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OXIDE FUEL ELEMENT DEVELOPMENT
QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING JUNE 30, 1970

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Contract At(30-1)-4135
U. S. Atomic Energy Commission
Submitted to AEC-NY00 in September 1970

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SECTION 1

INTRODUCTION

GENERAL

This is the eighth quarterly progress report and covers the work performed during the fourth quarter of FY-1970.

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 cladding, 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 LMFBF.

SUMMARY OF PRIOR WORK

All work related to the present investigations and performed prior to the present period has been fully reported in:

- WARD-3791-28 "Oxide Fuel Element Development Quarterly Progress Report for the Period Ending September 30, 1968."
- WARD-3791-32 "Oxide Fuel Element Development Quarterly Progress Report for the Period Ending December 31, 1968."
- WARD-3791-37 "Oxide Fuel Element Development Quarterly Progress Report for the Period Ending March 31, 1969."
- WARD-3791-39 "Oxide Fuel Element Development Quarterly Progress Report for the Period Ending June 30, 1969."
- WARD-4135-1 "Oxide Fuel Element Development Quarterly Progress Report for the Period Ending September 30, 1969."
- WARD-4135-4 "Oxide Fuel Element Development Quarterly Progress Report for the Period Ending December 31, 1969."
- WARD-4135-8 "Oxide Fuel Element Development Quarterly Progress Report for the Period Ending March 31, 1970."

SUMMARY OF CURRENT PROGRESS

A summary of current progress for the tasks in this program is presented in this section. A description of progress is given in the individual task sections of this document.

OFBA-210

The OLYMPUS-IIA and IIB codes were developed. Both forms include models for fission gas release and cladding wastage. The OLYMPUS-IIB code incorporates a stress-enhanced cladding swelling model. Correlation of the code with experimental data is in progress. A detailed re-evaluation of available data has resulted in development of a revised equation for stress-free swelling of 20% cold-worked Type 316 stainless steel. The new equation has a ϕt power dependence of 1.5. Analysis of stress-induced swelling was continued.

OFBA-211

A model for fission enhancement of fuel creep has been developed for inclusion in the CYGRO-F code. Immersion densities of irradiated fuel were used as a basis. Predictions are in reasonable agreement with in-pile creep test results. Development of a gas bubble fuel swelling model has been initiated. Progress to date indicates that considerable development effort will be required to produce a model that is compatible with CYGRO-F.

OFBA-310

Subassembly WSA-1 was inserted in the EBR-II reactor at the end of run 43 (June 23, 1970). This subassembly has accumulated 1350 MWd exposure, and WSA-2 has accumulated 2850 MWd exposure. The experimental planning for the WSA-5 series of fuel pins was initiated. A re-analysis of WSA-1 and WSA-2 using the OLYMPUS-II code is in progress to provide a basis for planning the WSA-5 pins.

OFBA-311

Final design modifications were made to WSA-3 fuel pin end plugs to facilitate remote handling. Thermal-hydraulic analysis of WSA-3 fuel pins was virtually completed, including a detailed bypass flow analysis. The results show a total 4% bypass from all sources only reduces peripheral channel coolant temperatures about 25°F. Uncertainty factors were upgraded to improve the analyses. The calculated K_{eff} for WSA-3 is 0.35, which is well below the EBR-II limit of 0.6. Mechanical analyses have been completed and documented for the Data Package. Results of the grid support strap tab stress analysis show no restriction on deformation allowances. Project activities such as the flow test and approval of the xenon tagging procedure, which support the irradiation of WSA-3, have been completed. Experimental planning and design of WSA-4 fuel pins were initiated. Cladding material for these pins was ordered and machined end plugs were received.

OFBA-312

Preliminary design choices were made of fuel rod size, spacer grid design, and hot channel factors. Based on these assumptions, assembly pressure drop, mean outlet temperature, and maximum cladding and fuel temperatures were calculated. The COBRA code was employed in subchannel analysis. The effects of varying the turbulent mixing factor or the outer rod row to duct (edge) distance were studied.

The revised design requirements for the FFTF backup core and LMFBR test grid assembly are presented. The changes include the latest revisions of the FFTF design requirements. A preliminary design analysis of the LMFBR and FFTF backup core honeycomb grid assembly components was performed. Results show that the proposed design is compatible with FFTF requirements.

The fuel rod cladding sizing analysis shows that the FFTF fuel rod design could be used in a gridded test assembly to achieve LMFBR prototypical fluence and burnup goals without exceeding the allowable ductility limited cladding strain.

The analysis verifies that flow induced fuel rod vibration affects the spacing between the grids only in the fission gas plenum region of the fuel rod. Fuel rod bow limits grid spacing in the core zone. Fuel rod bowing due to thermal expansion, material swelling, and irradiation induced-creep produced less than 2% linear closing of the flow channel between adjacent fuel rods. The maximum flow channel closing including the effects of thermal stability and creep buckling was conservatively calculated to be only 55% of the allowable limit.

The honeycomb grid cell and grid dimple dimensions were determined so that both allowable stress and allowable pressure drop requirements were satisfied. The grid dimple design accommodates initial fuel rod-grid misalignment and limits the fuel rod-grid interaction load due to differential growth. In a preliminary analysis of two alternative grid designs (i.e., two versions of the line-of-sight grid concepts), it was found that design modifications must be made to satisfactorily accommodate differential growth between the fuel rods and grid.

OFBA-313

Eleven grids were fabricated for the test subassembly and hand-worked to produce the proper variations in the dimple circle diameters. The fabrication of 40-dummy fuel pins is complete and all the components are ready for final assembly. Modifications to the test facility cannot be completed until DFAT is terminated. WBTL-2 of Subtask 2 was terminated after 2100 hours of operation at reference conditions. The samples from WBTL-2 have been removed, cleaned, and weighed. The objective and the status of WBTL-3 are reported under Task NCAA-300 of Coolant Technology Branch Contract AT (30-1)-4173.

OFBA-410

Samples of grid strap material received from Hamilton Precision Metal were analyzed spectrographically. The vendor's analyses were confirmed except for one batch which showed a high nitrogen content. This batch will be used for out-of-pile testing. The inner grid assemblies were completed for the flow test subassembly and the fretting test subassembly. Fabrication of the inner grid assemblies for WSA-3 was initiated. It was concluded that radiography was not suitable for grid inspection. The previously reported method of periodic destructive examination is still the prime method of assuring grid joint quality. The indexing system for electrodischarge machining of liner flanges has been modified to produce acceptable flange concentricity. Electrodischarge machining is better than milling for maintaining dimensional control of the slot pitch and true position of the slots in the bottom grid assembly. Welding keys (on T-bars) into the bottom grid support did not produce thermal distortion. The liner loading and the grid loading fixtures were received and installed. They are both working satisfactorily.

OFBA-411

All the tooling required under Subtask A has been received and made workable. The hydraulic test loop was cleaned and a test assembly was installed. Flow distribution measurements were made and compared with the flow distribution calculated from COBRA. The agreement between the two was excellent.

OFBA-510

Work continued to identify the principal fabrication variables having the greatest effects on fired pellets. Wet milling was selected on the basis of homogeneity and fired density results. An investigation of several different binder additions showed Acrowax to be the most suitable. Trials of preslugging and pressing processes resulted in current investigations at 3 - 8 tsi and pressures to give final green densities between 40 and 50% T.D. In order to obtain the proper stoichiometry control, increased moisture content in the sintering gas and one or more refires are employed. Recalcining operations on PuO_2 at 700°C were successful in producing higher fired densities than previously obtained. Work on fabrication of WSA-3 fuel pellets continued.

OFBA-511

All analytical chemistry on WSA-1 and -2 fuel and insulator pellets was completed. Oxygen-to-metal determinations on trial batches of $(\text{U}, \text{Pu})\text{O}_2$ resulted in changing the sintering atmosphere from a N-6% H_2 atmosphere to a high-moisture content A-5% H_2 atmosphere. Results for average particle size and BET surface area on milled powders showed the effectiveness of the wet milling operation. Extensive autoradiographic and metallographic examinations were conducted to investigate particle size and distribution and homogeneity in $(\text{U}, \text{Pu})\text{O}_2$ fuels. Detailed characterizations of the first large batch of fuel has resulted in establishing better processing conditions for subsequent mixed-oxide batches.

OFBA-512

Fabrication and testing of all the fuel pins for WSA-1 was completed, and the assembly was shipped to EBR-II. Three glove boxes were made operational for fuel pellet loading and final pin fabrication of WSA-3. Fabrication of dummy fuel pins for flow testing and for the fretting tests have been completed and the pins delivered. Certified tubing has been received and fuel tube preparation and bottom end plug welding is under way for WSA-3. The xenon tagging apparatus has been constructed and trial runs performed. Results of analyses of gas samples have not been completed. The design of a new profilometer to survey the surface topography of fuel pins has been completed. Construction will begin next quarter.

OFBA-513

The data package for WSA-1 was completed. Preparation of the package for WSA-3 is in progress. Fuel pellet integrity standards were selected and agreed upon. These will be used for selection of WSA-3 fuel pellets. Calculation of the density of insulator pellets for WSA-3 is complete, and 100 pellets have been selected for loading.

SECTION 2

OFBA-110 PROJECT ADMINISTRATION

F. A. Brassart, W. B. Heubel, and P. Murray

OBJECTIVES

The objectives of this task are to assure that the project is successfully completed, on schedule within budget, and to the satisfaction of the Atomic Energy Commission; to assure compliance with contracted obligations; and to coordinate this project with other AEC-sponsored and Westinghouse-sponsored LMFBF development projects.

Overall project direction and day-to-day administration will be provided under this task. Plans and controls will be established and maintained; periodic reviews will be held with the Commission; correspondences and reports will be coordinated; and day-to-day technical and administrative liason with the Commission will be provided.

PRIOR WORK

Revised work programs for FY-1970 were submitted to the Commission in December 1969. Technical work has continued on the basis of those work programs.

For FY-1970 administrative control, the project is divided into the following tasks:

- OFBA-110 Project Administration
- OFBA-210 Fuel Performance Analysis
- OFBA-211 Development of Analytical Methods and Lifetime Codes
- OFBA-212^(a) Design Limit and Transient Survival Testing of Fuel Element
- OFBA-310 Encapsulated Mixed-Oxide Fuel Pin Irradiation Tests
- OFBA-311 Mixed-Oxide Irradiation Tests in Grid-Type Subassemblies
- OFBA-312 217 Pin Grid-Type Fuel Assembly Design and Development

(a)

A final report (WARD-4135-6) for Task OFBA-212 is in reproduction and is expected to be issued during July 1970.

OFBA-313 Fabrication, Development, and Testing of LMFBR Fuel Assemblies
OFBA-410 Fabrication, Development of EBR-II Grid-Type Subassemblies
OFBA-411 Subassembly Design and Hardware
OFBA-510 Pellet Fabrication
OFBA-512 Fuel Element Fabrication
OFBA-513 Product Control and Documentation

This breakdown is more detailed than that used in FY-1969 and reflects increased effort in FY-1970. Much of that increased effort is associated with fuel pin fabrication each of which is controlled and reported separately. OFBA-211, 212, 312, and 313 are new tasks as of FY-1970. In addition, separate tasks have been established for the design of encapsulated and unencapsulated fuel pin tests.

CURRENT PROGRESS

The quarterly progress report (WARD-4135-8) for the period ending March 31, 1970 was prepared and distributed.

Schedule 189a/Work Plans for FY-1971 were prepared and submitted to the New York Operations Office.

SECTION 3

OFBA-210 FUEL PERFORMANCE ANALYSIS

W. K. Appleby, A. Biancheria, A. Boltax, and T. P. Soffa

OBJECTIVES

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

PRIOR WORK

Preparation of the OLYMPUS-II fuel pin behavior code was completed, and correlation of the code with experimental data was initiated. Analytical expressions were presented for computing irradiation-induced creep in solution-treated and 20% cold-worked Type 316 stainless steel. Illustration of preliminary OLYMPUS-II results were shown including the effects of irradiation-induced creep. A model for stress effects on void growth was developed. The model indicated that stress-induced void growth can give rise to large dimensional changes at temperatures above 550°C.

CURRENT PROGRESS

OLYMPUS-II Development

Two forms of the OLYMPUS-II code are now available for further study. OLYMPUS-IIA and IIB are identical, except for the stress-induced void growth feature in OLYMPUS-IIB. Refined calibration of the OLYMPUS-IIB code with irradiation data is in progress.

The following models have been incorporated into the OLYMPUS-II code:

1. Fission gas release
2. Cladding wastage
3. Stress-enhanced swelling
(OLYMPUS-IIB only)

Fission Gas Release

Available fission gas release data from mixed oxide fuel pins under fast flux conditions indicate that the fractional release depends mainly on

burnup and the density of the fuel. [1-4] According to this model and the experimental evidence, the number of moles of fission gas released per cm³ of fuel after a given burnup is,

$$m(B) = 4.0665 \times 10^{-4} \cdot \bar{y} \cdot d \cdot B \cdot f(B,d) \left[\frac{\text{moles}}{\text{cm}^3} \right] \quad (1)$$

where: $\bar{y}$ = fission gas yield (atoms/fission)

d = fractional density of fuel

B = burnup (a/o)

and

$$f(B,d) = 1 - (2d-1) \frac{1 - e^{-\alpha_1 B}}{\alpha_1 B} - 2(1-d) \left(\frac{1 - e^{-\alpha_2 B}}{\alpha_2 B} \right) \quad (2)$$

with

$$\alpha_2 = 17.2$$

$$\alpha_1 = -1.42d + 1.564 \quad (3)$$

The recommended value for $\bar{y}$ is 0.274.

Cladding Wastage

Evaluation of strain history of a fuel pin requires that the cumulative effects of cladding wastage be known as a function of time, burnup, and temperature. Consider a section of a fuel pin in which the internal pressure imposed on the cladding is P . This pressure, P , is composed of two parts, the fission gas pressure and the fuel-cladding mechanical interaction pressure. The hoop stress developed in the cladding wall is given by,

$$\sigma_{\text{hoop}} = \frac{PD}{2t} \quad (4)$$

where D is the cladding diameter, and t is the thickness. The stress-strain history of the cladding will be highly sensitive to t and P . The use of the as-fabricated nominal thickness over the life of the fuel pin would predict low strain values, while on the other hand, the use of some lower arbitrary values may be too conservative. The effective fuel cladding wall thickness used in the stress-strain analysis of a fuel pin must make adequate cumulative allowance for:

1. Fabrication tolerances
2. Sodium corrosion and erosion thinning
3. Loss of cladding thickness due to fuel-cladding reactions
4. Possible defects in the cladding that are below the limit of detection

A one-mil allowance is provided for cladding tolerance and defects.

To account for sodium corrosion the following equation is used.

$$\Delta t_1 = \Delta Y \{V^{.29} \cdot C^{1.12} \exp (-3.66 - 156.4/T)\} \quad (5)$$

where: Δt_1 = thickness loss due to sodium corrosion (mils)

ΔY = time of exposure (years)

V = sodium velocity (ft/sec)

C = oxygen content of sodium (ppm)

T = temperature, °R

Typical values for the sodium velocity and oxygen content of the sodium are 20 ft/sec and 10 ppm, respectively.

Fuel cladding reaction thinning is accounted for by the following empirical equation,

$$\Delta t_2 = 5.6 \times 10^{-4} (T - 950) + 0.0426 \quad (6)$$

where Δt_2 is the depth of fuel-cladding reaction in mils/ 10^4 MWD/Te, and T is the cladding temperature in °F.

The effective cladding thickness is obtained from,

$$t_e = t_o - \Delta t_1 - \Delta t_2 \quad (7)$$

where t_o is the nominal as-fabricated cladding wall thickness and t_1 and t_2 are given by Equations 5 and 6, respectively. B is the burnup in 10^4 MWD/Te.

Stress Effects on Void Growth

The model for stress-induced void growth, presented in the last quarterly report, was utilized to develop an analytical expression relating the additional swelling due to hydrostatic stress. The incremental swelling due to hydrostatic stress, σ_H , is given by:

$$\delta \left(\frac{\Delta V}{V} \right)_{\sigma} = \frac{A D_V \Omega \left(\frac{\Delta V}{V} \right)_s \sigma_H t}{r^2 k T} \quad (8)$$

where: A = a constant

D_v = vacancy diffusion rate

Ω = atomic volume

$(\Delta V/V)_s$ = stress-free swelling

t = time of stress application

r = void radius

k = Boltzman's constant

T = absolute temperature

Figure 3-1 compares $(\Delta V/V)\sigma$ with $(\Delta V/V)_s$ for solution-treated Type 316 stainless steel cladding after an exposure of 5×10^{22} n/cm² under a hydrostatic stress of 1000 psi. The dimensional change caused by stress-induced void growth can be considered a mechanism for high temperature irradiation-induced creep.

In the current analytical models of stress-induced void growth, the effect of hydrostatic stress on swelling is directly proportional to the stress-free swelling. Since the magnitude of stress-free swelling at high temperatures is relatively unknown and may be much smaller than shown in Figure 3-1, the values indicated for stress-induced swelling must be considered preliminary. Stress-enhanced swelling is expected to attain a maximum in the temperature range 600-700°C; i.e., below the temperature limit for stress-free swelling.

Stainless Steel Swelling

As a result of a detailed re-evaluation of available irradiation data, a new equation has been developed for the stress-free swelling of 20% cold-worked Type 316 stainless steel.

$$\begin{aligned} \% \Delta V/V = 9 \times 10^{-35} (\phi t)^{1.5} [4.028 - 3.712 \times 10^{-2} T + 1.0145 \times 10^{-4} T^2 \\ - 7.879 \times 10^{-8} T^3] \end{aligned} \quad (9)$$

where: ϕt = fast fluence (E > 0.1 Mev)

T = temperature in °C

Figure 3-2 shows a comparison of the current FFTF design equation for 20% cold-worked 316 (referred to as the 1.69 equation because of the fluence exponent) with the new equation (the 1.5 equation). The stress-free swelling values indicated above 550°C are conservatively estimated (presumably on the high side).

Figure 3-1. Comparison of Stress Free Swelling and Stress-Induced Void Growth of Solution Treated Type 316 Stainless Steel

Figure 3-2. 20% Cold Worked Type 316 Stainless Steel Swelling

SECTION 4

OFBA-211 DEVELOPMENT OF ANALYTICAL METHODS AND FUEL LIFETIME CODES

M. S. Beck, A. Biancheria, A. Boltax, and B. L. Harbourne

OBJECTIVES

The objective is to develop detailed design codes for performance of mixed-oxide fuel rods over the range of parameters characteristic of the LMFBR. This task is closely related to Task OFBA-210. The major emphasis of Task OFBA-211 is the development of the CYGRO code for fast reactor applications.

The activities of Task OFBA-211 can be divided into four areas, with initial emphasis on the first three areas,

1. Development of analytical methods to predict steady state behavior of the fuel rod system
2. Identification of information needed to improve the models
3. Development of integrated design codes
4. Development of design models to predict off-normal transient behavior of the fuel system

The starting point for this program is the CYGRO-2 code developed at Bettis Atomic Power Laboratory. The modifications of CYGRO-2 for application to fast reactor fuel pin studies are called the CYGRO-F series codes.

PRIOR WORK

Comparison of code predictions with experimental pin results showed good agreement for moderately-rated, cold-worked Type 316 stainless steel cladding pins, but diametral strains in highly rated solution-treated Type 316 stainless steel cladding pins were substantially overestimated. The latter results may come from the use of inappropriate properties to describe cladding behavior.

Sensitivity studies on FFTF pins showed that although cladding strains increased with increasing smeared density and decreasing gap size, the performance was only slightly affected by these parameters, particularly below about 8 a/o burnup. Cladding behavior was strongly influenced by internal stresses developed through differential cladding swelling.

CURRENT PROGRESS

Fuel Creep Properties

In the initial formulation of fuel creep properties for use in CYGRO-1, the few available UO_2 thermal creep data were used to develop an equation of the form:

$$\dot{\epsilon} = 10^c (\sigma/1000)^g \quad (10)$$

where c and g are temperature-dependent parameters, $\dot{\epsilon}$ the strain rate, and σ the stress.

Recent studies on UO_2 produced data which could be represented by:

$$\dot{\epsilon} = A\sigma + B\sigma^{4.5} \quad (11)$$

where A and B are dependent on temperature, fuel density, and grain size. Comparison of Equations 10 and 11 shows that the exponent g should be a function of applied stress.

In addition to thermal creep deformation, the existence of fission-enhanced creep in UO_2 may be expected from the work of Konobeevski^[5] on metal fuels. It was identified in Bleiberg's low temperature swelling studies.^[6] Current in-pile creep experiments on UO_2 by BMI^[7] show that a substantially enhanced creep deformation does occur. These data do not yet permit a systematic description of the effect.

Immersion density data^[6] were used to derive the dependence of irradiation-induced creep on temperature and fission rate. Density change is the result of the solid volume change due to solid fission products and the change in pore volume. The rate of change of pore volume, $\Delta\dot{p}$, is given by:

$$\Delta\dot{p} = [(\rho_o/\rho_b) + b\Delta\dot{V}_s (\rho_o/\rho_{100}) - 1] / b [1 - (\rho_o/\rho_{100})] \quad (12)$$

where: ρ_{100} = theoretical density

ρ_o = initial fuel density

ρ_b = density after burnup, b

$\Delta\dot{V}_s$ = solid fission product swelling rate

Using the value of $0.16 \text{ v/o}/10^{20}$ fissions cm^{-3} for $\Delta\dot{V}_s$ (determined from these experimental results), $\Delta\dot{p}$ was calculated from the density results. Regression analysis of $\Delta\dot{p}$ on burnup shows a reduction in porosity at low burnups; i.e., the fuel is being hot pressed. This process may be described by the Murray-Livey^[8] equation:

$$\Delta \dot{p} = - \frac{3}{4} \frac{\sigma}{\eta} \cdot p \quad (13)$$

where p is the porosity and η the fuel viscosity. Equation 13 and the $\Delta \dot{p}$ - burnup regression equation ($\Delta \dot{p} = \alpha_1 + \alpha_2 B$) may be solved for the fuel viscosity η at zero time, giving:

$$\eta = - 3\sigma P_0 / 4\alpha_1 \dot{f} \quad (14)$$

where $\dot{f}$ is the fission rate. The effect of fission rate was determined by normalizing the strain rate to a mean temperature and stress using Konobeevski's model [5] for fission-induced creep:

$$\dot{\epsilon}_I = k \sigma \dot{f} / (T_m - T) \quad (15)$$

where T_m = melting temperature ($^{\circ}K$) and k is a constant.

Linear regression of the adjusted irradiation-induced creep rates gives:

$$\log_{10} \dot{\epsilon}_I = -17.531 + \log_{10} \dot{f} \quad (16)$$

at $640^{\circ}C$ (mean fuel temperature) and 2000 psi. Equating Equations 15 and 16 yields a value for k so that irradiation-induced creep rates can be determined for any stress, temperature, and fission rate. Figure 4-1 shows sample creep curves for a fission rate of 10^{14} fissions- cm^{-3} -sec.⁻¹

It is useful to define regions of dominance of alternate creep models to simplify their inclusion in a fuel pin performance model. Three creep mechanisms must be considered.

1. Diffusion-controlled thermal creep ($\dot{\epsilon} \propto \sigma$)
2. Dislocation climb thermal creep ($\dot{\epsilon} \propto \sigma^{4.5}$)
3. Fission-induced creep

From Equation 11, using available UO_2 creep data, [9] stress-temperature combinations can be defined at which diffusion creep is equal to dislocation climb thermal creep. Similarly a fission rate may be determined at which fission-induced creep is equal to thermal diffusion creep. Figure 4-2 shows the dominant creep mechanisms on a temperature-fission rate-stress diagram.

These results show that for a fission rate of 10^{15} fissions- cm^{-3} -sec⁻¹ (typical of LMFBR) fission-induced creep will predominate up to a temperature of $\sim 1800^{\circ}C$. Curves for stresses up to 10,000 psi are shown, although based on the CYGRO creep model, fuel stresses rarely exceed

Figure 4-1. UO₂ Fission-Induced Creep

3788-4

Figure 4-2. Dominant Fuel Creep Mechanisms

2000 psi. At a stress of < 2000 psi dislocation climb creep would only become important if $T > 2000^{\circ}\text{C}$ and $\dot{f} > 10^{16}$ fissions- $\text{cm}^{-3}\text{-sec}^{-1}$. It appears, therefore, that only diffusion thermal creep and fission-induced creep will be significant deformation mechanisms in LMFBR fuel pins.

It was decided that the omission of dislocation-climb thermal creep for LMFBR fuel pin modelling studies was justifiable. This simplification permits the retention of the original form of the creep law (see Equation 10). The exponent g is always unity and c varies with temperature and fission rate as shown in Figure 4-3.

Figure 4-3. Fission-Enhanced Creep in Oxide Fuels
3788-5

The inclusion of fission-enhanced creep significantly weakens the fuel at low temperatures. Figures 4-4 and 4-5 show the effects of the changes in the fuel creep model on the performance of a typical LMFBR fuel pin. Cladding stress, strain, and diametral changes are all substantially decreased. Growth of the central void is not affected, but at high burnups, it will be utilized more easily for swelling accommodation.

Fuel Swelling

The modelling of fuel swelling in CYGRO-F has been restricted to empirical "solid" swelling calculations. The stress-strain system developed around a

Figure 4-4. Effect of New Fuel Creep Model on Cladding Diameter Changes

Figure 4-5. Effect of New Fuel Creep Model on Cladding Stress-Strain History

void or bubble effectively prevent the operation of any swelling model which depends on the nucleation and growth of gas bubbles. The development of a gas bubble swelling model must therefore be preceded by a modification of this aspect of the stress-strain analysis.

The new stress-strain analysis under development is based on the concept proposed by Small.^[6] The analytical element is a spherical shell surrounding a single fabricated pore. Gas bubbles may nucleate and grow in the shell under the influence of the average stress, temperature, and fission rate. The external loading of the shell is determined by the behavior of similar elements in neighboring annuli of the fuel.

The dynamic swelling model which will be used with the above stress-strain analysis will describe the nucleation, growth, coalescence, and migration of gas bubbles. The basic features of the model will be similar to those employed in BUBL-I^[11] with two important differences. First, the Monte Carlo approach used in BUBL-I to describe the life history of a bubble is too complex to employ in CYGRO-F and will be replaced by an empirical nucleation rate, which may be controlled by structure or fission rate. Secondly, the loss of a gas bubble by migration will be followed by nucleation of new bubbles in the denuded region. Thus the constant volume increase in a given region predicted by BUBL-I will be approximated by a cyclic volume variation, but the radial expansion of that region will not be recovered. Dislocations and grain boundaries will be temporary traps for gas bubbles so that time-temperature relationships for these structural features will have to be developed.

SECTION 5

OFBA-310 ENCAPSULATED MIXED-OXIDE FUEL PIN IRRADIATION TESTS

A. Biancheria, B. L. Harbourne, and U. P. Nayak

OBJECTIVES

The objectives of this task are to plan, conduct, and evaluate in-reactor experiments of critical areas of special sensitivity to fuel-pin behavior analysis.

PRIOR WORK

Design, performance, and safety analyses of the WSA-1 and WSA-2 subassemblies were completed. The data package for WSA-1 and WSA-2 (except for four fuel pins with fuel motion restrictors) was submitted to the EBR-II Project. Subassembly WSA-2 was inserted in the reactor at the start of run 41B on March 27, 1970.

CURRENT PROGRESS

The data package for the four fuel pins with fuel motion restrictors and the as-built data for WSA-1 were submitted to the EBR-II Project early in this quarter. The capsules for WSA-1 were shipped to the EBR-II Site on May 8, 1970. The subassembly WSA-1 (designated X088 by the EBR-II Project) was inserted in the reactor at the beginning of run 43. At the end of run 43 (June 23, 1970), WSA-1 had accumulated an exposure of 1350 MWd, and WSA-2 had accumulated 2851 MWd.

A re-analysis of the WSA-1 and WSA-2 fuel pins was started using the OLYMPUS-IIA code. This information will be used to provide a basis for the design of the WSA-5 series of fuel pins which contains replacements for WSA-1 and WSA-2 fuel pins to be removed at the interim examination. Tentatively the interim examinations for WSA-1 and WSA-2 are scheduled at approximately 3 a/o (12,500 MWd exposure) and 4-5 a/o (15,000 MWd exposure) burnup, respectively.

The experimental planning for WSA-5 series fuel pins was initiated. A request for the "Approval in Principle" will be made during the next quarter.

SECTION 6

OFBA-311 UNENCAPSULATED MIXED-OXIDE FUEL PIN IRRADIATION TESTS

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OBJECTIVES

This task is part of the overall irradiation program on mixed-oxide fuel to study special areas of fuel-rod behavior and assembly performance. The specific objectives are to design, irradiate, and evaluate grid-type subassemblies with unencapsulated mixed-oxide fuel pins for the following purposes.

1. Demonstrating satisfactory performance of Type 316 stainless steel grid-type subassemblies in a fast reactor environment.
2. Investigating the combined effects of radiation, flowing sodium, stress, and temperature on the mechanical and corrosion properties of grid material and on pin-grid interaction.
3. Developing statistically significant design information on mixed-oxide fuel pin performance.

The grid-type subassembly is further intended to provide an irradiation vehicle which will minimize the interaction between individual pins.

PRIOR WORK

Parametric studies were performed to determine the effect of design variables and various operating conditions on the WSA-3 subassembly performance. An experimental plan was devised for which detailed thermal, mechanical, and nuclear analyses were initiated. The approval-in-principle was received for irradiation of WSA-3 in EBR-II. Fuel pin and subassembly hardware design drawings were completed. Hardware and most other components have been received. The safety analysis was performed and reported. The fuel pin shipping container was partially completed. Fuel pellet specifications were established and fuel fabrication was initiated.

CURRENT PROGRESS

Fuel Pin Design

Final design modifications were made to the WSA-3 fuel pins during the quarter. The changes, which were confined to the end plugs, are a direct result of discussions with EBR-II Project personnel regarding the reconstitution capability in the hot cell. Two sets of one-inch-long

undercuts were added to each end plug (top and bottom) to provide sufficient space between adjacent rows of pins for spacing tools to maintain proper pitch during assembly and disassembly. Also, the threaded stud on the top end plug was changed to an unthreaded stud with a pin inserted through it at a right angle to the pin axis. This will serve as a suitable grasping device to match the socket in a corresponding support rod to be utilized in hot cell handling for reconstitution. The EBR-II Project has agreed to evaluate and provide a concept for such handling.

Experimental planning for WSA-4 fuel pins was initiated. Calculations were made to determine what power ratings could be achieved by various pellet density, smear density, and fuel enrichment combinations in Rows 3, 4, and 5 of EBR-II operating at 62.5 MWt. For proposed linear ratings of 12-14 kw/ft, the most probable U-235 content in uranium would be 65-85%. Therefore, a request was subsequently issued to the AEC and approval obtained to downgrade fully-enriched material to 75% $\pm$ 10% for WSA-4 fuel pins. Work is continuing to determine the required fuel pin and pellet parameters to accomplish the prime objective of the experiment; i.e., operation in a grid-type subassembly with contact between pin and grid early in life and continuing to the end of life.

Subassembly Design - Thermal-Hydraulic

Final calculations of WSA-3 fuel pin nominal operating conditions and individual channel coolant temperatures were made in preparation for the experiment Data Package to be submitted to EBR-II. The results are shown in Table 6-1 and Figure 6-1. The values represent the current grid fabrication results of an average 0.283-inch pitch. The COBRA^[12] and NICE codes were used for WSA-3 operating in EBR-II position 6n1 at 62.5 MWt. The axial temperature profiles for the hottest (highest rated) pin, W3-1, are shown in Figure 6-2.

In order to more adequately characterize temperatures and effects in peripheral channels, a detailed bypass flow analysis was carried out. The work was performed using a complex hydraulic network which represents several grids and liners within the subassembly and the FLOPSY^[13] code. The schematic in Figure 6-3 shows the flow patterns which were, for analytical purposes, ultimately simplified to two major bypass flow regions and one main region. The results indicate that approximately 2% bypass flow occurs in each outer region; the annulus between the outer and inner can and that between the inner can and liner. Heat transfer to this total 4% bypass flow would result in a 25°F reduction in the coolant outlet temperature of the peripheral region, which is considered acceptable. Details of the analysis will appear in the Experiment Description and Safety Analysis package now in preparation.

Prior to calculating hot channel operating conditions for WSA-3, a re-evaluation of current uncertainty factors was performed. The previously used three-factor method was converted to a five-factor system, the individual components of which are shown in Table 6-2. The most significant addition is the channel closing subfactor which is based in part on the channel closing analysis briefly discussed in the mechanical

Table 6-1. Nominal Peak Operating Conditions

Pin Number	Linear Rating	Total Pin Heat Generation	Coolant Outlet Temp. °F	Cladding Outer Surface Temp. °F	Mean Cladding Temp. °F	Cladding Inner Surface Temp. °F	Fuel Surface Temp. °F	Fuel Centerline Temp. °F	Burnup Percent a/o
W3-1	11.316	11.316	1020	1036	1061	1086	1603	3735	5.00
W3-2	10.316	10.316	1018	1033	1055	1078	1542	3557	4.90
W3-3	10.701	10.701	1010	1025	1048	1072	1559	3566	4.70
W3-4	9.930	9.930	995	1009	1030	1052	1502	3341	4.60
W3-5	10.941	10.941	1026	1042	1066	1090	1585	3701	5.00
W3-6	11.033	11.033	1030	1046	1070	1094	1593	3669	4.90
W3-7	10.077	10.077	1022	1036	1058	1080	1530	3504	4.70
W3-8	10.406	10.406	1010	1024	1047	1070	1541	3484	4.60
W3-9	9.618	9.618	992	1006	1027	1048	1482	3253	4.40
W3-10	10.855	10.855	1022	1038	1062	1085	1577	3618	5.00
W3-11	10.667	10.667	1031	1047	1070	1093	1572	3645	4.90
W3-12	10.747	10.747	1025	1040	1064	1087	1573	3590	4.70
W3-13	9.757	9.757	1013	1027	1048	1070	1505	3426	4.60
W3-14	9.735	9.735	1001	1015	1036	1057	1495	3413	4.40
W3-15	9.362	9.362	983	996	1016	1037	1460	3317	4.30
W3-16	10.611	10.611	1014	1029	1053	1076	1557	3624	5.00
W3-17	10.584	10.584	1025	1040	1063	1087	1563	3545	4.90
W3-18	10.309	10.309	1022	1037	1060	1082	1545	3467	4.70
W3-19	10.406	10.406	1014	1029	1051	1074	1544	3487	4.60
W3-20	9.659	9.659	1003	1016	1037	1058	1492	3272	4.40
W3-21	9.333	9.333	988	1001	1022	1042	1462	3170	4.30
W3-22	8.969	8.969	968	980	1000	1019	1425	3054	4.10
W3-23	10.637	10.637	1014	1029	1052	1075	1558	3629	4.90
W3-24	10.389	10.389	1019	1033	1056	1079	1547	3575	4.70
W3-25	9.757	9.757	1012	1025	1047	1068	1504	3425	4.60
W3-26	10.069	10.069	1004	1018	1040	1062	1517	3386	4.40
W3-27	9.406	9.406	992	1005	1026	1046	1469	3332	4.30
W3-28	8.768	8.768	973	985	1004	1024	1417	3174	4.10
W3-29	10.264	10.264	1007	1021	1044	1066	1531	3443	4.70
W3-30	10.406	10.406	1009	1024	1046	1069	1541	3484	4.60
W3-31	9.442	9.442	1001	1015	1035	1056	1478	3346	4.40
W3-32	9.729	9.729	992	1005	1027	1048	1488	3283	4.30
W3-33	9.351	9.351	977	990	1011	1031	1455	3167	4.10
W3-34	9.707	9.707	992	1006	1027	1049	1487	3403	4.60
W3-35	10.026	10.026	993	1007	1029	1051	1506	3367	4.40
W3-36	9.079	9.079	983	996	1016	1036	1443	3252	4.30
W3-37	9.040	9.040	971	984	1003	1023	1432	3236	4.10

Figure 6-1. Coolant Temperature Distribution for WSA-3 in 6n1

Figure 6-2. W3-1 Axial Temperature Profiles (Nominal conditions)

3788-8

Figure 6-3. Flow Components in WSA-3

Table 6-2. Uncertainty Factors for 37-Pin Subassembly

	Coolant Enthalpy	Coolant Film	Fuel Cladding	Fuel-cladding Gap	Fuel	F _g ^E
<u>Fuel Deviations</u>						
Pellet diameter	-	1.005	1.005	1.005	1.005	1.005
Pellet density	-	1.015	1.015	1.015	1.015	1.015
U+Pu weight, %	-	1.001	1.001	1.001	1.001	1.001
Pu in U+Pu	-	1.003	1.003	1.003	1.003	1.003
Fissile Pu and U in Total U+Pu	-	1.001	1.001	1.001	1.001	1.001
Fuel pin fissile content	1.015	-	-	-	-	-
Fuel Stat. Factor	1.015	1.016	1.016	1.016	1.016	1.016
<u>Other Stat. Factors</u>						
Cladding thickness	-	-	1.020	-	-	-
Gap conductance	-	-	-	1.200	-	-
Film coefficient correlation	-	1.150	-	-	-	-
Sodium specific heat & density	1.040	-	-	-	-	-
Sodium conductivity	-	1.028	-	-	-	-
Cladding conductivity	-	-	1.018	-	-	-
Orificing uncertainty	1.030	1.012	-	-	-	-
Uncertainty in bypass flow prediction	1.010	1.003	-	-	-	-
Differential pressure at subassembly position	1.013	1.004	-	-	-	-
Total Stat. Factor	1.055	1.154	1.0313	1.201	1.016	1.016
Fuel Conductivity	-	-	-	-	1.100	-
Neutron & Gamma Flux Distribution	1.070	1.10	1.10	1.10	1.10	1.10
Channel closing ^(a)	1.068	1.035	-	-	-	-
Uncertainty in predicting flow distribution	1.050	1.015	-	-	-	-
Circumferential variation around cladding	-	1.360	-	-	-	-
<u>Power level variations</u>						
Level measurement	1.02	1.02	1.02	1.02	1.02	1.02
Transient overload	1.05	1.05	1.05	1.05	1.05	1.05
Total Factor	1.356	1.942	1.215	1.415	1.317	1.197
^(a) This subfactor includes grid cell variations, cold bow, thermal bowing and bow amplification.						

design section. Also, a subfactor for circumferential temperature variation of the cladding was re-evaluated using methods described in Reference 14. The major change in these factors is the manner in which they are derived. Many of the subfactors are combined statistically and then multiplied by non-statistical factors. The changes or improvements are more noticeable when compared to early factors tabulated in a previous quarterly report.[15]

Subassembly Design - Nuclear

The nuclear analyses to determine if WSA-3 is sub-critical when flooded and surrounded with an infinite water reflector has been completed. Results of the PDQ calculations indicate a K_{eff} of 0.35, which is well below the EBR-II limit. Detailed results are being documented for the EBR-II Data Package.

Subassembly Design - Mechanical

The flow induced vibration analysis described last quarter was completed. The resultant vibration frequency and amplitude were small for the conservatively assumed conditions.

The amplification of fuel rod bowing deflection due to thermal instability has been reassessed by including the effect of creep-relaxation of the bending stresses in the fuel rod. Conservative limits on flow channel closing were determined for various thermal gradients across the fuel rod by assuming complete creep-relaxation of the fuel rod bending stresses.

A modified version of the Rapier and Jones^[16] thermal stability analysis was used to calculate flow channel closing for various values of beginning of life temperature drop across the 37-pin subassembly fuel rod. The bowing resulting from the differential irradiation-induced swelling across the fuel rod is super-imposed upon the beginning of life thermal bow along with manufactured pre-bow to give the initial deflection (deflection before thermal instability effects are considered). The modification includes the effects of an axial compressive load on the amplification of initial deflection. The effects of biaxial straining of the cladding on the critical buckling load of the fuel rod have been included. Axial straining is produced by the axial compressive load. Circumferential and radial cladding strains are produced by fission gas pressure and/or fuel forced straining.

The amplification of initial bow due to critical buckling and axial loads is shown in Figure 6-4. These data were used for the channel closing-analysis. Results of the analysis are illustrated in Figure 6-5, which shows percentage channel closing against fuel rod temperature gradients. These data were used to arrive at the uncertainty factors mentioned earlier. These values are limiting quantities, since complete relaxation of the bending stresses in the fuel rod is assumed.

The grid support strap tab design was altered slightly to better accommodate deflections due to grid/inner can differential growth. Cut-outs were made along the support strap welds, as seen in Figure 6-6, to reduce the grid

Figure 6-4. Amplification of Initial Bow vs the Ratio of Axial Load to Critical Buckling Load
3788-10

Figure 6-5. Coolant Flow Channel Closing vs Temperature Drop Across Fuel Rod
3788-11

tab bending stiffness. Therefore, the tab stress analysis was revised. The detailed analysis has been prepared for description in the Data Package. The results show that for all possible operating conditions, the tab design and deflections present no problems and are conservatively safe. For an allowable grid tab bending stress limit of $1.5 S_m$, the grid tab deformation is unrestricted.

Figure 6-6. Grid Support Strap Tab and Analytical Model

3788-12

Program Activities

A final review of WSA-3 subassembly drawings with EBR-II Project personnel was conducted at the EBR-II site. With the exception of fuel pin end plug minor design changes, the design met with the Project approval. In their opinion, initial cold assembly and final post-irradiation disassembly can be effected using present standard practices. Reconstitution handleability remains a question which will be resolved after an evaluation by FEF personnel at EBR-II.

Grid fabrication has proceeded with success as noted in Task 410. Eleven grids were made for the mock-up subassembly, which was completely fabricated and flow tested during the quarter. Successful results of the tests are described in Task 411. This subassembly is now being readied for shipment to the EBR-II Site for developing handling techniques and procedures to be used for WSA-3.

Fuel and fuel pin fabrication is proceeding as discussed in Tasks 510 and 513. The required xenon tagging procedure has been tried, demonstrated in the presence of an EBR-II Irradiations Engineer, and tentatively approved for the fuel pins.

The 2R inner container for the 6M shipping package for fuel pins has been fabricated and sent to Westinghouse, where an internal holder will be constructed to accommodate the 37 WSA-3 fuel pins.

Cladding tubing and machined end plugs have been ordered for WSA-4, of which the plugs have been received. The tubing is scheduled for delivery in late July.

SECTION 7

OFBA-312 217-ROD GRID-TYPE FUEL ASSEMBLY DESIGN AND DEVELOPMENT

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OBJECTIVES

The objective of this program is to develop a grid-type fuel assembly for the LMFBR program and to provide a backup design for the FFTF reactor. This program will include the design, development, and evaluation of the grid, and grid-type assembly.

PRIOR WORK

A preliminary analysis was made of grid type, grid stresses, rod vibration, and grip strap wastage. Manufacturability studies were continued. A thermal and hydraulic parametric study was made for determination of fuel rod and grid design limits within the constraints of the FFTF geometry and coolant flow conditions.

CURRENT PROGRESS

Preliminary Thermal Design

General

The aim of obtaining significant metal swelling rates at high burnups over-rode the desirability of steeper temperature gradients in the choice of the assembly location in the core. For this reason the open loop location adjacent to the central assembly was selected as the preferred location for testing the gridded fuel assembly in the FFTF. In this location the linear power reaches 10 kw/ft at some period of core life.

Honeycomb type grids were assumed for analysis. The fuel rods were assumed to be identical with those of the FFTF driver fuel assemblies in size and fuel enrichment.

Hot Spot Factors

The hot spot factors were reviewed. Table 7-1 summarizes the current hot spot factor values which form the basis for the initiation of preliminary design. These factors will be reassessed during preliminary design of the gridded fuel assembly.

Preliminary Design

Following the parameter study and the mechanical analysis of the honeycomb grid, the following preliminary design choices were made.

Table 7-1. Preliminary Hot Spot Factors for Gridded Assembly for FFTF

	$F_{\Delta H}$	F_{Θ}	$F_{Clad.}$	F_{Gap}	F_{Fuel}
Non-statistical (a)					
Power control band	1.02	1.02	1.02	1.02	1.02
Inlet flow maldistribution	1.05	--	--	--	--
Intra assembly flow maldis	1.04	--	--	--	--
Local channel closing	1.06	1.08	--	--	--
Mixing & conduction	0.99	--	--	--	--
Circumferential temp. variation of cladding	--	1.25 (1.10) (b)	--	--	--
Direct Combination	1.17	1.377 (1.212) (b)	1.02	1.02	1.02
Statistical (3 σ)					
Power lever measurement	1.09	1.09	1.09	1.09	1.09
Orificing uncertainties	1.05	--	--	--	--
Assembly tolerance effect on flow	1.03	1.02	--	--	--
Unit cell flow area	1.05	1.050	--	--	--
Fuel fissile content, inch length	1.03	1.03	1.03	1.03	1.03
Cladding thickness and conductivity	--	--	1.12	--	--
Heat transfer coeff.	11	1.15	--	1.20	--
Fuel thermal conduc- tivity	--	--	--	--	1.10
Statistical Combination	1.122	1.175	1.154	1.22	1.139
Total Hot Spot Factor	1.313	1.618	1.177	1.25	1.162
(a) Nuclear uncertainties in calculation, data, etc. assumed to be included in Radial and Axial Nuclear Factors					
(b) For fuel temperature only					

Assembly Location in FFTR	Near central open loop
Number of Rods	217
Rod Diameter, (in.)	0.230
Triangular Pitch, (in.)	0.2875
Pitch to Diameter Ratio	1.25
Rod to Rod Clearance, (in.)	0.0575
Rod to Duct Edge Distance, (in.)	0.054
Number of Honeycomb Grids	14
Grid Strap Thickness, mils	13
Grid Strap Height, (in.)	1.156
Active Fuel Height, (in.)	36.0
Rod Length, Nominal, (in.)	92.0

These parameters led to the following performance data:

Assembly Mixed Mean Coolant Outlet Temp., (°F)	988
Maximum Cladding Temp., (°F)	1200
Maximum Fuel Temp. - Nominal Power - (°F)	4455
Maximum Fuel Temp. - 13% Overpower	4850
Rod Bundle Pressure Drop (psi)	77.5

The thermal performance of the assembly and the rods was analyzed by the use of the "NICE" and "COBRA" codes. The principal restraints were the cladding temperature limit of 1200°F and the available assembly pressure drop. Figure 7-1 illustrates that with a mixed mean coolant outlet temperature of 988°F, the assembly pressure drop just equals the available head. The corresponding maximum channel outlet temperature without hot spot factors calculated with the COBRA code is 1012°F. Applying the hot spot factors to the enthalpy rise, film drop, and cladding temperature drop, the maximum cladding inside surface temperature is 1200°F. The mixed mean outlet temperature cannot be raised without exceeding the cladding temperature limit, and it cannot be lowered without the required pressure drop exceeding the available head. More extensive study of the hot spot factors and the grid pressure drop are expected to widen the safe design margins without having to increase the rod pitch. The maximum pitch which would be accommodated in the present FFTR fuel assembly duct would be 0.289 inch ($P/D = 1.256$). The selection of the P/D would provide either a margin in the rod bundle pressure drop or allow a reduction in the assembly mixed mean coolant outlet temperature as illustrated in Figure 7-1. This choice is also under investigation.

Figure 7-1. Gridded Assembly for FFTF

3788-13

Subchannel Analysis

Flow and temperature distributions in the test assembly were obtained by the "COBRA" code.

A comparison of models representing one-half of the gridded fuel assembly, one-sixth sector of the assembly, and a strip model consisting of a strip two flow channels wide along a diametral plane across the assembly was made to check the accuracy of the methods. (Part of this investigation was funded under Westinghouse DGR funds.) Figure 7-2 shows the coolant temperatures at the core outlet for one-half of the gridded fuel assembly. (The flux is symmetrical about the plane connecting the assembly center to the center of the core.) Two percent turbulent mixing was assumed. In the absence of an edge channel grid strap design, a loss coefficient for the edge channels had to be estimated. Examination of the one-sixth sector of the assembly gave results which agreed exactly with those obtained when analyzing one-half of the assembly by COBRA at the same time. Therefore, parameter variation studies were and will continue to be made with a one-sixth sector. The diametral strip model did not produce quite as satisfactory agreement as the one-sixth sector shaped model. It differed by about 1%. Figure 7-3 shows the flow distribution of one-sixth of the core at the outlet.

The effect of the turbulent mixing factor was examined. This effect is presented in Figure 7-4 which shows that additional turbulent mixing increases the temperatures in the edge channels and decreases that of the inboard channels, thus reducing the temperature gradient, as expected.

The increase in maximum cladding and maximum fuel temperatures is shown in Figure 7-5 for steady overpower up to 15% for the "hottest" rods. The rods with the highest linear power rating (10 kw/ft) are located in the cooler edge channels. The maximum cladding temperature rises to 1297°F at 15% overpower. The central fuel temperature reaches the melting point at 13.8% overpower.

A COBRA run was made to determine how the assembly would perform with the same coolant rate if it were placed into the central core position. Its hottest rod would be located in the center and more rods would have high axial loadings. Figure 7-6 is the temperature profile of the assembly in the central and in the adjacent open loop locations. It shows that maximum outlet temperature after moving the assembly to the center is only 7° higher (1019°F) than that of the assembly in the near-central location. However, the mixed mean outlet temperature would be increased to 1000°F. Increase of the flow rate to reduce the maximum cladding temperature to 1200°F would result in about the same maximum outlet temperature for both cases.

The cumulative effect of increased grid spacing within manufacturing tolerances could, if distributed uniformly, cause a reduction in edge channel depth. The effect of this deviation from nominal dimensions is a reduction in temperature gradients across the outer rows of rods and a more uniform temperature distribution generally. This is illustrated by Figure 7-7 which was obtained by COBRA analysis.

ONE HALF OF GRIDDED ASSEMBLY IN OPEN LOOP LOCATION
OF FFTF ADJACENT TO CENTRAL ASSEMBLY

TOTAL ASSEMBLY FLOW RATE = 188.666 lb/hr

INLET TEMPERATURE = 600°F

ROD DIAMETER = 0.230 in.

PITCH/DIAMETER = 1.25

TURBULENT MIXING FACTOR = 2%

14 HONEYCOMB GRIDS

13 MIL STRAP THICKNESS

1.156 in. STRAP HEIGHT

GRID LOSS COEFFICIENTS:

INBOARD CHANNEL = 1.4

EDGE CHANNEL = 2.2

CORNER CHANNEL = 2.2

Figure 7-2. Core Outlet Temperature Distribution

1.25 PITCH/DIAMETER
 2% TURBULENT MIXING
 GRID LOSS COEFFICIENTS = 1.4
 $K_{\text{INBOARD CHANNEL}} = 1.4$
 $K_{\text{EDGE CHANNEL}} = 2.2$

EDGE CHANNEL
 INBOARD CHANNEL
 SUBCHANNEL FLOW RATE (lb/hr)

CORE
 C- CORE

CHANNEL IDENTIFICATION NUMBER

3788-15

Figure 7-4. Core Outlet Temperature of Selected Coolant Channels at Different Rates of Turbulent Mixing - By COBRA Code
3788-16

Figure 7-5. Gridded Assembly for FFTF Hot Rod Temperatures at Over-Power
3788-17

Figure 7-6. Coolant Outlet Temperature Profile by COBRA Code

3788-18

Figure 7-7. Effect of Edge Clearance on the Coolant Temperature at Core Outlet of Selected Flow Channels in the Sector Adjacent to the Central Assembly

Preliminary Mechanical Design

Design Basis

The operating conditions and design criteria for the 217-pin grid-type fuel assembly, which will be irradiation tested in an FFTF open loop, must meet both FFTF and LMFBR design objectives. The approach to satisfy both sets of requirements was reviewed by the Westinghouse FFTF and LMFBR design groups.

The LMFBR project prefers the highest achievable temperatures and fluences ($> 2.0 \times 10^{23} \text{ n/cm}^2$ @ $E > 0.1 \text{ MeV}$). Some of the major FFTF requirements are 10 kw/ft to match their hot rod rating, 80,000 MWD/Tonne peak burnup, the ability to achieve 400 MWt from a total core of grid-type fuel assemblies together with a peak flux of $7 \times 10^{15} \text{ n/cm}^2$, and the ability to meet the FFTF fuel assembly baseline pressure drop.

The basic design requirements [17] will be utilized to establish performance up to the FFTF design objective of 80,000 MWD/Tonne peak burnup. LMFBR design limits and requirements will be utilized to establish performance for greater burnups to meet LMFBR objectives of fluences in the range of $2.0 \times 10^{23} \text{ n/cm}^2$ ($E > 0.1 \text{ MeV}$) and peak burnups of greater than 100,000 MWD/Tonne where FFTF design criteria are not applicable.

The revised design requirements are given in Appendix A. (a)

Fuel Rod Cladding Sizing

The first step in the fuel rod design was to determine the gridded test assembly operating parameters compatible with the current design requirements. A parametric analysis was performed to determine the end-of-life fuel rod strain for various operating conditions. The parameters that were varied in the analysis were:

1. Peak fuel burnup
2. Fuel enrichment (fluence to burnup ratio)
3. Maximum fuel rod mid-wall cladding temperature
4. Fuel rod gas plenum volume

The total ductility limited strain was calculated for the FFTF core design goal of 80,000 MWD/Tonne peak burnup. The operating life was then extended until the total ductility strain exceeded the rupture strain limits given in section 5.3.2 of the design requirements (see Appendix A). The total ductility limited strain in the cladding hoop direction was calculated as the sum of the following components:

- (a) Appendix A contains Westinghouse Proprietary information and is transmitted to the Atomic Energy Commission for "Official Use Only."

1. Hoop strain induced by the cladding radial temperature gradient
2. Internal pressure stress-induced hoop strain
 - a. Short-time plastic strain
 - b. Primary thermal creep strain
 - c. Secondary thermal creep strain

The strain analysis included the following:

1. Cladding wastage losses throughout life due to
 - a. Sodium corrosion
 - b. Fuel-cladding reaction
 - c. Fretting-wear
2. Internal fission gas pressure increase throughout life
3. The effect of a tri-axial stress state on the effective hoop strain
4. The integrated effect of increasing stress on the primary and secondary thermal creep strain rates
5. The effect of three standard deviations on the variations in cladding material properties

The results of this analysis indicated that the current FFTF fuel rod design could be used in a grid-type fuel assembly to achieve LMFBR prototypical fluence and burnup goals without exceeding the allowable ductility limited strain given in the design requirements of Appendix A.

The operating parameters that meet the FFTF backup core requirements and the LMFBR prototype requirements are given below:

<u>Operating Parameter</u>	<u>FFTF Backup Core</u>	<u>LMFBR Prototype</u>
Life in full power days	349	545
EOL fission gas pressure (psi)	750	980
Maximum cladding mid-wall temperature (°F)	1156	1176
Cladding radial temperature difference (°F)	42	42
Nominal cladding thickness (in.)	0.015	0.015
Peak fast fluence (n/cm ²)	1.28x10 ²³	2 x 10 ²³
Peak fuel burnup (MWD/Tonne)	80,000	124,900

The ductility limited hoop strain is dependent upon local cladding temperature and fuel burnup which vary axially along the fuel rod. The reference fuel rod design was analyzed for maximum cladding temperature and maximum burnup to determine fuel rod ductility limited hoop strain for various levels of fuel burnup as shown in Figure 7-8. However, the maximum fuel burnup occurs at the central axial core location while the maximum cladding temperature occurs at the axial core outlet region. The effect of the axial variation in local cladding temperature and local fuel burnup on fuel rod cladding ductility limited hoop strain for the maximum fuel burnup of 124,900 MWD/Tonne is shown in Figure 7-9. Figure 7-9 also shows the reduction in fuel rod cladding hoop strain when mean valued material properties are used instead of 3 σ limits on cladding material properties.

Since several of the material parameters changed after the fuel rod analysis had been performed, the presented data is preliminary and calculations have to be repeated.

Fuel Assembly Coolant Duct Design

The grid-type fuel assembly coolant duct must be compatible with the remainder of the FFTF reactor core system. In the preliminary analysis, the current FFTF fuel assembly duct was considered for extension beyond its current design goal.

Differential axial thermal expansion of the coolant duct and fuel rod produces axial friction loading of the duct through the grid support structure. However, this additional axial loading is compensated by the elimination of the duct loading produced by the volumetric swelling of the wire-wrapped fuel rod bundle.

Since the coolant duct temperature and stresses are not excessive, the additional duct straining due to longer core residence time is not significant. The extended residence time results in a higher fast fluence level and additional duct swelling.

The requirement that the coolant duct must be compatible with the FFTF reactor core restraint system dictated use of the existing coolant duct design. An analysis of the reference duct design was performed based on the following assumptions:

1. The coolant duct temperatures were conservatively estimated as being equal to the adjacent coolant temperature with all hot channel factors applied.
2. The coolant duct was assumed to be subject to a peak fast flux level of $4.244 \times 10^{15} \text{ n/cm}^2/\text{sec}$. The fluence was proportional to time of exposure as measured by peak fuel burnup.
3. Volumetric swelling calculations were based on section 5.1.1 of Appendix A using global limits of +40% and mean values.

The results of these calculations, coolant duct swelling as measured across outside flats as a function of peak fuel burnup, are given in Figure 7-10.

Figure 7-8. Fuel Rod Cladding Strain for Reference Design

Figure 7-9. Axial Variation in Local Hoop Strain at 124,900 MWD/T Peak Burnup

Figure 7-10. Fuel Assembly Duct Swelling
3788-21

For the duct swelling with the + 40% confidence limit, interference with the adjacent assembly would begin at 135,000 MWD/Tonne peak burnup. This burnup level is 8% beyond the grid-type fuel assembly burnup goal of 125,000 MWD/Tonne.

Effect of Grid Axial Spacing on Flow Induced Vibration

The liquid sodium coolant flow induced vibration amplitudes of the grid-type assembly fuel rods have been estimated using a modified form of Burgreen's correlation.[18] The modification is the incorporation of the affect of an axial compressive load (produced by friction forces between fuel rods and grids) on the fuel rod vibration frequency and amplitude. The vibration amplitude, A, is given by Burgreen's correlation as follows,

$$A = [0.83 \times 10^{-10} K S^{1/2} Q]^{0.769} H \quad (17)$$

K has the value 5 for simply supported fuel rod spans, S is the ratio of the hydrodynamic forces of the fluid to the elastic restoring force of the fuel rod and is given by:

$$S = \frac{76.8 \rho_s V^2 L^4}{\pi^4 g EI} \left(\frac{1}{P/P_{cr}} \right)^2 \left[\sec \left(\frac{\pi}{2} \sqrt{P/P_{cr}} \right) - 1 - \frac{\pi^2}{8} P/P_{cr} \right] \quad (18)$$

Where: ρ_s = liquid sodium coolant density
 V = liquid sodium coolant flow velocity
 L = fuel rod span length
 g = acceleration due to gravity
 E = effective elastic modulus
 I = moment of inertia
 P = axial load
 $P_{cr} = \frac{\pi^2 EI}{L^2}$, critical buckling load of fuel rod span

Q , in Equation 17, is the ratio of the hydrodynamic forces of the fluid to the damping forces on the fuel rod and is given by

$$Q = \frac{\rho_s V^2 L^2}{0.8 \pi^2 \mu} \sqrt{\frac{\gamma_R}{g EI (1-P/P_{cr})}} \quad (19)$$

where: γ_R = length density of fuel rod including fuel
 μ = liquid sodium coolant viscosity

H , in Equation 17, is the hydraulic diameter and is given by

$$H = D_o \left[\frac{2\sqrt{3} C^2}{\pi} - 1 \right] \quad (20)$$

where: D_o = fuel rod outer diameter
 C = pitch to diameter ratio

For a general axial loading, the grid span length could be found such that the amplitude of vibration was at the maximum limit of 0.001 inch. Substituting $A = .001$, Equation 17 in terms of L becomes:

$$\left(\frac{.001}{H} \right)^{1.3} = 0.83 \times 10^{-10} K (\bar{S})^{1/2} \bar{Q} L^4 \quad (21)$$

where: $\bar{S} = S/L^4$
 $\bar{Q} = Q/L^2$

The span length, L, is given by:

$$L = \left[\frac{(.001/H)^{1.3}}{0.83 \times 10^{-10} K (\bar{S})^{1/2} \bar{Q}} \right]^{.25} \quad (22)$$

The fuel rod span length was calculated for various ratios of (P/P_{cr}) for both the reflector and plenum regions of the fuel rod. The following end-of-life conditions were assumed:

<u>Parameter</u>	<u>Reflector</u>	<u>Plenum</u>
Mean sodium coolant density (lb/in ³)	0.0297	0.0297
Mean sodium coolant viscosity (lb/ft-hr)	0.570	0.570
Mean sodium coolant velocity (ft/sec)	30.0	30.0
Fuel rod pitch (in.)	0.286	0.286
Fuel rod modulus of elasticity (psi)	21.5x10 ⁶	21.5x10 ⁶
Fuel rod E.O.L. outside diameter (in.)	0.225	0.225
Fuel rod E.O.L. inside diameter (in.)	0.205	0.205
Fuel rod cross-sectional area (in ²)	0.0398	0.00676
Fuel rod weight density (lb/in ³)	0.283	0.283
Fuel rod area moment (in ⁴)	3.91x10 ⁻⁵	3.91x10 ⁻⁵

Figure 7-11 shows the allowable ratio of axial fuel rod load P to the critical buckling load, P_{cr} , for a given grid span length such that the amplitude of fuel rod vibration in the core and reflector regions will not exceed the allowable limit of 0.001 inch. For the fission gas plenum, the allowable axial loading grid span length relationship for 0.001 inch fuel rod vibration is shown in Figure 7-12.

The analysis indicates that flow induced vibration effects the spacing between the grids only in the region outside the core zone. Thermal bow limits grid spacing in the core zone as indicated in the following two subsections.

Effect of Grid Axial Spacing on Fuel Rod Thermal Bow

The fuel rod bowing was calculated using the STASYS [19] computer code which considers the bowing due to differential thermal expansion, differential material swelling, and differential irradiation-induced creep.

Figure 7-11. Core and Reflector Region Fuel Rod Vibration Analysis
3788-22

Figure 7-12. Gas Plenum Region Fuel Rod Vibration Analysis
3788-23

The analysis considered the fuel rod next to the duct wall. The peak fluence of $2 \times 10^{23} \text{n/cm}^2$ was based on a residence time of 546 days (124,900 MWD/T Burnup) in the near-central closed loop test position. Although this test position has high flux levels, the radial flux gradient is small.

Since the irradiation-induced creep is stress dependent, axial loads were applied at each grid location shown in Figure 7-13. Based upon the assumption that all three dimples contact the fuel rod with a radial load of 1.5 pounds at each dimple and a coefficient of friction = 1.0, the axial load is 4.5 pounds per grid cell.

Fuel rod bowing due to both fluence and temperature gradients is plotted in Figure 7-14.

This analysis has neglected flow channel closure effects due to thermal instabilities. The maximum bow in the core is 0.56 mils. This should not produce sufficient thermal gradients to significantly amplify the bow due to thermal instabilities.

Significant results of this analysis are given below:

<u>Region</u>	<u>Maximum Bow (mils)</u>	<u>Maximum Bow Induced Axial Cladding Stress (psi)</u>
Lower reflector	0.07	45
Active core	0.56	3.350
Upper reflector	1.00	3.565
Gas plenum	2.65	3.935

Thermal Stability Analysis

A modified version of the Rapier and Jones^[20] thermal stability analysis is used to calculate flow channel closing. The bowing resulting from the differential irradiation-induced swelling across the fuel rod is super-imposed upon the beginning of life thermal bow along with manufactured pre-bow to give the initial deflection $\hat{z}_0$ (deflection before thermal instability effects are considered). The modification includes the effects of an axial compressive load on the amplification of initial deflection $\hat{z}_0$. The effects of biaxial straining of the cladding on the critical buckling load of the fuel rod have been included. Axial cladding straining is produced by the axial compressive load. Circumferential and radial cladding straining are produced by fission gas pressure and/or fuel forced straining.

Figure 7-13. Fuel Rod Grid Spacing

Figure 7-14. Fuel Rod Bowing Due to Temperature and EOL Fluence Gradients
3788-25

The analytical model assumed is a pinned-pinned single-span beam with normalized axial coordinate X . Since the fuel rod span of this model is stress free, the deflections obtained shall be the limit of those in one span of the multispan (grid supported) fuel rod after complete creep-relaxation of the stresses in the fuel rod.

In Reference 16, Rapier derives an expression for the thermal bow of heated fuel rods cooled by forced convection.

$$L(Z_i) = \frac{\partial^2 Z_i}{\partial X^2} + R(Z_i + Z_o) + RS \int_0^1 (Z_i + Z_o) dX = 0 \quad (23)$$

where: Z_i = induced bow

Z_o = initial bow

R and S = parameters given in Reference 16.

X = axial coordinate

In the above equation the axial load can be included by adding the term,

$$\frac{P \cdot \ell^2}{EI} (Z_i + Z_o)$$

The resulting equation is now:

$$\frac{\partial^2 Z_i}{\partial X^2} + \frac{P \ell^2}{EI} (Z_i + Z_o) + R(Z_i + Z_o) + RS \int_0^1 (Z_i + Z_o) dX = 0 \quad (24)$$

Z_o is the initial bow before the axial and thermal loads have been applied. The induced bow Z_i is produced simultaneously by a thermal gradient and axial load. For $R = 0$, Equation 24 is the second order differential equation for buckling of a predeformed beam. The axial load is a dead weight load.

The differential Equation 24 is solved approximately by Galerkin's method. For a simply supported beam a sine deformation mode is the exact buckling mode. For a short mixing length, it is also a good approximation for the thermal bowing mode.^[20] For predeformation and induced bending, the same mode will be assumed to satisfy the static and dynamic boundary conditions.

$$Z_i = A \sin \pi \cdot X$$

$$Z_o = B \sin \pi \cdot X$$

Substitution into equation (24) gives:

$$\pi^2 A \sin \pi \cdot X + \frac{P \cdot \ell^2}{E \cdot I} \cdot (A+B) \sin \pi \cdot X + R (A+B) \sin \pi \cdot X + R S (A+B) \frac{2}{\pi} = 0 \quad (26)$$

The integral in Equation (24) can be solved before the Galerkin integration.

With Equation 25 as an approximation function for Z_i , the Galerkin method minimizes the error in A. The method requires the following integration,

$$\int_0^1 L(Z_i) \sin(\pi \cdot X) dX = 0 \quad (27)$$

With:

$$\int_0^1 \sin(\pi \cdot X) \cdot dX = \frac{2}{\pi} \quad (28)$$

the resulting equation for A is:

$$\pi^2 A + \frac{P \ell^2}{EI} (A+B) + R(A+B) + RS (A+B) \frac{4}{\pi^2} = 0 \quad (29)$$

The ratios of the induced bow A to total bow (A+B) in the rod center is:

$$M = \frac{A}{A+B} = \frac{\ell^2 P}{\pi^2 EI} + \frac{R}{\pi^2} \left(1 + 4 \frac{S}{\pi^2}\right) \quad (30)$$

The last term of Equation 30,

$$\frac{R}{\pi^2} \left[1 + \frac{4}{\pi^2} S\right] = M_{th} \quad (31)$$

is the ratio of the induced thermal bow to the final bow. This equation is identical with Equation 37.31 of Reference 16, but is derived in a different way.

The first term of the right hand side of Equation 30 is:

$$\frac{P \ell^2}{\pi^2 EI} = \frac{P}{P_{cr}} = M_s \quad (32)$$

P_{cr} is the buckling load of a simply supported beam. P/P_{cr} is also the ratio of the bow induced by the axial load to the total bow. This can be easily derived from Southwell's formula for buckling of a rod with predeflection.

With Equations (31) and (32) in mind, we can simplify Equation (30):

$$\frac{A}{A+B} = M = M_s + M_{th} \quad (33)$$

The ratio of the bow induced by an axial load plus thermal load to final bow is the sum of $M_s + M_{th}$. M_s is the ratio of the bow induced by axial load to the total bow. The total bow is the sum of initial bow and induced bow due to axial load plus thermal load.

The calculations have been performed with very conservative temperature profiles, fission gas pressures, and material parameters. The maximum flow channel closing in the core region is 8.10% which is less than the 10% closing limit given in the design requirements in Appendix A.

This analysis shows that the fuel rod bow due to thermal gradients and fluence gradients is insignificant. However, since a possible enhancement of the fuel rod bow due to internal pressure and axial load has not been considered, the analysis must be considered preliminary. A preliminary assessment of this effect is underway. It will be a major effort to implement both effects into the matrix analysis of the fuel rod.

Honeycomb Grid Plate Bending Stress Analysis

The stress analysis of the grid is based on the method given in Appendix E of Reference 21. This method assumes the grid loading to be the result of flow-induced pressure drops and friction interaction with the fuel rods. The friction interaction is converted to an equivalent pressure drop and combined with the flow induced pressure drop to obtain an effective uniform loading acting over the grid plate. The stresses produced by thermal and irradiation effects are then combined.

The honeycomb grid has been modeled as a perforated circular plate such that the load per unit area on the model is the same as that on the actual grid. The honeycomb grid cell is shown in Figure 7-15.

The parameters used in the analysis are shown in Table 7-2.

The stress limit at 1120°F is 11,560 psi. For 135 full stress cycles during the fuel assembly lifetime specified in the design requirements of Appendix A, the fatigue stress is not the limiting stress.

The grid bending stress has been calculated assuming a friction load of 1.5 pounds between the fuel rod and each of three dimples per grid cell. The required grid height for variable grid strap thickness is plotted in Figure 7-16.

Figure 7-15. Honeycomb Grid-Dimple Design (Dimensions in inches)

3788-26

Figure 7-16. Grid Dimensions for an Allowable Stress of 11,560 psi
3788-27

Table 7-2. Parameters Used in Bending Stress Analysis	
Strap thickness (t)	0.012 - 0.015 in.
Grid height (h)	0.75 - 01.75 in.
Number of fuel rods	217
Temperature of grid	1120°F
Pressure drop across each grid	5 psi
Sodium velocity	30 ft/sec
Oxygen content of coolant	10 ppm
Modulus of elasticity (E)	22×10^6 psi
Poisson's ratio (ν)	0.3

The grid design must also meet available pressure drop requirements. Comparison of the available pressure drop in FFTF, and the pressure drop required for a grid design shows that two combinations of grid height and grid strap thickness are acceptable.

<u>Strap Thickness</u>	<u>Grid Height</u>	<u>Minimum Mixed Mean Outlet Temp., °F</u>
0.013	1.156	983.5
0.014	1.125	990.5

Comparison of the available and required pressure drops for different reactor outlet temperatures are shown in Figure 7-17.

Grid Dimple Design Analysis

The grid dimple has been incorporated into the honeycomb grid design primarily to limit the radial grid-fuel rod interaction load due to fuel rod swelling to an acceptable level. The grid dimple interaction loads determine the fuel rod axial friction loading; a 1.5 pound dimple load has been found to be acceptable. The grid dimple design was based upon the following requirements.

1. The initial grid dimple load due to fuel rod misalignment will not cause grid dimple collapse or strap buckling.
2. The fuel rod growth at end-of-life will not cause grid dimple collapse.

The grid dimple shown in Figure 7-15 was approximated as a fixed-fixed beam with an effective length to account for the stiff dimple radius and the diffusion of the bending stress into the parent grid strap. Calculations were based upon a beginning of life grid dimple thickness of 13 mils. The dimple deflection was based upon the elastic-plastic strain of cold-worked Type 316 stainless steel @ 1100°F. It is conservative to neglect the reduction of fuel rod-dimple load due to grid thermal and irradiation-induced creep. Figure 7-18 shows the grid dimple deflection as a function of the grid dimple load.

Figure 7-17. Variation of Fuel Assembly Pressure Drop with Mean Coolant Outlet Temperature for 14 Honeycomb Grids

3788-28

Figure 7-18. Fuel Rod - Grid Dimple Interaction Loads for a Beginning of Life Dimple Thickness of 13 Mils at 1100°F

3788-29

The differential fuel rod-grid radial growth at end-of-life was approximately 6.46 mils. The grid dimple load at this deflection is below the dimple load considered in the grid stress calculation (1.5 pounds). Assuming a fuel rod misalignment of 0.001 in. per inch of grid span length gives a dimple load at beginning of life of 0.933 pounds at cold condition. For a dimple width of 0.11 inches, this load produces no plastic deformation.

On the basis of this analysis, the grid dimple dimensions are selected as follows:

1. Dimple cutout length - 0.500 in.
2. Length of dimple - 0.125 in.
3. Dimple strap thickness - 0.110 in.
4. Dimple width - 0.110 in.

These dimensions are shown in Figure 7-15.

Line of Sight Grids

In addition to the honeycomb grid several alternative grid configurations have been reviewed. Two line-of-sight grid configurations, one with straight grid straps in three different planes (Triplanar grid), and the other with intersecting grid straps in a single plane (Monoplanar grid), have been analyzed. Both configurations may cost less than a honeycomb grid.

During the preliminary structural analysis of the two line-of-sight grid concepts, it was determined that modifications must be made for each design to satisfactorily accommodate differential growth between the fuel rods and grid.

SECTION 8

OFBA-313 FABRICATION, DEVELOPMENT, AND TESTING OF LMFBR FUEL ASSEMBLIES

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and W. L. Wilson

SUBTASK 1. HEXAGONAL GRID-FUEL ROD FRETTING AND WEAR TEST

OBJECTIVES

The objectives of this program are to evaluate the wear damage caused by those grid-pin interactions, induced in a fuel pin assembly typical of WSA-3 design, when operating in dynamic sodium, (WSA-3 will be the first 37-pin grip-type subassembly to be inserted in EBR-II) and to similarly evaluate a 37-pin subassembly which will utilize the final grid design proposed for the first 217-pin subassembly designed for irradiation in FFTF.

PRIOR WORK

The drawings for the subassembly and for the test capsule were completed and detailed. Quotes were received for the tungsten carbide pellets and for the fuel pin end plugs.

Operation of the DFAT test rig, which is to be modified for the Subassembly Fretting Test (SAFT), showed that the air blast cooler in the by-pass line was inadequate. Design of a replacement cooler was initiated.

CURRENT PROGRESS

Program Planning

The original program plan called for four fretting tests, but this was reduced to two (see first quarterly report). Because of this cut back, no data would have been obtained on the effect of varying the dimple diameter on wear behavior. The objectives of the first fretting test were therefore changed to obtain these data.

This change in approach required that grids be produced with various dimple circle diameters within each grid. Variations in fit were also required along the length of each rod. Many of the rods will see the nominal diametral gap along their entire length, while others see varying diametral gaps.

The dimple diameter was specified for each grid cell in each of the eleven grids to give a range of fits. These dimple diameters were considered the most suitable for determining the effects of fuel rod fit on fretting and vibration within the assemblies.

Test Subassembly Fabrication

Eleven grids were fabricated and carefully inspected using 100 to 1 magnification ratio in the Optical Comparator Inspection Procedure. After inspection and measurement of each cell the grids were hand worked to produce the required dimple circle diameters according to the grid maps which had been drawn up.

Dimple circle diameters were increased by using gage pins. Generally the required diameter was obtained by using a pin three mils larger than the required measurement due to spring back. A hand tool was designed which could be used to extend the dimples, thus decreasing the circle diameter. This tool functioned adequately.

The grids were reinspected after hand working and all dimple circle diameters were within 1 mil of the required value except for a few cells which were accepted with deviations within 2 mils.

Forty dummy fuel pins were fabricated and shipped from Cheswick to ARD.

The grids and liners have been inserted into the inner can, all machining on the subassembly is complete and the components are ready for final assembly.

Test Facility Modifications

Approximately 35% of the machining work for the test capsule was completed. Final machining and welding cannot be done until DFAT is terminated and the test section removed. This should occur towards the end of July.

A new heat exchanger for the by-pass loop was designed and components were ordered. Test cell thermocouple instrumentation drawings were completed and released for fabrication.

SUBTASK 2 - SODIUM CORROSION OF FUEL SYSTEM WELDMENTS AND BRAZEMENTS

OBJECTIVES

The purpose of this task is to provide quantitative sodium corrosion data from welded and brazed joints exposed under conditions simulating actual LMFBR service. The program has been designed to examine the corrosion behavior of welded and brazed fuel pin spacer grid joints and simulated end closure welds principally by weight change and metallography.

PRIOR WORK

Not Charged to this Contract

Operating experience with sodium cooled reactors indicated that weldment corrosion should not be more severe than base metal corrosion. However, only very limited quantitative data[22,23] are available on sodium corrosion of welds at 1200°F.

The selection of brazing alloys for service in sodium has been studied at ORNL, [24] AI, [25] UKAEA, [26] AND CEN-EURATOM. [27]

A literature survey indicated that several fuel rod support grid brazing alloys, including Nicrobraz 50, were potentially the most suitable for further sodium corrosion evaluation, prior to a selection being made for a Westinghouse LMFBR design application. Accordingly, a sodium test loop, WBTL-1, was constructed and operated for 3500 hours over the temperature range 900-1230°F to evaluate Inconel 718 fuel rod support grid joints brazed with Nicrobraz 50. Further details and preliminary results from this work will be reported in Task NCAA-300 on Contract AT(30-1)-4173. This loop system has since been rebuilt and is being used in this present subtask.

Charged to this Contract

A program involving the evaluation of the effects of exposure to sodium on several types of Type 316 stainless-steel fuel-system jointures was designed. Spot welded grid type samples, brazed joints, and simulated end closure weld samples are being tested.

The behavior of welds in both annealed and cold-worked stainless steel after exposure is being examined. Unwelded samples are included for comparison purposes. A limited number of end closure welds containing a known defect are also being tested. One test section contains brazed grid samples, for some of which a post brazing homogenization treatment has been included prior to exposure to sodium.

An existing sodium corrosion testing loop was redesigned and rebuilt for operation under this task. The loop system (WBTL-2) is a modification of the Materials Testing Loops (MTL), which have operated successfully at ARD during the last few years.

This loop was designed with five test section locations, two of which operate at 900°F and the remaining three at 1200-1230°F. Within any one of the three test sections containing the grid type samples, three velocity regions (2.5, 6.5, 9.5 fps) have been arranged by variation of the sample size and sodium flow area. In the two test sections containing the end closure welds the sodium velocity is constant at 9.5 fps. It is intended that the system will operate for approximately 3000 hours at temperature. At the end of the previous period, 1250 hours at reference sodium conditions had been accumulated.

CURRENT PROGRESS

Analyses during the operation of WBTL-2 have indicated that the oxygen content of the sodium has been maintained below 10 ppm. Although it was originally intended to continue the run for about 3000 hours, WBTL-2 was shutdown during the current period after 2100 hours of operation at reference conditions. This decision was made since some of the loop components, such as the pump and flowmeter, were required for WBTL-3 and because redesign of this system was essential to reduce downtime.

The objectives and design of WBTL-3 are reported under Task NCAA-300 of Coolant Technology Branch Contract AT(30-1)-4173.

WBTL-2 has been disassembled and the samples removed, cleaned, and weighed. Meanwhile, identical samples have logged 2200 hours at 900°F and 1200°F in out-of-sodium thermal aging capsules. Since these samples are mixed with Inconel 718 samples for comparison with those in WBTL-1, they will be aged for 3500 hours, which was the length of the WBTL-1 run.

CURRENT PROGRESS

Materials

Samples of grid strap material, supplied by Hamilton Precision Metals, have been spectrographically analyzed to check the chemical composition. These analyses confirmed the ladle analysis and Hamilton check analysis except for a deviation in nitrogen content on one of the samples examined; five samples showed nitrogen contents between .033 and .041 wt.%, the sixth sample was reported to have .063 wt % nitrogen. The batch of material having the higher nitrogen content will be used for out-of-pile subassembly tests. All of the subassembly structural materials have now been analyzed to check the suppliers reported chemical analysis. Final inspection of liners, inner cans, and outer cans order from Carpenter Technology was witnessed at the Carpenter plant March 30-April 2. All of these components have been delivered to ARD.

Grid Fabrication

There are three primary stages in the fabrication of a 37-pin grid,

1. Resistance welding corrugated straps to form the honeycomb array called an inner grid assembly.
2. TIG welding six formed straps to form a hexagonal support strap assembly which is slotted and contoured to fit the profile of the inner grid assembly.
3. TIG welding an inner grid assembly into a matching support strap assembly to form a completed grid assembly.

The various subassembly components are identified in Figure 9-1. During the current period final adjustments were made to the grid strap forming tool as reported under OFBA-411, and inner grid assemblies were completed for the flow test subassembly and the fretting test subassembly. Fabrication of inner grid assemblies for WSA-3 was also started.

A critical feature of the support strap assembly design is the profile of the flow tabs which are intended to provide a 0.001-0.005-inch peripheral gap around the inner grid assembly. Accumulation of tolerances on both the inner grid assembly and the support strap assembly is the primary problem in producing assemblies with the correct gap. Specific problems that have been identified and controlled are as follows:

1. Twisting of the support straps during welding
2. Electrode dimensions for machining flow tabs
3. Positioning of tab machining electrode to be correctly oriented and concentric with the support strap assembly
4. Slot width, pitch, and true position

Figure 9-1. Subassembly Components; Grids, Liners and Cans
3679 -60

5. Distortion of grids and support straps while fitting grids and support straps together
6. Thermal distortion of grid and support straps during welding of the grid to the support strap assembly

The preceding sources of error in the peripheral flow gap are in addition to any accumulation of tolerances on the inner grid assembly pitches and cell center true positions. The approach adopted to control the peripheral gap has been to machine the flow tabs undersize and then push them in to fit the profile of the inner grid assembly during welding of the support strap assembly to the inner grid assembly. This approach works only when true positions of cell centers, flow tabs, and slots are accurate within approximately 0.005 inch.

Another aspect of welding the inner grid assembly to the support strap assembly is control of dimple circle diameter and pitch in the peripheral region of the grid. The objective is to hold the same tolerances obtained during resistance welding of the inner grid assembly; i.e., $\pm .001$ on dimple circle diameter and $\pm .003$ or better on the pitch. The first problem encountered in this assembly operation was that the fixture locating pins were on a 0.285 inch pitch, approximately 0.002 inch larger than the actual grid pitch nominal. Consequently, one of the dimples of the inner grid was deformed by the fixture to give an unacceptable dimple circle diameter. The original fixture pins were removed and the grid cells were positioned by nine locating pins from the resistance welding fixture, located in a small plate with holes on a 0.283-inch pitch. The modified fixturing effectively controlled the peripheral pitches but as many as four peripheral cells were deformed to give out-of tolerance dimple circle diameters. It was decided that the inner grid assemblies for the flow test would be made with oversize straps so that all of the peripheral cell dimple circle diameters could be fixed on the resistance welding fixture. These inner grid assemblies were cut to size prior to inspection, with the peripheral cells held in place with extra rows of half-size weld nuggets; the other half of these welds are removed during cutting. By controlling the dimple circle diameter of these peripheral cells prior to final grid assembly welding, the fixturing requirements during grid assembly welding were less demanding, and acceptable grid assemblies could be made.

Inspection data on the grid assemblies were reviewed by Reactor Engineering and flow test subassembly grids were selected on the basis of dimple circle diameter, cell pitch, peripheral flow gap, tolerance accumulation and flow channel area distribution. Representative samples of grid joints were radiographed using a variety of techniques. It was concluded that radiography was not suitable for grid inspection because of the accessibility problem and lack of sensitivity to the presence of defects. Consequently, the previously reported periodic destructive testing during fabrication is still the prime method of assuring grid joint quality.

Liner Fabrication

The indexing system for electrodischarge machining the liner flanges has been modified to produce acceptable flange concentricity; at one end of

The liner, the flange must fit into a support strap assembly, and at the other end, the flange must fit the inner can. Approximately 50 liners have been flanged to size during the current period.

Spacers were resistance welded to liners for the flow test assembly and these liners were fitted to their respective grids. The corner radii of these liners had to be ground to provide a smooth fit into the grid assemblies.

Bottom Grid Fabrication

A first attempt at welding keys (or T-bars) into a bottom grid support indicated that no gross thermal distortion occurred. However, the bottom grid support used for this trial was slotted by milling and the slot dimensions were out-of-tolerance on both key pitch and true position. Subsequent hand working provided acceptable key pitches and true position; this bottom grid was used for the flow test subassembly.

Slots in another two bottom grid supports were made using electro-discharge machining and dimensional control of slot pitch and true position was greatly improved. Subsequent welding of keys into these slots provided bottom grids within tolerance.

Subassembly Assembly

The liner loading fixture and grid loading fixture, both procured under OFBA-411, were received and fitted with welding torch positioners to facilitate location of components during loading into the inner can. These fixtures were proof tested by welding two grids and two liners into a section of inner can. Dimensional inspection of the loaded grid and liner positions indicated that both fixtures performed satisfactorily.

To facilitate loading of grids and liners into the inner can, support fixtures have been designed and made. The primary function of these fixtures is to hold the inner can straight and rigid so that the possibility of misalignment during loading of grids is minimized.

Fabrication of the inner can assembly for the flow test subassembly was completed; an inner can assembly consists of an inner can containing eleven grids and ten liners welded into position. Minor positioning problems were encountered during loading of the grids and modification to the grid loading fixture were made to provide more effective support and positioning of the grids.

A six foot length of 0.2308-inch diameter, 20% cold worked, stainless steel tubing was pushed through all 37 cells of the 11 grids. Loads required to extract this length of tubing from representative grid cell positions were measured with a simple spring balance. Central grid positions required approximately 1 to 1-1/2 pounds to extract the tube. The majority of the peripheral grid positions required approximately 2 to 2-1/2 pounds to extract the tube. The tightest position was at a corner cell where 3 to 3-1/2 pounds was required to extract the tube. It was concluded that there would be no difficulty in loading the 0.2300-inch diameter dummy fuel pins, which are 0.0008-inch smaller in diameter.

The inner can assembly was then radiographed to check axial grid and liner gaps. In addition, two peripheral grid cells were loaded with dummy fuel pins to check the gap between the pins and the liners; a typical radiograph is shown in Figure 9-2. The radiography confirmed that all components were satisfactorily positioned, but, precise measurements could not be obtained because of parallax. The dummy fuel pins were loaded by hand and fitted to the bottom grid support; subassembly components are shown in Figure 9-3. The outer can was then pushed onto the inner can and the end fittings were attached as shown in Figure 9-4. Flow test instrumentation was then attached and the subassembly was passed on to the Thermofluids group for testing. Screws rather than welds were used for all final assembly operations to facilitate disassembly for orifice changes.

A meeting was attended at the EBR-II site to discuss assembly and disassembly procedures for the irradiation test subassemblies. Quality control requirements for subassembly structure hardware were also discussed and ARD will provide the EBR-II Project with a proposed quality control plan for their review and approval. A verbal presentation of this plan was made at the meeting.

Figure 9-2. Typical Radiograph of Grid and Liner Positions in Inner Can Assembly
3788-30

Figure 9-3. Flow Test Subassembly Components Prior to Final Assembly

Figure 9-4. Completed Flow Test Subassembly

SECTION 10
OFBA-411 SUBASSEMBLY DESIGN AND HARDWARE

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SUBTASK 1. SUBASSEMBLY HARDWARE

OBJECTIVES

The objective of this subtask is to design and fabricate tooling necessary for forming grid and subassembly components. Mechanical design of subassembly hardware, consistent with the design analysis of OFBA-311, is also included in this subtask.

PRIOR WORK

Mechanical design of all subassembly hardware and tooling fixtures was completed. Manufacture and procurement of the fixtures were completed with the exception of the grid and liner inserting tools. Major checkout, modification, and trial operations of the grid strap forming tool were nearly completed.

CURRENT PROGRESS

During this quarter a grid was produced with all cells having dimple circle diameters within the range 0.232 ± 0.0008 . The permanent grid tool shims were ground to the same size as the temporary shims which were used for this grid.

The liner inserting tool and grid inserting tool have been received and checked out. At the close of this quarter all tooling had been received and made workable.

SUBTASK 2. FLOW TESTING

OBJECTIVES

The objectives of this subtask are to plan, conduct, and evaluate out-of-pile flow tests intended to characterize hydraulic parameters of 37-pin subassemblies.

PRIOR WORK

A housing was designed to permit mounting of the 37-pin assembly in the thermofluids water loop. Detailed construction drawings were issued to the ARD model shop. Manufacturing was started.

CURRENT PROGRESS

The hydraulic test loop was cleaned. A flow orifice was installed and calibrated by the weigh-tank method. Manometers, thermometers, and pressure gages were installed. The test housing was completed by the model shop and was fitted to the loop. Upon receipt of the assembly, pressure connections were attached up-and downstream of the flow orifice and of a grid. The assembly was installed in the housing and instrument connections were completed. Figure 10-1 is a photograph of the test section in place in the loop showing:

1. Circulating pump
2. Manometer for flow orifice
3. Rotameter
4. Orifice flanges
5. Housing
6. Pressure tap connections
7. Manometers
8. Flow straightening section
9. Loop cooler

Flow tests were made by adjusting the flow to a constant value, adjusting the cooling water rate to maintain a constant loop temperature, waiting until the manometers had reached equilibrium with the respective pressures in the test section, and recording the readings. The first orifice had been dimensioned for the 5-N-2 position and gave too much flow at a head corresponding to 41 psi sodium at 850°F mean temperature. A second orifice containing seven sharp-edged $0.198 \pm \begin{smallmatrix} .001 \\ -.000 \end{smallmatrix}$ inch diameter holes gave a flow rate just slightly above the desired 33.7 gpm. The characteristic pressure drop equation was determined by a combination of hand analysis of inlet and outlet pressure drops and results from COBRA runs of the rod bundle. It is based on the velocity of the 1.125-inch diameter inlet passage flow area.

$$\Delta p = \frac{48.64}{144} \times \frac{\rho V^2}{2g} + 50.39 \left(\frac{V}{V}\right)^{0.2} \times \frac{\rho V^2}{2gx144} + \frac{\rho H}{144} \quad (34)$$

Inserting the property values for sodium and water at the operating temperatures of 850°F and 70°F, respectively, we get

$$\frac{\Delta p_{H2O}}{\Delta p_{Na}} = 1.244 \text{ and } 1.244 \times 41 = 51 \text{ psi.} \quad (35)$$

Figure 10-1. Test Section Components

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That is at 33.7 gpm water flow rate the assembly pressure drop in a vertical position has to be at or slightly below 51 psi. Since the test loop flow capacity did not exceed 28 gpm, the flow data had to be extrapolated to 33.7 gpm. This was done graphically as shown in Figure 10-2. Also shown is an equivalent curve for sodium at 850 °F mean temperature. The 0.198-inch orifice hole size appears to produce the desired flow rate at the 41-psi sodium head available at location 6-N-1.

A flow distribution test was made to confirm the results obtained by use of the COBRA code. Total and static head probes were installed at the outlet end of the assembly as shown in Figure 10-3. The liner was extended to the outlet edge to maintain the assembly flow pattern.

The probes consisting of 1/16-inch O.D. thin walled stainless steel tubes were inserted to about one-inch upstream from the outlet edge. The last grid was two-inches upstream. The tube locating wires extended only 1/2 inch into the assembly. Thus partial flow blockage occurred 1/2 inch downstream of the measuring point. The figure shows the location of the probes and the flow rates calculated from the measured velocity heads. The subchannels located in the planes midway between the corners were not accessible to instrumentation because of the ribs of the upper adaptor.

Table 10-1 compares the measured subchannel flow rates with those calculated by COBRA.

Table 10-1 Subchannel Flow Rates							
Subchannel Location	Velocity Head ins. H ₂ O	Velocity ft/sec	Subchannel Flow Area in ²	Flow Rate Measured lb/hr	Flow & Profile Correction Applied	Calculated Subchannel Flow Rate lb/hr	Type of Channel
				(13,500 Total)	(13,600 Total)		
1	12.18	8.085	.0139	175	176	182	inboard
2	8.13	6.605	.0171	176	177	169	edge
3	14.12	8.705	.0139	188	189	182	inboard
4	6.50	5.906	.0120	110	119	139	corner
5	13.32	8.455	.0139	183	184	182	inboard
6	6.50	5.906	.0120	110	119	139	corner
7	12.95	8.337	.0139	180	181	182	inboard
8	6.35	5.838	.0171	155	156	169	edge

Average subchannel flow rates were: inboard = $182 \frac{\text{lb}}{\text{hr}}$; edge channel = 166 lb/hr. Agreement between calculated and measured flow rates of the inboard and side channels is excellent. The measured corner channel flow rate is low. This can be explained by smaller channel size and therefore larger proportional blockage by the probes. The total flow measured was

Figure 10-2. 37-Pin Gridded Assembly for EBR-II Flow Test Results with Orifice of 7, 0.198 in. Diameter Holes

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Figure 10-3. Installation of Flow Distribution Probes

13,500 compared with 13,600 lb/hr calculated. The mass balance indicates that the corner channel flow rate should be higher than measured. But even if the flow rate at the corners were only 119 lb/hr the maximum outlet temperature would be only 1055°F if isolated from the bypass flow. Actually it will be lower. It is believed that the correct corner channel flow rate is closer to the calculated value than the measured value indicates.

Notwithstanding the relative simplicity of the measuring method, excellent agreement between measured and calculated subchannel flow distributions was obtained, thus supporting the use of the COBRA code to analyze sub-channel conditions.

SECTION 11

OFBA-510 PELLET FABRICATION

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OBJECTIVES

The objectives of this task are to demonstrate a process capable of producing mixed uranium-plutonium dioxide fuel of the required isotopic, physical, and chemical form to meet all aspects of OFBA-310 and -311; to demonstrate the capability for all necessary and pertinent support analytical determinations in the control of process variables and requirements defined by fuel specifications; and to fabricate mixed uranium-plutonium dioxide fuel for all phases of the contract and irradiation program.

PRIOR WORK

Initial work on the fabrication of mixed-oxide fuel indicated that some investigations would be required to meet the homogeneity and high density (95% T.D.) fuel specifications. A series of milling experiments were devised to provide such information and suitable material charges and milling times were obtained from the results. In general, the dry milling process produced pellets in which the largest PuO_2 particles were less than the 100μ limit but a number of UO_2 particles were noted up to 150μ . Attempts to eliminate such UO_2 particles by wet milling were successful; the major portion of these samples appearing similar to co-precipitated material as observed by α -autoradiography.

To evaluate the effectiveness of the milling conditions, pellet densities were measured after standard pressing and sintering operations. Green pellets were pressed at 15 tsi to densities between 42% and 52% T.D., and these were sintered at 1650°C for four hours. Fired densities up to 96% T.D. were obtained on pellets without binder additions with marked decreases being observed when binders were added. A dry milling process has tentatively been identified for the production of low density (90% T.D.) fuel.

CURRENT PROGRESS

Investigation of Process Variables

Although processes were identified early in the period for the fabrication of 90% and 95% T.D. fuel, some work continued to determine the effects of additional process variables. In most cases, the effort was directed toward a more complete understanding of the characteristics of the available material and to identify those variables that had the greatest effects on fired pellet density.

Mixing and Milling

Some detail was given in the previous quarterly regarding factorial and randomized block experiments that were undertaken to determine optimum mixing and milling conditions. In these instances, the homogeneity (α - autoradiography) results were used as a measure of mixing efficiency and pellet fired densities were used to measure milling effectiveness. The two appeared related in that improved homogeneity and higher densities occurred together and wet milling has been used for subsequent production batches to ensure reaching the 95% density requirement.

Binder Additions

The fine powders produced by the wet milling route do not readily press without lubricant or binder additions. Carbowax, Stearatex, and Acrawax additions have been investigated on a number of batches with Acrawax invariably being the best for most work. This material is non-hygroscopic and blends readily with oxide powders to form homogeneous mixtures, whereas both Carbowax and Stearatex tend to agglomerate during blending producing large pores in the resultant fired pellets. Compared to Acrawax, the other binders^(a) tend to reduce the pellet fired densities and this is attributed to the large pores formed in the structure.

In earlier investigations, binders were added prior to preslugging. More recent investigations suggest that acceptable pellets can be produced if the binder is added after preslugging and that less additive (up to 1w/o) is required in this instance to obtain good press feed. Work is continuing in this area to permit a more detailed analysis.

Preslugging & Pressing

Mention was made in the last report of internal defects in a number of pellets. These flaws, when present, are observed on metallographic sections and on the surface of ground pellets. The cause of the problem has not been positively identified, and it appears that the percentage of defect pellets varies widely from one batch to another. Both binder burnout and pellet pressing conditions have been considered as likely problem areas with both being investigated. Good press-feed material has been produced by preslugging at 8 tsi and crushing the slugs to pass a 35# screen. Investigation of preslugging at 16 tsi (close to the final pressing pressure) produced many cracked pellets and work is now underway preslugging between 3 and 8 tsi. The above work has also been combined with variations in final pressing pressure to produce green densities between 40% and 50% T.D., in order to optimize pellet quality, final density, and shrinkage during firing conditions.

Stoichiometry Control

Modifications were necessary to the firing cycle to permit binder removal at low temperatures. From pellet weight loss determinations up to

^(a)The term binder in this and following instances is used loosely and includes lubricants.

1000°C, it was concluded that most of the binder had been removed by 500°C and a furnace heating rate of 100°C/hour to 500°C and 300°C/hour sintering temperature had been used. On scaling-up from experimental work (approximately 50g batches) to fully loaded sintering boats (approximately 500g), a marked decrease in the O/M ratio has been noted. It has been concluded that carbon in the Acrawax tends to reduce the mixed-oxide to an O/M ~ 1.91 ; and a considerable amount of oxygen is required to increase this to the required value, O/M ~ 1.97 . Calculation of the equilibrium H_2/H_2O value for this O/M ratio at 1650°C yields 210 ppm H_2O for an Ar/5% H_2 sintering gas mixture (see Figure 11-1). At this moisture content, impractically long sintering temperatures are required to attain equilibrium at current gas flow rates of 4 scfh. Furthermore, O/M determinations on small batches of pellets (presumably close to equilibrium) suggest that the O/M ratio changes during the cooling period, although the kinetics of the reactions are not completely understood. It has, therefore, been necessary to increase the moisture content of the sintering gas to 1000 ppm and one or more refires are employed for final control of stoichiometry. Additional equipment has been ordered to provide finer control and analysis of the sintering gas used.

Figure 11-1. Moisture Addition (ppm) to Argon/5% Hydrogen vs Temperature (°C) for Equilibrium Conditions ($U_{0.75}Pu_{0.25}O_{1.97}$)

3788-33

Calcining

Recently, work has commenced on recalcining PuO_2 at $\sim 700^\circ\text{C}$ and comparing this to the standard ARHCO 450°C calcined material. Results indicate that somewhat higher fired densities are attained without noticeably affecting other properties.

Fuel Fabrication

Jet milling is being used in most of the work to satisfy density and homogeneity requirements. Details of processing conditions for batches scheduled for WSA-3 fuel are given in Table 11-1. In these cases, a maximum of 2 kg per batch have been processed (governed by box fissile limits) with such batches requiring three sintering runs (governed by sintering boat capacity). Fired densities and O/M ratios are checked prior to final grinding and pellet examination. At this point, complete sampling of the batch is undertaken and the remaining pellets sent for individual selection. Fabrication of the fuel pellets for WSA-3 will be completed by the end of July 1970.

Additional Work

In order to maintain the fuel fabrication schedule for subsequent irradiation sub-assemblies, additional PuO_2 and enriched UO_2 will be needed, and approvals were required to permit downgrading of the fully enriched UO_2 . Since suitable PuO_2 (low ^{240}Pu content) was not readily available, the nitrate was ordered and will be converted to oxide under known conditions. Both the nitrate and enriched UO_2 are expected shortly. Permission to downgrade fully enriched UO_2 to 75% ^{235}U has also been received, and modifications to the glovebox operating procedures are proceeding satisfactorily to permit this material in the line.

Table 11-1. Fabrication Conditions For WSA-3 Mixed-Oxide Fuel

BATCH	MILLING CONDITIONS						PRESSING CONDITIONS			FIRING CONDITIONS		
Number	Oxide Wt. kg ^a	M/V ^b	MC% ^c	Type	Media	Mill Time (hrs)	Pre-slugging Pressure	Screened Granule Size	Green Density % T.D. ^a	Maximum Firing Temp & Time ^d	Firing Gas ^{d,e}	Fired Density % T.D.
31	2.0	4	40	Wet; Water	ZrO ₂	90	8 tsi	-35#	45.5	1650°C 4 Hrs.	1000 ppm H ₂ O 4 scfh	95
32	1.4	5	40	Wet; Oxylene	WC	16	8 tsi	-35#	46	1600°C 4 Hrs.	1000 ppm H ₂ O 4 scfh	90
34	2.0	4	40	Wet; Water	ZrO ₂	24	5 tsi	-35#	46	1650°C 4 Hrs.	1000 ppm 4 scfh	
36	1.6	3	40	Wet; Water	ZrO ₂	50	Final Parameters to be Determined					

- a. Values quoted are approximate.
- b. The amount of material in a mill can be expressed conveniently as the ratio of its volume to that of the voids in the ball charge. If the solid material and its suspending medium just fills the ball voids, the M/V ratio is one, for example.
- c. The load of grinding medium can be expressed in terms of the percentage of the volume of the mill that it occupies, i.e., a bulk volume of balls half filling a mill is a 50% media charge.
- d. Final pellet density and O/M ratio have been controlled by one or more firings and gas conditions. The values quoted are maxima for any batch.
- e. Standard sintering gas used is argon-5% hydrogen. Moisture as specified is added to control the oxygen potential of the sintering gas.

SECTION 12

OFBA-511 OXIDE FUEL CHARACTERIZATION

J. Y. Chang, P. M. French, W. R. Jacoby, and P. D. Shalek

OBJECTIVES

The principal objectives of this task are to develop characterization techniques and to characterize and evaluate the uranium dioxide and mixed uranium-plutonium dioxide (powders and pellets) developed and fabricated under Task OFBA-510. Product characterization is required for control and performance evaluation objectives.

A secondary objective is to determine the various physical and mechanical properties, e.g., thermal conductivity, expansion, etc., of these materials at each compositional change. These are necessary to complement the fabrication process (OFBA-510) and the irradiation programs (OFBA-310 and -311). This task will require some National Laboratory support.

PRIOR WORK

Sampling procedures and tentative specifications were established for the various materials to be characterized in the mixed-oxide fuel program.

It was demonstrated that hydrogen is chemisorbed onto pellets cooled in Ar-5% H_2 , and this hydrogen manifests itself as a moisture indication during analysis. Furthermore, the work indicated that porous pellets cooled in Ar-, He-, or N_2 -5% H_2 atmospheres may show both excessive moisture and TGR readings after storage.

Metallographic and autoradiographic evaluation of the UNC fuel batches (for WSA-1 and WSA-2) was completed. Composite porosity photographs revealed different pore structures for the various fuel batches.

An investigation was made of the effects of various process variables upon sintered fuel homogeneity of test batches of $(U,Pu)O_2$ pellets. Three types of basic inclusions were observed in this material and studies were conducted to determine their nature and source.

CURRENT PROGRESS

Analytical Chemistry

The one remaining item to be completed on $(U,Pu)O_2$ pellet batches for use in the WSA-1 and -2 subassemblies was the determination of total gas release (TGR). The data, obtained at 1200°C, are given in Table 12-1 along with final moisture values. It can be seen that all the values effectively meet the specification limit of 0.05 cc/g for TGR (exclusive of H_2O) with one exception, namely, the 0.121 cc/g value for the

Table 12-1. Total Gas Release and Moisture Values for (U,Pu)O ₂ Pellets Manufactured by UNC for WSA-1 and WSA-2			
²³⁵ U Enrichment (%)	Nominal Density (% Theoretical)	Total Gas Release ^(a) (cc/gm)	H ₂ O Content (ppm)
93	89	0.036	32
	91	0.121 (0.073)	38
	95	0.016	8
65	89	0.036	4 ^(b)
	91	0.054	35
	95	0.028	8
43	91	0.021	3 ^(b)
	95	0.028	4 ^(b)
^(a) UNC reported values in the range 0.011 - 0.020 cc/gm for the pellets prior to shipment to ARD. ^(b) Pellets were dried in flowing Ar at 700°C.			

Table 12-2. Effect of Chemisorbed Moisture on TGR and Moisture Determinations			
Treatment	H ₂ O Released (ppm = (cc/gm))		Total Gas Released (cc/gm)
a. Pellet heated to 700°C in CEC moisture analyzer	64	0.080	0.080
b. Pellet from (a) heated to 1200°C in TGR apparatus			0.040
	Sum (a) & (b)		0.120
c. Avg. for 3 pellets heated directly to 1200°C in TGR			0.119

93%-enriched, 91%-dense batch. The value of 0.073 cc/g (given in parenthesis), however, is considered to be a more realistic evaluation of this parameter. A test run on a number of pellets from this particular batch indicated that chemisorbed water was being measured twice--both during the moisture analysis and during the TGR determination. This is probably because the high temperature of its release rendered the cold trap in the TGR apparatus ineffective (see Table 12-2).

The 0.073 cc/g value for the 93%-enriched, 91%-dense batch was obtained by subtracting the average moisture content (38 ppm $\equiv$ 0.048 cc/g) from the TGR value (0.121 cc/g). On this basis, the TGR is only slightly over the specification limit and should not be expected to significantly affect fuel pin performance. One final observation should be made with regard to this TGR data - between 90 and 95% of the gas released was hydrogen.

All analytical chemistry analyses were completed on batch U-11 of the UO₂ insulator pellets to be used in WSA-1 subassembly. The values obtained for the various attributes confirm that the material is within specification. A TMI value of 256 ppm was recorded -- well within the allowable limit of 3,000 ppm. Impurity analyses were also undertaken on some sintered (U,Pu)O₂ pellets, where the starting UO₂ and PuO₂ powders had been wet milled for 64 hours using ZrO₂ balls. A Zr pickup of 150 ppm was observed, which is considered acceptable for this material.

O/M determinations were undertaken on small trial batches of (U,Pu)O₂ pellets, where different firing conditions had been employed, i.e., sintering temperatures of 1600 or 1650°C in a dry N₂/6%H₂ atmosphere with heating rates ranging from 100 to 400°C/hour. O/M values ranged from 1.951 to 1.962, indicating that small differences in fabrication conditions did not have a very significant effect on stoichiometry.

Carbon and O/M values were obtained for fabrication trials on green pellets heated to 500 and 1000°C, details being given in Table 12-3. The data indicate that virtually all excess oxygen in the (U,Pu)O_{2+x} is lost in this temperature range with the sintering conditions used. However, from the 1% binder added to the powder, 1600 ppm C still remains in the pellets after heating to 1000°C for one hour. This level of residual C is significant, since calculations show that 2,500 ppm C is sufficient to reduce (U,Pu)O_{2.00} to (U,Pu)O_{1.88} when CO₂ is the reaction product. Based on these results, the firing schedule includes a 6-hour soak at approximately 1000°C to obtain decarburization, before proceeding to the sintering temperature.

Some evaluation was made of larger pellet batches (~150 pellets per batch), which had also been sintered in N₂-6%H₂. Low O/M values in conjunction with low densities appeared to be related to high nitrogen, high total gas release, and, in certain instances, high moisture values. Details are given in Table 12-4, along with data obtained after a portion of the batch was refired in Ar-5%H₂ with 150 ppm moisture in an attempt to bring the O/M value within specification. The following comments are relevant at this time:

Table 12-3. Carbon and O/M Values of Green Pellets Heated in Ar-5%H ₂		
Maximum Heating Temperature (°C)	O/M	Carbon Content (ppm)
500	2.22	5000-8000
1000	2.04	1600
Note: 1% Acrawax added as binder and the O/M of the starting UO ₂ powder was 2.366.		

Table 12-4. Analytical Chemistry Data on a (U,Pu)O ₂ Pellet Batch of Nominally 90% Theoretical Density Sintered in N ₂ -6%H ₂ and then Refired in Ar-5%H ₂				
Condition of Pellets	O/M	N Content (ppm)	H ₂ O Content (ppm)	TGR (cc/g)
Sintered in N ₂ -6%H ₂ + 320 ppm H ₂ O for four hours at 1550°C	1.937	2500	56(a)	0.4
Refired in Ar-5%H ₂ + 150 ppm moisture for two hours at 1400°C	1.956	36	N.D.	N.D.
(a) After two weeks storage				
N.D. - Not determined				

1. The data indicate that the binder used (1% Acrawax causes a depression of O/M values beyond that anticipated for low density pellets under the sintering conditions. Acceptable carbon levels (~ 40 ppm) can, however, be obtained in the sintered pellets.
2. Although not shown in the table, the O/M value obtained is often higher than the calculated equilibrium value. In one case, a refired batch attained an O/M value of 2.007 compared to an equilibrium value of 1.981. It is suspected that at least surface oxidation occurs during the cooling cycle.
3. The nitrogen values are obtained by the Kjeldahl method, which measures "nitride nitrogen" as opposed to total nitrogen. It is considered somewhat surprising, therefore, that the nitrogen value can be reduced so dramatically by refiring in Ar-5% H_2 plus moisture at a temperature as low as $1400^\circ C$.

Because of the problems encountered with N_2 - H_2 atmospheres, it was decided that future sintering would be undertaken in an Ar-5% H_2 atmosphere, with a moisture addition to achieve desirable O/M values. Moisture levels ranging from 110 to 1000 ppm have been used. Use of partially covered sintering boats for small experimental firings (10-20 pellets) gave mixed O/M values--some low, but others acceptable. The purpose of the sintering boat cover was to minimize cracking by thermal shock in pellets which "see" the furnace tube. Extension of this experimental procedure to 200 or 400 pellet batches, however, resulted in large O/M gradients across the boat, the particular values depending on cover configuration, H_2/H_2O ratio, gas flow rate, and batch size. The maximum O/M gradient observed ranged from 1.988 at the top front of the boat where the gas enters to 1.900 at the bottom center and 1.953 at the back top. With 400 pellets per boat, the pellets are 4-5 layers deep, and obviously the sintering gas is being used very inefficiently. Subsequent refirings (up to a maximum of three) of such batches in open boats were successful in raising the general O/M level and, in some cases, in minimizing O/M gradients throughout the boat. However, selective sampling at strategic boat positions is still required in order to establish the minimum refire conditions necessary to obtain equilibrium. Indications are that batches brought to acceptable O/M status by the above procedure will have C and N values well within specification limits.

The refiring operations have to furnish enough moisture at the desirable concentration to oxidize the pellets back up to an acceptable O/M level (~ 1.97). Assuming 100% oxidizing efficiency for the refiring gas (Ar-5% H_2 with 1,000 ppm moisture at 4 cfh), it has been calculated that 4.5 hours at sintering temperature are required to produce an O/M increase of 0.06 in 100 g of $(U,Pu)O_2$ pellets.

Powder Characterization

Values for the average particle size (equivalent spherical diameter) and BET surface area have been obtained for $(U,Pu)O_2$ powders milled under different conditions. Details are given in Table 12-5, along with information previously generated^[29] for PuO_2 and enriched UO_2 powders for comparison. Thus, the milling conditions have been successful in

reducing the average particle size of the mechanically mixed (U,Pu)O₂ powders. There is insufficient evidence at the present time to establish a trend of the variation of surface area with milling conditions.

Table 12-5. Average Particle Size and Surface Area of Various Powders			
Sample Description	Condition	Average e.s.d (μ)	BET Surface Area (m ² /gm)
Starting UO ₂	As Received	0.57	3.57
Starting PuO ₂	As Received	1.31	40.5
Batch UP-26	Dry milled, then wet milled in oxylene	0.42	12.14
Batch UP-28	Dry milled for 30 hrs.	0.60	12.32
Batch UP-29	Wet milled in oxylene for 42 hrs.	0.47	8.17

Metallography and Autoradiography

Much of the effort this period has been concerned with evaluation of pellets from the various trial batches described in OFBA-510 and the first large-scale production batch (batch UP-31). Twelve visual autoradiographic standards have been established for the acceptance or rejection of fuel for the WSA-3 subassembly. The standards cover both the size and distribution of UO₂- and PuO₂- containing particles. Figure 12-1(a) is an autoradiograph of acceptable material and 12-1(b) an autoradiograph of rejectable material. Table 12-6 lists the maximum diameters of the largest UO₂- and PuO₂- containing particles greater than 30 μ in each autoradiograph. For material to be acceptable for use in WSA-3 subassembly, it was decided that no uranium-containing particle should exceed 150 μ in diameter, and there should be no more than five such particles in the range 100-150 μ . Furthermore, no Pu-containing particle shall exceed 100 μ in diameter. The total volume of U and Pu containing particles greater than 90 μ shall not exceed 5% of the total volume.

Autoradiographs of pellets representing the wet milled conditions generally revealed an excellent homogeneity, comparable with good coprecipitated material.^[30] These pellets, however, had isolated cases of uranium-containing particles exceeding 100 μ in diameter, but no plutonium-containing particles exceeding 20 μ . It is felt that the problem of these large uranium-containing particles can be related to a previous observation^[31] that 6.6 w/o of the starting UO₂ powder would not pass through a 105 μ screen. The indication is that the starting UO₂ powder contains a small percentage of coarse, hard particles that cannot easily be broken down by normal milling techniques. This is illustrated in Figure 12-2. A coarse particle, approximately 400 μ in diameter, was taken and crushed with some difficulty between two microscope slides. The large particle fractured into another smaller one approximately 300 μ in diameter.

(a) ACCEPTABLE MATERIAL WET MILLED
FOR 16 HOURS IN OXYLENE

(b) UNACCEPTABLE MATERIAL DRY
MILLED FOR 22 HOURS

Figure 12-1. Autoradiographs of Sintered (U, Pu) O₂ Pellets
3788-34 (From visual standards)

Table 12-6. Maximum Diameters of High UO ₂ and PuO ₂ -Containing Particles in Figure 12-1 Greater than 30μ			
Figure 12-1(a)		Figure 12-1(b)	
UO ₂ Particles (μ)	PuO ₂ Particles (μ)	UO ₂ Particles (μ)	PuO ₂ Particles (μ)
40	None	90	50
50		110	50
80		130	50
100		150	50
		150	55
		150	60
		200	65
		200	80
		210	90

(a) COARSE UO_2 PARTICLE $\sim 400\mu$ IN DIAMETER (MAG 75X)

(b) FRACTURED UO_2 PARTICLE PLUS FINES. (MAG 75X)

Figure 12-2. Coarse UO_2 Particle Before and After Fracturing
3788-35

Metallographic examination revealed that many of the pellets from the trial batches contained cracks and inclusions of a type similar to the ones previously observed^[31] and exhibited a density gradient across a section. However, a significant reduction in the volume of these defects was observed in later batches. Microhardness surveys have been undertaken on the inclusions referred to above, and data is given in Table 12-7 along with information from pellets in the first full-scale production batch (batch UP-31). There is reasonable agreement with data from the literature to confirm our predictions that the uranium-rich and plutonium-rich inclusions are UO_2 and PuO_{2-x} , respectively. The microhardness values obtained for the nonfissile inclusions indicate that they are metallic in nature. Similar nonfissile inclusions have also been observed at LASL^[32] when they milled the powders with stainless steel balls. Microprobe examination of these inclusions at LASL indicated the elemental composition to be close to that of stainless steel. The nominal density of the first full-scale (U,Pu) O_2 production batch was 95% theoretical and the areas where the indentations were made estimated to be 99% theoretical. It is somewhat surprising to see the large increase in hardness from that of the 95% dense area and no immediate explanation of the differences is apparent. This work will continue in the next period.

Table 12-7. Microhardness Values of the Various Constituents in (U,Pu)O ₂ Pellets ^(a)			
Material	Microhardness DPHN (kg/mm ²)	Estimated Phase Density (% Theoretical)	Microhardness Data from Literature ^(b) (DPHN-kg/mm ²)
(U,Pu)O ₂ Matrix - trial	759	95	600
UO ₂ Inclusions batches	673	96	634 - 657
PuO _{2-x} Inclusions	542	99	545 (α-Pu ₂ O ₃)
Nonfissile Inclusions	309	99	---
(U,Pu)O ₂ - Batch 31-1R ^(c)	1260-1350	99	---
(U,Pu)O ₂ - Batch 31-2	1100-1140	99	---
^(a) A 100g load was employed			
^(b) O.J. Wick, <u>Plutonium Handbook</u> , Vol. I, pp. 261-265, Gordon and Breach, 1967.			
^(c) Sintered and then refired			

The effect of binder additions upon porosity distribution has been examined as illustrated in Figure 12-3. The pellets were all sintered for four hours at 1650°C. The 1% Acrax addition yields a fine, uniform porosity distribution which, however, is coarser than material with no binder. Addition of 1% Steratex gives a coarse structure with a wide range in the diameter of pores. Steratex is not presently used, primarily because it does not produce free flowing powder.

Pellets sintered for four hours at 1650°C in a N₂-6%H₂ atmosphere, having only a 2 ppm moisture content, were found to contain a fissile secondary phase, not previously observed in Figure 12-4(a). The O/M values of these batches were found to be in the range 1.920-1.934, in line with the metallographic observation. Figure 12-4(b) shows the structure of the same batch of pellets refired under the same conditions, except the moisture content of the furnace gas was increased to 120 ppm. As anticipated, the amount of second phase has been significantly reduced. Increasing the moisture content of the sintering gas to 450 ppm assisted in the elimination of this phase, and acceptable batch O/M values were obtained in small size batches.

A limited number of samples have been examined from the first large production batch, UP-31, in which pellets were sintered in two fires^(a) to 95% theoretical density. As stated above, the purpose of refiring was to

^(a) Sintered at 1650 for four hours in Ar-5%H₂ with 250 ppm moisture with a covered boat; refired at 1600°C for two hours with 150 ppm moisture and the lid removed from the boat.

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(a) NO BINDER

(b) 1% ACRAWAX

(c) 1% STEARATEX

MAG 75X. AS POLISHED

ALL PELLETS SINTERED AT 1650°C FOR 4 HOURS IN Ar-5% H_2 WITH 120 ppm MOISTURE

Figure 12-3. The Effect of Different Binder Additions on Porosity Distributions in (U, Pu) O_2 Pellets

(a) SINTERED FOR 4 HOURS AT 1650°C IN A
N₂-6%H₂ ATMOSPHERE WITH 2 ppm MOISTURE

(b) AS (a) WITH A REFIRE AT 1650°C FOR
4 HOURS IN A N₂-6%H₂ ATMOSPHERE WITH
120 ppm MOISTURE

NOTE DEPLETION OF SECOND PHASE
COMPARED TO (a)

MAG 600X. AS POLISHED

Figure 12-4. Effect on (U, Pu) O₂ Pellet Microstructure of Different
Moisture Contents in the N₂-6%H₂ Sintering Gas

3788-37

eliminate the O/M gradient across the boat (containing 200-400 pellets), bring the O/M of the pellets within specification range, and adjust density. The average grain size was determined to be approximately 9μ (ASTM No. 9). Two discreet groups of voids were observed, one having a 1-25 μ size range and the other a 50-90 μ size range, as shown in Figure 12-5. The large pores are believed to be due to the dry addition of 0.5% Carbowax to the preslugged granules used in this batch, during the last stage of powder preparation. The small voids are believed due to the 1% Acrawax. A typical pore size distribution in pellets from this batch is shown in Figure 12-6, which clearly demonstrates the two pore size ranges.

Figure 12-5. Porosity Distribution in an As-Sintered (U, Pu) O₂ Pellet
(Mag 600X, As Polished)
3788-38

All pellets examined to date from the three sub-batches of Up-31, after different firing conditions, revealed a well-sintered structure, but contained some degree of microcracking and a small quantity (<.1 v/o) of metallic inclusions of varying sizes, the largest inclusion being 100 μ x 20 μ (Figure 12-7). Microhardness values for the (U,Pu)O₂ matrix and inclusions were 1100-1350 and 300 DPHN respectively; the latter value being very similar to that previously observed, as shown in Table 12-7.

Figure 12-6. Typical Pore Size Distribution in a (U, Pu) O₂ Pellet from Batch UP-31

3788-39

Figure 12-7. Metallic Inclusions in a (U, Pu) O₂ Pellet
3788-40 (Mag 600X, As Polished)

Autoradiographs of these pellets which were produced from powder milled for 90 hours in water using ZrO₂ balls, showed reasonable homogeneity. No plutonium-containing particles were observed having a diameter greater than 90 μ and there were only a few uranium particles which exceeded the specification diameter. In addition, a much higher degree of solid solution than found in earlier batches was apparent.

SECTION 13
OFBA-512 FUEL ELEMENT FABRICATION

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OBJECTIVES

The principal objectives of this task are to:

1. Fabricate and characterize the required fuel pins and capsules needed for WSA-1 and WSA-2, two 19-pin assemblies for irradiation testing.
2. Fabricate and characterize fuel pins for WSA-3, an unencapsulated 37-pin assembly for irradiation testing.
3. Produce and evaluate pins containing axial fuel motion restrictors. These restrictors will be rigidly fixed to the cladding by crimping the cladding into a circumferential groove in the restrictor discs which are located within the cladding and serve to compartmentalize the fuel pellets.
4. Adapt methods for xenon tagging of oxide pins and to fabricate pins containing the xenon gas.
5. Fabricate at least 37-prototype pins (except for active fuel) for a hydraulic flow test and an additional 37-pin set for a simulated fretting corrosion test.

PRIOR WORK

Fabrication and encapsulation of the 19-fuel pins for WSA-2 was completed. Fabrication of the 19-fuel pins for WSA-1 was completed and encapsulation initiated. Four of the WSA-1 pins contained axial fuel motion restrictors. Development of xenon tagging procedures and welding methods to meet the requirements of the WSA-3 subassembly were initiated.

CURRENT PROGRESS

WSA-1 Void Deployment Irradiation Pins

All fabrication and testing of the 19-encapsulated irradiation test pins for the WSA-1 assembly were completed on April 24 and were shipped to the EBR-II site on May 7. Unlike the WSA-2 assembly (which was completed February 7) 4 of the 19 pins for WSA-1 had the fuel pellets divided into 4 compartments by 4 crimped stainless steel discs in each pin. In 15 of the 16 fuel compartments, the pellets were in contact with the bottom disc or end plug immediately after fabrication. A 0.037-inch gap existed in the other compartment. After the 800°F centrifugal sodium bonding treatment,

the pellets in this compartment still were not seated, but after the subsequent 1000°F heat treatment, the pellets were in contact with the spacer disc. Details of the fabrication of these 4 special pins were described in the previous period report.

WSA-3 Equipment Installation

Three glove boxes in the Plutonium Oxide Laboratory were made operational for fuel pellet loading and final pin fabrication of WSA-3. In Box #11 a vibratory feeder, was adapted for use as a pellet loading device as shown in Figure 13-1. A fuel tube is fastened to the pellet loading tube through the plastic "udder bag" shown. Vibrational intensity has been adjusted to allow slow entry of pellets into the fuel tube. With this device excessive tube handling and seating of pellets with push-rods is eliminated.

The outfitting of a three-compartment decontamination box has been completed. This box is used for cleaning the open tube ends after pellet loading and then installing a temporary plastic plug.

Considerable effort has been spent in commissioning the welding chamber and associated equipment. This chamber is shown in Figure 13-2, along with the Merrick Engineering TCR-300 T.I.G. power supply. The chamber, having an internal volume of approximately 70 ft³, is capable of being evacuated to 10⁻⁵ mm Hg vacuum prior to backfilling with helium. The pumping system consists of a Stokes 80-cfm mechanical pump, a 6-inch Blue-line oil diffusion pump, and a 5-cfm Welch backing pump. The chamber is equipped with three entrance ports. The 6-inch aluminum pipe attached to the bottom of the chamber is capable of being easily detached and loaded simultaneously with 6-fuel rods up to 65 inches in length. The large door on the left side facilitates loading and unloading large items from the interior, in addition to any major revisions required to the interior of the chamber. A pass-through port is located on the right side which is capable of rapid evacuation and backfilling allowing small items to be passed in and out of the main chamber without disturbing the quality of the atmosphere within the main chamber. The three gloves (the center glove is ambidextrous) allows for complete utilization of the entire chamber interior. The electrode holding device, shown through the one-inch thick Herculite window, is a simple two-dimensional compound slide rest which allows accurate positioning of the electrode during welding. The power supply is equipped with a capacitor discharge-type starting mechanism which allows for easy gas ionization and subsequent arc starting in pure helium. This power supply is also equipped with a current programming module which can be arranged to automatically and accurately sequence a given current program within any desired period of time. Subsequent recording of the current and voltage traces are made on an Esterline Angus dual channel recorder. These traces are properly identified and filed as part of the quality assurance data package for individual rods.

Hydraulic Flow Test Prototype Pin Assembly (WSA-3F)

Welding procedures were required for the new type of end plug closure for the flow test assembly. Since the fuel tube and the end plugs are not press fit, they were first tack welded together so they could be driven as

Figure 13-1. Tube Being Loaded with Fuel Pellets from Within Box #11.
Pellet Movement is Controlled by Vibratory Feeder

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13-4

Figure 13-2. Weld Box and Power Supply in the Plutonium Oxide Facility

one during the girth welding. Four tack welds, 90° apart, were used. It was found that the tack welds were melted into the girth weld during final welding. The tacking procedure also reduced the average, as-welded, run out of the end plugs from 0.012 to 0.008 inch.

After radiography, helium leak checking, visual inspection, and metallographic examination, the welding profile shown in Figure 13-3 was selected on the basis of penetration, repeatability, and minimum amount of as-welded runout. Figure 13-4 shows a section of a typical weld using the parameters given in Figure 13-3.

Weld buildup or maximum diameter across the weld, although minimized by the selected welding parameters used, still did not meet the required 0.232-inch maximum diameter specification. All 37 welds of the Flow Test Assembly had material removed from the diameter so as to meet the specified 0.232-inch diameter dimension, and all fuel pins were straightened in the weld region in order to meet the 0.006-inch run out specifications. The Flow Test Assembly was delivered on June 12, 1970.

Fretting Test Prototype Assembly (WSA-3Z)

A program was undertaken to reduce the weld buildup encountered in the Flow Test Assembly by modifying the end plug to fuel tube joint design. The initial joint design, Figure 13-5(a), was modified by machining 0.010 ± 0.001 inch off the bottom end plug shoulder diameters for 40 plugs, as shown in Figure 13-5(b). Figure 13-6 shows a section of a typical weld obtained with this end plug modification. Buildup was reduced but not enough to meet specifications. To remove additional metal, the joint design was modified again by recutting the back radius as shown in Figure 13-5(c). Forty Fretting Test top end plugs were reworked to conform to this modification. Weld parameters were changed to those in Figure 13-7 in order to minimize run out and buildup. Figure 13-8 shows a section of a typical weld using this final modification.

Figure 13-9 shows a comparison of the maximum weld nugget diameters measured for the three end plug designs. As in the Flow Test rods, the bottom end plug welds of the Fretting Test rods required removal of material in order to meet maximum diameter specifications. The top end plug welds required minimum dressing or material removal, and are still being evaluated. The Fretting Test rods were delivered on June 30, 1970.

WSA-3: 37-Pin Fueled Assembly

Thirty spacer tube and spring assemblies, which serve to hold the fuel down in the fuel tube, have been completed with the remaining assemblies to follow shortly. Fifteen prototype top end plugs have been made in order to evaluate the tack welding of the spiral pins in place in order to form a bayonet-type fitting to mate with EBR-II pin lifting devices.

Certified tubing has been received, and fuel tube preparation and bottom end plug welding for the Fueled Assembly is underway.

Figure 13-3. Arc Current Program During Welding of End Plugs for WSA-3F Flow Test Assembly and Bottom End Plugs for WSA-3Z Fretting Test Assembly 3788-43

Figure 13-4. Weld Nugget Using Program Shown in Figure 13-3 (Mag 20X)

(a) ORIGINAL END PLUG DESIGN

(b) FIRST MODIFICATION

(c) SECOND MODIFICATION

Figure 13-5. Tubing-End Plug Modification Program to Minimize Weld Metal Build-up for WSA-3

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Figure 13-6. Weld Nugget Using First End Plug Modification Shown in Figure 13-5 (b) (Mag 20X)

Figure 13-7. Arc Current Program During Welding of Top End Plugs in WSA-3Z Fretting Test Assembly

Figure 13-8. Weld Nugget Using Second End Plug Modification Shown in
Figure 13-5 (c) (Mag 20X)
3788-47

Figure 13-9. Frequency Distribution of Measured Maximum Weld Diameters for the Three End Plug Designs Shown in Figure 13-5

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Xenon Tagging

The xenon tagging apparatus has been constructed and trial runs performed using prototype fuel rods with the exception that stainless steel pellets replace the mixed-oxide fuel pellets. Figure 13-10 shows the interior of the welding box with the xenon tagging apparatus positioned over a fuel rod. Results from the initial tagging experiments, consisting of six consistency trials and four pressure dependency trials, will soon be analyzed by mass spectrometry. The tagging apparatus is located completely within the welding chamber in order to minimize xenon gas usage resulting from the volume of external piping. The entire system was helium leak tested and the gas plenum void volume of the tagging apparatus was determined to be 4.8 cc. With a nominal charge of six fuel rods per weld cycle, the estimated xenon tag gas loss will be approximately one cc. The intended tagging procedure has been written and approved by EBR-II personnel after witnessing a demonstration of the procedure on prototypical fuel rods on June 30. Enriched xenon tag gas has been blended, analyzed, and received from EBR-II. The enriched xenon isotopic composition, along with natural xenon isotopic gas compositions, are presented in Table 13-1. The xenon isotopic ratios, relative to ^{124}Xe , are the important criteria. These unique isotopic ratios are important if an element in this subassembly should fail in the reactor, the tag gas would be liberated to the reactor argon cover gas, where it would be detected.

Table 13-1. Comparison of Isotopic Composition and Ratios of Natural Xenon and the Enriched Xenon Tag Gas Mixture for the WSA-3 Fuel Rods									
Xenon Isotope (Vol. %)									
	124	126	128	129	130	131	132	134	136
Natural Xenon	0.096	0.090	1.92	26.44	4.08	21.18	26.89	10.44	8.87
WSA-3(a) Enriched Tag Xenon	0.849	0.340	3.04	31.52	4.02	19.59	24.01	9.04	7.59
Natural Xe Isotopic Ratios $\text{Xe}/^{124}\text{Xe}$	1.0	0.938	20.0	260	-	-	-	-	-
Enriched WSA-3 Tag Mixture Isotopic Ratios $\text{Xe}/^{124}\text{Xe}$	1.0	0.40	3.59	37.2	-	-	-	-	-
(a) ANL Designated Tag Mixture No. 160 A 372. Each 30 ml Cylinder Contains 11.9 V/O Xe in Helium.

Figure 13-10. Interior of the Weld Box Showing the Xenon Tagging Apparatus
Positioned on a Prototype Fuel Tube

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Gas loss calculations have been made based on the present design of a WSA-3 fuel rod which incorporates a 0.053 inch nominal diameter hole at the top of the spacer tube. The diffusion of 11.3 V/O Xe in He into an infinite volume of He through this 0.053 inch diameter hole was considered. The diffusion coefficient for Xe in He (D_{Xe}^{He}) was calculated^[32] from the kinetic theory of gas equations involving a binary gas mixture. D_{Xe}^{He} at one-atm pressure and 300°K was estimated to be 0.782 cm²/sec. Using this value for the diffusion coefficient, the amount of xenon loss was calculated for various time intervals. Figure 13-11 shows the relatively slow loss of xenon tag gas by a diffusion mechanism from the open end of the fuel pin after an initial charging of one cc nominal xenon. Based on these calculations, the only apparent problem will be in ascertaining that the evacuated void volume of the fuel pin receives an equilibrium charge of the pre-mixed Xe in He tag mixture.

Profilometry of WSA-3 Tube Surfaces

A new profilometer has been designed and will be constructed and evaluated during the next quarter. It will be used to survey the surface topography of the WSA-3 fuel rods.

Figure 13-11. Calculated Xe Tag Gas Loss Caused by Diffusion From a WSA-3 Fuel Rod (0.053 diameter opening)

3788-50

SECTION 14

OFBA-513 PRODUCT CONTROL AND DOCUMENTATION

W. B. Bargerstock, W. R. Jacoby, and R. K. McGeary

OBJECTIVES

The objective of this task is to provide quality control for the entire program from raw material acceptance to final product verification. Included is responsibility for documentation of all data and information needed for final records from all phases of fabrication and material handling.

During FY-1970, at least 79-irradiation pins are required: two 19-pin encapsulated assemblies (plus four additional compartmented pins), and the first 37-pin assembly. In addition, detailed records will be required for prototype pins, test pins, encapsulated samples, and library samples. Data for more than 3,000 pellets will be keypunched and processed.

PRIOR WORK

A complete data package was compiled for the WSA-2 subassembly. Accumulation of data for the WSA-1 subassembly was in progress.

CURRENT PROGRESS

A complete data package was compiled for the WSA-1 assembly of 19-sodium encapsulated irradiation test rods and this information was presented to the Fuel Irradiation Project personnel. Information was in such form that the pertinent data could be extracted by this group and sent to EBR-II personnel for experiment approval. These functions are continuing in a routine manner for the WSA-3 assembly.

Fuel pellet integrity standards were selected and agreed upon by the pellet fabricators, the irradiation project representative, and the product control and documentation representative. Certain pellets showing chips, cracks, and pits were categorized as: (a) acceptable, (b) marginal, or (c) unacceptable. These were photographed and will be used as comparison standards for selection of WSA-3 fuel pellets.

Computer program calculation of the density of UO_2 insulator pellets for WSA-3 is complete. Mean density was 91.7% of theoretical with a 0.67 standard deviation. All of the pellets were mechanically proof tested by vibratory agitation for 15 minutes while held in cladding tubing under a normal spring hold-down load. This testing served to reveal those pellets showing edge chipping. A minimal amount of chipping was observed following the testing, and 100 pellets were selected for loading.

SECTION 15
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