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MASTER

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INTEGRAL FAST REACTOR
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ABSTRACT

This report summarizes highlights of the technical progress made in the Integral Fast Reactor (IFR) Program in FY 1992. Technical accomplishments are presented in the following areas of the IFR technology development activities: (1) metal fuel performance, (2) pyroprocess development, (3) safety experiments and analyses, (4) core design development, (5) fuel cycle demonstration, and (6) LMR technology R&D.

I. IFR PROGRAM OVERVIEW

The IFR Program continued to make progress during the reporting period in all aspects of fuels performance demonstration, pyroprocess development, safety experiments and analyses, core design development, fuel cycle demonstration, and LMR technology R&D. Although funding shortages have caused delays in the Fuel Cycle Facility (FCF) modification activities, significant progress has been made in the review of the Final Safety Analysis Report and the operational readiness review documentation efforts.

The University of Chicago Special Advisory Committee for the IFR continues to be enthusiastic about the potential of the IFR and is highly impressed with the technical progress as indicated by the following excerpts from the executive summary of the Committee report:

"The IFR Program to date has been very successful in the sense that no phenomena have been discovered that detract from the original promise. The Program, therefore, becomes both more important and more visible in discussions of national and international energy policy.

The potential advantages of the IFR visualized in past years continue coming closer to reality. These include enhanced safety; proven, very high fuel burnup; proliferation resistance; reduced waste-disposal problems; an integral, self-contained, and potentially lower cost fuel cycle; and a nearly inexhaustible supply of fuel. This Committee sees little likelihood that any serious technical problems will emerge that would eliminate the IFR as the front-runner for supplying nuclear electricity when light water reactors become uneconomic as a result of finite uranium supplies. The Committee believes the cost of developing the IFR to a state of readiness is small compared to the benefits of having an assured, uninterrupted supply of electricity at predictable cost.

The Committee believes it is timely to initiate steps toward international collaboration and joint funding of the demonstration plants discussed above. Such a program, including relevant R&D and component development, will require substantial financial resources."

The Joint Program with Central Research Institute of Electric Power Industry (CRIEPI) of Japan has been expanded to increase the scope of work to include participation in WBS 500 Fuel Cycle Demonstration and to add the Japan Atomic Power Company as a party to the contract, representing the Japanese utilities.

II. METAL FUEL PERFORMANCE

A. Experience with Advanced Driver Fuels in EBR-II

1. Introduction

Several metallic fuel element designs have been tested and used as driver fuel in Experimental Breeder Reactor-II (EBR-II). Recent goals have been to demonstrate the performance of large numbers of IFR/ALMR-like fuel elements and to provide source material to begin the reprocessing demonstration.

The previously used Mk-II design was the basis for several new designs, including the Mk-III and Mk-IV, that are described in the next section. In 1987, qualification testing of the Mk-III design began for use as a driver fuel for EBR-II. This was followed in 1989 by the Mk-IIIA and Mk-IV designs. The next fuel design, the Mk-V, is being planned for the IFR fuel cycle demonstration that will include utilization of the ternary alloy U-Pu-Zr and reprocessed fuel.

All of the advanced fuel designs tested have demonstrated the ability to exceed the exposure capability of standard subassembly hardware and have done so without breach. Irradiation in EBR-II has indicated that many design options are available to deliver high fuel burnup (>15 at.%) and reliable operating performance under both steady-state and transient operating conditions. Postirradiation examination of these designs has provided data to support the modeling of metallic fuel performance and to assist designers in optimizing fuel designs for advanced reactors.

2. Basic Fuel Designs

The fuel alloy used for the Mk-II design was uranium-fissium (U-5Fs: 5 wt % fissium where Fs is nominally 2.5% Mo, 1.9% Ru, 0.3% Rh, 0.2% Pd, 0.1% Zr, and 0.01% Nb). In 1984, work was initiated to demonstrate an acceptable fuel design for the IFR program based on the uranium-plutonium-zirconium fuel alloy. There were several advantages to increasing fuel element diameter, so a 61-element subassembly and a 37-element subassembly were designed for use in the

experiment program. Fuel alloys irradiated in EBR-II have been based on U-xPu-10Zr, where x included 0, 3, 8, 19, 22, 26 and 28 wt %. A variety of cladding materials have been tested, including the austenitic alloys 316 SS and D9, and the martensitic alloy HT9 which is now the reference choice for ALMRs.^{1,2}

In 1987, the standard EBR-II driver design was changed from the 91-element design to the 61-element design using a larger fuel element; a qualification program was initiated to determine an acceptable burnup limit. This larger fuel element design, the Mk-III design, used U-10Zr fuel clad in 20% CW D9. The design characteristics are shown in Table II.1. The overall fuel element length was increased to 74.9 cm (29.5 in.) to employ maximum plenum volume. The control and safety rod design could not be easily modified to accommodate the larger fuel elements, so the original 61-element design and 316 SS cladding used for the Mk-II fuel element were retained in these subassemblies and only the fuel alloy was changed. These elements were designated Mk-IIC and Mk-IICS, where S indicated a shorter element. The Mk-III manufacturing campaign, that included the equivalent of a full core loading, is now complete with the last Mk-III fueled subassemblies approaching end-of-life in EBR-II. The driver fuel operated up to 10 at.% burnup and was then removed based on subassembly hardware limitations. Although the qualification program for Mk-III fuel has been terminated, some elements in the qualification program have continued irradiation and now exceed 17 at.% burnup.

TABLE II.1. Design Parameters (Nominal) of EBR-II Driver Fuel Elements

Item	Mk-IA	Mk-II	Mk-IIC	Mk-IICS	Mk-III	Mk-IIIA	Mk-IV
Fuel Alloy, wt.%	U-5Fs	U-5Fs	U-10Zr	U-10Zr	U-10Zr	U-10Zr	U-10Zr
Enrichment, wt.% ²³⁵ U	52	67	78	78	66.9	66.9	69.6
Fuel-slug Mass, g	64	52	47	47	83	83	78
Fuel Smeared Density, %	85	75	75	75	75	75	75
Cladding-wall Thickness, cm	0.023	0.030	0.030	0.030	0.038	0.038	0.046
Cladding-wall OD, cm	0.442	0.442	0.442	0.442	0.584	0.584	0.584
Length, cm	46.0	61.2	63.0	53.6	74.9	74.9	74.9
Cladding Material	304L	316	316	316	CW D9	CW 316	HT9
Spacer-wire Diameter, cm	0.124	0.124	0.124	0.124	0.107	0.107	0.107

The Mk-III fuel is being replaced with Mk-IIIA fuel elements that use the same fuel composition in 20% cold-worked 316 SS cladding. This cladding was chosen based on availability and acceptable performance in EBR-II, rather than improved capabilities. Because of irradiation-induced swelling/creep characteristics, the Mk-IIIA element with 316 SS cladding exhibits more diametral strain than seen in either the D9 or HT9 clad elements, and burnup potential may be less, but performance to 10 at.% burnup has been excellent. The fluence/burnup ratio for EBR-II driver, while varying with burnup and core position, produces an average of $1 \times 10^{23} \text{ n/cm}^2$ ($E > 0.1 \text{ MeV}$) maximum exposure to the cladding at 10 at.% burnup. The operating parameters for the Mk-III/IIIA fuels are listed in Table II.2. The qualification program is expected to indicate acceptable performance in EBR-II continuing through the range of 15-20 at.% burnup.

The Mk-IV design, which added the IFR reference HT9 cladding, has been irradiated in qualification subassemblies, but has not been included as a standard driver fuel in EBR-II. The initial procurement of HT9 tubing for the Mk-IV elements has been reserved for use in the Mk-V fuel elements and Mk-IIIA fuel elements are being used until the Mk-V element production can be initiated.

TABLE II.2. Mk-III Operating Conditions*

Reactor Row	Subassembly Flow l/s	Coolant Outlet Temp., °C	Peak Pin Power, kW/m	Peak Inside Cladding Temp., °C
1	6.3	477	49	547
2	6.1	478	50	549
3	5.6	485	48	557
4	4.5	508	47	555
5	4.0	514	44	592
6 (Normal Flow-- Corner)	3.1	523	37	596
7 (High Flow-- Flat)	3.5	518	40	595

*Maximum for hottest operating conditions in a nominal core.

Due to similar performance characteristics of U-10Zr and U-20Pu-10Zr fuel designs, data from the performance of Mk-IV fuel will be used to support qualification of the Mk-V design (U-20Pu-10Zr/HT9).

3. Irradiation Results

To date, over 13,000 Mk-III, -IIIA, -IV, -IIC, -IICS and other special test elements have been irradiated as part of the IFR Demonstration program. A part of these were irradiated in qualification subassemblies for each fuel design. These subassemblies are typically irradiated in a test group of four; three to cover various operating conditions in the core, and the fourth irradiated under 2σ peak cladding temperature conditions. Postirradiation examination data at intermediate burnup is now available for Mk-III, -IIIA and -IV fuel. Because the fuel alloy is U-10Zr in all of these designs, the only differences in performance have been due to the different cladding materials. Several performance characteristics have been measured including fuel swelling, fission gas release, and cladding diametral strain.

a. Fuel Swelling

Fuel swelling has long been recognized as an important feature of metallic fuels, impacting both core neutronics and fuel performance, and has been investigated for IFR fuel.³ To accommodate fuel swelling, the fuel slug is designed to provide a 75% smeared density when it comes into contact with the cladding. This occurs after approximately 2 at.% burnup. The axial growth of the fuel depends significantly on Pu content and has been found to be very low (<6%) for Pu concentrations of 19 wt % and above, but for the binary fuels can be between 6 and 13%. Note, the axial growth of the driver fuel elements compares well with that measured for U-10Zr experimental elements irradiated under a variety of conditions (see Fig. II.1).

b. Fission Gas Release

Fission products include a significant quantity of noble gases ($\approx 25\%$) that contribute to the swelling of the fuel and tend to increase the internal pressure in the fuel element throughout its life. If the fuel swells

Fig. II.1. Axial Elongation for U-10Zr.
(Range of experiment data shown shaded; Mk-IIIA, -IV data range plotted.)

enough, fission gas bubbles that form in the fuel can interconnect allowing the gas to be released to the fuel element plenum. After irradiation, the pressure and volume of gas in the plenum are measured by using a laser to puncture the cladding and collecting the gas released. Pressure and the derived gas release for driver fuels are shown in Table II.3. Gas release characteristics have been found to be independent of fuel alloy (Pu concentration). The pressure created by the gas is usually the major life-limiting phenomenon, so similar lifetimes are expected for fuel elements of like design, relatively independent of Pu concentration.

c. Cladding Strain

The amount of time that fuel elements can now spend in the reactor has increased to the point where thermal and irradiation-induced strain in the cladding (due to swelling and creep) are significant. The overall effect is measured as cladding diametral strain. Swelling characteristics of austenitic stainless steels have led to the use of martensitic stainless steels because they do not exhibit significant irradiation-induced swelling. Creep effects

TABLE II.3. Average Plenum Pressures and Fission Gas Release

Type	Burnup at. %	Pressure*	Gas Release
		MPa (psi)	%
Mk-III	9.2	2.4 (350)	83
Mk-III	14.4	3.6 (520)	80
Mk-IIIA	9.2	2.2 (320)	76
Mk-IV	8.7	2.1 (310)	74

*Pressures are reported for room temperature conditions ($\approx 300^\circ\text{K}$).

become significant when burnup is increased and high stresses are created, and operating temperatures are high. For driver fuels in EBR-II, operating temperatures are relatively low, and differences in the performance of different cladding materials are largely due to irradiation-induced deformation. The deformation characteristics of the various cladding materials are compared in Table II.4. When operating temperatures are increased, the ferritic HT9 cladding loses strength much sooner than the austenitic alloys, and reactor life-time of the fuel element can be significantly decreased.²

d. Off-normal Performance

Behavior of the driver fuel under off-normal conditions (loss-of-cooling, transient overpower) is also tested to demonstrate that the fuel will survive these events without cladding breach. A series of ex-reactor overheating tests are used to qualify the fuel for loss-of-cooling events.⁴ In addition, a recent test has shown that Mk-IIIA and -IV fuel, after irradiation

TABLE II.4. Peak Diametral Strain (≈ 9 at.% Burnup)

Type	Cladding	Average of Peak Strain	Maximum Strain
		%	%
Mk-III	CW D9	0.81	1.17
Mk-IIIA	CW 316	1.07	1.26
Mk-IV	HT9	0.42	0.70

to 9 at.% burnup (near the burnup limit) could survive a 0.1%/s overpower transient to 40% overpower without breach of the cladding.

4. Conclusions

Advanced driver fuels tested in EBR-II have all performed well under both steady-state and transient conditions. Postirradiation examination of driver fuel at the current EBR-II burnup limit has indicated that performance is consistent with previous experimental data. There have been no fuel element failures detected in the Mk-III, -IIIA, and -IV driver fuel programs, and a significant number of Mk-III and Mk-IIIA fuel elements have now been operated to 10 at.% burnup, under various core conditions, and performed well. Although the IFR demonstration program will require conversion to Mk-V fuel elements containing U-Pu-Zr fuel, the performance of the binary Mk-III and Mk-IIIA fuels in EBR-II has exceeded all requirements for the driver fuel.

B. Irradiation Performance of Full-length Metallic IFR Fuels

1. Introduction

The core height for EBR-II (343 mm) is only approximately one-third of that of an ALMR.⁵ To apply the substantial EBR-II database to an ALMR, it is imperative to establish that the greater ALMR core height, i.e., fuel column length, will not adversely affect fuel pin performance. The central issue is whether the increased height and weight of an ALMR fuel column would cause fuel compaction, leading to enhanced localized fuel/cladding mechanical interaction (FCMI); or impeded fission-gas release, leading to, again, enhanced FCMI from increased fuel swelling. To address this issue, the IFR-1 irradiation experiment with pins containing a 914-mm-long fuel column was conducted in the Fast Flux Test Facility (FFTF) between September 1986 and October 1988.

2. Test Description

The IFR-1 irradiation test consisted of 169 fuel pins: 18 with U-19wt.%Pu-10wt.%Zr fuel, 19 with U-8wt.%Pu-10wt.%Zr fuel and the remaining 132 pins with U-10wt.%Zr fuel. To attain approximately the same linear power, different ^{235}U enrichments were used for the three fuel types. The cladding for all pins was 20% cold-worked D9, with a 6.86-mm-OD and a 0.56-mm wall. (D9 is a titanium-stabilized Type 316 stainless steel.) The 914-mm-long fuel column consisted of three stacked, equal-height, solid fuel slugs produced by injection casting. Immediately above and below the fuel column were depleted U-10wt.% Zr blanket slugs, each 165-mm-long. The nominal diameter of the as-cast fuel and blanket slugs was 4.98 mm, yielding a smeared density of 75%, the same as in the reference IFR fuel design. The plenum/fuel volume ratio of the pins was 1.0 at the reactor operating temperature after expansion of the sodium. Pin-to-pin spacing in the assembly was maintained with 1.37-mm-dia, 20% CW D9 wires wrapped on the fuel pins at a 152-mm axial pitch.

The irradiation vehicle for the IFR-1 test was a standard FFTF open-core subassembly with a D9 duct. An orifice incorporated in the vehicle provided the desired flow of sodium coolant. There was no built-in instrumentation in the subassembly; however, the above-core instrumentation tree in the FFTF provided data on the subassembly outlet temperature of the primary coolant during irradiation.

3. Irradiation History

The burnup goal of 10 at.% for the IFR-1 test was successfully achieved in five consecutive FFTF cycles (9A through 10B) in 620 equivalent-full-power days (EFPDs) without cladding breaching or anomalous events.

There was no interim reconstitution between the cycles; the same Row-4 core position and subassembly orientation was maintained throughout the irradiation. The peak linear power of the pins at the beginning-of-life (BOL) ranged from 44 to 48 kW/m, depending on the radial position of the pin. At the end-of-life (EOL), the linear power decreased to 29-33 kW/m because of fissile burnout. The peak pin burnup and fast fluence ($E > 0.1$ MeV) at EOL were

10.1 at.% and $15.6 \times 10^{22} \text{n/cm}^2$, respectively. Because of the chopped-cosine flux distribution in the FFTF, the power, burnup, and fluence at the ends of the fuel column were approximately one-half the peak values. The peak temperatures of the inside surface of the cladding were, respectively, 608 and 533°C at BOL and EOL.

4. Test Results

a. Assembly Performance

Following reactor discharge, the IFR-1 vehicle and fuel element bundle were examined to evaluate the condition of the assembly. Duct dilation was small, 2.7% max flat-to-flat and 1.0% max corner-to-corner, and within the FFTF database.⁶ There was essentially no bowing or elongation of the duct. The pull force required to remove the duct from the fuel-pin bundle was minimal, indicating insignificant bundle/duct interaction. Visual inspection of the fuel-pin bundle confirmed that the condition of the bundle was excellent, with no discernible signs of distortion or cladding wear.

b. Fuel Column Stability

Fuel column stability, the key figure-of-merit for the IFR-1 test, was evaluated with postirradiation neutron radiography and axial gamma scanning. Ancillary evidence of fuel column elongation during the irradiation was obtained from the measured coolant temperature at the subassembly outlet.

Eighteen of the 169 test pins were radiographed; all showed mild elongation of the fuel column. The average elongations were 7.0, 6.1, and 2.5% for the 0, 8, and 19 wt % Pu fuel types, respectively. These magnitudes are more modest, by 1-2%, than those of their respective EBR-II siblings,⁷ as shown in Fig. II.2.

Both radiographic and gamma scan data show that the postirradiation axial fissile distribution within the fuel column is uniform, except at the very top, where the fuel density is noticeably lower. The reduced density at the fuel top was apparently the result of a less-restricted growth into the plenum. None of the pins examined showed signs of deleterious local

fuel compaction. The ^{106}Ru scan data, which mimic the fission density profile, for the representative U-19wt.%Pu-10wt.%Zr Pin 181193 is shown in Fig. II.3.

Fig. II.2. Fuel Column Elongation in the IFR-1 Fuel Pins. (Closed symbols: IFR-1; open symbols: EBR-II database.)

Fig. II.3. Distribution of ^{106}Ru Activity in IFR-1 Pin 181193, Showing No Evidence of Localized Fuel Compaction. (The activity is lower at the slug/slug interfaces [arrows] due to the lower local fuel density (TOP and BOF refer to top of fuel and bottom of fuel respectively.)

Because of fuel column elongation, the top end of the fuel was raised above the core, where the fission densities were markedly lower, resulting in a reduction of the thermal output of the assembly. This behavior was detected by the FFTF instrument tree measuring the outlet temperature of the assembly coolant. The temperature data suggest that axial growth occurred early in life, starting at about 0.2 at.% burnup and ending at about 0.6 at.% burnup (between the 10th and 40th EFPD.) The lack of changes afterward suggests a possible lockup between the fuel and the cladding.

c. Cladding Deformation

The cladding strain profiles for all IFR-1 pins examined were similar. None showed anomalous strain peaks that would suggest enhanced localized FCMI from fuel relocation. A representative strain profile, that of Pin 181193, is shown in Fig. II.4. In regions above and below the fuel column, where cladding loading was from fission gas alone, the cladding strain was essentially zero, indicating that plenum volume in the IFR-1 fuel pins was adequate in accommodating the released fission gas from the fuel. The slight "shoulders" (0.1-0.2%) at the top and bottom ends of the fuel column reflect the small FCMI contribution to the total strain at those locations. In the fuel region, the strain profile was bell-shaped, with a maximum of 3.3% near the axial midplane. Cladding density data, also displayed in Fig. II.4, showed that most of the cladding strain was due to fast-neutron-induced void swelling of the D9 cladding material. The creep component, i.e., the difference between the total and swelling strains, was small (0.6% max).

d. Fission-gas Release and Fuel Restructuring

Fission-gas sampling and metallographic examination was completed for U-19wt.%Pu-10wt.%Zr Pin 181193. The gas collected in the pin plenum represented a fission-gas release fraction of 72%, which is consistent with the EBR-II database (63-78%) for comparable fuels at 10 at.% burnup.⁷ Thus, the data indicate that the longer fuel column did not impede the release of fission gas from the fuel. This result correlates with the cladding strain data, which showed no significant FCMI.

Fig. II.4. Cladding Strain Profile of IFR-1 Pin 181193. (The closed circles represent the component of fast neutron-induced void swelling of the D9 cladding.)

Complex fuel restructuring, i.e., formation of radial zones with different composition and microstructure, resulted from the irradiation. Similar restructuring has been noted in U-Pu-Zr fuels irradiated in EBR-II. The extent of restructuring in Pin 181193 varied along the length of the fuel depending on local power and temperature conditions. A transverse metallographic section at $X/L = 0.70$ and the corresponding alpha and beta-gamma auto-radiographs are shown in Fig. II.5. Fuel swelling caused closure of the sodium-filled, 0.76-mm-dia fuel/cladding gap. Distinct redistributions of Pu and fission products are evident from the alpha and beta-gamma auto-radiographs. Some of the zones are highly porous; these zones probably provided the long-range connectivity in the axial direction that facilitated the release of fission gas to the plenum.

e. Fuel/Cladding Metallurgical Interaction

Metallurgical interaction between the fuel (including fission products) and cladding during the irradiation was minor in the Pin 181193. The depth of cladding reaction ranged from 0 μm at both ends of the fuel column to approximately 60 μm at the axial location of $X/L = 0.70$. Compared with the as-built cladding thickness of 560 μm , the amount of cladding wastage was not significant.

Alpha Auto-radiograph

Betta-Gamma Auto-radiograph

Fig. II.5. Transverse Metallographic Section and α and β - γ Radiographs of Pin 181193 at $X/L = 0.70$

The condition of the fuel/cladding interface at $X/L = 0.70$, where the maximum cladding interaction occurred, is shown in Fig. II.6. This location is directly opposite a fuel surface crack at approximately two o'clock in Fig. II.6. Cladding interaction at other azimuthal locations was considerably shallower. Two reaction bands in the cladding, denoted A and B in Fig. II.6, can be seen. In the microprobe analysis, lanthanide fission products were detected at the far edge of the outer cladding reaction band A, adjacent to the unreacted cladding. The fuel crack probably provided a conduit for enhanced vapor-phase transport of lanthanides to the cooler fuel/cladding interface. In the inner reaction band B, in addition to lanthanides, U and Pu were detected. In both the inner and outer cladding reaction bands, Ni was nearly completely depleted. The loss of Fe, on the other hand, was minor and limited to the inner band. The Fe and Ni lost from the cladding were detected in the fuel adjacent to the reacted cladding. Chromium, the other major constituent in D9, showed the least mobility in the interdiffusion, being concentrated in a sub-band on the inward side of band A. The source of the Cr concentration was apparently from band B.

5. Discussion

The IFR-1 test was terminated when the design goal burnup of 10 at.% was reached. Judging from the excellent condition of the discharged fuel pins, e.g., minimal fuel/cladding mechanical and metallurgical interactions, it is reasonable to expect that the lifetime limit of the IFR-1 fuel pins may be substantially beyond the attained 10 at.% burnup. The early concern that longer fuel columns may cause localized fuel compaction, enhanced FCMI, and premature fuel pin failure was thus unfounded. Six other IFR fuel irradiation experiments have been successfully completed in the FFTF with more than 800 HT9-clad U-10Zr fuel pins, many with operating conditions substantially more aggressive than those of the IFR-1 pins. All have attained high burnup, with the peak exceeding 16 at.%, and none exhibited cladding breach. The results from these and the IFR-1 tests clearly demonstrated that full-length metallic fuel pins possess excellent high-burnup potential.

Fig. II.6. Fuel/Cladding Metallurgical Interaction in Pin 181193. (The arrows denote the extent of cladding reaction. The bright sub band in band A is enriched in Cr and lanthanide fission products.)

6. Conclusions

The IFR-1 test successfully reached the goal burnup of 10 at.%. All pins maintained their cladding integrity. The results from postirradiation examinations indicate that the irradiation behavior of full-length IFR-1 fuel pins is comparable to that of shorter EBR-II siblings. Fuel column stability was excellent and cladding strain from fission-gas loading and FCMI was insignificant. These positive results support the use of the extensive EBR-II irradiation database for the development of prototypical ALMRs.

C. Fuel/Cladding Compatibility in U-19Pu-10Zr/HT9-Clad Fuel at Elevated Temperatures

1. Introduction

This segment of the fuel/cladding compatibility program focused on the study of U-19 wt.% Pu-10 wt.% Zr fuel and HT9 cladding. Samples for this study were taken from three fuel elements irradiated in EBR-II Subassembly X441: element DP-17 to approximately 5.6 at.% burnup, and elements DP-16 and 21 to approximately 11 at.% burnup. The specific objectives of these tests were to determine (1) the threshold temperature above which eutectic formation may occur during off-normal reactor events; (2) the variation of the threshold temperature with burnup; and (3) the variation of cladding penetration and penetration rates with burnup, test temperature, and time at test temperature.

These objectives support the safety case for the next EBR-II driver core, Mk-V, which will have U-20Pu-10Zr fuel and HT9 cladding and a burnup goal of 10 at.%. The slight difference in Pu content between the Mk-V driver and the fuel used in this investigation is not expected to result in significant differences in FCI. The operating conditions for the X441 subassembly were more aggressive than those planned for the Mk-V core: 600°C peak cladding inner-surface temperature and 49 kW/m peak linear power at beginning of life (BOL) for X441 versus 580°C and 43 kW/m at BOL for Mk-V. These operating parameters therefore envelope those of the Mk-V design.

2. Test Conduct

The fuel/cladding compatibility tests were conducted in the Fuel Behavior Test Apparatus (FBTA)⁸ in the Alpha-Gamma Hot Cell Facility (AGHCF) at the Argonne-Illinois site. The procedures and the apparatus for the tests have been described elsewhere.^{8,9}

Prior to testing, samples from each element were examined in the as-irradiated state to determine the extent of FCI that occurred from irradiation to the given burnup. The results (Table II.5) show clear dependency of cladding penetration with cladding temperature during irradiation. Cladding temperatures and linear power were calculated by the LIFE-METAL computer code. In the case of the DP-21 sample at an axial position (X/L) of 0.9, the effect of local linear power rating, i.e., the radial temperature gradient driving force to bring the fission products to the fuel surface, and perhaps the local burnup are seen to have a greater effect than the local cladding temperature. Microprobe analysis indicated that iron migration into the fuel and lanthanide penetration into the cladding were greater for the higher-burnup DP-16 and DP-21 samples than for the DP-17 samples. However, the higher iron content was principally in areas of radial cracks in the fuel that had healed with low-zirconium fuel. The ingress of iron, therefore, was circumferentially nonuniform. The greater lanthanide

TABLE II.5. Cladding Penetration in As-irradiated (Pretest) Samples

Sample and Axial Position	Local B/U (at.%)	Linear Power (kW/m)	Cladding ID Temp. (°C)	Cladding Penetration (μm)
DP-17				
0.13	5.1	46.9	439	0
0.53	5.5	50.8	514	0
0.87	4.6	42.0	561	28
DP-16				
0.22	10.9	47.6	451	0
0.78	10.3	44.9	536	31
DP-21				
0.50	11.4	49.9	505	4
0.78	10.2	44.3	546	57
0.90	9.2	40.0	560	37

penetration into the cladding was associated with greater concentrations of lanthanides at the fuel/cladding interface because of the higher burnup. Although similar concentrations were found in the as-irradiated medium-burnup samples, they were not as extensive and their presence in the cladding was not significant.

Metallography was conducted on every tested sample to search for evidence of eutectic formation and to measure the extent of cladding penetration. Even with the uniformly isothermal conditions in the test specimen in the FBTA, FCI in the samples was rarely uniform circumferentially. For conservatism, only the maximum penetration was measured and reported.

Selected samples were analyzed by electron microprobe analysis (EMP) and scanning electron microscopy (SEM) to determine the phases and constituents present in the fuel and cladding after eutectic formation and to provide insight into the FCI mechanism.

3. Results

A summary of the 25 compatibility tests conducted in this phase of the program is shown in Table II.6. In addition to the test parameters, the table includes the element operating parameters (burnup, linear power, and cladding inner-diameter temperature) for each test sample. These parameters were calculated by interpolating output data from the LIFE-METAL computer code. The linear power and cladding temperature are burnup averaged.

The following sections describe the relevant test results.

a. Threshold Temperature

Eutectic formation begins at the fuel/cladding interface and changes the microstructure of the fuel from the typical three-zone structure to one that contains large pores distributed in the periphery, as shown in Fig. II.7. This porosity delineates a region that was above the solidus temperature and below the liquidus temperature of the "fuel," i.e., the local material composition representative of the changes that have accrued with the combination

TABLE II.6. Summary of Recent FBTA Compatibility Tests

Sample and Test Number	Axial Position (X/L)	Local B/U (at.%)	Linear ^a Power (kW/m)	Cladding ^a ID Temp. (°C)	Test Temp. (°C)	Test Duration (h)	Max. Total Cladding Penetration (μm)	FCI Fuel Area ¹ Melt Fraction ^b (%)
<u>DP-17</u>								
91-05	0.48	5.6	51.2	505	725	7.0	74	4
91-03	0.57	5.5	50.5	520	740	1.0	0	0
91-09	0.39	5.6	51.5	489	770	1.0	27	10
91-08	0.42	5.6	51.2	494	800	0.1	18	9
91-07	0.45	5.6	51.2	501	800	0.5	78	33
91-01	0.55	5.5	50.5	517	800	1.0	114	34
91-06	0.50	5.6	51.2	510	800	2.0	142	26
<u>DP-16</u>								
91-33	0.69	10.7	46.6	525	625	1.0	34	0
91-30	0.75	10.4	45.3	533	650	1.0	46	0
91-32	0.61	11.1	48.6	515	650	12.0	36	0
91-35	0.59	11.1	48.6	510	650	36.0	75	0
91-36	0.64	10.9	47.9	517	660	12.0	81	12
91-31	0.72	10.5	45.9	528	675	1.0	59	5
92-02	0.25	11.0	48.2	456	675	1.0	29	0
91-34	0.67	10.8	47.2	521	675	12.0	140	31
91-29	0.83	9.7	42.6	540	700	1.0	58	9
<u>DP-21</u>								
91-19	0.88	9.3	40.7	559	650	1.0	29	0
91-17	0.83	9.8	42.6	554	700	1.0	76	13
91-15	0.89	9.2	40.4	560	740	1.0	87	18
91-18	0.54	11.3	49.2	512	740	1.0	58	12
91-16	0.85	9.6	42.0	556	770	1.0	80	66
91-12	0.68	10.8	46.9	534	800	0.1	42	27
91-13	0.72	10.6	46.2	540	800	0.5	79	63
91-28	0.52	11.3	49.5	508	800	0.5	37	58
91-11	0.80	10.0	43.6	550	800	1.0	100	100
91-14	0.73	10.5	45.9	541	800	2.0	104	100

^aTime-averaged over burnup.^b0 indicates no eutectic formation, i.e., below threshold temperature.

of burnup and test exposure. Microprobe analysis shows that the region of eutectic formation coincides with the depth of iron migration into the fuel from the cladding. Initially, eutectic formation results from the iron already in the fuel due to interdiffusion during irradiation. More eutectic formation occurs as additional iron diffuses during the test at elevated temperatures, i.e., above the cladding's steady-state irradiation temperature. Because iron migration into the high-burnup fuel was circumferentially nonuniform, the initial eutectic formation was likewise circumferentially nonuniform and localized. At the higher temperatures and in longer-duration tests, when iron migration during the test was significant, eutectic formation was more uniform.

Fig. II.7. Comparison of Microstructures in (a) As-irradiated and (b) As-tested Fuel Samples. Large pores in as-tested sample delineate solid-plus-liquid region developed during testing.

For the DP-17 element at 5.6 at.% burnup, the threshold temperature for eutectic formation was 740 to 770°C for a 1-h test and decreased to 725°C after 7 h when in-situ iron migration became significant. At 800°C for 6 min (0.1 h), the areal melt fraction was comparable to that at 770°C for 1 h; this indicates that iron already in the fuel was the likely causative factor.

For the higher-burnup elements (DP-16 and DP-21 at 11 at.% burnup), the threshold temperature was 650 to 675°C for a 1-h test, and 650 to 660°C for

a 12-h test. A 1-h test at 675°C with fuel from the bottom of the element (Test 92-02) showed no indication of eutectic formation. This, coupled with the absence of FCI at this elevation in the as-irradiated condition (Table II.5), indicates that the cladding temperature during irradiation, 450°C, was too low for interdiffusion and that the 675°C test temperature was too low and the 1-h test duration too short to cause iron diffusion during the test. Tests 91-18 and 91-28, with fuel from near the midplane of element DP-21, suggest that a cladding irradiation temperature of about 510°C is necessary for iron migration into the fuel.

b. Cladding Penetration

The total maximum cladding penetration, including the as-irradiated component, for all tests is shown in Table II.6. Inspection of the table indicates penetration into the cladding at lower temperatures for the higher-burnup fuel than for the medium-burnup fuel, but that at 800°C the total penetration for both burnups is essentially the same. Furthermore, penetration as a function of time at 800°C has actually leveled off, as shown in Fig. II.8, for both the medium- and high-burnup specimens. Further inspection of Tables II.5 and II.6 for DP-21 Tests 91-15 and 91-18 and 91-13 and 91-28 indicates that penetration occurring only during the FBTA tests was essentially the same for the

Fig. II.8. Cladding Penetration vs Time for Tests at 800°C

same test conditions regardless of the samples' cladding temperature during irradiation. In these tests and with these elements, the as-irradiated cladding condition does not appear to have had an effect on the amount of penetration at the elevated temperatures.

c. Cladding-penetration Rates

The net cladding-penetration rates during the tests were calculated from the as-irradiated penetration depths given in Table II.5, and the total posttest penetration depths given in Table II.6. By interpolation between measured elevations given in Table II.5, an as-irradiated FCI value was calculated for each axial position of an FBTA-tested sample. These values were then subtracted from the total posttest FCI penetration, in Table II.6, to yield a net penetration that is the basis for determining the penetration rate during the elevated-temperature test. The net cladding penetration rates for both the medium- and high-burnup fuels in 1-h tests fall within the range of values reported for the lower-burnup U-19Pu-10Zr/HT9 fuels and other metallic fuel systems,⁹ as shown in Fig. II.9. As plotted in the figure, the data suggest that a different Arrhenius-type relationship exists between the penetration rate and the test temperature for each burnup regime.

d. Metallurgical Changes in the Cladding

Two different microstructural regions of the high-burnup-fuel cladding were seen in the post-FBTA-test cladding: a darker etching structure on the outer half of the cladding, and a whitish "denuded" region next to the fuel. Similar results were seen for the 5.6-at.% burnup samples. An SEM and a microprobe examination on a similar sample found no constituent gradients to explain the denuded region; however, neither the SEM nor the microprobe used can detect low-atomic-number elements, such as carbon. The denuded region was found to be softer than the nondenuded material. The hardness of the cladding decreased from about 290 DPH in the as-irradiated state to 190 DPH in the nondenuded material and to 150 DPH in the denuded region after a 2-h test at 800°C.

Fig. II.9. Cladding Penetration Rate vs Temperature for 1-h Tests

The microstructure of the denuded region changed from the tempered martensite structure of HT9 to one that consisted of large ferrite grains and coarsened carbides as shown in Fig. II.10. This microstructure is typical of decarburization in tempered martensite steel.¹⁰ Grain size decreased on moving from the fuel/cladding interface into the unaffected cladding. If decarburization did occur, the larger grains would have resulted because there are no carbide particles to pin the grain boundaries and mitigate the growth above the grain-growth temperature.

4. Discussion

The data in Table II.6 show that the effect of burnup is essentially to reduce the temperature at which interaction occurs, but that the rate at which the cladding penetration rises with test temperature is lower for higher-burnup fuel than for medium-burnup fuel. The lower slope of the net penetration rate (Fig. II.9) indicates that a different mechanism is operating in the

Fig. II.10. Denuded Layer After Etching, Lanthanide-rich Layer Is Dark

higher-burnup regime, and it further implies that the burnup effect would not be detrimental at the higher temperatures. Indeed, the penetration rate at the higher temperature is at the lower side of the Fuel/Cladding Chemical Interaction (FCCI) data band that is used for design purposes. Although the rates at the lower temperatures are at the upper end of the data band, the data are at the lower bound for liquid-phase formation, and the data for solid penetration (open circles in Fig. II.9) show a very rapid decrease in rate.

The different mechanisms operating in the medium- and high-burnup fuels may be attributed to the greater abundance of lanthanide fission products in the higher-burnup fuel. It is probable that the lanthanides also promote iron transport into the fuel, which leads to a lower threshold temperature for eutectic formation in that fuel. A lanthanide-enhanced mobility could reduce the thermal energy required to transport the incremental iron content necessary to cause formation of a liquid phase mixed with the solid.

The formation of a liquid-plus-solid mixture in eutectic formation would not have negative implications on reactor safety because, as can be seen in Fig. II.7, the resulting material is a self-supporting foam, not a free-falling

liquid. In whole-element furnace tests, such foam has been found to move axially into the element plenum. Furthermore, those tests have shown that fuel elements maintain their integrity by significant margins during simulated off-normal reactor events that produce such foams.¹¹

5. Conclusions

Tests conducted at elevated temperatures in the Fuel Behavior Test Apparatus on irradiated U-19 wt.% Pu-10 wt.% Zr fuel clad with HT9 have provided the following information:

1. The cladding penetration mechanism at 11 at.% burnup is different than that at 5.6 at.% burnup. The presence of lanthanide fission products at the fuel/cladding interface apparently causes the difference.
2. Cladding penetration begins at a lower temperature in the higher-burnup fuel, but the rate of increase as a function of increasing test temperature is lower in the 11 at.% burnup fuel than in the 5.6 at.% burnup fuel.
3. For both 5.6 and 11 at.% burnup fuels, the cladding penetration rate falls off with increasing time at a given test temperature.
4. The results from the 5.6- and 11-at.% burnup fuel fall within the FCCI data correlation for 1-h duration tests.
5. The threshold temperature for eutectic formation in a 1-h test on a U-19Pu-10Zr/HT9 element is 740 to 770°C for a 5.6 at.% burnup element and 650 to 675°C for an 11 at.% burnup element.
6. The element operating history (burnup, cladding temperature, and linear power) can lower the threshold temperature for FCI melting but apparently has little effect on cladding penetration at elevated temperatures.

D. Performance of HT9 Clad Metallic Fuel at High Temperature

1. Introduction

Steady-state testing of HT9 clad metallic fuel at high temperatures was initiated in EBR-II in November of 1987.^{12,13} At that time U-10 wt.% Zr fuel clad with the low-swelling ferritic/martensitic alloy HT9 was being considered as driver fuel options for both EBR-II and FFTF. The objective of the X447 test described here was to determine the lifetime of HT9 cladding when operated with metallic fuel at beginning of life inside wall temperatures approaching 660°C. Though stress-temperature design limits for HT9¹⁴ preclude its use for high burnup applications under these conditions due to excessive thermal creep, the X447 test was carried out to obtain data on high temperature breach phenomena involving metallic fuel because little data existed in that area.

2. Test Description

The subassembly consisted of 19 HT9 clad and 30 D9 (a Ti-modified 316 SS) clad 5.84-mm (0.230-in.) diameter fuel elements with 0.38-mm (0.015-in.) thick cladding and 1.07-mm (0.042-in.) diameter wire wrap. The remainder of the 61 positions in the bundle consisted of solid dummy elements added to reduce the mixed-mean sodium outlet temperature below the administratively imposed limit of 544°C.

The beginning-of-life inside wall temperatures were calculated by the COBRA-WC computer code to range from 630°C to 660°C for the HT9 elements which were located in the center of the hexagonal bundle and from 525°C to 620°C for the D9 elements. The fuel smeared densities were 75% and the peak core-midplane power was 330 W/cm. The plenum-to-fuel volume ratio was 1.5.

The X447 test was interrupted at 4.7 at.% burnup (284 equivalent full power days) for an interim examination which showed all elements intact and in good condition. The subassembly hardware was replaced and the test continued for an additional 335 equivalent full power days to a maximum burnup of 10.0 at.%. Eleven hours prior to the scheduled reactor shutdown, a fuel element breach was detected. An element breach was indicated by the presence of a xenon "tag" gas

in the reactor cover gas. Mass spectroscopy analysis of specifically blended xenon isotopic ratios showed that the "tag" was one which had been injected into the plena prior to closure welding of the X447 elements. A short-lived delayed neutron signal was associated with the breach event, coincident with increased fission gas concentrations. The spike was 140 cps above the normal 370 cps background in EBR-II, and lasted for about 10 min, which is typical for a metallic fuel breach.¹⁵ Previous analysis showed that another breach had occurred 30 days prior to the end of the run. The analysis of this "tag" was ambiguous, but X447's tag mixture was identified as a possible match. As described in detail in the subsequent section, two breached elements were eventually found in the X447 test, suggesting that the earlier breach had indeed occurred at approximately 9.5 at.% burnup. Simultaneous breaches of the two elements was ruled out because the total amount of xenon fission gas detected in the latter breach event was consistent with about 75% of the inventory of a single X447 element.

3. Postirradiation Examination Results

a. Unbreached Elements

At 4.7 at.% burnup the cladding strain in the HT9 clad elements was negligible below $X/L_0 \sim 0.70$ (L_0 = original fuel column length of 34.3 cm). Typical peak diametral strains at top-of-core were approximately 0.8%. Because fuel-cladding mechanical interaction is weak at this smear density and burnup, and cladding irradiation swelling is nil, the localized strain resulted from fission gas loading at this high temperature region of the element.

Fission gas analyses were performed on four unbreached elements after the test was terminated at 10 at.% burnup and the release fraction was found to range from 72% to 76% of theoretical production, typical for U-10Zr at 10% burnup. Assuming an average gas temperature of 600°C, the pressure in-reactor would be 7 MPa (1015 psi) at 10 at.% burnup. Spiral contact profilometry measurements were made on all of the elements. The peak diametral strains ranged from 0.5% to 2.0% in the unbreached HT9 clad elements with maxima at top-of-core as expected for thermal creep dominated deformation. Failure strains in pressurized tube furnace tests are typically between 2% and 4% strain, indicating that few of the unbreached elements had significant margin to failure remaining

at 10 at.% burnup. The D9 elements had peak strains ranging from 0.6% to 1.1% with maxima near the core-midplane as expected for swelling and irradiation creep dominated deformation.

b. Breached Elements

Postirradiation element weights showed that two of the HT9 clad elements (DP70 and DP75) had lost 2.9 and 2.7 g, respectively, typical for metallic fuel breaches when bond sodium and fission gas are expelled as the high pressure fission gas rushes out of the breach site. Neutron radiography indicated that the bond sodium was indeed absent in these two elements. This much weight loss represents a significant venting of fission gas and bond sodium since only 0.9 g of stable fission gas is produced at 10 at. % burnup and 1.7 g of bond sodium is loaded during manufacture. If one assumes 75% of the fission gas is located in the plenum and open fuel porosity, then 2.4 g of weight loss can be accounted for if complete venting (and no fuel loss) occurred. The remaining weight loss (0.4 g on the average) likely comes from the 0.8 g of fission product cesium. Cesium is insoluble in the fuel and is found as a liquid in isolated pores or alloyed with the bond sodium because they are completely soluble above 98°C, the melting point of sodium. Loss of cesium during breach is consistent with gamma-ray scans for the Cs-134 and Cs-137 isotopes, which showed that the intact elements had about twice the cesium activity in the fuel column as the breached elements.

Visual examination revealed sodium deposits on the cladding exterior at the top of the fuel column. However, after cleaning off the deposits, which presumably came from residual bond sodium oozing out of the breach, no cracks were visible at 7X. Longitudinal irregularities were seen, which were later identified as necked regions associated with cracks. The two breached elements were adjacent to each other in the hexagonal bundle. The breach locations did not face each other. However, both breaches were located in the narrowest region of the coolant channel. Figure II.11 shows the general fuel structure in the region of the breach in element DP70.

Fig. II.11. Transverse Optical Metallography (As-polished) of the Breach Site in Element DP70

Optical metallography showed that the breach occurred at the top of the fuel column in each element and corresponded to the regions of highest diametral cladding strain (4.0% in DP70 and 1.6% in DP75 including some necking strain). Figure II.12 shows laser profilometry data (averaged 30° azimuthal scans) for the upper 1/5 of the fuel column for the breached elements and the hottest (per COBRA-WC code analysis) intact sibling element. Numerous cracks emanate from the cladding inner surface in the upper 4 cm of the breached elements. While no through cracks were intercepted by the serial grinding process, local necking was observed at the largest cracks which extended almost 90% through the wall. Figure II.13 shows a large crack and associated necking in element DP75. Optical metallography of the unbreached DP04 element showed less pronounced cracking, but narrow cracks of up to 20% of the wall were found associated with fuel/cladding chemical interaction (FCCI) discussed below.

Fig. II.12. Cladding Diametral Profilometry Profiles (30° azimuthal averages for breached Elements DP70 and DP75 and intact Element DP04)

Significant fuel cladding chemical interaction was observed in both breached elements and to a lesser extent in two unbreached siblings. The interaction was associated with two phenomena:

1. Brittle layers form in the cladding adjacent to the fuel that are rich in the lanthanide-series (rare earth) fission products. The layers are nonuniform in depth around the perimeter of the fuel. Iron and nickel have diffused out of this band into the fuel. Lanthanide penetration on grain boundaries ahead of the bulk layer is observed. Microhardness measurements showed the interaction layers to be very hard at ambient temperature (550 to 1080 DPH), and that they contained numerous cracks. Transverse metallographic sections taken at 6.4 mm intervals exposed penetration layers up to 45% of the wall thickness in breached elements and up to 20% of the wall in unbreached elements as shown in Fig. II.14.

Etched

Fig. II.13. Transverse Optical Metallurgy of the Breach Site in Element DP75 in the As-polished and Etched Conditions

Fig. II.14. Penetration Depth of the Rare Earth Fission Products in the HT9 Cladding for Two Breached Elements, DP70 and DP75, and Intact Elements DP04 and DP11

2. Soft layers of coarsened grains lacking the lath structure of the tempered martensite were also observed in the cladding. These regions extended up to 0.06 mm (0.0025 in.) deeper than the lanthanide layer. These layers are thought to be carbon depleted and were found in areas outboard of the lanthanide regions but also in regions where no lanthanide reaction had occurred. These layers were uncracked in the unbreached elements. In the case of breached elements, the soft layers had been penetrated by major cracks which had first penetrated the lanthanide layer.

4. Discussion

Having presented the phenomenology of the postirradiation examination results from breached and intact elements tested at elevated cladding temperatures, the observed breach behavior can be discussed in the context of the issues raised regarding the operability of HT9 clad metallic fuel under these conditions. Given what is known about the creep properties of HT9, were the breaches at 10 at.% burnup unexpected? That is, to what extent did the large amounts of cladding penetration by lanthanide fission products and carbon depletion accelerate the time to failure? Given that the calculations of cladding temperatures are reliable, can the differences in lanthanide penetration between the breached and intact elements be explained?

a. Cumulative Damage Fraction (CDF) Calculations

The use of the cumulative damage or life fraction is a widely used method for predicting the lifetime of components subjected to creep damage at elevated temperatures. In this approach, it is assumed that the time to rupture t_r has previously been determined for constant stress σ and constant temperature T conditions out of reactor. The fractional damage which occurs in time interval dt is simply dt/t_r . It is assumed that damage accumulates linearly so that rupture will occur when:

$$1 = \int_0^{t_f} \frac{dt}{t_r[\sigma(t), T(t)]}$$

In reality, due to the statistical uncertainty in the experiments used to obtain the stress rupture correlations and uncertainties in the in-reactor experiment itself, fuel element breach does occur at CDF values more or less than unity.

A number of CDF calculations were made in an attempt to determine if the breach in element DP70 could have been predicted. A beginning-of-life midwall temperature of 637°C was calculated for the cladding at the breach site. The cladding temperature was assumed to decline 0.06°C per full power day due to fuel burnup.

Estimates of the cladding stress history were made using the measured fission gas pressures and the plenum pressurization rate was assumed to be linear with time. Gas pressure loading was assumed to be the only source of stress. Nominal calculated temperatures were used but cladding wastage was conservatively applied from beginning-of-life. Using 0.076 mm (0.003 in.) of cladding wall thinning (the level of wastage observed in the intact elements), the hoop stress at 10 at.% burnup reached 68 MPa. The CDF for this case was 0.007, indicating a very low probability of breach. The creep rate for HT9 in this temperature regime increases rapidly with temperature. For example, a 15°C increase in cladding temperature increased the calculated CDF to 0.03, still a low probability of breach. Raising the midwall temperature 25°C increases the CDF to 0.09, still a low failure probability but one which would generally be avoided by proper design or operating conditions.

If the maximum lanthanide penetration which was observed in the breached elements was applied, i.e., 0.17 mm (0.0067 in.), the calculated hoop stress at 10% burnup increases to 98 MPa. For the nominal temperature condition, the CDF at 10% burnup is 0.06. Applying the 15°C temperature increase to this case raised the CDF to 0.3, while the 25°C increase raises the CDF to 0.8. CDF values of 0.3 and 0.8 represent a significant probability of breach.

Since significant cladding wastage applied from beginning-of-life and temperatures somewhat above nominal were required to achieve CDF predictions representing a significant probability of failure, it appears that stress rupture damage has been underpredicted in the breached elements. However, plausible explanations for the apparent underpredictions of stress rupture damage can be made. The obvious ones would be a stress rupture correlation with large uncertainties in the temperature and stress regime of interest, temperature calculation errors (a bias toward cold temperatures), "hot spots" in the bundle, or fuel/cladding mechanical interaction forces that were ignored. However, irrespective of these effects, it appears that the fuel/cladding interaction has not been modeled in sufficient detail to judge the true nature of the discrepancies. Sharp cracks spanned the brittle lanthanide band in the cladding, even in an unbreached element. The unaffected cladding is therefore subjected to stress concentrators and would likely suffer a loss in lifetime compared to an unnotched sample subjected to the same thermal and pressure

conditions. Second, the coarse-grained, carbon (or carbide) depleted zone outboard of the lanthanide zone may not be as creep resistant as the bulk HT9 it has replaced. The high temperature strength of HT9 depends on the size, number and spacing of the carbide precipitates, and it may be inappropriate to assume that the carbon depleted zone has strength equal to the nondepleted zone.

The CDF methodology for predicting breach in the X447 elements has met with limited success. More work is required to quantify the effects of crack initiators, carbide-depletion, and uncertainties in temperature calculations and ex-reactor stress rupture data.

b. The Effect of Breach on Fuel Temperatures

Approximately twice the lanthanide penetration was observed in the two breached elements than in the two intact elements. Breaches should occur in the elements with the highest cladding degradation assuming similar stress states, but temperature calculations show that the two breached elements are not significantly hotter than others in the X447 test. The peak beginning-of-life inside cladding temperatures for the four elements destructively examined were calculated as follows: DP04: 658°C, DP11: 654°C, DP70: 654°C, DP75: 639°C. Peak beginning-of-life fuel centerline temperatures are likewise not very different: DP04: 752°C, DP11: 747°C, DP70: 746°, and DP75: 730°C.

The thermal conductivity of metallic fuel is highest at beginning-of-life and decreases with burnup as the fuel swells and the fuel/cladding gap is redistributed as porosity in the bulk fuel. Significant pore interconnection is geometrically favored and experimental observations show that the bond sodium enters ("logs") the open porosity. This tends to improve the conductivity by offsetting somewhat the effects of the gas-filled pores.

As described in Sect. D.3.b, significant sodium loss took place after breach. Because logged sodium increases fuel thermal conductivity, it follows that expulsion of the sodium after breach can raise fuel temperatures. This can be expected to increase the migration rates of the lanthanides toward the cladding which in turn allows more FCCI to occur after breach. The magnitude of the effect would of course be dependent on the amount of time spent at full power in the breached condition.

To test this hypothesis, calculations were made of radial fuel temperature profiles as a function of the fraction of sodium logged for the conditions at or near the breach site in the X447 elements at 10% burnup. The fuel surface temperature was fixed at 677°C both before and after breach. The transverse metallography was used to construct a two-zone porosity model. The inner zone extended from $r/r_0 = 0$ to $r/r_0 = 0.33$ and was assigned a porosity fraction of 50%. The outer zone was assigned a total porosity of 33%, yielding the overall porosity of 35%.

Because the exact amount of sodium logging is not known, bounding calculations were performed for the case where sodium filled all available pore space ($B = 1$) and for the case of zero sodium logging ($B = 0$) (Fig. II.15). Two intermediate cases are also shown for 10% and 50% sodium logging ($B = 0.1$ and $B = 0.5$). Significant temperature increases can be expected whenever fuel with large amounts of logged sodium lose their sodium inventory. For any postulated mechanism of lanthanide transport, fuel temperature increase would tend to

Fig. II.15. Fuel Temperature Profiles for 10 at.% Burnup at the Breach Site Showing the Effect of B , the Fraction of Pore Volume Which Is Logged with Sodium

increase the kinetics of the process. One of the mechanisms for fission product transport in metallic fuel currently being investigated is vapor transport via interconnected porosity. This mechanism is based on the fact that a gradient in fission product vapor pressure from fuel centerline to fuel surface will cause vapor transport down the temperature gradient toward the cladding interface. The ratio of the difference in the vapor pressure at fuel centerline to that at fuel surface has been calculated for the cases of complete sodium venting ($B = 0$) and complete sodium logging ($B = 1$). For these extremes, the vapor pressure difference increases by the following factors: Nd:90, La:315, Ce:300, Pr:135, Sm:25. Thus, at least for the case of vapor transport, enhanced lanthanide migration after breach is plausible because the driving force increases significantly with fuel temperature gradient.

The enhanced fuel cladding chemical interaction observed in the breached elements compared to unbreached siblings needs to be understood. Because the mechanism described above is strictly a postbreach phenomenon, it would be unnecessarily conservative to use the FCCI data from breached elements to derive a steady-state cladding wastage correlation. Comparisons of breached versus unbreached elements in other tests are needed to clarify this issue.

5. Conclusions

The following observations were made for the performance of high temperature metallic fuel irradiated in the X447 test.

1. Fifteen HT9 clad U-10Zr fuel elements were irradiated at peak cladding temperatures between 630° and 660°C, with two cladding failures occurring at 9.5 at.% and 10.0 at.%.
2. The breach events were benign with regard to reactor operations.
3. Peak diametral strains in the intact HT9 clad elements ranged from 0.5% to 2.0%. The breached elements showed strains of 1.6% and 4.0% which included some contribution from local necking.

4. Significant fuel/cladding chemical interaction was observed in the hottest cladding region. Lanthanide and cladding constituent interdiffusion resulted in brittle layers in the cladding which were prone to cracking in both the breached and intact elements examined. A soft grain-coarsened layer which may have been carbide-depleted was also observed beyond the lanthanide layer.
5. Cumulative damage fraction calculations indicated that the element lifetime was shorter than expected.
6. A possible mechanism for accelerated postbreach fuel/cladding chemical interaction was postulated based on fuel overheating due to sodium bond venting.

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III. PYROMETALLURGICAL PROCESS DEVELOPMENT

A. Process Flowsheet and Chemistry Studies

The present pyrochemical process flowsheets will handle any mix of fuel from the IFR; they yield products from which fresh fuel can be fabricated and they incorporate fission products and cladding hardware into waste forms suitable for long-term storage. The electrorefining step that is at the heart of the pyrochemical process may be summarized as follows. Spent fuel in its stainless-steel cladding is chopped into approximately quarter-inch lengths and placed in an anode basket. The anode basket is introduced into an electrorefining vessel that contains molten LiCl-KCl eutectic salt over a cadmium pool at 550°C (775 K). Sufficient oxidizing agent, such as CdCl_2 , is added to oxidize rare earth, alkali, and alkaline earth metals to their chlorides; these remain in the salt. Enough additional oxidant is added to maintain 2 mol% of actinide chlorides in the salt. The basket is connected to a dc power supply and made anodic; nearly pure uranium is removed from the spent fuel by electrotransport to solid cathodes, then the transuranic (TRU) metals (Pu, Np, Am, and Cm) and any remaining uranium in the feedstock are electrotransported to liquid cadmium cathodes. The cadmium cathode product will be contaminated with small amounts of rare earths. Because of the radioactivity of Am, Cm, and the residual rare earths, this product requires remote handling. Noble metal fission products remain in unoxidized form throughout the process; they are removed in the basket with the cladding hulls, although a fraction may fall to the bottom of the electrorefiner. Radiation levels and heat release that result from buildup of fission products require that rare earth chlorides be periodically removed from the salt by reduction into cadmium solution, and that accumulated noble metals be removed by filtration.

A baseline critical path analysis and a flowsheet having heat and isotopic mass balances were contributed to the updated operational strategy for the IFR pyroprocessing demonstration.¹ An "advanced" IFR flowsheet with compositions and operating conditions has also been generated, for use in guiding process and equipment development. This flowsheet will be used to evaluate proposed process changes that appear feasible, but have not yet been tested. Feedstock processing and product processing in the advanced flowsheet are similar to those in the

demonstration flowsheet for the planned Fuel Cycle Facility (FCF) at EBR-II; the differences between the flowsheets lie mostly in the area of fission product removal and waste handling. The advanced flowsheet incorporates the following ideas: (1) countercurrent extraction for oxidation, reduction, and removal of TRU elements from the electrolyte salt [this allows rapid, continuous-loop processing of the salt for removal of rare earth fission products and returns TRU elements to the electrolyte]; (2) removal of fission product metals (including Sr, Ba, and Cs) from the electrolyte using zeolites [this increases the useful lifetime of the salt, decreases the salt waste volume, and results in a much less radioactive salt waste]; (3) salt electrolysis to provide process oxidant (UCl_3) and reductant (Li in Cd solution) [this avoids the increase in salt waste volume associated with nonrenewable reagents, and also avoids the deposition of cadmium in the electrolyte, and hence in the waste, that is caused by oxidation with CdCl_2]; (4) retorting of volatile metals from the feedstock [this allows Cs, Rb, and Na to be handled separately from the salt, and prevents sodium buildup from raising the liquidus temperature of the electrolyte to the point where the entire salt mass must be discarded]; and (5) elimination of the lower cadmium pool from the electrolyte [introduction of cadmium into the electrolyte proper is avoided by use of UCl_3 as the oxidant, and actinides are now electrotransported directly from the anode basket to the cathode; it thus becomes possible to avoid cadmium contamination of cladding hulls and noble metal wastes from the electrolyte, and removal of solids that accumulate in the electrolyte may also be easier without the cadmium pool].

In all flowsheets, removal of pure uranium on a solid mandrel electrode is possible because uranium chloride is considerably less stable than the chlorides for the other actinides and the active-metal fission products. In cadmium solution at process temperature, however, there is a compensating stabilization of the rare earths and the TRU metals due to formation of intermetallic compounds with cadmium; uranium in cadmium solution does not form an intermetallic compound. As a result, mixtures of actinides and fission products are found in cadmium solutions that contact the process salt, such as the cadmium cathode and the cadmium used for salt renewal processing (Sect. III.C). Because predicting the compositions of the salt and the cadmium that contacts it is critical to process development, the distributions of actinide elements² and the most significant rare earth, alkali, and alkaline earth elements^{3,4} between cadmium and molten LiCl-KCl have been measured.

The distributions are described by pairwise separation factors; at equilibrium, the distribution of elements is such that all pairwise separation factors are satisfied. The general reaction that describes the distribution of a pair of elements between the salt and metal phases is:

By convention, Eq. (1) is written with the more active metal, M, on the left and refer to the M-M' separation factor. The separation factor, SF, corresponding to Eq. (1) is:

$$SF = \frac{[MCl_x]^{3x} [M']^{3/y}}{[M]^{3/x} [M'Cl_y]^{3/y}}, \quad (2)$$

where the brackets indicate the value of the concentration.

The concentrations can be measured in any units, as long as the units cancel. With this choice of equation, the separation factor remains constant with concentration as long as the ratios of the activity coefficients remain constant. Because most actinides and rare earths form trichlorides, the separation factor usually reduces to simple ratios of concentrations.

Measured separation factors have been constant with concentration to within the available precision of analyses; they can, therefore, be multiplied and divided in the same way that equilibrium constants are combined when the corresponding chemical equations are added or subtracted. It is thus possible to calculate the separation factor for any element pair if the separation factors of both elements relative to a third element are known. This approach is used to derive a single set of separation factors for all the elements studied. It also allows derivation of large separation factors that cannot be measured directly. As the distribution ratio of two elements becomes large, it becomes increasingly difficult to measure directly, because at least one metal or chloride concentration becomes very small at all oxidizing conditions.

The chemical analyses that have been made of actinides and rare earths in salt-cadmium systems since the beginning of IFR pyroprocessing work have been statistically analyzed. Separation factors in the 48 wt % LiCl-52 wt % KCl eutectic salt that is presently used, as well as the alkaline earth-rich salt (nominally 35 wt % BaCl₂, 32% CaCl₂, 23% LiCl and 10% NaCl) used early in the development work, are shown in Table III.1.

Laboratory experiments were undertaken to measure separation factors among the rare earths, Ba, Sr and Li.⁴ Separation factors among Nd, Pr, Gd, La, Dy, Ce, Y, Sm, Eu, Ba, Sr, and Li were measured using Inductively Coupled Plasma-Atomic Emission Spectroscopy (ICP-AES). In addition, isotope dilution mass spectroscopy was used to measure the Nd-Pu separation factor with maximum accuracy and, thus, to establish a rare earth-actinide link, since several of the rare earth-neodymium separation factors had been measured. The relatively small concentrations of the americium daughter of plutonium were also measured by gamma counting. Table III.2 gives measured values of the separation factors. Because Sm, Eu, Li, Ba, and Sr are considerably more active than the other metals measured, evaluation of their separation factors, which are comparatively very large, is still underway. Table III.2 gives measured values of the separation factors.

TABLE III.1. Mean Separation Factors for Several Actinides and Rare Earths in Molten LiCl-KCl and Earlier Salt^a

Element	LiCl-KCl Salt	Alkaline Earth-rich Salt
Np	2.12 ± 0.42	-
Pu	1.88 ± 0.09	1.32 ± 0.14
Am	3.08 ± 0.78	2.85 ± 0.98
Cm	3.52 ± 0.59	2.3 ± 1.2
Ce	45 ± 6	48 ± 13
Nd	39 ± 6	33 ± 5

^aQuoted uncertainty is standard deviation of the sample population.

TABLE III.2. Separation Factors Among Rare Earths, Plutonium, and Americium in Molten LiCl-KCl Eutectic^a

Element Pair	Separation Factor
Nd-Pu	23.4 ± 1.2
Am-Pu	1.54 ± 0.15
Nd-Am	15.5 ± 1.1
Nd-Pr	1.02 ± 0.04
Gd-La	1.10 ± 0.33
Dy-La	3.79 ± 1.41
La-Ce	2.72 ± 0.37
La-Nd	3.01 ± 0.71
Y-La	42.5 ± 9.3
Y-Nd	140 ± 21

^aQuoted uncertainty is twice the standard deviation of the mean.

Combining separation factors gives the following series: U, 1 (reference); Pu, 1.88; Np, 2.12; Am, 3.08; Cm, 3.52; Pr, 43.1; Nd, 44.0; Ce, 49; La, 130; Gd, 150; Dy, 500; and Y, 6000. This set is used for all extraction calculations. The more active metals, (Sm, Eu, Li, Ba, and Sr) have separation factors that appear to be larger than 10^{10} ; they are expected to remain almost entirely in the salt phase, even when the actinide and other rare earth elements have been reduced completely into the metal phase.

B. Process Development Studies

1. Electrotransport to Liquid Cadmium Cathode

In the electrorefining process, plutonium metal cannot be collected on a solid cathode because UCl_3 is present in the electrorefiner salt. The uranium chloride in the cell electrolyte reacts with deposited plutonium and converts it to plutonium chloride. Plutonium, deposited in cadmium, is at much lower chemical activity so that it will coexist with uranium chloride in the electrolyte. Therefore, a liquid cadmium cathode is used to collect plutonium in the IFR electrorefining process. To reduce the size of the cathode, it is

necessary to collect plutonium in the liquid cadmium well in excess of its solubility, 3.62 wt % at 500°C. The precipitating phase in this case is the intermetallic compound, PuCd_6 . Uranium will also collect in the cadmium cathode, but when its solubility in cadmium is exceeded (at 500°C) the precipitate is metallic uranium. Uranium and plutonium form no known ternary compound with cadmium. The problem in the development of this liquid cadmium cathode has been in handling the solids: to settle them and compact them into the liquid cadmium and to reduce their growth into the electrolyte phase from the cadmium/salt interface. Some kind of agitation within the cathode crucible must be provided; otherwise, metal solids grow on top of the liquid cadmium pool within the crucible and overflow, causing electrical shorting. The experience with stirred cathodes is described in previous reports in this series.^{5,6}

Earlier work with stirred cathodes (using paddle or propeller type agitator blades) revealed two basic problems with this approach. First, collection efficiencies for the heavy metals (HM) were of the order of 40-60%. (Collection efficiency is defined as the amount in grams of HM collected in the cadmium ingot multiplied by 100, then divided by the amount of HM which should have been there based on the ampere-hours of current passed. One ampere-hour will deposit 2.9 g of HM at 100% current efficiency, and current efficiencies are expected to approach 100%.) Attaining collection efficiencies close to 100% is of some importance since this allows the use of time-integrated current to control and measure the amount of plutonium concentrated in the cathode. The second problem is related to phase separation of the salt layer which adheres to the top of the cadmium ingot. With cathode deposit growth into the electrolyte area, the salt layer on top of the metal ingot is very difficult to remove because the ingot top is very rough and uneven. In recent laboratory-scale work, a "pounder cathode" has been developed which shows promise of improving on the shortcomings of the stirred design.

The action of the pounder cathode is described with reference to Fig. III.1. The ceramic crucible containing the cadmium is located within the molten electrolyte bath of the electrorefining cell. A fixed metal rod conducts the current to the cadmium pool which interfaces with the electrolyte layer. A ceramic pounder essentially fills the crucible above the cadmium layer and is attached to and is driven by a metal drive tube, which is attached at its top

Fig. III.1. Cathode Pounder Design

(cold) end to the drive mechanism (not shown in Fig. III.1). A notch in the pounder provides better electrolyte access, and the drive tube moves the pounder in an up-and-down as well as a rotary motion. This action pumps salt in and out of the crucible and essentially compresses the cadmium surface on the down stroke. This causes the solids, which tend to grow upward into the electrolyte, to be pushed down and submerged in the cadmium. An overall view of the apparatus, while being assembled for cold mockup testing, is shown in Fig. III.2. (The ceramic pounder is visible because the cathode crucible is not in place.)

Ten runs have been completed with the cathode. Partial results for these runs are reported in Tables III.3 and III.4. (Chemical analyses are not yet complete.) Runs NE-1 and NE-2 were done with only plutonium present in the system; for the balance, uranium was also present in the electrorefiner anode and cell electrolyte. The first seven runs were done with a cell temperature of 497°C, and the last three runs were done at 425°C. The amount of cadmium in the cathode crucible varied slightly from run to run but was a nominal 200 g.

In Table III.3 it is seen that collection efficiencies are high. There is no systematic loss of HM from the cathode, as was experienced with the stirred

Fig. III.2. Cold Mockup Test of Powder Cathode

TABLE III.3. Experimental Results for Tests of Collection Efficiency with Pounder Cathode Design Cathode

Test Designation	Wt of HM Ingot, g	Predicted ^a HM Wt, g	Collection Efficiency, %
NE-1	17.83	20.79	85.8
NE-2	38.27	36.53	105.0
NE-3	16.42	20.79	79.7
NE-4	36.64	38.61	94.9
NE-5	34.53	35.64	96.9
NE-6	35.11	35.64	98.5
NE-7	-	39.46	-
NE-8	-	35.88	-
NE-9	23.61	25.04	94.3
NE-10	6.60	6.83	96.6

^aBased on the ampere-hours of current passed.

TABLE III.4. Heavy Metal Loadings Obtained in Four Tests of Pounder Design Cathode

Test Designation	HM in Ingot, g (excess of solubility)	Amount of PuCd ₆ , g	Vol % Insolubles	Wt % Insolubles
NE-1	17.83 (10.09)	38.54	13.8	16.4
NE-2	38.27 (30.70)	117.27	45.9	51.1
NE-3	15.53 Pu (8.32) 0.895 U	31.78	12.2	14.6
NE-4	34.88 Pu (27.24)	104.06	39.8	44.0

cathode, and collection efficiencies approach 100%. Table III.4 gives cathode loadings for four runs. Run NE-1 was the first run (made with Pu only in the system) and the total ampere-hours were limited. After successful completion of this run, the ampere-hours were increased to give the higher loading achieved in

NE-2. For Run NE-3, uranium was added to the system; again the ampere-hours were limited. In Run NE-4 this addition was repeated to give the higher loading. These runs show that cathodes with ≥ 40 vol % solids (at operating temperature, 500°C) can be obtained at high collection efficiencies.

Figures III.3 and III.4 show the metal ingots with the salt layers removed, revealing a surface which is relatively smooth. It was relatively easy to remove the salt layer, since growth into this salt layer had been well controlled. The imperfection in the center of the ingots is caused by the electrode, which had been removed prior to cooling and solidification of this ingot.

The laboratory-scale development of the pounder cathode for collection of plutonium in the electrorefiner has been highly successful. The device has overcome the shortcomings of the stirred cathode. Present work is directed toward exploring a number of cathode design variables in order to better define design parameters. This information is needed in order to scaleup the pounder cathode to engineering scale (3 kg of Pu).

2. Electrorefiner Drawdown Process Development

At some point in the pyrochemical processing of spent IFR fuel, the concentration of alkali, alkaline earth, and rare earth fission products in the electrorefiner salt must be reduced to lower the amount of heat generated therein. A heavy metal drawdown step (an operation to reduce the HM concentration in the salt) is required before removing the fission products from the salt. Results from drawdown tests in the engineering-scale electrorefiner are presented below.

The objectives of the drawdown tests were to determine the lowest concentration of uranium in the salt that could be achieved with low rare earth concentration in the cathode product, the lowest concentration of uranium in the salt (with no regard to the composition of the cathode product), and the lowest concentration of rare earths (cerium, neodymium, and yttrium) in the salt that can be achieved with electrotransport. Possible drawdown reactions that occur with a lithium-cadmium alloy (5.8 wt % lithium) loaded in the anodic dissolution

Fig. III.3. Product Ingots (ANL Neg. No. 13410K)

Product of NE-1

Product of NE-2

Product of NE-3

Product of NE-4

Fig. III.4. Product Ingots (ANL Neg. No. 13411K)

baskets and a solid or liquid cadmium cathode include the following: (1) electrotransport of lithium to the cathode and lithium reduction of uranium and rare earths at the cathode, and (2) lithium reduction of uranium and rare earths at the anode and electrotransport of uranium and rare earths to the cathode. Design requirements of the drawdown equipment include provisions to control the rate of reduction by lithium, to produce good separation between the uranium and rare earths, and to collect the uranium and rare earth products over a wide range of salt compositions.

At high (1.75-6.68 wt %) uranium concentrations in the salt during the drawdown tests, the cathode deposits contained 70 to 90 wt% uranium. At low (0.015 wt %) uranium concentration in the salt, the cathode deposit contained less than 0.1 wt % uranium. The compositions of two cathode deposits collected during the drawdown tests are given in Table III.5. The effect of salt concentration on the concentration of uranium and rare earths in the cathode deposits collected during the drawdown tests is shown in Fig. III.5.

The following observations were made during the drawdown tests:

1. The uranium concentration in the salt was reduced to less than 0.01 wt %.
2. Only 13% of the rare earths were removed from the salt, while 99.9% of the uranium was removed.
3. The rare earth concentration in the salt was reduced to less than 0.01 wt %.

The following conclusions were drawn from these tests:

1. Good separation can be achieved between uranium and rare earths in the salt.
2. Uranium and rare earth concentrations in the salt can be reduced to low levels by electrotransport.
3. Drawdown tests are needed to determine the separation between plutonium and rare earths in the salt.

Future efforts will be directed to the development of improved electrodes with higher drawdown rates.

TABLE III.5. Composition of Deposits from Drawdown Tests

Component	Cathode Deposit, ^a wt %	
	High Uranium Content	High Rare Earth Content
Uranium	70.0	0.6
Salt	30.2	79.6
Cerium	0.6	17.7
Neodymium	0.5	9.8
Yttrium	^b	4.3

^aEstimated uncertainty in the chemical analysis is ± 5 to 10%.

^bThe yttrium chloride in the deposit accounted for all of the yttrium detected in the samples.

Fig. III.5. Effect of Salt Concentration on Concentration of Uranium in Product

3. Recovery of Zirconium by Electrotransport

Electrotransport of zirconium to a solid or liquid cadmium cathode is one of the options being investigated to remove zirconium from the electro-refiner. With repeated electrotransport cycles, zirconium, a major constituent

of the processed spent fuel, accumulates in the electrorefiner. Results from testing in earlier laboratory-scale and engineering-scale electrorefiners indicated the following:

1. Zirconium adheres to the wall of the electrorefiner vessel and surfaces of electrodes and mixers in the electrorefiner.
2. Zirconium in excess of solubility in the cadmium pool forms Cd_2Zr .
3. The composition of material deposited on corrosion samples in the engineering-scale electrorefiner was 9.2 wt % salt, 74.5 wt % cadmium, 3.2 wt % uranium, and 13.1 wt % zirconium.

In this report period, zirconium electrotransport tests were conducted in the engineering-scale electrorefiner with the following objectives:

1. Collect zirconium transport data at 0.5 wt % zirconium (highest achievable concentration) in the salt and at less than 0.01 wt % zirconium in the salt.
2. Obtain rate data for the electrotransport of zirconium to a solid mandrel cathode with a catch crucible to recover material that does not adhere to the mandrel and to a liquid cadmium cathode.
3. Determine the concentration and form of zirconium in cathode deposits that are collected under various operating conditions.

The compositions of two of the cathode deposits collected during the zirconium electrotransport tests are given in Table III.6. The highest zirconium concentration (33.0 wt %, 13 vol %) in the deposits was achieved in tests with low (<0.01 wt %) zirconium concentration in the salt. The zirconium concentration was 11.3 wt % in the deposit collected with 0.5 wt % zirconium in the salt. The uranium concentrations in these deposits were 1.0 and 0.58 wt %, respectively.

The cathode deposit with 33.0 wt % zirconium (13 vol %) indicates a promising method that should be pursued for removing zirconium deposits with high solids content from the Fuel Cycle Facility electrorefiner. Future efforts will be directed to the development of improved electrodes for zirconium recovery by electrotransport.

TABLE III.6. Composition of Zirconium Deposits

Component	Conc., ^a wt %	
	Intermediate Drawdown Step (<0.01 wt % Zr)	Transport after Drawdown Operation (0.5 wt % Zr)
Zirconium	33.0	11.3
Salt	57.3	80.6
Uranium	1.0	0.58
Cadmium	<0.01	0.40

^aConcentration of zirconium in salt.

C. Waste Treatment Process Development

Processes are being developed to recover TRU elements from the spent salt and metal discharged from the electrolyzer and to separate the fission products so that the treated salt and metal can be recycled. The current work is guided by a reference flowsheet that is a modification of the flowsheet shown in the previous annual report. Similar to the previous flowsheet, the salt from the electrolyzer is first contacted with a molten Cd-U alloy to extract TRU elements and leave rare earths in the salt. The product metal solution is retorted to remove cadmium, and the TRU-rich residue is returned to the electrolyzer. Salt from the extractor is contacted with a Cd-Li alloy to remove uranium, most of the rare earths, and residual traces of TRU elements. The salt produced by this stripping step should have a total actinide content below 0.1 ppm and an alpha activity of <10 nCi/g (370 Bq/g). In the new flowsheet, the stripped salt is passed through a column of zeolites to sorb a fraction of the remaining fission products so that about 90% of the salt can be returned to the electrolyzer. The salt-zeolite mixture is immobilized in an aluminosilicate matrix and packaged for ultimate disposal in a geologic repository.

In another flowsheet modification, cadmium from the salt stripping step, which contains rare earths and small amounts of actinides, is contacted with an

Al-Cu alloy to extract rare earths and actinides from the cadmium. The Al-Cu mixture is combined with cadmium from the electrorefiner and the cladding hulls, which contain the noble metal fission products, and the mixture is then retorted to remove cadmium, which is returned to the process. The retort residue is melted to produce the metal waste form.

Engineering-scale equipment for salt extracting and stripping tests have been assembled and are nearly ready for operation. In the reference process, salt extraction will be carried out in a train of centrifugal contactors similar to those used for aqueous-organic systems. A single-stage pyrocontactor and much of the supporting apparatus have been fabricated and assembled in a glovebox.

A salt stripping system has also been fabricated and is being installed in a heated well in the glovebox with the engineering-scale electrorefiner. The system consists of a stirred vessel for stripping tests, a pump and transfer line for transferring salt and cadmium between this vessel and the electrorefiner, devices for adding lithium to the stripping alloy at controlled rates, a pumped filter assembly, and various test instruments.

Work on metal waste treatment was focused on the use of a molten Al-Cu alloy to remove rare earth fission products from cadmium and immobilize them in the final waste form. This technique has the potential for greatly reducing the amount of cadmium that must be distilled. The liquid Al-Cu alloy also provides a means for dispersing fission product heat during distillation.

1. Salt Extraction

The first step in the reference process for treating spent electrorefiner salt is a salt/metal extraction to preferentially recover TRU elements. The equipment being assembled for tests of molten salt-metal extraction consists of (1) a single-stage pyrocontactor with a 4-cm diameter rotor, (2) tanks to feed salt and metal to the contactor, (3) tanks to receive salt and metal from the contactor, (4) tanks to clean and treat salt and metal, (5) heated transfer lines, and (6) instruments, controls, data acquisition system, and a glovebox with an argon purification system.

The pyrocontactor was assembled, and its furnace was installed on a support plate in a glovebox. Preliminary tests with water revealed no vibrations at rotor speeds up to 2600 rpm, and demonstrated that the fluid flow rate can be adequately controlled by means of the fluid head in the feed tanks, and an orifice and a valve in the line to the contactor. The salt and metal feed tanks were fabricated and installed. Using water, it was shown that load cells supporting these tanks can measure the amount of fluid in the tanks and the feed rates to the contactor. With the installation of thermocouples, heaters, and insulation, the raffinate and treatment tanks were completed. Two cranes were installed in the glovebox and certified. Windows and gloves were installed on one side of the glovebox in order to identify needs for specialized tools and to establish operating procedures.

The major work remaining before glovebox closure is (1) the installation of the heated transfer lines; (2) pretests of the complete system, including flow tests with water, and tests of the instrument and control systems; and (3) installation of electrical power to the glovebox, as well as its temperature and pressure control systems. Closure and evacuation of the glovebox are expected to be completed in March 1993, with the initial salt and cadmium flow tests beginning in April.

In the first planned series of extraction tests, CeCl_3 dissolved in LiCl-KCl eutectic will be transferred into Cd-Li solutions, or cerium dissolved in cadmium will be transferred into salt containing CdCl_2 . Stage efficiencies, entrainment, and flooding characteristics will be determined from these experimental results. In the next test series, the actinide-rare earth separation in the waste treatment process will be demonstrated using rare earths as stand-ins. Lanthanum (the stand-in for TRU elements) will be preferentially extracted from salt containing LaCl_3 and YCl_3 (the stand-in for rare earth chlorides) using cerium (the stand-in for uranium) dissolved in cadmium. These extractions constitute a valid demonstration of the IFR extraction process because of the similarity of the separation factors among cerium, lanthanum, and yttrium with the factors among uranium, TRU elements, and rare earths, as shown in Table III.7.

TABLE III.7. Comparison of Separation Factors (SF) for Electrorefining Process and Salt Extraction Process

IFR Process		Pyrocontactor Tests	
Element	SF ^a	Element	SF ^b
U (basis)	1.0	Ce (basis)	1.0
Average TRU	~2	La	2.0
Average Rare Earth	60-100	Y	88

^aDistribution coefficient of element/distribution coefficient of U.

^bDistribution coefficient of element/distribution coefficient of Ce.

2. Salt Stripping

One of the steps in the reference waste treatment process is salt stripping, in which salt from the extraction step is contacted with a Cd-Li alloy. The lithium reduces essentially all of the uranium and residual TRU elements from the salt, most of the rare earths, but none of the alkali metals and alkaline earths. A prototype salt stripper system has been fabricated and is being installed adjacent to the engineering-scale electrorefiner. A major purpose of the tests is to study the rate and extent of uranium and rare earth transfer from the salt to the Cd-Li alloys. In addition, the performance of components such as liquid-level probes, reference electrodes, devices for introducing Li-Cd alloy, and filtration apparatus will also be determined.

Prior to the installation of the stripper system into the engineering-scale glovebox, equipment handling and pretesting runs were performed in a glovebox mockup. The performance of the salt/cadmium pump was determined using water over a range of impeller speeds from 800 to 1800 rpm. Fluid heads developed at the pump discharge at low flow ranged from 0.8 to 7.3 m. At 1150 rpm, the low-flow head was close to that predicted by the manufacturer of a similar pump, but the measured head dropped off more rapidly with increased flow rate. Nevertheless, the pump performance at about 1200 rpm meets the requirements (2.3 m of head and about 5 L/min) for pumping salt or cadmium between the electrorefiner and stripper vessel.

Several tests demonstrated operation of the handling of the transfer line between the pump outlet and the receiver vessel, which may be either the stripper or electrorefiner. These tests also demonstrated successful mating of the freeze seals that are used to join the ends of the transfer line to the pump discharge or receiver vessel. The seal consists of an inner metal-to-metal seal that joins the flow tubes and an outer solder seal filled with a silver alloy (melting at 627°C) and a flux. Fluid leaking through the metal-to-metal seals flows by gravity back into the vessel through an annulus between the two seals. It was necessary to heat the solder seals to at least 700°C in order to seat them smoothly. At a head of 4.5 m of water, only a drop-wise leakage through the metal-to-metal seal was observed. At an argon gas pressure of 0.68 MPa(g) (10 psig), there was no detectable gas leakage past the solder seal at 500°C or room temperature. With the addition of a positive clamp to exert force on the metal-to-metal seal and an engagement gauge, the transfer line assembly was ready for installation in the glovebox.

Preparations were made for evaluating several types of devices to measure the molten salt and metal levels in the stripper vessel. These include single and multicontact conductance probes, a pressure probe, and an inductance probe. Fluid levels are determined by the conductance probe by the abrupt change in electrical conductance when the fluid level contacts a fixed conducting rod. The pressure probe measures the rise in gas pressure in a closed volume as the probe lower end is immersed in the fluids. The induction probe measures the salt level by changes in the inductance as a hollow metal ring floating on the salt rises around the inductance coils. In addition, the total weight of salt and metal in the stripper vessel will be measured by load cells that support the vessel flange.

Testing of the equipment in the glovebox mockup has been completed, and installation in the glovebox was started.

3. Metal Waste Treatment

In the reference flowsheet, the cadmium-rare earth stream from the salt stripping operation is treated to separate the cadmium for return to the stripping process and to immobilize the rare earths and residual actinides into

a waste form. About 4000 kg of cadmium must be treated per 1000 kg of actinides fed to the electrorefiner. During this processing, the fission products must be kept dispersed to facilitate heat removal. In an earlier flowsheet concept, cadmium was removed by transpiration distillation rather than vacuum retorting to avoid entrainment, with copper being added to aid in heat removal and to form a waste matrix. However, distilling cadmium from copper-rich alloys appeared to be too slow for IFR waste processing.

In a new process concept, most of the cadmium is separated from rare earths by extracting them into an Al-33 wt % Cu alloy, which is a eutectic with a melting point of 548°C. With this technique, less than 200 kg Cd must be distilled per 1000 kg actinides processed.

The rare earths are extracted almost completely into the aluminum alloy, where they precipitate as ternary intermetallic compounds. The aluminum phase contains less than 2 wt % dissolved Cd. The immiscible cadmium phase contains negligible amounts of rare earths, about 0.5 wt % Al, and 0.2 wt % Cu. The aluminum must be removed from the cadmium before it is returned to the stripping step.

Experiments were undertaken to identify the rare earth compounds precipitating from Al-Cu alloys. With cerium added to a two-phase liquid, Al-33 wt % Cu/Cd melt, a precipitate formed in the aluminum phase with an approximate composition of CeCu_4Al_8 . With high cerium loadings, this precipitate wet with Al-Cu settled to the bottom below the cadmium phase. The overall cerium contents of the aluminum phase ranged from 0.8 to 1.9 wt %, but in all cases they were less than 10 ppm in the cadmium. When lanthanum was added to Al-31 wt % Cu-5% Zr solution, a precipitate formed and was tentatively identified by X-ray diffraction as $\text{LaCu}_2\text{Al}_{10}$, although the X-ray diffraction pattern of this material has not been reported.

The cadmium vaporization rate was measured by passing a stream of argon over a two-phase Al-33 wt % Cu/Cd melt. The melt temperature was estimated from a thermocouple above the melt that read about 10°C higher than the melt in this temperature range. The vaporization data are summarized in Table III.8. In Run 1010, the weight loss appeared to be slightly greater than the cadmium

TABLE III.8. Cadmium Transpiration from a Two-phase Al-33 wt % Cu/Cd Melt (initial Al-Cu, ~27 g; initial Cd, ~40 g; melt surface area, ~5 cm²)

Run	Estimated Melt Temperature, °C	Argon Flow Rate, mL/min	Time at Temperature, h	Weight Loss, %Cd	Cd Vapor Flux, g/(h•cm ²)
159	731	350	4.0	3.9	0.076
1012	753	160	5.0	34.7	0.53
1010	757	250	18.0	101.6	0.46

added, but this was attributed to experimental error. For all three runs, the vaporization rates from this two-phase liquid system were about 4 to 5 times less than the rates from pure cadmium under comparable conditions, but were several times higher than rates from copper-rich cadmium alloys. The rates from the two-phase liquid metal are in a range such that the IFR retort would be practical.

D. Waste Management

1. Salt Waste Immobilization

Salt-occluded zeolite is being investigated as a material for treating the IFR waste salt. It was prepared by passing simulated IFR process salt through a zeolite column. The column was divided into four sections. Each section was broken into small chunks that were quickly washed to remove the salt adhering to the surface. The powders were then dried at 110°C (383 K) until constant weight was obtained. Powders from the second quarter from the top were analyzed using inductively coupled plasma atomic emission spectroscopy (ICP-AES) to determine the concentrations of Al (12.8 wt %), Ba (4.97 wt %), K (6.0 wt %), Li (4.15 wt %), Na (0.77 wt %), and Sr (0.85 wt %). Atomic emission spectroscopy was used for determination of Cs (6.7 wt %); Cl⁻ (16.0 wt %) and I⁻ (0.08 wt %) were measured by ion chromatography. The large chloride ion concentration indicates that a significant amount of salt was occluded, and the large concentrations of barium, cesium, and strontium indicate that ion exchange occurred. The sample preparation and salt occlusion and ion exchange properties of zeolite A have been published.⁷

Salt-occluded zeolite shows promise for waste immobilization, but the powdered material has to be consolidated into a monolithic form before disposal in a geologic repository. Preliminary hot-pressing experiments were conducted to densify salt-occluded zeolite powders that had been washed and dried. Pressing temperature is the most significant variable. Except for baseline elevation in the X-ray diffraction (XRD) spectra for pellets pressed at temperatures greater than 725°C (1000 K), the spectra of all pellets were the same as those of the powders. At temperatures greater than 725°C (1000 K), densification occurred, but some mass was lost during pressing. The mass loss is believed to be caused by evaporation of salt. Baseline elevation in the XRD spectra was attributed to the formation of a glassy phase that probably formed as the salt evaporated. Pellets formed at temperatures between 615 and 685°C (888 K and 958 K) were very chalky.

Pellets from both high- and low-temperature pressings were leach tested at 90°C (363 K) for seven days in deionized water. Pellets with the glassy phase lost less than 1 wt % of their mass, whereas pellets formed below 650°C (958 K) lost up to 11 wt %. One possibility is that the glassy phase improves the leach resistance properties of the salt-occluded zeolite powders, by providing a second phase between particles and thus limiting access to the particle surfaces.

2. Waste Form Qualification

Previously reported work has shown that, after washing and drying, salt-occluded zeolite powders have good leach resistance in room-temperature leach tests and are radiation stable to 10^9 rad.⁸ Because leach resistance is such an important property, it was investigated further. The PCT test method^{9,10} was selected (even though it is not a leach test per se) because its use in the particle size range of interest is well established. The time dependence of the elemental releases provided preliminary information on leaching mechanisms from the salt-occluded zeolite powders. The data at seven days were compared to data on borosilicate glass taken under similar, but not identical, conditions.

The PCT tests for obtaining preliminary information on elemental release mechanisms from salt-occluded zeolite A were run with deionized water at

90°C (363 K) for 3, 7, 14, 28, and 56 days. The leachates were analyzed for cation and anion content. The amount of each element, i , that moves from the zeolite to the water is expressed by the normalized mass loss, NL , defined as:

$$NL = C_i V / f_i A \quad (3)$$

where C_i is the concentration of the element i in the leachate in g/ml, V is the volume of leachate in mL, f_i is the fraction of the element i in the solid initially, and A is the surface area of the solid in m^2 . The specific surface area of the powders was calculated to be $0.75 m^2/g$ by assuming cubic particles, an average particle size of $3.8 \mu m$, and a density of $2.1 g/cm^3$.

The normalized elemental mass loss is plotted versus time in Figs. III.6 and III.7. The average normalized mass loss for aluminum was small ($0.036 g/m^2$) and constant within the 10% experimental error. The average normalized mass loss for silicon was also small ($0.028 g/m^2$) and constant within experimental error. Thus, there appears to have been little or no hydrolysis of the zeolite framework.

The normalized mass losses for strontium and barium could not be calculated because strontium and barium concentrations were below the ICP-AES detection limits of 5 and 20 ppb in all the leachates. These detection limits correspond to normalized mass losses of 5.4×10^{-5} and $1.4 \times 10^{-5} g/m^2$. Thus, strontium and barium are, for all practical purposes, not leached. These ions may occupy more stable sites in the zeolite than the alkali metals. There is some evidence that multivalent cations may replace the sodium ions in the ion exchange sites, i.e., they may become charge-compensating cations.¹¹

The normalized mass loss for cesium was considerably lower than that of the other alkali metals. It varied from $0.030 g/m^2$ for 3 days to $0.056 g/m^2$ for 56 days. This may result from the lower hydration energy ($263 kJ/mol$),¹² from occupation of a more stable site, or from steric effects. The normalized mass losses for the other alkali metals increased in the order $Na < K < Li$; for the 56 day tests, these values were 0.22, 0.25 and $0.90 g/m^2$, respectively. The concentrations of lithium and chloride ions are approximately equal and approach

Fig. III.6. Normalized Mass Losses (NL) in Cations for Salt-occluded Zeolite A vs Time in 90°C Leach Tests with Deionized Water

Fig. III.7. Normalized Mass Losses for Anion in Salt-occluded Zeolite A vs Time in 90°C Leach Tests with Deionized Water

0.02 M. This concentration represents about 65 wt % of the total lithium, which is high, but comparable to the amount of sodium lost from sphene glass-ceramic phases leached in Ca-Na-Cl brines.¹¹

Leach test data can also be expressed in leachability units, L , defined as

$$L = \frac{C_i V}{f_i A d} \quad (4)$$

where C_i , V , f_i , and A have the same meaning as above, and d represents the duration of the test in days. Leach data from the 7-day test are given in Table III.9 in these units. Also given are the leachabilities of the various components of SRL-202-6 glass.¹⁰ Both the salt-occluded zeolite and the SRL-202-G glass were leached under similar conditions. One difference was in the surface area of the powders, which was $0.018 \text{ m}^2/\text{g}$ for glass and $0.75 \text{ m}^2/\text{g}$ for the zeolite. As shown in Table III.9, the leachabilities of the various components of the salt-occluded zeolite are lower than those of the borosilicate glass.

TABLE III.9. Leachabilities from Salt-occluded Zeolite and Borosilicate Glass^a

Component	Borosilicate Glass SRL-202-G g/(m ² •d)	Salt-occluded Zeolite g/(m ² •d)
Al	0.14	0.0048
B	0.29	ND ^b
Ba	ND ^b	BDL ^c
Cs	ND ^b	0.0066
K	0.22	0.031
Li	0.29	0.12
Na	0.28	0.018
Sr	ND ^b	BDL ^c
Si	0.23	0.0041

^aTest conditions: 90°C, 7 days, $V/m = 100 \text{ mL/g}$, deionized water.

^bND = Not determined.

^cBDL = Below detection limit in leachate.

The data from the leach tests for the salt-occluded zeolite and the SRL-202-G borosilicate glass can also be expressed as a normalized release unit, C_i/f_i . The normalized releases for aluminum and silicon from the salt-occluded zeolite were 0.25 and 0.22 g/L, respectively. These are comparable to the releases for aluminum, boron, and silicon from borosilicate glass at 0.18, 0.37, and 0.28 g/L, respectively. The release of the alkali metal cations was somewhat greater from the occluded zeolite than the borosilicate glass. The largest difference was seen for lithium, the smallest ion. The two methods for expressing the leach test results represent two extremes, because the former assumes the surface area differences are linear, whereas the latter completely neglects surface area effects. The comparison is strictly valid only if the leaching mechanisms for the two types of materials are similar.

In bulk glasses, the void size is about 2.2 to 2.4 Å. The aperture size of the sodium form of zeolite A, the starting material, is about 4 Å. However, in salt-occluded zeolite A, the sodium was replaced by other ions and 10-12 salt molecules filled the cavities. The size of the aperture and the effect of the occluded salt on water transport are not known. The losses of the relatively large amounts of lithium and chloride ions suggest that water was able to penetrate the cavities, and that the hydration energy of the occluded ions exceeded their binding energy to the aluminosilicate matrix framework and to each other. The large initial release is attributable to the diffusion of the occluded salt from the zeolite cavities and dissolution.

The hydration energies of lithium, sodium, and potassium are 519, 406, and 322 kJ mol⁻¹.¹³ The normalized mass losses for lithium and potassium in the salt-occluded zeolite follow the same order as the hydration energies, but sodium does not. One possible explanation for this finding is that sodium occupies a more stable site than lithium and potassium. The starting material in these experiments was the sodium form of zeolite A, and a small amount of sodium (<1%) was not exchanged when the zeolite was treated with the molten salt. This "unexchangeable" sodium may have greater binding energy and, hence, greater leach resistance than the lithium and potassium.

Another possible loss mechanism is ion exchange. However, ion exchange between lithium and potassium in the solution and cesium, strontium, and

barium in the zeolite is minimal. Ion exchange between the alkali metal ions and hydrogen ion, or cation hydrolysis, does not occur to an appreciable extent either. The pH of the leachates was measured and, though there was some scatter, the overall effect was that the pH decreased with time, rather than increasing, as it would if metal cations were exchanging with hydronium ion. The pH was 10.8 in the 7-day test, and 10.3 in the 56-day test. These results are under study.

As mentioned above, the normalized mass loss for aluminum and silicon was the same within experimental error in all the leach tests. Thus, the dissolution of these elements was solubility limited. Further evidence for this conclusion was that the normalized losses for these elements decreased at least tenfold when the V/m ratio was reduced from 100 to 10 mL/g. The normalized losses for the alkali metals, however, were about the same in the two types of leach tests.

The normalized mass loss for chloride and iodide for the 28-day test (Fig. III.7) was 1.1 and 0.25 g/m², respectively. Chloride loss may be larger than iodide loss because of a steric effect; chloride's ionic diameter is 3.62 Å and iodide's is 4.40 Å.

In studies of alkali leaching from glass, two time-dependent functions for normalized mass loss have been observed, viz, $NL \propto t^{0.5}$ and $NL \propto t^1$. In one model, the distinction between the two types of kinetics is based on the size of the micropores in the structure. In glass, the initial pore size is about 2.2–2.4 Å. Molecular water does not readily penetrate through the pores, as its diameter is 2.8 Å. Water diffusion is slow, and the normalized mass losses follow $t^{0.5}$ kinetics. Eventually, the surface of the glass reacts and the pore size increases to about 10 Å. Water readily penetrates this material, then the mass loss rate becomes linear.^{14,15} If the mechanisms for alkali leaching from salt-occluded zeolite and glasses are similar and if the microporous structures are also similar, one or both of these dependences should be observed. However, the normalized elemental mass loss data in Figs. III.6 and III.7 do not fit linear curves. There is a large initial release, followed by a slow release of both cations and anions. The release of the alkali metals (Li, K and Cs) does not fit the model for alkali leaching from glass, indicating that the microstructures and rate of water transport through it are different. Regression analysis of

these data versus $t^{0.5}$ showed that only sodium fit this dependence. Since there was no evidence of network hydrolysis (the concentrations of aluminum and silicon were essentially constant in all the leachates), the $t^{0.5}$ dependence is coincidental.

3. Nonzeolite Mineral Waste Forms

Consolidated sodalite, formed from salt-loaded zeolite, is a major alternative to the consolidated zeolite waste form. Initial studies of sodalite formation were encouraging. Heating mixtures of NaOH, Al_2O_3 , SiO_2 , and salt produced sodalite in 5 to 75% yields. Subsequently, a review of the literature on sodalite was completed. A synthetic method was sought that would consistently provide high rates, complete reaction, and a pure product. Sodalite syntheses have involved (1) aqueous media, (2) solid-state, high-temperature reactions, (3) organic media, and (4) molten-salt media. Since aqueous and organic media would add comp¹ cation and expense to IFR processing, little attention was given to information in these areas. A common reaction mixture includes salt, SiO_2 , Al_2O_3 , and a source of sodium oxide, such as NaOH, Na_2CO_3 , or $NaAlO_2$. In some cases, the source of all components except the salt is supplied by a synthetic zeolite or by a mineral such as kaolin or bentonite. Chang¹⁶ noted that many methods of sodalite synthesis are fraught with difficulties, including low yield, and recommended preparation from zeolite. Experimental work on the direct conversion of zeolites to sodalite has begun with studies on thermal conversion of zeolite A.

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IV. SAFETY EXPERIMENTS AND ANALYSES

A. TREAT Experiments

Planning and Preparations for Test M8

In-reactor transient fuel testing in TREAT was performed in the 1980s to investigate the response of IFR fuel to overpower anticipated transient without scram (ATWS) conditions. Six tests, designated M2 through M7, were performed in flowing-sodium loops. They have been described previously.¹ In those tests, two or three fuel pins, each in its own flowtube, were heated with exponentially-rising overpower transients. The experiments demonstrated the failure resistance, prefailure fuel expansion, failure mechanism and mode, and initial postfailure fuel motion of fuel for several combinations of burnup, fuel composition, and cladding type. The results are summarized in Table IV.4 of Ref. 1. The 8-s-period power rise employed in all of those tests resulted in a duration at high cladding temperature that was too short to allow much creep of the cladding, even with fuel pins of high burnup (high internal pin pressure). Consequently, cladding failures did not occur until the fuel-cladding interface temperature reached approximately 1080°C, at which the cladding penetration rate by fuel-cladding chemical interaction and liquefaction increases very sharply. Irradiated ternary fuel in HT9 cladding (the reference IFR fuel design) was not yet available from EBR-II for those tests. Thus, HT9-clad U-10Zr fuel and D9-clad U-19Pu-10Zr fuel were used in Tests M5, M6, and M7, and older-design, EBR-II Mk-II fuel was used in Tests M2, M3, and M4.

The next experiment in the series, Test M8, is planned to be performed in 1994. This test has the dual objectives of providing support to the safety case for irradiation of Mk-V fuel (HT9-clad U-20Pu-10Zr fuel with ~1.5 plenum/fuel volume ratio) to high burnup in EBR-II and investigating cladding failure characteristics (margins, mechanisms, modes) under conditions in which cladding creep is expected to play a more significant role. To accomplish these objectives, the fuel pins to be tested will have high plenum pressure (corresponding to that in a Mk-V fuel pin at 15 at.% burnup), and the power transient will be slower than in the previous M-series tests. The slower transient will require greater TREAT energy generation, which will be possible

by using a different (half-slotted) TREAT core configuration (see below). The power transient in M8 is planned to have a relatively-fast rise to ~30 kW/ft in the test fuel (just short of the onset of fuel melting), followed by an exponential rise with ~30 s period. Unlike in Tests M2 through M7, cladding failure in M8 is expected to occur well before the onset of rapid cladding penetration by melt formation.

As in Tests M5 through M7, two pins will be tested in individual sodium flow tubes. In M8, the two pins will be nominally identical. Because two Mk-V fuel pins of 15 at.% burnup will not be available, however, similar fuel pins of approximately the same plenum pressure (HT9-clad U-19Pu-10Zr fuel with 1.1 plenum/fuel volume ratio irradiated to 12 at.% burnup) will be used. One pin will be intentionally overcooled by a few percent so that its failure will lag that of the other pin. As in the previous tests, the TREAT power will be suddenly reduced at the instant that fuel pin failure occurs. Failure will be indicated by a sudden reduction in the sodium flowrate in the corresponding flowtube. The overcooled pin, with higher initial flowrate, will be expected to remain intact but almost reach the point of failure. Posttest metallurgical examination of both the failed and unfailed pins will be performed. As demonstrated in the previous tests, this approach yields significant information regarding cladding failure and the immediate postfailure in-pin and ex-pin fuel motions. The fuel motions will be determined by the TREAT Fast Neutron Hodoscope and by the final fuel and cladding disposition determined from the posttest examinations.

Because of the revised TREAT core configuration and new reactor control system, neutronic calibration measurements (M8-CAL) were performed in TREAT to determine the power coupling between the TREAT core and the test fuel. To provide a viewing path into the core so that fast neutrons generated in the test fuel could reach the collimator of the Fast Neutron Hodoscope, for Tests M2 through M7 a row of TREAT fuel elements was removed on both the hodoscope side and the opposite side of the test vehicle in the core. This full lateral open slot in the core reduced the amount of TREAT fuel that was in the hodoscope's view and which, therefore, contributed strongly to the hodoscope's background signal. The removal of core fuel, however, reduces the excess reactivity available and thereby reduces the amount of energy that can be generated by TREAT

during a power transient, since TREAT has no rapid heat-rejection capability and a stored energy increase leads to a loss of reactivity. For the longer, more-energetic transient planned for M8, additional core energy is required, and all fuel elements on the opposite side of the test vehicle, farthest from the hodoscope, will be needed--except that a dummy graphite element will be placed immediately behind the test vehicle to shield some of the core neutrons from the hodoscope view. The M8-CAL irradiations provided data for determining how much test fuel energy can be generated in the half-slotted core relative to that in the full-slotted core. The irradiations also permitted a determination of the effect of the core change on the hodoscope's signal-to-background ratio. Calibration results indicate that a peak-axial energy generation of 1800 kJ/m in the test fuel is achievable in the half-slotted core.

One of the most valuable aspects of the M-series experiments is the close relationships among the tests. This permits effects of individual, key parameters to be discerned without unnecessary ambiguity. Following this principle, Test M8 will be similar to Tests M2 through M7 except for the more-slowly-rising power and a different combination of fuel/cladding characteristics. Test M9 is currently scheduled to be performed in late 1994.

B. Determination of Irradiated-fuel Thermal Conductivities from TREAT Test Results

Assessing the thermal conductivity of modern high-swelling sodium-bonded metallic fuel is neither theoretically straightforward nor easily amenable to laboratory measurements. Therefore, fuel thermal conductivity was estimated *in-situ* from posttest measurements of the melting isotherms that occurred in unbreached metal fuel pins from TREAT Tests M5 through M7. Of the six irradiated IFR prototype fuel pins subjected to severe overpower and melting, four remained intact. Three of the pins were of ternary alloy (U-19Pu-10Zr) and one of binary (U-10Zr) alloy. All were subjected to extensive posttest examination. Importantly, these test pins span a key range of low burnup where development of gas-filled porosity could lead to minimal values of conductivity.

In-pin power generation and calculated fuel surface temperatures were validated by agreement between measured and calculated coolant temperature rises during the tests. The measured radial distance of the melt (or solidus) front inward from the fuel surface, in conjunction with a known fuel surface temperature and known radially-dependent pin power generation, then allowed the temperature-averaged fuel thermal conductivity across the annulus of unmelted fuel to be directly calculated. Uncertainties were derived by estimating sources of error in the computation. Some errors are randomly distributed from measurement to measurement. Other errors, such as pin power and melting temperature, are inherently correlated for measurements within a single test pin or for all measurements made for each fuel alloy. The most precise determinations are at lower axial locations where the fuel barely reached the melting threshold. To account for circumferential nonuniformity, averaged values of melt radii were determined from the measured cross-sectional area delineating the maximum extent of melting.

Results may be simply summarized by assuming that a single averaged correction, X , to the conductivity of unirradiated alloy applies to each fuel type and peak burnup. These results are: $\langle X \rangle = 0.74 \pm 0.11$, 0.73 ± 0.11 , and 0.73 ± 0.05 for 0.8 at.% burnup ternary, 1.9 at.% burnup ternary, and 2.9 at.% burnup binary, respectively. In the burnup range spanned by ternary fuel, there is some evidence of a systematic decline of the conductivity correction with burnup from $X = 1$ at zero burnup to $X = 0.5$ at about 1.7 at.% burnup, followed by a sudden jump to $X = 0.8$ as burnup increases above that level.

The measured corrections indicate modest reductions of conductivity with fuel swelling. The observed magnitudes and trends strongly suggest that infiltration by bond sodium will likely mitigate the reduction caused by swelling.

C. Severe Accident Phenomenology

Numerous analyses and tests have shown that all classes of slowly initiated transients in metal-fueled, pool-type fast reactors would be self-terminated, without scram and without damage to the core or its environs. "Slowly initiated" may be taken to mean that reactivity insertion rates, flow rundown rates, and

rates of change of inlet temperature are slow compared to the thermal time constants governing the rate of reactivity feedback from the temperature coefficients. More specifically this means reactivity insertion rates limited to less than several cents per second and primary pump coastdown rates with halving times greater than the order of 10 s. In pool-configuration reactors the primary system heat capacity is so large that the condition of slowness on inlet temperature automatically would be met for any combination of faults in the balance of plant, however rapidly they might develop.

Core damage therefore would be anticipated only for transients initiated rapidly and within the primary system. In such a case we consider the consequences of a sudden (i.e., extremely abrupt having no coastdown) loss of primary flow coupled with failure to scram*--perhaps initiated by an unusually large earthquake. Core damage would appear to be inevitable in this type of accident--cladding failure and fuel dispersal in fact would be the expected mode of shutdown. A principal question therefore is the severity of the transient, particularly any potential for doing mechanical damage to the reactor vessel or its appurtenances. Transient severity would be determined by the interplay between the major feedback mechanisms, namely: 1) the positive component due to the spread (radially and axially) of coolant voiding, the negative components of 2) Doppler broadening, and 3) gas-driven, axial expansion of fuel within intact cladding, and finally 4) the character of the postfailure fuel motion whose amplitude dominates in the final analysis. The latter two mechanisms were the subject of much of the analysis.

The principal conclusion is that cores with total coolant void worth $\lesssim 10\%$ would experience low energy transients incapable of damaging the reactor vessel. This is found to be a consequence largely of the large negative feedback associated with prefailure expansion of the reactor fuel.

*If scram were accomplished (along with system integrity sufficient for natural circulation decay heat removal), core damage again would not be anticipated because the energy stored in the fuel pin temperature increment above the coolant does not exceed the heat of vaporization of the static coolant occupying the coolant subchannel.

In a sudden loss of flow, coolant voiding eventually would drive the reactor to high power (a few 10s x normal assuming a total void worth of a few dollars). Doppler feedback then would cause a temporary power plateau until within-intact-cladding fuel melting first reached the outlet end of the core. At this time, gas-driven molten fuel axial expansion within intact cladding would start to reduce power back to only moderately elevated levels. Subsequently, cladding would start to fail and a sonic-velocity-controlled blowdown of molten fuel into the coolant channels would commence. This blowdown process would generate a reactivity pulse which initially would be positive (unlike the high axial failure location obtaining in slow ramp rate accidents, the failure location moves downward for higher ramp rates) then would peak and go negative as fuel approached and exited from the ends of the core--thereby terminating the transient. The width of the positive part of the pulse corresponds to the time for an acoustic wave to travel from the failure site to the axial ends of the fuel sections of the pins and to return and therefore would be small (~10 ms). This characteristic plus the comparatively low power level existing at the times of failure result in a small radial advance of the failure front before the first pins to fail would start to generate massive negative reactivity and start the shutdown process. Only a small fraction of the core therefore could be in a positive reactivity mode at any one time.

Positive reactivity from postfailure fuel relocation therefore would be small, the corresponding power increase modest, and in view of the short duration of this phase, the energy integrated over it would be negligible. The total transient energy therefore would be limited to that required to fail only a small fraction of the core fuel pins, namely about two full power seconds above normal steady-state conditions. This amount of energy is capable of doing no more than about 0.005 full power seconds of destructive work (~10 MJ at 10^3 MWe reactor power rating, e.g.).

Fuel relocation during both pre- and postfailure times would be driven by that fission gas residing in open porosity of the fuel and which would have become encapsulated into the fuel as it melted (plus perhaps a certain amount of vaporization of ingested bond sodium and coalescing intragranular gas). Metallic fuel pins are unique in two crucial respects; first in having fission gas distributed throughout the entire volume of the pins--in contrast to oxide fuels

which typically have central regions (where most transient melting would occur) of columnar and equiaxed structures which largely are gas free; and second in having the fuel melt and become mobile at a lower temperature (and hence at an earlier time in the transient) than the cladding.

D. SAS4A Modeling for Severe Accident Studies

The development of mathematical models describing the phenomenology discussed above for coolant and fuel behavior under beyond-design-basis conditions is being implemented in the SAS4A computer code, which is being used for analysis of a number of ALMR safety, design, and licensing questions.

The SAS4A code has been developed over many years in the liquid-metal-cooled reactor program to analyze severe, core disruption accidents. In the IFR program, SAS4A is being extended to treat disruption of metallic fuel in the initiating phases of undercooling and overpower accidents. Metallic fuel model development is being carried out in the areas of fuel and cladding behavior, within-pin fuel relocation prior to cladding failure, and postfailure fuel relocation. As they are developed, these models are compared to available experimental data for validation. The models are also used to perform quantitative scoping studies of severe accidents.

Modeling of metal fuel and cladding behavior in SAS4A has focused on phenomena that are 1) important in predicting cladding failure conditions, and 2) important to pre- and postfailure fuel relocation. These phenomena are fission gas generation, release, and migration; internal element pressure loading and cladding strain; and fuel-cladding chemical interaction and cladding thinning due to eutectic formation. In SAS4A, modeling of these phenomena is integrated with the fuel element heat transfer and coolant dynamics models to provide the capability to predict the time and location of cladding failure in severe accident sequences.

Prefailure, within-pin fuel relocation of molten metallic fuel is described with the PINACLE model in SAS4A. This phenomena arises due to the low fuel melting point vis-a-vis the cladding, the high thermal conductivity of metallic fuel, and the associated upward bias in the development of a molten fuel region

within the cladding due to the upward-flowing coolant. The upward bias of the fuel and cladding temperature field plays an important role in both overpower and undercooling accidents and is directly influenced by the coolant flow rate history. In many severe accident sequences, particularly overpower events where the full coolant flow rate is maintained, the molten cavity in the fuel pin may extend to the top of the fuel column, leading to the onset of rapid within-pin molten fuel relocation and fuel ejection into the fission gas plenum space prior to cladding failure. This removal of fuel material from the core provides a significant negative reactivity effect that acts as a self-limiting accident mitigation feature. Within-pin fuel relocation has been observed and quantified in transient overpower metal fuel testing in the TREAT M-series experiments. PINACLE provides a detailed transient fuel and fission gas motion calculation that includes treatment of fuel melting, molten cavity formation and pressurization by fission gas, fuel relocation inside the bottled molten cavity, and in-pin fuel ejection above the original fuel column.

Cladding failure in severe accident sequences initiates the SAS4A postfailure fuel relocation model, LEVITATE. LEVITATE describes both in-pin and ex-pin fuel relocation, together with sodium boiling, cladding melting, and pin disruption. Originally designed to treat disruption and relocation of oxide fuel elements, LEVITATE is being extended to treat unique metal fuel disruption phenomena. Metal-fuel-specific models have been added to LEVITATE describing cladding eutectic penetration (both at the inner and outer cladding surface), cladding ablation and thinning, and cladding axial failure propagation. The particulate/droplet flow regime has been completed and is now in use. This flow model is significant because it allows the use of LEVITATE in situations where a dispersed fuel flow regime might become important. Only simple model changes have been made to the oxide fuel freezing and fuel flow regime models to reflect the characteristics of metal fuels. These will be replaced by more sophisticated metal-fuel-specific models in the future.

Although the metal fuel models in SAS4A are still being developed, their current status is sufficient to allow scoping analyses of severe accident sequences, including abrupt coolant flow coastdown without scram, and high ramp rate transient overpower without scram. In particular, analyses of abrupt unprotected loss-of-flow sequences of varying flow coastdown rates in a

metal-fueled ALMR using SAS4A have demonstrated the accident mitigation characteristics of metallic fuel. In these calculations the motor torque for the primary coolant pumps was set to zero and the moment of inertia of the pump and motor was parametrically decreased to simulate an increasingly rapid flow rate decay. Table IV.1 summarizes the outcome of SAS4A calculations with respect to several key events, i.e., coolant boiling, fuel melting, and fuel element failure. Coolant boiling occurs first when the flow decay halving-time, $t_{1/2}$, drops below 1.0 s. However, in this case the coolant flow recovers and no fuel melting occurs. Fuel melting and element failure are predicted only when the flow decay halving-time becomes $t_{1/2} = 0.3$ s. Fuel melting occurs at 3.95 s and the first fuel element failure occurs in a 4.2 s in the leading power fuel element. The fuel and coolant behavior after fuel element failure were analyzed in detail for this case, and the results are summarized in Fig. IV.1. Figure IV.1 shows the reactor power rise associated with coolant boiling as coolant voids from the core, introducing positive reactivity. The inlet coolant flow shown in Fig. IV.1 is for the lead fuel element (Channel 1), and the chugging behavior is characteristic of liquid metal coolant boiling. The voiding of coolant leads to overheating of the fuel element and failure at 4.2 s, followed by fuel dispersal and neutronic shutdown due to the negative fuel relocation reactivity. This shutdown reactivity is characteristic for metallic fuel, and comes about because of the high axial location of the initial cladding failure. This location is in turn caused by the relatively high value of the fuel thermal conductivity (low energy storage in the fuel's incremental temperature rise above

TABLE IV.1. Timing of Key Events for SAS4A Loss-of-flow Calculations

Case	$t_{1/2}$, s	Boiling Time, s	Fuel Melting Time, s	Failure Time, s
1	2.50	-	-	-
2	1.60	-	-	-
3	1.00	6.0	-	-
4	0.60	4.0	-	-
5	0.30	3.0	3.95	4.2
6	0.15	2.7	3.65	4.0

Fig. IV.1. Power and Flow History for Rapid, Unprotected Loss-of-flow Sequence

the clad), and the upward bias in the axial temperature profile in the cladding. Because of this characteristic, metallic fuel would appear to have an innate mitigative impact on severe accident consequences, which will be further investigated as facilitated by the ongoing SAS4A model development and analysis activities.

E. Bridging the Gap Between Short-term Transient and Long-term Creep Failure Data for HT9 Cladding

1. Introduction

In order to realize the benefits of an ensured durable long life for IFR cladding tubes, a reliable method is needed for predicting their failure under normal and off-normal service conditions. This includes the ability to predict overall life, the effects of short-time transients on overall life, and the possible occurrence of failure during such transients. There is a difference between long-time and short-time failure behavior of the IFR cladding alloy HT9. The key to resolving this difference lies in understanding the effects of microstructural change on strength and rupture life.

The as-heat-treated microstructure of HT9 is that of tempered martensite. This microstructure can coarsen during high-temperature service, thereby reducing its strength. Conversely, the appearance of secondary carbide precipitates^{2,3} during high-temperature service can produce an increase in high-temperature strength. Thus, the mechanical behavior of HT9 cladding tubes can evolve over time, going through successive stages of strengthening and softening. This will affect the way damage is accumulated at each stage of service life. In devising a cumulative damage correlation to be used in a model to predict cladding failure, it is necessary to give consideration to changes in microstructure and mechanical behavior. The ability to address these effects in a scientific way, and include them in a reliable failure model are the goals of the work reported here.

2. Background

a. Rupture

The discrepancy between long-time and short-time high-temperature rupture behavior of HT9 is shown in Fig. IV.2. The failure time t_r is normalized by an Arrhenius factor, and the resulting quantity, $t_r \exp(-Q_r/kT) \equiv \theta$, is the Dorn rupture parameter commonly used in this manner to reduce data taken over a wide temperature-time range to a single master curve.

In Fig. IV.2, however, two master curves are shown: one for short-time transient tests,⁴ and another for long-time creep-rupture tests.⁵ The short-time transient rupture data, reflecting the occurrence of failure over a wide temperature range, were brought into coherence along a single curve by using an activation energy for short-time behavior $Q_r^S = 70.17$ kcal/mole in the Dorn parameter, while the long-time rupture data required an activation energy of $Q_r^L = 154$ kcal/mole to do this. From this fact, it is clear that the temperature dependence of the rupture time is very different for long-time and short-time tests. The creep-rupture data exhibit, for large θ , longer rupture times at a given stress than the transient data, implying that rupture itself is delayed by strengthening processes that occur during long-time testing.

Fig. IV.2. Normalized Transient (FCTT⁴) and Creep Rupture Data⁵

b. Deformation

There is a connection⁶ between the dichotomous rupture behavior discussed above and a similar discrepancy between long-time and short-time flow strength of the material. Long-time high-temperature creep data⁷ appear to exhibit a higher strength than high-temperature short-time tensile data⁸ for the same heat of material given the same heat-treatment. Secondary carbide precipitation occurring during the longer time creep tests but not occurring during the short-term tensile tests can account for the increase in strength and the delay in rupture life. Figure IV.3 shows a plot of creep and tensile data, in which the flow stress is normalized by the temperature-dependent Young's modulus, and the rate is normalized by an Arrhenius factor. This body of data serves to indicate the strengthening effects of precipitation. The tensile data (open circles) reveal a rate-independent flow stress at high strain-rate and low temperature but show a behavior parallel (in slope) to the creep behavior at low rate and high temperature. The solid line in the figure represents a mathematical correlation⁶ that fits the tensile behaviors in the two extremes and provides for a smooth transition between the two. The dashed curve is an extrapolation of the tensile correlation into the creep regime. The gap along the rate

2 of 2

Fig. IV.3. Normalized Creep⁷ and Tensile⁸ Data

axis between the creep data sets at each temperature and tensile flow behavior in this regime implies that the creeping material is considerably stronger than the tensile-tested material. The lowest temperature creep data set (425°C = 698 K) is essentially in line with the tensile data. The other creep data sets each shift increasingly to lower normalized rate with increasing test temperature. Taking the data at 425°C as a baseline, the shifts in normalized rate are reconciled by the factor

$$\exp[(Q_g/k)(1/T - 1/698)], \quad (1)$$

where T is the absolute temperature of the creep test. The activation temperature, $Q_g/k = 25,484$ K, was determined from the time and temperature dependencies of the precipitation strengthening process. It is close in magnitude to the activation temperature for diffusion of chromium in ferrite.

3. Analysis

a. Background

The data in Fig. IV.3, representing published tensile results,⁸ are really the calculated⁶ saturation flow stresses σ_s corresponding to the reported yield and ultimate stresses as fit by a Voce-type equation of the form:

$$\sigma = \sigma_s - (\sigma_s - \sigma_1) \exp(-\varepsilon_p/\varepsilon_c), \quad (2)$$

where σ_1 is associated with the yield stress ($\varepsilon_p = 0$) and ε_p is the plastic strain. The tensile data for Fig. IV.3 were obtained at a strain rate of $4 \times 10^{-4} \text{ s}^{-1}$ at temperatures ranging from 25°C to 650°C. The primary concern here is formulating the high-temperature low-rate portion of the tensile behavior, which can be expressed in terms of strain rate as:

$$\dot{\varepsilon}^t = \dot{\varepsilon}_o^t (E/\sigma_{so})^n (\sigma_s/E)^n \exp(-Q_c/kT), \quad (3)$$

where $\dot{\varepsilon}_o^t = 1.64 \times 10^{14} \text{ s}^{-1}$, $E/\sigma_{so} = 2.525 \times 10^2$, $n = 2.26$, and $Q_c/k = 3.6739 \times 10^4 \text{ K}$, is the creep activation temperature determined previously.⁶ Equation (3) is the dashed line extended from the tensile data. It reflects the intrinsic high-temperature flow behavior of HT9 if precipitation were not to occur during the test.

The creep data in Fig. IV.3 can be fit⁶ with an equation similar to Eq. (3), but with the applied stress replaced by a reduced or effective stress given by

$$\sigma_e = \sigma_s - \sigma_o. \quad (4)$$

Hence,

$$\dot{\varepsilon}^c = \dot{\varepsilon}_o^t (E/\sigma_{so})^n [(\sigma_s - \sigma_o)/E]^n \exp(-Q_c/kT), \quad (5)$$

For each creep data point σ_o is calculated by finding the value of σ_e that causes Eq. (5) to give the measured creep rate at the test

temperature. The creep data display the same stress exponent as the high-temperature tensile data, i.e. $n = 2.26$. For this condition to be satisfied, it is mathematically necessary that the back stress σ_o be proportional to the applied stress, i.e.

$$\sigma_o = f\sigma_s, \quad (6)$$

where f at each temperature is a constant less than unity obtained from Eqs. (1), (3) and (5).

b. Transition Behavior

Each creep data set in Fig. IV.3 eventually undergoes a transition with increasing stress toward intrinsic flow behavior represented by the tensile correlation. This is because the strengthening produced by precipitation has a maximum level: $\sigma_o = \sigma_{om}$. It no longer increases proportionately to σ_s in this region. This effective weakening at applied stresses above σ_{om}/f results in apparent plateaus in the transition region between creep and tensile flow. It reflects essentially the same phenomenon that produces the transition in rupture behavior over time discussed in reference to Fig. IV.2. This observation provides an opportunity to understand this behavior by analyzing short-time mechanical test results only.

First, the transition region in Fig. IV.3 is characterized. From the creep plateaus at each temperature,

$$\sigma_{om}/f = 13.26 \exp(2291/T) \text{ MPa}. \quad (7)$$

The apparent activation energy for creep flow in this region is defined by

$$Q_{ct}/R = - d(\dot{\epsilon}^c)/d (1/T), \sigma/E \text{ constant} \quad (8)$$

Performing this differentiation, using Eqs. (1) and (7) to determine f and σ_{om} , an apparent creep activation energy of 146 kcal/mole is calculated in the transition region (at $\sigma/E = 1.3 \times 10^{-3}$). This value is close to the activation

energy for creep-rupture time (Fig. IV.2), but over twice that for creep flow below the transition. Therefore, what happens in the transition region in Fig. IV.3, reflects behavior during long-time rupture tests. The plateau stress level at each temperature delineates in this sense the regime of short-time behavior above it from that of long-time behavior below it. This model can be used as a guide to determine which rupture correlation (Fig. IV.2) to use under a given set of conditions. Experiments, described briefly below, aimed at better understanding behavior in the transition region have been planned and performed.

4. Mechanical Behavior Experiments

Two kinds of constant-rate high-temperature tensile tests on as-heat-treated material have been performed: those performed in the intrinsic flow regime to verify the tensile correlation; and those performed in the transition region to investigate further the validity of the above model. Samples are austenitized (1038°C, 5 min) and tempered (760°C, 30 min) in flowing helium. The tests are performed in flowing helium to limit oxidation. Further details of these experiments are presented in Ref. 9.

Intrinsic flow behavior was determined at a strain rate of $4 \times 10^{-4} \text{ s}^{-1}$ at 400, 500 and 600°C. These three tests span the range from rate-independent behavior to creep-like flow. The results, plotted in Fig. IV.4, show good agreement with the correlation based on published tensile data.⁸ The parameters of a second series of tests were chosen to place them in the transition region discussed above. Two tests have been completed thus far: one at 600°C at a rate of $1.38 \times 10^{-6} \text{ s}^{-1}$; and another at 575°C at $1.5 \times 10^{-7} \text{ s}^{-1}$. The results of these tests are also shown in Fig. IV.4. The curves drawn through the data represent model calculations for behavior in the plateau region. The model for this behavior was based on creep test results,⁷ so the agreement supports the model and indicates consistency between creep flow and very-low-rate tensile flow in the transition region. The shifts in strength from intrinsic flow behavior caused by precipitation at the test temperatures are also shown in the figure, as are some of the published creep data obtained at stresses below the plateau stress.

Fig. IV.4. The Results of Tensile Tests Performed in the Intrinsic Flow Region (+) and in the Transition Region at 575° and 600°C Shown in Comparison with Model Calculations

5. Discussion and Conclusions

A major concern with regard to predicting IFR cladding failure stems from the discrepancy between long-term and short-term creep and rupture behavior in HT9. A simple path to resolving this problem has been devised in the work described in this report. It lies in a better understanding of high-temperature flow in the transition region between intrinsic flow, where precipitation does not influence strength, and extrinsic flow, where it does. The transition between the two regions represents the same process that causes the transition over time in rupture behavior. This can be illustrated by calculating rupture times at 650°C from the two correlation shown in Fig. IV.2. The plateau stress at this temperature is 158 MPa. At 100 MPa, the short-time correlation predicts failure at about 11 h, while the long-time correlation predicts failure at 360 h. Because the applied stress is well below the plateau stress, it would be expected that the long-time correlation would prevail because rupture is delayed by precipitation. The results of recent "Whole-Pin-Furnace" performed on HT9 at ANL¹⁰ support this position.

The establishment in this work of a microstructurally-based link between the dichotomies in rupture and flow behaviors has resulted in the following accomplishments: a physical basis has been developed for understanding the discrepancy between long-time and short-time failure behavior; a means has been provided to determine when to apply a particular rupture-life correlation; and a scientific methodology has been created that allows the use of the results of short-time tensile results to answer important questions regarding service life of IFR cladding tubes. Further experiments in this vein based on the models have been planned and are being conducted. Additional theoretical modeling concerning rupture-life prediction is also underway.

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V. CORE DESIGN DEVELOPMENT

A. Validation of IFR Neutronics Codes via EBR-II Applications

The IFR suite of neutronics codes is currently being implemented and validated for EBR-II core analysis. The purposes of this project are to provide accurate and efficient support for the routine EBR-II operations, to facilitate the conversion of the EBR-II core to ternary (U-Pu-Zr) metallic fuel, to provide accurate estimates of the isotopic mass inputs to the Fuel Cycle Facility, to use EBR-II as a test bed for implementing the methods which will be used to address challenging physics issues during the design and operations of innovative IFR cores, and finally to deliver to the industrial sector a suite of Verified and Validated (V&V) core design tools.

The EBR-II reactor will be playing a fundamental role in meeting the successive objectives of this project, as it will serve to provide valuable physics data, and also necessary operational experience in utilizing and qualifying state of the art codes and methodologies. The EBR-II core has some unusual design features which make challenging demands on neutronics analysis tools: the first section of this summary describes the physics studies which have been performed to evaluate the analytical needs of EBR-II; the second section reviews the computer codes which make up the suite of codes which address these needs; the third section describes the on-going experimental validation program; the fourth section overviews the numerical validation effort, which addresses issues which cannot be practically assessed through experimental measurements in an operating reactor.

The EBR-II fast reactor has a small core (height = 35 cm, radius = 35 cm) surrounded by a radial steel reflector, itself surrounded by a radial blanket. There are axial steel reflectors below the core and above the fission gas plenum. This configuration leads to a high neutron leakage probability with significant transport effects, and spectral variations. Physics studies of simplified models of the EBR-II core have been performed¹ by comparing broad group diffusion theory flux and eigenvalue predictions to broad and fine group transport theory calculations and continuous energy Monte Carlo calculations. Results indicate that the criticality predictions are very sensitive to the neutronic formulation:

regardless of core loading, the diffusion theory eigenvalue systematically underpredicts the transport eigenvalue by about 4.5% if the same multigroup cross sections are used. Transport calculations require a large number of energy groups (>200) to approach the Monte Carlo predictions within 0.2%. Nevertheless, power density comparisons indicate that diffusion theory predictions are excellent (within 3%) for fueled subassemblies in the core and are slightly biased (approximately 10% overprediction) in the radial blanket.

The standard IFR suite of codes is being used for the core follow analysis of EBR-II: these codes are MC²-2/SDX for the generation of multigroup cross-sections, DIF3D (nodal hexagonal-Z formulation) for the generation of multigroup fluxes, REBUS-3 for the depletion calculations, RCT for the "reconstruction" of pointwise isotopics, and ORIGEN (ORNL--modified to EBR-II/IFR) for activity and heat rate generation. DIF3D is also used for prediction of operational quantities in a coupled neutron-gamma mode. Advanced codes, such as the VIM Monte Carlo code and VARIANT² (3D nodal transport code), are used for specific applications; in particular, it is expected that VARIANT will be used with increased frequency to meet the challenging aspects of future IFR designs.

All of these codes have been well-validated for use in large fast reactor analysis; additional experimental and analytical validation is nevertheless required to address the challenging physics characteristics of EBR-II.

EBR-II operational data has been very useful for validating specific elements of the suite of codes; in particular, control rod worths and subassembly exchange worths are readily available for each run, and have been in excellent agreement with values calculated by DIF3D. Use of the methods has also helped obtain accurate predictions of these key operational quantities. For example, a key prediction made is the reactivity change caused by core loading modifications. The methods which had been used until recently resulted in relatively large mispredictions (3σ errors $\approx$ 70 cents). Recent introduction of the IFR suite of codes is summarized in Table V.1,³ and results in mispredictions which, so far, have been below 20 cents.

Future measurements will also follow the changing neutronic characteristics of the core during the conversion to U-Pu-Zr ternary driver fuel.

TABLE V.1. Comparison of DIF3D Predicted Exchange Worths with Experimental Values for Recent Loading Changes

Loading Change	No. of SA Exchanges	Experimental Worth (cents)	DIF3D Worth (cents)	Error (DIF3D-exp)
157D to 157E	3	-80.0	77.1	3
157J to 157K	1	64.0	69.4	5
157K to 158A	18	59.0	42.7	-16
158B to 159A	59	-26.7	-26.6	<1
159A to 159B	1	112.9	114.8	2
159B to 159C	49	35.4	26.7	-9
158B to 159C	12	107.0	115.3	8
158B to 160A	21	125.4	144.3	19
160A to 160B	2	-63.5	-63.1	4
160B to 160C	1	-7.3	-5.3	2

Experimental validation has been obtained from the irradiation of IFR subassemblies and from routine EBR-II operations. IFR subassemblies have been irradiated in EBR-II since 1984; these subassemblies were in the core for several cycles, and some were dismantled, reconstituted and reirradiated. After their irradiations were completed, chemical analyses were performed to estimate concentrations of burnup indicators, uranium and plutonium isotopics were obtained, and gamma spectrometries were performed to evaluate axial burnup profiles of individual fuel elements. Core follow analyses of EBR-II were initiated prior to the irradiation of the first IFR subassembly; these analyses rely on a detailed EBR-II core model, and faithfully follow its power history, control rod position, and fuel handling operations. Calculated quantities using the IFR suite of codes were compared to measured burnups, isotopics, and axial burnup profiles. Agreement was usually within the experimental uncertainties.⁴ This indicates that the accuracy of the neutronics codes is acceptable for predicting FCF isotopic mass input. Comparisons will continue as more experimental data become available.

Several neutronics experiments are currently being planned, for example: radial and axial dosimetry, blanket burnup measurements, actinide consumption

measurements, subassembly and source substitutions. Accumulation of experimental data will be a key element for the validation of an integrated suite of codes for IFR analysis.

Experimental data from an operating reactor are necessarily limited in scope and quantity; thus an analytical validation program has been set up to supplement the measured data. Analytical validation has consisted of comparing results from the standard suite of codes to results from higher order methods for very detailed models of EBR-II. Agreement is usually good for core quantities which are of particular interest to FCF and EBR-II operations; larger discrepancies (10 to 15%) appear in reflector and blanket subassemblies. Recent implementation of the VARIANT 3D transport code has allowed a significant reduction in these biases, while still providing reasonable computing performance. For example, Fig. V.1⁵ shows the errors in calculated total flux incurred when using DIF3D and VARIANT, as compared to a continuous energy Monte Carlo reference solution; it illustrates the significant improvement obtained with VARIANT in the outlying regions of the core. Analytical validation will continue and will provide valuable information for EBR-II and future IFR designs.

Validation of the suite of neutronics codes has been successful for EBR-II operations and FCF isotopic mass input characteristics. Numerical validation and specific experiments are being pursued to prepare for the implementation of state-of-the-art methodologies for future IFR design and analysis.

B. Mass Tracking (MTG) System

The MTG Software System is a collection of software to provide material control and accountability, criticality safety and process control support for the Fuel Cycle Facility (FCF). It is a near-real-time system that connects to EBR-II analysis at both the front and the back of the fuel cycle. Its input and output are largely interactive, it involves significant database applications, and it will be distributed on a network of Sun workstations, dumb terminals and smart terminals connected to programmable logic controllers which run equipment in the FCF hot cells.

Fig. V.1. Total Flux Calculated With DIF3D and VARIANT as Compared to the VIM Reference Monte Carlo Solution

At the heart of the MTG System is a database that tracks all containers and items of process material in FCF. That database, in turn, consists of a relational database manager (Oracle) as well as a large number of sequential files defining compositions.

All interactions with the database, both for reading and writing, are through individual programs, called MTG System Tasks, which model physical or operational processes. These tasks, which are executed synchronously and which may run in parallel are controlled by a system of Servers which run continuously. The servers use native Unix and TCP/IP features to manage the execution of the tasks. The servers receive their instructions from Operator Control Stations manned by equipment operators throughout the facility, from Terminal Sessions run by engineers through terminals on their desks, and, in a few instances, from an electronic mail-back service.

The software required to support depleted uranium operations in FCF is nearly completed and is currently being tested and qualified. Additional software is being written, and in some cases has yet to be designed, to support

plutonium operations. On the EBR-II side of the fuel cycle a calculational sequence which uses existing codes (mainly REBUS-3, RCT and ORIGEN) and a fuel element database is being created which, in equilibrium, will take operational data from EBR-II and mass data from the MTG System and couple these to handle all segments of the closed IFR fuel cycle.

C. Collaboration with GE on PRISM Mod B/92 Design

The IFR metal fuel has been proposed for use in a wide range of core designs; small modular cores (PRISM⁶ or SAFR⁷) and large monolithic cores (3500 MWt US/European study⁸) have been evaluated with a variety of core configurations (homogeneous or heterogeneous). In recent years, IFR core design work has focused on conceptual evaluation of several generic fast reactor design issues; in particular, core designs with reduced sodium void worth and/or transuranic burning capabilities have been investigated. In the past year, interactive collaboration with the GE industrial team has been renewed; this effort focuses on the detailed development and analysis of the PRISM Mod B design.

The small, modular PRISM (Power Reactor Innovative Small Module) design was developed by GE as part of the DOE fast reactor design competition in the 1980s, and the PRISM concept was chosen as the reference design for the DOE sponsored Advanced Liquid Metal Reactor (ALMR) program in January 1989. The reference ALMR design (called Mod A in GE notation) is rated at 471 MWt, utilizes electromagnetic primary pumps, and uses a reactor vessel auxiliary cooling system (RVACS) for passive decay heat removal in degraded conditions. In addition, a strict vessel diameter constraint is applied to allow rail shipment.

The purpose of the Mod B design study is to evaluate the potential for increased power production in PRISM-like reactor designs. In particular, potential economic benefits of larger module size are being assessed, and safety behavior relative to the Mod A design is being analyzed. (The Mod B design developed in 1991 (PRISM Mod B/91) by GE exhibited only marginal economic benefits, and safety behavior similar to the Mod A design; however, it was envisioned that further design and optimization of the Mod B concept would be performed.) The Mod B/92 design was developed to achieve maximum power

production while maintaining the essential PRISM design features. The reactor size is constrained by the fabrication limit for a solid plate reactor closure (a maximum vessel diameter of 33 ft); and a core barrel inner diameter of 15-16 ft is implied. Given the vessel size, the power output is constrained by the capacity of the RVACS decay heat removal system. For the enhanced RVACS design utilized in this study, a maximum power output of 840 MWt is targeted. Based on secondary system considerations, the coolant outlet is specified at 930°F with a 250°F core temperature rise. An EM primary pump was retained from the Mod A design.

Given this new reactor envelope, power level, and coolant temperature bounds, collaborative efforts with GE have focused on developing a favorable reactor design. In this report, neutronics design, thermal-hydraulics design, and transient safety analyses for the Mod B/92 core will be highlighted.

1. Mod B/92 Neutronics Design

The current Mod B neutronics design analyses have focused on the development of a 840 MWt fissile self-sufficient core design with emphasis on reducing economic costs; future analyses will address the application of reduced void worth or transuranic burner cores in the Mod B concept. The core design criteria were established in close collaboration with the GE core design group and a representative fuel assembly geometry was specified. For the Mod B/92 design study, the driver assemblies contain 217 ternary metal fuel pins (75% smear density) using 0.293 in. outer diameter HT9 cladding; blanket assemblies contain 127 binary metal fuel pins (0.428 in. outer diameter) at a fuel smear density of 85%. Both assembly types utilize HT9 assembly ducts with a duct outside flat-to-flat dimension of 5.63 in.; the duct walls are 0.175 in. thick with a 0.175 in. interassembly gap. These assemblies are significantly smaller than the 6.11 in. (flat-to-flat) assemblies proposed for the PRISM Mod A design; for the proposed Mod B assemblies, the duct venting used in the Mod A design is not required because the duct dilation is significantly reduced. In addition, preliminary evaluations of the duct dilation predictions indicate that the use of refined calculational methods (finite element analysis⁹ and inclusion of recent HT9 swelling data) results in significantly lower dilation predictions.

Therefore, reductions in the required duct wall and gap thickness or the use of larger assemblies will be investigated in future studies.

A wide range of potential core configurations for the Mod B/92 design were evaluated; variations in the internal blanket arrangement, driver and blanket height, and cycle length were analyzed in a series of trade studies.¹⁰ Conventional metal fuel and HT9 structure performance limits were applied: peak linear power limit based on fuel solidus temperature, 155 MWd/kg peak discharge burnup limit, and 3.6×10^{23} n/cm² peak fast fluence limit. The core configuration studies also addressed the placement of three different types of nonfueled assemblies: nine control assemblies are utilized to assure adequate shutdown margins, four ultimate shutdown assemblies are allocated as an alternate shutdown system, and six gas expansion modules (GEMs) are utilized to enhance the loss-of-flow reactivity feedback. The latter two assembly types were carried over from previous GE design requirements (i.e., Mod A); and future studies will address the cost-benefit of retaining them. Based on these trade studies, a cycle length of 22-24 months was targeted and a compact core configuration was sought; it was mutually agreed that configurations with fewer assemblies are preferable to derated designs, although derated designs allow longer cycle lengths. In addition, 1/3 core batch reload symmetry was maintained for the driver assemblies for a three batch refueling scheme. The proposed Mod B/92 core configuration is shown in Fig. V.2; for this layout, both the driver and blanket assemblies have an active height of 42 in. and the cycle length is 23 months with an 85% capacity factor.

In the Mod B/92 core design, blanket shuffling is utilized to eliminate the large outlet temperature swing between fresh and irradiated blanket assemblies (see next section). The blanket shuffling scheme is defined by both the number of shuffling paths and the direction of motion within each path. Exhaustive neutronics analyses indicated that 10 shuffling paths allow the longest average blanket residence time without violating the fluence limits and a modified in-out (internal blanket residence first) movement scheme yields superior nuclear performance (the burnup reactivity swing and fluence levels are lowest). The nuclear performance characteristics of the Mod B/92 core design with blanket shuffling are summarized in Table V.2.

Fig. V.2. Proposed Mod B/92 Core Configuration

In Table V.2, the performance characteristics of the Mod B/92 design are compared to the Mod A and Mod B/91 design evaluations. As shown in Table V.2, the enhanced RVACS system proposed for Mod B/92 allowed a power level increase from 750 MWt in Mod B/91 to 840 MWt; both Mod B designs produce significantly more power than the 471 MWt Mod A reference design. Because of the severe diameter constraint of the Mod A design, a tall core (53 in.) is required; a more conventional core height of 42 in. is proposed for the Mod B/92 design. Because of the increased power rating and decreased height, the Mod B/92 design has a much larger core diameter than the Mod A design; however, the Mod B/92 core diameter is 19 in. smaller than the Mod B/91 design. The compactness of the Mod B/92 configuration is further demonstrated by the higher linear power (10 kW/ft) compared to the derated (6.7 kW/ft) Mod B/91 design. Because the enrichment has decreased from 28% in Mod A to 23% in Mod B/92, the metal fuel thermal conductivity and fuel solidus temperature will increase (see Refs. 11 and 12) allowing Mod B/92 to operate at a higher linear power rating while maintaining similar fuel melting temperature margins. The fuel cycle payoffs of the Mod B/92 core are clearly demonstrated in the mass flow performance characteristics. The larger Mod B/92 design requires significantly less initial fissile material per MWt (2.26 kg/MWt as compared to 2.74 for Mod A and 2.92 for Mod B/91), and fewer pins manufactured per MWt-year (8.0 pins/MWt as compared to 8.2 for Mod A and 12.8 for Mod B/91).

TABLE V.2. Comparison of Nuclear Performance Characteristics

Configuration	Mod A	Mod B/91	Mod B/92
Power, MWt	471	750	840
No. of Driver Assemblies	42	132	126
No. of Internal Blanket Assemblies	24	66	60
Height, in.	53	36	42
Core Diameter, in.	93	154	135
Cycle Length, months	20	24	23
Breeding Ratio	1.11	1.11	1.04
Burnup Swing, % Δk	0.35	0.64	0.44
Peak Linear Power, kW/ft, Driver (BOL)	9.52	6.73	10.1
Blanket (EOL)	10.5	11.1	11.2
Enrichment, wt.% TRU/alloy	28.0	23.3	23.1
Peak Discharge Driver Burnup, MWd/kg	149	132	154
Internal Blanket	43	41	53
Peak Driver Fast Fluence, 10^{23} n/cm ²	3.22	3.22	3.50
Blanket	3.32	3.17	3.62
Initial Inventory, kg, Fissile Pu	1,290	2,190	1,896
Total Heavy Metal	16,036	27,196	22,935
kg fissile/MWt	2.74	2.92	2.26
Equilibrium Loading, kg/y, Fissile Pu	235	348	330
Total Heavy Metal	2,787	4,277	3,608
No. of pins/MWt-year	8.21	12.84	8.03
Net Fissile Pu Gain, kg/y	15.2	23.1	9.6

2. Mod B/92 Thermal-hydraulic Design

The thermal-hydraulic design analyses focus on allocation of the coolant flow to the various Mod B/92 assemblies. The total coolant flow is dictated by the reactor power level and design coolant temperature rise; however, the relative allocation to each assembly can be tailored using orificing. As

mentioned earlier, blanket shuffling is utilized in the Mod B/92 design. This severely reduces the blanket overcooling penalty because the assembly flow rate must only provide adequate cooling for the maximum power production over a single cycle residence; to exploit the advantages of blanket shuffling, gridplate orificing must be utilized. For the Mod B/92 design analyses, eleven distinct gridplate orificing zones are allowed for the fueled (driver and blanket) assemblies.

The relative flow allocation to the orificing zones is tailored to meet a variety of temperature constraints for the fuel, cladding, and coolant; typical temperature targets are:

1. Adequate margin-to-melt must be maintained at the fuel centerline even at 11% overpower conditions,
2. Clad temperatures must be low enough that cumulative damage does not cause pin failures, and
3. Thermal striping potential between adjacent assemblies must be maintained below 370°F.

In practice, the fuel centerline and thermal striping temperature constraints are met by most typical orificing schemes. Therefore, the relative flow allocation is targeted such that pins discharged from each orificing zone will have roughly the same cumulative cladding damage (failure probability).

An analytical tool for estimating the cumulative damage of driver and blanket pins was previously developed by GE for PRISM analysis; cumulative damage is quantified using the cumulative damage fraction (CDF) and strain measures applied to a pin's burnup, temperature, and fluence history. The relative damage to both blanket and driver pins must be estimated because while the blanket pins operate at a lower burnup (and corresponding lower fission gas pressure), they have a longer residence time (extended exposure to high temperatures). For the Mod A design, results indicated that excessive strain levels could be expected for long residence blankets. Therefore, the Mod A flow allocation was tailored so that the irradiated blankets were overcooled to reduce their cumulative

strain; this orificing scheme diverts flow from the driver assemblies and fresh blankets, increasing their operating temperatures.

Several refinements to the CDF/strain estimator code were investigated as part of the Mod B/92 design analysis. The most important change was the application of a strain hardening approach¹³ for evaluation of the HT9 thermal strain correlations; the time-hardening approach utilized in the original code does not adequately account for the damage history. In addition, the burnup dependence of fission gas release fraction as described in Ref. 14 causes less fission gas to be released in the low burnup blanket pins. The effects of these changes on the CDF and strain predictions is shown in Table V.3. Using the time-hardening strain approach, it is found that overcooling of the blankets is needed to reduce the blanket strain level from 13.5% to 0.8%; however, the strain-hardening predictions indicate that overcooling of the blankets may not be necessary. When the gas release fraction differences between the blanket and driver fuel are applied, it is clear that overcooling of the blanket assemblies

TABLE V.3. Pin Damage Estimates

Parameter	Overcooled Blankets ^a		Equalized Temperatures ^b			
	Blanket Path A	Driver (4,2)	Base		60/80% Gas Release ^c	
			Blanket Path A	Driver (4,2)	Blanket Path A	Driver (4,2)
CDF	1.33E-4	3.07E-3	1.42E-3	5.43E-5	6.96E-5	1.16E-5
Strain, % Time-hardening	0.804	7.22	13.5	0.79	0.51	0.60
Strain, % Strain-hardening	0.814	1.76	1.19	0.84	0.48	0.64
Peak 2 σ Clad Midwall T, (°F) (°C)	1200 (650)	1200 (650)	1150 (621)	1150 (621)	1150 (621)	1150 (621)

^aMod A orificing criteria, irradiated blankets are overcooled to reduce their strain level causing higher temperatures in the driver regions.

^bSimple orificing criteria, peak 2 σ clad midwall temperatures are equalized for all orificing zones.

^c60% fission gas release assumed in the blankets, and 80% in the drivers (note that 100% gas release is assumed in the base calculation).

is unnecessary. The simple orificing criteria of equalized peak cladding temperatures for each coolant orificing zone roughly equalizes the discharge blanket pin (CDF = $6.96\text{E-}5$ and strain = 0.48%) and discharge driver pin (CDF = $1.16\text{E-}5$ and strain = 0.64%) damage levels.

Using the equalized cladding temperature criteria, core orificing was performed for the Mod B/92 design using the multi-assembly steady-state thermal-hydraulic code SUPERENERGY-2.¹⁵ Three-dimensional coupled neutron-gamma heating distributions were calculated for the BOEC and EOEC critical configurations. Using these power distributions, each fueled assembly is modeled in SUPERENERGY-2 using subchannels for each pin, stage factors to account for fresh batch effects, and a simplified interassembly heat transfer model. For each subchannel (every pin in the core), coolant, clad, and fuel temperatures are calculated for each axial mesh (1 in. tall) at nominal and 2σ conditions. The fueled assemblies were grouped together into orificing zones based on their power level; four orificing zones were utilized for the driver assemblies and the blanket assemblies were allocated to seven zones. Flow through the nonfueled assemblies was explicitly modeled (about 1.5% of the total flow) and a cold bypass flow of 1.5% was assumed. The relative flow rates of the fueled orificing zones were then iteratively modified until the peak 2σ cladding temperatures of each orificing zone were equalized. The equalized peak 2σ cladding temperature for the Mod B/92 design was 1148°F (620°C). In addition, adequate overpower fuel margin-to-melt temperatures were observed; and, thermal striping criteria were met.

3. Response of B/92 Reactor to Unprotected Accidents

The transient response of the Mod B/92 design to various unprotected accidents has been calculated using the SASSYS systems analysis code. A detailed model of the reactor core, the primary sodium circuit, and the secondary sodium circuit was developed. For the purposes of this study, three double-fault accident scenarios were considered: an unprotected, (i.e., unscrammed) loss-of-flow (LOF), an unprotected transient overpower (TOP) and an unprotected loss-of-heat-sink (LOHS). The goal of the study is to determine the minimum margins during the transients to failure thresholds such as coolant boiling or fuel pin failure.

a. Unprotected LOF Accident

The Mod B/92 design uses electromagnetic (EM) pumps in the primary sodium circuit. These pumps are driven by synchronous machines, which in turn are powered by offsite power. The accident scenario assumes a loss of the offsite power with failure to scram the reactor. The synchronous machines coast down, providing a continual drop in the power being supplied to the EM pumps. This causes the flow in the primary sodium circuit to decrease with time. Due to the means for powering the EM pumps, the drop in power to the pumps can be tailored to provide the optimum coastdown for limiting the coolant temperature rise, within the constraint of the inertia available in the synchronous machines. The current proposal provides an instantaneous drop in voltage to the EM pump of 15% at the loss of offsite power, conserving the inertia in the synchronous machines for later in the coastdown when the peak temperatures will occur. The flow coastdown and the power history during the transient are shown in Fig. V.3a.

The Mod B/92 design also incorporates gas expansion modules (GEMs) at the periphery of the core. These are gas filled assemblies, closed at the top, such that at normal flow and inlet plenum pressure conditions, the gas is compressed and sodium partially fills each GEM to a level above the top of the active core region. During a loss-of-flow accident, the drop in inlet plenum pressure allows the gas in each GEM to expand downwards, increasing the neutronic leakage of the core and providing substantial negative reactivity feedback. For the Mod B/92 design, a total of approximately -0.4% of reactivity is obtained from the GEMs. This negative reactivity feedback is generated very early in the transient, and persists as long as the pumps are not restarted. For the purposes of this analysis, it was assumed that the negative reactivity associated with the GEMs is created in the first 10 sec of the transient, as shown in Fig. V.3b, where the GEM reactivity is identified as a "programmed" reactivity. The timing of this reactivity is adjustable based on the design of the GEM. Parametric variation of the timing showed that there is very little difference in the peak temperatures obtained during the transient as long as the GEMs respond within the first 30-40 sec after the start of the flow coastdown.

Fig. V.3. Results for an Unprotected LOF Accident in the Mod B/92 Reactors. (a) Normalized Power and Flow vs Time, (b) Reactivity vs Time, and (c) Peak Coolant and Clad Temperatures vs Time.

The net result is that the use of metallic fuel, with its favorable operating and reactivity feedback characteristics, along with the GEMs, control rod driveline expansion, and appropriate design of the radial restraint system to maximize core radial expansion causes significant net negative reactivity to be generated during the unprotected LOF transient. This minimizes the mismatch in power and flow from the flow coastdown and provides large margins to the failure thresholds, as shown in Fig. V.3c. The results are shown for the highest temperatures obtained in the hottest fuel driver assembly. The transient response is characterized by two peaks, one at about 9 sec and the second, higher temperature, peak of 696°C at 50 sec. The minimum margin to coolant boiling is over 250°C throughout the transient. Even if a conservative estimate of the minimum temperature for the onset of fuel/cladding interaction is used, the peak cladding temperature exceeds this value for only 100 sec. Cladding damage for this length of time would be negligible. At 200 sec after the start of the transient, the peak coolant and cladding temperatures are down to their nominal steady-state values, and the peak fuel temperature has been reduced 200°C.

b. Unprotected TOP Accident

The accident scenario for this accident is that an uncontrolled withdrawal of the control rods occurs, with a failure to scram. The Mod B/92 design uses adjustable control rod stops to limit the reactivity inserted from such an event to 0.4\$. For the purpose of this analysis, it is assumed that the reactivity is inserted at a rate of 0.02\$/sec, which is a bounding value with the expected insertion rate being lower in practice. The addition of 0.4\$ of reactivity causes a power increase, which is counteracted mainly by the negative reactivity feedback generated from control rod driveline expansion and radial expansion of the core. Also, for simplicity, it is assumed that the steam generator is capable of operating at power levels higher than normal, such that the sodium temperature leaving the steam generator in the intermediate loop is constant throughout the time duration followed in this calculation.

The initial rise in power is calculated to peak at 1.82 times nominal power. There is then a drop in power over the next 100 seconds to about 1.6 times nominal. A new steady-state value of 1.56 times nominal power is

established approximately 400-500 sec after the start of the transient. (This is due to the assumption of a constant sodium temperature leaving the steam generator; if the steam generator were not able to handle this power level, this accident would eventually cause a loss of heat sink as well). The peak coolant temperature is at least 400°C below the saturation point during the transient. The peak cladding temperature is 670°C. However, by 80 sec after the start of the transient, the peak cladding temperature drops to the threshold, and remains there. There should be negligible cladding damage.

c. Unprotected LOHS Accident

The unprotected loss-of-heat-sink accident is initiated by the loss of heat rejection capability at the steam generator, with failure to scram the reactor. The loss of the main heat sink causes the sodium coolant temperature leaving the steam generator to increase. This eventually causes the core inlet temperature to rise. The favorable reactivity feedback and operating characteristics of metallic fuel, combined with substantial negative reactivity feedback from control rod driveline expansion and radial core expansion reduce the core power so that the peak core temperatures do not increase substantially. Calculations of the accident consequences show that the core inlet temperature begins to rise at about 30-40 sec after the loss of the steam generator heat removal capability. The peak coolant temperature is reached at 95 sec after the start of the accident, after an increase of 26°C from the steady-state. The rapid drop in core power then reduces the peak coolant temperature so that by 1500 sec, the core is essentially at decay heat power level, and the peak coolant temperature is 14°C below its steady-state value. The margin to coolant boiling is estimated to be at least 500°C throughout the accident, and the peak fuel/cladding interface temperature of 550°C is below the conservative threshold for fuel/cladding interaction.

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VI. FUEL CYCLE DEMONSTRATION

A. Status of Facility Modifications

The most influential factor affecting this year's progress was that the Department of Energy (DOE) was unable to sustain funding for the Project for the final six months of the fiscal year. It had been anticipated that a contract with the Japan Atomic Power Company (JAPC) would relieve the funding problem in the second half of the year. However, DOE was unable to execute the contract until FY 1993. As a result, the construction work force was drastically cut, procurements were halted, drafting effort and some engineering effort were reduced. Priorities were reassigned in order to keep the Project moving forward at an optimum pace under the assumption that a normal funding profile would resume in FY 1993. In spite of this, significant progress was made with the facility, equipment, and documentation. The extent of this progress is described below.

1. Corridor Modifications

The Corridor Modifications task was reactivated after a 12-month hiatus due to lack of engineering support. Major changes include removal of offices from the operating floor, upgrade of the entryway and display area, plumbing modifications, and provision for women's locker room. Upgrade of the passageway roof was also determined to be necessary.

The construction Installation and Test Instruction (ITI) was signed, materials arrived on site, and work began. The first areas of construction are the new Health Physics offices and demolition for the new women's locker room. Construction on the passageway began in mid-September.

2. Transfer Systems

The new Small Transfer lock was installed in the penetration between the Air Cell and Argon Cell. This will be the main transfer system between the cells and will handle all fuel and waste transfers as well as small equipment items.

Relocation and seismic bracing of the pneumatic tubing in the transfer tunnel was completed. The terminal for the pneumatic transfer system in the spray chamber was completed and a test item was successfully transmitted.

The weldment for the shield plug in the south wall of the transfer tunnel was completed and installed. All drive shafts and feed throughs were installed and the concrete shielding was then placed in the plug.

The platen was installed on the spray chamber cart in the transfer tunnel cart system. The hydraulic power supply (shown in Fig. VI.1) for the transfer tunnel rams was installed on its mounting pad in the basement and the hydraulic piping to the rams was installed and leak tested. Alignment of the cart motors was completed and checkout and testing of the hydraulic drive ram units commenced. The limit switches on the carts were adjusted and the carts were successfully operated by the computer control system as part of the PLC software checkout process.

Fig. VI.1. Power and Controls to Ram Hydraulic Package
(ANL Neg. No. 103-V12247)

Installation of the anchor bolts cable drive assembly and vertical drive assembly for the platen safety restraint (PSR) was completed. Fabrication of all components for the platen safety restraint were completed and installation is in progress, as shown in Fig. VI.2.

The transfer tunnel shield door was installed with the exception of the electrical wiring.

3. Cask Transfer System Modifications

The final design review for the cart structure was held. Checking of the drawings for the structure and machinery was completed. The drawing package was submitted to the ANL Industrial Safety organization for design concurrence. Air cell floor penetration components were reviewed and design changes were made. Drawing checking was completed and a fabrication request was issued. This work covers fabrication of two bag-out rings and modifications to the bag vent tubes.

4. Suited Entry Repair Area (SERA)

Fabrication of the SERA west window frame was completed. Installation of the west window and personnel entry seal door has been completed. All windows were installed in both the shielded and unshielded SERA glove walls. Fabrication of the east window was also completed.

A preliminary design package for the SERA bag-out system was completed and reviewed. Conceptual design review packages were submitted for the bag-out cart and electro-mechanical manipulator pit hatch cover.

5. Argon Cell

Core boring of the Safety Exhaust System (SES) and Non-Destructive Analysis (NDA) penetrations was successfully completed.

A mockup of the central observation room window shield tanks was constructed and filled with low strength concrete. No shrinkage cracks occurred

Fig. VI.2. Safety Restraint for Equipment Transfer Lock Platen
(ANL Neg. No. 103-V12222)

during curing. After the compression sample is tested, placement of shielding concrete in the central observation window cavities and Window A-4 can proceed.

6. Argon Purification System

The assembly of the control cabinet was going well at year's end. The analyzers, all the flowmeters, and all but one gauge were installed. The electrical work has been completed. Construction of the hydrogen storage building was begun. The excavation work and installation of the concrete pad are complete.

7. Argon Cell Feed/Bleed System (FBS)

Relocation of the 70 psig surge tank has been completed. Modification of the vacuum pump room wall to meet seismic requirements was 90% complete at year's end. A temperature sensor was selected for the carbon filter in the exhaust system.

8. Argon Cell Pressure/Temperature Control System (PTCS)

The instrumentation portion of the electrical schematics describing the Pressure/Temperature Control System (PTCS) was completed. The Small Logic Controller (SLC) interface is being designed and information to add phase failure relays to the input/output database was provided. The schematics for the PTCS and Recirculation System have been revised to reflect the interface to the Small Logic Controllers. Preliminary work on the PTCS ITI is in progress.

An entry into sub-cell D was made to determine requirements for replacing two existing Resistance Temperature Detectors (RTD's) located at the inlet to each recirculation loop. New RTD's will be installed to be used as the primary sensors of argon cell temperature. Fabrication of the cylinder racks for the emergency argon system was completed. The design review report for the PTCS and the feed/bleed system was issued and resolution of review board comments and recommendations began.

9. Refrigeration System

A decision has been reached to control the cooling system by sensing the pressure in the Argon Cell rather than by sensing temperature. Assembly of the freon detector was started and instructions for installation was approved.

The Statement of Work for the manufacturer of the system to conduct a formal engineering evaluation of the existing 50-ton chiller system and the 5-ton cooling system for the argon purification system was approved and sent to Procurement.

10. Argon Cell Safety Exhaust System (SES)

All of the major SES components have been received, including the isolation dampers, expansion joints, three-valve manifolds, and HEPA filter assemblies.

Final design approval was received for the SES piping supports/anchors package including a new design for the Argon Cell SES inlet piping and shielding.

The ASME stress analysis for the reconfigured SES air-line system has been completed. All stress levels were below the allowables so the reconfigured system does meet the stress criteria. Structural analyses for the Safety Equipment Building (SEB) dampers and HEPA filter housing were reviewed and accepted.

11. SES Sense and Command System (SCS)

The Phase II Final Design Review for the Sense and Command System (SCS) was held in May. The SES Sense and Command design review committee requested that a study be performed to determine the probability of inadvertent malfunction and if a number of alternatives could be implemented to reduce the calculated number of inadvertent system actuations. The basic conclusion of this study was that the rate of inadvertent actuation is a consequence of the design philosophy (i.e., fail safe) and not the implementation of the SES Sense and Command design. In summary, the study shows that the SES system may cause a spurious actuation about once every two years.

Fabrication and assembly of the Sense and Command System was completed and acceptance tests were conducted at the vendor's facility. Several correctable minor deficiencies were noted. The Sense and Command System was received at ANL in September. Certified Vendor Data, e.g., seismic test reports, and drawings, were received and are being reviewed for acceptance.

12. Safety Equipment Building (SEB)

A temporary membrane roof was installed on the Safety Equipment Building. Painting of the SEB and connecting tunnel was completed. Temporary space heaters were installed to maintain adequate temperatures for the safety class equipment in the building. The lifting beams were load tested and the diesel storage tanks and diesel generators set in place. Installation of the fire suppression system was completed. The Safety Equipment Building mechanical and electrical contract was completed. Fig. VI.3 shows the class 1E panel and seismically qualified uninterruptible power supply in the SEB and Fig. VI.4 shows the diesel generator load test banks installed just outside the SEB.

13. Electrical Power Systems

Motor control centers (MCC) F2, F4, and E2 were mounted; MCC-F2 (shown in Fig. VI.5) was energized. MCC F1A and F1B were removed after all loads were removed and all five UPS units were installed. The unit in the southwest corner of the FCF basement is shown in Fig. VI.6. Installation and Test Instructions (ITI) for transferring the suspect stack exhaust fan No. 1 from motor control center F1B to the new MCC E-2 were approved.

Nutherm successfully conducted a seismic-type test for the 5 kW heaters and Class 1E 9 KVA transformers. The personnel radiation monitors, five uninterruptible power supplies, the stack monitoring equipment, the uninterruptible power supply support equipment consisting of seismically qualified switches, transformers, and panels, and the last Class 1E power system equipment items (motor control centers) were received before year's end.

14. Stack Monitor

Installation of steel support and decking was completed for the equipment mezzanine. Installation was then completed, including moving and mounting all the new electronic cabinets (Fig. VI.7). Sample probes and sight-ports, sample tube lines and brackets were installed. The electrical installation on the stack (less heat trace system) was also completed. Fig. VI.8 shows the work beginning on installation of the stack components and Fig. VI.9 shows the completed installation of the sample transport lines.

Fig. VI.3. Seismically Qualified Uninterruptible Power Supply (UPS) Installed in the Safety Equipment Building (ANL Neg. No. 103-V12241)

Fig. VI.4. Diesel Generator Load Test Banks (ANL Neg. No. 103-V12243)

Fig. VI.5. Motor Control Center F2 Installed and Energized
(ANL Neg. No. 103-V12251)

Fig. VI.6. UPS and Support Equipment in Southwest Corner of FCF Basement
(ANL Neg. No. 103-V12246)

Fig. VI.7. Stack Monitor Cabinets
(ANL Neg. No. 103-VI2239)

Fig. VI.8. Beginning Installation of Stack Components
of Stack Monitoring System (ANL Neg. No. 103-
VI2041)

Fig. VI.9. Stack Monitor (ANL Neg. No. 103-V12171)

The stack monitoring calibration procedure was completed, and the entire stack monitoring plan was submitted to the DOE area office for review and transmittal to the State of Idaho.

15. Air Cell and Building Exhaust Systems

A final design review of the ventilation system instrumentation and controls was held. Fabrication of the SERA and spray chamber stainless steel exhaust ductwork was completed. Installation of the ductwork for the building exhaust system began. The purge system for the air cell windows is complete and the system is in service.

Condensate lines were rerouted behind the building exhaust fans and above the basement fire system preaction valve. Eight DOP test modules (for insertion between the first and second stages of the air cell exhaust system HEPA filter bank) were received. The ITI to install these modules was completed. An ITI to connect air cell exhaust fan No. 1 (Fan "A") to the new motor control center MCC-E1 was completed. The ITI to connect air cell exhaust fan No. 2 (Fan "B") to the MCC-E2 was approved.

Bids for the building exhaust system fan ripout/install contract have been received. The new building exhaust fans are scheduled to be installed in December after the award of the contract.

16. Radioactive Liquid Waste System (RLWS)

Shielding and seismic analyses of the shielding walls for the RLWS tank room were completed. The tank room sump design was reconfigured to include a HEPA filtered, gasketed cover with hard piping of overflow lines for enhanced ALARA operation in the event of a tank overflow.

Three process tanks fabricated to ANL's designs were delivered. Fixed price quotations were received for the other two major tanks in the RLWS. However, an existing tank from Rocky Flats that ANL has been attempting to acquire will be used as a substitute for one of these to save cost. This includes the neutron-poison rods that go into the poison-rod tank.

A review was completed of earlier designs of the high-pressure spray system for the decontamination spray chamber. The spray chamber sump fabrication was completed. Design of the penetrations for the new shield walls around the RLWS tank room was also completed.

17. Facility I&C

Fifteen system schematic drawings supporting installation of systems serviced by the facility programmable logic controllers (PLC's) were completed and approved. Thirty-one system schematics are now completed. The remainder are associated with Argon Cell recirculation, Argon Cell pressure temperature

control, SES, and refrigeration. The pulling of Ethernet and data-highway cable between 10 PLC cabinets is in progress, as is pulling wire and cable between the PLC cabinets and their corresponding devices and equipment. Installation in progress is shown in Fig. VI.10.

18. Miscellaneous

Installation of the building fire sprinkler piping was completed, but the final inspection revealed an error in the elevation of many of the sprinklers. Modifications corrected the problem and brought the system into conformance with code.

B. Status of Process Equipment

1. Fuel Element Chopper (EC)

The Fuel Element Chopper, shown in Fig. VI.11, was remotely installed and assembled in the Argon Cell in FY 1991. The EC is the first piece of

Fig. VI.10. PLC Cabinet 500 A Interior With Conduit and Wiring Installation in Progress (ANL Neg. No. 103-V12245)

Fig. VI.11. Fuel Element Chopper Installed in the FCF Argon Cell
(ANL Neg. No. 103-U12260)

operable equipment in the FCF Argon Cell. The system was operated several times during FY 1992 for checkout and demonstration for visitors. In cell testing of the FC with depleted uranium will be delayed until Phase 2 modifications to FCF are completed.

2. Electrorefiner (ER)

The electrorefiner, shown in Fig. VI.12, is the most complex piece of process equipment to be used in the IFR fuel cycle demonstration. Substantial progress was made in designing, fabricating, assembling and testing ER components and remote handling fixtures. As with the overall project, progress in the second half of the year was slowed by the project funding hiatus.

Significant progress was made in assembling and testing control cabinets. Assembly of Cabinet No. 1 (port mechanisms) is complete and operator control station/programmable logic controller (OCS/PLC) Port A, B and C software

Fig. VI.12. FCF Remote Electrorefiner (Partial Assembly)

interface development testing was completed using the PEQA (Prototype Electrorefiner Quadrant Assembly). A new larger PLC 5-40 was received and is successfully operating with the electrorefiner PLC code developed previously on the PLC 5-25. Cabinet No. 1 was disconnected from the PEQA and moved near the electrorefiner, to which it is being temporarily wired. Interface testing for Port D will be completed at the ER. The control system checkout demonstrated operability of valve and latch assembly (VLA) valve actuator motor, VLA latch motor, bracket latch assembly (BLA) latch motor, and electrode assembly (EA) rotation motor. Routing of correct signals from all limit switches and resolvers on the test stand to the PLC was also verified.

The cabinet No. 2 interconnection wiring diagrams and control schematics were finished. All cabinet No. 2 components were ordered, but cabinet Nos. 2 and 5 were delayed by the funding problem. Control system checkout of Cabinet No. 3 with OCS/PLC development software has confirmed operability of all cabinet hardware. Cabinet No. 4 was constructed, inspected, delivered and tested. This cabinet contains the electrode power supplies. Testing consisted of operation with a dummy load of 300A connected to each power supply. All components showed proper indications and operating conditions. The cabinet ventilation system proved to be capable of providing proper cooling under simulated worst-case conditions.

The bulk loading system for initial charging of cadmium and salt into the electrowrefiner was received as were two core sampler assemblies. The cadmium and salt stirrer and a complete spare were also received. Initial fitup with the ER and functional testing was successful.

Preliminary testing of the cadmium and salt stirrer was completed using water as the test fluid in a 55-gal drum container. Power requirements were as expected although a vortex was formed at speeds above 100 rpm. The ER cadmium and salt stirrer assembly test report was completed and distributed in June. The four ER baffles have been redesigned to provide full baffling in the salt as well as the cadmium. This change is required (as verified in the stirrer test) to reduce vortex formation in the salt at higher speeds. The qualification test will include a test configuration that includes full baffling to minimize vortex formation.

The ER heater control system test was successfully completed although, one temperature indicating controller (TIC) failed and had to be replaced. The primary vessel required approximately 4.7 kW to maintain a temperature of 250°C.

The first furnace tests of a high-temperature level detector probe for the instrument assembly proved to be satisfactory. Testing in the Mk-III electrowrefiner is planned for FY 1993.

A mockup of the electrorefiner cover gas (CGS) sub-system was assembled for flow and pressure control testing purposes. Preliminary data indicate that the system leak rate at 2 in. Water pressure differential is less than 0.3 scfm of argon. For this test, the ball valve was closed, the cadmium and salt stirrer was in place, and maintenance plugs were installed in the four electrode assembly (EA) ports and one instrument port. The other instrument port was used for a static pressure tap and the mockup CGS piping was connected to the CGS Port. With all EAs in the loaded position in the ER, the argon gas leakage was found to be an acceptable 0.7 scfm at 2 in. H₂O pressure differential.

The valve and latch assemblies (VLAs) have been installed and mechanically qualified on the electrorefiner. Permanent wiring of four valve and latch assemblies and six EA brush assemblies was completed. Temporary wiring between ER components and the control cabinets was installed. A new VLA electromechanical actuator assembly was received and is being readied for testing. It is intended to provide the additional travel needed for reliable operation of the slide valve assembly, a problem encountered with the old design.

The ball valve assembly and a spare were assembled and trial-fitted to the front port of the ER. Some minor mechanical interferences were noted and are being corrected.

All mechanical and electrical components for the bracket and latch assemblies (BLA) have been received, along with the special handling fixture needed for the remote assembly of the BLAs on the elevator and rotator assemblies (ERA). The bracket and latch assemblies were assembled and mounted on the elevator and rotator assemblies. The high current cable assemblies for the BLAs were completed. Electrode assembly (EA) fabrication for both anodes and solid cathodes is now complete. The cathode unit for Port A was assembled and trial fit to the Port A bracket and latch assembly.

All elevator and rotator assemblies have been delivered, along with spare parts. After trial fitting on the electrorefiner main assembly, one unit was mounted on a test stand. Wiring of the amphenol connectors to the elevator rotator assembly (ERA) stepper motors was completed. The 5000 ft of high-temperature shielded triax cable for in-cell service was received. Fitup and

testing of the first cable festoon assembly was completed at the elevator and rotator test stand. Fabrication of cable festoon assemblies (CFA's) for Ports A and C was also completed. Permanent wiring was completed for six electrode assembly (EA) rotation motor-to-bracket harnesses and six EA harness assemblies. Temporary interconnection wiring of the ER control cabinets for Phase I qualification testing is complete except for wiring checkout.

The Phase I qualification testing of the ER heater assembly and support ring, and support structure assembly, primary vessel, cover assembly, power connector assembly, vessel sensors and internal components were completed as of June 3, 1992.

3. Cathode Processor (CP)

Two cathode processors are being constructed--an in-cell unit for Idaho and a prototype unit for Illinois. The two units are shown in Fig. VI.13. As with other major equipment, substantial progress was made, but the funding shortfall curtailed activity in the second half of the year.

Several important pieces of hardware were received during the course of the year. These included the spreader bars for the CP vessel, counterweights for the cover flange assembly and the vessel assembly, the auxiliary heater handling fixture, all electrical connectors for the induction power system, and the coil power cable assembly.

Significant progress was made in installation of components. Wiring on the PLC/interface cabinet for the in-cell apparatus has been completed, inspected and approved. All of the junction boxes for the in-cell cathode processor unit have been wired. Installation of the upper vessel internals (copper shield, furnace liner assembly, and cover flange assembly) was completed. Fig. VI.14 shows the CP internals during assembly. The cathode processor OCS was connected to the equipment; initial operation through the OCS has started. The demonstration of the vessel installation and removal was completed. A coil power cable assembly has been assembled and installed on the in-cell vessel. The thermal radiation shields for the isolation valve assembly were installed. Thermocouple mounting modifications were completed and the thermocouples were

Fig. VI.13. In-cell and Prototype Cathode Processors Undergoing Assembly (ANL Neg. No. 13853)

Fig. VI.14. Remote Cathode Processor (Internals During Cold Assembly) (ANL Neg. No. 12223K)

installed on the auxiliary heater. The vacuum pump was reassembled, checked out, and installed in the in-cell CP unit in preparation for auxiliary heater tests. The oxygen meter for the prototype test facility was received, installed, and checked out.

Progress was also made in component and sub-system testing. A test of the recovery procedure to remove/install a lift actuator on the stand/actuator assembly was successful. Preliminary tests of remote installation of the cover flange bolts and installation of the vessel hold down bolts were completed. Demonstration of the furnace liner assembly to condenser/receiver assembly alignment procedure was successfully completed, assuring that all the associated hardware is within tolerance. The in-cell vessel was evacuated and leak checked. The auxiliary heater power supplies (including spares) were tested at full power. The in-cell auxiliary heater was also tested to full power. The in-cell vacuum pump was tested at load while connected to the in-cell vessel. The vessel was back-filled with argon, and the pump was operated in air without cooling for 10-12 h.

Several needed modifications were also completed. The stand/actuator upper plate has been modified to accommodate the induction power cable assembly. The vessel assembly counterweight was modified, installed, and accepted. The lift resolver assembly rework was completed. Gear box base modifications for the lift resolvers were completed. Modifications on the coil power cable assembly to improve remote handling of the top receptacle were completed. Modifications on the prototype coil power cable assembly were also completed. New cover flange bolts were received and modified. The modified gear box center of gravity was determined and the lift plate modifications completed. All vacuum pump modifications have been completed. Modifications of the prototype test facility to accommodate the vacuum pump are in progress.

The Project also completed several key design milestones for the cathode processor. Design modifications for relocating the resolver to the outboard gear box were reviewed and approved. The induction power cable assembly design was completed, as was design of the valve carrier assembly installation handle. The design and checking of the receiver crucible handling device has been completed. Design of the lifting handle was modified to locate the center of gravity of the coil power cable assembly.

Shipment of the in-cell cathode processor to Idaho is anticipated in mid-FY 1993. Completion of the prototype unit will follow.

4. Injection Casting Furnace

Phase I remote qualification of the injection casting furnace began in FY 1991. Mechanical and electrical assembly qualification were completed early in FY 1992. Remote qualification of the casting furnace hardware and software is complete, and the system has been disassembled and moved out of the qual shop (mockup area) to an interim location prior to installation in the cell. A few modifications were required. For example, cooling holes were added to near the bottom of the pallet assembly to increase coolant flow near the bottom ends of the molds. The injection casting furnace is shown in Fig. VI.15.

Fig. VI.15. Injection Casting Furnace Ready for Installation in the Argon Cell
(ANL Neg. No. 103-CC5752)

The injection casting furnace is ready for Argon Cell installation as soon as facility modifications have reached the appropriate stage.

5. Fuel Pin Processor

The fuel pin processor, shown in Fig. VI.16, has been completed and is ready for remote qualification in the mockup area as soon as space is available. Functional testing (Phase I qualification) was successfully concluded. The Phase II qualification plan has been completed. Phase II software checkout has begun.

6. Vertical Assembler/Disassembler (VAD)

Remote qualification of the vertical assembler/disassembler has been completed. In FY 1992 emphasis was on qualification of the forced cooling system

Fig. VI.16. Fuel Pin Processor During Assembly and Checkout in the Engineering Laboratory (ANL Neg. No. 103-U12765)

for the apparatus. The flow test system used with the VAD to check for flow blockage after assembly was installed.

7. Fuel Element Welder and Fuel Element Settler

Substantial progress was made on the fuel element welder. The apparatus has been assembled and Phase I qualification is almost complete. Much of the effort has gone into the control software.

Communication links between the OCS, PLC, and the equipment were installed. Electrical, mechanical, and software problems have been corrected. Motor acceleration and deceleration parameters for the X-Y-Z robot have been established. The checkout of the switches and resolvers was completed. The X-Y-Z robot homing routine is complete. The X-Y-Z robot has shown that it can consistently pick-up and insert end plugs into the element jackets. The vision system has been connected and checked. PLC routines were developed for the vision system and the xenon tag gas apparatus. New lights and background were installed for the vision system to optimize picture resolution. The rotational drive PLC routine was completed. Modifications were made to the compressor assembly and the xenon tag gas sub-system due to problems discovered during qualification.

The welder electrical cabinet is complete and undergoing continuity checks. Manual operation of the system using the OCS has been demonstrated successfully. The PLC routine for semiautomatic mode was completed and checked, and testing of automatic operation is underway. OCS and PLC communications are being checked and software interlocks are being incorporated into the PLC ladder logic.

The element settler is designed to assure that fuel is in the proper position within the fuel pin, prior to insertion into the reactor. The settler will occupy a space immediately adjacent to the element welder. Hardware for the element settler has been completed. The unit is being assembled in the mockup area for remote qualification.

A short documentary videotape of the element welder has been produced. A photograph of the element welder in the FCF mockup area is shown in Fig. VI.17.

8. Fuel Element Inspection

Initial qualification of the X-ray system by the vendor was successfully completed. A video tape detailing the X-ray equipment, including real time fuel element examinations, was completed. The X-ray system was then inspected at the vendor shop and found to be acceptable. The entire system was shipped to ANL-W.

For initial qualification of the system, the X-ray system was temporarily installed in the Building 772 low-bay X-ray cell, as shown in Fig. VI.18. The sodium meniscus in a dummy fuel element was located and quite vividly shown in real time. The interface between the bottom of the fuel pin and element

Fig. VI.17. Element Welder During Assembly and Checkout in the FCF Mockup Area (ANL Neg. No. 103-U12418)

Fig. VI.18. X-ray Imaging System for Element Inspection Station Undergoing Development Testing in ITF (ANL Neg. No. 103-12884)

jacket was easily located and examined. During the vendor checkout the shutters were found to have sustained minor damage prior to reaching ANL-W. The shutters were returned to the vendor, repaired, and returned to ANL-W.

The X-ray tube vendor has been contacted with respect to the 125KV high tension line that leads from the tube head (to be located in cell) to the high power supply (to be located outside of the cell). The two major concerns of this line is the ability to splice it and its radiation resistance. Work proceeded on X-ray shielding and design of a 125 kV high-tension cable disconnect.

Initial shielding calculations were completed for the X-ray system. Significant background illumination in the image intensifier occurs at 2 R/h. Therefore, shielding is being designed to decrease the air cell background to no more than 200 mR/hr.

C. Process Control and Accountability

Substantial progress was made in computer software development for process control and material accountability, but as with other major FCF Project tasks, inadequate Project funding sharply reduced the effort in the second half of the fiscal year. Several major design reviews were held and significant progress was made in installing cabinet wiring and network cable links. Procurement of control systems hardware was completed. Conversion to a new release of the control system software (Paragon) was also completed.

1. Operator Control Station (OCS)/Programmable Logic Controller (PLC) Software

Phased final design reviews were held for the electrorefiner OCS and PLC software systems, including a Phase II final design review for the cover gas system OCS/PLC software. A final design review was also held for the cathode processor OCS. During March, a Phase II final design review was held for the facility control software. All items were approved for coding except the cell pressure control system and the argon recirculation and cooling system, which required further definition from the equipment designers. The argon system control including the argon cell pressure control is being redesigned for control on pressure signals rather than temperature. The small transfer lock (STL) control software was tested informally. Control software for the transfer tunnel carts was informally tested with temporary field wiring. The carts were successfully operated using the programmable logic controller and operator control station.

The casting furnace assembly testing was completed and the software (See Fig. VI.19) was placed under configuration control. Formal out-of-cell testing by independent personnel resulted in requests for several modifications, which have been completed.

The pin processor control software has completed its informal testing. The coding of the element welder end plug insertion software was completed and successfully demonstrated. A preliminary design review for the element inspection software was held and final design was started.

Fig. VI.19. PLC/OCS Screen for Injection Casting Furnace

2. Mass Tracking System (MTG) Software

The WEIGH task, which supports database processes associated with balance operations, is now running. Terminal sessions and MTG tasks have been written to permit manual entry of new container identities and weights into the MTG database. This software was successfully tested using the production MTG servers to manage communication and control. A "replay" option has been added to the task that models the casting operation; this feature is important to the procedure that will be developed to "tune" information in the MTG database using chemical analysis of casting samples. The operations terminal session was extended to handle the assignment of SPM numbers to items in the MTG System database.

Work continued on the database of subassembly and element data that will eventually provide input both to EBR-II physics analysis and to the Mass Tracking System. The database has been reconstructed to include date information that has not previously been available. The error checking and audit information edits done earlier using ad hoc software have been consolidated and formalized.

The MTG task intended to support miscellaneous queries is now running with its first two options: OCS logon support and a request for information on

the location of items within the FCF. The MTG task that supports container-to-container transfers has been modified to accommodate situations where there are either no specific source or specific destination containers, and also to accommodate a generic type of transfer in which there is a single source container, but multiple destination containers. Crucible unloading is an example of the latter type of operation. Terminal sessions have been extended to define containers which are magazines and to provide a means for manual entry of the composition of relatively simple items, such as feed material. The initial zone-to-zone transfer software and the balance software was also completed for the operator control stations and mass tracking system. An OCS computer screen for a zone-to-zone transfer is shown in Fig. VI.20.

The MTG task that supports casting has been expanded to include the mold-pallet loading and unloading phases and to include adjustments to item masses that can be calculated once certain destination container weights are known.

Wylbur execs have been written for the IBM/MVS system that provide a user interface for generating a variety of reports and edits from the Physics Analysis Database. Code has been written that collects data on individual spent subassemblies for transmission to the MTG's database.

Fig. VI.20. Material Transfer Path OCS Screen for the Mass Tracking System

D. Preparations for FCF Operation

Throughout the year, facility operations personnel were fully involved with activities necessary for startup. The staffing level is up to eight systems engineers, six chief technicians, and four operations technicians. Development of operating procedures, maintenance instructions, and the FCF training program has been focused on startup of depleted uranium operations (Phase 2 startup) by April 1993. Engineering and technical support personnel worked on reactivating existing facility systems and refurbishing and installing remote handling equipment.

The Final Safety Analysis Report (FSAR) was sent to DOE in August 1991. Approximately 400 questions eventually were asked by DOE and their consultants. The number of responses that have been accepted totals around 250. The final responses are expected to be resolved by mid-FY 1993, after which Rev. 1 of the FSAR will be issued. Two major issues were developing acceptable dose guidelines and seismic analysis of the FCF building.

The Technical Safety Requirements (TSRs) document was completed and submitted to the Nuclear Facilities Safety Committee (NFSC) in May. Over 100 questions were received, all of which have now been answered. The Laboratory expects to submit the document to DOE for review and approval in the second quarter of FY 1993.

The Criticality Hazards Control Statement (CHCS) was submitted to the NFSC March 5, 1992. This document employed an innovative approach to demonstrating criticality safety. For each major process step, a square matrix of unlikely and very unlikely events was developed. Each significant physical characteristic was represented. Every combination of two events was modeled and calculated, with all remaining physical parameters being represented by nominal values unless physically constrained otherwise. Probabilities were estimated using a simplified probabilistic risk assessment approach. Because there is little experience with pyrometallurgical processes, this approach eliminated the overly restrictive and hard-to-justify normal technique of trying to find a single bounding event for each process step. The NFSC generated approximately 200 questions, all of which have been satisfactorily answered. Submission of the CHCS to DOE for review will occur during FY 1993.

Preparation of a detailed plan for phased facility startup is ongoing. The plan will cover preparations for depleted uranium processing, cold Pu processing and irradiated fuel processing with emphasis on what systems are required for each and what review and approval levels are required. The approach being taken is to break the remaining work into discrete packages which will be reviewed and approved as they are completed.

The stack monitoring QA Plan and reporting procedures for permit compliance were transmitted to the State of Idaho through DOE. An Idaho Air Quality Permit requirement to notify the State of design changes was met in January. The Project received notification, via a documented telecon between DOE-ID and the State Department of Environmental Quality, indicating that the State of Idaho does not consider changes in release estimates to be significant or to constitute a major modification. Therefore, a new permit is not required.

The Environmental Assessment (EA) was reviewed to determine whether the facility modifications, including process equipment design, conflict in any way with statements or commitments made in the EA. No such conflicts were identified.

E. Preparations for Demonstration Experiments

As part of the start-up preparation for the Fuel Cycle Facility, operating strategies for the pyroprocessing demonstration have been examined. The goal of this work is to develop a reprocessing campaign that provides enough fuel to meet the needs of EBR-II and that provides the data needed for the further development of the Integral Fast Reactor's pyrochemical process.

As part of the strategy development, the integration of all pieces of equipment in FCF has been considered, but special emphasis has been placed on the operation of the electrolyzer. The start-up phase of the ER includes loading the vessel with salt and cadmium and performing pretreatment operations. The plan for initial operations is to employ depleted uranium, followed by plutonium and depleted uranium to test the equipment and operating procedures prior to the addition of irradiated material to the system. At the end of start-up, the plutonium to uranium ratio in the salt will be appropriate for normal operations.

After the ER start-up phase is completed according to the projected strategy, a single 20 kg batch of irradiated fuel will be processed in the ER to increase the ^{235}U enrichment of the ER products to greater than 50%. The normal operating cycle will then begin to recover reprocessed fuel for EBR-II.

Because only a limited amount of irradiated ternary fuel is now available, simulated material will initially be used to demonstrate the pyrochemical process. The simulated irradiated ternary fuel will consist of irradiated binary fuel mixed with cold plutonium metal. With this loading, the recovery of the transuranic elements in the presence of high concentrations of fission products using liquid-cadmium cathodes will be demonstrated thoroughly. The limited amount of irradiated ternary fuel will also be reprocessed at the appropriate time during the operating campaign.

For the proposed first campaign, the typical operating cycle will consist of three 20 kg loadings of the simulated-ternary fuel during which only solid cathodes will be recovered. These three operations will be followed by the recovery of four liquid-cadmium cathodes. This recovered plutonium and uranium will be used for the production of fuel for EBR-II.

The operating strategy for the first campaign, including start-up, has been modeled using the PYRO code. The results of this work indicate that during the first campaign enough fuel can be reprocessed to refuel completely the EBR-II core. Work is continuing to optimize the operating strategy and to use the results to make an estimate of the samples required during the first campaign.

The PYRO simulation is also being applied to the ER waste operations after the first operating campaign. This model predicts that the ER vessel alone can be used to process the bulk of the salt after the campaign. Portions of the ER contents will be processed using more advanced equipment and techniques as part of the waste demonstration. Work in this area is continuing.

The specification for the process chemicals has been revised. Preliminary indications are that all chemicals will be delivered to meet the present Project schedule.

VII. LIQUID METAL REACTOR TECHNOLOGY RESEARCH AND DEVELOPMENT

A. Seismic Qualification of Unanchored Process Equipment in the Fuel Cycle Facility

Seismic qualification of equipment within the FCF argon cell presented a unique technical challenge. The conventional solution to seismic loading of equipment is to anchor the equipment. It was recognized early in the project that experience with the remote operation of newly-developed equipment in the limited space available would likely lead to requirements to shift the equipment locations. Anchorage of new equipment after the cell was sealed would also be very difficult. Moreover, loads transmitted by the equipment during a seismic event to the cell liner would present an additional challenge to the liner. The cell liner is the primary confinement of radioactive material, and breach of this confinement during an earthquake is the most severe accident scenario. For these reasons the project chose to bypass conventional anchorage of equipment in the argon cell in favor of engineered seismic qualification of equipment in an unanchored state.

The seismic concerns for equipment consist of:

- (1) Overturning, rocking or sliding of equipment resulting in damage to the equipment, its connecting cables, or adjacent equipment and structures.
- (2) Stresses and displacements in the equipment resulting in loss of function either during or following the seismic event.

The latter issues must be addressed by both anchored and unanchored equipment. These issues are not normally very severe for equipment because equipment is usually designed to displacement criteria and the stresses are quite small. For unanchored equipment these issues are somewhat less of a concern because sliding acts as a seismic isolation feature reducing the accelerations seen by the equipment. However, impact stresses associated with stable rocking or sliding can be of concern for unanchored equipment.

Anchored equipment addresses the first issues directly by preventing overturning, rocking and sliding. Unanchored equipment must either assure by design and analysis that the phenomena do not occur or address the effect of these phenomena.

The project approach to the concerns of overturning, rocking and sliding are:

- (1) Assure that equipment will not overturn with sufficient controlled margin to achieve the project performance goal of 10^{-5} annual probability of exceedence;
- (2) Assure that equipment either will not rock or, if rocking is expected under DBE conditions, the expected rocking is considered in the design of the equipment using normal structural design margins to achieve the performance goal; and
- (3) Quantify the maximum expected displacement and velocity due to sliding and include this design motion in the equipment design criteria.

1. Overturning

Overturning is a catastrophic response to the horizontal and vertical accelerations induced by the seismic floor motions. It can be prevented by providing a sufficiently broad base compared to the height of the equipment center of gravity. This section describes the analyses used to quantify the minimum ratio of the equipment base dimension to the height of the center of gravity needed to assure that overturning would not occur.

The initial studies considered a rigid body model of the equipment and used static analysis, linear response spectrum analysis and non-linear time-history analysis in conjunction with the argon-cell-floor seismic response spectra and time histories. The rocking motion of a rigid body on a flat surface is a strongly nonlinear problem because of the impacts which occur when the flat base of the equipment returns to strike the floor. The linearized model used in

the response spectrum analysis avoided this singularity by substituting a linear restoring force. The stiffness of the restoring force was chosen so that the elastic work done by the linear restoring force equaled the increase in the gravitational energy required to tip the center of gravity of the rigid body to a point directly over the tipping axis. This gross simplification only has validity for motions which are close to overturning.

The results of this linear response spectrum analysis can be interpreted as a predicted stability limit for overturning. The limit depends on two geometric properties of the rigid body, the height of the center of mass of the rigid body above its base, h , and the ratio of the base radius to h . This latter stability parameter is termed α ; larger values of α make the equipment more stable. The analysis also shows that, for fixed α , increasing the size of the equipment, i.e., increasing h , increases equipment stability.

The exact nonlinear equations can be integrated numerically to simulate response to floor accelerations. This requires an impact assumption when the flat base of the rigid body strikes the flat floor during rocking. Using a conservative assumption that no energy is lost in the impact, 600 cases were evaluated in a parametric study. The results of this study confirmed the adequacy of the linear model in predicting the stability limit. Figure VII.1 shows some of these cases plotted in the h - α parameter plane with open circles showing stable solutions and solid circles showing unstable simulations. The stability limit predicted by the linearized rocking model is shown as a continuous line.

Another approach to the overturning stability limit is to assume that the equipment is deformable and that it builds up energy in its deformation modes in response to the floor motion. Having reached its peak response energy, this energy is assumed to be available to overcome the stabilizing gravity potential represented by the body geometry. Such a linear response spectrum analysis indicates that if the fundamental frequency of deformation is above 3 Hz, the total deformation energy is not more than the energy in the linear tipping model. Thus the stability criteria associated with this analysis is nearly identical to that shown for the linear response spectrum tipping model.

Fig. VII.1. Stability Criteria from Overturning Analyses and Design Criteria for Unanchored Equipment in the Air and Argon Cells

The basic approach used to address seismic hazards requires a controlled margin to ensure that the equipment will not overturn in a more-severe less-probable seismic event than the design basis earthquake. This is normally achieved in design using a factor of safety of about 4 in conjunction with an abnormal event multiplier of 1.7. The result is a factor of $4/1.7 = 2.35$. In the overturning analysis this factor was applied to the stability criteria predicted by the various analyses by increasing the parameter α by 2.35. Figure VII.1 shows the stability limit given by the deformable body analysis increased by this factor.

Finally, the figure shows the FCF design criteria for overturning of unanchored equipment in the air and argon cells. The equipment must have a center of gravity greater than 6 in. and a stability parameter, α , greater than 0.52 for $h > 20$ in. In the region $6 < h < 20$ in., the minimum α decreases linearly with h . In addition, the equipment is required to have a natural frequency above 3 Hz.

2. Rocking

The overturning criteria has sufficient margin to assure that rigid equipment will not overturn with an exceedence probability of less than 10^{-5} per year. However, deformable equipment could develop sufficient energy to cause tipping (but not overturning). This stable rocking motion has the potential for damaging the equipment base and must be considered in the equipment design. A first attempt to address this issue, based on energy considerations, resulted in criteria which were too restrictive for design. In order to remove the excessive conservatism in the energy based criteria, a special purpose computer code was written to analyze stable rocking motions of deformable equipment. The code integrates the coupled equations of rigid body rocking and equipment deformation using the argon cell floor accelerations and the equipment-specific deformation parameters as input. It calculates if rocking will occur, and if so, the number and magnitude of the impacts. Initial applications of this code indicate that equipment rocking will not be a problem for FCF equipment.

3. Sliding

The potential for sliding can be inferred from the response spectra. In the limit of no frictional resistance to sliding, the equipment stays fixed in space while the floor moves. The maximum relative displacement between the floor and the equipment is given by the low frequency portion of the displacement spectra which is 5.5 in. For sufficiently large friction, the equipment does not slide and the relative motion is zero. For intermediate values of friction, rigid equipment will slide with maximum displacement and velocity between these values. If the equipment is not rigid, there is potentially an interaction between the equipment deformation and the sliding. If the deformation modes are symmetric and/or the equipment frequency is sufficiently high compared to the frequency content of the floor motion, this interaction will have only a small effect on the relative position and velocity of the equipment. However, if the equipment frequency is sufficiently low and the deformation mode is not symmetric about a vertical axis, a "walking" effect can occur and the relative displacement can be substantially greater than that of rigid equipment. This walking effect occurs because during one half of a vibration cycle the floor normal reaction is increased; this increases the frictional resistance and limits horizontal motion

in one direction. During the second half cycle the normal force is reduced; this reduces the frictional resistance and allows more horizontal motion in the opposite direction.

To quantify this potential walking phenomena, a mathematical model of a deformable sliding body was developed and parametric studies were conducted using the argon cell floor seismic time histories. The effects of friction coefficient, deformable mass participation, deformation frequency, and angle of asymmetry of the deformation were studied. Numerous cases (575) were calculated with the following conclusions:

- (1) The qualitative conclusions for rigid and symmetrically deformable bodies described above were confirmed;
- (2) Walking can occur and is most severe for unsymmetric, low-frequency, deformation modes with large participating mass and low, but nonzero, friction; and
- (3) If the deformation frequency is above 3 Hz, the maximum relative displacement is 6 in. and the maximum relative velocity is 13 in./sec regardless of the mass participation factor or the coefficient of friction.

As a result of these studies the sliding criteria used in the design of FCF equipment are: frequency greater than 3 Hz, sliding radius of 6 in., and sliding velocity of 13 in./sec. The controlled margin associated with these criteria is contained in the design margins for assessing the loads and deformations associated with impacts.

B. Sloshing Behavior of Electrefiner Containing Two Liquids During A Design Basis Earthquake

The electrefiner is an essential element in the pyrometallurgical processing of the IFR fuels. The main vessel of the electrefiner is an upright circular cylindrical tank. It contains two liquid solutions--liquid cadmium in the lower portion of the tank, and molten LiCl and KCl salt in the upper portion

of the tank. When the tank is excited dynamically during an earthquake, it is necessary to know: (1) How much is the sloshing wave height? (2) Does the freeboard provide enough space to prevent the liquid from impacting the roof? and (3) Will the sloshing motion at the interface between cadmium and LiCl and KCl salt become so large that it may expose cadmium to the atmosphere in the argon cell? To answer these questions, a study was performed at ANL to examine the sloshing response in a tank containing two liquids. Since no solutions to this problem existed in the literature, both analytical and numerical approaches were used to solve this problem so that the results obtained by the two approaches can be cross checked. Only the highlights of this study are presented herein.

The following features of the electrorefiner are used in the study:

Tank	Height	31 in.
	Radius	20 in.
	Freeboard	13 in.
Liquid Cadmium	Density	535 lb/ft ³
	Height	6 in.
LiCl and KCl	Density	138 lb/ft ³
	Height	12 in.

The electrorefiner vessel is modeled, shown in Fig. VII.2, as an upright, circular cylindrical tank of radius R which is filled with two liquids to a total height H . The lower portion liquid, identified as Liquid I, has the heavier mass density, ρ_1 and the upper portion liquid, identified as Liquid II, has the lighter mass density. The heights of Liquid I and II are H_1 and H_2 , respectively. Both liquids are considered to be incompressible and inviscid. The motion experienced by the tank is considered to be a horizontal earthquake excitation, the characteristics of which have been established for the FCF facility. Since Veletsos has shown that the sloshing response is insensitive to the flexibility of the tank wall,¹ the tank wall is assumed to be rigid.

The response quantities examined include the maximum sloshing wave height at the free surface of the liquid system, the associated natural frequencies and the related vertical displacement at the level of the interface of two liquids. The natural frequencies are used for the computation of the sloshing wave height

Fig. VII.2. Mathematical Model

which in turn is used for the determination of the required freeboard. The vertical displacement at the interface is used to examine the possibility of separation of two liquids. It should be noted that the analysis presented herein is based on the linear theory. It may be argued that the separation of two liquids is a nonlinear problem, and, therefore, the linear theory can not be applied. However, the purpose of this analysis is not to study the separation of two liquids quantitatively but only to study the possibility of the separation qualitatively. This goal can be achieved by the application of the linear theory which provides information about the incipience of any separation. The separation is possible only when the vertical displacement at the level of the interface of two liquids approaches the free surface of the top liquid, see the sketch shown in Fig. VII.3. The interface displacement can be calculated by using the linear theory.

Both analytical and numerical (finite element) methods are employed in the analysis so that the solutions obtained by two methods can be cross checked. Both methods were developed at ANL. In the analytical approach, the governing

Fig. VII.3. Sketch of Sloshing Response
(Separation is possible only when $\delta_1 + \delta_2 \geq H_2$)

equations for the liquid field are solved exactly. For the numerical approach, the finite element computer code, FLUSTR-ANL² is used to perform the analysis.

1. Analytical Approach

The hydrodynamic pressures induced in Liquids I and II are the solutions to Laplace's equations and appropriate boundary conditions. The sloshing wave height is determined from the argument that the hydrodynamic pressure at the mean liquid surface is equal to the weight of the liquid column above. From the analysis it is shown that the maximum sloshing wave height is given by a series form which involves an infinite number of terms, each term of which represents the contribution of the various sloshing modes of vibration of the liquid. Considering only the first three modes, the maximum wave height, denoted by $d_{\max}$, can be calculated from the following:

$$d_{\max} = \left[1.04 \frac{A_1(t)}{g} + 0.076 \frac{A_2(t)}{g} + 0.028 \frac{A_3(t)}{g} \right] R$$

where g is the gravitational acceleration, and $A_i(t)$, $i=1, 2$ and 3 are the pseudoacceleration response functions for an undamped single-degree-of-freedom system that has a circular natural frequency equal to that of the i th sloshing mode of vibration of the liquid and which is excited by the acceleration. The first three natural frequencies of the sloshing mode of vibration of the liquid system are found to be 0.897 Hz, 1.614 Hz, and 2.044 Hz.

2. Numerical Approach

The finite element code, FLUSTR-ANL, is used to perform numerical analysis. The finite element model for the tank is shown in Fig. VII.4. It consists of 660 fluid elements, 264 for Liquid I and 396 for Liquid II. Thin fluid elements (174) along tank wall and base are used to capture the behavior of the liquid at these interfaces. Nodes 131, 144 and 157 are at the surface of Liquid II, and nodes 689, 702 and 715 are located at the interface of two liquids. The validity of the finite-element mesh used for the tank-liquid system is confirmed by comparing the sloshing wave height obtained from the computer code with that obtained from the analytical analysis. After the agreement between two solutions was reached, the vertical displacement at the interface

Fig. VII.4. Finite Element Mesh

of two liquids obtained from the computer code was used to assess the possibility of separation of two liquids. Note that the analytical solution for the vertical displacement at the interface of two liquids was not pursued at the time of the study due to the troublesome and tedious mathematic derivations. This solution, however, may be pursued in the future.

3. Presentation of Data

The input excitation is a 10-sec argon cell floor acceleration time history established for the Fuel Cycle Facility. This time history is shown in Fig. VII.5. It has a maximum acceleration of 0.31 g which occurs at $t = 4.64$ sec. The maximum sloshing wave height computed based on the analytical approach is 7.9 in. which is less than the freeboard provided, 13 in. The maximum sloshing wave height determined by the FLUSTR-ANL computer code is 8.4 in. which differs from the analytical result by only 0.5 in. This error of 6% is well within required engineering accuracy. Also, it was observed that the timewise variations of the response curves obtained from the two methods are in good agreement. Thus, the accuracy of the computer code as well as the finite element mesh used is validated. The maximum vertical displacement at the level of the

Fig. VII.5. Input Acceleration

interface of two liquids is found to be 1.82 in. During the sloshing motion under the excitation considered, the worst scenario would be the free surface moving downward while the interface moves upward. If this worst scenario happens, which is very unlikely, the total displacement of the top surface relative to the interface will be $1.82 + 8.4 = 10.22$ in. which is still less than the thickness of Liquid II, 12 in.; therefore, exposing liquid cadmium to the atmosphere in the cell is not possible.

4. Conclusions

The following conclusions are drawn from the study:

- (1) The 13 in. freeboard provided is sufficient to avoid impact on the roof during sloshing response to the given 0.31 g floor acceleration.
- (2) The possibility of separation of liquid salt from liquid cadmium is unlikely; therefore, the liquid cadmium would not be exposed to the Fuel Cycle Facility Argon Cell atmosphere.

C. ALMR Electromagnetic Pump Development

The current ALMR concept being developed by General Electric requires that several large electromagnetic (EM) pumps operate submerged in the reactor vessel to pump the primary system sodium. These pumps are designed to operate without an active cooling system relying instead on the pumped and surrounding sodium to remove internally generated heat. The electrical windings of these self-cooled annular linear induction pumps will reach temperatures near (538°C) 1000°F. Conventional high voltage coil winding insulation is limited to about 200°C. An insulation system for an EM pump is required that can operate continuously at 538°C for 40 years at 1000 volts AC, and be commercially available. These insulation systems are very sensitive to total insulation thickness, voltage gradient, and mechanical effects causing cracking. A program to develop and test this insulation system has been in progress since 1985. The program has consisted of the following components:

- (a) Small bar sample tests for materials evaluations,
- (b) Tests on prototypic EM pump coils, and
- (c) Prototypic testing of a full diameter-1/4 length EM pump.

Initial work was directed at screening of candidate materials by testing small bar samples at operating temperature and voltage. As a result of the bar sample tests several promising materials including woven Fiberglas reinforced mica tape and woven ceramic fiber tape were selected. The glass/mica tape was selected for further testing due to space and cost reasons. A program was initiated to determine the estimated lifetime of the glass/mica tape insulation at 538°C by accelerating the chemical aging of bar samples with the application of over-temperature at voltage. The lifetime at 538°C, the normal operating temperature, can be determined by running selected higher temperature accelerated aging tests to failure and using the Arrhenius Principle to predict the lifetime at 538°C.

The thickness of the insulation has been established at about 1.9 mm (0.075 in.). This insulation thickness is a trade-off between an acceptably low voltage gradient and adequate heat transfer from the high temperature pump windings to the cooler magnetic iron stator. Generally mica has a poor thermal conductivity. Both bonded and unbonded glass/mica tape layers have been studied. Binders such as mono-aluminum phosphate and ceramic cements have shown acceptable electrical performance. These binders are applied to the tape by brush as the tape is wrapped on the bar sample with up to 15 tape layers on straight test bars of copper. Due to the large water content of these binders care must be taken to slowly heat the insulated bar to remove the water before applying significant voltage to the samples. After this curing operation, samples are tested at 680-750°C and a voltage of 1500-2500 volts at 60 Hz applied between the copper bar and an external electrode. Data on the leakage current through the insulation is taken and the time-to-failure recorded. The times-to-failure have extended from a few hundred hours to about 50,000 h. Based upon the high temperature accelerated aging tests of the bar samples the estimated lifetime at the normal operating temperature (538°C) is estimated to be in excess of 100 years.

This insulating technique was next applied to prototypic EM pump coils having shown the feasibility of glass/mica tape insulation of bar samples. Four small ovens are used for testing bar samples and four large ovens are used for testing the large prototypic pump coils. The full-size EM pump coils are prototypic of planned ALMR pumps and are about 58.4 cm (23 in.) OD, 40.6 cm (16 in.) ID, and 2.54 cm (1.0 in.) thick. A series of coils were insulated using mono-aluminum phosphate and ceramic cement binders. These coils were tested at both ultra-high temperature (730°C) and at near normal operating temperature (550°C). The results showed that both binders tended to crack at the ultra-high temperature but performed satisfactorily at normal temperature.

Oven testing of both bar samples and coils addressed the chemical effects of high temperature and voltage on the materials but did not address the mechanical stresses imposed on the materials in an actual EM pump. For this reason the General Electric Company designed and built a test article EM pump which would provide the mechanical stress conditions expected in an ALMR. The test article EM pump was 1/4 the length of a prototypic pump but of full diameter. The pump was tested in the small sodium test facility constructed of 5.08 cm (2 in.) diameter pipe at ANL-E. The pump operated at prototypic current density and magnetic flux density; subsequently, each of the 24 coils in the pump was subjected to prototypic electrical, thermal, and mechanical stresses. Unlike conventional annular linear induction pumps, the coils of this pump can be easily replaced. The initial coils were insulated with unbonded glass/mica tape. A test of 2047 h between August 1988 and March 1989 showed that this unbonded insulation system was unsatisfactory due to shrinkage resulting in loosening of the coils in the pump stator and subsequent coil vibration, insulation abrasion, and coil failure. Shrinkage was caused by the loss of the Fiberglas woven backing for the mica at temperatures higher than normally allowable for Fiberglas. This pump was rebuilt with a second set of coils. These coils were insulated at ANL with glass/mica tape bonded with a ceramic cement which does not shrink at high temperature. Earlier ANL oven tests had confirmed the good electrical performance and stability of the ceramic cement bonded coils. The ceramic cement was applied to fill all the voids in the open Fiberglas woven fabric backing of the tape. When cured and operated at high temperature the Fiberglas deteriorates but the multilayered tape structure remains intact due to the solid ceramic cement. Other improvements were made to the pump including

additional ground fault insulation and better radial and axial clamping arrangements to prevent stator vibrations. These pump modifications were performed by ANL with GE consultation. The test of the modified pump began in July 1989 on a 24-h per day, 5-day per week schedule. With the exception of two visual examinations of the stator, this schedule was continued through March 1990 with a total accumulation of 3611 h of operation. There was no evidence of any electrical fault or malfunction during this time. The ground leakage current, an important measure of coil performance, did not vary indicating no observable aging effects. Also, visual examinations showed no significant deterioration of the insulation. Some loosening of the radial clamping bands was observed but pump vibrations were minimal. The weekly schedule of pump operation required a temperature cycle once per week between 205 and 482°C (400 and 900°F) with a total of 33 thermal cycles over the testing period. The severe thermal cycling (33 cycles) represented a very significant mechanical stressing of the pump. These tests have successfully demonstrated the feasibility of fully developing a high temperature electromagnetic pump without an active cooling system.

The accelerated aging tests employing the small bar samples and the full-size coil test have continued through Fiscal Year 1992. The ovens in which the bar samples and pump coils are being tested are shown in Fig. VII.6. The results of the accelerated aging tests with the small bar samples are shown in Table VII.1. The oven internal air temperature and continuous 60 Hz voltage impressed across the insulation are illustrated. The original loadings of these ovens included samples which were unbonded (dry), bonded with Secon 5 ceramic cement (Secon), and bonded with mono-aluminum phosphate (MAP). The numbers of bar samples which remain on test, having not experienced failure, are presented. No bar samples failed in Ovens 1 and 2 during Fiscal Year 1992. Extrapolation of these data for the bonded bar samples in Ovens 1 and 2 indicate an expected time-to-failure at 538°C of over 1×10^6 h. Oven 3 has been taken out of service. The Oven 4 Secon 5 bonded samples being tested at 2500 volts at 730°C had many early failures but the higher test voltage prevents using this data in combination with 1500-volt data to make lifetime predictions. Also, since the data is all from samples at one temperature it cannot by itself allow an Arrhenius prediction of the insulation lifetime. These 2500-volt samples at 730°C were failing at a faster rate than similar samples at 1500 volts and 730°C. This data supports the view that the ALMR pump voltage should be kept to a minimum.

Fig. VII.6. Ovens for Testing Bar Samples and Pump Coils
(ANL Neg. No. 6813)

TABLE VII.1. Accelerated Aging Tests with Small Bar Samples

Oven Number	Temp., °C	Continuous Voltage	Number & Types of Samples on Test		Total Hours
			Original	Remaining	
1	680	1500	5 dry & 5 Secon	3 dry & 5 Secon	33,670
2	730	1500	5 dry & 5 Secon	0 dry & 5 Secon	33,868
3	680	1500	12 MAP	0 MAP	*
4	730	2500	20 Secon	1 Secon	19,783

*The oven 3 heater control failed on 7-1 91 after 49,890 h of testing.
Three MAP samples remained on test at the time of heater control failure.

On October 12, 1991 (Saturday), the Oven 4 temperature controls failed, resulting in a loss of power to the oven. The oven temperature had dropped from the nominal 730°C to 60°C on October 14 (Monday). During the power outage, three of the nine Secon 5 bar samples faulted, shutting off the 2500 volt supply. On October 14, the oven temperature controls were restarted after repairs were completed. Heat-up back to 730°C was done at zero voltage. After Oven 4 reached a stable temperature of 730°C, the voltage was increased gradually from 0 to 2500 volts. During the voltage increase, four of the remaining six bar samples faulted at voltages between 2000 and 2500 volts. Oven 4 was brought to 2500 volts with two of the Secon 5 bar samples remaining on test. These failures were most likely caused by cracks in the mica tape which resulted from the rapid drop in temperature. Table VII.2 summarizes the bar sample failure data during this period. Subsequent to the Oven 4 temperature control failure, on December 3, 1992, another bar sample (No. 65) in Oven 4 faulted after 13,523 h. This leaves one bar sample remaining under test conditions of 730°C and 2500 volts.

Examination of the bar sample data in Table VII.1 for the Secon insulation system shows that the Secon samples on test in Ovens 1 and 2 have experienced no failures at 1500 volts and temperatures of 680 and 730°C, respectively. The non-prototypic test results of Oven 4 (2500 volts and 730°C) prior to the Oven 4 temperature controls failure and during the Oven 4 recovery strongly supports the

TABLE VII.2. Summary of Bar Sample Failure Data of Oven 4

Sample No.	Failure Voltage	Temp., °C	Comment
61	2500	<730	Failed during cooling
67	2500	<730	Failed during cooling
66	2500	<730	Failed during cooling
70	2000	730	Failed during voltage increase
63	2100	730	Failed during voltage increase
76	2200	730	Failed during voltage increase
79	2350	730	Failed during voltage increase

position that the ALMR pump voltage should be kept to a minimum--certainly below 200 volts, but preferably at or below 1500 volts. Tests on the prototypic test article EM pump were conducted at a 24-h per day, 5-day per week schedule. This weekly schedule of pump operation required a temperature cycle between 205 and 482°C for shutdown and startup. The nominal operating temperature of the Secon insulation system is 538°C. The voltage during test article operation was 370 volts. No failure or degraded insulation characteristics were observed during these test article operations. These experimental results support the earlier findings from accelerated aging tests of the bar samples, that the estimated lifetime at normal operating conditions is expected to be in excess of 100 years.

High temperature and voltage tests on full-size (23-in. dia.) EM pump coils have continued through Fiscal Year 1992 to determine both the electrical and mechanical performance of prototypic coils, individually. Six full-size coils continued on test and no failures were experienced during Fiscal Year 1992. Table VII.3 summarizes the test results with the six full-size coils. All coils exhibited steady leakage current during this period indicating acceptable electrical performance.

TABLE VII.3. High Temperature and Voltage Tests on Full-size Coils

Oven Number	Temp., °C	Continuous Voltage	Number & Types of Samples on Test	Total Hours
5	550	1500	Coils 17 & 18 - Secon	32,612
6	550	1500	Coil 16 - MAP	42,594
7	500	1500	Coil B - Secon	30,750
8	500	1500	Coils 19 & 20 - Secon	31,398

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VIII. LIST OF PUBLICATIONS

The technical papers on the IFR Program activities, which have been published through the end of FY 1992 are listed below. The publications are categorized into the following subject areas: IFR Concept, Metal Fuel Performance, Pyroprocessing, Safety, Neutronics and Core Design, and Fuel Cycle Demonstration.

IFR Concept

1. Y. I. Chang, "Progress and Status of the Integral Fast Reactor (IFR) Development Program," Proc. of the Amer. Power Conf., 506-8, Chicago, IL (April 13-15, 1992).
2. Y. I. Chang, "A Next-Generation Reactor Concept: The Integral Fast Reactor (IFR)," presented at 7th KAIF/KNS Ann. Conf., Seoul, Korea (April 21-22, 1992).
3. W. H. Perry, R. M. Singer, R. W. King and K. C. Gross, "Advanced Condition Monitoring Techniques and Plant Life Extension Studies at EBR-II," Proc. of the Intl. Conf. on Plant Life Extension, Upgrading, Repair, Refurbishment, Upgrading and Aging (PLEX-91), 251-66, Berlin, Germany (December 2-4, 1991).
4. C. E. Till and Y. I. Chang, "Integrating the Fuel Cycle at IFR," Nucl. Eng. Intl., 33-34 (November 1992).

Metal Fuel Performance

1. D. C. Crawford and G. S. Was, "The Role of Grain Boundary Misorientation in Intergranular Cracking of Ni-16Cr-9Fe in 360°C Argon and High Purity Water," Metall. Trans., 23A:1195-1206 (April 1992).
2. M. J. Lee, L. K. Chang, C. E. Lahm, and D. L. Porter, "The Impact of Duct-to-Duct Interaction on the Hex Duct Dilation," Proc. for the 1992 ASME PVP Conf., 231:105-14, New Orleans, LA (June 12-25, 1992).
3. R. G. Pahl, D. L. Porter, D. C. Crawford, L. C. Walters, "Irradiation Behavior of Metallic Fast Reactor Fuels," J. Nucl. Mater., 188:3-9 (August 1992).
4. V. L. Smith-Wackerle and C. L. Trybus, "New Life for Unusable Electro-polished TEM Foils," Ultramicroscopy, 40:187-90 (May 1992).
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Pyroprocessing

1. T. Koyama, T. R. Johnson, and D. F. Fischer, "Distribution of Actinides in Molten Chloride Salt/Cadmium Metal Systems," to be published in J. Alloys and Compounds.
2. J. J. Laidler, "Recycling Nuclear Fuels," to be published in The New Explorers, Weldon Owen Publishing, Sydney, Australia.

Safety

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2. F. E. Dunn, "Integrated Intra-Subassembly Treatment in the SASSYS-1 LMR Systems Analysis Code," Proc. of the Fifth Intl. Topical Mtg. on Reactor Thermal Hydraulics, NURETH-5, 2:408-15, Salt Lake City, UT (September 21-24, 1992).
3. K. C. Gross and K. K. Hoyer, "Spectrum-Transformed Sequential Testing Method for Signal Validation Applications," Proc. 8th Power Plant Dynamics, Control & Testing Symp., I:36.01-12, Knoxville, TN (May 27-29, 1992).
4. K. C. Gross and R. V. Strain, "Sinusoidal Source-Perturbation Experiments with a Breached Fuel Element in EBR-II," Nucl. Tech., 98:113-23 (April 1992).
5. K. C. Gross, L. L. Briggs, and D. K. Leibfritz, "Distributed Computing System for Reactor Simulation and Control," Trans. Am. Nucl. Soc., 64:310-11, San Francisco, CA (November 1991).
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