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Westinghouse Advanced Reactors Division

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MASTER

OXIDE FUEL ELEMENT DEVELOPMENT

QUARTERLY PROGRESS REPORT

FOR THE PERIOD ENDING DECEMBER 31, 1968

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CONTENTS

	Page
INTRODUCTION.....	1
Summary of Objectives.....	1
Summary of Current Progress.....	1
OFAA-110-PROJECT ADMINISTRATION.....	3
Objectives.....	3
Prior Accomplishments.....	3
Current Progress.....	3
OFAA-210-ANALYTICAL STUDIES OF OXIDE FUEL ELEMENT BEHAVIOR.....	4
Objectives.....	4
Prior Work.....	4
Current Progress.....	4
Analysis of Stainless Steel Swelling Data.....	4
Evaluation of Fuel Rod Performance.....	8
Sodium Logging.....	8
Factors Affecting the Creep Strength of Type 316 Stainless Steel.....	14
Thermo-Mechanical Effects.....	14
Solid Solution Effects.....	19
OFAA-310-OXIDE FUEL IRRADIATION TESTS.....	24
Objective.....	24
Subtask A - Encapsulated Oxide Fuel Pin Irradiation Tests.....	24
Subtask B - Unencapsulated Pin Subassembly Tests.....	24
Prior Work.....	24
Subtask A - Encapsulated Oxide Fuel Pin Irradiation Tests.....	24
Subtask B - Unencapsulated Pin Subassembly Tests.....	25
Current Progress.....	25
Subtask A - Encapsulated Oxide Fuel Pin Irradiation Tests.....	25
Void Deployment Program.....	25
Fuel Spacers.....	26
Subtask B - Unencapsulated Subassembly Tests.....	35
OFAA-410-FABRICATION DEVELOPMENT OF EBR-II SUBASSEMBLIES.....	41
Objectives.....	41
Prior Work.....	42
Current Progress.....	42
Inconel 718 Grid.....	42
Effect of Brazing Thermal Cycle on the Mechanical Properties of Inconel 718.....	42
Sodium Corrosion Testing.....	42
Development of Subassembly Inspection Techniques.....	46
Type 316 Stainless Steel Grid.....	46

CONTENTS (Cont.)

	Page
OFAA-510-Fabrication of Oxide Fuel Pins.....	47
Objectives.....	47
Prior Work.....	47
Current Progress.....	48
Program Planning.....	48
REFERENCES.....	52
EXTERNAL DISTRIBUTION LIST.....	54

FIGURES

Figure	Title	Page
1	All Available Type 304 Stainless Steel Swelling Data.....	6
2	Type 304 Stainless Steel Swelling (Normalized to 5×10^{22} by $\Delta V_a (\phi t)^{1.6}$).....	7
3	Stress vs. Secondary Creep Rate of Annealed Type 316 Stainless Steel at 1200°F.....	15
4	Comparison of Secondary Creep Rates of Annealed and 20% Cold-Worked Type 316 Stainless Steel at Various Temperatures.....	16
5	Stress Required to Produce Secondary Creep Rate of 10^{-5} Hr^{-1} for Annealed and 20% Cold-Worked Type 316 Stainless Steel.....	17
6	Stress vs. Secondary Creep Rate of Annealed Type 316 Stainless Steel at 1100°F.....	20
7	Stress vs. Secondary Creep Rate of Annealed Type 316 Stainless Steel at 1100°F.....	21
8	Stress vs. Secondary Creep Rate of Annealed Type 316 Stainless Steel at 1300°F.....	22
9	Spacer-Cladding Conceptual Design.....	29
10	Spacer Temperature as a Function of Spacer-Cladding Conductance.....	31
11	Fuel Centerline Temperature as a Function of Spacer-Cladding Conductance.....	32
12	Axial Temperature Profile in Space Pin.....	34
13	Average Power vs. P/D Ratio.....	36

TABLES

Table	Title	Page
1	Creep Constant at Lower Stress Levels.....	11
2	Constants for the Relation $\ln A = B_1 + B_2 T + B_3 T^2$	12
3	Creep Constants at High-Stress Levels.....	13
4	Comparison of Secondary Creep Rates of Type 316 Stainless Steel.....	18

TABLE (Cont.)

Table	Title	Page
5	Chemical Analysis of Data Presented in Figures 6, 7, and 8.....	23
6A	Oxide Void Deployment Program 19-Capsule Subassembly.....	27
6B	Oxide Void Deployment Program Additional Capsules.....	28
7	Effect of Fuel-Cladding Conductance on Maximum Spacer Temperature	33
8	Cladding Temperatures in Preliminary 37-Pin Assembly Designs.....	38
9	Effects of Varying Grid Strap Thickness and Total Number of Grids (Dimpled Grid Length = 0.5 Inch).....	39
10	Elevated Temperature Tensile Tests on Inconel 718 (0.016-Inch-Thick Sheet).....	43
11	Stress Relaxation of Inconel 718 (0.016-Inch-Thick Sheet)....	44
12	Creep Testing of Inconel 718 (0.016-Inch-Thick Sheet).....	45
13	Proposed Pin Fabrication Process.....	49
14	Process Variables Associated with the Fabrication of Mixed Oxide Fuel Pellets.....	50
15	Fabrication Process Material Analyses.....	51

INTRODUCTION

This is the second quarterly progress report and covers the work performed during the second quarter of fiscal year (FY)-1969.

SUMMARY OF OBJECTIVES

The overall program approach is to conduct parallel analytical and experimental studies on mixed oxide fuel pins with close liaison and feedback between the two activities. In the analytical work, the initial efforts will be directed to compiling all pertinent data on the irradiation behavior of austenitic stainless steel clad, mixed oxide fuel pins. A similar effort will be directed toward compiling data on the behavior of Type 316 austenitic stainless steel cladding - in particular on high-burnup fuel pins. These data will be analyzed by current theoretical models, and where required, new models and computer codes will be devised. These analytical techniques will be used to provide the technical basis for the experimental irradiation programs. This procedure, in turn, will permit reliable parametric studies of design variables of interest to FFTF and LMFBFR.

SUMMARY OF CURRENT PROGRESS

A summary of current progress is shown below. Detailed progress is reported in the individual task sections of this document.

Under Task OFAA-110, authorization for the FY-1969 scope of work and full FY-1969 funding were obtained. Formal approval of our FY-1969 work program and formal concurrence with our October request for Government furnished materials and services were also received. A thorough review and updating of program status and plans were conducted for the AEC's mid-year review.

Under Task OFAA-210, the analysis of stainless steel cladding swelling data continued. A concurrent analysis was performed by PNL, and the results are reported. A preliminary examination of the effects of a cladding failure on fuel behavior was completed. Discrepancies between the data reported on the creep strength of Type 316 stainless steel by various investigators were investigated and indicate that minor variations in composition or mechanical or thermal treatment can cause significant variations in mechanical properties and microstructure.

Under Task OFAA-310, preliminary nuclear and thermal design studies were conducted on a conceptual design of a fuel pin with fixed spacers compartmenting the fuel column. A spacer should provide more reproducible fuel column expansion characteristics during reactor start-ups and shutdowns. A change from a spring-finger design to the DFR/PFR design caused the grid material to be changed from Inconel to stainless steel. Design drawings and more detailed analyses of the 0.230-inch pin O.D. and 1.24 P/D ratio design incorporating Type 316 stainless steel dimpled grids in a 37-pin subassembly with 60 7/8-inch-long pins are proceeding.

Under Task OFAA-410, a decision was made to halt development studies on brazed Inconel 718 grids, and to re-direct the grid development program to a spot-welded Type 316 stainless steel grid, similar to that used in DFR and presently specified for PFR. Resistance spot-welding trials are planned to evaluate the problem of weld metal expulsion.

Under Task OFAA-510, a literature survey was conducted to provide a background reference work for the facility and to aid in the confirmation of the process parameters selected. This survey confirmed that the process which WARD intends to follow is consistent with current technology. The void deployment and restrictor experiments were discussed with representatives from PNL.

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OFAA-210 ANALYTICAL STUDIES OF OXIDE FUEL ELEMENT BEHAVIOR

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OBJECTIVES

The maximum utilization of expensive and limited irradiation test data is achieved by a continuing theoretical analysis of the data. The objectives of this task are to provide analyses of the effects of irradiation on deformation mechanisms, fracture mechanics, and structure of cladding materials and the inter-relationship of these processes with the stresses imposed by fuel swelling. The technical approach involves the development of theoretical models and appropriate computer codes. The basic objective of this program is to provide an understanding of the behavior of stainless steel clad, oxide fuel elements exposed to fast neutron flux.

PRIOR WORK

Regression analyses of all available cladding swelling data were performed, and the data were examined for the influence of cold working and carbon content on swelling. The effect of fluence on swelling in the temperature range 450 to 550°C was established as $\Delta V/V = 0.191 (\phi t)^{1.143}$, and confidence levels were rigorously calculated for this expression. The effect of nitrogen on post-irradiation cladding ductility was identified. A statistical analysis of United States (US) and United Kingdom (UK) creep data for unirradiated, solution annealed, Type 316 stainless steel was performed. It was shown that both the US and UK creep data at 1200°F can be considered to be from the same population. A fuel-swelling model based upon the Eyre and Bullough model was devised for both oxide and carbide fuel.

CURRENT PROGRESS

Analysis of Stainless Steel Swelling Data

The analysis of stainless steel swelling data reported in the last quarterly report has continued. The status at that time was that all Type 304 stainless steel and Type 316 stainless steel (annealed and cold-worked) data in the temperature range 450 to 550°C were considered together and assigned weighting factors according to their quoted accuracy of measurement. The fluence dependence was assessed by a least-squares method to fit the data to an equation of the form:

$$\Delta V/V = k (\phi t)^n \quad (1)$$

where: $\Delta V/V$ = percentage increase in volume
 ϕt = fluence for $E > 0.1$ Mev
 K, n = constants

This treatment yielded a fluence dependence of $n = 1.14$.

A concurrent analysis was performed by PNL. This analysis utilized the available Type 304 stainless steel data (PNL, ORNL, ANL, GE) with the exception of the low fluence - low swelling PNL data points which were rejected because of doubts regarding their validity.

The temperature dependence of swelling was examined by an Arrhenius-type relationship, and an equation of the following form was proposed to describe swelling as a function of fluence and temperature to $T \approx 550^\circ\text{C}$ (assumed to be the temperature at which swelling is a maximum).

$$\Delta V/V = K (\phi t)^n e^{-Q/RT} \quad (2)$$

where:

$\Delta V/V$ = percentage volume increase
 ϕt = fluence for $E > 0.1$ Mev
 T = temperature $^\circ\text{K}$
 R = gas constant $1.987 \text{ cal } ^\circ\text{K}^{-1} \text{ mole}^{-1}$
 Q = activation energy cals mole^{-1}
 K, n = constants

Two equations were reported by PNL:

$$\Delta V/V = 6 \times 10^{-34} (\phi t)^{1.6} e^{-10,200/RT} \quad (3)$$

$$\Delta V/V = 1.2 \times 10^{-34} (\phi t)^{1.6} e^{-7,000/RT} \quad (4)$$

Figure 1 shows all of the currently available Type 304 stainless steel swelling data. It can be seen that with the exception of the low fluence - low swelling PNL points, the data are well described by a fluence exponent of 1.6. Figure 2 indicates the temperature dependent part of cladding swelling at a fluence of 5×10^{22} . The points shown in Figure 2 were normalized to 5×10^{22} by use of the 1.6 fluence exponent. Equations (3) and (4) can be readily distinguished on Figure 2 by noting that the equation with the higher activation energy goes with the lower curve.

At the present time, PNL recommends the upper curve in Figure 2. For design applications, the upper curve would provide a conservative design basis. It is interesting to note that the preliminary cladding swelling data reported for solution treated Types 347 and 316 stainless steels by GE appear similar to that reported for Type 304 stainless steel.

Figure 1 All Available Type 304 Stainless Steel Swelling Data

Figure 2 Type 304 Stainless Steel Swelling (Normalized to 5×10^{22} by $\Delta V \alpha (\phi_T)^{1.6}$)

Evaluation of Fuel Rod Performance

A detailed analytical study is in progress on all available mixed oxide irradiation data. Particular attention has been directed to the limited data reported from DFR and the preliminary irradiation data reported by GE*. The preliminary results of this analysis were reviewed with the AEC in late November, 1968. The initial output of the analysis is the irradiation program defined in Task OFAA-310.

Two computer codes, FIGRO and CYGRO, are being run with input parameters obtained from actual experimental fuel pins irradiated in EBR-II. The CYGRO code has not yet yielded significant results, due primarily to difficulties in definition of proper input parameters. The mathematical models in CYGRO are being changed to improve the calculation procedure.

Sodium Logging

A preliminary examination of the effects of cladding failure on fuel behavior was completed. Consideration was given primarily to sodium logging effects. The objective of the limited study was to provide a basis for evaluation of the consequences of cladding failure and the preliminary definition of a cladding failure experimental program.

After a small cladding defect has appeared, it is possible for sodium to enter the fuel pin as a result of power cycling. Various modes of serious cladding rupture and detrimental effects on fuel can be envisaged upon further power cycling due to the presence of this sodium.

An analysis of the case in which the fuel pin is completely filled with sodium was made. This may be one of the less likely situations, but it does lead to the highest pressures generated, and the type of analysis is relevant to other situations. Pressure is generated by the considerable thermal expansion and low compressibility of the sodium. Using typical oxide fuel parameters, maximum pressures were calculated when the limitation to pressure relief is set by flow down the fuel/cladding annulus and by flow through a circular cross-section defect. Significant pressures (1000 psi) are generated only if severe restrictions are placed upon sodium flow.

Assuming sodium temperature rise to be exponential, it was shown that flow down the annulus rather than through a cladding defect can appear limiting. For a 10-minute power cycle, significant pressures are generated only when the fuel cladding gap approaches 0.1 mil. It is felt that this situation essentially corresponds to fuel/cladding contact which is expected in oxide fuel after relatively low burnup. Compared with this, the diameter of a hole in the cladding must approach 0.1 mil for high pressures to be generated in the 10-minute power cycle. The idea of such a hole of uniform cross section seems unrealistic and introduces difficulties in postulating how ingress of sodium is achieved.

* Preliminary data presented at 1968 Winter ANS Meeting.

The presence of a gas space in the plenum introduces a further pressure relief mechanism in the high compressibility of the gas. For a particular fuel pin design, it has been shown (for the assumed temperature rise in the plenum of 700°F) that significant pressure could appear if the initial gas volume was less than two cm³. Having introduced a plenum gas space, a further pressure-generating model was examined. Sodium at the fuel center was assumed to vaporize, and an expanding liquid/vapor interface within the fuel forces sodium into the plenum. As a limiting case, the defect is assumed plugged, and at equilibrium, the vapor pressure of sodium corresponding to the temperature of the liquid/vapor interface is exerted throughout the fuel can. Allowance for the effect of the void commonly found at oxide fuel centers was made. Significant pressures can occur if the volume of gas in the plenum at the start of power cycling falls below 1.5 cm³.

A simple addition of the two effects indicates that 3.5 cm³ or 16% of the fuel plenum volume is the region below which significant pressures are a possibility. Filling the plenum to 50% is possible due to gas contraction over the same temperature as before. To further fill the plenum, it appears that a series of unrealistic events must be postulated. The introduction of extra holes or their enlargement to assist some sodium filling process also assists pressure relief.

Consideration of pressures generated within the fuel is complicated by non-homogeneity from a density viewpoint. However, pressure generated within the center void was examined. The center void is considered initially full of sodium which vaporizes as a power cycle proceeds. Pressures exerted within the void are trivial even when the limitation to flow imposed is equivalent to a one mil-diameter hole. Irradiated fuels always show many radial cracks whose total resistance to flow is certainly less than such a hole.

Secondary Creep Rates of Type 316 Stainless Steel in the Annealed and 20% Cold-Worked State

The stress dependence of secondary creep rate $\dot{\epsilon}_s$ at low-stress levels for both metal and alloys is given by the power relation:

$$\dot{\epsilon}_s = A_s e^{-\Delta H/RT} \sigma^n \quad \text{or,} \quad (5)$$

$$\dot{\epsilon}_s = A \sigma^n \quad (6)$$

where A and n are independent of stress. At high-stress levels, the stress dependency of secondary creep rate is:

$$\dot{\epsilon}_s = A_s e^{-\Delta H/RT} \exp(B\sigma) \quad \text{or,} \quad (7)$$

$$\dot{\epsilon}_s = A \exp(B\sigma) \quad (8)$$

The existence of Equations (5) and (7) led to the belief that different mechanisms control secondary creep at high-and low-stress levels.

An abrupt change in mechanism is unlikely, and it has been shown that Equations (5) and (6) are satisfied by a single stress function.^[1]

$$\dot{\epsilon}_s = A_s e^{-\Delta H/RT} (\sinh \alpha \sigma)^n \quad \text{or,} \quad (9)$$

$$\dot{\epsilon}_s = A (\sinh \alpha \sigma)^n \quad (10)$$

A linear behavior for the entire stress range is predicted for a plot of $\log \dot{\epsilon}$ against $\log \sinh \alpha \sigma$.

Description of the secondary creep rate of Type 316 stainless steel over a stress and/or temperature range requires the constants A, n, α , and B to be specified as a function of temperature. Annealed and 20% cold-worked secondary creep rates of Type 316 stainless steel were fitted to Equation (5) by a least-squares method.^[2,3,4] The tentative A and n values for data at low-stress levels are listed in Table I.

The n values for solution-treated and 20% cold-worked Type 316 stainless steel are effectively independent of temperature for the temperature range investigated, 1000 to 1300°F. Rowe, Stewart, and Burgess also report constant values of n for annealed Type 316 stainless steel in the temperature range 1350 to 1650°F.^[5] Average values for n were taken while A varied according to the relation:

$$\ln A = B_1 + B_2 T + B_3 T^2 \quad (11)$$

The constants for 20% cold-worked and annealed Type 316 stainless steel are listed in Table 2.

The creep constants for 20% cold-worked Type 316 stainless steel are based upon the assumption that "softening", i.e., recovery and/or recrystallization of the cold-worked state will not occur. Palentine reports accelerated creep rates at 700°C exceeding those of annealed material, which is characteristic of material that underwent recovery and recrystallization.^[3] However, additional tests at longer times have not shown this effect.

Both 20% cold-worked and annealed Type 316 stainless steel creep rates at the high-stress levels, i.e. above the break in the log-log plot of stress versus secondary creep, were fitted to Equation (5) because Equation (7), though proving to be a good fit at the higher stress levels, did not accurately fit the data in the intermediate stress levels, i.e. the region between the break in the creep curve and the higher stress levels. Overall, Equation (5) proved to be a more satisfactory fit to high-stress data, especially for the cold-worked condition. The A and n values for 20% cold-worked and annealed Type 316 stainless steel are listed in Table 3. No

Table 1

Creep Constants at Lower Stress Levels

Annealed Type 316 Stainless Steel

Temperature (°F)	A	n	Stress Level (ksi)
1000	3.7787×10^{-21}	3.2211	≤ 30
1100	8.5866×10^{-21}	3.2075	≤ 25
1200	4.7944×10^{-20}	3.2142	≤ 20
1300	2.9189×10^{-19}	3.1660	≤ 11

20% Cold-Worked Type 316 Stainless Steel

Temperature (°F)	A	n	Stress Level (ksi)
1022	3.039×10^{-25}	3.9109	≤ 25
1202	2.0239×10^{-24}	3.9974	≤ 20
1292	9.1984×10^{-24}	4.1515	≤ 17

Table 2

Constants for the Relation $\ln A = B_1 + B_2 T + B_3 T^2$

Material Condition	B_1	B_2	B_3	N
20% Cold Worked	-38.6285	-4.1153×10^{-2}	2.3029×10^{-5}	4.0199
Annealed	-29.452	-4.253×10^{-2}	2.492×10^{-5}	3.2022

Table 3

Creep Constants at High-Stress Levels

Annealed Type 316 Stainless Steel			
Temperature (°F)	A	n	Stress Level (ksi)
1000	1.075×10^{-49}	9.6002	≥ 30
1100	4.647×10^{-50}	9.8576	≥ 25
1200	1.1446×10^{-35}	6.9287	≥ 20
1300	3.4461×10^{-37}	7.5479	≥ 11

20% Cold- Worked Type 316 Stainless Steel			
Temperature (°F)	A	n	Stress Level (ksi)
1022	1.0921×10^{-35}	6.4354	≥ 25
1202	1.5516×10^{-63}	13.0768	≥ 20
1292	2.6027×10^{-36}	7.1205	≥ 17

functional relationship between the constants A and n with temperature for both material conditions could be formulated.

Equation (5) in conjunction with Equation (11) permits interpolation of the secondary creep rate of annealed Type 316 stainless steel in the temperature range 1000 to 1300°F for stress levels ≤ 11 ksi and for 20% cold-worked Type 316 stainless steel in the temperature range 1022 to 1292°F for stresses ≤ 17 ksi. Because Equations (5) and (7) limit the applicable stress level over a given temperature range, effort has been directed towards arriving at the constants A, α , and n of Equation (9).

Present indications are that the sinh function is capable of effectively describing the secondary creep rate of annealed Type 316 stainless steel at high- and low-stress levels. Figure 3 illustrates this for annealed Type 316 stainless steel at 1200°F. The constants for Equation (9) when $\dot{\epsilon}$ hr⁻¹, are: $A = 1.92 \times 10^{-7}$, $\alpha = 1 \times 10^{-4}$, and $n = 2.739$. The constants A, α , and n are currently being investigated for 20% cold-worked and annealed Type 316 stainless steel over the temperature range 1000 to 1500°F.

Figure 4 compares the secondary creep rate of 20% cold-worked and annealed Type 316 stainless steel in the temperature range 1000 to 1300°F. The beneficial effect of cold working is apparent, and in Figure 5 this is shown more clearly. At 1000°F, the difference in stress required to produce a creep rate of 1×10^{-5} hr⁻¹ is approximately 8 ksi. Even at 1300°F, a difference of 4 ksi is noted.

Factors Affecting the Creep Strength of Type 316 Stainless Steel

Discrepancies between the data reported by various investigators indicate that minor variations in composition or mechanical and thermal treatment can cause significant variations in mechanical properties and microstructure.

Thermo-Mechanical Effects

Investigators agree that cold working has beneficial effects on the creep-rupture properties of austenitic stainless steels.[6,7,8,] However, they also agree that extensive recovery and recrystallization may result, leading to a decline in strength. Limited evidence suggests that cold-worked or warm-worked Type 316 stainless steel followed by a stabilizing heat treatment is more effective in increasing the creep strength as well as reducing the tendency for recovery and recrystallization than is working alone.[9,10] Table 4 compares the secondary creep rates of Type 316 stainless steel at approximately 1100°F given different thermo-mechanical treatments. Cold working 20 to 25% as compared to the annealed condition reduces the secondary creep rate by approximately a factor of 4. Cold working followed by a stabilizing treatment in which dislocations are locked by carbide precipitation reduces the creep rate by a factor of 10 over the annealed condition. Creep rate is reduced further when the material is warm worked 25% at 750°F followed by aging at 1202°F for 168 hours. Mazza attributes the difference in secondary creep rate between the cold-worked and aged and warm-worked and aged material to the depletion of carbon in the matrix of the cold-worked and aged material. The intersecting deformation twins found in the cold-worked and aged Type 316 stainless steel provide a suitable site for precipitation of $M_{23}C_6$ [9]. No deforma-

Figure 3 Stress vs. Secondary Creep Rate of Annealed Type 316 Stainless Steel at 1200°F

Figure 4 Comparison of Secondary Creep Rates of Annealed and 20% Cold-Worked Type 316 Stainless Steel at Various Temperatures

Figure 5 Stress Required to Produce Secondary Creep Rate of $10^{-5}\%$ HR^{-1} for Annealed and 20% Cold-Worked Type 316 Stainless Steel

3145-5

Table 4

Comparison of Secondary Creep Rates of Type 316 Stainless Steel $\sim 1100^{\circ}\text{F}$
Given Different Thermo-Mechanical Treatments (Stress = 32 ksi)

Creep Rate (% hr^{-1})	Material Condition
$1.2 - 3 \times 10^{-3}$ $\sim 6 \times 10^{-4}$ 4×10^{-4} 1×10^{-4}	Annealed 20% Cold Work 25% Cold Work 30% prestrain, 24 hr at 900°F and 216 hr at 1300°F
8×10^{-5} 1×10^{-5}	25% Cold Work, aged 168 hr at 1202°F Warm worked 25% at 750°F , aged at 1202°F for 168 hr

tion twins were found for Type 316 stainless steel strained 25% at 400°C or after Creep testing at 1100°F.

Solid Solution Effects

In addition to cold working and precipitation, creep strength can be affected by solid solution effects. Solute atoms impart strength to the solid solution by contributing to lattice friction hardening and dislocation locking. It has been shown that of the variability in the creep-rupture strength of Type 316 stainless steel at 1100, 1300, and 1500°F, 33 to 54% was accounted for by variation of the chemical factor, $C + 2.75N + 0.15 P$.^[11] The additional variability was believed to be due to thermal and mechanical history, and to the inherent variability in creep-rupture testing.

Figures 6, 7, and 8 show the secondary creep rate data of annealed Type 316 stainless steel from various sources.^[2,10,12,13,14] Table 5 lists the chemical analysis and reference symbols. Nitrogen content was not reported for all data. Although the data were not rigorously analyzed, there are indications that data in which the Mo, C, and P contents were relatively higher showed the greater creep strengths.

Variation in the creep-rupture properties as well as other mechanical properties is normal and to be expected from commercial practices. Standard specifications, for example, provide some manufacturing tolerances in regard to chemical analysis and heat treatment as practical necessities. Furthermore, fabrication practices and thermal cycles are known to vary widely. Some spread in properties as a result of variations in steel composition or the kind, size, distribution, and orientation of microstructural constituents representing the net effect of prior history is inevitable. Nevertheless, by properly identifying those effects of chemical composition and thermo-mechanical history of the material, substantial improvements in mechanical properties can be realized.

Figure 6 Stress vs. Secondary Creep Rate of Annealed Type 316 Stainless Steel at 1100°F

Figure 7 Stress vs. Secondary Creep Rate of Annealed Type 316 Stainless Steel at 1100°F

Figure 8 Stress vs. Secondary Creep Rate of Annealed Type 316 Stainless Steel at 1300°F

Table 5

Chemical Analysis of Data Presented in Figures 6, 7, and 8

SOURCE	1	2	3	4	5	6	7
Reference No.	13	2	12	2	10	2	14
C	0.042	0.08	0.07	0.063	0.06	0.071	0.057
Mn	1.28	1.40	1.94	1.73	--	1.54	1.80
P	0.0065	0.012	0.01	0.019	--	0.015	0.024
Si	0.33	0.45	0.38	0.43	--	0.28	0.36
S	0.01	0.011	0.021	0.014	--	0.016	0.014
Cr	17.5	16.42	18.0	16.98	17.85	17.49	17.71
Ni	11.5	13.44	11.4	13.48	10.70	13.25	13.39
Mo	2.5	2.81	2.15	2.44	2.00	2.20	2.26
Co	--	--	--	--	--	--	0.13
N	--	--	0.043	--	0.048	--	--
Grain Size ASTM	6	3-5	4-6	4-5	--	3, 4,	--
Hardness	VPN 138	--	VPN 127- 148	VPN 138	--	--	--
Symbol	◇	■	□	●	△	▲	▴

OFAA-310 - OXIDE FUEL IRRADIATION TESTS

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OBJECTIVE

The overall objective of this task is to plan, conduct, and evaluate in-reactor experiments of critical areas of special sensitivity to fuel rod behavior analysis and fuel rod assembly performance. The work will be conducted under two major subtasks:

Subtask A - Encapsulated Oxide Fuel Pin Irradiation Tests

The major objectives of this subtask are to:

- a. define irradiation test programs,
- b. develop materials design data,
- c. design irradiation test fuel pins,
- d. conduct irradiation tests, and
- e. evaluate irradiation test results.

Data and models developed under Task OFAA-210 will be utilized in this subtask and in subtask B.

Subtask B - Unencapsulated Pin Subassembly Tests

The objective of this subtask is to design, irradiate, and evaluate grid-type subassemblies with unencapsulated mixed oxide fuel pins. A primary objective of the test is to provide a vehicle for statistical testing of fuel pins while minimizing the interaction between individual fuel pins.

PRIOR WORK

Subtask A - Encapsulated Oxide Fuel Pin Irradiation Tests

Existing AEC-sponsored oxide irradiation programs were reviewed to provide a basic understanding of the state-of-the-art and to define areas where WARD effort would be most effective. The review indicated two areas for study:

- a. the effects of fuel column length on pin performance, and
- b. the optimum use of voidage to give maximum pin life.

The role of spacers was analyzed under normal and transient conditions; it was concluded that some benefits may be obtained from their inclusion. The benefits may be offset by the economic penalties incurred by the spacers.

Subtask B - Unencapsulated Pin Subassembly Tests

Preliminary parametric analyses were initiated to determine the operating conditions that could be achieved in a grid-type, 37-pin subassembly in EBR-II. Variables of pressure drop, coolant outlet temperature, fuel pin diameter, fuel pin length, pitch-to-diameter ratio (P/D), coolant velocity and flow rate, and number of grids (or grid spacing) were examined considering EBR-II design limitations and FFTF proposed design parameters. A review of the DFR Type 316 stainless steel grids involving a dimpled ferrule was initiated to provide a comparison with Inconel 718 "spring-finger" type grids.

CURRENT PROGRESS

Subtask A - Encapsulated Oxide Fuel Pin Irradiation Tests

Void Deployment Program

The incorporation in the pin of free space, in which fuel swelling may be accommodated, reduces the cladding strain and so prolongs the pin life. The void may be deployed within the fuel as porosity with varying characteristics or external to the fuel as a circumferential gap, a central hole, or as axial spaces. Because a force is required to direct swelling into the void, cladding strength may be regarded as a void deployment variable.

Using an arbitrary ground rule of a 30-pin experiment to operate in Mark A 19-capsule EBR-II subassemblies, several alternative irradiation programs were developed. Comparison of the proposed tests with available data suggested that high burnups would be necessary to produce significant cladding strain -- the principle parameter for evaluating pin performance.

Following discussions with the AEC, it was decided to investigate methods of accelerating the void deployment program to yield meaningful results over a range of operating conditions with a burnup of about 50,000 MWD/T. This requires experimental pin designs in which high burnup effects will become evident early in life. Three design features may be employed to achieve this objective:

- a. reduced free space within the pin;
- b. low-strength cladding, i.e. thin cladding at high temperatures;
and
- c. low-plasticity fuel, i.e. low average fuel temperatures resulting from low linear power ratings.

The second and third approaches require a compromise solution, and the low rating requirement conflicts with the objective of an accelerated program. Current efforts are devoted to developing a performance model (see OFAA-210) on which experimental fuel pin design can be based. A tentative irradiation test, planned to be reviewed with the AEC in January, is shown in Table 6.

Fuel Spacers

Preliminary nuclear and thermal design studies were conducted on a conceptual design of a fuel pin with fixed spacers compartmenting the fuel column. By using a fixed-spacer concept, the height of the fuel column could be increased as a result of thermal expansion of the cladding. The time delay of such a reactivity feedback is slow compared with the prompt acting Doppler effect; therefore, it is only expected to be useful for a flow coastdown accident and not during a power transient where the cladding temperature is lagging behind the fuel temperature. Such a spacer should also provide more reproducible fuel column expansion characteristics during reactor start-up and shutdown.

Calculations showed that with a cladding temperature rise of approximately 200°F in a flow coastdown accident, the thermal expansion reactivity feedback would be approximately -12 cents. This feedback assumes 100% of the ΔL of the cladding is utilized to increase the length of the fuel column; for the case of a finite number of spacers, this feedback would be slightly less.

The spacer design considered is shown in Figure 9, which shows a spacer in contact with the fuel above it, but separated from the fuel below it by a gap. This gap provides void deployment for axial fuel expansion. The spacer is fixed in the fuel column by crimping the cladding or by some similar technique.

The TOSS code was used to analyze the complicated heat transfer aspects of this type of spacer. Such analysis is necessary to ensure that the spacer temperatures and gradients are not excessive. The first set of parametric studies made use of TOSS to solve the two-dimensional heat transfer problem by assuming that conduction was the only mechanism between the spacer and the fuel above it. It was found that the fuel-cladding conductance, the spacer-cladding conductance, and the thickness of the spacer have a very significant effect on the maximum spacer temperature and temperature drop. It was also noted that the chill effect on the fuel was not significant at distances greater than 0.50 inch from the spacer-fuel contact region.

Preliminary calculations were done to establish the effect of thermal radiation on spacer temperature and indicated that radiation heat transfer could not be neglected, especially at higher power ratings. The analysis with the TOSS computer code was then modified to include radiative heat transfer for all spacer thermal calculations.

Table 6A

Oxide Void Deployment Program 19-Capsule Subassembly

Capsule #	Fuel Density		Cladding (316 SS)			Max Clad Temp (°C)	Rating (kW/ft)	Burnup (at%)
	Pellet	Smear	Type	I.D. (in.)	Thickness (mil)			
1	92	82.5	ST	0.200	12.5	500	6	5
2	92	85	ST	0.200	15	500	6	5
3	92	85	CW	0.200	12.5	500	6	5
4	92	87.5	ST	0.200	17.5	500	6	5
5	95	87.5	CW	0.200	10	500	6	5
6	90	87.5	ST	0.223	10	550	8	5
7	95	87.5	ST	0.223	12.5	550	8	5
8	92	90	ST	0.223	10	550	8	5
9	92	90	CW	0.223	10	550	8	5
10	95	90	ST	0.223	15	550	8	5
11	95	92.5	ST	0.223	15	550	8	5
12	95	92.5	CW	0.223	15	550	8	5
13	95	92.5	CW	0.223	15	550	8	5
14	95	92.5	ST	0.200	12.5	550	11.4	5
15	95	92.5	ST	0.200	12.5	600	11.4	5
16*	92	82.5	ST	0.200	12.5	500	6	5
17*	95	87.5	CW	0.200	17.5	500	6	5
18*	92	90	ST	0.223	12.5	550	8	5
19*	95	90	CW	0.223	10	550	8	5

* Spacer Pins

Table 6B
Oxide Void Deployment Program
Additional Capsules

Capsule #	Fuel Density		Cladding			Clad Temp (°C)	Rating (kW/ft)	Burnup (at%)
	Pellet	Smear	Type	I.D. (in.)	Thickness (mil)			
1A	90	85	ST	0.200	12.5	500	6	3
2A	90	82.5	ST	0.200	15	500	6	8
3A	90	87.5	CW	0.200	12.5	500	6	3
4A	95	85	ST	0.200	17.5	500	6	8
5A	92	87.5	ST	0.200	17.5	500	6	3
6A	95	87.5	CW	0.200	17.5	500	6	3
7A	95	90	CW	0.200	17.5	500	6	3
8A	92	92.5	ST	0.223	10	550	8	3
9A	95	92.5	ST	0.223	10	550	8	3
10A	95	87.5	CW	0.223	10	550	8	8
11A	95	92.5	CW	0.223	10	550	8	3
12A	95	90	ST	0.223	15	550	8	8
13A	95	92.5	ST	0.223	15	550	8	3
14A	92	90	CW	0.223	15	550	8	8
15A*	95	90	ST	0.200	15	525	~12	8
16A*	95	92.5	CW	0.200	12.5	525	~12	8
17A*	95	92.5	CW	0.223	15	550	~16	8

* Spacer Pins

- A - SPACER THICKNESS 0.10-0.20 in.
- B - DEPTH OF TOP RECESS 0.020-0.050 in.
- C - CRIMP DEPTH 0.01-0.02 in.
- D - SPACER-CLADDING GAP (PARALLEL SECTION) 0.0005-0.0015 in. RADIAL
- E - PARALLEL SECTION HEIGHT 0.05-0.10 in.
- F - CRIMP HEIGHT (INSIDE) 0.050-0.100 in.
- G - DIAMETER OF RECESS 0.08-0.100

Figure 9 Spacer-Cladding Conceptual Design

Figure 10 shows the results of a parametric study using TOSS with both conduction and radiation. The maximum spacer temperature and temperature drop are shown as a function of the thermal conductance between the spacer and cladding at 14 kW/ft with unrestructured fuel. A conductance of 1000 corresponds to a 0.001-inch radial gap filled with helium, while a value of 2000 could be obtained if there was actual contact pressure. These results indicated that the limiting design condition would be at the end of life when this gap would be filled with fission gas. The dashed curves in Figure 10 are for a spacer whose central portion has been dished out to break off thermal contact with the fuel, reducing the heat transfer from the fuel to the spacer and, therefore, reducing the spacer centerline temperature.

By reducing the heat loss from the fuel to the dished spacer, the fuel would see less of a chill effect (Figure 11) and would restructure if the centerline temperature was above $\sim 1600^{\circ}\text{C}$. Restructuring of the fuel would in turn further reduce the fuel and spacer temperature.

The value of fuel-cladding gap conductance has a significant effect on spacer temperatures. The results shown in Figure 11 assume a value of 2030 Btu/hr-ft²-°F for this parameter. Table 7 gives the maximum spacer temperature for three values of fuel-cladding conductance at a value of spacer-cladding conductance of 120. All other parameters are the same as Figure 10 for an undished spacer.

The presence of a spacer in the fuel column will tend to cool the fuel below its normal operating temperatures. This is important because it will directly affect the swelling and restructuring characteristics of the fuel in the vicinity of the spacer. The axial temperature profiles of the fuel and spacer for three radial positions are presented in Figure 12. The radial position is normalized to the pellet radius, a.

Listed below are some of the conclusions determined from the thermal parametric studies. These conclusions are based on preliminary criteria of a maximum allowable spacer temperature of 2000°F, and a maximum allowable temperature drop across the spacer of 300°F.

- a. A spacer 0.05-inch thick will have too high a temperature when in contact with fuel at 14 kW/ft. There are several design alternatives: (a) increase the thickness of the spacer until the maximum temperature is below 2000°F; (b) choose the locations of the spacers to be far enough from the center of the fuel rod such that the temperature is below 2000°F; (c) take credit for restructuring of the fuel above the spacer where this can be done with certainty. The chilling of the fuel should be taken into consideration when determining the restructured diameter; (d) have a small thin refractory metal disc on the top of the stainless steel spacer.
- b. The gap conductance between the fuel and the cladding at the end of life must be determined with as much accuracy as possible because of its effect on the various temperature profiles. Any uncertainty in this conductance must be accounted for by design conservatism. Consideration should be given to any changes in fuel swelling characteristics due to the spacer cooling the fuel.

Figure 10 Spacer Temperature as a Function of Spacer-Clad Conductance

Figure 11 Fuel Centerline Temperature as a Function of Spacer-Cladding Conductance

Table 7
Effect of Fuel-Cladding Conductance
on Maximum Spacer Temperature^a

Fuel-Cladding Conductance (Btu/hr-ft ² -°F)	Maximum Spacer Temperature (°F)
500 ^b	3501
1000	2809
2030	2434

^aAll parameters are the same as for Figure 10, spacer is undished.

$$h(\text{spacer-cladding}) = 120 \frac{\text{Btu}}{\text{hr-ft}^2\text{-°F}}$$

^bThe fuel center temperature is above melting for this conductance.

Figure 12 Axial Temperature Profile in Spacer Pin

Based on the above considerations, a preliminary design of a spacer was begun. This design includes the fuel pin layout, spacer concept, and the associated equipment required to fix the spacer into the fuel pin cladding. Figure 9 shows a schematic of the spacer-cladding conceptual design that is presently being considered for the irradiation tests. Although this concept requires an additional analysis before a final design can be initiated, the basic size and shape was determined from the parametric studies described above.

To conduct detailed mechanical, thermal, and material analyses, information is needed on spacer-cladding interaction. Several laboratory tests are planned to obtain the required information which includes the following:

- a. the force required to pull the spacer past the cladding crimped location;
- b. change in local metallurgical condition of the cladding due to crimping; (Samples will be made of the spacer-cladding cross section to investigate the extent and range of the effect)
- c. spacer-cladding crimp profiles to aid in more accurately establishing the spacer-cladding gaps used in the thermal analysis; and
- d. tube burst tests to determine the failure behavior of a tube containing crimped-in spacers.

The detailed design of the laboratory test equipment and components is about 85% complete.

Subtask B - Unencapsulated Pin Subassembly Tests

The preliminary parametric analyses of variables in a "spring-finger" grid-type subassembly operating within EBR-II limitations were completed. The analyses were performed to determine which test parameters would provide the best operating conditions to optimize the in-pile performance of a grid-type design. Linear power ratings were calculated using two approaches -- the attainable ratings based on (a) the heat removal capacity of the coolant for various flow rates, and (b) EBR-II fission rates for 50-MWt operating conditions and a specific composition. The analyses for the former included variations in pin diameter, P/D ratio, total rod length, and number of grids. Results are shown in Figure 13, where the active fuel column height, total pressure drop through the assembly, and coolant ΔT were held constant near the experimental limits. For all comparable cases, higher ratings are attainable for larger pin sizes, larger P/D ratios, shorter rod lengths and fewer grids. Similar curves for velocity and flow rate were also established.

The latter approach examined fully enriched fuel (20% PuO_2 - 80% UO_2) of two sizes -- 0.200-inch-diameter pellets in a 0.235-inch-O.D. tube and 0.215-inch-diameter pellets in a 0.250-inch-O.D. tube. The results, shown on Figure 13, represent the highest average power capable of being generated in the 3n2 position in EBR-II. The analysis indicates a P/D near 1.20 or greater would be required in a subassembly test to adequately remove the heat generated by these pellet sizes.

Figure 13 Average Power vs. P/D Ratio

Subsequent to these analyses, the prime parameters of the FFTF were changed to a 0.230-inch-O.D. fuel pin with a P/D ratio of 1.24 in the subassembly. These values are in the desired range indicated by the parametric analyses and, thus, were selected as the design parameters for this experimental grid assembly. By using this pin diameter, the program will be able to utilize the large supply of well-characterized cladding tubing being procured for development purposes in conjunction with the FFTF program. The grid design employed for this program, however, will not necessarily represent the design that would be considered for FFTF.

A conceptual layout drawing and preliminary thermal analysis of the selected experimental design were completed. The subassembly included seven "spring-finger" type Inconel 718 grids equally spaced over a 60 7/8-inch rod length. The maximum flow rate through the subassembly is 59 gpm in Row 4 of EBR-II, which corresponds to a free-stream velocity of 16.9 ft/sec. Results of the analysis are shown in Table 8, where a comparison to the largest reasonable combination of parameters -- 0.250-inch pin diameter and 1.25 P/D ratio -- is also included. Two cases for each set of parameters were calculated. One case was based on the 911°F mixed mean outlet temperature, which is the maximum possible from an unorificed assembly with the design parameters in the 4n2 position. To achieve this temperature with the larger set of parameters, orificing is necessary. The second case for each design was based on a maximum permissible EBR-II outlet temperature of 1000°F. Orificing is required to achieve this temperature for both designs. Because the coolant temperature is more sensitive to flow rate than power rating, the variation in cladding temperatures is small for these different parameters. Consequently, selection of a 0.230-inch pin diameter does not prevent achieving maximum cladding temperatures.

During the latter part of this quarter, a change from Inconel 718 to Type 316 stainless steel for grid material was effected (see OFAA-410 for further discussion). This change was based in part on the successful performance of Type 316 stainless steel grids in DFR subassemblies and the uncertainty of using Inconel 718 in EBR-II. An inquiry to EBR-II to determine the latter is being prepared while the design and analysis of a 316 grid subassembly is proceeding.

The use of an austenitic stainless steel for grids necessitates a dimpled ferrule design where the flexure of the entire grid is used to provide tension on the rods. This contrasts with the "spring-finger" design used for the high-nickel alloys like Inconel 718. In the dimpled ferrule design, a grid height of 1/2 inch is sufficient whereas 1-1/2 inches is needed for the "spring-finger" design. The shorter grid would reduce the pressure drop across the grid, thus allowing higher coolant velocities. However, there may be more grids required over the total rod length, resulting in a greater total grid loss coefficient. A preliminary analysis of the effect of varying grid strap thicknesses and total number of grids was made for the two sets of parameters previously examined. The results are shown in Table 9 for a rod length of 60-7/8 inches and a dimpled grid length of 1/2 inch.

Table 8

Cladding Temperatures in Preliminary 37-Pin Assembly Designs

	P/D = 1.24 unorificed	O.D. = 0.230 in. orificed	P/D = 1.25 orificed	O.D. = 0.250 in. orificed
Coolant Outlet Temp (°F)				
Nominal	911	1000	911	1000
Hot Channel	922	1116	992	1116
Maximum Cladding Surface Temp (°F)				
Nominal	934	1027	938	1032
Hot Channel	1052	1188	1063	1201
Maximum Cladding Inside Temp (°F)				
Nominal	977	1071	990	1084
Hot Channel	1110	1246	1132	1270
Maximum Mean Cladding Temp (°F)				
Nominal	956	1048	964	1058
Hot Channel	1081	1217	1098	1236
Power Rating (kW/ft)				
Average	10.84	10.84	13.17	13.17
Peak	13.03	13.03	15.84	15.84
Max Coolant Velocity (ft/sec)	17.3	12.5	17.1	12.3

Table 9

Effects of Varying Grid Strap Thickness and Total Number of Grids
(Dimpled Grid Length = 0.5 inch)

Grid Strap Thickness (mils)	Number of Grids	P/D = 1.24 Rod O.D. = 0.230 in.			P/D = 1.25, Rod O.D. = 0.250 in.		
		Velocity (ft/sec)	Flow Rate (gpm)	Power for 900°F Outlet (kW/ft)	Velocity (ft/sec)	Flow Rate (gpm)	Power for 900°F Outlet (kW/ft)
7.5	7	21.99	77.66	13.46	23.19	100.89	17.48
	9	21.26	75.06	13.01	22.40	97.45	16.89
	11	20.59	72.70	12.60	21.69	94.34	16.35
	13	19.98	70.55	12.23	21.04	91.52	15.86
	15	19.42	68.56	11.88	20.44	88.93	15.41
8.0	7	21.86	77.18	13.38	23.06	100.30	17.38
	9	21.10	74.49	12.91	22.24	96.77	16.77
	11	20.41	72.07	12.49	21.52	93.59	16.22
	13	19.78	69.86	12.11	20.85	90.71	15.72
	15	19.21	67.84	11.76	20.25	88.08	15.26
10.0	7	21.28	75.12	13.02	22.50	97.86	16.96
	9	20.43	72.14	12.50	21.60	93.97	16.29
	11	19.68	69.48	12.04	20.81	90.52	15.69
	13	19.00	67.08	11.62	20.10	87.42	15.15
	15	18.38	64.91	11.25	19.47	84.70	14.68
12.0	7	20.65	72.91	12.64	21.90	95.25	16.51
	9	19.72	69.64	12.07	20.92	91.03	15.78
	11	18.90	66.75	11.57	20.07	87.32	15.13
	13	18.18	64.19	11.12	19.34	84.14	14.58
	15	17.53	61.90	10.73	18.69	81.29	14.09

Design drawings and more detailed analyses of the 0.230-inch pin O.D. and 1.24 P/D ratio design incorporating Type 316 stainless steel dimpled grids in a subassembly with 60-7/8-inch-long pins are proceeding.

OFAA-410-FABRICATION DEVELOPMENT OF EBR-II SUBASSEMBLIES

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OBJECTIVES

The original objectives of this task, defined in the last quarterly report (WARD-3791-28), were recently revised during review meetings with AEC-RDT personnel in late November, 1968. A decision was made to halt development studies on brazed Inconel 718 grids and to re-direct the grid development program to a spot-welded AISI Type 316 stainless steel grid, similar to that used in DFR and presently specified for PFR. Consequently, the objectives of this task have been redefined as indicated below.

The objectives of this task are to develop fabrication procedures and produce 37-and 61-pin EBR-II subassemblies based on the PFR honeycomb grid design. The scope of this program will be limited to grids, cans, and end fittings with fuel pins being prepared under OFAA-510 of the Oxide Program.

The subassembly fabrication effort covered by this task will include the following:

- a. establish optimum welding parameters for grid fabrication;
- b. determine the optimum welding procedures for fabrication of can seam welds and can-to-grid joints;
- c. determine fabrication and assembly procedures for attaching any fittings required to support and locate the subassemblies in EBR-II;
- d. prepare simulative samples of all joints and evaluate the effects of fabrication and heat treatment operations on the mechanical properties of the joints;
- e. identify suitable inspection criteria for assessing the subassembly components;
- f. fabricate the simulative model for design verification testing;
- g. fabricate subassemblies for insertion in EBR-II; and
- h. identify post-irradiation examination procedures and parallel out-of-pile control grid tests, to facilitate grid performance evaluation.

PRIOR WORK

The mechanical design inputs required for the subassembly tests were identified, and Inconel 718 was selected as a reference grid material. Brazing alloys meriting further consideration in the balance of the program were identified as Microbraz 50, Coast Metals NP, and Coast Metals 60. Tensile, creep, and stress relaxation testing was begun to evaluate the effects of simulative brazing thermal cycles on the mechanical properties of Inconel 718. Test pieces were designed for sodium corrosion testing of brazed Inconel 718, and purchase orders were placed for both machining of the test pieces and a brazing fixture for their production. The development of grid inspection techniques was also initiated, and a brief series of tests were performed to evaluate the potential of electron beam welding axial fuel pin spacers.

CURRENT PROGRESS

The current progress is presented under headings which delineate work carried out before and after the change in program objectives.

Inconel 718 Grid

Effect of Brazing Thermal Cycle on the Mechanical Properties of Inconel 718

Machining of the following test pieces were completed: 55 creep, 34 stress relaxation, and 33 tensile. Of these, 19 various test pieces were thermally cycled to simulate a brazing temperature of 1875°F for two hours and given an aging treatment of 1325°F for eight hours followed by 1150°F for eight hours. A further nine test pieces were given a simulated brazing thermal cycle of 2150°F for 15 minutes and aged in the same way as the first batch. The mechanical property results obtained are shown in Tables 10, 11, and 12. No further work was performed on this subtask after November 26, 1968.

Sodium Corrosion Testing

Three brazing trials were performed with Microbraz 50, to determine the optimum brazing parameters for the manufacture of the sodium corrosion test pieces. Braze metal wetting of the Inconel 718 was enhanced by nickel plating the faces to be brazed. Metallographic examination of trial test pieces indicated that joint gap and braze metal cracking were difficult to control with the existing brazing fixture. Several test pieces were also made without fixturing to evaluate the possibility that the brazing alloy surface tension would provide suitable joint gaps. Although misalignment was a potential hazard with this latter technique, the initial results were promising. Thirty test pieces were made for the sodium corrosion program when work on this subtask was stopped on November 26, 1968.

Table 10

Elevated Temperature Tensile Tests on Inconel 718
(0.016-inch-Thick Sheet)

Simulated Brazing Cycle	Aging Treatment	Test Temperature (°F)	UTS (psi)	Yield Strength 0.2% Offset (psi)	Elongation (%)
1875°F for 2 Hr	1325°F for 8 Hr at 1150°F and furnace cool	800	158,793	136,180	Broke out of gage length
		800	160,606	140,151	9.5
		800	158,000	Recorder malfunction prevented measurement	9.6
		1000	149,500	131,450	8.6
		1200	147,250	129,400	
					Broke out of gage length

Table 11

Stress Relaxation of Inconel 718 (0.016-Inch-Thick Sheet)

Simulated Brazing Cycle	Aging Treatment	Test Temp (°F)	Test Duration (hr)	Initial Stress (ksi)	Final Stress (ksi)	Percentage Stress Relaxation
1875°F for 2 Hr	1325°F for 8 hr followed by 8 hr at 1150°F and furnace cool	1200	500	90.0	73.0	18.6
		1000	500	90.0	89.4	0.7
		800	500	90.0	89.4	0.7
		1200	500	70.0	57.5	17.0
		1000	521	70.0	68.8	1.6
		800	546	70.0	66.6	4.5

Table 12
Creep Testing of Inconel 718 (0.016-Inch-Thick Sheet)

Simulated Brazing Cycle	Aging Treatment	Test Temp (°F)	Test Duration (hr)	Stress (ksi)	Minimum Creep Rate (% Strain/hr)	Total Creep Strain
1875°F for 2 hr	1325°F for 8 hr followed by 8 hr at 1150°F and furnace cool	1200	504	70	0.00033	0.380
		1200	285	90	<0.00320	1.5 ^a
		800	500	70	0.00001	0.096
		800	501	70	0.00007	0.072
		800	514	90	0.00028	0.120

^a Rupture occurred after 285 hours

Development of Subassembly Inspection Techniques

Preliminary ultrasonic testing of simulated brazed grid joints have proved successful, and a probe was to be designed for grid inspection when preliminary drawings of the grid became available. Work on this probe design was stopped on November 26, 1968.

Type 316 Stainless Steel Grid

Following the decision to stop fabrication development of a brazed Inconel 718 type grid, the available information on the DFR and PFR grids was examined. The limited information on the DFR grid fabrication indicated that the grids were of spot-welded construction using 0.010-inch stainless steel strip formed in a honeycomb pattern in which each pin is located centrally in the cells by three small dimples.

Preliminary drawings of a 37-pin stainless steel grid have been developed. The same pitch-to-diameter ratio as that used for the Inconel 718 grid design is maintained. The width of the flats on the honeycomb cell in this design is approximately 0.15 inch. Weld metal expulsion could be a problem when using resistance processes, and welding trials are planned to evaluate this point. Since ultrasonic spot welding would not present this problem, there is an incentive to determine the potential of that process in this application. Discussions with Sonobond Corporation, Westchester, Pa., have indicated that they could perform welding trials in less than two weeks. It is planned to accept this offer, and welding trials are expected to commence at the end of January, 1969, by which time suitable stainless steel strips could be made available. Resistance spot welding trials will also be started at this time at Westinghouse R&D Center, where equipment availability has been established.

OFAA-510-FABRICATION OF OXIDE FUEL PINS

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OBJECTIVES

The objectives of this task are as follows:

- a. To identify a process capable of fabrication of mixed uranium and plutonium oxide fuel of the required geometric, stoichiometric, and physio-chemical properties to meet the objectives of the Task OFAA-310 program. The fuel will be essentially $(U_{0.8}Pu_{0.2})O_{1.98}$ in the form of pellets 1/4-inch diameter of 90 to 93% TD. This objective will include the writing of a process description, a materials description, and the identification of all necessary support analytical determinations in the control of process variables.
- b. To identify certain characterization parameters necessary to compliment the fabrication process outlined in (a) and the irradiation program OFAA-310, i.e. thermal conductivity, expansion, etc.
- c. To fabricate mixed uranium-plutonium dioxide fuel pellets for all phases of the Contract work and irradiation program.

PRIOR WORK

A complete and detailed fabrication process was written following the powder compaction-sintering-grinding route together with a fuel and process characterization description.

Relevant thermochemical data pertaining to the proposed WARD oxide fuel fabrication process were compiled and reviewed with those of other facilities. The physio-chemical process planned by WARD follows proven lines of synthesis and well established thermo-chemical data.

An Oxide Fuel Facility capable of fabricating up to ten EBR-II type pins per week, funded totally by Westinghouse, is now under construction and was fully reported in the last quarterly, WARD-3791-28. All major items of equipment were identified. All determinations, their precision and accuracy, were outlined.

CURRENT PROGRESS

A comprehensive literature survey was conducted to provide a background reference work for the facility and to aid in the confirmation of the process parameters selected. To date, a detailed appraisal of work done in the United States was completed, and this will be followed by work in other countries notably France, Belgium, Germany, and the UK. This survey has confirmed that the process which WARD intends to follow is consistent with current technology. Minor, but significant changes may be suggested in the reference process outlined in the last quarterly, i.e. a fallback to an "alternate" process in the following items:*

- a. a change from wet slurry milling to dry blending;
- b. a change from wet grinding of pellets to dry grinding.

These changes will effect the quantity of fissile material within the box and consequently ultimate through-put. Also, the changes will mean a completely "dry" process** that will significantly improve the glove box atmosphere.

PROGRAM PLANNING

Although a work program for this task was presented previously in WARD-3791-28, a more detailed reappraisal of OFAA-510 and the expected results in FY-1969 were made to produce a better understanding of the commitments and progress of the WARD Oxide Program.

The envisaged process (reported in WARD-3791-28) for producing mixed oxide pellets for irradiation fuel pin tests is summarized in Table 13. Process variables associated with this process will be investigated to either improve or enhance the scale-up potential of certain phases of the process or improve the irradiation properties of the fuel. Some of these process variables are shown in Table 14. At all stages of the process, a continuous monitoring of the pertinent properties or composition of the fuel will be performed, and these analytical determinations are given in Table 15.

The WARD void deployment and restrictor experiment (OFAA-310, Subtask A) fuel pellet requirements were discussed with representatives from PNL. PNL is currently fabricating pellets with 25% Pu and maximum pellet densities of 94%. Pellets of these characteristics would be available for the initial phase of WARD irradiation program.

* From survey and discussion with PNL representatives.

** Not counting the H₂O in the sintering gas.

Table 13

Proposed Pin Fabrication Process

A. Pellet Fabrication

1. Powder receipt and characterization
2. Mechanical "wet" mixing of oxides or dry blending.
3. Drying and granulation
4. Pre-compaction
5. Crushing
6. Sieving
7. Pelletization
8. Debinding
9. Sintering
10. Pellet characterization
11. Grinding--wet or dry
12. Outgassing
13. Vacuum or inert gas storage

B. Pin Fabrication

1. Assemble fully characterized pellets and "hardware"
2. Insert cladding into "udder" glove box bag
3. Load pellets
4. Load spacer and position
5. Crimp spacer
6. Load second layer of pellets and repeat (3)-(6)
7. Load upper UO_2 insulator pellet
8. Decontaminate
9. Cap end with plastic tube
10. Radiograph pin
11. Post into welding box
12. Evacuate and He back fill
13. End weld
14. Leak test
15. Radiograph end weld
16. Dimension check and profilemetry

C. Na Encapsulation

1. Load Na into capsule	9. Certify
2. Load pin into capsule	10. Pack and ship
3. Weld end plug	
4. Leak test	
5. Radiograph	
6. Na bond	
7. Eddy current test	
8. Dimensional check	

Table 14
Process Variables Associated with the Fabrication
of
Mixed Oxide Fuel Pellets

<u>Fabrication Variables</u>	<u>Control of</u>
A. <u>Powder Process</u>	
1. Particle size	Composition and Homogeneity
2. Surface area	
3. Blending technique	
4. Milling time	
B. <u>Pelletization Process</u>	
1. Pre-compaction	Void deployment and Pellet Density
2. Granule density	
3. Granule size	
4. Binder addition	
5. Binder type	
6. Pellet green density	
7. Pressing pressure	
C. <u>Sintering Process</u>	
1. Debinding temperature	Stoichiometry, Density and shrinkage
2. Debinding time	
3. Sintering temperature	
4. Sintering time	
5. Sintering atmosphere	
D. <u>Material Variables</u>	
1. Oxide initial stoichiometry	Fissile content
2. Pu/U ratio	
3. O/U + Pu ratio	
4. U isotopic content % ²³⁵ U	
5. Pu isotopic content % ²⁴¹ Pu	

Table 15

Fabrication Process Material Analyses

A. Control and Process Monitoring Analysis

1. Oxygen/Metal ratio
2. Plutonium/Uranium ratio
3. Microstructure
4. Density
5. Size

B. Selected Sample Analysis

1. Surface area of powder
2. Particle size of powder
3. Total impurity of powder
4. X-ray diffraction lattice parameter
5. Autoradiograph
6. Micro-probe analysis^a
7. H.T. Gas release

C. On Compositional Change

1. Selected thermal and physical properties^a

^aIn collaboration with a national library

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