Also, for a given number of gap ampere turns in a given volume, the torque is greater due to the larger radius arm.

For a given output and envelope, the homopolar model will use less power than the other contenders because of the utilization factors involved. The homopolar actuator has little, if any, unused space within the envelope. It allows more total coil wire volume, resulting in less power consumed.

Figure 39 shows the calculations for the modified homopolar actuator, a $1/2^\circ$ stepper type. No basic changes in configuration were considered, the only appreciable change being made in the length. The study results again were obtained by basic calculation and by ratio of the test results from the $1/2^\circ$ steppers previously built and tested. With a 2:1 gear ratio this drive begins to look attractive in a 5-1/4 in. diameter and 3-3/4 in. length, or a 5-1/2-in. diameter and 3-in. length package. The actuator requires about 20% fewer ampere turns than the heteropolar machine. Thus, at present, it appears that about 500 ampere turns are necessary to produce the desired torque.

An advantage in the modified homopolar envelopes is that the brake can be utilized to best advantage by internal positioning in the envelope, as opposed to an external brake for the other concepts. In addition, the brake tooth periphery is close to the outer diameter. The effect of diameter on the brake is similar to the effect of diameter on the stepper itself, i.e., the greater the diameter, the more the torque. This applies to braking torque as well as driving torque. Also, the tooth detent fabrication problems are reduced. A summary of envelope study conclusions is presented in Table 20.

TABLE 20
ENVELOPE STUDY SUMMARY

Parameter	Heteropolar	Homopolar	Modified Homopolar
	Variant 4	Variant 4	Variant 5
Gear ratio	3:1 or 4:1	2:1	2:1
Envelope	3.75 in. D x 5.4 in. L	3.75 in. D x 5.7 in. L	5 in. D x 4 in. L or 5.5 in. D x 3 in. L

Conclusions

- 1) Variants 1 and 3 represent a diameter limitation too small to be practical for any contending actuator, and are thus eliminated.
- 2) Variant 2 has a vertical slope limiting the diameter to a single rather small value; this variant is eliminated.
- 3) Variant 4a is very close to Variant 4, but since it is slightly to the left of Variant 4 in Figure 35 its diameter is somewhat restricted, as shown by the crossed lines in the figure. Thus, it is less advantageous than Variant 4 and is eliminated.
- 4) By comparison to Variant 5, Variant 5a is less acceptable for the same reasons as in the comparison of Variant 4 vs 4a, and is eliminated.
- 5) The homopolar actuator is more attractive for Variant 4 configurations than the heteropolar, because of the lower gear ratio.
- 6) The heteropolar actuator is relatively well developed in comparison with the other two concepts. The margin for increased knowledge is less than for the homopolar and the modified homopolar actuators. Most of the development problems associated with the heteropolar device are common to the other two concepts selected for further study.
- 7) The modified homopolar is the only suitable candidate for Variant 5 configuration.

c. Actuator Development Study

As a result of the envelope studies, conceptual layouts were developed for the homopolar and modified homopolar actuators consistent with the envelope assignments. The stepper motor was developed first; the brake was then developed. An analysis of each was performed, changes were made, and the process repeated. At the time of this writing it appears that all major design requirements can be met.

The following is a discussion of the two concepts:

(1) Homopolar Concept

(a) Structural Description (Actuator)

The homopolar actuator fits into an envelope 3.75 in. in diameter by 10.59 in. long. This envelope was developed by superimposing the results of a performance-vs-envelope study on the results of a study of actuator envelopes for several advanced compact nuclear reactors (see Figure 40). The length of 10.59 in. is an overall dimension, including a brake that is of smaller diameter. The "setback" allows use of this length. The length of the actuator stepper itself is only 6.25 in.

The primary components of the actuator are the stepper assembly and brake. The stepper consists of four individual-phase assemblies mounted in line on the shaft. The shaft is supported by bearings in the end covers; the end covers are held in place by the stator halves which also serve as the housing. See Figure 41 for assembly sequence.

The phase assembly consists of two field segments press-fitted together with the phase coil between them. The phase assemblies are then press-fitted onto the shaft with spacers between them. Dowel pins and bolts are next installed to accurately align and retain the phase assemblies. The end covers with the bearings shrink-fitted into them are installed on the shaft and are held in place by a groove in the stator-housing halves, which are assembled last.

Close matching of the thermal expansion coefficient of the various components has minimized distortion and stresses due to differential thermal expansion.

(b) Operation

The homopolar actuator is designed to take a $1/2$ -degree step. This is accomplished by machining 180 teeth, 1° wide, with 1° -wide slots between them, in each phase of the rotor assembly and the stator. When the phase assemblies are installed on the shaft they are aligned in such a way as to advance the teeth of each phase assembly $1/2$ degree from the previous assembly. To step the actuator, the field coil of the phase assembly rotor, which is misaligned with the stator teeth by $1/2^\circ$, is energized. The energizing of the coil sets up a magnetic field in the phase assembly and stator. Torque is developed on the

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BRAKE	
MAG GAP	0.060 in
PULL	46.2 lb
NUMBER OF TURNS	350
WIRE SIZE	#21 AWG
WIRE MATERIAL	OXALLOY
CURRENT	4.77 amp
V _{AMB}	14.8
V _{I200}	53.3
V _{I200 AND 500 hrs}	69

MOTOR	
NUMBER OF TEETH	180-1° WIDE (0.027)
GAP	0.008 in
TORQUE, max	93 lb-in
TORQUE, DYN	58.6 lb-in
TORQUE, RATED	35 lb-in
STEP ERROR	0.123°
COIL	
NI REQUIRED	540
WIRE SIZE	#22 AWG
MATERIAL	OXALLOY
NUMBER OF TURNS	186
V _{AMB}	7.2
CURRENT	2.9 amps
V _{I200}	25.9
V _{I200 AND 500 hrs}	33.6

WT_(LESS BRAKE) = 14.5 lb

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Figure 40. Sectional View — Homopolar Actuator

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Figure 41. Assembly Sequence-Homopolar Actuator

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Figure 42. Modified Homopolar Actuator

shaft by the flux, causing rotor teeth and stator teeth to line up. Energizing the four phases in a series results in four $1/2^\circ$ steps.

When any of the field coils are energized the brake is released by energizing the brake coil. Upon completion of each $1/2^\circ$ step the brake coil is deenergized and the brake is applied by the brake spring.

(c) Performance

The torque developed on the rotor shaft of the homopolar actuator is a function of the rotor tooth position with respect to the stator tooth. The maximum or stall torque of 93 lb-in. is developed at the beginning of the step, i.e., when the teeth are mismatched by $1/2^\circ$. The torque decreases as the cosine of the ratio of actual angular displacement divided by the nominal step of $1/2^\circ$, so that at the end of the step, zero torque is developed. The homopolar actuator develops its rated torque of 35 lb-in. with a step error of 0.123° ; i.e., the torque developed is lower than the rated torque between 0.123° and the end of the step.

(2) Modified Homopolar Concept

(a) Structural Description (Figure 42)

The boundary of the Modified Homopolar Actuator (MHA) fits into an envelope 5-1/8 in. in diameter by 4-1/8 in. long. This design point has again been selected by superimposing the results of an electromechanical performance vs envelope study upon the results of an envelope study based on projected reflector systems for several advanced compact nuclear reactors. This technique of optimizing shows a narrow range of envelope sizes available between the extremes of 5-1/2 in. diameter by 3 in. long and 5.0 in. in diameter by 4-1/2 in. long.

The primary structural areas of the MHA are identified as stator core, stator segments, rotor assembly, brake, and end covers. The actuator assembly consists of four structurally identical quadrants. These quadrants are magnetically separated from one another.

The one-piece stator core is the structural backbone of the actuator. A set of four magnetically separated stator segments is required to complete the magnetic circuit at the outside of the rotor rim. The stator core is designed to accept and/or house bearing provisions, electrical and mechanical requirements

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Figure 43. Detailed Electrical Connections, Modified Homopolar Actuator

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of the brake, field coils, end covers, the rotor assembly, and termination provisions for solenoid leads. The stator core is fully symmetrical about a plane through the actuator's half-length. These design features, and the location of the bearings in the planes of the rotor rims, will help to minimize tendencies for geometrical distortion due to thermal gradients.

A stator segment, properly aligned, is doweled to each quadrant of the stator core. The resulting circular gap(s) between these members will accept the rotor rim(s). To facilitate fabrication of the rotor, a design approach separating the rotor rim from the rotor disk is employed. Further, for best overall magnetic efficiency of the actuator, a dual rotor design has been selected. Accurate alignment between the two rotors, as well as between the respective stator areas, is obtained by angular positioning of each rotor disk to its respective rim at assembly. One rotor disk is fabricated integrally with the shaft; the other disk is assembled onto the shaft and is aligned angularly with the shaft by the use of a precision spline. To minimize potential distortion, the thermal expansion characteristics of rotor rim and disk have been closely matched. Centrally located in each core quadrant is a field coil (see Figure 42, Section B-B). The approach taken for bringing out and terminating the leads of the field and brake coils is shown in Figure 43.

Shown in Section A-A of Figure 42 are two proposed brake designs. (The differential slot brake design shown in Section E-E is described separately on Page 101 since it is a suitable candidate for both actuator concepts.) The brake concept described here is referred to as a tooth brake (Figure 42, Section A-A). It derives its magnetomotive force from two circular brake solenoid coils. These coils are fully enclosed and encapsulated after installation. The mechanical portion of the brake consists of a toothed ring that is guided concentrically by a bellows assembly and engages a matching set of teeth integrally with the rotor disk. The bellows assembly provides high torsional rigidity for the toothed ring, thereby affording positive rotational reference for the rotor assembly. The brake is of fail-safe design. A helical spring engages the tooth brake at all times when the brake solenoids are deenergized. The primary reason for using a dual brake is to double the circular tooth pitch by alternately letting one brake ring engage while the brake teeth at the other side of the

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actuator contact each other at their truncated faces (see Figure 44). This approach is necessitated by the very small unit step of the actuator ($1/2^\circ$) and the requirement of maintaining a minimum practical tooth size (0.032 in. at brake OD). Because of the small size of these teeth, refinement of conventional machining approaches may be required, in order to specify exceedingly close machining and assembly tolerances.

Figure 44. Tooth Brake Layout, Modified Homopolar Actuator

(b) Operation

The modified homopolar actuator is designed to take a unit step of $1/2^\circ$. The rotor rim has 180 slot/tooth ($\Delta\alpha = 2^\circ$) divisions equally spaced (Figure 42, Section C-C). Each of the 4 stator quadrants has the same tooth geometry and angular pitch as the rotor. The set of teeth pertaining to each quadrant has, however, been machined in an angular position advanced $1/2^\circ$ ($= 1/2$ tooth width) with reference to the previous quadrant. Assuming that the field coil of one quadrant is energized, geometric alignment between the rotor and stator teeth results due to the magnetic flux (forces) passing through the rotor gaps. To take a step ($1/2^\circ$) the field coil of the adjacent quadrant is energized. In this quadrant at the beginning of the step the rotor-stator teeth are misaligned by the width of

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one-half tooth. These geometric-magnetic interactions result in the development of a tangential magnetic force at the rotor gap area, tending to bring about geometric alignment of the stator-rotor teeth. As a consequence, a torque about the actuator shaft is developed and a new position of equilibrium, advanced by $1/2^\circ$ (nominal) from the previous position, is established. Repeating this process, control of the rotor position is obtained by applying a programmed sequence of energizing pulses to the field coils. The brake is unlocked by energizing the brake coils whenever any field coil is energized. Upon terminating the sequence of steps the brake coils are deenergized, thereby causing the brake, under the effect of the brake spring, to return to the locked or normal mode of operation (Figure 42, Section A-A).

(c) Performance

The geometric-magnetic design features of the MHA concept cause the driving torque to be a function of the relative tooth position between rotor and stator. The maximum torque is developed at the beginning of a step, i.e., when the teeth show the maximum design mismatch of one-half of the tooth width. This torque is sometimes referred to as the stall torque, since any external torque exceeding it would overpower the actuator and cause it to yield in the direction of the load torque. The calculated stall torque for the MHA actuator is 91.5 lb-in.

The rated load torque for this actuator is 35 lb-in. The net driving torque of the actuator declines as the cosine of the ratio of actual angular displacement divided by the nominal step of $1/2^\circ \times 90^\circ$. Driving motion continues until the net driving torque, near the end of the full step, has declined to a value equal to the load torque. When the load torque equals the rated design torque of 35 lb-in., the resulting calculated step error is 0.125° . Lesser external torque loads will reduce this step error.

Design considerations for the tooth brake are governed primarily by low permissible stresses due to the 1200°F operating temperature, the small step increment, the holding torque required (35 lb-in.), and the requirement of minimum performance deterioration over 30,000 hours of operating life. While these factors also govern the design of the actuator's magnetic drive system, they appear to have a more restrictive effect upon the brake system design.

Two criteria on which to judge performance of a tooth brake have been established. The first one, designated as the "just-holding" condition, is defined to exist when the external load torque reaches a magnitude that just begins to separate the tooth disks against the spring force.

The other criterion, designated as the "zero-positioning" condition, is defined to exist when the net effects of spring force, tooth geometry, and friction produce a positive driving torque that effectively overcomes the angular load error and results in zero angular error of the rotor upon locking the brake. In either case, it has been conservatively assumed that no magnetic torques exist when and after the brake core has been deenergized.

Analytical expressions for both cases have been worked out and plotted parametrically. As an example, for the just-holding condition and considering no friction, a net spring force of 5 lb will provide a holding torque of 35 lb-in. with an included angle of 30°.

For the zero-positioning condition a net spring force of 11 lb will result in self-positioning for friction coefficients of $\mu \leq 0.20$ at end of life.

(d) Materials and Stresses (Figure 42)

1. Stator-Rotor Rim

The governing material considerations for the modified homopolar actuator are the magnetic properties of stator and rotor for the given operating environment.

Based on satisfactory performance of similar actuators the same material, i.e., Hiperco 27, is recommended for use in this design. Hiperco 27 remains quite ductile after annealing, shows creep properties satisfactory for this application (0.2% at 1200°F, 1500 psi, 3×10^4 hr) is capable of operating at high flux levels, and shows good dimensional stability (i.e., no phase changes) within the range of operating temperature. Normal operating stresses for both rotor rim and stator areas are very low (<700 psi).

2. Rotor Disk

The primary consideration when selecting the rotor disk material is a coefficient of linear expansion compatible with the same property of Hiperco 27.

Further, it is desirable that the disk material be nonmagnetic. The results of a review of candidate materials show that no alloy will meet both requirements at an operating temperature of 1200°F. Detail calculations on the effects of magnetic vs nonmagnetic rotor disks are in process.

At present, two candidate materials (both magnetic) are under consideration for the rotor disks - stainless steel, type 410, and Carpenter 636 (AISI-616), a stainless alloy similar to SST-420. Final choice will depend on detail design considerations and on the results of the magnetic analysis. Both alloys display a creep-rupture strength of about 7000 psi over 10,000 hours at 1200°F. The actual continuous expected stress level is very low (<1000 psi).

3. Brake Bellows (Figure 42, Section A-A)

The following considerations apply to the tooth brake concept. Based on past use and fabrication experience the bellows material recommended is Inconel 718. For the design dimensions shown, the following set of compatible data for the recommended bellows material is practical at 1200°F operating temperature.

Stock thickness (in.)	0.003
Length (neutral) (in.)	0.200
Stroke (in.)	±0.030
Number of convolutions	10
Washer type	Nested-ripple, single-ply
Axial spring rate (lb/in.)	120
Torsional spring constant (lb/in. / °)	2000
Maximum stress (psi)	2500
Expected cycle life (hr)	Over 10,000.

4. Brake Spring (Figure 42, Section A-A)

The following considerations apply to the tooth brake concept. The design spring force, determined previously, is 17 lb nominal. Using a corrected fibre stress of 23,000 psi at 1200°F, the following set of compatible spring data results:

Spring mean diameter (in.)	2.35
Wire diameter (in.)	0.177 (No. 7 AWG)
Normal travel (in.)	0.040
Normal design load (lb)	17 *

*11 lb estimated normal design load at end of life.

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Figure 45. Differential Slot Brake

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Maximum spring load when stepping (lb)	17.5
Number of active coils	6
Spring rate (lb/in.)	13.

(3) Differential Slot Brake (Applicable to Both Homopolar and Modified Homopolar Concepts)

To prove the feasibility of a differential slot brake, a preliminary stress-deflection analysis, based on curved beam theory, has been made. The design point suggested results in the following approximate parameter values.

Cross-section of flexural pin	$w = h = 0.050 \text{ in.}^2$
Maximum deflection force per pin	$P_1 = 0.45 \text{ lb}$
Radius at bend of flexural pin "a"	$= 0.075 \text{ in.}$
Deflection at end of pin	$\delta = 0.085 \text{ in.}$
Maximum tensile fibre stress	$\sigma_{\theta \text{ max}} = 35,000 \text{ psi}$
Net force required of spring	$F_{sp} = 19 \text{ lb}$
Estimated design spring force	$F_D = 28 \text{ lb.}$

In the differential slot brake concept, contrary to the tooth brake concept*, the initial magnetic force requirement when unlocking the brake is quite small. Therefore, a brake solenoid the same size or smaller than projected for the tooth brake concept may be used to overcome this relatively strong spring.

(a) Structural Description

Design features of the differential slot brake (DSB) are shown in Figure 45. Main components or subassemblies of the DSB are identified as the rotor assembly, the slot ring assembly, slot brake ring, the flexural pins, the bellows assembly, solenoid coil assembly, and the end cover.

(b) Operation

The differential slot brake depicted in Figure 45 is of a fail-safe design. The normal mode of operation is characterized by deenergizing the solenoid. Consequently, the brake spring engages the slot brake ring with a predetermined number of pins. The engagement motion of the slot brake ring is controlled

*See pages 95 through 99

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radially by the sliding bearing, and rotationally by the bellows assembly. The desired depth of engagement between slots and pins is controlled by circular shims.

The principal reason for advancing a brake concept which involves a differential or vernier design feature lays in the need for increasing the exceedingly small peripheral pitch (0.012 in. per $1/2^\circ$ at OD) resulting from the small actuator step. No positive locking tooth brake concept appears feasible at such a small circular pitch. The solution suggested is based on not making the angular tooth pitch at the engagement area equal to the actuator's unit step; instead, the actuator's unit step is set equal to the difference of angular pitch between two different systems of indices. As a result, the peripheral pitch at the area of engagement becomes independent of the (small) actuator step. However, in this brake concept, instead of locking action at each tooth the positive locking action occurs at only one point or at most a few discrete points on the brake's periphery.

At any point where, for the given actuator position, no engagement is to occur (i. e., where a pin does not match a slot), the pin must be free to "clear the way" for the system of slots moving towards the axial position corresponding to the brake being locked. Figure 46 shows the principal modes of operation for the brake concept considered. The design selected provides for 45 flexural pins and 48 slots in the slot brake ring assembly. Therefore, for any actuator position three flexural pins, equally spaced, will provide position locking action as shown on Figures 45 and 46.

Appropriate clearances at the engagement area assure that only the pins positioned for locking will engage the corresponding slots while all other pins are pushed out of the way.

(c) Performance

In contemplating a performance analysis on the differential slot brake, it is advisable to begin with an analysis of the elastomechanical behavior of the flexural

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Figure 46. Differential Slot Brake - Modes of Operation

pins. The results of a preliminary analysis are based on the curved-beam theory. Considering boundary envelopes for maximum stress, maximum force per pin, and range of deflection required, several choices for suitable design points remain. The design point suggested results in the following approximate parameter values:

Cross section of flexural pins	$\sim 0.037 \text{ in.}^2$
Bending radius of pin "a"	$\approx 0.070 \text{ in.}$
Maximum deflection force per pin	$P_1 \approx 0.25 \text{ lb}$
Deflection at end of pin	$\delta \approx 0.52 \text{ in.}$
Maximum tensile fibre stress (compressive)	$\theta_{\text{max}} = 40,000 \text{ psi}$
Net spring force required	$F_{\text{sp}} = 19 \text{ lb}$
Estimated design spring force	$F_D = 23 \text{ lb.}$

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The DSB concept does not require that the brake solenoid overcome spring force. The initial magnetomotive force of the brake solenoid may be small (~ 10 lb) and just enough to overcome friction. Consequently, the resulting brake solenoid may be relatively small, since the brunt of the spring force is balanced by the sum of deflection forces acting upon the flexural pins.

The accuracy of step error obtainable under full load with the DSB is 0.20° , or better. This value is based on electromagnetic positioning of the rotor to within a 0.12° or better of true position taken under load.

(d) Materials and Stresses

The differential slot brake design (Figure 45) suggests the review of three possible critical areas, viz, flexural pins, bellows assembly, and spring. The requirement for long-term performance (30,000 hr) at high temperature (1200°F) under radiation ($\sim 1 \times 10^{20}$ nvt) makes imperative the requirement for reliable information on the candidate materials' properties at these conditions. Limitation of past material research programs has not, however, allowed reliable determination of material properties for the more severe environment given. It is well known that materials properties such as stress-rupture level, creep, load relaxation, fatigue limit and others cannot, in general, be predicted analytically, nor can they be extrapolated beyond a very small range. For these reasons, it will be necessary to make estimates based on data obtained from tests conducted at lower temperatures; the attendant confidence for these estimates may well be much below levels customarily expected.

(e) Special Considerations

1. Friction Brake Concepts

Past experience with friction brakes has indicated the coefficient of friction to be extremely low with certain material couples; other couples are subject to wear and galling. For these reasons, a friction brake concept was not a prime consideration for this program.

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Developmental work on sliding couples has continued, nonetheless. At the time of this writing all available data on friction couples is in process of compilation. When this is completed, analysis of the data will be made for possible application to a friction brake. Should the analysis indicate a sufficiently high coefficient of friction, along with other desired properties, a friction brake concept will be considered.

2. Bearings and Prevention of Adhesion

The major problem area concerning the tooth brake concept is the possibility of self-welding. One planned remedy consists of an application of dry-film lubricant applied to the tooth area, suitably burnished. To prevent the eventual loss of dry film by sublimation, a running face seal at the actuator shaft is required.

In the alternate brake concept (differential slot brake) it appears that by appropriate detail design the possibility of adhesion can be minimized so as not to require a running face seal.

Two design approaches for bearings are considered. Shown in Figures 40, 42, and 45 are P-5N vs Al_2O_3 couple journal and/or thrust bearings. This approach to bearing design does not require dry-film lubrication.

An alternate bearing approach consists of substituting the graphite bearings with dry-film-lubricated journal bearings. In this case, the presence of adequate dry-film lubricant must be assured throughout the actuator's lifetime by an effective sealed design approach. It is anticipated that this can be accomplished by the previously mentioned approach of sealing the actuator and the dry-film coating of surfaces interior to the actuator; this would simplify bearing design. Attendant to this approach is considerable reduction of, and practically constant, radial and axial bearing clearance for the rotor.

2. Electromagnetic Analytical Technique Support - Air Gap Geometry Task

In calculations used to predict the performance of electromagnetic devices such as motors and actuators it is necessary to insert certain experimentally derived factors to account for flux fringing and other phenomena not amenable to rigorous analysis. Factors available in the literature must be experimentally verified to assure that previous assumptions made will apply to the geometry of the new design. Since the 1/2-degree step required by the high-torque actuator

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Figure 47. Experimental Actuator - Rotor and Stator Parts

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Figure 48. Experimental Actuator - Assembly

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Figure 49. Dynamometer Torque Test

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necessitates an extremely small tooth width, it is particularly essential that constants be experimentally derived since extrapolation from much larger widths would be hazardous. The fixtures used to obtain this test information are designed so that data can be obtained for a wide variety of geometry and can thus be applied to all contemplated actuators in the future. The result will be greater accuracy in the prediction of performance, thus requiring less safety margin in the design.

A major area of concern is the effect of slot, tooth, and gap size variation on torque. This can best be evaluated by physically producing the variations and measuring the effects. By incremental changes, a series of curves can be generated from which "K" factors may be obtained. With these factors applied to correct basic calculations, a more precise performance prediction will result.

Consistent with the plans for this program an "Air Gap Geometry Task" was setup, involving the manufacture of hardware and a test program. This phase of the task was concerned with the manufacture of stator poles and a rotor with different slots and gap sizes. These will be placed in an S8DS actuator frame for test.

A new rotor approximating the S8DS actuator rotor size was machined from Hiperco-27 stock. The rotor was made slightly oversize so that progressively larger air gaps can be obtained by grinding the OD. Slots were then cut around the periphery. The slots, divided into four groups, were manufactured with two groups of 0.020 in.-wide slots and teeth, and two groups of 0.040 in.-wide slots and teeth. Each group spans 90° of arc and each like group is 180° opposed to its twin. Figure 47 shows rotor configuration.

For the stator two pairs of poles were constructed. The poles are similar to S8DS actuator stator poles except that the number and size of slots match the ones previously described for the rotor. Figure 47 shows stator pole configuration.

The general test procedure is as follows: All the poles of an existing S8DS actuator will be removed. One set of the new poles will be installed 180° opposed. The new rotor will be assembled into place and the machine will be tested for torque vs input. This will be repeated for the other set of poles. Figure 48 shows assembly of parts in frame. Figure 49 shows torque test by dynamometer.

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TABLE 21
SCOPE OF COIL MATERIALS TECHNOLOGY PROGRAM

Material Investigated	Materials	Temperature Range (°F)	Maximum Temperature (from information available) (°F)	Properties Monitored at Temperature	Post-test Examinations	Remarks
Encapsulant	AI-1 cement PBX cement	1200 to 1400	1100	Insulation resistance, coil to coil at 50 and 500 volts; effect of thermal cycles at beginning and end of test	Physical strength Cracking Compatibility	Look for change in resistance at temperature and compatibility with wire
Conductor	Copper Oxalloy Cube alloy Nickel-clad silver	1100 to 1400	900	Coil resistance; effect of thermal cycles at beginning and end of each test	Grain growth Chemical attack Tensile strength Ductility	Grain growth may be limiting factor; Met. Lab. analysis will be used extensively.
Bobbin	A. Inconel with 1) Mica 2) PBX 3) AI cement 4) Enamel as ground insulation B. Aluminum oxide	1200 to 1400	A. 1000 B. 1100	Insulation resistance from coil to ground at 50 to 500 volts; effect of thermal cycles at beginning and end of each test	Integrity of insulation on metal bobbin Compatibility Examine Al ₂ O ₃ bobbin for cracks	

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The rotor will then be removed, the diameter ground to increase the air gap, and the test procedure repeated. The rotor will be successively ground again and the test operations repeated. The procedure will then be repeated with a pair of S8DS poles and rotor. In each case, the same winding will be used. Shown below are the combinations to be tested (in inches):

<u>Slot Width</u>	<u>Gap Length (Approximate)</u>
0.060	0.008 (existing S8DS parts) to 0.011
0.040	0.005 to 0.008 to 0.011
0.020	0.005 to 0.008 to 0.011

The results of the test will show static torque vs input (ampere turns) for the above combinations, and an experimentally determined set of curves, showing the trend of torque with variation of air gap and slot size, will be derived. Knowledge of this is a key factor in the successful continuation of the Advanced Actuator Program.

3. Coil Materials Technology

The coil materials technology program includes both the development and evaluation of the individual constituents of a high-temperature coil. The main constituents are the encapsulant, conductor, and the bobbin. A test program has been established to define the limitations of service operation variables such as temperature, voltage, and thermal cycling on these subcomponents. An outline of the scope and status of this program is shown in Table 21; a description of the individual efforts follows:

a. Encapsulant

In electromagnetic devices, insulation is required to physically separate and electrically isolate adjoining turns. A technique of wet-winding has been developed at AI in which the conductor is pulled through a suspension of inorganic insulation as the coil is formed. Subsequent high-temperature curing of the insulation produces a rigid encapsulated coil. The general physical requirements for the insulation are (1) adequate electrical resistance, (2) physical integrity following curing, and (3) chemical inertness with respect to the bobbin and conductor.

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Figure 50. Typical Values of Insulation Resistance of Three High-Temperature Encapsulants at 1000°F and 10^{-8} torr

Figure 51. Typical Test Coils Wet-Wound with AI-1 Encapsulant

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The encapsulating materials which have been utilized in high-temperature components on the SNAP program to date are PBX and SAP II cements. Both of these materials satisfy (1) and (2) above, but have a tendency to attack the wire conductor due to their acidic properties. Though the degree of this attack is not severe, this condition should be eliminated in order to achieve ultimate temperature capabilities of the wire.

A new encapsulating material has been developed as part of the coil materials technology program. The main improvement in this new material, called AI-1 cement, is the reduction in acidity of the fluid suspension. The acidity is approximately 300 times less than that of the encapsulant (SAP II) employed in electrical devices on the SNAP 8 reactor. Tests indicate an absence of chemical attack on copper wire in samples immersed up to 72 hours. Initial tests have been conducted at 1000°F and 10^{-8} torr to compare the electrical resistance to both PBX and SAP II. The results, shown in Figure 50, indicate that the electrical insulating properties of AI-1 cement are similar to PBX and SAP II. Figure 51 shows two typical coil samples fabricated using the new AI-1 cement.

As indicated in Table 21, the scope of this investigation involves evaluation of AI-1 cement and PBX at temperatures to 1400°F. Tests are now being conducted at 1200°F. The material compatibility with the wire and bobbin will be assessed in post-test examination following each temperature evaluation.

b. Conductor

The great majority of coil wire used on all SNAP programs to date has been chrome-plated copper. This wire was obtained in a three-step process. The copper wire was purchased from Source A and sent to Source B for chrome-plating. After applying approximately 60 millionths of an inch of chrome, the wire was sent to Source C for application of glass insulation. Though difficulty of maintaining quality control is obvious, very few technical problems have arisen with this arrangement.

The chrome-plating was intended as a protective coating against chemical attack during coil winding. The cements employed have an acid binder which will attack copper wire while the encapsulating cement is fluid. Analyses of both coil test samples and wires ruptured in developmental units indicates that

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the chrome plating has not been altogether effective. Figure 52 shows a typical surface cracking of the chrome-plating. The cement then preferentially attacks the copper at the crack sites.

Although failures resulting from this corrosion have not been numerous, the presence of this condition reduces the ultimate strength of the wire. A program has therefore been initiated to evaluate other materials for the high-torque actuator (see Table 21).

Grain growth in copper becomes excessive at elevated temperatures. Figure 53 shows typical grain sizes in copper after two hours at 1300°F. The increase in grain size reduces conductivity and occasions possible weak spots in the conductor. As grain sizes approach the wire diameter, rupture can occur in flexure or in thermal cycling.

Figure 54 shows comparable grain size distribution in a stainless-steel-clad copper wire. This sample and the copper wire in Figure 53 were both exposed to 1300°F for two hours as part of a preliminary investigation of grain growth. The stainless steel serves to protect the copper core during encapsulation and also furnishes high-temperature strength. Tests are now being conducted at 1100°F and 1200°F to evaluate this conductor in long-term operation. Unknown parameters under investigation are the change in resistance of the copper due

Figure 52.
Corrosion of Copper
at Cracks in
Chrome-Plating

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Figure 53. Grain Size in 30 AWG Copper Wire
After Two Hours at 1300°F
(250X)

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Figure 54. Grain Size in 30 AWG Stainless-Steel
Clad Copper After Two Hours at 1300°F
(250X)

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to interdiffusion of elements from the stainless steel and the integrity of the wire in thermal cycling. The difference in thermal expansion characteristics of the stainless steel and copper may work a hardship on the conductor.

Metallurgical evaluation of all candidate conductor materials, based on grain growth and tensile strength, will be performed following each test. Compatibility of the wire and encapsulant will also be determined.

c. Bobbins

(1) Materials

Ceramic bobbins have proved successful in control-drum actuators at temperatures to 1025°F. Design considerations on the high-torque actuator dictate that a parallel investigation be conducted on metal bobbins and ceramic bobbins at temperatures to 1400°F. Preliminary actuator housing designs indicate that bobbin mounting can be more readily facilitated by using metal bobbins. Ceramic bobbins will be evaluated on a backup basis.

Inconel has been selected as the bobbin material for its high-temperature strength and absence of volatile elements which would sublime at 1400°F. These bobbins will require ground insulation. The materials which will be evaluated are given in Table 21. Tests have now been initiated at 1200°F on each of these candidate materials; ceramic bobbins will also be tested.

(2) Configuration

The initial design of the envelope for the homopolar concept of the high-torque actuator indicates that the coil geometry will differ from that in our previous experience. Typical metal bobbins for the homopolar machine have been fabricated, see Figure 55. These coils will be utilized to study problems which will be encountered in coil winding, coil termination, and other procedures associated with coil manufacture.

Figure 55. Prototype of Metal Bobbins for High-Torque Actuator