December 1994

ANL-IFR-246

INTEGRAL FAST REACTOR PROGRAM
ANNUAL PROGRESS REPORT
FY 1994

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IFR TECHNICAL MEMORANDUM NO. 246

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ABSTRACT

This report summarizes highlights of the technical progress made in the Integral Fast Reactor (IFR) Program in FY 1994. 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

Argonne's Integral Fast Reactor (IFR) Program has been the centerpiece of the Department of Energy's advanced reactor development. In January 1994, the Department of Energy announced in its FY 1995 budget request that the IFR Program, along with the associated Actinide Recycle Program, would be terminated as of October 1, 1994. EBR-II was shut down on September 30, 1994 after 30 years of highly successful operation.

In place of the IFR Program, the Department of Energy plans to establish the following redirected programs starting in FY 1995 in order to utilize the Laboratory's nuclear R&D capabilities to help solve more near-term higher priority issues:

- **DOE's spent nuclear fuel treatment**, which would apply electrometallurgical technology developed under the IFR and Actinide Recycle Programs for the treatment of DOE spent nuclear fuel inventory stored at various DOE sites.
- **Reactor and fuel cycle safety**, including establishment of an international reactor safety institute to improve safety of Soviet-designed reactors, and development of advanced analyses and computational techniques applicable to advanced light water reactors.
- **Reduced Enrichment for Research and Test Reactors Program**, with a new task on the development of high density fuel, which would enhance the ongoing program and applications.
- **Decontamination and decommissioning technology**, based on experience with the EBR-II system and other Laboratory facilities, that can be applied generically to nuclear power plants and fuel cycle facilities.

The main focus of the redirected program will be placed on the continued development of electrometallurgical technology so that: (1) it can be successfully applied to treat the EBR-II spent fuel for disposal as part of the EBR-II shutdown activity, and (2) it can be made available as a candidate technology to treat a variety of DOE spent fuel such as N-Reactor fuel and other fuel types that require treatment before disposal in a repository.

The refurbished Fuel Cycle Facility (FCF) will play a key role in both applications. A substantial progress has been made toward completing the facility modifications for initial hot operation. The Argon Cell has been closed, filled with argon, and adequate argon purity has been obtained and maintained. The safety exhaust system as well as the Argon Cell cooling system have been installed, tested, and made operational. The facility is now ready to start hot operation.

Major process equipment, including the electrorefiner, cathode processor and casting furnace have been installed and are in final startup testing in the Argon Cell. The electrorefiner has been loaded with the process chemicals and cadmium and the system has been heated to the operating temperature of 500°C. The next step involves charging the electrorefiner with depleted uranium and testing electrorefining operations.

The Operational Readiness Review (ORR) process has started for the operation with actual spent fuel. It is expected that the ORR approval and the DOE authorization for startup will be obtained in early 1995.

In parallel to the FCF operation, the R&D effort will emphasize the waste treatment processes and waste form qualification, testing of high throughput electrorefiner concepts, and development of head-end processes for treatment of various DOE spent fuel types.

Because of the importance associated with the redirected program mission on DOE spent fuel treatment, the Department of Energy requested the National Academy of Sciences to establish a Committee on Electrometallurgical Techniques for DOE Spent Fuel Treatment to conduct a fast-track study. This Committee will address the question whether electrometallurgical techniques represent a potentially viable technology for DOE spent fuel treatment that warrants further research and development.

II. METAL FUEL PERFORMANCE

A. Fuel/Cladding Compatibility in Full-length Metallic IFR Fuels at Elevated Temperatures

1. Introduction

The IFR-1 experiment contained fuel elements with a U-19Pu-10Zr wt% alloy in D9 cladding and was irradiated to 9.4 at. % burnup in the Fast Flux Test Facility (FFTF). One purpose of the experiment was to compare the fuel/cladding chemical interaction (FCCI) behavior between elements irradiated with a relatively flat power profile in the Experimental Breeder Reactor II (EBR-II) to full-length (91.44 cm of fuel) elements irradiated with a power and temperature profile more prototypical of future commercial reactors, as in the FFTF. Analysis of various specimens from EBR-II experiments has shown that different combinations of linear power and cladding temperature significantly affect FCCI. The comparison would establish a link between the large EBR-II database of FCCI information to that of the prototypical power/temperature profile, which would be required for licensing of the Integral Fast Reactor (IFR). Element 181193 from the IFR-1 experiment was destructively examined in Argonne National Laboratory's Alpha-Gamma Hot Cell Facility. A description of the experiment, as well as the FCCI results from as-irradiated samples, was reported by Tsai and Neimark.¹ Results from elevated temperature testing on samples from this element, and a comparison with the EBR-II data, are presented here. Elevated-temperature testing is used to assess behavior under abnormal reactor situations.

2. Test Matrix and Results

The elevated temperature tests were aimed at determining the threshold temperature for eutectic formation (i.e., fuel-surface liquefaction due to infusion of iron and nickel, which lower the fuel solidus temperature) and the depth of cladding penetration by the fuel and fission products. The tests were conducted in the Fuel Behavior Test Apparatus (FBTA);² results for other fuel/cladding combinations were discussed earlier.^{3,4}

Elevated-temperature testing concentrated in three regions of element 181193: highest linear power/moderate cladding temperature ($X/L \sim 0.50$), highest cladding temperature/lowest power ($X/L \sim 0.95$), and high power/high temperature ($X/L \sim 0.75$). This last region is where the largest amount of FCCI was observed in EBR-II elements. For comparison, samples from EBR-II element A-850, with the same fuel and cladding combination as the IFR-1 element and irradiated to 11.2 at.% burnup, were also tested in the FBTA. Elements A-850 and 181193 had approximately the same peak cladding temperature (600°C) and linear power (51 kW/m) at beginning-of-life (BOL).

The duration of all tests was 1 h at temperatures between 675 and 775°C . Operating parameters for element 181193 were derived from the FFTF cycle reports; while those for A-850 were interpolated from output data from the LIFE-METAL computer code. The results are shown in Table II.1.

TABLE II.1. FBTA Test Matrix for Element 181193

X/L	Test Temperature ($^{\circ}\text{C}$)	Test Duration (h)	BOL PICT ($^{\circ}\text{C}$)	BOL Power (kW/ft)	Total Cladding Wastage (μm)	Eutectic Formation
0.56	675	1	569	15.7	41	No
0.57	700	1	571	15.7	40	Yes
0.55	700	1	568	15.7	61	Yes
0.52	725	1	564	15.7	95	Yes
0.53	750	1	565	15.7	97	Yes
0.74	700	1	592	12.6	41	No
0.75	725	1	593	12.4	48	Yes
0.72	750	1	593	12.4	66	Yes
0.73	775	1	592	12.9	85	Yes
0.92	725	1	601	8.5	46	No
0.93	750	1	602	8.3	13	N

The threshold temperature for eutectic formation in element 181193 was between 675 and 700°C in the highest linear power region, and rose to between 725 and 750°C near the top of the fuel column. By comparison, the threshold temperature in the EBR-II irradiated element A-850 (Table II.2) was 675 to 700°C at the top of core for a 1-h test. The percentage of the fuel surface affected was low in every case.

A comparison of data from the three different fuel regions of element 181193 shows that the linear power rating has a strong effect on both cladding penetration and fuel eutectic formation. As linear power decreases toward the top of the fuel column, the threshold temperature for eutectic formation increases and cladding penetration decreases, apparently because of the absence of critical reactants at higher elevations. Eutectic formation at these temperatures depends on diffusion of iron and nickel from the cladding, which together with the diffusion of fuel components into the cladding is strongly affected by the presence of fission products (mainly lanthanides) at the fuel/cladding interface as a result of irradiation.⁴ Higher linear power increases both the radial temperature gradient and the production of fission products, and both effects lead to more lanthanide fission products at the fuel/cladding interface. As a result of higher linear power, diffusion is increased for both fuel components into the cladding and iron and nickel into the fuel. Therefore, the threshold temperature for eutectic formation is lower in regions of high linear power and higher in regions of lower power, i.e., towards the ends of the fuel column.

TABLE II.2. FBT A Test Matrix for Element A-850

X/L	Test Temperature (°C)	Test Duration (h)	BOL PICT (°C)	BOL Power (kW/ft)	Total Cladding Wastage (μm)	Eutectic Formation
0.76	675	1	566	14.6	88	No
0.83	700	1	574	13.8	107	Yes
0.73	723	1	563	15.0	108	Yes

Unlike linear power, cladding temperature during irradiation evidently has little effect on elevated-temperature performance. While higher temperatures will facilitate interdiffusion between lanthanide fission products and iron, the thermal exposure alone will not result in an increased fission product inventory at the fuel/cladding interface.

The cladding penetration and threshold temperature found in the high-power/moderate-temperature (i.e., worst-case) region of FFTF element 181193 were comparable to those found in the region of EBR-II element A-850 with the largest amount of FCCI and the lowest threshold temperature. This leads to the conclusion that the elevated-temperature results from EBR-II elements are representative of the worst-case conditions for an element with a prototypical power and temperature profile.

B. Mark-V/VA Fuel Pins and Elements Specification

The following is a transcript of the Mark-V/VA Fuel Pins and Elements Specification which has been completed and approved for use over the past year. It is designed to be a minimum list of requirements to produce a fuel that will perform in a manner enveloped by the Safety Case that has been written for operation of the EBR-II core with U-20Pu-10Zr fuel. Limitations listed in previous specifications for EBR-II fuel which were designed to aid the manufacturing organization in process control have intentionally been eliminated. Appended to the document are two memos which were instrumental to the determination of the needed chemistry control in terms of fissile content.

The termination of the IFR Program comes just as the specification was completed and it is the intent of this documentation to help to archive the document in a manner available to all those with an interest. In this way, if there is a future use for such information, the specification can easily be retrieved.

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Specification No E-1257-009-ES

EBR-II
MARK-V AND-VA FUEL PINS AND ELEMENTS

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ARGONNE NATIONAL LABORATORY	Specification No. E-1257-0009-ES		
TITLE: MK-U AND VA FUEL PINS AND ELEMENTS	00		
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1.0 SCOPE OF WORK

1.1 Work Included

1.1.1 The Fabricator (defined as the ANL-W Facility Operations Division) shall injection cast, inspect, and store fuel pins. These fuel pins shall then be assembled into fuel elements per this specification. These operations shall be done in accordance with this specification and approved ANL drawings.

1.2 Special Conditions

1.2.1 The Fabricator shall be fully responsible for all process procedures, process development, fabrication, quality control procedures, and inspection of the fuel pins and elements.

1.2.2 The Fabricator shall furnish all records and reports listed in Section 5.5 to the Fuel Performance Engineer for review, acceptance and sign-off.

2.0 APPLICABLE DOCUMENTS

The following documents form a part of this Specification except as modified by this Specification. Where there is a conflict between the documents cited and the revised issue thereof, the Fabricator shall notify the Fuel Performance Engineer and use the latest revision unless otherwise directed by the Fuel Performance Engineer. In the event of conflict between the documents referenced and this specification, the requirements of this Specification will supersede.

2.1 Standard Specifications

2.1.1 ANSI/ASME Standards

ANSI/ASME
NQA-1-1989

Quality Assurance Program
Requirements for Nuclear
Facilities

2.1.2 Federal and Military Specifications

MIL-STD-105E

Sampling Procedures and Tables
for Inspecting by Attributes
(April 29, 1963)

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2.1.3 Argonne Specifications

1. E1257-0006-ES   EBR-II Driver Fuel Cladding, Mark-IIIA
2. F0001-0036-DS-00   EBR-II Driver Fuel Cladding, Mark-IV
3. F0001-0049-DS-0   EBR-II Driver Fuel Cladding, Mark-V (including Technical Addenda to P. O. #22891006)
4. F0001-0048-DS,   EBR-II Driver Fuel Spacer Wire, Rev.1 Mark-IIIA
5. F0001-0040-DS-00   EBR-II Driver Fuel Spacer Wire, Mark-IV
6. E1257-0007-ES   EBR-II Fuel Jacket Fabrication
7. F0000-0014-AQ   Fuel Cycle Division Quality Assurance Program Manual

2.2 Drawings

The materials and items specified herein shall be fabricated and assembled in conformity with the following listed drawings (latest revision):

PART DESCRIPTION	MK-V NO.	MK-VA NO.
Fuel Element Assembly	EB-1-54821-C	EB-1-54820-C
Top End Plug	EB-1-54747-B	EB-1-54748-B
Fuel Pin	EB-1-54481-B	EB-1-54126-B
Jacket Assembly - 29 1/2" Element	EB-1-54389-C	EB-1-54271-C
Cladding - 29 1/2" Element	EB-1-54390-B	EB-1-54272-B
Element Tip	EB-1-54391-C	EB-1-54273-C

NOTE: MK-V Element hardware is made from HT-9 stainless steel, and MK-VA from 316 stainless steel.

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3.0 FUEL PIN

3.1 Physical Requirements of Fuel Pins

3.1.1 Fuel pins shall meet the requirements defined in the following table:

<u>ELEMENT TYPE</u>	<u>DIAMETER.</u> <u>(avg.)in.</u>	<u>DIAMETER. (local)in.</u>	<u>LENGTH, in.*</u>
MK-V	0.168 ± 0.003	0.168 + 0.005, - 0.010	13.5 ± 0.1
MK-VA	0.173 ± 0.003	0.173 + 0.005, - 0.010	13.5 ± 0.1

*Value applies to one segment or the sum of two segments.

Straightness of the pins must allow insertion into the jacket with less than 4 pounds of force. The Fuel Performance Engineer will approve Fabricator's method to ensure this prior to fabrication.

3.2 Chemical Properties of Fuel Pins

3.2.1 The chemistry variation (represented by the mass of a given element/isotope) may be expressed as:

$$1) \\ 0.411 * {}^{234}\text{U} + {}^{235}\text{U} + 0.138 * {}^{236}\text{U} + 0.059 * {}^{238}\text{U} + 1.192 * {}^{239}\text{Pu} + \\ 0.436 * {}^{240}\text{Pu} + 1.355 * {}^{241}\text{Pu} + 0.339 * {}^{242}\text{Pu} = \text{FC}$$

$$\text{FC} = \begin{array}{cc} \text{Mark-V} & \text{Mark-VA} \\ 44.3 \pm 2.2 \text{ g} & 46.9 \pm 2.3 \text{ g} \end{array}$$

NOTE: These numbers are expressed in grams per pin, and the variation allowed should be ensured to 3σ confidence. Note also that these FC values were derived from assuming the Mark-V and Mark-VA fuel pins nominally weigh 77.0 and 81.6 grams, respectively.

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and 2)

The uranium, plutonium, and zirconium should be present in the amounts,

ELEMENT	WEIGHT PERCENT
URANIUM	70.0 +1.0/-1.5(avg.); ±1.5 (top,middle,bottom)
PLUTONIUM	20.0 ±1.0(avg.); ±1.5 (top,middle,bottom)
ZIRCONIUM	10.0 ±1.0(avg.); ±1.5 (top,middle,bottom)
U+Pu+Zr	100.0 +1.0/-1.5(avg.)

3.2.2 The impurity levels of the following elements shall be reported.

Cadmium (Cd) ¹	Carbon (C)
Potassium (K) ¹	Nitrogen (N)
Lithium (Li) ¹	Oxygen (O)
Chlorine (Cl) ¹	Silicon (Si)
Iron (Fe) ¹	Yttrium (Y) ¹
Chromium (Cr) ¹	Tantalum (Ta) ¹
Nickel (Ni) ¹	

¹Reporting of these elements is only required when requested by the Fuel Performance Engineer

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The following restrictions on impurity levels shall apply where the concentration is given in parts per million by weight.

$$C + N + O + Si < 2000 \text{ ppm}$$

- 3.2.3 The minor transuranics, americium (Am) and neptunium (Np) shall be reported. Whenever any of the feedstock contains gallium (Ga) it will become an impurity in the final fuel product and should be reported. Am shall not exceed 0.5 wt%.
- 3.2.4 For irradiated uranium and/or plutonium starting material, the following fission products shall be reported:

<u>RARE EARTHS *</u>	Neodymium	(Nd)
	Lanthanum	(La)
	Cerium	(Ce)
	Praseodymium	(Pr)
	Samarium	(Sm)
<u>OTHERS¹</u>	Tellurium	(Te)
	Molybdenum	(Mo)
	Ruthenium	(Ru)
	Rhodium	(Rh)
	Palladium	(Pd)

* The total accumulation of rare earths in the final product shall be held to less than 5000 ppm (by weight) of the alloy

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3.3 Fabrication

- 3.3.1 The Fabricator shall blend the uranium, plutonium, and zirconium in the proper proportions to achieve a homogeneous and uniform uranium-plutonium-zirconium alloy fuel of the composition specified in Section 3.2.
- 3.3.2 Casting of the fuel pins shall be done in a vacuum or in argon atmosphere containing not more than 300 ppm total impurities. Subsequent operations of demolding, cutting to length, and inspection shall also be done in an inert atmosphere containing not more than 300 ppm of water and oxygen. See 4.3

4.0 FUEL ELEMENT

4.1 Sodium Bonding

The bond sodium height, as observed in the plenum of the element shall be controlled to 0.5 ± 0.25 inches above the fuel pin. This value includes any trapped sodium observed in the plenum of the element above the fuel in the shape of a right circular cylinder.

Bonding shall be performed at a minimum of 450 °C and a maximum of 560 °C.

4.1.1 Bond Sodium Weight

The weight of bond sodium added to the jacket shall be:

Mark-V

Mark-VA

1.816 ± 0.015 g

1.942 ± 0.015 g

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4.2 Fuel Pin Position

The fuel pin shall be in a position within the jacket, after sodium bonding so that it is within 0.100-inches (± 0.035 inch accuracy) of being seated on the lower end plug. Rebonding and reinspection can be attempted if the element fails this inspection.

4.3 Element Construction

Jacket requirements are included in the jacket specification (E1257-0007-ES, F0001-0036-DS-00 and F0001-0049-DS-0) for the various fuel element types - this construction is done external of the hot cells. The final closure weld, where-by the top end plug is welded to the jacket tube, shall be done in an inert atmosphere containing not more than 300 ppm total of water plus oxygen.

- (a) The weld of the end plug to jacket shall have a shape approximately a hemisphere, and a maximum diameter as specified on the design drawing.
- (b) By process qualification it shall be demonstrated that the end plug weld to jacket shall have 100% penetration of the jacket wall. In addition, the minimum effective weld thickness (root-to-surface distance less any porosity and inclusions) shall be equal to or greater than 80% of the jacket wall thickness. Process qualification demonstrates that 10 consecutive end plug weld to jacket meet the weld requirements of this specification. Failure of any of the 10 welds require process requalification.

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5.0 QUALITY ASSURANCE AND CONTROL

5.1 Quality Verification Program Requirements

The Fabricator shall plan, establish, implement, and maintain a documented quality verification program that fulfills the requirements of NQA-1, QAP W0001-0929-QM-01, 10CFR Part 830, and any additional quality assurance requirements of this specification.

5.2 Qualification Requirements

5.2.1 Operational Procedures

Fabrication Operating instructions shall be reviewed by the Fuel Performance Engineer.

5.2.2 Analytical Procedures

The Fuels and Engineering Division Analytical Laboratory Section shall establish precise and accurate analytical techniques that will yield measurement uncertainties within the confidence intervals shown below at the 95% (2 sigma) confidence level for each individual sample measured:

Element or Isotope	Relative percentage of the weight fraction present
Uranium (total)	+/- 0.5%
Uranium 234	+/- 5.0%
Uranium 235	+/- 0.5%
Uranium 236	+/- 5.0%
Uranium 238	+/- 0.5%

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Element or Isotope (con't.)	Relative percentage of the weight fraction present (con't.)
Plutonium (total)	+/- 0.5%
Plutonium 239	+/- 0.5%
Plutonium 240	+/- 1.0%
Plutonium 241	+/- 15.0%
Plutonium 242	+/- 20.0%
Americium	TBD
Neptunium	TBD
Zirconium	+/- 5.0%
Elements listed in Section 3.2.2 and 3.2.4	Best effort basis (typically < +/- 15%)

The accuracy of the uranium, plutonium and zirconium measurements shall be determined by the analyses of certified reference material or traceable secondary control standards which are trended with laboratory control charts as described in the Analytical Laboratory Quality Assurance Plan. The measurement of the impurity and carryover constituents shall be done on a best effort basis using available standards for purposes of calibration and uncertainty estimates. Analytical procedures and evidence supporting the confidence levels stated above shall be submitted to the Fuel Performance Engineer for approval prior to production.

5.3 Quality Conformance Inspection

5.3.1 Responsibility

Unless otherwise specified, the Fabricator shall be responsible for the performance of all tests and inspections required prior to submission to the Fuel Performance Engineer of any fuel element for acceptance. The performance of such tests and inspections does not limit the right of the Fuel Performance Engineer to conduct such other

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tests and inspections as the Fuel Performance Engineer deems necessary to assure that all fuel pins are in conformance with all requirements of this specification. Acceptance by the fabricator is not acceptable.

5.3.2 Surveillance at Source

The Fabricator's manufacturing, inspection, and test operations will be surveyed at the Fabricator's plant by the Fuel Performance Engineer. The Fabricator shall make available to the Fuel Performance Engineer those manufacturing and inspection records of work in process specific to MK-V and MK-VA. When notified in writing, the Fabricator shall perform or repeat, under surveillance of the Fuel Performance Engineer, selected inspections and tests required by applicable drawings and specifications.

5.3.3 Inspection Requirements

Inspection requirements are listed in Table I. Where "Item Qualification" is listed as the inspection plan, the quantity represented shall be accepted only when all the samples meet the referenced requirement. Where "Item Qualification" requires destructive testing of fuel pins, items rejected for an attribute not affecting the properties to be evaluated may be used.

5.3.4 Composition Control and Homogeneity Control

5.3.4.1 Samples for chemical analysis shall be taken directly from fuel pins cut to finished length (i.e., samples may not be taken from ends cut, sheared, or machined from fuel pins or other scrap or residue material from the casting batch).

5.3.4.2 Composition and impurity level determinations shall be made on at least one randomly selected fuel pin from each batch. Melt composition and impurity levels shall be verified on an alloy sample taken from the center of a fuel. Techniques and personnel shall be qualified for procedures used.

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5.3.4.3 Samples for homogeneity determination shall be taken from at least the bottom, center, and top end of a randomly selected finished fuel pin. Samples for homogeneity determination shall be taken from one pin from each batch. The purpose of the homogeneity sampling is to insure complete mixing of the alloying materials.

Homogeneity verification requires all elements referred to in the specification, including isotopic heavy metal, impurities, and alloying elements to be within the stated acceptable range (3.2.1). The ranges of homogeneity shall be identical to the ranges called out in the fuel composition, including uranium, plutonium, and alloying elements.

5.3.4.4 Samples shall be taken and temporarily stored under conditions which will minimize oxidation or other deleterious chemical reactions. Sample containers shall be approved by the Fuel Performance Engineer.

5.3.5 Measuring and Test Equipment Controls

5.3.5.1 Measuring and testing equipment and measurement standards shall be calibrated and utilized in an environment controlled to the extent necessary to assure continued measurements of required accuracy giving due consideration to temperature, humidity, vibration, cleanliness, and other controllable factors affecting precision measurement. When applicable, compensating corrections shall be applied to calibration results obtained in an environment which departs from standard conditions.

5.3.5.2 The following tolerances apply to the required accuracy on the physical measurements of completed fuel pins.

- (a) Length - measure to ± 0.02 in., standards accurate to 0.002 in.

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- (b) Weight - weigh to ± 0.05 g, standards accurate to 0.005 g.
- (c) Pin Diameter - measure to ± 0.001 in., standards accurate to 0.0001 in.

5.4 Control of Nonconforming Items

NCR's shall be submitted to the Fuel Performance Engineer by the Fabricator if the Fabricator proposes to furnish fuel pins or elements not in accordance with specification requirements. All such reports shall include a complete identification of the material or items involved, complete description of the nonconformance, and identification of specification requirements affected. Nonconformances must be approved in writing by the Fuel Performance Engineer prior to assembling into subassemblies.

5.5 Records and Reports

5.5.1 The Fabricator shall furnish one completely signed copy of certification records at the time of completion of the fuel elements. No lot of fuel elements shall be considered complete until such documents have been received by the Fuel Performance Engineer. Certification records shall include the following:

- 5.5.1.1 Certification that each fuel pin has been inspected for length, diameter, weight, straightness and that each pin, (including the combination of two pin segments) was within specified tolerances.
- 5.5.1.2 Certified reports of item qualification for fuel chemical composition as specified in Section 3.2.
- 5.5.1.3 Computer printout of items (a) through (c) are required for acceptable fuel pins only.
  - (a) Casting batch number.
  - (b) Pin or element number.

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- (c) Fuel-pin weight to the nearest 0.05 g. This shall be the exact weight determined during inspection.

5.5.2 The Fabricator shall maintain log books or files of the following operations which show at least the noted information. All such log books or certified copies shall be submitted to the Fuel Performance Engineer at the end of the program.

5.5.2.1 Casting operation log book shall show the date, the casting batch number, the melt temperature at or just prior to casting, furnace pressure just before and just after casting, mold insertion time, and any other casting parameters deemed important by the Fabricator. The process work sheet package satisfies this requirement.

6.0 NOTES AND SUPPLEMENTAL INFORMATION

6.1 Definitions

- 6.1.1 The term "fuel pin" shall apply to the uranium-plutonium-zirconium alloy body of finished dimensions and weight (this may apply to two pin segments). Note that other documentation may often refer to fuel 'pin' as fuel 'slug.'
- 6.1.2 The term "fuel element" shall apply to the finished stainless steel clad, sodium bonded, fuel. Note that other documentation may often refer to fuel 'element' as fuel 'pin.'
- 6.1.3 The term "lot" shall refer to a quantity of material produced in one operation, or in one series of operations.
- 6.1.4 The terms "top" and "bottom" shall refer to the upper and lower extremities, respectively, of the fuel pin, or any component part thereof. The top is the upper end when cast.
- 6.1.6 The "confidence interval" shall be defined as the interval about the measured value that has a 95% probability of including the true average value of the sample unless otherwise specified.

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- 6.1.7 "Accuracy" shall be defined as the maximum allowable deviation of a measurement from an absolute value of Fuel Performance Engineer-approved standard.
- 6.1.8 "Variance" shall be defined as the square of the standard deviation.
- 6.1.9 "Fabricator" shall be defined as the organization, or predefined representative of the organization that casts the fuel and produces the final fuel element/element product.
- 6.1.10 "Fuel Performance Engineer" shall be defined as the representative of the 'Buyer' organization, in this case the FE Division.

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TABLE I. Quality Conformance Inspection

Characteristic	Requirement	Inspection Plan	Method of Testing and Criteria
5.3.3.1 Fuel Pin Weight	See 3.2.1	100%	Balance (pin mass needed to calculate the numbers in 3.2.1) ²
5.3.3.2 Fuel Pin Length	3.1.1	100%	Fabricator's Choice [accuracy requirements in Section 5.3.5.2 (a)] ²
5.3.3.3 Fuel Pin Diameter	3.1.1	100%	Digital gauging probes (LVDT based), laser profilometry, or hand micrometer [accuracy requirement in Section 5.3.5.2(c)]. Diameter shall be measured at minimum 1 in. intervals over the length of pin. ¹
5.3.3.4 Fuel Pin straightness	3.1.1	100%	See section 3.1 ¹
5.3.3.5 Fuel Pin Chemical Composition	3.2	Item qualification as required in Section 5.3.4	Published methods. ²

1. Scrap
2. Scrap or submit to Buyer as nonconforming item.
3. Scrap or rebond; maximum of three bond cycles

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TABLE I. Quality Conformance Inspection (Cont'd)

Characteristic	Requirement	Inspection Plan	Method of Testing and Criteria
5.3.3.6 Plug end-to-jacket weld	4.3	(a) -(b) 100% (c) one per magazine, 36 elements minimum	The jacket does not need to be loaded with sodium or fuel, but the test weld shall be representative of a production weld in every other respect. A test weld that is unacceptable shall require immediate requalification of the operator and equipment. ¹ (a) Visual (b) Shape inspection - doing height, roundness, pre-weld inspection of and plug seating. (c) Metallographic sample preparation in accordance with ASTM E3 and a micrograph taken at 25X minimum. Testing shall be done on a jacket or upper portion of a jacket.*
5.3.3.7 Sodium Bond/level	4.1.1	100%	X-ray inspection for bond-height level ³
5.3.3.8 Fuel Pin Position	4.2	100%	X-ray inspection ³
5.3.3.9 Maximum diameter of plug end-to-jacket weld	See EB-1-54821-C (MK-V) EB-1-54820-C (MK-VA)	100%	Visual inspection using image recognition system ²
5.3.3.10 Handling damage		100%	Visual ¹

The footnotes describe the disposition to be made for rejects discovered during inspections described in Table I.

*Mounted specimens with all necessary identification shall be submitted to the Fuel Performance Engineer within 10 days after Fabricator completes examination.

ARGONNE
NATIONAL
LABORATORY

INTRA-LABORATORY MEMO

December 22, 1993

To: D. J. Hill RA

From: J. Y. Ku *JK* RA
L. L. Briggs *LLB* RA

Subject: Scoping Evaluation of the Effect of Increasing Fuel Manufacturing Variations on Safety Analyses of Mark-V and Mark-VA Fuels

This memorandum describes a scoping evaluation of the effect of a postulated increase in the uncertainty of fuel manufacturing variations on the safety analyses of Mark-V and Mark-VA fuels [1]. The evaluation was prompted by the analysis reported in Ref. 2 indicating a possible regional variation of up to about 1% in power per unit mass due to variations of specific powers in individual nuclides in a Mark-V core. The calculations included herein were made for two cases: one assuming a 5% overall uncertainty, and the other a 6% overall uncertainty, in the slug power due to fissile content variations. The six tables shown below illustrate how the uncertainty factors applied to the hot channel factors analyses in Ref. 1 would be affected.

The slug power can be expressed as $P = \sum \alpha_i m_i$, where α_i is the specific power of nuclide i (normalized to ^{235}U) and m_i is the mass of nuclide i . Then, the deviation in power is simply $dP = \sum \alpha_i dm_i + \sum m_i d\alpha_i$ or $\Delta P = \sum \alpha_i \Delta m_i + \sum m_i \Delta \alpha_i$. The first term on the right-hand side of the equation represents the power deviation caused by variations of the mass of nuclide i , as shown in the fissile content variation expression in section 3.2.1 of the fuel specification (FS) [3]. The second term indicates the slug power deviation caused by the deviations of the specific power from the nominal value (the values listed in Table 1 of Ref. 2).

The current FS allows a $\pm 4\%$ (44.5 ± 1.8 g for Mark-V and 47.2 ± 1.9 g for Mark-VA) deviation in the slug power due to chemistry variations, based on fixed specific powers from Ref. 4. However, as indicated in Ref. 2, the specific power of each nuclide varies to some extent depending on the spectrum in different core locations (Table 1 of Ref. 2), and the regional slug power could deviate additionally by as much as 1% in a situation of extreme variations simultaneously in regional specific power, fuel pin mass, and nuclide composition [2]. This means that the deviation in the slug power could be as high as $4\% + 1\% = 5\%$. Therefore, if the actual tolerable slug power variation ΔP is required to be within 4%, the fuel content expression in Ref. 1, with the nuclide specific powers replaced by the values recommended in Table 1 of Ref. 2, should allow only 3% (i.e., 4% minus 1%) variation in fuel fissile content (as suggested in Ref. 2). The implication is that it will impose a constraint probably too restrictive for slug manufacturing, especially when remotely-controlled processes are involved.

An alternative is to retain the 4% fissile content variation currently proposed in the FS, so that the actual slug power tolerance will be $4\% + 1\% = 5\%$. The implication is that the uncertainty factors analyses in Ref. 1 have to be reevaluated with a higher (effective) uncertainty factor related to the slug power variation, to include the effect of the specific power deviation. A scoping evaluation is presented in Tables 2, 4, 5, and 6 below to show the impact of the increased uncertainty in fuel manufacturing variations on the original analyses in Tables 1 and 3 below. The rationale supporting the scoping evaluation is described below.

The uncertainty factor of fuel diameter (shown in Table 1), in the original hot channel factors analysis for Mark-V fuel pins in row 2, is not included in Table 2 for the current analysis. In addition, the uncertainty factor of fissile fuel concentration is replaced by an effective uncertainty factor of fissile fuel content to account for a 5% uncertainty in the slug power due to fuel manufacturing variations and specific power deviations. The uncertainty factor of fuel diameter is excluded because the fuel diameter tolerance will not contribute to any additional fuel temperature rise once the linear slug power is determined, as can be seen from the relationship between the linear power and the fuel temperature, $\chi = 4 \pi \int \kappa dT$, where χ is the linear power, κ is the fuel thermal conductivity, and T is the fuel temperature with T_s (surface temperature) and T_{cl} (center-line temperature) as the lower and upper bounds, respectively. The hot channel factors analysis performed with this 1.05 uncertainty factor results in an overall fuel hot channel factor of 1.213 and a 0.5K increase at the fuel hot spot (compare Tables 1 and 2). The 1.035 hot channel factor, used to account for the fuel temperature rise, is smaller than the 1.05 factor for the temperature rises in other regions due to the effect of the increase in thermal conductivity as the fuel temperature increases, which can be verified from the above equation. The 1.013 hot channel factor used to account for the fuel temperature rise due to the uncertainty in power level measurement is smaller than the 1.02 hot channel factor in other regions for the same reason. Another hot channel factors analysis performed, for Mark-V fuel pins in row 6, with this 1.05 uncertainty factor results in an overall cladding hot channel factor of 1.145 and a 2.0K increase at the cladding hot spot (compare Tables 3 and 4).

To allow for the possibility that the tolerance on fuel manufacturing variations might need to be as high as 5%, analyses were also done with a 1.06 effective uncertainty factor of fissile fuel content. Table 5 shows a 1.215 overall hot channel factor and a fuel temperature 0.9K higher than the fuel temperature obtained in the original analysis (Table 1) at the hot spot for the fuel pin in row 2. Also, Table 6 shows a 1.149 overall hot channel factor and a cladding temperature 2.9K higher than the cladding temperature obtained in the original analysis (Table 3) at the hot spot for the fuel pin in row 6.

In summary, the slug power uncertainty due to the constraints given in the current FS and the deviation in the specific power discussed in Ref. 2 will be $4\% + 1\% = 5\%$ at most. The analysis of the first case which uses an effective (bounding) factor of 1.05 to reflect the uncertainty in the slug power is sufficient to account for both slug manufacturing variations and specific power deviations. Since the changes in the overall hot channel factors and in the hot spot fuel and cladding temperatures are small, they make an insignificant impact on the safety analysis. Therefore, designating the uncertainty in fissile fuel content to be 5% is consistent with

the analyses of Ref. 1. In addition, the analysis for the other case with a 1.06 effective factor indicates that the constraint required by the FS [3] for the slug manufacturing variations can be relaxed somewhat, if necessary, by allowing an increase in uncertainty from 4% to 5%, without causing a significant impact on the Mark-V safety analysis.

References

1. L. K. Chang and D. J. Hill, "Safety Analysis and Technical Basis for Establishing an Interim Burnup Limit for Mark-V & Mark-VA Fueled Subassemblies in EBR-II," to be published.
2. H. S. Khalil and J. A. Stillman, "Recommended Fissile Content Parameters for the Mark-V Fuel Specification," ANL Intra-Laboratory Memo to distribution, December 13, 1993.
3. D. L. Porter, "Mark-V and -VA Fuel Specification," draft, November 1, 1993.
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Table 1 Hot Channel Factors Analysis For Mark-V Fuel Pin in Row 2
(Table 4.4 of Ref. 1)

(Nominal Coolant Temp. Rise, 255. Deg. F or 142. Deg. C)

Thermo-Physical Quantity (Nom. Temp. Rise, Deg. C)	Uncertainty Factor	Coolant (82.7)	Film (15.4)	Cladding (53.5)	Gap (14.6)	Fuel (315.7)	Total Uncertainty
DIRECT:							
Power Level Measurement (Temp. Rise)	1.020	1.020 84.4	1.020 15.7	1.020 54.6	1.020 14.9	1.013 319.8	
STATISTICAL:							
Neutron & Gamma Flux (Temp. Rise)	1.070	1.070 5.9	1.100 1.6	1.100 5.5	1.100 1.5	1.100 32.0	46.4
Flow Rate Thru Subassem. (Temp. Rise)	1.070	1.079 6.7	1.025 0.4				7.1
Flow Profile in Subassem. (Temp. Rise)	1.050	1.056 4.7	1.018 0.3				5.0
Clad Thickness (Temp. Rise)	1.030			1.030 1.6			1.6
Fuel Diameter (Temp. Rise)	1.020		1.040 0.6	1.040 2.2	1.040 0.6	1.040 12.8	16.2
Fissile Fuel Concentration (Temp. Rise)	1.010	1.010 0.8	1.010 0.2	1.010 0.5	1.010 0.1	1.010 3.2	4.9
Clad Thermal Conductivity (Temp. Rise)	1.070			1.080 4.4			4.4
Fuel Thermal Conductivity (Temp. Rise)	1.250					1.250 80.0	80.0
Film Heat Transfer Coef. (Temp. Rise)	1.160		1.200 3.1				3.1

$$\text{SQRT}(\text{SUM}(X \cdot X)) = 94.5$$

$$T(\text{fuel}) = 644.3 + 84.4 + 15.7 + 54.6 + 14.9 + 319.8 + 94.5 = 1228.2 \text{ (K)}$$

$$\text{Overall Hot Channel Factor} = (1228.2 - 644.3) / (1126.2 - 644.3) = 1.212$$

Table 2 Postulated Hot Channel Factors Analysis For Mark-V Fuel Pin in Row 2

(Nominal Coolant Temp. Rise, 255. Deg. F or 142. Deg. C)

Thermo-Physical Quantity (Nom. Temp. Rise, Deg. C)	Uncertainty Factor	Coolant (82.7)	Film (15.4)	Cladding (53.5)	Gap (14.6)	Fuel (315.7)	Total Uncertainty
-----							
DIRECT:							
Power Level Measurement (Temp. Rise)	1.020	1.020 84.4	1.020 15.7	1.020 54.6	1.020 14.9	1.013 319.8	
STATISTICAL:							
Neutron & Gamma Flux (Temp. Rise)	1.070	1.070 5.9	1.100 1.6	1.100 5.5	1.100 1.5	1.100 32.0	46.4
Flow Rate Thru Subassem. (Temp. Rise)	1.070	1.079 6.7	1.025 0.4				7.1
Flow Profile in Subassem. (Temp. Rise)	1.050	1.056 4.7	1.018 0.3				5.0
Clad Thickness (Temp. Rise)	1.030			1.030 1.6			1.6
Fissile Fuel Content (Temp. Rise)	1.050	1.050 4.2	1.050 0.8	1.050 2.7	1.050 0.7	1.035 11.2	19.7
Clad Thermal Conductivity (Temp. Rise)	1.070			1.080 4.4			4.4
Fuel Thermal Conductivity (Temp. Rise)	1.250					1.250 80.0	80.0
Film Heat Transfer Coef. (Temp. Rise)	1.160		1.200 3.1				3.1

							SQRT(SUM(X*X)) = 95.1

$$T(\text{fuel}) = 644.3 + 84.4 + 15.7 + 54.6 + 14.9 + 319.8 + 95.1 = 1228.7 \text{ (K)}$$

$$\text{Overall Hot Channel Factor} = (1228.7 - 644.3) / (1126.2 - 644.3) = 1.213$$

Table 3 Hot Channel Factors Analysis For Mark-V Fuel Pin in Row 6
(Table 4.6 of Ref. 1)

(Nominal Coolant Temp. Rise, 305. Deg. F or 169. Deg. C)

Thermo-Physical Quantity (Nom. Temp. Rise, Deg. C)	Uncertainty Factor	Coolant (169.4)	Film (9.8)	Cladding (27.6)	Total Uncertainty
DIRECT:					
Power Level Measurement (Temp. Rise)	1.020	1.020 172.8	1.020 10.0	1.020 28.2	
STATISTICAL:					
Neutron & Gamma Flux (Temp. Rise)	1.070	1.070 12.1	1.100 1.0	1.100 2.8	15.9
Flow Rate Thru Subassem. (Temp. Rise)	1.070	1.079 13.7	1.025 0.2		13.9
Flow Profile in Subassem. (Temp. Rise)	1.050	1.056 9.7	1.018 0.2		9.9
Clad Thickness (Temp. Rise)	1.030			1.030 0.8	0.8
Fuel Diameter (Temp. Rise)	1.020		1.040 0.4	1.040 1.1	1.5
Fissile Fuel Concentration (Temp. Rise)	1.010	1.010 1.7	1.010 0.1	1.010 0.3	2.1
Clad Thermal Conductivity (Temp. Rise)	1.070			1.080 2.3	2.3
Film Heat Transfer Coef. (Temp. Rise)	1.160		1.200 2.0		2.0

$$\text{SQRT}(\text{SUM}(X^2)) = 23.7$$

$$T(\text{clad}) = 644.3 + 172.8 + 10.0 + 28.2 + 23.7 = 879.0 \text{ (K)}$$

$$\text{Overall Hot Channel Factor} = (879.0 - 644.3) / (851.1 - 644.3) = 1.134$$

Table 4 Postulated Hot Channel Factors Analysis For Mark-V Fuel Pin in Row 6
(Nominal Coolant Temp. Rise, 305. Deg. F or 169. Deg. C)

Thermo-Physical Quantity (Nom. Temp. Rise, Deg. C)	Uncertainty Factor	Coolant (169.4)	Film (9.8)	Cladding (27.6)	Total Uncertainty
DIRECT:					
Power Level Measurement (Temp. Rise)	1.020	1.020 172.8	1.020 10.0	1.020 28.2	
STATISTICAL:					
Neutron & Gamma Flux (Temp. Rise)	1.070	1.070 12.1	1.100 1.0	1.100 2.8	15.9
Flow Rate Thru Subassem. (Temp. Rise)	1.070	1.079 13.7	1.025 0.2		13.9
Flow Profile in Subassem. (Temp. Rise)	1.050	1.056 9.7	1.018 0.2		9.9
Clad Thickness (Temp. Rise)	1.030			1.030 0.8	0.8
Fissile Fuel Content (Temp. Rise)	1.050	1.050 8.6	1.050 0.5	1.050 1.4	10.5
Clad Thermal Conductivity (Temp. Rise)	1.070			1.080 2.3	2.3
Film Heat Transfer Coef. (Temp. Rise)	1.160		1.200 2.0		2.0

$$\text{SQRT}(\text{SUM}(X \cdot X)) = 25.8$$

$$T(\text{clad}) = 644.3 + 172.8 + 10.0 + 28.2 + 25.8 = 881.0 \text{ (K)}$$

$$\text{Overall Hot Channel Factor} = (881.0 - 644.3) / (851.1 - 644.3) = 1.145$$

Table 5 Postulated Hot Channel Factors Analysis For Mark-V Fuel Pin in Row 2

(Nominal Coolant Temp. Rise, 255. Deg. F or 142. Deg. C)

Thermo-Physical Quantity (Nom. Temp. Rise, Deg. C)	Uncertainty Factor	Coolant (82.7)	Film (15.4)	Cladding (53.5)	Gap (14.6)	Fuel (315.7)	Total Uncertainty
DIRECT:							
Power Level Measurement (Temp. Rise)	1.020	1.020 84.4	1.020 15.7	1.020 54.6	1.020 14.9	1.013 319.8	
STATISTICAL:							
Neutron & Gamma Flux (Temp. Rise)	1.070	1.070 5.9	1.100 1.6	1.100 5.5	1.100 1.5	1.100 32.0	46.4
Flow Rate Thru Subassem. (Temp. Rise)	1.070	1.079 6.7	1.025 0.4				7.1
Flow Profile in Subassem. (Temp. Rise)	1.050	1.056 4.7	1.018 0.3				5.0
Clad Thickness (Temp. Rise)	1.030			1.030 1.6			1.6
Fissile Fuel Content (Temp. Rise)	1.060	1.060 5.1	1.060 0.9	1.060 3.3	1.060 0.9	1.042 13.4	23.6
Clad Thermal Conductivity (Temp. Rise)	1.070			1.080 4.4			4.4
Fuel Thermal Conductivity (Temp. Rise)	1.250					1.250 80.0	80.0
Film Heat Transfer Coef. (Temp. Rise)	1.160		1.200 3.1				3.1
SQRT(SUM(X*X)) =							96.0

$$T(\text{fuel}) = 644.3 + 84.4 + 15.7 + 54.6 + 14.9 + 319.8 + 96.0 = 1229.6 \text{ (K)}$$

$$\text{Overall Hot Channel Factor} = (1229.6 - 644.3) / (1126.2 - 644.3) = 1.215$$

Table 6 Postulated Hot Channel Factors Analysis For Mark-V Fuel Pin in Row 6

(Nominal Coolant Temp. Rise, 305. Deg. F or 169. Deg. C)

Thermo-Physical Quantity (Nom. Temp. Rise, Deg. C)	Uncertainty Factor	Coolant (169.4)	Film (9.8)	Cladding (27.6)	Total Uncertainty
DIRECT:					
Power Level Measurement (Temp. Rise)	1.020	1.020 172.8	1.020 10.0	1.020 28.2	
STATISTICAL:					
Neutron & Gamma Flux (Temp. Rise)	1.070	1.070 12.1	1.100 1.0	1.100 2.8	15.9
Flow Rate Thru Subassem. (Temp. Rise)	1.070	1.079 13.7	1.025 0.2		13.9
Flow Profile in Subassem. (Temp. Rise)	1.050	1.056 9.7	1.018 0.2		9.9
Clad Thickness (Temp. Rise)	1.030			1.030 0.8	0.8
Fissile Fuel Content (Temp. Rise)	1.060	1.060 10.4	1.060 0.6	1.060 1.7	12.7
Clad Thermal Conductivity (Temp. Rise)	1.070			1.080 2.3	2.3
Film Heat Transfer Coef. (Temp. Rise)	1.160		1.200 2.0		2.0

$$\text{SQRT}(\text{SUM}(X^2)) = 26.7$$

$$T(\text{clad}) = 644.3 + 172.8 + 10.0 + 28.2 + 26.7 = 881.9 \text{ (K)}$$

$$\text{Overall Hot Channel Factor} = (881.9 - 644.3) / (851.1 - 644.3) = 1.149$$

**ARGONNE
NATIONAL
LABORATORY**
INTRA-LABORATORY MEMO

December 13, 1993

TO: Distribution

FROM: H. Khalil and J. A. Stillman Reactor Analysis

SUBJECT: **Recommended Fissile Content Parameters for the Mark-V Fuel Specification**

The limit on the variability of fissionable nuclide masses per pin in the Mark-V fuel specification [1] can be expressed in the form:

$$\left| \sum_i \alpha_i \delta m_i \right| \leq x \cdot F, \quad (1)$$

where α_i is the specific power for nuclide i (normalized to U-235), $\delta m_i \equiv m_i - m_i^{nom}$ is the deviation from nominal of the mass of nuclide i , and F is the nominal pin power (in units of equivalent U-235 mass):

$$F \equiv \sum_i \alpha_i m_i^{nom}. \quad (2)$$

In the (draft) fuel specification [1], the "tolerance" x in Eq. (1) is 4% at the 3σ confidence level, and the values of α_i for the eight nuclides included in the summation (U-234, 235, 236, 238; Pu-239, 240, 241, 242) are those provided in ref. 2. These α_i were calculated for a particular region (core center) of a specific ternary-fueled EBR-II configuration [2]. In this memo, a recommendation concerning the choice of the parameters α_i is provided, and the implications of this choice and of the assumed tolerance x on the potential local power variability in Mark-V core configurations are addressed.

To help determine the appropriate set of α_i parameters, the sensitivities of the α_i to core configuration (e.g. specific transuranic isotopic mix) and core location were first investigated. The α_i were found to be insensitive to core configuration, but quite sensitive to core location. For example, the inner-core value of α for Pu-239 was found to be approximately 12% greater than the outer-core value, because the harder spectrum at the center of the core increases the Pu-239/U-235 fission ratio. This spatial dependence of the specific power parameters implies that locally applicable values (say α_{ri}) are in general different from the set of α_i selected for use in the fuel specification; it would probably be impractical to employ α_i sets tailored to different core locations in the fuel specification.

The deviations $\delta\alpha_i (= \alpha_{ri} - \alpha_i)$ of the local parameters from the selected set of α_i can cause the local power relative variability to exceed the fissionable content tolerance x . It can be shown that the local power relative deviation from nominal can be guaranteed not to exceed a value y given by

$$yF_r = xF + \left| \sum_i \delta\alpha_i \delta m_i \right|, \quad (3)$$

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where F_r is the power (in equivalent U-235 mass) produced by the nominal pin nuclide masses in the region characterized by the α_{ri} . [F_r is defined by an equation analogous to Eq. (2) with the α_i replaced by α_{ri}].

The suggested basis for selecting the α_i parameters to be used in Eq. (1) is to obtain the lowest justifiable upper bound on the local power variability y for any location of any Mark-V core configuration. Minimization of this upper bound can be accomplished approximately by utilizing α_i values in the middle of the respective ranges observed for the various nuclides i ; this minimizes the magnitudes of the $\delta\alpha_i$ that need to be considered in Eq. (3). The associated upper bound on y can then be determined by (a) utilizing maximum deviations $\delta\alpha_i$ from the selected α_i in Eq. (3); (b) considering an appropriately broad range of the δm_i , subject to the constraint of Eq. (1); and (c) evaluating F_r using the minimum α_{ri} value for each nuclide i . The constraint on the δm_i was satisfied by restriction of the U-235/U mass fraction to values permitted by Eq. (1).

By first hypothetically neglecting deviations of fissionable nuclide masses from nominal [i.e. $\delta m_i \rightarrow 0$ in Eq. (3)], but allowing for the largest observed deviations of the α_i from mid-range values, the upper bound on y corresponding to $x = 4\%$ (given by $0.04 F/F_r$) was found to be 4.15%. Hence the spatial dependence of the α_i by itself tends to cause the local power variability upper bound to exceed the fissionable content tolerance x by up to 0.15%. If, in conjunction with utilizing the maximum α_i deviations, the U-235/U mass fraction is selected [within the range permitted by Eq. (1)] such that y is maximized, the value by which y exceeds x is increased to 0.21%.

To account additionally for the relatively wide range of potential nuclide mass deviations from nominal, the upper bound on y must be further increased. To determine a conservative (i.e. sufficiently large) upper limit on y , the solution of Eqs. (1) and (3) was obtained allowing for the full range of expected or permitted variations in nuclide masses. The variations considered were in total pin mass (+/- 5%), Pu mass fraction (19% to 21%), U mass fraction (69% to 71%), Pu isotopics (Pu-239/Pu mass fractions ranging from 0.6 to 1.0), and U isotopics (U-236/U-235 mass ratios ranging from 0.01 to 0.285). These variations were considered individually (to isolate the effect of each variation) and collectively. The individual effects were found to be very nearly additive and can probably be represented adequately as linear functions of the variation magnitude. Again allowing for the largest observed deviations $\delta\alpha_i$ along with the U-235/U mass ratio that maximizes y , the increase in y required to accommodate the maximum variation of each type was found to be as follows:

• Regional variation of the α_i (as previously discussed)	0.21%
• Pin mass variation	0.26%
• Pin plutonium mass fraction variation	0.16%
• Pin uranium mass fraction variation	0.03%
• Plutonium isotopic fraction variation	0.04%
• Uranium isotopic fraction variation	0.27%

The increment associated with Pu isotopics is surprisingly small and is attributable to the compensating effects of the different Pu isotopes and of the associated adjustment of the U-235/U mass fraction made to satisfy Eq. (1). The relatively large increment associated with U isotopics is caused by the large (factor of ~30) U-236/U-235 ratio variation considered.

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The arithmetic sum of the above individual increments (0.97%) is in good agreement with the increment (0.99%) computed with the variations considered simultaneously. Accordingly, the bounding value of y needed to *accommodate all extreme variations simultaneously* is approximately $x + 1.0\%$, i.e. $y = 5\%$ for $x = 4\%$. A decrease of y would require some restriction of the above-considered variations. Based on the foregoing numerical results, restriction of the allowed U-236/U-235 mass ratio to a value < 0.05 would by itself permit y to be reduced to about 4.8%.

The mid-range values of α_i used to estimate of the bounding value of y , and recommended for use in the Mark-V fuel specification, are shown in Table 1. These values were computed using the updated isotope fission and capture energy-release factors provided in reference 3.

Table 1: Nuclide specific power parameters.

Nuclide, i	Recommended (Mid-range) α_i	Maximum α_i	Minimum α_i	Current α_i (Reference 1)	Previous α_i (Reference 4)
U-234	0.411	0.495	0.326	0.47	0.51
U-235	1.000	1.000	1.000	1.00	1.00
U-236	0.138	0.171	0.106	0.16	0.20
U-238	0.059	0.074	0.043	0.07	0.10
Pu-239	1.192	1.257	1.126	1.21	1.23
Pu-240	0.436	0.527	0.346	0.50	0.51
Pu-241	1.355	1.367	1.342	1.30	1.34
Pu-242	0.339	0.415	0.263	0.39	0.41

In summary, the recommended isotope specific power parameters shown in Table 1 should be utilized in the fuel specification. The upper bound y on the variability of the local pin power resulting from the use of these α_i parameters was shown to exceed the tolerance x in pin fissionable (equivalent U-235) mass as a result of the spatial dependence of the α_i and the deviations of pin nuclide masses from nominal. The magnitude by which y exceeds x is at minimum 0.2% (to account for spatial variations of the α_i) and at maximum 1.0% (to account additionally for simultaneous extreme variations in fuel pin mass and composition).

References

1. Draft Specification, "Mark-V and -VA Fuel Elements," December 1, 1993.
2. H. Khalil, "Power and Reactivity Deviations Resulting from Mark-V Fuel Composition Variations," ANL Intra-Laboratory Memo, November 2, 1993.
3. R. N. Hill and R. P. Jacqmin, "Updating Isotopic Energy Conversion Factors," ANL Intra-Laboratory Memo, June 4, 1993.
4. P. J. Finck, "Mk-V Fuel Specifications-Fissile Masses," ANL Intra-Laboratory Memo, August 2, 1993.

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C. Temperature Gradient Driven Constituent Redistribution in U-Zr Alloys

1. Introduction

In order to understand the constituent redistribution in U-Pu-Zr alloys, which can be very complex phase systems, the understanding of redistribution in U-Zr, a simpler system, can be used to provide theoretical guidance. It was the purpose of this analytical study to investigate and model the postulated thermo-transport of atomic species in the U-Zr system. The manifestation of thermo-transport in nuclear fuel has been well characterized in other types of fuel rods, such as oxygen and plutonium transport in mixed oxide fuels.⁵ The first observations of this phenomenon in metallic U-Pu-Zr fuel was reported by Murphy et al.,⁶ who identified three different radially concentric microstructural zones primarily resulting from radial Zr and U redistribution during irradiation. Renewed interest in metallic fast reactor fuel has yielded additional postirradiation data⁷ that confirm the initial observation and show that substantial radial redistribution of U and Zr occurs at relatively short radiation exposures or burnup (Fig. II.1). It is observed that the net migration of Zr in the ternary U-Pu-Zr alloy can be either up or down the temperature gradient depending on the local fuel temperature.

Constituent redistribution in metallic alloy fuels leads to radial changes in (a) the local fuel melting temperature, (b) the local power production, and (c) the temperature profile. The radial power and temperature profiles determine the birthplace of fission products and their subsequent migration, and ultimately affect fuel-cladding chemical interaction, which is believed to be the life-limiting phenomenon for metallic fuel elements. Thus, understanding and predictive capability of the constituent redistribution in metallic alloy fuels is of significant importance.

Others have attempted to model the constituent redistribution in metallic fuel. Ishida et al.⁸ attempted to model redistribution in U-Pu-Zr; however, far-reaching assumptions and approximations were required in order to obtain a model usable in a fuel performance code. The reason for this is the paucity of thermodynamic and diffusion data for the ternary U-Pu-Zr alloy. To circumvent this problem, these researchers modeled the simpler and better

Fig. II.1. Metallographic Cross Section with Superimposed Radial Microprobe Scans of Element T179, U-19Pu-10Zr, at 8.5 in. from Bottom of Fuel

characterized binary U-Zr alloy and attempted to extrapolate this work to the U-Pu-Zr system. Similarly, Ogawa et al.,⁹ developed a model for constituent redistribution in U-Zr alloys without attempting to extrapolate to the U-Pu-Zr system. The problem with these previous modeling attempts is that until recently constituent redistribution in U-Zr alloys had not been definitively observed. It was, therefore, impossible to validate the binary alloy redistribution model, making an extrapolation to the ternary alloy tenuous. This section treats recent postirradiation data on U-Zr fuel, irradiated to 10 at. % burnup at high temperature in which constituent redistribution is definitely observed and characterized. A mechanistic thermo-transport model is proposed to describe the U-Zr redistribution phenomenon, which is incorporated into a computer subroutine capable of eventually interfacing with a general fuel performance code such as LIFE-METAL. Comparison of the model calculations with experimental data shows good agreement.

2. Experimental

The fuel elements in this experiment, designated subassembly X447, consist of injection cast U-22(± 1) at.% Zr pins with a diameter of 4.34 mm and a length of 340 mm. These pins were clad in 600 mm long austenitic (D9) or ferritic (HT9) stainless-steel tubes having diameters of 5.84 mm and a wall thicknesses of 0.38 mm. The annulus between the fuel pins and cladding contained sodium to provide a thermal bond. The irradiation experiment was performed in the Experimental Breeder Reactor II (EBR-II) at a maximum linear power of 330 W/cm to a peak heavy metal burnup of 10 at.%. The upward sodium coolant flow, with a core inlet temperature of 371°C, resulted in peak cladding temperatures in the range of 630 to 660°C occurring at the top of the fuel pin. Postirradiation examinations were performed at 5 at.% peak burnup, reached after ~ 284 equivalent full power days (EFPDs) and 10 at.% burnup after ~ 619 EFPDs. Neutron radiographs indicated radial zone formation near the top of the fuel pins (i.e., in the highest temperature part of the fuel). These locations were selected for detailed metallography and electron microprobe analysis; two such sections are shown in Figs. II.2 and II.3. The similarities between the U-Zr samples and the U-Pu-Zr sample, shown in Fig. II.1, are obvious. Both fuels developed a porous, Zr-enriched center and an apparently dense Zr-depleted intermediate zone. However, there are some key differences. The intermediate zone in U-Pu-Zr is completely depleted of the original high Zr bcc phase and indeed of nearly all Zr. A comparison of the amount of Zr lost from this zone and the amount gained by the center zone leads to the conclusion that a significant fraction of Zr must also have migrated radially outward. Sections taken at elevations corresponding to lower fuel temperatures show depleted centers in U-Pu-Zr elements,⁷ which can only be explained by outward Zr migration (i.e., Zr migration down the temperature gradient). In contrast, evidence of outward Zr migration has not been found in the binary U-Zr fuel. In the latter, the depleted intermediate zone retains a fair amount of the high Zr-phase [which is the cause of the irregular Zr composition profiles observed in the binary fuel sections (Fig. II.4) compared to those for the ternary fuel] and the amount of Zr lost from the depleted intermediate zone balances the amount gained in the enriched center.

Fig. II.2. Metallographic Cross Section with Superimposed Radial Microprobe Scans at Top of Fuel Element DP-81 at 5% Peak Burnup (as polished)

Fig. II.3. Metallographic Cross Sections (etched) at 14 in. from Bottom of Fuel Pin, DP-11, at 10 at. % Peak Burnup (left), and 12 in. from Bottom (right) with Superimposed Radial Microprobe Scan at 14 in. Increase in diameter of central γ -phase zone at 14 in. indicates higher fuel temperature toward top of fuel pin.

Radial composition profiles were obtained with a defocused electron beam having a sample spot of $40 \times 40 \mu\text{m}$. Two examples are shown in Fig. II.4; one taken at an elevation 32.6 cm from the fuel bottom ($X/L \approx 0.95$) of element designated DP-81 having a peak element burnup of 5 at.%, and another taken at an elevation of 31.9 cm from the fuel bottom ($X/L \approx 0.93$) of the element designated DP-11 having a peak element burnup of 10 at.%. The large irregularities in the scans are primarily due to the two-phase microstructure of the alloy. Absolute compositions in each of the three radial zones, given in Table II.3, were quantified using U and Zr standards.

Fig. II.4. Radial Microprobe at Top of Fuel Pins DP-81 (5 at.% peak burnup) and DP-11 (10 at.% peak burnup)

TABLE II.3. Composition in at. % of Bands in U-Zr Sample A/G 395A2 (10% Burnup). Each value is the average of measurements taken at five similar locations.

Description	Phase	Uranium	Zirconium
Fuel Central Zone	γ	52	48
Intermediate, Dense Zone			
High-Z major phase	β	99	~ 1
Low-Z minor phase	γ_2	60	40
Outer Zone			
High-Z major phase	α	99	~ 1
Low-Z minor phase	γ_2	48	52

With the aid of the U-Zr phase diagram (Fig. II.5), the composition profiles may be interpreted in the following way. By 5 at. % burnup, the maximum Zr concentration in the center zone has increased from the initial 23 at. % to approximately 45 at. % and consists of a single phase (γ -U-Zr). The Zr concentration in the intermediate two-phase zone has dropped commensurately resulting in a reduction of the high Zr γ -phase fraction. By 10 at. % burnup, additional diffusion has diminished the Zr concentration gradient in the central zone, but the Zr concentration in the center zone has increased only slightly to ~ 48 at. %. As indicated in the phase diagram, 45 at. % Zr roughly corresponds to the point where the γ phase at the fuel center is in equilibrium with the γ_2 phase in the intermediate zone. Any further increase in Zr concentration at this location indicates a lower temperature. This can only be accomplished by transforming a corresponding temperature range of the intermediate two-phase zone ($\gamma + \beta$) that exists at lower burnup back to the single high-Zr γ phase. This postulated interpretation suggests the possibility of utilizing these zone boundaries as reliable temperature markers, which would permit a more accurate reconstruction of the radial temperature profile in fuel pins.¹⁰ Evaluating this possibility is an important motivation for the modeling effort described in the subsequent sections.

Fig. II.5. Partial U-Zr Phase Diagram with Approximate Radial Composition at the Top of Fuel Pins at 0, 5 and 10 at.% Peak Burnup

3. Model Development

The mass flux of a species subject to potential gradients can be calculated using the phenomenological equations of nonequilibrium thermodynamics and Onsager's reciprocity theorem.¹¹ To simplify the model, however, it is assumed that (1) the cross terms in the phenomenological equations are negligible, and (2) the uranium flux in the interdiffusion region is equal to the negative of the zirconium flux (i.e., the fluxes are observed in a fixed reference system represented by the fuel element cladding and the radial centerline of the fuel pin). This assumption is justified by the observation that a practically stationary situation develops early during the irradiation period; i.e., by approximately 0.5 at.% burnup. During further irradiation, the fuel remains in contact with the cladding, and no void at the fuel pin centerline develops (Figs. II.2 and II.3).

In the composition and temperature ranges of interest, both single and dual phase fields exist (Fig. II.5). Within a single-phase field, the Zr flux is calculated as

$$\tilde{J}_{Zr} = -\tilde{D} \left(\frac{\partial C}{\partial r} + \frac{\tilde{Q}^* C}{RT^2} \frac{\partial T}{\partial r} \right), \quad (1)$$

where C is the Zr concentration (mol/cm^3), R is the universal gas constant ($\text{J}\cdot\text{mol}^{-1}\cdot\text{K}^{-1}$), and r denotes the radial dimension in the cylindrical fuel pin. $\tilde{D}$ is the U-Zr chemical or interdiffusion coefficient (cm^2/s), defined by the Darken equation as

$$\tilde{D} = X_{Zr} D_u + X_u D_{Zr}, \quad (2)$$

where X_{Zr} and X_u are the Zr and U mole fractions, respectively.

The thermo-transport parameter $\tilde{Q}^*$ ($\text{J}\cdot\text{mol}^{-1}$) is the apparent process heat of transport defined as the heat associated with the redistribution of one mole of Zr or U in the fixed referenced system.¹⁷

In a dual phase field, the approach of Shewmon¹² is employed. The validity of Shewmon's treatment has been verified by several experiments, mainly at Iowa State University, with dilute alloys; C in Fe and H in Zr, Shewmon;¹² Co in Fe-Ni, Okofar et al.;¹³ and Co in Th, Axtell and Carlson.¹⁴ Assuming conditions of local equilibrium, the solid solubility limit of Zr in the γ_2 phase varies with temperature as

$$C = C_o \exp \left[-\frac{\overline{\Delta H}_s}{RT} \right], \quad (3)$$

where $\overline{\Delta H}_s$ is the partial molal enthalpy of solution (J/mol) of Zr in U. It is apparent in the phase diagram, shown in Fig. II.5, that the solubility of Zr in the γ_2 phase decreases with increasing temperature in the two-phase regions existing under the present experimental conditions. Differentiation of Eq. (3) yields:

$$\frac{\partial C}{\partial r} = C \cdot \frac{\overline{\Delta H}_s}{RT^2} \cdot \frac{\partial T}{\partial r}. \quad (4)$$

Substitution of Eq. (4) into Eq. (1) gives the expression for calculating the Zr flux in a dual phase field:

$$J_{Zr} = -\tilde{D} \frac{V_f (\overline{\Delta H_s} + \tilde{Q}^*) C}{RT^2} \frac{\partial T}{\partial r}, \quad (5)$$

where the volume fraction of the phase of interest, V_f , has also been introduced.

These flux equations were implemented within a finite-difference based computer program for solving the diffusion equation in radial geometry. The computer program discretizes the radial dimension of the fuel (Fig. II.6), where R is the fuel radius. For the general node P at time t , the concentration of Zr is calculated as

$$C_P = C_P^0 + 2\Delta t \frac{[r_W J_W - r_E J_E]}{[r_E^2 - r_W^2]} + S \Delta t, \quad (6)$$

where C_P is the Zr concentration at time t (mol/cm³), C_P^0 is the Zr concentration at the previous time-step $t-\Delta t$ (mol/cm³), Δt is the time-step size (s), r is the radial distance from the fuel center (cm), J is the Zr flux (mol/cm²-s), and S is the volumetric source of Zr as a stable fission product (mol/cm³-s). Explicit time-marching is employed, and the time-step size is calculated to be

Fig. II.6. Computer Model Discretization Scheme

$$\Delta t < \frac{(\Delta r)^2}{2\tilde{D}}, \quad (7)$$

for stability. The temperature profile within the fuel is computed on the same numerical grid given a fuel surface temperature and linear heat generation rate; the heat generation is radially distributed in proportion to the fissile uranium density, which evolves with time. It was observed, however, that the temperature profile changes slowly during the Zr redistribution, so the temperature profile is updated using time-steps much larger than those used for the diffusion calculation.

The computer program saves the average Zr concentration for each node. Polynomial equations were developed to describe the phase boundaries shown on the partial phase diagram in Fig. II.6. For each time-step and each node, the computer program determines the phase field within which the node lies from the average Zr concentration and temperature of the node. If two phases are present at a node, the computer program computes the mole and volume fractions of each phase, evaluates material properties for both phases, and computes the Zr flux, J_{Zr} , using Eq. (5) for both phases. An option exists within the computer program to discontinue computing the Zr flux through a phase if its volume fraction falls below a user-defined limit.

4. Model Parameters

a. Literature Values

The parameters required by the model are material properties of the δ , α , β , and γ phases. Specifically, values or equations for the interdiffusion coefficients, activity coefficients, and heat of transport of each phase are needed for the analysis in all existing phase fields.

Since none of the required data exist for the intermediate δ phase, the model can either apply properties equal to those for the α phase, or no diffusion is tracked in the δ phase. For this reason, the model is not considered reliable for temperatures below 617°C, where the γ phase is present. This is of no great concern as no discernable constituent redistribution appears to occur in the binary alloy below this temperature.

U-Zr interdiffusion data are unavailable for the α phase, so the diffusion coefficient was assumed to be equal to the uranium self-diffusion coefficient in this phase.¹⁵⁻¹⁷ For the interdiffusion coefficient in the β phase, an equation was developed using the known activation energy of U self-diffusion¹⁸ along with unpublished U-Zr interdiffusion measurements in the β phase.¹⁹ The α and β phases are uranium phases with low solubilities for Zr (Fig. II.5) and are treated as Henrian solutions.²⁰ Thus, the computer program treats the partial molal enthalpy of solution in the α and β phases as negligibly small, compared to that in the high-Zr γ phase. The heat of transport for these phases are considered to be model variables.

The bcc γ phase (γ -U, β -Zr) is by far the most widely studied phase. The computer program uses temperature and Zr content-dependent interdiffusion coefficient equations extracted from the U-Zr interdiffusion data of Adda et al.¹⁵ Furthermore, the relationship for the Zr activity coefficient (γ) given by Leibowitz et al.²⁰ is used to calculate the partial molar enthalpy of solution $\overline{\Delta H_s}$:

$$\overline{\Delta H_s} = -RT^2 \frac{\partial \ln \gamma}{\partial T} . \quad (8)$$

Although, the heat of transport of the γ phase is considered to be a model variable, an estimate of its magnitude can be made from the electron microprobe traces given in Fig. II.4 and will be discussed in the next section. Table II.4 summarizes the material properties used in the computer program.

TABLE II.4. Nominal Model Parameters Employed in the Computer Program

Parameter	Phase		
	α	β	γ
Diffusion Coefficients: D ₀ (cm ² /s) Q (J/mol)	2.0 x 10 ⁻³ 167,480	5.7 x 10 ⁻¹ 175,850	$\log(D_0) = -2.67 - 8.05 X_{Zr} + 9.13 X_{Zr}^2$ $Q = 127574 - 106903 X_{Zr} + 174271 X_{Zr}^2$
Activity Coefficients	negligible	negligible	$RT \ln(\gamma_{Zr}) = X_U^2 (43837 - 22 T - 44206 X_{Zr} + 9234 X_{Zr}^2)$
Heat of Transport	unknown	unknown	-50 to -100 kJ/mol

D = D₀ exp(-Q/RT) = diffusion coefficient

R = gas constant

T = temperature (K)

X_{Zr} = Zr mole fraction

X_U = U mole fraction

b. Estimation of Heat of Transport

The heat of transport for the γ phase can be estimated, without the use of diffusion coefficients, from the microprobe trace taken after irradiation to a peak burnup of 10 at. %, if it is assumed that the Zr-concentration profile has essentially reached its steady-state value (or the continuing mass flux is negligibly small). This appears to be a reasonable assumption since the profile in the γ phase at 10 at. % burnup is not drastically different from the profile taken at 5 at. % burnup. If the γ -phase material (central zone) has reached a steady-state (i.e., $J_{Zr} \approx 0$), Eq. (1) can be manipulated to solve for the heat of transport as

$$\tilde{Q}^* = R \frac{\partial \ln C_{Zr}}{\partial \left(\frac{1}{T} \right)} . \quad (9)$$

Rather than measuring the slope of the Zr concentration gradient from the Zr trace at 10 at. % burnup, it was inferred from the U trace which has a more discernible slope. The temperature

gradient in the inner zone, however, is subject to a considerable degree of uncertainty. The temperature at the intermediate and central zone boundary is assumed to be 692°C (from the phase diagram). The inner zone is a highly-porous region. These pores are presumably interconnected and may communicate with the gas and sodium containing plenum above the fuel pin. The model of Bauer²¹ for the effective conductivity of a medium containing sodium filled pores was employed by Yacout et al.¹⁰ to estimate the fuel centerline temperature for this element. They obtained a centerline temperature of 703°C assuming the pores were fully saturated with sodium, and a temperature of 730°C assuming the pores were nonconducting. This variation in the temperature gradient leads to estimates for $\tilde{Q}^*$ that range from -100 to -200 kJ/mol.

For a comparison, we may use Bober and Schumacher's⁵ definition of the process heat of transport:

$$\tilde{Q}^* = \frac{D_{Zr} Q_{Zr}^* - D_U Q_U^*}{\tilde{D}} \quad (10)$$

With the diffusion coefficients derived from Adda's experiments¹⁵ that are applicable to the composition and temperature in the Zr-enriched γ -phase of the center of the fuel pin (Table II.4), and values for $Q_U^* \approx +20 \text{ kJ}\cdot\text{mol}^{-1}$ from D'Amico²² and $Q_{Zr}^* \approx -120 \text{ kJ}\cdot\text{mol}^{-1}$ from Campbell,²³ we obtain $\tilde{Q}_\gamma^* \approx -80 \text{ kJ}\cdot\text{mol}^{-1}$, a value, perhaps fortuitously, close to our lower estimate of -100 kJ/mol, considering the uncertainties in all the coefficients.

5. Model Calculations

The computer model described previously was used to make calculations based on the conditions experienced by elements DP-81 and DP-11 from experimental subassembly X447, for which the electron microprobe examination results are shown in Fig. II.4. Element DP-81 was examined at a peak element burnup of 5 at. %; the microprobe trace shown in Figs.

II.2 and II.4, however, was taken at an axial elevation 32.6 cm from the fuel bottom ($X/L \approx 0.95$), which was calculated to have a local burnup of 3.6 at. %. Similarly, for element DP-11, which was examined after reaching a peak element burnup of 10 at. %, the local burnup at the elevation of the examination shown in Fig. II.3 (31.9 cm or $X/L \approx 0.93$) is 7.7 at. %.

The temperature profile given by Yacout et al.¹⁰ for element DP-81 at the axial location of the microprobe examination was used as a reference for these calculations; the beginning-of-life (BOL) fuel surface temperature and linear power were assumed to be 627°C and 240 W/cm, respectively. For DP-11, a fuel surface temperature of 637°C and linear power of 250 W/cm were used. The power was distributed radially in proportion to the radial U concentration. During the calculations, the temperature profile was allowed to evolve with the redistribution of the U and Zr constituents, buildup of porosity and subsequent ingress of bond sodium, and the reduction in element power due to fuel burnup. The fully dense U-Zr thermal conductivity was multiplied by a porosity correction factor that changed with burnup to empirically track the evolution of the fuel porosity and eventual ingress of some of the bond sodium. This porosity correction factor decreases from unity at the BOL to a minimum of 0.5 at 0.75 at. % burnup, and then increases linearly to 0.7 by 1.5 at. % burnup; above 1.5 at. % burnup the porosity correction factor remains at the constant value of 0.7. The resulting calculated peak fuel centerline temperatures were in the range of 720 to 735°C and occurred around 0.75 at. % burnup. Calculations were performed with out-of-pile diffusion coefficients and thermodynamic parameters (Table II.4). However, the extent of the calculated constituent redistribution turned out to be negligibly small.

It was necessary to enhance the diffusion coefficients for all phases (α , β , and γ), as calculated by the equations given in Table II.4, uniformly by a factor of 10 to yield the kinetic response matching the experimental observations. The results of the two calculations are shown in Figs. II.7 and II.8. In these two figures, the heat of transport of the γ phase ($\tilde{Q}^*$) is varied parametrically between 0 and -150 kJ/mol. A value of $\tilde{Q}^* \approx -50$ to -100 kJ/mol appears to produce good agreement between the model calculations and the microprobe results. Figure II.9 shows the calculated end-of-life (EOL) γ -phase fractions across the fuel pin radius for both elements DP-81 and DP-11.

Fig. II.7. Model Calculations Compared to Microprobe Results Near Top of Fuel Column for Element DP-81 from Experiment X447 (3.6 at. % local burnup)

Fig. II.8. Model Calculations Compared to Microprobe Results Near Top of Fuel Column for Element DP-11 from Experiment X447 (7.7 at. % local burnup)

Fig. II.9. Evolution of the Radial Distribution of the γ -phase Volume Fraction

6. Discussion

Careful analysis of the results of the model calculations, displayed in Figs. II.7 through II.9, provides interesting insights into the nature of the redistribution mechanism. Moreover, the model calculations aid in the interpretation of the microprobe results, specifically the qualitative and quantitative differences between the microprobe traces for DP-81 versus DP-11.

Figure II.7 shows the results of the model calculation for element DP-81 at 3.6 at. % local burnup; the calculation using $\tilde{Q}^* \approx -100$ kJ/mol yields the best agreement with the DP-81 microprobe trace. The model calculation predicts that three phase fields exist across the fuel radius at this burnup, an annular $\alpha + \gamma$ zone on the periphery of the fuel, and intermediate $\beta + \gamma$ zone, and a central zone of pure γ , as indicated in Fig. II.8 for the $\tilde{Q}^* \approx -100$ kJ/mol calculation. The redistribution calculation for this element, examined at low burnup, was found to be very sensitive to the way in which the fuel temperature is calculated early in the element's life. The burnup dependence of the thermal conductivity, described in the previous section, was

found to yield the best calculation results when compared to the experimental data. At the terminal burnup of 3.6 at. %, the apparent zone boundary at $r/R \approx 0.20$ is the $\beta + \gamma \sim \gamma_1 + \gamma_2$ phase boundary). The calculation indicates the temperature of this zone boundary is $\sim 684^\circ\text{C}$ somewhat to the right of the triple point in the phase diagram, not a large discrepancy considering computational and experimental uncertainties. The apparent zone boundary at $r/R \approx 0.64$ is the $\alpha + \gamma_2 \sim \beta + \gamma_2$ phase boundary that occurs at 662°C .

Figure II.8. shows the results of the model calculation for element DP-11 at 7.7 at. % local burnup; a $\tilde{Q}^*$ in the range of -50 to -100 kJ/mol yields the best agreement with the DP-11 microprobe trace. Since this fuel element went to a much higher burnup, the calculation is much less sensitive than the previous calculation to the temperature variations that occur during the first two percent burnup. For these conditions the model calculation also predicts that three phase fields exist across the fuel radius, an annular $\alpha + \gamma$ zone on the periphery of the fuel, an annular intermediate $\beta + \gamma$ zone, and a central zone that is pure γ ; shown on the figure are the calculated locations of the phase transitions for $\tilde{Q}^* \approx -100$ kJ/mol. The calculation indicates that the apparent zone boundary at $r/R \approx 0.46$ is again the $\beta + \gamma \sim \gamma$ phase boundary (calculated to be $\sim 667^\circ\text{C}$). The apparent zone boundary at $r/R \approx 0.72$ is, as before, the $\alpha + \gamma_2 \sim \beta + \gamma_2$ phase boundary that occurs at 662°C .

Figure II.9 shows the calculated γ -phase volume fractions across the fuel radii at the local burnup at the top of both DP-81 and DP-11. The calculation for DP-81 compares well with the measurements made on the metallographic section shown in Fig. II.2.

As mentioned before, it proved not possible to model the observed U-Zr redistribution with independently measured (ex-reactor) diffusion data for pure U-Zr alloys. However, a tenfold increase in all interdiffusion coefficients results in good agreement between model results and postirradiation data. Such a large increase cannot be justified by scatter in the ex-reactor data. Although there are considerable differences in compositions at the extremes of the phase diagrams between data of Adda²⁴ and Müller²⁵ and those of Fedorov,²⁶ in the Zr

concentration range of 20 to 60 at. % all diffusion data, including those of Petri,¹⁹ agree within a factor of two. It has been shown by Peterson¹⁸ that impurities affect the self-diffusivity of uranium. For example, fast diffusing elements like Co and Fe increase the U diffusion coefficient, whereas Nb—a slow diffuser—has the opposite effect. Considering the large variety of fission product elements present, an evaluation of their specific role appears quite intractable without further experimentation.

In addition to fission products, Pu is produced by neutron capture in ²³⁸U, at a rate of 0.05 at. % per % burnup. Dupuy²⁷ has shown that Pu greatly enhances the interdiffusivity in U-Pu alloys, even at small concentrations. Whether this effect can be extended to U-Zr alloys is not certain in the absence of experimental data.

It is interesting to note that in a related study of swelling behavior of the present U-Zr alloys Rest²⁸ also needed to increase diffusion coefficients in order to model the observed fission gas bubble morphology. The standard irradiation enhanced diffusion mechanisms do not yield a significant enhancement because at the high temperature prevailing in the diffusion zones thermally activated vacancies overwhelm irradiation induced point defects. Rest was able to calculate a tenfold increase of the diffusivity by invoking a mechanism proposed for irradiation induced solute segregation. The diffusing entity in this mechanism is an irradiation induced Zr-interstitial atom pair. In order for this to work, a rather large number of interstitial atoms have to survive recombination with vacancies. This can be accomplished by providing very effective vacancy sinks. These sinks may indeed exist in the form of a high-number density of small fission gas bubbles.

7. Conclusions

Analysis of electron microprobe examinations of two U-Zr fuel elements having peak burnups of 5 and 10 at. %, coupled with the development and validation of a mechanistic model, lead the authors to the following conclusions regarding constituent redistribution in U-Zr fast reactor fuel:

- (1) There exists a driving force for Zr to move up the temperature gradient owing to a negative $\overline{\Delta H_s}$ for the γ phase where it exists in the $\alpha + \gamma$, $\beta + \gamma$, and $\gamma_1 + \gamma_2$ phase fields; Zr depletion occurs primarily in $\beta + \gamma$, rather than $\alpha + \gamma$, due to a low-Zr mobility in the α phase.
- (2) There is evidence, from experimental as well as computational results, for a heat of transport of Zr in the γ phase (a γ -U, β -Zr solid solution) in the range of -50 to -100 kJ/mol.
- (3) As no discernable driving force for Zr transport down the temperature gradient appears to exist, a Zr depleted fuel center will not develop.
- (4) The transport model presented here can only match experimental observations when ex-reactor diffusion coefficients are assumed to be enhanced by irradiation effects.

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III. PYROMETALLURGICAL PROCESS DEVELOPMENT

A. Process Flowsheet and Chemistry Studies

The pyrometallurgical flowsheet for the processing of IFR fuel is being extended to other metallic spent nuclear fuels; no major changes in the basic flowsheet are foreseen. During this last year, a decision was made to focus metal waste form efforts exclusively on the stainless-steel-zirconium waste form; this approach results in considerable process simplification over a previous candidate, the Cu-Al metal waste form. Previous reports in this series have given details of the process flowsheet and chemistry, for which refinement is continuing. To summarize the current reference process: spent fuel in its cladding is mechanically reduced to pieces of approximately quarter-inch size, which are placed in an anode basket. The basket is introduced into the molten LiCl-KCl electrolyte of an electrorefiner, along with sufficient oxidant, such as UCl_3 , to convert the active fission products (alkali, alkaline earth, and rare earth) metals to their chlorides. These remain in the salt, which also contains a concentration of 2 mol% of actinide chlorides. The basket is connected to the positive pole of a dc power supply, then current is passed to a solid cathode, where nearly pure uranium metal is deposited by reduction of uranium chloride. An equivalent amount of actinide metal from the basket is simultaneously oxidized to its chloride. In the same way, a liquid cadmium cathode is used to remove a mixture of uranium and transuranic (TRU) metals; this mixture is contaminated with small amounts of rare earth metals. Cladding hulls and noble metal fission products remain in unoxidized form throughout the process; they are melted under a chloride flux that contains an oxidant, such as CuCl_2 . Actinides and any active metals are found in the salt phase; the salt is periodically reduced to recover them for return to the electrorefiner feed. The metal phase is nearly actinide-free; it is cast into an ingot that is the metal waste form. The electrorefiner salt is periodically purified by reduction of the actinide chlorides in a multistage, countercurrent extraction of trace transuranium actinides that remain using uranium in cadmium, and removal of the active metal fission products by ion-exchange with zeolite A in a column. The column contents are converted to the mineral waste form.

B. Process Development Studies

1. Laboratory-scale Electrorefiner

During the past year a series of runs was completed in the laboratory-scale (15-cm dia) electrorefiner using the liquid cadmium "pounder" cathode. A liquid cadmium cathode is needed for the collection of transuranic elements. The "pounder" design cathode incorporates an axial and rotating motion agitator located above the electrolyte-cadmium interface in the electrode. The pounder agitator is basically an insulated ceramic cylinder which moves up and down and rotates about the cathode current lead. The cathode electrode in the laboratory-scale electrorefiner is a 5-cm (inside dia) crucible that contains a cadmium pool (200-250 g of cadmium). The agitator prevents the uncontrolled growth of heavy metal dendrites into the electrolyte phase above the cadmium interface and causes the cathode deposit to accumulate at high concentration in the cadmium phase.

In the plant-scale electrorefiner a noncritical 3-kg batch of transuranics, uranium, and rare earth fission products will be collected in the liquid cadmium cathode. The solubility of Pu in cadmium at 500° C is about 3.6 wt%. If the cathode is operated only up to the saturation limit, then 80.3 kg of cadmium (10.4 L) would be the required size of the cathode. This is a large cathode and requires a long retorting time to separate the cadmium (by vaporization) from the nonvolatile actinides in the cathode processor. When the solubility of transuranics such as Pu in cadmium is exceeded, an intermetallic compound (e.g., PuCd₆) forms as a precipitate solid phase. One important goal of this development work was to show that a liquid cathode could be developed which could handle the formation of solid in the liquid. The goal was to load at least 10 wt% transuranics into the cadmium. This would produce about 25 vol% solids in the cathode. The plant-scale size cathode would be 3.5 l and only 27 kg of cadmium would be needed to collect the actinides. In this run series it has been shown possible to collect > 10 wt% transuranics cathodically in the cadmium. The greatest concentration of transuranics achieved is 16 wt% which gave 51 vol% solid in the cadmium ingot. For a plant-scale cathode the cathode size would be reduced to 2.2 l and only 15.7 kg of cadmium would be needed to collect the transuranics.

Forty runs were made in development of the pounder cathode. Tables III.1 through III.3 give the highlights of these runs and some results. The first 22 runs were discussed in previous reports of this series. The purpose of these runs was to establish operational feasibility of the design and to investigate geometric and operational variables which were important to size scale-up.

During the first 22 runs of this series a cathode crucible made of dense BeO was used. This crucible had little taper on the inside cylindrical surface, The ingots were difficult to remove. After Run 22 a crucible with a 4° taper was substituted. This crucible was made of AlN. Thereafter the ingots released easily from the crucible. The taper was more important than the crucible material since the Cd ingot does not bond to either BeO or AlN.

For Runs 23-31 the loading of HM in the cadmium was increased and testing included a "pounder" with a greater open area. Increasing the open area of the pounder from 10 to 18% did not decrease the performance of the pounder in handling the solids, but it did decrease the cell resistance slightly as expected. Run 31 achieved the highest loading in the cadmium ingot, 16 wt% Pu and 3 wt% U.

The average collection efficiency for 26 of the runs during the series, Runs 1 through 31, is 97.5%. (For four of the runs no data are available and the low value obtained in Run NE-3 is unexplained and is not included.) The attainment of a high, predictable collection efficiency is important in transuranic collection in the plant-scale electrorefiner for criticality reasons. The number of ampere-hours passed will be used to limit the deposition in the cathode to a noncritical mass. The attainment of near 100% collection efficiency in this experimental work shows that this technique will be a practical approach.

TABLE III.1. Pounder Cathode Development

Runs	Purpose	Result	Problems
1-10	Operation Tests Pu/U >1 ^a	High loading >10 wt % Pu	Product ingots difficult to remove from low taper BeO
11-22	Investigate scale-up variables (Pu/U $\approx$ 2) ^b	Determined limits	Product ingots difficult to remove from low taper BeO
23-26	Repeat with 4° taper AlN crucible	Good release of ingot	
26-31	Variables, loading test	Open area 10% - 18% > 16 wt % Pu, >3 wt % U	
32-40	Run at higher U/Pu ADD RE	Termination due to shorting	Al ₂ O ₃ - Conductive AlN - Reaction

^aApplies to cell monitoring inventory at start of Runs 1 through 10.

^bApplies to cell inventory at start of Runs 11 through 31.

After Run 31 the uranium inventory in the electrorefiner was increased and rare earth chlorides were added to the electrolyte. In subsequent runs, it was necessary to terminate the runs early. In each case shorting occurred; these shorts were due to metal deposition along a narrow path either in the interior surface of the AlN crucible in the electrolyte phase or on the surface of the (Al₂O₃) pounder. The conductive path results from the reduction of the aluminum compounds by the uranium chloride in the salt. In earlier runs the uranium chloride activities were lower and this did not occur. Using only BeO for the fabrication of all the cathode parts should eliminate this problem.

2. Process-monitoring Instrumentation

Electroanalytical techniques have distinct advantages over other analytical techniques that make them particularly well-suited to *in situ* measurements of actinide concentrations in the electrolyte. These techniques fall into two categories: potentiometric and voltammetric.

TABLE III.2. Experimental Results

Test Designation	Operating Time, h	Total A-h	Wt of Product Ingot, g	Wt HM Ingot, g		Collection ^b Efficiency, %
				Analytical	Predicted ^a	
NE-1	7.0	7.0	234.33	17.84	20.79	85.8
NE-2	12.3	12.3	229.35	38.28	36.53	104.8
NE-3	7.0	7.0	218.44	16.67	20.79	80.2
NE-4	13.0	13.0	231.59	36.63	38.61	94.9
NE-5	12.0	12.0	216.27	34.53	35.64	96.9
NE-6	12.0	12.0	238.21	35.11	35.64	98.5
NE-7	12.95	12.95	209.70	12.53	38.46	32.6
NE-8	12.08	12.08	190.35	25.84	35.88	72.0
NE-9	8.43	8.43	225.10	23.61	25.04	94.3
NE-10	2.3	2.3	206.76	6.60	6.83	96.6
NE-11	4.0	4.0	185.12	-	-	-
NE-12	5.3	5.3	211.86	-	-	-
NE-13	5.4	3.07	215.43	-	-	-
NE-14	5.7	13.1	209.34	32.22	38.91	82.8
NE-15	3.15	9.16	183.10	26.30	27.21	96.7
NE-16	6.03	12.06	206.24	25.39	35.82	70.9
NE-17	5.48	10.97	190.77	32.05	36.94	86.8
NE-18	5.5	12.65	274.55	39.52	37.57	105.2
NE-19	2.55	5.87	243.13	16.65	17.44	95.5
NE-20	6.03	13.88	358.27	42.95	41.22	104.2
NE-21	2.30	5.29	260.25	16.41	15.71	104.4
NE-22	2.05	4.72	236.12	18.09	14.02	129.0
NE-23	1.93	4.45	228.95	No sample taken	13.22	-
NE-24	6.15	8.76	248.75	29.25	26.02	112.4
NE-25	5.77	13.26	286.78	45.67	39.38	116.0
NE-26	6.0	13.80	274.94	44.38	40.85	108.6
NE-27	4.8	11.1	280.12	31.81	32.86	96.8
NE-28	5.92	11.35	232.41	33.06	33.71	98.1
NE-29	5.93	14.04	294.07	42.31	41.70	101.5
NE-30	5.82	10.74	288.88	31.86	31.90	99.9
NE-31	8.18	19.14	302.46	58.79	56.85	103.4
NE-32	6.07	12.14	256.54	25.93	35.93	72.2
NE-33	6.00	12	222.47	14.88	35.64	41.8
NE-34	5.45	9.9	250.37	15.65	29.40	53.2
NE-35	6.0	0	269.02	-	0	-
NE-36	3.28	6.32	261.38	13.26	18.77	70.6
NE-37	4.70	9.4	246.14	18.12	27.92	64.9
NE-38	4.60	9.2	254.03	19.08	27.32	69.8
NE-39	5.35	10.70	248.33	18.27	31.78	57.5
NE-40	4.53	7.19	239.98	12.44	21.35	58.3

^a2.97 g HM per A-h used in calculation.^bCollection efficiency is defined as: $\frac{\text{HM Analytical}}{\text{HM Predicted}} \times 100\%$

TABLE III.3. Chemical Analysis of Ingots

Test Designation	Wt % HM		Pu/U in Product
	U	Pu	
NE-1	-	7.61	-
NE-2	-	16.69	-
NE-3	0.41	7.11	17.3
NE-4	0.76	15.06	19.8
NE-5	3.39	11.52	3.4
NE-6	3.61	11.13	3.08
NE-7	3.81	2.164	0.57
NE-8	4.32	9.237	2.14
NE-9	3.02	7.455	2.47
NE-10	1.17	2.009	1.72
NE-11	-	-	-
NE-12	-	-	-
NE-13	-	-	-
NE-14	3.01	12.388	4.12
NE-15	3.00	11.363	3.79
NE-16	2.494	9.818	3.94
NE-17	3.165	13.635	4.31
NE-18	2.66	11.73	4.41
NE-19	1.72	5.127	2.98
NE-20	2.61	9.383	3.60
NE-21	1.416	4.888	3.45
NE-22	1.425	6.236	4.38
NE-23	-	-	-
NE-24	2.574	9.185	3.57
NE-25	2.817	13.111	4.65
NE-26	2.805	13.337	4.75
NE-27	2.469	8.888	3.60
NE-28	2.498	12.298	4.92
NE-29	2.338	12.050	5.15
NE-30	2.401	8.627	3.59
NE-31	3.408	16.032	4.70
NE-32	8.694	1.414	0.163
NE-33	6.669	1.763	0.264
NE-34	4.325	1.924	0.445
NE-35	-	-	-
NE-36	3.164	1.911	0.604
NE-37	4.352	3.008	0.691
NE-38	3.452	4.058	1.176
NE-39	3.536	3.822	1.081
NE-40	2.320	2.862	1.234

Potentiometric techniques measure voltages of a reference electrode or an ion-specific electrode as an indication of chemical activity of a particular species. There are drawbacks to a potentiometric approach. Individual sensors for the specific actinides and rare earths of interest have not been developed. If such sensors were developed, it would require a whole array of sensors to follow the species of interest; these sensors measure chemical activity, not concentration. Concentrations can be determined only if activity coefficients of each species are well known.

Voltammetric techniques can measure multiple species with only three electrodes. Also, these techniques measure concentration, not activity. In all voltammetric techniques a potential waveform is applied to a working electrode and the resulting current is measured. The resulting current is usually proportional to concentration, not activity. The electrochemical cell shown in Fig. III.1 was placed in a working furnace well. As the diagram indicates, the cell has three electrodes: a tungsten-working electrode, a reference electrode, and a cadmium pool-counter electrode. A BAS-100B/W computer-controlled potentiostat was used to apply the potential waveforms and to read the resulting current.

Fig. III.1. Diagram of Laboratory-scale Process Monitoring Equipment

Because square wave voltammetry was found to be the preferable technique, this technique will be described briefly. In square wave voltammetry a potential waveform is applied at the working electrode. This waveform results from the superposition of square and staircase waveforms. The duration of each step must be the same as one-half the period of the square wave. The current is sampled near the end of each half cycle, i.e., near the end of the "forward" and "reverse" pulses. The key waveform parameters are the square wave amplitude and period and the amplitude of the step in the staircase waveform. The "forward" and "reverse" current can be plotted vs. the applied potential or the "forward" minus "reverse" current vs. the applied potential can be plotted. The latter method of plotting gives data that somewhat resembles a chromatogram with peak heights proportional to concentration and with peak position indicating the chemical species.

Semiquantitative simulations of square wave voltammetry analysis of electrorefiner salt have been performed by R. Mariani.¹ The results presented here agree with the simulation results. The simulated response is shown in Fig. III.2. (This figure was taken from Ref. 1 and modified slightly for better comparison with the experimental results.) Note that there are well-resolved individual peaks for U^{3+}/U , Np^{3+}/Np , and for Pu^{3+}/Pu . The rare earths, Sm^{3+}/Sm^{2+} and Am^{3+}/Am peaks are discernable but not always well resolved. Figure III.3 shows an experimental square wave voltammetry spectrum taken in a CsCl-LiCl-NaCl melt after addition of small amounts of $PuCl_3$, UCl_3 , and $DyCl_3$. The concentrations for U, Pu, and Dy as determined by sample analysis were 0.1 wt%, 0.06 wt%, and 0.05 wt%. A small amount of $DyCl_3$ was also added to help identify the mixed rare earth peak. The order of peaks in Fig. III.3 as well as the peak shape agree with the simulation in Fig. III.2. Consider first the U^{4+}/U^{3+} and the U^{3+}/U peaks in Figs. III.2 and III.3. In both cases the U^{4+}/U^{3+} is three times broader and one third the height of the U^{3+}/U peak. The U^{3+}/U should be 1.6 times as tall as the Pu^{3+}/Pu peak in Fig. III.3. The measured peak height ratio is 1.65. Thus the agreement is quite good, even at low concentrations.

Square wave voltammetry can also be used to track changes in concentration during a drawdown. Figure III.4 shows how the U^{3+}/U and Pu^{3+}/Pu peaks decreased during

Fig. III.2. Simulated Square Wave Voltammetry Response for Rare Earths, TRU's, Uranium, and Plutonium after 10 Pins of Ternary Fuel Have Been Processed in the HFDA (Adapted from Ref. 1.)

Fig. III.3. Experimental Square Wave Voltammogram of Molten Salt Containing U, Pu, Dy, and Zr. Identification of Zr peak is speculated but has not been confirmed.

Fig. III.4. Series of Experimental Square Wave Voltammograms Taken During Drawdown in Test Cell. Note the decrease in U waves with increasing amount of charge passed.

a drawdown. The uranium concentration is clearly decreasing as current is passed in a drawdown operation. Future work will focus on obtaining more quantitative results and on generation of a calibration curve using standard additions of PuCl_3 and UCl_3 .

C. Engineering-scale Electrorefiner

1. Electrorefiner Drawdown Operations

The pyrometallurgical process for recycling spent metal fuels from the Integral Fast Reactor (IFR) involves electrorefining spent fuel in a molten-salt electrolyte (LiCl-KCl-U/PuCl_3) at 500°C . At some point, the concentrations of alkali, alkaline earth, and rare earth fission products in the salt must be reduced to lower the amount of heat generated in the electrorefiner. The heavy metal concentration in the salt must be reduced before removing fission products from the salt. The operation uses a lithium-cadmium alloy anode (solid at 500°C) and a solid-mandrel cathode.

A schematic representation of the drawdown operation is shown in Fig. III.5. A lithium-cadmium alloy (5.8 wt% lithium) that is solid at 500°C is loaded into the anodic dissolution baskets and rotated at 75 rpm; current is driven from the anodic dissolution baskets to a solid-mandrel cathode that has a ceramic catch crucible below to collect products that fall off the solid cathode. Salt and cadmium mixers are operated at 150 and 50 rpm, respectively. The possible drawdown reactions include (1) electrotransport of lithium to the solid cathode and lithium reduction of the uranium and rare earth chlorides at the solid cathode and (2) lithium reduction of the uranium and rare earth chlorides at the anode and electrotransport of the uranium and rare earths to the solid cathode. The design criteria that must be met for the drawdown equipment include the following: (1) control of the reduction rate by lithium, (2) good separation between uranium and rare earths, and (3) the capability to collect uranium and rare earths over a wide range of salt concentrations.

The concentrations of uranium in the cathode deposits from the drawdown runs were found to fall into three ranges: low (<0.1 wt%), medium (31.8-39.2 wt%), and high (70-89.3 wt%). The results are summarized in Table III.4. The average uranium concentrations in the salt during Runs 58, 65, 70, and 71 ranged from 6.68 to 1.75 wt%, while the concentrations of uranium metal in the cathode deposits from these runs ranged from 70.0 to 89.3 wt%. These data are plotted in Fig. III.6. The concentrations of rare earth metals

Fig. III.5. Electrorefiner Drawdown Operation

TABLE III.4. Summary of Data from Drawdown Operations in the Engineering-scale Electrorefiner

U in Cathode Deposit wt%	U in Salt, wt%	Run	Rare Earths in Cathode Deposit, wt%
High			
89.3	4.33	65	<0.01
79.5	1.75	71	1.2
73.2	6.68	58	<0.01
70.0	2.98	70	1.1
Medium			
39.2	0.72	75	1.8
32.4	0.14	76	2.7
31.8	1.3	72	2.8
Low			
<0.1	0.015	77	31.8

Fig. III.6. Uranium Concentration in Cell Electrolyte and Cathode Deposit

(cerium, neodymium, and yttrium) in the cathode deposits were low (1.2 wt% or less). These results are shown in Fig. III.7. The cathode deposit is made up of deposited metal and occluded electrolyte. The range of average uranium concentrations in the salt during drawdown Runs 72, 75, and 76 was 0.14 to 1.30 wt%. The range of uranium metal concentrations in the cathode deposits from these runs was 31.8 to 39.2 wt%. These results

Fig. III.7. Rare Earth Concentration in Cell Electrolysis and Cathode Deposit

are plotted in Fig. III.6. The concentrations of rare earth metals in the cathode deposits from Runs 72, 75, and 76 were 2.8, 1.8, and 2.7 wt%, respectively. These results are shown in Fig. III.7. Over this range of uranium concentration in the salt, the concentrations of rare earths in the cathode deposits were still low (1.8 to 2.8 wt%, as shown in Fig. III.7). The average concentration of uranium in the salt during drawdown Run 77 was 0.015 wt% , and the concentration of uranium in the cathode deposit from this run was <0.1 wt%. This result is also plotted in Fig. III.6. The concentration of rare earths in the deposit was 31.8 wt%, which is shown in Fig. III.7.

Good separation can be achieved while removing uranium and rare earths in the salt from the engineering-scale electrorefiner. Only 13% of the rare earths were removed, while 99.9% of the uranium in the salt was removed. The uranium and rare earth concentrations in the salt were reduced to <0.01 wt%, using the *in-situ* drawdown techniques described.

2. Production of Low-salt Deposits

The deposition of uranium on a solid iron cathode surface has been studied in the engineering-scale electrorefiner under a variety of cell operating conditions. When unclad U-Zr or stainless-steel clad U-Zr is the anode feed material, cathodes with high, low, or no zirconium can be obtained. The salt (0-150 rpm) and cadmium (0-50 rpm) mixing rates, electrodeposition current (20-300 A) and voltage 0.2-0.8 V, and quantity of electricity (698-2517 Ah/kg uranium) passed through the electrorefiner were varied over a wide range of operating conditions.

The salt mixing rate (0-20 rpm), electrodeposition current (50 A), and voltage (0.2-0.3 V) for the low (1-5 wt% salt) salt-concentration deposit are lower than those used to produce the typical IFR solid-cathode deposit. The quantity of electricity (1108-1263 Ah/kg uranium) passed through the electrorefiner for the low-salt concentration deposit was greater than that (698-865 Ah/kg uranium) used to produce the typical IFR solid-cathode deposit.

A comparison of the low-salt concentration process with that used for simultaneous transport of uranium and zirconium to the solid mandrel cathode also shows some major differences. The low-salt concentration process uses a much lower electrodeposition voltage (0.2-0.3 V) than that (0.7 V) used in the uranium-zirconium codeposition process. The quantity of electricity passed through the electrorefiner is also less for the low-salt concentration process (1108-1263 Ah per kg uranium collected on the solid cathode) than that (1466-2517 Ah per kg uranium) used for the U-Zr codeposition process.

The processing steps used to produce uranium deposits with low-salt concentration will be incorporated into development of the advanced electrorefiner. The high throughput (> 40 kg uranium per h) and large batch-size (100 kg uranium) requirements for these electrorefiners can be met best with the low-salt concentration uranium deposits, which are expected to have a higher packing density (5 to 7 g uranium per cc) than that 1 g uranium per cc achieved with the typical IFR uranium deposit. The lower salt content of these cathode

deposits will also reduce the processing time in the cathode processor, which is used to consolidate the heavy metal and to separate the salt from the heavy metal.

D. Electrorefiner Waste Treatment Processes

As spent fuel batches are processed in the electrorefiner, fission products accumulate in the vessel. Fission products of the alkaline earth, alkali metal, and rare-earth groups build up in the electrolyte salt phase. The transition metal fission products tend to remain in the anode baskets together with the cladding hulls. Periodically, the electrolyte salt is removed from the electrorefiner for treatment to recover any TRU elements and remove a sufficient quantity of heat-generating fission products so that the salt can be recycled. During this reporting period, experimental process development studies have been carried out on (1) the use of a pyrocontactor (high-temperature centrifugal contactor) to investigate salt/metal extractions and (2) the pumping and filtration of salt and cadmium. Also, a new facility, the Waste Form Development Laboratory, is being built which will house engineering-scale equipment associated with the preparation of mineral and metal waste forms.

1. Salt Extraction

Several steps in treating the salt from the electrorefiner employ the use of pyrocontactors (high-temperature centrifugal contactors) to perform salt/metal extractions and recover TRU elements from the salt. A pyrocontactor test system was built and tests were carried out with molten salt and cadmium at 500°C. The pyrocontactor used was a compact single-stage unit with a 4-cm-dia rotor.

Three series of extraction tests were run. In the first test series, cerium was dissolved in cadmium and CdCl_2 was mixed with the LiCl-KCl eutectic salt. In the second test series, lithium was dissolved in cadmium, and CeCl_3 was mixed with the eutectic salt. The objective of the first and second test series was to demonstrate that the pyrocontactor could effectively mix liquid metal and salt, effect extractions between the phases, and separate the

phases at 500°C. This was accomplished by conducting simple extraction tests with the contactor using one quantitatively extractable element, such as the reaction between cerium and CdCl_2

and between lithium and CeCl_3 .

The objective of the third test series using cerium in cadmium to separate lanthanum from yttrium in the eutectic salt containing LaCl_3 and YCl_3 was to simulate the extraction of using uranium to separate the TRU elements from the rare earths in the electrorefiner spent salt containing $\text{TRU}(\text{Cl})_3$ and $\text{RE}(\text{Cl})_3$.

This was possible because of the set of separation factors (SF) for TRU and rare-earth elements relative to uranium are similar to the separation factors for lanthanum and yttrium relative to cerium.²

Extraction efficiencies were determined during each test series based on the ratio of the measured transfer of a constituent compared to the predicted transfer. The predicted transfer at equilibrium conditions was based on the XTRACT code.³ The average feed rates of salt and metal to the pyrocontactor were determined from load-cell data reflecting the quantities of material remaining in the feed tanks as a function of time. Samples of feed and effluent streams were collected and analyzed by Inductively Coupled Plasma-Atomic Emission Spectroscopy (ICP-AES).

In extraction tests with Ce in Cd and CdCl_2 in salt, the extraction efficiencies were determined from the measured and calculated transfer of Ce from the metal to the salt phase. The average extraction efficiencies were on the order of 20, 70, and 90% at contactor speeds of 2100, 2400, and 2700 rpm, respectively.

In the extraction tests with Li in Cd and CeCl_3 in salt, the extraction efficiencies were determined for the transfer of Li from the metal to the salt and Ce from the salt to the metal. The average extraction efficiencies were on the order of 50, 70, and 90% at contactor speeds of 2100, 2400, and 2700 rpm, respectively.

In the extraction tests with Ce in Cd and LaCl_3 and YCl_3 in salt, the extraction efficiencies were determined for the Ce, La, and Y transfer in the metal and salt phases. The average extraction efficiencies were on the order of 41, 47, and 70% at contactor speeds of 2100, 2400, and 2700 rpm, respectively. In this Ce- LaCl_3 - YCl_3 test series, lanthanum was successfully extracted from the test salt with yttrium remaining in the salt. Because this system is an effective surrogate for the $\text{U}-(\text{TRU})\text{Cl}_3-(\text{RE})\text{Cl}_3$ system, it should be possible to use the pyrocontactor to extract the TRU elements from the rare earths from the electrorefiner spent salt that contain TRU and RE chlorides using uranium in cadmium.

The test results showed that the contactor provided good mixing and separation of the metal and salt at high contactor speeds with stage efficiencies of 90% achieved at contactor speeds of 2700 rpm. Most of the extraction efficiency data fall within $\pm 20\%$ of the mean values. The material balance data are mostly within $100 \pm 10\%$. The trend of extraction efficiency increasing with increasing contactor speed was consistent throughout these tests. The pyrocontactor ran smoothly without any sign of vibration at speeds up to 2700 rpm. These tests demonstrated that the use of a long drive shaft for the pyrocontactor rotor successfully located the motor bearing away from the high process temperature region. The spindle bearing at the bottom of the contactor prevented rotor vibration caused by the use of a longer drive shaft. The baffles and vanes added to the housing and rotor surfaces enhanced the mixing and pumping of the salt and metal.

The development of a multistage (four-stage) pyrocontactor is proceeding. This multistage contactor will replace the single-stage unit in the test system. The design of a four-stage pyrocontactor is complete and the fabrication of the contactor unit is underway.

2. Salt/Cadmium Filtration Tests

Various steps in the pyroprocessing of spent fuels involve filtration of small amounts of insoluble constituents from the salt or cadmium. These constituents include oxides, nitrides, or carbides which will need to be filtered out of the electrorefiner salt along with any metal particles that may escape from the anode baskets. It may also be necessary to periodically filter insoluble materials such as noble metals from the cadmium. Hence a series of six salt and cadmium filtration tests has been carried out. Four of the six tests employed sintered stainless-steel filter elements manufactured by Pall Corporation and two used woven wire mesh stainless-steel filter elements manufactured by Norman Equipment Company. Two pore sizes for each type of element were employed, 10 μm and 35 μm . Five of the six tests involved filtering salt, and in one test cadmium was filtered. In each filtration test, flow and pressure data were obtained at various pump speeds. Each test involved filtration times of 20-48 h for a cumulative pumping/filtration time of 180 h for the six tests.

The filters were contained in a filter assembly as shown in Fig. III.8. The filters were attached to the pump assembly using a freeze seal connection. The pump/filter assembly was installed in the stripper vessel for these filtration tests. The stripper vessel contained approximately 19 l of cadmium and 57 l of LiCl/KCl eutectic salt which were used in previous stripping tests. The pump assembly could be positioned so that either salt or cadmium could be pumped from the stripper vessel through the filter and then returned to the stripper vessel.

Each filter assembly was instrumented with two internal thermocouples located at the top of the filter element that were used to indicate that the pumped fluid had entered the filter housing. A force-based flow probe assembly and a pressure transducer to indicate filter housing pressure were also part of the filter assembly instrumentation. The pump discharge line passed

Fig. III.8. Filter Assembly

through the center of the cylindrical filter element terminating at the top of the element. After the fluid entered the filter housing it passed through the filter and returned to the stripper vessel through a pipe that ran coaxially around the pump discharge line.

During the first three tests, problems were encountered with leaks in the filter occurring in the region where the sintered metal filters were welded. The problem was solved with a design change and fabrication of the end pieces for the filter elements at ANL, which were provided to the filter manufacturer. The manufacturer fabricated new filter elements to which they welded the end pieces. This eliminated the necessity of welding to the sintered metal filter element at ANL upon reassembly of the filters. The same strategy was followed during the subsequent fabrication of the woven wire mesh filter elements.

A flow probe which functions by measuring the force exerted by the moving fluid on a spherical probe placed in the flow path was used to measure the flow through the filter assembly. There were some mechanical problems with the flow probe, but modifications have been made and usable data were obtained for several tests.

A pressure probe has been used to measure the pressure in the filter assembly housing. This pressure measurement is the sum of the pressure drop across the filter element plus a pressure drop along the fluid return path. Measurement of the filter housing pressure can be an aid in determining the flow rate through the filter. In some tests, the pressure data were as expected, i.e., increasing pressure with increased pump speed. However in other tests, the filter housing pressure drifted down and decreased with time and sometimes even became negative. It is believed that this effect is due to the establishment of a siphon in the discharge line of the filter under conditions where the resistance downstream of the filter is larger than the resistance of the filter. Under these conditions it will be necessary to rely solely on the flow probe for measurement of the fluid flow.

Several of the filter housings have been cut open and examined for evidence of filtration of particulate material. A residue that remained in the filter housing during the filtration

of cadmium with a 35- μm -sintered stainless steel was found to contain a uranium-iron-intermetallic particulate, UFe_2 , ranging in size from 0.1 μm up to 10 μm . Also, a thin layer of black material on the filter element during the filtration of salt with a 10- μm -sintered stainless-steel filter consisted of UO_2 as one of the major components. These results are very encouraging as they demonstrate the use of these filters to remove particulate materials from both the salt and cadmium streams.

3. Engineering-scale Process Demonstration

A new facility, the Waste Form Development Laboratory, is being built which will house engineering-scale process demonstration equipment. Three negative pressure, argon atmosphere gloveboxes will be used. The first glovebox will contain the multistage countercurrent flow centrifugal pyrocontactor which will be used to investigate salt/metal extractions. A high temperature tilt-pour induction melting and casting furnace capable of operating at 2200°C which can melt and alloy 2.5 kg of simulated waste-form materials under a molten salt flux has been installed in the second box. The third glovebox will house a zeolite ion exchange column to obtain information on the kinetics of exchange and the stability and characterization of various pelletized zeolites (including binder materials). Additional equipment associated with the preparation of the mineral waste form will include apparatus to remove excess salt from the loaded zeolite, high-temperature blending equipment and a hot isostatic press capable of operation at 1450°C and 200 MPa. Operation of this laboratory is expected early in 1995.

E. High-level Waste Form Development

Two waste forms—a mineral form and a metal form—are being developed to contain the fission products from the pyrochemical treatment of spent nuclear fuel. The mineral form contains the fission products (rare earth, alkali and alkaline earth metals, halogens and chalcogens) that accumulate in the electrolyte salt; the metal form contains the cladding hulls, zirconium, and noble metal fission products, which are chemically unaffected by the pyrochemical process.

1. Mineral Waste Form Development

Glass-bonded zeolite and sodalite are candidate materials for the mineral waste form; both are formed from Zeolite A. The fission products are removed from the process salt by ion exchange with zeolite A that has been pre-equilibrated with LiCl-KCl. The fission product loaded zeolite and whatever liquid salt wets its surface are mixed with Zeolite A that contains no salt at about 725 K to sorb the liquid salt. The resulting material is either (a) mixed with glass powder and formed into a monolith of glass-bonded zeolite, or (b) converted to sodalite by heat treatment.

Fabrication parameters for making both waste forms are being determined. Laboratory-scale procedures—preparation of salt-loaded zeolite, ion exchange, blending, conversion to sodalite, and densification—have been mostly done with zeolite powders. Work is now under way to select a pelletized zeolite and to begin engineering-scale work. The performance of glass-bonded zeolite and sodalite is measured using the MCC-1 leach test procedure. The results are comparable to the performance of borosilicate glass.

a. Glass-bonded Zeolite

Glass-bonded zeolite was prepared using zeolite A powders and a boroaluminosilicate glass frit containing 7 wt% strontium and 8 wt% calcium. The samples were leached tested following the MCC-1 procedure for 28 and 56 days at 363 K in deionized water and in a concentrated NaCl-KCl-MgCl₂ solution. Leach performance is reported as a normalized (to concentrations, test duration, and surface area) release rate (NRR). The leach test results are summarized in Table III.5. In the deionized water leach tests, the normalized release rates for all elements were less than 1 g/m²d when the zeolite phase constituted 50 to 66 wt% of the zeolite-glass mixture. The NRR for the 56-day tests were about half of the 28-day NRR, indicating that the initial release predominates as it does in borosilicate glass.

The brine leach tests were run to determine if the zeolite phase functioned as an ion exchanger. The NRR in brine were about the same as those in deionized water for

TABLE III.5 Leach Tests as Normalized Release Rates

Duration (d)	28	56	28	28
Leachant	Water	Water	Brine	Water
Al	0.30	0.13	BDL ^a	0.28
Ba	0.21	0.08	0.43	0.22
B	0.31	0.19	0.49	0.35
Ca	0.19	0.14	0.70	0.17
Cs	0.60	0.32	0.57	0.61
K	0.73	0.38	b	0.63
Li	0.60	0.35	0.42	0.54
Na	0.67	0.37	b	0.71
Si	0.25	0.11	0.15	0.23
Sr	0.23	0.11	0.51	0.24

^aBDL=below detection limit.^bIons in brine.

the elements only in the zeolite phase (cesium and lithium) but higher for the elements only in the glass phase (boron and calcium), suggesting that ion exchange with the zeolite is not significant and that the ion exchange properties of the glass are greater than those of the zeolite. The NRR for glass-bonded zeolite prepared with rare earths (cerium, lanthanum, neodymium and yttrium) were less than 0.01 g/m²d. The NRR for the other elements were about the same as in Table III.5. Thus, the trivalent elements are retained to a greater extent than the uni- and divalent elements.

b. Ion Exchange

Kinetics of fission product ion exchange into zeolite were measured in a series of timed batch-exchange experiments. Clay-bound zeolite pellets were immersed in a molten simulated process salt for 10 min to 72 h. Results are shown in Fig. III.9. The ion-exchange process can be approximated by first-order kinetics. The divalent cations go through

Fig. III.9. Batch Ion Exchange Kinetics for Zeolite A Pellets

a concentration maximum after 6 h; they are displaced by the trivalent cations. Cesium exchange was rapid; its concentration remained constant.

c. Sodalite

Sodalite powders were synthesized from salt-occluded zeolite A, which was blended with dehydrated zeolite A to reduce the chloride ion content of the zeolite to the capacity of sodalite, nominally two chloride ions per sodalite unit cell. The zeolite mixtures were heated to >700 K for at least 24 h. X-ray diffraction analysis of the resulting powders indicated sodalite was the major crystalline phase. Nepheline was usually present as a minor phase. Washing of the powders indicated significant residual free salt after synthesis, particularly in samples with a high nepheline content. The powders were consolidated by hot-pressing, resulting in hard, polishable samples with densities between 2.2 and 2.4 g/cm³. There were no changes in composition between the initial powders and the resulting pellets after hot pressing at 1325 K.

Samples were leached in deionized water for 28 days at 363 K; mass losses were much lower for samples that had been washed to remove residual free salt prior to hot pressing. This indicates that residual free salt remains a problem in the production of a sodalite-based waste form.

d. Scale-up

All engineering-scale operations except dehydration and hot-pressing will be conducted in a large stainless steel zeolite glovebox [4 modules (168 in.) long; 2 modules high (84 in.), and 44 in. wide]. Installation is underway.

A zeolite column system is being fabricated to allow for columns of differing sizes to be studied. The column system is contained within an internally heated well. The zeolite column is immersed in a vessel containing the salt feed. A second vessel will serve as the product receiver vessel. The system is completely symmetric; either vessel may serve as the source or receiver, allowing the bulk of the salt to be reused in subsequent runs.

2. Metal Waste Form

The metal waste stream consists of noble metal waste constituents, such as metallic fission products filtered from the electrolyte salt (e.g., Mo, Zr, Ru, and Tc) and fuel cladding and process materials. The waste form alloy is an iron-zirconium-chromium-nickel alloy with additions from the noble metal fission products and minor elements in the fuel cladding. A systematic study of this alloy system is being carried out to understand the physical metallurgy of candidate waste forms and to develop optimal processing parameters.

HT9, 304-, and 316- stainless steels have been used to represent EBR-II blanket- and driver-cladding materials that have been considered for conditioning. Some fuel alloys contain significant amounts of zirconium that would be directed to the metal waste form. Waste form alloys with compositions ranging from 5 wt% Zr up to 20 wt% Zr were generated to represent potential waste form compositions for spent nuclear fuels with stainless-steel cladding;

cladding; there is an eutectic at ~ 15 wt % Zr in the Zr-Fe binary system. In addition, 304-SS alloys with 67, 83, and 92 wt % Zr were generated to represent potential waste forms for spent nuclear fuels with Zircaloy cladding; there is another eutectic at ~ 87 wt % Zr in the Zr-Fe binary system. Several alloys of each type were generated using the noble metal elements Ru, Pd, and Ag to simulate the noble metal fission products. The alloy samples that have been generated have been uniform and the phase structures are being characterized.

General corrosion tests were performed on the waste form alloys. Samples were immersed in simulated J-13 well water of pH 6-8 at 363 K (90°C) and 1 atm of pressure. The J-13 well water is representative of the ground water at the Yucca Mountain site in Nevada that is proposed for a high-level nuclear-waste repository. The waste form corrosion rates have been at the limits of resolution for the tests performed and further testing is proceeding.

A casting furnace and glovebox equipment have been designed and fabricated to scale up the experiments to 2.5 kg alloys. The experiments to be conducted in this facility will investigate alloying, fluxing, TRU extraction, and casting phenomena relevant to the metal waste form. The furnace consists of two separate high-temperature furnaces within a single-furnace chamber: (1) a tilt-pour-induction furnace to melt and alloys and (2) a graphite-resistance furnace to preheat the casting mold and/or to anneal the cast alloys.

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IV. SAFETY EXPERIMENTS AND ANALYSES

A. In-vessel Retention of Core Debris

1. Introduction

Extensive testing in EBR-II¹ and TREAT² has verified the technical basis for the Integral Fast Reactor (IFR) safety concept. In this strategy, no reactor damage or public risk results from either design basis accidents, in which all reactor safety systems function as designed, or from beyond-design-basis double-fault accidents, in which the reactor scram system is assumed to fail in addition to the initial failure. The area of beyond-design-basis safety has traditionally played a significant role in the evaluation of safety performance and the determination of containment requirements for licensability of liquid-metal-cooled reactors.

This report summarizes the findings of a task force assembled to investigate the potential for the IFR to provide public safety protection in a third class of postulated accident sequences. This accident class, in which the reactor suffers neutronic and/or thermal upset conditions sufficient to cause partial or whole-core fuel melting, is characterized by accident frequencies much lower than those for the extremely unlikely beyond-design-basis, double-fault accident sequences. In general, it is necessary to assume triple-fault accident initiators (e.g., loss of power to the coolant pumps, and failure of the reactor scram system, and seizure of the pumps simultaneously with the power failure) in order to overcome the passive safety mechanisms and to provoke fuel damage in the IFR concept.

The task force was given the responsibility to investigate the technical basis for evaluating the response of IFR to an assumed severe accident (i.e., core meltdown), and to assess the potential for in-vessel retention of the core debris in a permanently subcritical and coolable condition. (In-vessel retention of core debris maintains two barriers to public exposure, the reactor vessel and the containment building). In addition, the task force was charged with identifying important accident phenomena, characterizing and assessing the impact of

uncertainties, and identifying the additional experimental and analytical requirements necessary to reduce uncertainties to a level acceptable for licensing the IFR concept.

2. IFR Severe Accident Phenomena

a. Reactor Design Features

The principal safety-related features of the IFR concept that assure passively safe response are 1) liquid metal cooling, 2) a pool-type primary system, and 3) an advanced ternary alloy metallic fuel.

The IFR is cooled with liquid metal sodium, which has a relatively high specific heat capacity and thermal conductivity, and a normal boiling point far above the maximum operating temperature. This allows the reactor to operate near atmospheric pressure, and permits the primary circuit to be housed entirely in a single, thin-wall reactor vessel. Depressurization accident sequences are thereby eliminated as a cause of loss-of-cooling, and the large heat capacity of the primary system provides a significant thermal buffer in accidental transients. In severe accidents, the liquid sodium provides an effective decay heat removal medium and the pool-type primary system provides for retention and containment of coolant and core debris.

The most significant safety aspects of the IFR result from its unique fuel design, a ternary alloy of uranium, plutonium, and zirconium. This fuel was developed at Argonne based on experience gained through more than thirty years operation of the EBR-II reactor with a uranium alloy metallic fuel. In the IFR concept, the ternary alloy fuel is injection cast as cylindrical pins and placed inside the cladding. Liquid sodium bond in the fuel-cladding gap provides an efficient heat transfer medium that, along with the high fuel thermal conductivity, maintains low fuel operating temperatures. The fuel-cladding gap is sized for a low smear density (typically 75%) to accommodate irradiation-induced fuel swelling and to permit high burn-up.

b. Chemical Interactions

There are no chemical interactions between liquid sodium and steel cladding and structures, or between sodium and metallic fuel. However, there is a eutectic reaction between metallic fuel and steel. This is illustrated by the uranium-iron phase diagram shown in Fig. IV.1.³ The lower-temperature eutectic alloy forms at a temperature of 721°C (1330°F). Preliminary calculations indicate that molten uranium can liquify solid iron by eutectic reaction without the addition of any heat of fusion. Another important aspect of the eutectic interaction is the rate of penetration. The reaction is very slow up to temperatures around 1100°C where fuel melting begins. At 1100°C, the rate increases by about two orders of magnitude and 1 mm of steel can be liquified in about 1 s.⁴

c. Fuel Melting Accident Sequences

Extensive analyses of the response of metallic fueled reactors to double fault initiators such as loss of flow, loss of heat sink, or transient overpower with failure to scram have shown that the reactors passively adjust their power to match the available heat rejection capability without boiling coolant or melting fuel.⁵⁻⁷ Several tests at the EBR-II reactor confirm the analysis results.¹ In order to initiate coolant boiling and subsequent fuel melting and relocation, at least one additional fault must be assumed. In the case of a transient overpower (TOP) event, many control rods must move at the same time and/or the rod-stop device must fail, leading to the addition of an essentially unlimited amount of reactivity. Alternatively, a catastrophic earthquake might cause the core support structure to give way, allowing the core to fall away from the control rods. In the case of the loss-of-flow (LOF) accident, one must assume that the flow coastdown device fails or that the pump rotors seize. Such a catastrophic pump failure might be caused by a very large earthquake. For a loss-of-heat-sink (LOHS) sequence, it is necessary to assume that the emergency decay heat removal system is degraded or not operational, in addition to failure of the normal heat removal system and the scram system. In any event, such multiple fault initiators have extremely low probability of occurrence.

Fig. IV.1. Uranium-Iron Equilibrium Phase Diagram³

d. Severe Accident Phenomena

(1). Fission Product Behavior

Core debris retention is largely a matter of accommodating the heat generated by radioactive decay. Most of this decay heat is generated by the fission products with small contributions from actinides and structural activation products. To evaluate postaccident behavior, it is important to know the locations and magnitudes of the heat sources during the post-accident period.

Significant decay heat is generated by about 35 fission product elements. These chemical elements have a wide range of physical and chemical properties. Fuel released from the cladding in light water reactors (LWRs) is in an oxidizing environment because of the presence of water and steam. In oxide fueled liquid metal reactors (LMRs), the environment is more reducing, but there is a large quantity of oxygen associated with the fuel.

In a metal-fueled LMR, the environment is reducing and there is very little oxygen available to react with the fission products. This can result in significant differences in expected postaccident fission product behavior, and the need to evaluate the chemical behavior of fission products in the metal-fueled IFR.

The results of a preliminary analysis for IFR indicate that about 30% of the fission product decay heat will not remain with the fuel even at very short times. The analysis ignores transmutations, but this effect is not expected to change the overall results significantly.

(2). Extended Fuel Motion: Freezing and Plugging

Upon cladding failure in a sudden loss of flow transient, molten fuel released at near midplane elevations would be blown biaxially through coolant channels toward the core ends. Cladding within the core boundaries generally would be so hot as to preclude refreezing at those elevations. Upon entering cooler regions above and below the core, rapid cooling, refreezing and plugging could occur.

Refreezing and plugging of molten reactor fuel has been studied extensively, both theoretically and experimentally—mostly for uranium dioxide fuels. Uranium dioxide, with a melting temperature some 1500°C above that of steel, typically has been found to melt and entrain a layer of cladding as the flow front passes, forming a slug of molten cladding traveling ahead of the molten fuel. Plugging then occurs by refreezing this molten cladding to form a blockage farther down the channel. In contrast, molten metallic uranium, with a melting temperature below that of steel, seldom if ever would become hot enough to generate a contact temperature above the steel solidus and so would be expected to melt and entrain little if any cladding material near the flow front. At the low fuel densities near the flow front, the contact temperature also would be below rapid eutectic dissolution temperatures. Rather, the process would be one of desuperheating and refreezing the fuel. For temperatures of interest the fuel-wall contact temperature would be below the fuel solidus such that a solid fuel crust would start to

form upon contact. So long as significant melt superheat persisted (more than a few tens of degrees) crust growth would be limited by the convective heat transfer. Later, when superheat near the flow front had been reduced sufficiently, conductive heat transfer from the crust would exceed the convective heating from the melt and the crust could grow to occlude the channel completely.

Using the heat transfer assumptions outlined above, an analysis of penetration distances prior to complete plugging of ex-core coolant passages was performed. In these analyses, it was assumed that the above-core channel geometry was characterized by the core coolant channel hydraulic diameter, while the below-core coolant passages have larger diameter, open geometry typical for IFR metal fuel designs. These results predict that:

- (1) complete penetration of the coolant channels in the outlet, fission-gas-plenum region (length $\approx$ 150 cm) is highly unlikely, and
- (2) complete penetration of the inlet, lower-shielding region (length $\approx$ 50 cm) is highly likely for all radial regions.

The examples used initial fuel temperatures and velocities which are characteristic of those predicted for a sudden loss of flow transient. Any milder transient which leads to a cooler, lower velocity melt would be more plugging prone, even in large diameter inlet channels.

(3). Particulate Debris Bed Coolability

There are two limiting processes whereby molten core debris can flow downward from the core region. If the below core structure has large diameter channels, the molten core debris can flow rapidly from the core, reaching the lower core structures a few seconds after cladding failure. However, if the lower core structure has very small diameter channels, the core debris will freeze and plug the channels.

For the case with large diameter channels, the melt from a given subassembly will descend rapidly into sodium-filled areas because of the fission gas driving pressure. This could result in incomplete fragmentation or fragmentation with very small particles. The vigor of the melt-sodium contact may improve melt spreading in lateral directions, but the melt will have a high rate of decay-heat generation. Also, the melt should contain relatively little dissolved steel because of the rapid downflow.

For the case with small diameter channels, the melt will probably emerge from the assembly by meltthrough of the duct wall. The flow will then be within the spaces between ducts. Considering the difficult flow path for the melt, it is very unlikely that melt will reach the core support plates before the time when the decay heat has fallen and the fission gas pressure has been relieved, so that there would be a gradual entry into the sodium. Thus, the fragmentation should be relatively complete, but the degree of spreading may be small. The melt will probably contain considerably more steel than the eutectic composition of 12 wt% because of having to essentially melt through much of the lower structure.

Studies were performed by Gabor et. al., of the fragmentation and quenching of simulant melts into water⁸ and of uranium melts into sodium.⁹⁻¹⁰ The results indicate that for gravity fall, the fragments are relatively large, 3 to 16 mm on the average. In one test, the velocity was 10 m/s and the average particle size was only 0.59 mm. If the melt enters sodium under gravity fall, large particles are expected. But if the melt enters at high velocity, small particles may be formed. The debris bed void fractions for the tests were very large, ranging from 0.76 to 0.95.

In most previous considerations of particulate debris bed coolability, both in water and in sodium, the heat generated within the bed was removed by coolant boiling. Because of the high porosity of the metallic fuel beds, initial calculations were based first on heat removal by conduction and then by natural convection without boiling. The calculations indicated that the high-porosity beds expected for metal fuel in sodium would not be coolable by conduction alone.

The natural convection analysis indicated that large-particulate, high porosity debris beds would be coolable, but beds with finer particles (high velocity injection) would not be coolable without sodium boiling. Since the sodium boiling temperature is above the eutectic alloy liquidus temperature, a boiling eutectic debris bed would likely develop into the molten pool treated as follows.

(4). Molten Pool Coolability

Molten core debris flowing downward will be likely to fragment to form particulate debris. However, it is possible that some or all of the debris might not fragment or might fragment but not quench. This would result in the accumulation of a molten pool on the core support structure. It is also possible, but not likely, that a particulate bed could form initially and later dry out to form a molten pool. In any case, it is necessary to determine the probable behavior of a molten pool in the core support structure area.

The first melt to reach the core support plate will probably be quenched and frozen on contact with the plate and the surrounding sodium. As more core material arrives, the layer temperatures will increase toward equilibrium values. Steady-state temperatures for several melt layer conditions can be calculated using available heat transfer correlations. The calculations consider several heat transfer processes. In proceeding from the top of the system downward, the following processes must be considered:

- (1). Heat flow from the top of the frozen crust of the melt pool into bulk sodium,
- (2). Heat flow through the upper frozen crust,
- (3). Heat flow upward from the peak temperature within the melt pool to the upper frozen crust,
- (4). Heat flow downward from the peak temperature in the pool to the support plate,
- (5). Heat flow downward through the support plate, and

- (6). Heat flow from the bottom of the support plate into bulk sodium.

A steady-state thermal analysis of a molten pool was performed, yielding the axial temperature profile, and the amount of penetration of the core support plate by eutectic interaction corresponding to a fuel/plate interface temperature of 1071°C (see Fig. IV.1). The analysis results indicate that the support plate residual thickness for a 2% decay heat molten pool ranges from 4.0 to 5.3 cm as the pool uranium atom fraction changes from 0.33 to 0.16.

For transient calculations, the foregoing computational procedure was simplified. The primary simplification was to neglect downward heat transfer. The transient analysis shows that the bulk pool coolant temperature rises to a peak at about 30 h when decay power has fallen to the assumed rejection rate of 1/2%, and then declines. The fuel-plate interface temperature falls rapidly at early times dominated by rapid decay power decline, then more slowly as the power decays more slowly and coolant temperature rises and falls. The corresponding frozen crust thickness grows to a small thickness (~1 mm), disappears entirely, then begins a monotonic growth to complete freezing of the entire pool at a time just short of 100 h, when the fuel-plate interface temperature has fallen to the fuel solidus. Integration of the eutectic dissolution rate evaluated at the time dependent fuel-head interface temperature indicates about 1/2 in. reduction of the head thickness at the time the pool becomes completely frozen and dissolution effectively stops.

- (5). Recriticality

Fast spectrum reactors typically are designed with about one-half of the total volume of the subassembly ducts occupied by reactor fuel. Fuel expansion or contraction from this normal density distribution would reduce or increase reactivity—through changes in neutron leakage. Severe accidents in which fuel might melt and be released from cladding typically would be predicted to experience neutronic shutdown through fuel expansion. Fission gas retained in metallic fuel would contribute importantly to this expansion process. In some

instances the expanded fuel might not be coolable in place in which case further relocation to more compact, higher-reactivity configurations might occur. Assessment of the potential for reassembly to a critical state requires estimates of the dimensions and shapes of the changing core boundaries and of the changing fuel density distributions within those boundaries. Such space-time histories depend in part upon the manner in which the accident was initiated. Estimating core configurations is simplified somewhat by the fact that, for all classes of initiators, subassembly ducts are predicted to remain intact throughout the initial expansion process such that fuel released from cladding to coolant channels is constrained to move axially along these channels toward the core ends. Only subsequent to exiting into the inlet or outlet plenum is it free to expand radially as well.

Reactivity calculations have shown that: 1) removal of fuel from an average worth position reduces reactivity at a rate of about 1\$/% removed (removal may be taken to mean relocation from the core to either the inlet or outlet plenum); 2) removal of one half (or more) of the core fuel would preclude criticality in the remaining half even if it were to become fully compacted, axially within the ducts; 3) an entire core, fully compacted, could be critical outside the normal core boundaries only if it had spread radially to a diameter less than about twice the normal core diameter. These "rules of thumb" are applicable to a broad range of large reactors (a few hundred MWt or larger). In each case it is assumed that it has not been possible to insert control rods.

Using the above considerations, results from initiating phase accident analyses, and results from freezing and plugging, molten pool, and other post-initiating phenomena, it was concluded that the potential for recriticality events following a rapid loss-of-flow or slow transient overpower event would be extremely unlikely, while the recriticality potential after a degraded decay heat removal accident would be very small.

(6). Vessel Failure Mechanisms

A preliminary scoping assessment of vessel failure mechanisms identified three candidates: 1) pressure loading due to an energetic power excursion, 2) melting due to direct contact with high temperature molten fuel or fuel/steel alloy, and 3) creep rupture at elevated temperature over an extended time frame.

Initiating phase transient analyses¹¹⁻¹² have indicated that metallic-fueled IFR design concepts do not produce energetic severe accident sequences that challenge the reactor vessel by pressure loading. For such loading to develop, it is necessary to heat reactor core material (fuel and/or sodium coolant) to a level such that rapid vaporization occurs, producing pressures sufficiently high (hundreds of atmospheres) to break the reactor vessel. Available initiating phase analyses indicate that peak fuel temperatures are well under 2000 K, and consequently that fuel vapor pressure is not a matter of concern. In addition, the heat capacity ratio of the coolant and structures to the fuel is such that the fuel will quench following such an excursion without raising nonfuel materials to temperatures at which their vapor pressures become significant. Therefore, direct pressure loading of the reactor vessel to failure from an initiating phase power excursion appears to be very unlikely.

Direct contact of core fuel or fuel/cladding alloy with steel can result in liquefaction of the steel if the fuel alloy temperature is sufficiently high. The normal melting point of stainless steel is around 1700 K, but chemical interactions among fuel and steel components may lower the effective melting temperature of the steel (see the uranium-iron phase diagram in Fig. IV.1), depending on the concentrations of constituent nuclides. Melthrough of the reactor vessel might occur if direct contact with fuel alloy was established and vessel cooling was insufficient to maintain the local vessel wall temperature below the effective melting temperature. However, phenomenological analyses presented above and accident sequence analyses demonstrate that core materials will likely be coolable in the inlet plenum, either as a particulate bed or as a molten layer. Therefore, direct contact between fuel alloys and the vessel

would be precluded, since core materials would be contained in the inlet plenum in a coolable state.

Creep rupture of the reactor vessel at elevated temperatures for extended periods is the third candidate failure mechanism. While a detailed, coupled thermal-mechanics analysis would be necessary to predict vessel failures accurately, a scoping analysis of two possible failure sites was made for this report. The analysis utilized the 316 stainless-steel creep rupture database assembled by Pfeiffer¹³ in his analysis of reactor vessel failure. The method employed in the scoping analysis presented here consists of a static tensile stress estimate for accident loads at two locations, neglecting transient thermal stresses. The two locations analyzed were the vessel wall exposed to the hottest sodium (near the top just below the head), and the lower core support plate. Using prototypic loads and vessel and vessel internals designs, analyses indicated that creep rupture times for the vessel would be greater than 10 years, while the core support creep rupture time could range from hundreds of hours to hundreds of years, depending on the temperature.

3. Conclusions

The findings of this study are that extremely unlikely triple-fault accident initiators are necessary to produce severe accident conditions in IFR, and that even in the event of core melting, the reactor core debris will likely be cooled and contained within the reactor vessel, maintaining two barriers for the protection of public health and safety. This findings are based on preliminary, scoping analyses of anticipated severe accident phenomena, and on available experimental data. The severe accident phenomena considered include fuel-steel chemical interaction, molten fuel freezing and plugging, fuel debris bed coolability, molten pool coolability, recriticality, and core support structure and vessel failure mechanisms.

Although not reported here, the study results were used to provide identification of IFR severe accident phenomena uncertainties. Future analysis and experimental data requirements were defined based on the technical results summarized here.

B. Mark-V Safety Case

The document, "Safety Analysis and Technical Basis for Establishing an Interim Burnup Limit for Mark-V and Mark-VA Fueled Subassemblies in EBR-II," completed the ANL internal review process when approval of the document and the associated changes to the EBR-II Technical Specifications was granted by the Reactor Safety Review Committee in September 1994. Because of the shutdown of EBR-II by the Department of Energy (DOE) at the end of September, the document (generally referred to as the Mark-V Safety Case) has not been submitted to the DOE. However, so that the value of the work as a model of a safety analysis for advanced reactor fuels will not be lost, the document and all attachments are being put in final form to be archived. This work is to be completed in December 1994.

The major issues addressed in the Mark-V safety case are the following:

- Establishment of fuel design criteria which reflect the current understanding of fuel damage mechanisms.
- Derivation of a core design.
- Establishment of a duty cycle.
- Establishment of operational limits.
- Generation of safety analyses for normal and off-normal operation.

These items establish the case for operating EBR-II with a core of U-20Pu-10Zr fuel up to an interim burnup limit of 10 at. %.

The Mark-V safety case is based upon two principal requirements:

1. Ensure that sufficient fuel pin reliability is maintained so as to statistically prevent a "significant" number of fuel pin breaches during normal and off-normal reactor operation (up to and including unlikely events). A "significant" number of breaches may be defined as that which challenges the safe operation or performance goals of the EBR-II reactor. This has previously been defined as ensuring that no more than one fuel pin breach is expected per core loading. In other words, a 95% confidence that no more than one in 3000 pins will breach.
2. Maintain a coolable geometry of both the fuel pin and the fuel pin bundle for the useful lifetime of the subassembly, including normal operation and all off-normal events.

These performance requirements form the foundation for the establishment of the design criteria which were used in the fuel pin design. The reference fuel design (designated Mark-V) used cladding made of ferritic/martensitic alloy HT9. An alternative design (designated Mark-VA) was also considered; this design used cladding made of austenitic 20% cold worked type 316 stainless steel.

The fuel damage mechanisms which govern fuel performance depend on a few key physical phenomena: 1) mechanical deformation of the cladding, 2) cladding wastage, and 3) fuel melting. Based on these phenomena, a set of nine design criteria were established. These criteria were separated into those applicable to normal operation and those applicable to off-normal operation. The need for this separation is a result of the widely different time scales involved in the two categories of operation. The design criteria include use of cumulative damage function (CDF) methodology, a relatively new approach which was not used in previous EBR-II fuel design evaluations. The design criteria for normal operation include limits on the thermal component of plastic hoop strain, CDF, and primary hoop stress, plus the restriction of no eutectic liquefaction at the fuel-cladding interface. The design criteria for off-normal operation allow a limited amount of eutectic penetration, plus impose restrictions on CDF and plastic hoop strain. No sodium boiling is allowed during either normal or off-normal operation, and no fuel melting is allowed

except during extremely unlikely off-normal events. The criteria for the Mark-V and Mark-VA designs differ only in the limiting values for plastic hoop strain in both normal and off-normal operation.

The reference core design used to evaluate the Mark-V and Mark-VA fuel designs against the design criteria was a compact, six-row core which represents higher power densities and temperatures than would generally be found in EBR-II cores. It therefore provided a useful case from which to establish operating limits through the safety analysis. Enrichment for the six-row core was calculated so as to produce the same system eigenvalue as an identical Mark-II U-5Fs fueled loading configuration. The required enrichment (48%) resulted in an effective delayed neutron fraction β of 0.0053, significantly smaller than the value of 0.0067 for an equivalent uranium system. This change in delayed neutron fraction value increases the dollar worth of control rods and feedback coefficients. The effects of this decrease of β upon fuel pin temperatures and cladding damage have been investigated in detail for the most severe of the loss of flow, transient overpower, and startup events. The conclusion of the analysis was that the variation of the delayed neutron fraction has insignificant impact on the off-normal transient analyses.

Duty cycle selection and event categorization of normal and off-normal events was made based on EBR-II operational experience, the duty cycle which has been used traditionally, engineering judgment, and information obtained from the probabilistic risk assessment (PRA) analysis of EBR-II. The events were classified into the traditional five categories: normal, anticipated, unlikely, extremely unlikely, and beyond design basis events. The selection process followed five steps:

1. Evaluate EBR-II operation history and existing classification of events.
2. Determine equipment failure frequency and probability of failure based on the EBR-II PRA.

3. List all off-normal events needed for analysis, and categorize them based on frequency of occurrence and judgment.
4. Group events based on similarity and frequency.
5. Select the "umbrella" transient in each group based on severity of fuel damage, and determine the number of duty cycles in each group.

A safety analysis was then performed on the umbrella transients in the normal, anticipated, unlikely, and extremely unlikely categories. Normal operation was evaluated using the LIFE-METAL code, while off-normal events were evaluated with the SASSYS-1 code.

In specifying the conditions under which a fuel type can be approved for use in EBR-II, three types of operational limits must be considered. Least restrictive are Safety Limits, which are limits that protect the integrity of barriers to uncontrolled radiation release. In this case, the barrier in question is the fuel pin cladding. Next are Limiting Safety System Settings (LSSS), which give the settings for automatic protective devices that monitor variables having significant safety functions. These must be independent of the fuel type. Finally, there are Limiting Conditions for Operation (LCO), which are conditions on equipment and technical characteristics of the plant required to ensure safe facility operation.

For the Mark-V safety case, the LSSS of 15% overpower, 12% underflow, 15% over temperature rise, and 17 s period trip settings which are in use with the present Mark-III/IIIA core were retained. These values were part of the input to the steady-state and transient analyses. The LIFE-METAL code was used to perform a sensitivity study and derive initial LCOs based upon requirements for steady-state operation. These limits were then used in a scoping transient analysis of the full duty cycle using the SASSYS-1 code. The LCOs were reduced if any criteria were not met in the transient analysis. The outcome was to establish LCOs on reactor inlet temperature and on subassembly coolant temperature rise and power row by row. The LCOs thus established are listed by fuel type in Table IV.1. The LCO values were limited by one of three

TABLE IV.1. LCOs for Mark-IIC, Mark-IICS, Mark-III, Mark-IIIA, Mark-V and Mark-VA Fuels in EBR-II

Row No.	Mark-V			Mark-III, Mark-IIIA, and Mark-VA		
	Hot Channel Coolant Temperature, °F (°C)	Subassembly Power, kW	Fuel Burnup, at. %	Hot Channel Coolant Temperature, °F (°C)	Subassembly Power, kW	Fuel Burnup at. %
1	939 (504) ^b	989 ^b	10	940 ^b (504) ^b	992 ^b	10
2	943 (506) ^b	985 ^b	10	944 (507) ^b	989 ^b	10
3	969 (521) ^b	965 ^b	10	971 (522) ^b	970 ^b	10
4	1028 (553) ^a	917 ^a	10	1014 (546) ^c	879 ^b	10*
5	1054 (568) ^a	835 ^a	10	1024 (551) ^c	765 ^c	10*
6H	1056 (569) ^a	811 ^a	10	1027 (553) ^c	746 ^c	10*
6L	1063 (573) ^a	738 ^a	10	1034 (557) ^c	679 ^c	10*
7	1065 (574) ^a	712 ^a	10	1036 (558) ^c	655 ^c	10*
	Mark-IIC			Mark-IICS		
3	1004 (540) ^c	901 ^c	8.9	--	--	--
5	--	--	--	1029 (554) ^c	634 ^c	6.4

Limiting Phenomena:

^aFuel-clad eutectic formation

^bTernary fuel melting

^cCDF

*Due to steady-state wastage, either burnup or irradiation temperature must be restricted to keep the CDF within the design limits. In this case, the burnup is set at 10% and peak inside clad temperature restricted to 627°C.

phenomena in the design criteria: ternary fuel melting in the inner rows, fuel-clad eutectic formation in the outer rows of the Mark-V design, and CDF in the outer rows of the Mark-VA design. This represented a significant departure from earlier EBR-II fuel safety analyses, which did not have the benefit of cumulative damage methodology and which established a single LCO on subassembly outlet temperature, independent of location, based on avoiding fuel-cladding eutectic formation.

One of the most difficult issues to resolve in the Mark-V safety case was defining a safety limit. The safety limits described in the Technical Specifications for Mark-III/IIIA fuel are much more restrictive than is warranted by the definition of safety limit currently advocated by the DOE. Unlike the LCOs, safety limits are not a direct result of the off-normal safety analysis, since they represent parameter values which bound values achieved during all credible events, e.g., those events evaluated for the safety analysis. Safety limits do not necessarily provide values against which monitored process parameters can be evaluated during operation; LSSS and LCOs serve that function. However, if a safety limit is exceeded, it must be demonstrated that the plant can still be brought to a safe and stable condition and maintained in that condition.

After much consideration, it was decided that a single safety limit should be established for the Mark-V/VA designs and that limit should be on the value of maximum CDF attained in any pin in the core. The fuel design criteria evaluate cladding damage using a cumulative damage methodology which explicitly accounts for the effect of time dependence of eutectic penetration and strain. The design criteria are predicated upon one fuel failure per core, which requires restricting the CDF to 5% during normal operation and 10% accumulated over all anticipated design basis events plus the single most damaging unlikely event. Therefore, to be consistent with the fuel design methodology, the fuel safety limit should also be expressed as a limit on the CDF. The limit selected is that the CDF should not exceed 1.0 for the limiting driver fuel pins. This limit assures that the core is coolable, and so the plant can be brought to a safe and stable condition. At a CDF of 1.0 in the limiting driver fuel pins, at most 10% of the pins in the subassembly will fail, with the failed pins being in the inner rows of pins in the subassembly. Since metallic fuel cladding breaches are of the pinhole type, they are benign with respect to blockage formation, and so neither pin-to-pin failure propagation within a subassembly nor failure propagation between subassemblies is indicated. Therefore, even in the highly improbable case of reaching a CDF of 1.0 in the limiting fuel pins, core coolability would still be maintained and uncontrolled radiation release would be avoided.

Once the LCOs were determined, they were applied to a transient analysis of the full duty cycle. This analysis provided values for the design criteria parameters which, when evaluated

cumulatively across all applicable events in the duty cycle, could be compared with the design criteria limiting values. These comparisons showed significant margin between predicted parameter values and the design limits, even when 3-sigma uncertainties were taken into account in the safety analysis.

The Mark-V safety case demonstrated that EBR-II can be safely operated to a burnup of 10 at. % with a core of Mark-V, Mark-VA, or mixed cores as long as the subassembly outlet temperatures and powers are kept below those shown in Table IV.1. These temperature limits do not present any limitation on the operation of EBR-II. Extension of the burnup limit beyond 10 at. % was to be dependent upon confirmatory data from Mark-V irradiations, but the final limit was expected to be in excess of 15 at. %.

C. Coupling of FPIN2 and SASSYS/SAS4A

1. Introduction

The metal fuel version of the FPIN2 code, which provides detailed mathematical models to simulate fuel and cladding mechanical behavior, has been incorporated into the SASSYS/SAS4A whole-core accident code system to analyze the fuel element performance over a wide range of transients.¹⁴ In this implementation, SASSYS/SAS4A provides the fuel and cladding temperatures, and FPIN2 performs the analysis of fuel and cladding deformation. The FPIN2 results aid in the understanding of accident progression by providing the estimates of the axial expansion of fuel, time and location of cladding failure, and the condition of the fuel at the time of failure. The intended application for the integrated model is the design basis analysis of metallic IFR fuel elements for the purpose of margin-to-failure assessments.

The FPIN2 mechanics model uses a rigorous force-displacement formulation based on an implicit finite-element method to provide the fuel and cladding stresses and nodal displacements.¹⁵ The elements are defined in an (r,z) mesh; however, axial symmetry and generalized plane strain are assumed so that the analysis is essentially one-dimensional. The basic

fuel and cladding deformation models considered are the thermal expansions, elastic/plastic/creep deformations, cracking, and melting. For the metallic fuel elements, additional models for the fission gas swelling and release, molten fuel cavity formation, gas plenum, and fuel-cladding eutectic alloy formation are provided to complement the mechanics calculation.

The validation of the integrated SASSYS/SAS4A-FPIN2 model has been performed using the data from in-reactor TREAT tests M5, M6, and M7 for the prototypic binary and ternary fuels of the IFR concept^{16,17}. The integrated model calculations are compared to available experimental data for the six fuel elements in these tests. The comparisons include the coolant temperatures, margin to cladding failure, fuel melt fractions, cladding plastic strain, and fuel axial expansion.

2. Brief Description of the Tests and Pretransient Fuel Element Characterization

Each test contained two EBR-II irradiated fuel elements in separate flow tubes within a Mark-III sodium loop with thermocouples mounted on the flow tubes. Five of the six fuel elements studied were fabricated with U-19Pu-10Zr fuel and austenitic D9 cladding. The remaining fuel element contained U-10Zr fuel with ferritic alloy HT9 cladding. The baseline thermal conditions in the test fuel included a peak linear power density of 40 kW/m, an inlet temperature of 630 K, and 150 K coolant temperature rise along the active length of the fuel column. The transients were designed to be sufficiently severe to cause extensive fuel damage (an exponential power rise with an 8 s period at full flow), and were terminated at, or near, cladding failure with an overpower of 3.5 to 4.5 times the nominal. For each test, the power coupling factor, axial and radial power density profile, transient power, coolant inlet temperature, and coolant flow rate histories are obtained from the reported experimental data.

The pretransient fuel element characterization is mainly based on the sibling pin information. The mechanics calculation is initiated from a stress-free state for the hot, irradiated fuel elements in which the swollen fuel is assumed to be in contact with the cladding. A uniform fractional porosity distribution is assumed in the fuel which is determined from the as-irradiated

fuel geometry and density to obtain the correct fuel mass. Fission gas residing in the open porosity after the steady-state irradiation is determined from the local open pore volume and temperature assuming that the pore is in pressure equilibrium with the plenum. The distribution of fission gas retained in the fuel matrix and the fraction of it contained in grain boundary bubbles are calculated separately with the STARS code as a function of burnup.¹⁸

The axial distribution of the cladding fluence, needed in life fraction correlation evaluations, is determined from the burnup, fast flux-to-linear power ratio, and axial power profile. The cladding hardness parameter, used to describe the plastic deformation of the cladding with the known initial cold work and irradiation history, is determined from supplemental calculations that consider irradiation hardening, annealing, and grain growth.¹⁸

3. Comparison of Integrated Model and Test Results

a. Failure Location and Times

A comparison of measured and calculated cladding failure times for the six fuel elements in TREAT tests M5 through M7 are presented in Table IV.2. The integrated model analysis results indicate no cladding failure for M5 transients, in agreement with the experiments.* In tests M6 and M7, cladding failure was observed for the high burnup fuel elements, after which the power excursion was abruptly terminated. In the analysis, these fuel elements are predicted to fail at a time very close to the measured failure time. However, the integrated model also predicted a failure for the low burnup binary fuel with HT9 cladding in test M7, which was not observed during the experiment. For all cases, the estimated failure location was the top of the fuel column, in agreement with the experimental observations.

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*In test M5, the fuel elements were subjected to two transients because the first test was terminated prematurely. In this analysis, the two M5 transients are treated as separate tests.

TABLE IV.2. Comparison of Measured and Calculated Pin Failure Times for Six Pins in TREAT Tests M5 Through M7

TREAT Test		Fuel Type	Clad Type	Burnup (at. %)	Measured Failure		Calculated Failure	
					Time (s)	Location	Time (s)	Location
M5	Pin 1	U-19Pu-10Zr	D9	0.8	No Failure		No Failure	
	Pin 2	U-19Pu-10Zr	D9	1.9	No Failure		No Failure	
M6	Pin 1	U-19Pu-10Zr	D9	1.9	No Failure		No Failure	
	Pin 2	U-19Pu-10Zr	D9	5.3	13.24	Top of fuel column	13.27	Top axial segment
M7	Pin 1	U-19Pu-10Zr	D9	9.8	17.72	Top of fuel column	17.64	Top axial segment
	Pin 2	U-10Zr	HT9	2.9	No Failure		17.34	Top axial segment

The analysis indicates that the predicted failure of the low burnup fuel element in test M7 is primarily due to excessive eutectic attack. The correlation used in the model suggests a rapid eutectic penetration for temperatures in excess of 1353 K.⁴ The measured flow tube temperatures of nearly 1300 K almost certainly require that cladding inner surface temperatures exceeded this limit during the tests. Therefore, the lack of agreement on failure for this fuel element suggests that the eutectic penetration correlation does not accurately depict the behavior of binary fuel with HT9 cladding. Indeed, a supplemental analysis that assumes no acceleration in eutectic penetration rate does not predict a failure for this test, in agreement with the experiment.

b. Flow Tube Temperatures

Measured flow tube temperatures along the fuel column have been compared to calculated coolant temperatures for tests M5 through M7 and, in general, good agreement is obtained especially near the top of the fuel column. As an example, comparison of the measured and calculated temperatures for flow tube 1 in test M7 is shown in Fig. IV.2. The efficient heat transfer in metal fuel systems causes flow tube temperatures to closely follow

Fig. IV.2. Measured and Integrated Model Calculated Flow Tube Temperatures for Pin 1 in TREAT Test M7. Data points and dashed lines are thermocouple readings, solid lines are calculated temperatures.

the applied fission power with time delays less than a second. The sudden jump in thermocouple readings at the end of the transient marks the cladding failure. The agreement between the measured and calculated temperatures near the outlet is partially because these temperature measurements were used by the experimenters to fine-tune the reported power coupling factors. The larger discrepancies for the other thermocouple results along the fuel column are attributed to unaccounted thermal mixing delays for the sodium in the flow tube and the uncertainties in the axial power density profile.

The oscillations in the thermocouple readings in Fig. IV.2 indicate flow vibrations due to resonant movement of the fuel elements within the flow tubes. These deviations in flow geometry introduce uncertainties into calculated results since they were not accounted for in the thermal-hydraulic model. A separate sensitivity analysis has been performed to assess the effects of this and other uncertainties. The analysis reveals that the results for the high burnup fuel element in test M6 are somewhat sensitive to changes in flow rate. No failure is predicted

for that fuel element when the inlet flow is slightly increased. The uncertainties, however, do not justify the calculated low-burnup fuel element failure prediction in test M7.

c. Fuel Melt Fractions

The calculated peak melt fractions are in good agreement with posttest microstructural examinations of the unfailed pins. The calculated melt profiles at peak power and measured maximal once-molten area fraction at several axial locations are presented in Fig. IV.3. Agreement between measured and calculated extent of the melting provides a qualitative validation of the fuel thermal conductivity correlations and the empirical burnup correction for the irradiated fuel elements. The effects of the alloy redistribution on the fuel melt fraction have also been investigated by comparing the results from single and three radial zone models. Due to substantial zirconium migration from the middle to the central and outer regions, the initial melting of the high burnup ternary fuel does not occur at the fuel centerline. Posttest examination of unfailed pins reveals the existence of unmelted regions near the fuel centerline and supports the integrated model results.

d. Axial Fuel Expansion and Molten Fuel Extrusion to the Plenum

The transient fuel motion and relocation in the TREAT tests were measured using a fast neutron hodoscope. Posttest static scans were also performed. Lower gas retention due to higher operating temperatures, higher diffusion coefficients, and finer grain structures in Pu bearing fuels result in relatively small axial elongation in the fuel on the order of 1-5%. Because the fuel density is nonuniform near the fuel top, measured values include large uncertainties. The integrated model analysis, on the other hand, predicts a 3-9% axial expansion.

Discrepancies between the measured and calculated axial expansion data appear to be mainly due to the differences in the amount of molten fuel extruded to the pin plenum caused by retained gas bubble coalescence and swelling upon melting. Among the factors that could contribute to the difference between the measured and calculated molten fuel extrusion are

Fig. IV.3. Comparison of Measured and Calculated Molten Fuel Cavity Boundary at Peak Power for TREAT Tests: (a) M5 Pin 1, (b) M5 Pin 2, (c) M6 Pin 1, and (d) M7 Pin 2. Data points are the measurements.

the data used for retained fission gas, radial fuel composition variations, material thermal properties, and porosity distributions. A supplementary analysis indicates that the axial expansion is particularly sensitive to the variations in fuel thermal conductivity since 1) the resulting changes in radial temperature profile affect the amount of molten fuel and, therefore, the amount of fission gas released upon melting, and 2) the temperature difference also reflects upon the cavity gas temperature which, in turn, influences the swelling rate.

The thermal properties of irradiated fuel near the melting point are not well known. In SASSYS/SAS4A, quadratic functions of temperature are used to interpolate the thermal conductivity of a desired metallic binary and ternary alloy composition. Due to lack of data, the conductivity during and after melting is assumed to remain constant and equal to the value at the solidus temperature. Empirical or theoretical burnup correction options are available to account for the irradiation and sodium logging effects. Considering all the uncertainties in the magnitude of conductivity at elevated temperatures and the related effects of irradiation and sodium logging, the discrepancies between the measured and calculated extrusions are not regarded as hindering. Indeed, using the built-in FPIN2 conductivity correlation, which predicts about 20% larger conductivity that increases linearly beyond solidus temperature, reduces the calculated axial expansion by about 40%.

In all the TREAT tests analyzed, initial fuel melting is estimated to be below the fuel-plenum interface; therefore, the molten fuel zone does not have direct access to the plenum when melting begins. The resulting temporary imbalance between the cavity and plenum pressure is important in determining the amount and timing of molten fuel extrusion. Basically, extrusion is postulated to occur when the retained fission gas in molten fuel expands until its pressure equals that of the plenum after the plenum-cavity link is established. Fast neutron hodoscope measurements support this assumption and indicate a rapid expansion of the fuel coinciding with significant fuel melting. Because the initial extrusion rate is quite high, small variations in the timing of extrusion initiation would have significant impacts on axial expansion. Therefore, employing more restrained criteria for fuel mobility (in terms of fraction of heat of fusion absorbed) and extrusion threshold such as those used in SAS4A in-pin prefailure molten

fuel relocation module, PINACLE, has the potential to improve the axial fuel expansion comparisons.

Although important parameters for expansion of molten fuel, the variations in thermal conductivity and timing of extrusion initiation do not seem to play an important role in margin-to-failure assessments. Using the FPIN2 thermal conductivity correlation increases the margin to failure slightly, causing only minor modifications in Table IV.2 for the calculated failure times.

e. Cladding Plastic Strain

Contact and laser profilometry performed on the unfailed fuel elements in TREAT tests M5 through M7 showed essentially no diameter change (less than 1 mil) for all fuel elements studied. The calculated peak cladding plastic hoop strains in the unfailed D9-clad elements are also negligible ($\Delta D/D < 0.006\%$) in agreement with the measurements. In all cases, the peak permanent strain occurred at the top of the fuel column (top axial segment). The analysis also indicates that observed failures of the high burnup fuel elements in tests M6 and M7 are due to accelerated yielding of the cladding at the elevated temperatures.

The low burnup fuel element with HT9 cladding in test M7 for which the calculations predict a measurable increase in cladding diameter. However, this is primarily due to the unverified eutectic thinning of the cladding at elevated temperatures. Therefore, the results for this fuel element cannot be used as a validation basis for the cladding plastic deformation model.

4. Conclusions

Validation of the integrated SASSYS/SAS4A-FPIN2 code system has been performed using the data from TREAT tests M5, M6, and M7. The six metallic fuel elements in these TREAT experiments have been analyzed using the integrated model. The calculated results for thermal-mechanical response of the fuel elements have been compared to available

information such as flow tube temperatures, cladding failure time and location, fuel axial expansion, and melt fraction. In general, the calculated results are in good agreement with the experimental data for the key parameters related to timing, characteristics and the magnitude of the thermal and mechanical response of the fuel elements.

D. Closed-loop Passive Safety

1. Introduction

Advanced reactor concepts, to varying degrees, make use of intrinsic design features to improve safety over what is achievable using active systems alone. Such features have the potential to perform more reliably since they do not depend on electromechanical components but only on the natural laws governing heat, fluid flow and neutron production. Their introduction, however, causes the previously clear demarcation between the role of engineered safety systems and systems required for normal operation to blur. A passive system that serves a safety function may also play a role in aiding normal operation. Conversely, an active system used for normal operation may continue to operate during an upset thereby affecting the operation of passive systems designed to protect the plant during the upset. A very important issue thus arises: can the active system ever operate to confound proper operation of the passive safety system during the upset? To successfully address this issue, a disciplined structured approach to plant design is required, one that explicitly treats the integration of passive and active systems in achieving safety and normal operation goals.

Despite the clear need for such an approach, there is little evidence in the literature for the existence or use of such design methods. Perhaps part of the reason lies in the nature of the passive systems that appear in many of the new designs. Many of these are not purely passive but require the operator to perform a task, open a valve for example. Still, for those reactors with passive systems that are an integral part of the physics at normal operation, the fundamental need for a design approach remains.

A design methodology has been developed to address these issues for reactor designs based on the Integral Fast Reactor (IFR) concept. The IFR concept is marked by the use of passive safety systems that are an inherent part of the plant physics at normal operation and do not require operator initiation following an upset. The hallmark of the design method that has been developed is the systematic manner in which optimality with respect to unprotected safety is arrived at while constrained by normal operating requirements. The approach is not plagued by the need to identify the potentially infinite list of upset initiators and for each to determine the safety consequence. Rather, it is shown that all initiators fall into a small and manageable number of accident classes. It is shown that each class has associated physical mechanisms that limit the severity of any accident in the class. By defining a safety functional as the difference between maximum temperatures associated with these limits and actual failure limits, initiator consequences are analysed implicitly by class rather than explicitly by enumeration. This ensures that all challenges to the passive safety system have received consideration. This functional is maximized subject to normal operating requirements which are introduced as constraints.

The objective is to develop a strategy for designing and operating an IFR-type plant so that conventional control requirements and some new requirements are met. The conventional requirements include the ability to manage reactivity to startup and to shutdown, to change load, and to compensate for reactivity changes over the cycle. The new requirements are to (a) exploit the innate self-regulating property of an IFR core to passively shutdown the core without damage in response to unprotected upsets in the closed-loop plant and to (b) minimize the probability of the upset. The result would be a simplified control system and a reactor whose safety is less dependent on operator action and less vulnerable to automatic control system fault and/or operator and maintenance crew errors.

2. Physics and Models

The choice of reactor materials to a large extent determines the characteristics of any reactor type. In the case of the IFR, it is the use of metallic fuel and sodium coolant that provide for its unique safety characteristics. However, even though the physics of reactor

operation are largely determined by this initial selection, the designer must still understand the physics and exploit them to the best advantage. Part of this task involves the development of models to be used to optimize the design.

The combination of metal fuel and sodium coolant permit the design of a reactor that inherently adjusts power to a level that is safe for the inlet temperature and flowrate presented to it by the heat sink. This self-regulating property is the result of a large negative reactivity vested in the coolant temperature rise compared to the fuel temperature rise. Too much power raises the coolant temperature which in turn causes the power to drop. The size of the effect is what differentiates the IFR from other reactors; a relatively small change in coolant temperature can be used to compensate a relatively large change in power or reactivity change over the cycle. This self-regulating property is what the design engineer can make use of to limit temperatures during accidents and to change load and compensate for burnup with minimal need to rely on control rod reactivity.

The relationship between the conditions presented to the reactor by the outside world and the associated reactivity, which determines reactor power, can be represented through a reactivity balance. There are only three channels through which the outside world can influence the reactor (excluding the passage of a gas bubble through the core): inlet temperature, flowrate and external reactivity (control rods). The time constants of the temperature feedback processes are such that the feedback temperatures are essentially in equilibrium with the instantaneous power, flowrate and inlet temperature. A reactivity balance on the reactor then gives, relative to some equilibrium zero reactivity reference state,

$$\rho = (P-1)A + (P/W-1)B + C\delta T_{pc} + \delta \rho_{ext} \quad (1)$$

where,

- P = normalized power,
- W = normalized reactor flowrate,
- δT_{pc} = change in primary system cold leg temperature,
- $\delta \rho_{ext}$ = externally imposed reactivity, and

A,B,C = integral reactivity feedback parameters that are measurable at the full power operating point.

When nonequilibrium of the delayed neutron population can be precluded, as in the asymptotic state following an upset, the left-hand side of the above equation can be set to zero. The equation can then be solved for the new power level.

a. Unprotected Accidents

A simple yet powerful safety case can be developed for the IFR with Eq. (1) serving as its basis. By virtue of the fact that: (a) there are only three communication channels through which the core can be affected, and (b) the signal into each of these channels either increases or decreases, then all unprotected accidents are contained in the union of five generic accident classes. These classes are given in Table IV.3. In each of these five classes, the signal input through the channel is limited in its amplitude by a physical mechanism as listed in Table IV.3. The reactor power in the asymptote then is bounded and is given by Eq. (1). Safety analyses have shown that by proper choice of core design and primary system layout, one can achieve a safe response for these classes when taken one at a time.^{19,20}

TABLE IV.3. Unprotected Accident Classes

	Accident Classes	Physical Mechanism Limiting Signal Amplitude
Open-loop Plant	1. reactor flow runup	- cavitation of pump coolant
	2. reactor flow rundown	- establishment of natural circulation conditions
	3. reactor inlet temperature chilling	- freezing of intermediate system cold leg sodium
	4. reactor inlet temperature heatup	- loss of primary system heat removal capability
	5. external reactivity insertion	- BOC excess reactivity
Closed-loop plant	combined 1, 3 and 5	
	combined 2, 3 and 5	
	combined 1, 4 and 5	
	combined 2, 4 and 5	

The above result, while rather remarkable, is one step removed from practical application since it applies to the open-loop plant. It remains to be seen what the closed-loop behavior of the plant is, that is, the plant operating under a control system. The complicating factor in the case of the closed-loop plant is the hierarchical relationship between the control system and the actuators; a control system failure can initiate through actuator motion simultaneous occurrence of more than one of these classes.

A simple extension of the ideas from the open-loop case, however, leads to an upper bound for the conditions reached for all closed-loop accidents. Taking all possible combinations, two and three at a time, of the five open-loop accident classes envelopes all closed-loop accidents. The bottom of Table IV.3 summarizes the four bounding events. Eq. (1) can again be used to compute the asymptotic power for each of these four events.

b. Load Change

The conventional approach to load schedule design sets out to limit the rates of temperature change in plant components during load changes such that allowable thermal stresses are not exceeded. Typically this is codified as a set of prescribed transients with associated performance requirements.

To design for allowable stresses, it is helpful to view the plant response to a load change as composed of the asymptotic response to the new condition onto which is superimposed the dynamic component of the response. Each of these is controlled by different mechanisms and so, to some extent, the temperature swings for each can be set independently. The asymptotic response can be set in part by (a) adjustment of inherent feedbacks so that the reactor more closely follows changes in the imposed heat sink load, and by (b) the coordination of control variables—steam, primary system and intermediate system flowrate and control rod position as functions of steady-state load. The dynamic component is controlled in part by (a) plant component time constants, and by (b) the dynamic order of the control equations.

To design for the steady-state and dynamic components of the plant response, lumped parameter equations describing the plant load behavior and algebraic equations giving the dependence of control variables are written. By inspection one sees what can be achieved with respect to shaping the steady-state behavior of the plant.²⁰ Three load coefficients can be arbitrarily assigned through the three parameters m_p , m_i and m_r .

Acceptable dynamic response during a load change in an IFR appears achievable by simply adopting the control laws developed for the steady-state. The instantaneous values of the forcing functions are taken from the load schedule once the desired power trajectory has been specified. In general, if the load schedule is flat to begin with, if the longest component time constants are no greater than the time permitted to reach the new equilibrium state and if the full power reactor outlet design temperature is less than 1000°F, then acceptable dynamic behavior can usually be achieved without modification to plant component design or adding dynamic elements to the control laws. These three conditions are generally satisfied in an IFR-type plant. Response requirements during more rapid transients, scrams for example, may, however, necessitate greater controller complexity.

c. Compensation for Cycle Reactivity Changes

Any change in reactivity that occurs over the course of a cycle as a result of a change internal to the reactor can be compensated for through any of the three communication channels. Mathematical relationships can be written that describe what can be achieved.²⁰

3. Optimization of Design and Operation

The objective as defined earlier contains no reference as to how we are to proceed. Quite obviously there are degrees of freedom available in the way the reactor is designed and operated. To exploit these, one defines a functional that measures the degree of safety and then maximizes this functional with respect to these degrees of freedom subject to the constraints of the conventional control requirements. The functional is defined as the smallest margin that exists over the four worst case closed-loop accidents in Table IV.3. The margin is the difference

between a safety limit, cladding eutectic temperature for example, and the corresponding peak temperature.

a. Design

In practice, there is enough flexibility in the constraints and enough degrees of freedom in the parameter space that the solution to the above constrained optimization problem can be got by performing a single pass through a series of unconstrained optimizations. Each successive optimization involves a set of parameters of which there is a subset that controls the value of the current functional with the value that went before largely independent of these. One sees this in Table IV.4, which lists three optimization steps and their associated parameters. This single pass serial optimization is preferred because it is simpler to implement.

TABLE IV.4. Design Parameters Available for Optimization of Reactor Response

Optimization Step	Communication Channel	Parameters Controlling Response	
		Fixed by Prior Step	Free to Vary
1. Unprotected accident asymptotic response a. signal amplitude input to channel b. channel sensitivity	w δT_i ρ_{ext} w δT_i ρ_{ext}		w_{min}, w_{max} $T_{SOLIDUS}, K_{1IHX},$ K_{2IHX} ρ_{excess} B/A C/A $1/A$
2. Load change asymptotic response	w δT_i ρ_{ext}	B/A $C/A, K_{1IHX},$ K_{2IHX} $1/A$	m_p m_i m_R
3. Cycle reactivity change compensation	w δT_i ρ_{ext}	B/A $C/A, K_{1IHX},$ K_{2IHX} $1/A$	ρ_p ρ_i ρ_R

b. Operation

In general, if the number of search parameters in each of steps two and three in Table IV.4 exceeds the number needed to achieve acceptable burnup compensation and load schedule behavior, then these extra degrees of freedom should, according to our goals, be used to implement the burnup compensation and load schedule control schemes in the way that minimizes the probability of an unprotected accident. While there may be any number of ways to proceed, it seems quite obvious that the probability can be reduced by minimizing the probability of failure of three systems: the reactor flowrate control system, the reactor inlet temperature control system, and the rod control system. Both the inlet temperature and flow are controlled by pumps and electrical circuits whose failure rate should be relatively independent of setpoint change frequency since this equipment operates continually. Changing a setpoint does not cause additional circuits to energize or moving parts to be put in motion. On the other hand, the rod control system need not necessarily be energized except when rods are moved. Minimizing the demand for motion would seem to minimize the probability for rod worth insertion through an equipment failure. Thus, the probability of an unprotected accident could be reduced by minimizing the demand frequency placed on rods during either load changes or when compensating for cycle reactivity changes. In practice, we can achieve this by de-energizing the rods and using reactor inlet temperature and flowrate, rather than rods, to change load and compensate for cycle reactivity changes. Moreover, to transport the reactor heat we must have the primary and intermediate pumps operating. We see that only the control rods are available for elimination.

Nominally, the zero burnup swing core required by step one implies no rod worth available for insertion once full power is reached. This, however, neglects the presence of uncertainties and the need for some worth to cover these. There are two components. First, the worth that must be inserted into the core to take the reactor from cold subcritical to the low end of the load range is subject to the uncertainty in the calculated temperature defect, the calculated critical position and the actual fissile loading. Second, over the course of a cycle, the nonlinear component of the reactivity due to fuel axial growth and the calculational uncertainty

in predicting the burnup swing need to be covered. These various factors must be taken care of and thus there must unavoidably be some amount of worth available for insertion while at load.

There are two ways that we can operate the reactor given that some reactivity is needed at full power to cover these uncertainties. In the first approach, the reactivity needed to get to the low end of the power range (50%) can be vested in one set of rods, for conceptual purposes, Rods A. Once at partial power, the Rods A drive motors are electrically de-energized and access to the motor controller restricted. The reactivity needed for maintaining power operation (50-100%) through the cycle is vested in Rods B. The positions of Rods B need be changed only as frequently as is needed to compensate for axial growth reactivity loss and uncertainties in the nominally zero burnup swing. If reactor flowrate and inlet temperature are to provide compensation on the short term, of the order weeks, until they reach the limits of their ranges, then Rods B can be energized for a time long enough to move them so that flow and inlet temperature can be returned to their original values.

In the second approach, we build in a positive burnup swing equal to the worth of Rods B above. Then imagine a set of poison rods suspended above the core that enter the core under the force of gravity and a compressed spring. An electromechanical ratcheting mechanism on the rods allows us to insert negative reactivity a small increment at a time. A charging device physically separate from the rods is used to raise the rods during startup. Under this scheme, positive reactivity can be added only by the charging device and, under strict administrative controls, only during startup. Should the reactivity that builds into the core exceed the rate at which it is lost through axial fuel growth, the rods can be lowered into the core to make up the difference. Load control is achieved the same way as in the previous scheme.

4. An Example

The degree to which core damage can be eliminated during unprotected upsets by appropriate optimization was investigated for a 900 MWt advanced liquid metal reactor representative of those being developed in the U.S. The design, originally optimized with respect

to three of the five open-loop accidents shown in Table IV.3 with the result appearing as Case 1 in Table IV.5 was reoptimized with respect to the four closed-loop accidents of Table IV.3. The resulting values for A, B, C and the excess reactivity are, respectively, -15¢ , -45¢ , -0.24¢/°C and 0. In the optimization, we elected to use the concept of Rods A and B for control rods as described in the previous section. The estimated two-sigma worth of these rods is shown in Table IV.6.

The reactor response for the bounding closed-loop accidents proved rather remarkable. The results summarized in Table IV.6 show for runout limited to Rods B, the only rods ever energized after ascension to the low end of the power range, the peak cladding temperature remains below 710°C , a temperature that leads to cladding failure only after many hours. If both Rods A and B runout, then temperatures still remain substantially below the sodium boiling temperature of 950°C . The fuel axial growth component of Rods B is based on FFTF experimental data for 15 kW/ft IFR-type pins. Lower power ratings would increase the worth of Rod B and perhaps favor the alternative control scheme of the previous section.

The load schedule shown in Fig. IV.4 exhibits temperature swings that are acceptable. This load schedule was designed by using the two degrees of freedom m_p and m_i associated with sodium flowrates to maintain superheater outlet temperature and steam drum pressure constant. Because rod position is not used as a control variable, only two load coefficients could be arbitrarily assigned. The steam outlet conditions were selected since in general one attempts to maintain constant conditions at the turbine.

The temperature response during a load change is shown in Fig. IV.5. In this transient steam power was reduced from 100% to 50% at the rate of 3% per min. The sodium temperatures change without overshoot and are essentially in quasistatic equilibrium with the sodium and water flowrate forcing functions. The model used for these simulations was relatively simple, however. In future work, a plant-wide systems code will be used to more fully investigate the structural response of the heat transport system components to operation under all operating modes including normal load adjustments.

TABLE IV.5. Comparison of Passive Safety Characteristics:
H/D Parametric Study for 900 MWt Reactor

Parameter	Case			
	1	2	3	4
H/D ratio	0.484	0.299	0.192	0.060
BOC excess reactivity, \$	0.45	6.18	10.4	12.3
A, cents	-15	-14	-12	-15
B, cents	-37	-49	-63	-94
C, cents/°C	-0.27	-0.37	-0.50	-0.76

TABLE IV.6. Asymptotic Temperatures for Unprotected Closed-loop
Accidents in 900 MWt Plant

Case	Accident	Peak Cladding ID Temperature, °C (nominal)	
		Rods B Runout, 32¢	Rods A and B Runout, 56¢
1	chilled inlet, ^a primary pump overspeed, ^b rods runout	680	774
2	chilled inlet, loss of reactor flow, ^c rods runout	709	808
3	heating of inlet, ^d primary pump overspeed, rods runout	706	804
4	heating of inlet, loss of reactor flow, rods runout	706	804

^aSodium solidus intermediate cold leg

^bCavitation at 115%

^cNatural circulation

^dLoss of heat sink, zero power quasistatic reactivity balance

Fig. IV.4. Load Schedule for 900 MWt Plant

Fig. IV.5. Power Ramp from 100% to 50% at 3% per Minute for 900 MWt Plant

E. Summary of M8CAL Results

1. Introduction

The M8 calibration experiment was a series of 23 irradiations in TREAT performed to determine the relationship between the fission power generated in the TREAT core and the fission power generated in experiment fuel located in an in-core experiment vehicle and irradiated by core neutrons. This relationship is referred to as power coupling. The calibration experiment was planned to provide the power coupling information needed for planning and analysis of the M8 test (and subsequent tests similar in geometry to M8) to be performed in the post-upgrade TREAT core.

The principal parameter that describes the relationship between the TREAT power and the power in the test sample is the power coupling factor. In convenient units, this factor has units of test sample power per gram of test sample per unit of TREAT power. The sample fission energy deposition is derived from postirradiation measurement of the fission density in the sample. The TREAT power is measured as a function of time by ion chambers located at the edge of the core. The value of the power coupling factor depends upon several parameters including the composition of the fuel sample, the configuration of fuel assemblies in the core, the configuration of control rods in the core, and the axial locations and movements of the absorbing portion of the control rods during irradiations. During transients the effect of the control rods is time dependent.

The power coupling is determined by first measuring the fission density distribution in fresh fuel irradiated in TREAT under very-low power conditions. Then the result is scaled to the higher-power experiment conditions using measured fission densities in fissile monitor wires irradiated under very-low power and higher-power conditions.

The M8 experimental hardware is virtually identical neutronically to that employed previously in experiments M5 through M7. However, upgrade of the TREAT reactor between

1988 and 1990 resulted in significant changes to the neutronic environment within the TREAT core. One feature of the upgrade involved reconfiguring the array of transient rods and control/shutdown rods from a two-ring concentric pattern to a single-ring pattern. This caused changes in the neutron flux distribution at the experiment location as well as at the location of power and energy instruments at the core edge. Critical positions of the control/shutdown rods and transient rods were also changed.

Since power coupling factors are sensitive to the radial flux distribution in TREAT, they had to be characterized in the new core. It was principally for this reason that the M8CAL irradiations were performed. A large database of such calibration information had been developed for the pre-upgrade core configuration. "Bridging" data were needed to allow the information obtained using the new core to be analyzed and understood on the basis of the previous database. Calibration runs were included in M8CAL specifically for that purpose. Irradiations were also included to determine effects on power coupling and transient correction factors from changing the core loading from a full-slotted configuration as used in the M2 through M7 tests (for optimizing hodoscope performance) to a half-slotted configuration (for maximizing test-fuel energy deposition) needed for test M8 and later tests. Additional irradiations were included to span transients whose kinetics range between the 8-s-period exponential power transient used in M2 through M7 and longer-period transients planned for M8 and future tests. All M8CAL irradiations were performed after the reactor upgrade.

The methodology used to determine power couplings was similar to that used in past calibration experiments. Low-power irradiation of fresh fuel in a calibration vehicle is used to determine directly a test fuel-to-reactor power coupling by comparing energy release in the reactor with radiochemically measured fissions in the test fuel. Changes in power coupling due to burnup and swelling in extrapolating from fresh fuel to irradiated test fuel are calculated by neutronic analyses. Further extrapolation of measured power coupling from low-power irradiations to high-power transients are made by comparing fissions measured in a series of monitor-wire irradiations without test fuel present. These extrapolations of power coupling from low power to transient irradiations assume a constant ratio of fissions in test fuel to fissions in a monitor wire.

The M8CAL irradiations were performed in two campaigns. The first, comprising six irradiations in October and November 1990, was performed using a TREAT core loaded with a full-lateral slot through the core in line with the Fast Neutron Hodoscope. The second, comprising 17 irradiations between August 1992 and March 1993, was performed using a half-lateral slot extending only from the hodoscope to the test vehicle. Most of the samples irradiated were enriched-uranium flux monitor wires. Two irradiations each used a pair of fresh fuel pins similar in design to pins that are expected to be used in M8 and later tests. The compositions of uranium-zirconium test fuel were included containing 0, 19, and 28 wt% of plutonium.

All fuel irradiations were performed in the half-slotted core. Extrapolation of the results to the full-slotted core was accomplished by neutronic analyses and comparison of monitor-wire irradiations. M8CAL also extends the notion, developed previously, that the transient effects on power coupling may be correlated to control rod position, i.e., that a common correlation adequately describes rod position effects in data taken from both full-slotted and half-slotted TREAT cores.

The Fast Neutron Hodoscope was operated during many of the irradiations to collect reference information on signal and background strengths as well as neutron scattering effects in the experiment vehicle that will be useful in planning hodoscope operations for M8 and future experiments in the new core.

2. Description of the Calibration Hardware

Mark-IIIC test vehicles were used for all M-series loop experiments. The M8CAL experiments were conducted using a neutronic mockup of the Mark-IIIC vehicle. It was the same vehicle that had been used for trial transients and U-Zr monitor-wire irradiations prior to tests M2 through M7, including the M2CAL and M7CAL calibration series. Sodium was not required neutronically in the calibration vehicle, and it was not used.

Four fuel pins were required in the experiment: two U-10 wt% Zr pins, a U-19 wt% Pu-10 wt% Zr pin, and a U-28 wt% Pu-10 wt% Zr pin.

Nineteen U-Zr flux monitor wires were used in the experiment. Two 8-in. (20.3 cm) long, 0.030-in. (0.076 cm) diameter high-enriched uranium wires were composed of 3.6 wt% uranium, enriched to 93.1 wt% U-235. The remaining 17 low-enriched uranium wires were composed of 6.0 wt% uranium, enriched to 19.8 wt% U-235. The low-enriched wires were 0.040 in. (0.102 cm) in diameter and all but one were 8-in. (20.3 cm) long. [The one exception was 60 in. (152 cm) long.]

3. Experiment Operations

The variety of TREAT reactor operations used in the calibration irradiations is shown in Table IV.7.

TABLE IV.7. Matrix of Reactor Operations Used to Irradiate Fuel and Monitor Wires and to Provide Hodoscope Reference Data

Run Type	Full-slotted Core		Half-slotted Core	
	Rod Configuration	TREAT Energy (MJ)	Rod Configuration	TREAT Energy (MJ)
Heat Balance				
Full transient	B	655	C	655
Partial transient	B	405	C	423
8-s Period				
Full transient	D	996	D	1807, 1868
Partial transient	D	662	--	--
30-s Period				
Full transient	--	--	D	1957
Partial transient	--	--	C	602, 1199
80-s Period				
Full transient	--	--	D	1798
Partial transient	--	--	--	--
Low-level Steady-state	B	437H,* 667H*	A B	576H,* 480H* 576H

Note: Two TREAT energy values in a table entry indicates that a transient of each energy was performed.

*"H" indicates that the hodoscope was operated during at least one run at this energy.

Eight irradiations were run at a TREAT power level of 80 kW for 100 min, each generating 480 MJ. These irradiations are referred to as low-level steady-state (LLSS) runs. One of the LLSS runs irradiated a U-10Zr pin and a U-19Pu-10Zr pin; another of the LLSS runs irradiated a U-10Zr pin and a U-28Pu-10Zr pin; the six other LLSS runs irradiated monitor wires. Both of the fuel irradiations and one of the LLSS wire irradiations were performed in the half-slotted core; the remaining five LLSS wire irradiations were performed in the full-slotted core.

In addition, thirteen runs were made using power transients to irradiate monitor wires. Several types of transients were involved: (a) constant-power transients that are typically used before an experiment as heat-balance verifications of test fuel heating and cooling, and (b) transients with exponential-power rises to peak power and having exponential periods of 8 s, 30 s, and 80 s. The 8-s period transients were used to compare with similar transients run before TREAT upgrading. The 30-s period transient represents the transient planned for the M8 test. Transients with an 80-s period were included to provide information potentially useful for longer-term planning. Because the power coupling varies with rod position, the transients were performed with various rod configurations (designated A, B, C, or D) and various total TREAT energies, i.e., full and partial transients. The integral effect of power coupling on rod position during the transient is related to the total rod displacement and hence to the TREAT energy generation during the run. The Fast Neutron Hodoscope was operated during both of the runs with fuel pins to measure the relative signals from the pins. The hodoscope was also operated during several of the runs without fuel pins to measure the relative effect of neutron scattering from various components of the test vehicle (which contributes to the background signal).

4. Findings and Conclusions

M8CAL extended the database of TREAT power coupling measurements to the post-upgrade M-series core configurations. Focusing on tests M8 and beyond, in which reactor energy will need to be maximized, measurements concentrated on the half-slotted core. Power coupling measurements were performed on three fuel types including first measurements on high-Pu-fraction fuel. Measured axial peak power coupling factors were within several percent of

pre-upgrade values, and their relative values were in reasonable agreement with analytical expectations. Many M8CAL power coupling results are extensions of and similar to those found in M2CAL and M7CAL. In addition, the measured axial power shape was determined to be the same as measured pre-upgrade in the full-slotted core.

Based on the M8CAL data, sample energy generation capability in the full- and half-slotted cores are as given in Table IV.8. The upper limit of energy deposition and reactivity available for transient control was a physical limit, in the full-slotted core. In the half-slotted core, a reactivity of 6.5% was available physically, but only 5.8% is permitted administratively in conformance with TREAT operating specifications. Upper limits of TREAT energy release in a controlled transient are transient-dependent but principally determined by available reactivity and independent of the TREAT core loading. In particular, available reactivities 4.15%, 4.6%, and 5.8% correspond to maximal TREAT energy depositions of 1000, 1600, and 1900 MJ, respectively.

M8CAL data demonstrated that during low-power operation, power coupling is strongly influenced by the chosen method of reactor control and the rod configuration at critical. This is because, even though the transient and control/shutdown rods are symmetrically located

TABLE IV.8. TREAT Test Sample Energy Deposition Capability in a Controlled ("Shaped") Transient Based on M8CAL Results*

TREAT Core Configuration	TREAT Core Reactivity	Maximum Energy Generation in Fresh Fuel, J/g		
		Fuel Composition, wt%		
		U-10 Zr	U-19 Pu-10 Zr	U-28 Pu-10 Zr
Full-slotted	4.15%	5663	5268	4846
Half-slotted	5.8%	9321	8670	7975

*Not all of the available core fuel positions in the corner of the core were loaded with fuel elements.

relative to the experiment at the core center and to the transient power meters at the corners of the core, the transient rods are much closer to the steady-state meter situated on the west side of the reactor. Physically, the principal effect of using the control/shutdown rods instead of the transient rods to control the reactor (Configuration A instead of B) is to increase neutron flux to the steady-state power meter (the transient rods being fully withdrawn) and, consequently, decrease power couplings by the same factor.

Monitor-wire irradiations from previous M-series calibration work (M2CAL and M7CAL) indicated no significant differences in axial power shape for rod motions ranging between LLSS conditions and maximal high-power transients. No confirming measurements were made during M8CAL, since the same control rod configuration was used to irradiate the single long monitor wire and in all fuel irradiations.

Using a standard assumption concerning the proportionality of test fuel and monitor wire fissions, the large number of monitor-wire irradiations in M8CAL was used to empirically determine the dependence of power coupling on control rod position. M8CAL results extended previous studies by (1) including low-level steady-state (LLSS) irradiations with different rod configurations, (2) including movement of transient rods as well as control/shutdown rods, and (3) correlating data from both full and half-slotted cores. Good correlation between power coupling factors and control rod position was found. This showed that (1) LLSS power coupling depends importantly on whether transient or control/shutdown rods are used to control the reactor and (2) the dependence of power coupling on control rod position is essentially the same for the full- and half-slotted core. It also now allows better prediction of power coupling values for other transients than the one used in the M8CAL irradiations.

Procedures for analyzing hodoscope data from TREAT transients have been refined to correct the data more accurately for the effects of control-rod position on both the axial flux tilt and ion-chamber power reading. Hodoscope data-processing algorithms have been modified to compensate for these rod-position effects. The end result is a constant, power-normalized, response for all hodoscope channels in the unfueled M8CAL tests. An improved analysis, with increased sensitivity to movement of the test fuel, minimally affected by control rod position, is expected for the M8 test.

Work with M8CAL hodoscope data also led to the development of computer methods to form an axial response curve from hodoscope vertical scan data. For unfueled scans, this response depicts the axial distribution of the fast-neutron background; this background will be used to perform an improved hodoscope detector calibration for the M8 test. With fueled test assemblies, the fuel distribution is accurately revealed by a suitable analysis of the axial response. A direct comparison of axial response with the radiochemical axial fission distribution yielded excellent agreement for one of the calibration pins. The importance of optimizing the fixed horizontal collimator position for a vertical scan to obtain an accurate axial response is emphasized by the M8 calibration results. A comparison of properly aligned pretest and posttest axial responses should provide a highly sensitive means to observe axial fuel rearrangement in the M8 test.

In light of comparison of the specifications for M8 and for previous tests for which the hodoscope performance is known, it appears that the sensitivity of the hodoscope is more than adequate to determine the location and extent of pin failure, even though prefailure axial fuel motion in M8 may be less extensive than in previous tests.

F. Summary of Anticipated Future Testing Needs in TREAT

1. Test M8

The response of IFR fuel to overpower anticipated transient without scram (ATWS) conditions was investigated in reactor transient fuel testing in TREAT performed in the 1980s. Six tests, designated M2 through M7, were performed in flowing-sodium loops. They have been described previously.²¹ 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. 21. 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 eutectic liquefaction increases sharply. Irradiated ternary fuel in HT9 cladding 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 Mark-II fuel was used in Tests M2, M3, and M4.

The next experiment in the series, Test M8, will investigate cladding failure characteristics (margins, mechanisms, modes) under conditions in which cladding creep is expected to play a more significant role. To accomplish this objective, the fuel pins to be tested will have high plenum pressure (corresponding to that in a Mark-V fuel pin at about 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 half-slotted TREAT core configuration. 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.

One of the most valuable aspects of the M-series experiments is the close relationship 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.

As in Tests M5 through M7, two pins will be tested in M8 in individual sodium flow tubes. The two pins will be nominally identical. Because two Mark-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 high initial flowrate, is 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. A new test train designed for M8, and a suitable Mark-IIIC loop, are on hand for this test.

2. Test M9

After Test M8, the highest priority which the TREAT program will need to address is the investigation of the fuel response to loss-of-flow (LOF) without scram conditions. This will be the objective of Test M9. Information to be obtained in the test will include the prefailure in-pin fuel expansion, axial location of cladding breach, quantity of fuel ejection from the pins, and in-channel fuel dispersal.

A key issue in the LOF accident scenario is the response of a high-void-worth core to a rapid loss of flow. The positive reactivity excursion can be limited if the fuel melts and expands axially within the cladding before the cladding becomes hot enough to fail. That could occur since the half-width of the power pulse is small compared to the thermal time constant of the fuel pin. The decrease in the reactor power at the time of cladding failure caused by in-pin fuel motion would reduce the radial coherence of pin failures and therefore limit the reactivity pulse.

The feasibility of obtaining, in a TREAT test, the correct time sequence of events (in-pin fuel relocation prior to cladding failure) with sufficient time margin to resolve and measure phenomenological behavior was evaluated. SAS4A results show that such a test can be designed to present the correct event sequence and phenomena, although, because of uncertainties and limitations of TREAT performance characteristics (thermal spectrum, control rod speeds), time "windows" for sequence events would be narrower (i.e., in-pin relocation and cladding failure

would be closer in time) than in the predicted reactor sequence. It is uncertain if the window is wide enough to clearly discern whether in-pin fuel motion precedes cladding failure, but that uncertainty does not negate the value of performing the test. Principal uncertainties in the analysis of the fuel pin behavior include the molten fuel fraction before failure, the failure time relative to the onset of fuel motion at the fuel top, and the cladding conditions required to cause failure at this high heating rate.

A test train identical to the one slated for use in M8 is available for M9. It was designed for a TOP test. Its suitability for a LOF test will need to be evaluated, particularly regarding (1) the axial locations of thermocouples on the outer surface of the flowtubes and (2) the thickness of the flowtubes themselves. In the train, the thermocouples are concentrated near the top of the fuel column to provide maximum information about fuel failure and postfailure fuel relocation in an overpower sequence in which failure occurs close to the top of the fuel column. For a LOF sequence, however, failure is expected to occur in the fuel midplane region. Regarding flowtube thickness, although there has not been a significant breach in a flowtube in any of the M-series tests performed so far in which fuel pins failed (despite much molten fuel in the channel), the potential for flowtube meltthrough in a LOF sequence is greater. An existing loop, used in previous M-series tests, is available. Its shaping collar configuration provides an axial peak-to-average power ratio of approximately 1.1, a feature that will need to be accounted for in the detailed experiment planning. Fuel pins with reference (HT9) cladding material have not yet been selected for M9.

3. Subsequent Tests

Test M8 will probably be the last TOP test needed on EBR-II-length fuel. The investigation of loss-of-flow accident behavior will need to include several tests, of which M9 will be the first, in order to investigate key parametric dependencies and phenomena of primary importance. Tests of greater prototypicality are also needed to permit the occurrence of the integral effects on fuel and coolant dynamics that are associated with long fuel columns and/or fuel-pin bundles. Two types of geometry are envisioned for these tests: long-fuel pins in single-pin geometry as in M8, and short EBR-II-length fuel pins in three- or seven-pin bundle geometry.

The order of testing in these two latter series, including the accident type to be investigated, will be determined by the priorities of data needs for SAS4A validation.

G. TREAT Fast Neutron Hodoscope

1. Introduction

The TREAT fast-neutron hodoscope is a unique radiation imaging system developed and installed at the TREAT reactor about 30 year ago to provide time-resolved fuel-motion data.²² These data are an essential component of the IFR transient fuel testing program carried out in the Transient Reactor Test (TREAT) facility. Data on the movement of fuel under severe accident conditions are used to clarify and quantify the safety issue of the magnitude and sign of fuel-motion-generated reactivity changes during severe IFR accidents. In general, fuel dispersion is a desirable phenomenon, because it tends to shut down the chain reaction by introducing negative reactivity feedback; and fuel compaction is undesirable, as a mechanism that would introduce positive reactivity feedback. A hodoscope, optimized for tests on single pins, was installed about 17 years ago in the CABRI reactor.²³

TREAT experiment fuel motion data from the hodoscope have shown that sodium-bonded metal fuel has a generic tendency to expand, rather than compact, under transient heating. (See, for example, Ref. 24.) The test fuel pins have been observed to expand during transient heating prior to fuel failure, and the postfailure fuel movements are highly dispersive. The overall TREAT transient program for investigating IFR fuel provides realistic simulation of postulated severe fuel transient accidents, with attention to the effects of fuel design parameters and spanning the full range of prior irradiation including high burnup. The IFR TREAT experiments are designated as the M-series. To date, all IFR fuel experiments have simulated transient-overpower conditions. Transient-undercooling simulations with IFR fuel were planned to be introduced in the near future. As of the end of FY 1994, seven M-series tests had been performed, and hodoscope calibrations had been completed for test M8.

2. Hodoscope Description

a. Hodoscope Layout

The hodoscope views fission neutrons emitted by the test fuel. Spatial information is obtained from an array of neutron detectors external to the TREAT shielding, each detector aligned with one slot in a 360-slot steel collimator. Figure IV.6 is a general cutaway view of the TREAT reactor, showing the hodoscope collimator system and the viewing path through reactor shielding, reactor reflector, and reactor core. The hodoscope views an area about 120 cm high by about 7 cm wide at the reactor core center. Each collimator slot defines a viewing area, or "pixel" approximately 34 mm high by 7 mm wide. A more detailed drawing showing the horizontal plan view of the hodoscope and the vertical section through the hodoscope vertical midplane is presented in Fig. IV.7. This figure shows how the hodoscope is cross-focused vertically, so that it can cover the full core height in the core center, despite the limited vertical opening in the reactor shielding. Note that the detectors at the extreme bottom of the detector array view pixels at the extreme top of the test region. Figure IV.7 also shows how the collimator slots are cross-focused in the horizontal plane.

Methane-filled proton-recoil proportional counters comprise the primary detector array. Zinc-sulfide Hornyak-button scintillator detectors aligned behind the respective proportional counters are a backup secondary array. Pulse height data from either array are processed through pulse-height discriminators to bias out lower energy, scattered-neutron background. (Bias is set at approximately 0.5 MeV.) The detectors count neutrons simultaneously, and are read out as "frames".

b. Transient Operation

Frame data are recorded on a high-speed hard disk by a computer-controlled data acquisition system, at rates up to 1000 frames per second. Data from successive frames may be added together in posttest data analysis to improve counting statistics, consistent with the speed of fuel motion. For typical experiments, data combined to provide rates on the order of 20-50 frames per second are adequate to provide good representations of fuel motion.

Fig. IV.6. TREAT Reactor Cutaway, Showing Hodoscope in Place to View Test Section

Fig. IV.7. TREAT Hodoscope Plan View and Horizontal Layout

c. Steady-state Scanning

During steady-state operation of the TREAT reactor, the hodoscope collimator system may be moved under automatic computer control to scan the TREAT test section region in order to obtain high-resolution digital radiographic data of the "before" and "after" condition of test fuel. Figure IV.7 shows the pivot points used to tilt the collimator-array system. Note that the collimator is tilted vertically around the point in the reactor shielding where the collimator rays intersect, not about the pivot bearing located 133 cm back from this point of intersection. Location of the horizontal scanning centerline of the collimator system is not as critical, as indicated in Fig. IV.7. Scan data can be deconvoluted to improve scan radiograph resolution by mathematically removing the effects of the finite viewing dimensions of the collimator pixels.²⁵

3. Summary of M-series Fuel Motion Results with Ternary-alloy IFR Fuel

The last three M-series tests, M5, M6, and M7, were run with reference IFR ternary-alloy fuel that had been irradiated in EBR-II.² Results were consistent with those from previous transient-overpower tests with uranium-fissium Experimental Breeder Reactor (EBR)-II fuel alloy. Each test contained two pins, each nominally 343 mm long by 4.3 mm diameter, located side-by-side in separate flow tubes, spaced in the east-west plane so that both pins could be viewed individually by the hodoscope. The test section was contained in a Mark-III TREAT integral sodium loop. Sodium flow, initial temperature, and the reactor power-versus-time profile were set to generate prototypical thermal conditions in each test pin, for a reference overpower accident with an 8-s exponential period. Specific test requirements included terminating the power transient on the brink of failure for some fuel pins and just after failure for others. For this specific test configuration, the centerlines of the test pins were 51 mm north of the centerline of the TREAT core, and the test-section region viewed by the hodoscope was 121.5 cm high by 6.2 cm wide.

Table IV.9 is a tabular summary of the results from M5, M6 and M7. Test pins are listed in order of increasing prior burnup. The designators E and W indicate the east and

TABLE IV.9. Summary of Hodoscope Data from TREAT Tests M5, M6, and M7

Pin	Burnup (at. %)	Length Increase (%)	Relative Reactivity Worth Change in Test (%)		
			Prefailure	Postfailure	Total
M5F1W	0.8	-0.8	-1.0	(no failure)	-1.0
MF52W	0.4	-0.4	-1.0	(no failure)	-1.0
M5F1E	1.9	0.0	-0.8	(no failure)	-0.8
M5F2E	1.9	2.5	-2.3	(no failure)	-2.3
M6W	1.9	3.0	-5.0	(no failure)	-5.0
M7E	2.9	3.7	-1.9	(no failure)	-1.9
M6E	5.3	(failed)	-6.2	-16.8	-23.0
M7W	9.8	(failed)	-4.5	-21.0	-25.4

west pin, respectively, in a test. Preceding characters denote the TREAT experiment. Both M5 pins were run twice, in experiments denoted M5F1 and M5F2, respectively. All pins except M7E were clad with D9 alloy. M7E was clad with HT9. Reactivity worth changes are calculated by multiplying the observed transient axial fuel distributions by a cosine-squared worth distribution appropriate to EBR-II, integrating axially.

Note that there is not a direct correlation between observed change in length and reduction in fuel reactivity worth as calculated from the hodoscope mass-distribution data. This is because the fuel column in a pin with intact cladding undergoes internal changes in fuel and fuel-void distribution. Thus, a pin with intact cladding that had a relative increase in local voids near the pin midplane could have significant reduction in reactivity worth even if the vertical extent between the two ends did not change. It should be emphasized that the table lists length increases and relative worth changes for each test. Thus, the final lengths of pins M5FW and M5FE were increased by 1.2% and 2.5%, respectively, and the relative worth changes of these two pins after the second M5 experiment were -2.0% and -3.1% respectively. The two pins that failed, M6E and M7W, failed at 4.5 and 4.4 times nominal power in a fast reactor: 40 kW/m peak linear power, with 630K inlet temperature and 150K coolant temperature rise.

Figure IV.8 shows the relative reactivity worth changes calculated from the digital hodoscope fuel distribution data from experiment M7. Test pin E did not fail, but pin W did.

Fig. IV.8. Reactivity Worth Data from Test M7

4. Summary of M8 Calibration Results

After the completion of test M7, the configuration and operation of the TREAT control rods, experiment-reactivity compensation rods, and shutdown rods were changed so that all of the rods of each type were operated in a ganged mode. M8 was to be the first TREAT experiment using this mode. Thus, it was necessary for the M8 calibration series to include a comprehensive examination of rod-motion-generated reactor flux tilt effects, in addition to the usual checks of hodoscope instrument calibration, mass sensitivity, and scattered neutron background arising from the details of the design and positioning of the test loop. An additional, important, goal of each hodoscope calibration campaign is to exercise the system completely, including accurate alignment for the specific test configuration, reactor-hodoscope instrument/control interfaces, operation during scanning and transient operations, transient data collection, data correction, and data analysis.

The M8 transient was to use an exponential power excursion with a 30-second period. Calibration tests covering a range of transient power shapes and peak powers were performed to provide hodoscope data covering the expected range of counting rates. Figure IV.9 shows the power profile for a relatively low-power M8 calibration test (TREAT Transient No. 2873, which had a peak power 60.7 MW). Note the range of power covered during the simulation of an exponential power accident, and the rapid power rundown from peak power to a low-power "shelf," followed by reactor scram. The role of the low-power shelf is to provide sufficient fuel signal to permit the hodoscope to follow any post-peak fuel motion that occurs prior to final freezing in place.

Fig. IV.9. Power Data from M8 Transient (Transient No. 2873)

Flux-tilt hodoscope counting corrections for the M8 envelope were found to approach 20% over the full 120-cm core height, including flux-tilt effects on the ion chamber used for power normalization. (Maximum correction from the top to bottom of an M8 test pin is about 5%). The hodoscope data-processing algorithms were modified to compensate for these effects.

The M8 mass resolution calculated from M8 calibration data is a single-channel (single-pixel) uncertainty of 0.55 g fissile, or 8% of the fissile fuel viewed by a single channel that is aligned with one of the test pins. This is the one-sigma uncertainty, based on average counting statistics for a single channel in the 30-s exponential-period portion of the transient, for a 4-ms counting frame. Higher precision would be obtained by combining counting data from two or more successive 4-ms frames.

The full range of posttest data analysis was exercised using the M8 calibration data. Deadtimes and other nonlinearities in the data from the hodoscope proportional-counter array were measured, and corrections were incorporated in the data-processing algorithms. The experimentally based deadtime corrections averaged about $0.7 \mu\text{s}$, but varied from channel to channel.

Horizontal and vertical profiles of reactor background and power in the test region were obtained for use in detailed data-processing mass normalization. Figure IV.10 is a plot of horizontal scan data from an M8 calibration run and a deconvolution performed to obtain the "true" horizontal count-rate profile. Fuel shine from neutrons scattered from structural materials near the test pins was characterized from horizontal scan data. Fuel shine was found to constitute the order of 16% of the measured fuel signal.

Plots of the vertical scan data were analyzed to obtain an axial peak-to-average fuel power ratio of 1.11, in good agreement with the radiochemical-analysis value of 1.10. Figure IV.11 displays vertical scan data from counters located in a hodoscope column aligned with one of the M8 calibration test pins. The hodoscope-observed fuel length is in good agreement with the as-manufactured data. Parameters for setting up the hodoscope for the M8 pretest transients and scans were determined. Final vertical and horizontal alignment of the hodoscope collimator would be set on the basis of pretest scan data.

Fig. IV.10. Averaged Horizontal M8 Calibration Scan Data and Their Deconvolution

Fig. IV.11. Axial Scan of M8 Calibration Test Pin

H. Fuel Cycle Facility Ventilation System—Modeling and Results

This section describes an integrated study of the Argonne-West Fuel Cycle Facility (FCF) interconnected ventilation systems during various operations. Analyses and test results include first a nominal condition reflecting balanced pressures and flows followed by several infrequent and off-normal scenarios. This effort is the first study of the FCF ventilation systems as an integrated network wherein the hydraulic effects of all major air systems have been analyzed and tested.

The FCF building consists of many interconnected regions in which nuclear fuel is handled, transported and reprocessed. The ventilation systems comprise a large number of ducts, fans, dampers, and filters which together must provide clean, properly conditioned air to the worker occupied spaces of the facility while preventing the spread of airborne radioactive materials to clean areas or the atmosphere. This objective is achieved by keeping the FCF building at a partial vacuum in which the contaminated areas are kept at lower pressures than the other worker occupied spaces. The ventilation systems of FCF and the EBR-II reactor were analyzed as an integrated totality, as demonstrated in Fig. IV.12. The network model shown in Fig. IV.13 for the TORAC code was then developed.²⁶

The scope of this study was to assess the measured results from the acceptance/flow balancing testing and to predict the effects of power failures, hatch and door openings, single-failure faulted conditions, EBR-II isolation, and other infrequent operations. The studies show that the FCF ventilation systems are very controllable and remain stable following off-normal events. In addition, the FCF ventilation system complex is essentially immune to reverse flows and spread of contamination to clean areas during normal and off-normal operation.

1. Introduction

The Fuel Cycle Facility (FCF), located at Argonne-West, consists of five principal interconnected ventilation systems including two in which nuclear fuel is handled and processed.

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Fig. IV.13. Normal Operation

These latter two systems include the Air Cell Exhaust System (ACES) and the Safety Exhaust System (SES) for the argon cell. The remaining systems include the Building Supply Air System (BSA), the Building Exhaust System (BES), and the Stack Exhaust System (STES). In addition to the argon cell, these systems consist of a large number of rooms, doors, hatches, ducts, fans, dampers, and filters which together must provide clean, properly conditioned air to the worker occupied spaces of the facility. These systems must also prevent the internal spread of airborne radioactive materials to clean areas or to the external atmosphere. These objectives, which must be met at all times including upset conditions, are achieved in part by keeping the FCF below atmospheric pressure such that the contaminated areas are kept at lower pressures than the cleaner, worker occupied spaces. In addition, the general design objective is for air to be transported from relatively cleaner to relatively more contaminated areas, in particular for the ACES and BES systems. This causes the most contaminated spaces, in general, to have the lowest pressures in the facility.

The ventilation systems of FCF and EBR-II are analyzed as an integrated totality, as demonstrated in Figs. IV.12 and IV.13, since these two facilities have their effluents joined at the STES inlet. Some of the infrequent/abnormal cases were included to assess the mutual impact of FCF and EBR-II on each other. Note that EBR-II ventilation should itself be considered as two closely joined systems, i.e., the reactor building proper plus the Cover Gas Cleanup System (CGCS). The TORAC code²⁶ models the effects of the relatively high head EBR-II fans and the very low head CGCS fans as well as isolation events for EBR-II.

2. Development of the Ventilation System Model

A major purpose of the modeling effort is to calculate spatial pressures and pathway flowrates at various locations through the FCF during normal and off-normal conditions. The schematic given in Fig. IV.12 shows the basic flow distribution of air and argon throughout the facility which indicates that supply air is admitted to both the main floor and basement, which for the most part, is transferred to the main (first) floor in three different areas. Note that air is also supplied via infiltration through door/window leaks and small wall cracks which bypass the BSA fan.

a. Flow Network Model for FCF

Other than the argon cell exhausted by the SES, the most heavily contaminated air is handled by the Air Cell Exhaust System (ACES) which involves the air cell, decontamination systems, the air cell to argon cell transfer tunnel, and the suited entry repair area. The cleanest air in the facility is handled by the BES which transports air through large spaces in the basement as well as the main floor. Exhaust from the ACES and BES join via piping just before entering the STES where EBR-II/CGCS effluent also joins. Note that the SES exhaust enters the STES downstream from the stack fans near the base of the 200 ft stack.

From the above basic flow diagram and requirements for nodal detail, a flow network representation of the above ventilation systems was constructed as shown in Fig. IV.13. The basic approach, which was designed to be consistent with the available analytical codes, is to have branches that join each other at nodes. By definition, each branch must contain either a fan or a hydraulic resistance and each node is either a boundary node or an internal volume node. Note that boundary nodes contain no volume and are usually simulated at atmospheric pressure where infiltration air enters or leaves the facility. The internal nodes, however, always contain volume and together include all the volume and mass within the FCF building. The basic assumptions made in the TORAC analysis, in general, can be grouped into those associated with gas dynamics, pressure loss characteristics and approximations in the detail of the hydraulic network model. Some rooms are lumped into larger regions even though these spaces may not be at truly equal pressure and may have multiple inlet/outlet flow paths. An effort was made, however, to define transfer air paths and ducted paths separately since they can have different flow characteristics. Note that transfer air paths typically include doorways and hallways but ducted paths can include fans, filters, dampers, ducts and junctions. The TORAC code can distinguish between filters having different characteristics such as HEPA filters, pre-filters, and charcoal filters. In addition, the individual ΔP vs flow equation for filters can be adjusted to approximate the effects of being highly loaded and/or operating at very high flows. All HEPA filters in this study are assumed to have a linear pressure drop vs. flow characteristic, while a flow squared dependence is assumed for all dampers, valves, orifices, ducts and infiltration paths.

A choice was made to simulate all of the branches at boundary nodes by a damper resistance and (except for nodes 44 and 54) have them represent infiltration points at atmospheric pressure. Node 44 is a simplified representation of the air inlet and fans for the FCF roof corridor. Node 54 represents the inlets and fans for the safety equipment building containing SES equipment. Note also that some flow to the roof corridor bypasses the exit cell in going to the pair of fans downstream from the cell. As described below, node 1 is the boundary node for supply air to FCF and nodes 47 and 64 represent atmospheric air inlet points to the EBR-II and CGCS, respectively.

b. Safety Exhaust System

The argon cell itself is not represented in the model since flow from this cell to the stack occurs only during an abnormal event involving the argon cell and/or the SES. The SES actually consists of two "trains", each of which has an argon branch (which normally does not operate) and an air branch (which always operates). In the plant, one of the trains is designated the active train and the other is thus the standby train. In the TORAC model, only the air branch of the SES is simulated and it is represented by two dampers and a fan connected in series. This branch connects the subcells and cubicles (containing air and located adjacent to the argon cell) and transports the effluent directly to a point just upstream of the stack base. The simulation of all of the branch resistance by two dampers is a good approximation because of the high hydraulic resistance in the system. Thus, the effect of the pressure drop of the two HEPA filter banks in the branch is small compared with the remaining losses.

Some TORAC cases have been run that simulate both the SES air and argon branches in operation. This condition was approximated by "converting" the argon to air (by similarity laws) and increasing the total air flow through the SES dampers and fan. For a more sophisticated treatment of the SES and argon cell operation, other ANL-IL models were developed specifically to analyze the argon cell transient pressure/temperature behavior and fire dynamics, respectively.

c. EBR-II Reactor Building and CGCS Ventilation Systems

Sufficient detail of the EBR-II ventilation systems was included to model the effects of FCF disturbances on EBR-II/CGCS and vice versa. The EBR-II reactor (containment) building has an air supply system which feeds two parallel cooling systems, one for the reactor shielding and the other for the instrument thimbles containing ion chambers (for measuring neutron flux). During normal operation, this air is combined and flows out of containment, passing through HEPA filters and isolation valves. Next it joins the CGCS effluent on its way to the STES fan inlet plenum. The CGCS radiation monitoring is done in the CGCS building attached to the reactor building.

3. The TORAC Code

The TORAC code employs the same node and branch flow network structure that is common to most HVAC network codes. The code is designed to predict airflows in an arbitrarily connected network that may include process cells, high bays, corridors, laboratory offices, canyons, truck locks and offgas systems. Thus, the code simulates the movement of air into, through, and out of the facility. The hydraulic network can contain components such as ducts, filters, dampers, rooms, blowers, and infiltration paths.

The code uses the lumped-parameter approach and no direct effect of spatial distribution of parameters is considered. All ventilation system components that exhibit resistance, such as dampers, filters, and blowers, are located within the branches (which join the nodes) of the network. These internal nodes all contain volume as opposed to the external boundary nodes which do not. The pressure drop characteristics of most components must vary with flow squared except for filters, infiltration paths²⁷ and blowers which can be different. The continuity equation must be satisfied at every node along with a pressure flow relationship for every branch. In addition, the equation of state for a perfect gas must be satisfied for each volume node.

The code also has the capability to analyze material transport with a basic model that includes convection, depletion, entrainment, and filtration of the material. Material transport effects and tornado disturbances, however, were not included in the current study. Note also that TORAC does not treat thermal effects; thus buoyancy driven flows due to temperature and elevation variations are excluded. Although the code can simulate a process in the steady-state or transient domain, most of the studies to date were solved in steady-state since only equilibrium test data were available. One very useful feature of TORAC is that it calculates resistance coefficients which can be used directly as inputs for future runs—when nodal pressures and branch flow rates are specified as input initial conditions. Note also that branches can be closed, component resistances can be changed, and fans can coast down (or start up) during a transient. Simulating branch loss changes is most easily done via damper resistance changes; thus such branches should include dampers in their paths.

The TORAC source code was obtained from the Software Center and was installed at ANL-East in 1991. The IBM mainframe version of the code was later converted to the SUN System (of the ANL Reactor Analysis Division) in 1992.

4. Method of Analysis

The flow network model, shown in Figs. IV.13 through IV.16, has inscribed values of nominal pressures and flows at the nodes and branches, respectively. Although these values were derived from acceptance test/flow balancing data, measurements were not directly available for all 64 nodes and 85 branches—but instead for only 16 internal nodes and 25 branches. Thus, the nominal values for the remainder of the network were obtained using fundamental principles such as mass balances in order to make hydraulic loss estimates from the basic set of measured pressures and flows. Because TORAC does not perform an engineering analysis from physical data such as duct dimensions, valve coefficients or filter properties, the above set of node and pressure data becomes the basic input to establish the initial conditions for transient analyses. Note that the flow (CFM) is given for each branch, and the pressure (in. w.g.) is given (in italics, with decimal point) for each node.

Fig. IV.14. Normal Operation Except Supply Fan Off

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Fig. IV.15. One BES and Supply Fans Off

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Fig. IV.16. One ACES and Supply Fans Off

Head vs flow data were required for the nine fans in the network (five in FCF and four in EBR-II/CGCS). The required data for normal fan operation were available for all of the major FCF fans. In regard to EBR-II fans, the head curve data were available for only the shield cooling and instrument cooling fans—the remaining head curves had to be estimated from similarity laws knowing only the design operating points for these fans. All fans operate at constant speed, except for startup/coastdown conditions, and most of them have attached inlet vanes (IV) for automatic flow control. Although the effect of variable IV can be modeled by TORAC, in this study they were held at constant settings (usually 100% open or full closed). For off-normal fan operation, unpowered fan characteristics were required for forward and reverse flows, for both stationary and free-spinning fan blades. This information is difficult to obtain and had direct measurements for these conditions were available for only the new BES fans. The unpowered data for the remaining fans were then estimated from the BES data using similarity laws and taking into account each fan's normal operating characteristic.

All pressures shown are relative to the local atmospheric pressure at the INEL site (338 in. w.g.), and all nodes are taken to be effectively at the same elevation because isothermal conditions are assumed. Thus all pressures are shown as differentials relative to atmosphere.

5. Results of TORAC Analyses

a. General

The TORAC hydraulic network model is the only tool utilized at ANL thus far for analyzing complex interconnected ventilation systems. Besides validation studies with measured FCF data, earlier TORAC simulations have been validated elsewhere for other facilities.²⁶ The data obtained from flow balancing and infrequent operations testing indicated that the main floor pressures and infiltrations were higher than expected, while most of the pressures in the basement and ACES spaces were lower than expected. Although not all cases studied can be included in this section, the complete study did include the following aspects:

- Parametric variations about the normal operating state
- Infrequent/abnormal operation
- Local and overall electric power failures
- Single failures of active components
- Unlikely stack exhaust system failures
- EBR-II isolation

b. Normal Operation—With and Without the Supply Fan

After the TORAC model was "calibrated" to the nominal operating pressures and flows, another (near normal) condition was simulated in which the BSA fan was turned off and no other system changes were made. The TORAC results for the BSA fan turned off are given in Fig. IV.14 which shows, by comparison with Fig. IV.13, the supply fan to have a very small effect on the total FCF flow and its distribution, but a large effect on the building infiltration flowrates. It is important to note that no infiltration rates were directly measured—they are deduced from internal flow balances made from test data. As shown in both Figs. IV.13 and IV.14, the main floor pressure differentials for the BSA fan on or off remain very small. They range from -0.005/-0.008 in. w.g. below atmosphere (BSA fan on) to -0.024/-0.025 in. w.g. (BSA fan off), as noted in the main floor nodes 5, 27 and 38 shown in Figs. IV.13 and IV.14. Noting the increases in the infiltration rates (see flows in branches 53, 54 and 55), it is observed that the infiltration path resistances must also be small.

With the BSA fan turned off, the total infiltration increases by about 97% which is reflected in the main floor pressure changes and in the decreased BSA flow (through the free-spinning supply fan)—a predicted decrease of about 95%. The assessment of this operating condition showed that the FCF design requirements of pressure and flow distribution were still met and, in addition, provided valuable data for additional TORAC code validation.

c. Operation with Failure of One BES Fan

FCF operation with one BES fan failed is infrequent but still regarded as allowable in terms of maintaining acceptable pressures and flows throughout the building. In this event, the isolation damper of the failed fan branch is closed, and the BSA fan is interlocked to trip out upon a low BES fan flow. Note that the IV control for the operating BES fan would attempt to maintain total BES flow and thus open its IV to maximum. In addition, the STES IV control would attempt to do likewise to maintain the stack flow; however, the TORAC study did not include these IV control effects. The results given in Fig. IV.15 show that the FCF pressures and flows of primary concern are not strongly affected by the failure of one BES fan. The total BES flow would decrease by no more than 35% and the STES flow by no more than 14%, while the total ACES flow is predicted to increase by about 7% due to compensating changes in system pressures.

TORAC does predict flow reversals in two main floor branches, namely 25 and 27. The consequences of these reversals are not serious, however, since they involve supply air flowing in ducted paths between clean areas. Note that the pressures mainly affected by this event, as expected, are basically associated with the BES, such as nodes 6 through 9, 12, 13, and 39.

d. Operation with Failure of One ACES Fan

FCF operation with one ACES fan failed is also infrequent and regarded as allowable because acceptable pressures and flows are maintained throughout the building. In this event, the isolation damper of the failed fan branch is again closed, and the BSA fan is interlocked to trip out upon a low ACES fan flow. Note that the IV control for the operating ACES fan would attempt to maintain total ACES flow and thus open its IV to maximum. The STES IV would again attempt to go full open to maintain stack flow. Since the TORAC study did not include these control effects, all changes in pressures and flows will again tend to be overpredicted. The results given in Fig. IV.16 show that the pressures of primary concern are not significantly affected by the failure of one ACES fan. The total ACES flow would decrease

by no more than 36% and the STES flow by no more than 8%, while the total BES flow is predicted to increase by about 8% due to system pressure changes.

TORAC again predicts benign flow reversals in the main floor branches 25 and 27. Note that the pressures mainly affected by this event, as expected, are associated with the ACES such as nodes 21, 23, 28 through 33, and 52.

6. Conclusions

Upon completion of this study, it is concluded that the effects of hydraulic disturbances to the interconnected ventilation systems do not severely challenge the FCF. The results reported herein show that the ventilation systems are very controllable and remain stable following off-normal events. In addition, the FCF ventilation system complex is essentially immune to reverse flows, negative infiltration, and spread of contamination to clean areas during normal and off-normal operation. Compared to normal conditions with all fans operating, the acceptance/infrequent operational testing and TORAC analyses showed that the ventilation systems have a high tolerance for a major fan being out of service. The examples studied include a failure of the building supply fan considered separately; and failure of one of the air cell exhaust fans, and one of the building exhaust fans—each interlocked to trip out the supply fan. These results indicate that the facility can continue to operate under these abnormal conditions and still provide acceptable pressures and flows throughout the building.

The comparison of the TORAC code predictions with measured results of many aspects of upset scenarios indicated good agreement. The code was judged to be a satisfactory tool for analyzing various FCF operating conditions with the simulated hydraulic network that includes both FCF and EBR-II/CGCS.

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V. CORE DESIGN DEVELOPMENT

A. Progress and Applications of the Variational Nodal Method

1. Introduction

The variational nodal method is finding increased use for the performance of multidimensional, multigroup transport problems in reactor physics. It is implemented at Argonne National Laboratory as the VARIANT (VARIational, Anisotropic Nodal Transport) module of the DIF3D code system.^{1,2} The module solves multienergy-group odd-order spherical harmonics equations through the P_5 approximation, and several intermediate angular approximations are also available.^{3,4} Nodal calculations may be performed, with anisotropic scattering, in x-y, hexagonal, x-y-z and hexagonal-z geometries. It is used extensively for fast reactor criticality and gamma heating calculation in conjunction with the IFR program and the analysis of EBR-II.⁵ More recently, it has been adapted to perform calculations in the French fast reactor code system, ERANOS, at Cadarache, and has also been used for the analysis of thermal reactor experimental configurations.^{6,7}

A number of the variational nodal method's defining features result in the elimination of some of the shortcomings found in other nodal formulations.^{3,8} The method originates from a well-defined variational principle to which numerical approximations are applied with a classical Ritz procedure. Moreover, the construction of the functional ensures nodal balance regardless of the trial functions used in the Ritz procedure. Unlike other interface-current nodal methods, the use of spherical harmonics trial functions in angle creates a clear hierarchy of angular approximations, and unlike S_N nodal methods, there are no ray effects. Likewise, the use of complete polynomials in the spatial trial functions eliminates the diagonal depression found in nodal methods based on quasi-one-dimensional DP_N approximations. The variational nodal method converges to a well-defined spherical harmonics approximation as the spatial mesh size, h , tends to zero. A more computationally efficient way to reduce truncation error, however, is through p-type convergence in which the order of the polynomial spatial trial functions is increased instead of reducing the dimensions of the spatial nodes.

2. Formulation

The Variational Nodal method is based on a functional of the form

$$F[\psi, \chi] = \sum_v F_v[\psi, \chi] \quad (1)$$

where

$$F_v[\psi, \chi] = \int_v dV \left\{ \int d\Omega \left[\frac{1}{\sigma} (\hat{\Omega} \cdot \vec{\nabla} \psi)^2 + \sigma \psi^2 \right] - \sigma_s \phi_i^2 - 2\phi S \right\} + 2 \sum \int d\Gamma \int d\Omega \hat{n}_\gamma \cdot \hat{\Omega} \psi \chi_\gamma \quad (2)$$

is the contribution from the node v . Requiring the functional to be stationary with respect to variations in ψ and χ , the even- and odd-parity components of the angular flux, yields the even-parity equation

$$-\hat{\Omega} \cdot \vec{\nabla} \frac{1}{\sigma} \hat{\Omega} \cdot \vec{\nabla} \psi(\vec{r}, \hat{\Omega}) + \sigma \psi(\vec{r}, \hat{\Omega}) = \sigma_s \phi(\vec{r}) + S(\vec{r}) \quad (3)$$

within the nodes and continuity conditions on ψ and χ across the nodal interfaces. The functional has been generalized to treat multigroup fixed source and eigenvalue problems and both within-group and group-to-group anisotropic scattering. Vacuum boundaries are also incorporated as modified natural boundary conditions, while reflected boundaries are essential. For brevity and clarity, the within-group isotropic scattering form of the functional given by Eq. (2) is used to briefly summarize the method.

The derivation of the equations used in variational nodal computations begins with a classical Ritz procedure. Writing the even-parity flux as an expansion of trial functions in space and angle with unknown coefficients, the even-parity flux appears as

$$\psi(\vec{r}, \hat{\Omega}) \approx \mathbf{f}^T(\vec{r}, \hat{\Omega}) \boldsymbol{\zeta} \quad \vec{r} \in V_v, \quad (4)$$

and the odd-parity flux at the interfaces take the form

$$\chi(\vec{r}, \hat{\Omega}) \approx h^T(\vec{r}, \hat{\Omega})\chi \quad \vec{r} \in \Gamma_v. \quad (5)$$

The known trial functions are complete polynomials in space and even and odd spherical harmonics, respectively, in angle. The nodal contribution to the reduced functional is then

$$F_v[\zeta, \chi] = \zeta^T A \zeta - 2\zeta^T s + 2\zeta^T M \chi \quad (6)$$

where the A and M matrices are integrals over the known trial functions, and s is the group source vector.

Requiring F to be stationary with respect to a variation of the unknown coefficient vectors, ζ and χ yields

$$A\zeta = s - M\chi \quad (7)$$

within the node, and requires continuity of the quantity

$$\psi \equiv M^T \zeta \quad (8)$$

across the interfaces. Combining Eqs. (7) and (8) yields

$$\zeta = M^T A^{-1} s - M^T A^{-1} M \chi. \quad (9)$$

These equations, coupled with the continuity requirements on ψ and χ across the nodal interfaces may be viewed alternately as a form of hybrid finite-element method or as a generalization of the T^{-1} form of a response matrix, in which the even-parity moments at the surface are given in terms of the odd parity moments.⁹ To make use of the physical analog of neutrons crossing interfaces, however, these equations are transformed by defining vectors of partial current moments:

$$j^\pm = \frac{1}{4} \Psi \pm \frac{1}{2} \chi \quad (10)$$

Eliminating the even- and odd-parity fluxes then yields that conventional response matrix form:

$$j^+ = Rj^- + Bs \quad (11)$$

where

$$R = \left[\frac{1}{2} M^T A^{-1} M + I \right]^{-1} \left[\frac{1}{2} M^T A^{-1} M - I \right] \quad (12)$$

and

$$B = \left[\frac{1}{2} M^T A^{-1} M + I \right]^{-1} \frac{1}{2} M^T A^{-1}. \quad (13)$$

The even-parity flux within the node can be recovered from the auxiliary equation

$$\zeta = A^{-1}s - A^{-1}M(j^+ - j^-). \quad (14)$$

3. Implementation

VARIANT, the production implementation of the variational nodal method, has a number of salient features. It makes extensive use of geometry, cross section, editing and other modules of the DIF3D code system, resulting in compatibility for a large number of reactor physics calculations. Extensive use is made of symbolic manipulation to evaluate the large arrays of known space-angle integrals which result from the Ritz procedure used to discretize the equations. The arrays are evaluated using Mathematica and the results stored as data statements in the response matrix generating FORTRAN subroutines of VARIANT. Since the calculations are performed in dimensionless form, each time a new geometry or trial function set is implemented they need be carried out only once, and stored in the appropriate subroutine.

The response matrix equations are solved with a red-black (or in the case of hexagonal geometry a four-color) iterative algorithm. The iterations are accelerated by partitioning the matrices such that only one term per interface, the partial current, is included in the iterations except for periodic updates using the entire response matrix.¹⁰ The code, as originally written for the CRAY XMP, is highly vectorized. More recently it has been ported to IBM RS6000 and SPARC 20/50 work stations, and there the vectorized code seems to work well,

often performing at rates in excess of 35 MFLOPS on the RS6000. Limited one-group fixed-source x-y calculations have also been performed using the method on a Connection Machine-5, with indications that the red-black algorithms are highly effective on parallel computers.¹¹

The three-dimensional capability of the code is illustrated with two of the Takeda benchmarks,¹² models I and IV. Table V.1 shows eigenvalues and rod worths for a variety of methods applied to the model problems. VARIANT results for diffusion and transport (using a P_3 expansion) are presented for several different machines. The advantages of vectorization are obvious from the CPU times, as clearly demonstrated by significantly smaller ratios of transport to diffusion running times on the vector architecture of the CRAY.

TABLE V.1. Eigenvalues and Control Rod Worth: Takeda Benchmarks I and IV

Method	Takeda I			Takeda IV		
	$k_{\text{eff}}(\text{rod out})$	$k_{\text{eff}}(\text{rod in})$	rod worth	$k_{\text{eff}}(\text{rod out})$	$k_{\text{eff}}(\text{rod in})$	rod worth
VARIANT P_1	0.9296	0.9328	$-3.69 \cdot 10^{-3}$	1.0751	0.8539	$2.13 \cdot 10^{-1}$
VARIANT P_3	0.9774	0.9632	$1.51 \cdot 10^{-2}$	1.0959	0.8801	$2.24 \cdot 10^{-1}$
P_n	0.9776 ± 0.00058	0.9630 ± 0.00078	$1.45 \cdot 10^{-2} \pm 0.0022$	1.0942 ± 0.0015	0.8819 ± 0.010	$2.20 \cdot 10^{-1} \pm 0.120$
S_n	0.9772 ± 0.00007	0.9624 ± 0.00008	$1.58 \cdot 10^{-2} \pm 0.00003$	1.0887 ± 0.0043	0.8927 ± 0.011	$2.02 \cdot 10^{-1} \pm 0.100$
Monte Carlo	0.9778 ± 0.00046	0.9623 ± 0.00048	$1.63 \cdot 10^{-2} \pm 0.0007$	1.0951 ± 0.00035	0.8799 ± 0.00033	$2.23 \cdot 10^{-1} \pm 0.005$

Machine Type	CPU Time (s)			
	Takeda I		Takeda IV	
	VARIANT P_1	VARIANT P_3	VARIANT P_1	VARIANT P_3
SPARC 2	44.4	613	23.2	1430
SPARC 20/50	15.1	224	8.3	414
IBM RS6000	6.4	101	3.7	243
CRAY XMP	7.2	32	4.1	56

4. Current Work

As experience has been gained using the standard form of VARIANT, documented in Refs. 1 and 2, interest has grown in further development to enable the treatment of more complex and physically demanding problems and in further reducing computation costs. In attempting to meet these requirements, current work has also exposed the need for further theoretical development. The following section is a review of current progress in diffusion theory, burnup calculations, highly heterogeneous cores, higher-order spherical harmonics approximations, perturbation theory and heterogeneous nodes.

a. Diffusion Calculations

The primary thrust of the variational nodal method has been in the development of three-dimensional transport methods for fast reactors. However, interest has increased in using it as an alternative to existing nodal diffusion methods, particularly for the treatment of thermal reactors in hexagonal geometry. Comparisons have been made between VARIANT and the DIF3D-finite-difference and DIF3D-nodal diffusion modules.¹³ Representative eigenvalue and power distribution errors are tabulated in Table V.2 for three, two-dimensional benchmarks in hexagonal geometry. The results indicate that VARIANT is generally more accurate than DIF3D-nodal diffusion; a conclusion which carried over to comparisons which have thus far been made for three-dimensional hexagonal benchmarks.

Table V.2 also indicates that VARIANT does very well relative to DIF3D-nodal in CPU times for two-dimensional problems. At present, however, VARIANT is significantly slower than DIF3D-nodal in three dimensions. The axial sweeping scheme which accounts for this is presently under revision. It should also be noted that VARIANT coding was optimized for vector computers and is most effective for the larger dimensions of transport response matrices. Conversely, DIF3D nodal was optimized to minimize the number of floating point operations. Since the codes are both presently run on work stations, it may be beneficial to configure a version of VARIANT to be optimal to diffusion calculations on scalar machines.

TABLE V.2. K_{eff} and Node-Power Maximum Deviations for Two-dimensional Hexagonal Geometry Test Cases^a

Code/ Code Option ^b	ANL Large HWR ^c			VVER-1000 ^d (0.5 albedo)			IAEA ^d (0.125 albedo)		
	K_{eff} (%)	P_{node} (%)	CPU ^e (s)	K_{eff} (%)	P_{node} (%)	CPU ^e (s)	K_{eff} (%)	P_{node} (%)	CPU ^e (s)
DIF3D-FD ($\Delta/2$)	-0.0004	0.06	155.71	0.0012	0.16	52.61	-0.0082	0.43	38.46
DIF3D-FD (Δ)	-0.0015	0.25	17.92	0.0048	0.64	7.81	-0.0327	1.70	7.43
DIF3D-Nodal	-0.0167	1.44	1.32	0.0235	4.94	1.17	-0.4097	20.19	1.11
ANC-HM	0.0048	0.2	-	0.0035	1.2	-	0.0110	0.4	-
VARIANT-40.11	0.0051	0.14	1.62	0.0576	5.48	1.27	0.0506	1.69	1.08
VARIANT-41.11	0.0002	0.03	2.43	-0.0019	0.79	1.44	0.0302	1.08	1.26
VARIANT-61.11	-0.0002	0.01	2.66	0.0006	0.04	1.46	-0.0003	0.01	1.42
VARIANT-62.11	-0.0000	0.00	3.26	-0.0009	0.16	1.59	0.0018	0.07	1.47
VARIANT-41.33	0.0239	0.71	7.11	0.0710	3.85	4.06	0.0049	2.18	2.33
VARIANT-61.33	0.0239	0.71	8.63	0.0757	4.83	4.29	-0.0268	3.47	3.87
VARIANT-62.33	0.0230	0.69	16.51	0.0736	4.62	7.54	-0.0222	3.29	5.86

^aDeviations in all cases are relative to reference fine-mesh diffusion results.

^bThe VARIANT solution designated as VARIANT-mn.ij employs polynomials of order m for the within-node flux and source and order n for the node-surface currents, and the P_i and P_j (e.g., P_3) transport approximations within the node and on the node-surface, respectively.

^cThe DIF3D-FD($\Delta/2$) and DIF3D-FD(Δ) solutions were obtained using 384 and 96 triangular cells/hexagon, respectively, and were used to obtain the reference FD solution via Richardson extrapolation.

^dThe DIF3D-FD($\Delta/2$) and DIF3D-FD(Δ) solutions were obtained using 864 and 216 triangular cells/hexagon, respectively, and were used to obtain the reference FD solution via Richardson extrapolation.

^eCPU time on the IBM RS6000 workstation.

b. Burnup Calculations

Recently, the VARIANT module has been incorporated into the REBUS¹⁴ code system. This allows the performance of three-dimensional burn-up calculations in conjunction with very accurate diffusion solutions and transport capabilities. To illustrate this new application of VARIANT, Tables V.3 and V.4 list results from two benchmark problems taken from a validation suite for the DIF3D/REBUS package. VARIANT diffusion and transport results are provided and compared to finite difference and transverse integrated nodal methods. In its current implementation, VARIANT is rather inefficient for this application since the entire set of response matrices is recalculated on each REBUS iterations, thus dramatically increasing

TABLE V.3. Reactivity Changes for Two Three-dimensional REBUS Benchmarks

Method	XYZ Benchmark			Hex-Z Benchmark		
	Initial K_{eff}	Final K_{eff}	Reactivity	Initial K_{eff}	Final K_{eff}	Reactivity
VARIANT P_1	1.06109	0.80719	0.296	1.00609	1.03549	$-2.82 \cdot 10^{-2}$
VARIANT P_3	1.06630	0.80237	0.308	1.01375	1.04612	$-3.05 \cdot 10^{-2}$
DIF3D FD	1.06440	0.80739	0.299	1.02106	1.05452	$-3.11 \cdot 10^{-2}$
DIF3D Nodal	1.06144	0.80729	0.297	1.01049	1.03882	$-2.70 \cdot 10^{-2}$

TABLE V.4. CPU Times for Two Three-dimensional REBUS Benchmarks

CPU Times (s)*		
Method	XYZ Benchmark	Hex-Z Benchmark
VARIANT P_1	29.6 (37.32)	34.9 (48.9)
VARIANT P_3	573.0 (581.0)	1170. (1190.0)
DIF3D FD	11.3 (29.1)	29.4 (62.8)
DIF3D Nodal	15.9 (34.0)	27.1 (60.1)

*The first time listed corresponds to CPU time spent on neutronics, and the time in parenthesis is the total REBUS job time.

the cost of the entire burn-up calculation. In addition, only the surface averaged partial current is passed between REBUS iterations, and the higher order moments are lost. This results in VARIANT performing a few extra outer iterations to recover the higher order partial current moments. While correcting the partial current moment problem is straight forward, the problem of recalculating response matrices is more difficult. A first step would involve recalculating response matrices only for those nodes in burn-up regions. A more sophisticated addition might involve the development of an interpolation scheme allowing approximation of new response matrices for small changes in cross section values, thus mitigating the cost of generating new response matrices for each burn-up step. While such developments are not strictly necessary to apply accurate VNM diffusion approximations to burn-up calculations, the utilization of the VNM transport capabilities with realistic reactor configurations motivates examination of interpolation schemes.

c. Highly Heterogeneous Cores

Running times may be divided into the formulation of the response matrices and the solution of the resulting red-black or four color algorithm. In problems where the number of unique node types is small compared to the total number of nodes, the time required to generate the response matrices is insignificant compared to the solution time. For these classes of problems high-order (nearly exact) spatial approximations may be used within the nodes with little cost penalty, since the solution time is dependent only on the response matrix dimension, and, therefore, on the level of space-angle approximations along the interfaces. For highly heterogeneous reactor cores such as the EBR-II, however, there are nearly as many node types as nodes, and thus the generation of response matrices may occupy a substantial fraction of the total computing time. Since the generation time increases rapidly with the number of terms in the within-node space-angle approximation, it becomes important to use the lowest-order space angle approximation which is compatible with the interface approximation.

A number of approaches are being examined for reducing the response matrix formulation effort without a commensurate loss in accuracy. The most ambitious consists of eliminating the present red-black iteration scheme in favor of a matrix splitting techniques

which operates on Eqs. (7) and (8) directly and thereby does away with the $\mathbf{A}$ matrix inversion required to form the response matrix for each node type. Other approaches maintain the solution algorithm but concentrate on reducing the number of internal trial functions, and therefore the dimension of the $\mathbf{A}$ matrix which must be inverted. Reduced and simplified spherical harmonics approximations have been formulated.⁴ The reduced method has been employed in hexagonal-z problems, while to date simplified P_3 has only been implemented for x-y geometry test problems.

Examination was also undertaken to determine the lowest-order complete polynomial spatial approximation which can be used with a given interface approximation. Here, however, examination of diffusion methods indicated that unless the $\mathbf{M}$ matrix which couples the spatial approximation within the node with those at the interface has the same rank as the number of interface basis functions, convergence of the red-black response matrix iterations cannot be guaranteed beyond five or six decimal places.¹⁵ In two-dimensional hexagonal geometry, for example, a sixth-order polynomial with 28 terms is required for the twelve-basis-function linear interface approximation. Investigations are being undertaken to find finite-element or other classes of internal trial functions which will allow substantially fewer terms to be used in achieving the required rank and therefore reduce the computational effort required to form the response matrices.

d. General Order Spherical Harmonics Calculations

Until recently, only P_1 and P_3 approximations have been formulated in the variational nodal methods. In the P_1 approximation the odd-parity interface angular trial function is $P_1(\mu)$ where μ is the direction cosine perpendicular to the interface. In the P_3 approximation, the correct number of odd-parity conditions is obtained by requiring interface continuity of $P_1(\mu)$, $P_1(\eta)$, $P_1(\xi)$, $P_3(\mu)$, $P_3(\eta)$ and $P_3(\xi)$ moments,³ where η and ξ are the direction cosines parallel to the interface. Unfortunately, including the corresponding P_5 or higher terms does not yield the correct number of interface conditions necessary to obtain higher order P_N approximations.

To circumvent this difficulty the odd-parity interface conditions have been reformulated in terms of the classical spherical harmonic interface conditions of Rumyantsev.¹⁶ The result is the use of the standard odd-parity spherical harmonic set with the $Y_n, \pm n$ terms deleted for all odd n .¹⁷ While not identical to the present P_3 odd-parity trial functions, the new formulation has very little effect on the accuracy of P_3 solutions. However, it does mean that this variational nodal method can be viewed as a nodal formulation which reduces to the classical P_N equations in the absence of spatial discretization errors. Thus far, the new trial functions have been implemented in x-y geometry. Figure V.1 shows P_1 , P_3 and P_5 solutions, with both old and new P_3 interfaces included, for the Azmy benchmark problem,¹⁸ along with those obtained from a nodal S_N code.^{11,19}

Fig. V.1. Azmy Benchmark: Flux Near Vacuum Boundary ($Y = 9.84$ cm)

e. Adjoint and Perturbation Calculations

The ability to carry out both exact and first order perturbation calculations within the framework of the variational nodal method offers a substantial enhancement to its value. This requires as a prerequisite, however, that a code be available to perform adjoint as well as forward neutron transport calculations. With fine mesh diffusion or discrete ordinate codes this poses little difficulty; it can be shown that the mathematical adjoint formed by taking the adjoint of the forward equation, and physical adjoint, formed by discretizing the adjoint equation, are the same. This property allows the same code to be used for forward and adjoint calculations, simply by inverting the energy group ordering. The same property holds for the variational nodal method, allowing the VARIANT code to be used for forward and adjoint calculations.²⁰ This is in sharp contrast to nodal methods based on quasi-one-dimensional solutions of the diffusion or transport equation. There, the mathematical and physical adjoints are not the same, and the mathematical adjoint is required to evaluate the perturbation expressions, while the existing nodal algorithms solve the physical adjoint problem. As a result, similarity transforms or related techniques must be utilized to relate physical and mathematical adjoints. Thus far, these have been developed only for diffusion theory.

A postprocessing code is under development which takes geometry and cross section files from VARIANT, combines them with the forward and adjoint output flux files, and performs exact or first-order perturbation calculations. Work is completed in two-dimensional geometry; the code is being generalized to treat both anisotropic and three-dimensional problems. A companion paper discussing this subject will be presented at this conference. Work on perturbation with anisotropic scattering is also nearing completion.

f. Flux Reconstruction and Homogenization

Heretofore, the variational nodal method has been formulated under the assumption that each node is homogeneous. This is not a fundamental restriction, however, since the Ritz procedure by which the equations are derived can in principle accommodate changes of

cross sections within the nodes, and trial functions, such as the piecewise polynomials of finite element methods, to more accurately model the heterogeneity. There is much to do in deciding the optimal tack to take in blending whole-core variational nodal calculations with treatments of the local heterogeneities which must be modeled in order to derive accurate pin-to-pin power distributions. Work is underway in two dimensions to examine this problem.

5. Conclusion

Previous work has demonstrated the efficacy and flexibility of the variational nodal method. Implementation within the DIF3D code suite allows the use of the VNM for broad classes of problems, and work continues to enlarge the set of VNM applications. Diffusion theory calculations now provide very accurate results for thermal reactor configurations in hexagonal and Cartesian geometries. The VNM also allows accurate handling of pin-power reconstruction, unlike typical transverse leakage methodologies. In conjunction with REBUS, the VNM can now be applied to burn-up calculations using diffusion and transport theory. Alternate solution methods are currently being investigated. One approach involves direct solution of the flux moments, avoiding the response matrix formulation. Other approaches involve finding an improved spatial basis in an attempt to reduce the number of spatial moments required in hexagonal geometries. Investigation of alternate angular bases is also continuing. Expansion of surface fluxes in standard odd-parity spherical harmonics ensures equivalence of the VNM with the P_N equation in the absence of spatial discretization errors, and reduced angular approximations allow near transport accuracy for the cost of diffusion calculations. Completion of a perturbation module for the VNM will round out the capabilities of the method. Further, discretization of heterogeneous subassemblies, when implemented with curvilinear geometries, will put the VNM in direct competition with Monte Carlo codes, and allow more realistic problem representation in complicated geometries. Future work will be directed at improved acceleration and iteration techniques, and implementation of the VNM within a kinetics code for use in solving time dependent problems.

B. Statistical Methods in MCA

1. Electrorefiner Volume Calibration

a. Introduction

Material control and accountability of special nuclear material in a nuclear facility with process steps involving liquids requires an accurate measurement of the liquid content of the process tanks. In the pyrometallurgical process step of the Fuel Cycle Facility (FCF), the in-process nuclear material is dissolved in an upper layer of liquid salt and a lower layer of liquid cadmium in the electrorefiner. Thus, to estimate the in-process inventory of special nuclear material in the electrorefiner requires the estimation of a calibration equation, which relates the tank's measurement system, such as in the case of the electrorefiner the liquid level probe, to the volume of the liquid.

The electrorefiner tank, as most process tanks, is not a smooth right-circular cylinder for which a single linear volume calibration curve could be fitted over the whole height of the tank. Rather, the tank contains many internal components, which cause systematic deviations from a single linear function. Thus, an important objective of the tank volume calibration is to identify those tank levels over which a single, preferably linear, function will suffice to define the volume versus height relation. These should result in a sufficiently small inventory difference variance, and thereby meet material control and accountancy uncertainty limits for the material balance area.

To this end, three calibration runs, consisting of a series of incremental additions of known masses of water, have been made in the electrorefiner of the FCF.²¹ The basic data—incremental mass of water, liquid level, and liquid temperature—form the basis of the calibration. Prior to each run, the electrorefiner was completely emptied.

The tank conditions at calibration, such as temperature and working fluid, should ideally reflect operating conditions. This is certainly not the case for a calibration of the electrorefiner with water at room temperature as the calibrating liquid, as opposed to Li-K Cl and cadmium at about 500°C.²² However, the segmentation of the calibration curve into regions where the cross-sectional area of the tank is close to constant, or smoothly varying, can be derived from the water data and subsequently applied to the Li-K Cl calibration run.

b. Data Standardization

The data for the three runs of the water calibration are shown in Figs. V.2 through V.4. For runs one and two, 30 liquid level measurements were made of the first incremental addition and again of the last incremental addition. For each of the 30 incremental additions of liquid in between, only three liquid level measurements were taken. In these two runs the volume increments were about 13 liters each, which resulted in a final volume of about 416 liters and a liquid level of 21 in. The 30 measurements of the same liquid level at the beginning and end of these two runs will serve to estimate the variance in the level probe measurements. The third run was designed to give a finer resolution of the height versus volume relation over the first 7.1 in. and the last 3.2 in., by adding the water in increments one fourth of those in runs one and two. In between, the volume increments of water were the same as in the previous two runs.

An exogenous variable, which can affect the calibration of the tank volume and its application to material control and accountancy, is temperature. The temperature of the electrorefiner tank, the liquid level probe, and the liquid varies, not only from calibration run to calibration run, but also over a single calibration run. The calibration function relating a liquid level measurement to the volume of the liquid, together with the associated uncertainties, are estimated at some fixed reference temperature. In principle, the conditions under which the calibration measurements are made should be as close as possible to the conditions under which the process measurements are made. This, in the case of the electrorefiner, which operates at 500°C, is not possible with water. However, the water data can serve to establish the methodology for estimating the form, and defining the segmentation, of the calibration function.

Fig. V.2. Standardized Values—Run 1

Fig. V.3. Standardized Values—Run 2

Fig. V.4. Standardized Values—Run 3

especially in view of the fact that there are three runs of water calibration data, while multiple calibration runs will not be possible with the salt and cadmium.

The data in the three water calibration runs were standardized to a temperature $T_o = 20^\circ\text{C}$. That is, the length of the liquid level probe was adjusted for temperature by

$$P(T_o) = P(T)/(1 + \alpha \Delta T),$$

where $\Delta T = T - T_o$, $P(T)$ is the measured probe length, and $\alpha = 0.14 \times 10^{-4}$ is the linear expansion coefficient for stainless steel.²³ Similarly the tank volume was adjusted by

$$V(T_o) = V(T)/(1 + 3\alpha \Delta T). \quad (2)$$

The volume of the liquid was calculated by

$$V(T) = m/\rho(T), \quad (3)$$

where m is the mass of water (sum of mass of incremental additions) and $\rho(T)^*$ the density²⁴ at the measurement temperature.

During the calibration runs only the temperature of the liquid was measured. For data standardization this temperature was assumed to be also applicable to the liquid level probe length and the tank volume expansion. The maximum deviation of the temperature of the measurements from the reference temperature is 4.6°C. This results in an adjustment which is less than the readability of the liquid level gauge. It must be kept in mind, however, that the electrorefiner tank volume calibration with the eutectic mixture LiCl-KCl will take place at 410°C and the operation of the electrorefiner will be at 500°C. Under these circumstances the adjustments will be significant.

c. Data Screening

The volume $V(h_o)$ of the tank below some level h_o is given, in general, by the expression

$$V(h_o) = \int_0^{h_o} A(h)dh, \quad (4)$$

where $A(h)$ is the cross sectional area of the tank at level h . If $A(h)$ is constant, the volume of the tank is linear in h . In Figs. V.2 through V.4 the standardized calibration data are plotted for runs one through three, respectively.²¹ The data appear to be linear given the coarse resolution of the vertical axis.

The engineering design drawing of the electrorefiner tank (see Fig. V.5) indicates, however, significant structures inside the tank, which will result in a nonuniform cross

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*A cubic fit to density vs temperature data from 12.0 to 25.0°C (at 0.5°C increments).

Fig. V.5. Electrorefiner Intervals

sectional area of the tank with elevation. From Fig. V.5, discontinuities in the tank cross section would be expected at least at the 2, 6-3/4, 8-1/2, 15-3/4, 17-1/2, and 20-1/4 in. levels. All the standardized data for the three runs are plotted in Fig. V.6. In addition, the above expected points of discontinuity are marked with vertical lines, and a regression line through the data together with a 95% confidence interval are also shown. It is clear that the volume versus liquid level relationship is different at different levels, indicating differences in the cross-sectional area of the tank. It is difficult, however, to associate specific levels with the points of discontinuity.

A measure of the predictive power of the data, and the eventual correlations derived from it, is its ability to identify the points of discontinuity in the cross sectional area of the tank. To this end two types of diagnostic plots are very helpful.²⁵ The first, shown in Fig. V.7a, and referred to as a profile plot, is a plot of the residuals from a linear fit to all the calibration data vs the liquid level probe measurement. This way of presenting the data has the effect of showing the variation in the volume with respect to tank height on a greatly expanded vertical scale, thereby giving a better measure of the extent to which the volume is a nonlinear

Fig. V.6. Standardized Values—All Runs
(Linear fit nonzero intercept)

function of height. On this plot markers for changes in the tank's cross-sectional area are abrupt changes in the tank's profile plot.

The tank heights in Fig. V.7a, which were previously identified with changes in tank cross-sectional area from the engineering drawings, are clearly supported by the profile plots. However, the profile plots strongly indicate an additional level at ~5 in. at which the tank's cross-sectional area changes. This region lies in Fig. V.4 approximately between the 4.5 in. and 5.0 in. levels.

It is noted that the data for the three runs do not coincide. That is, the between-run differences are significantly greater than the within-run measurement errors. A method has been prescribed for the statistical alignment of data from several runs.²⁵ The procedure is based on choosing an initial segment of the data, which appears to be linearly related, and fitting a single linear function to the data from all runs. The volume or height adjustment for each run is then the ratio of the mean of the residuals for the run to the slope of the regression or merely the mean of the residuals, respectively. The result of this procedure is shown in Fig. V.7b.

Fig. V.7a. Regression Analysis on All Runs

Fig. V.7b. Calibration Alignment for All Runs
 (Alignment method: Liquid level residuals for points Nos. 3 through 5)

The second diagnostic plot²⁵ is based on the fact that, if the expression in Eq. (4) is differentiated with respect to height, the derivative is equal to the cross-sectional area. The incremental slope for the data in the three calibration runs is shown in Fig. V.8 as a function of liquid level. As in the previous two plots, the levels at which changes in the tank cross-sectional area should be expected, based on the engineering drawing, are also indicated. Again, these can be identified with changes in the cross-sectional area. In addition, the incremental slope plot confirms the previous observation that there is a significant change in the tank cross-sectional area a little below the 5-in. level.

d. Identification of Calibration Segments

For the statistical estimation of the calibration equation it is highly desirable that the regions of interest for material control and accountancy calculations of the fissile inventory of the tank have a constant cross section; in which case the calibration equation is linear. From the analyses in the previous section the tank height can be tentatively segmented into seven sections as shown in Table V.5. Four segments are deemed linear for modeling purposes based on the available data, while the other three are not.

Fig. V.8. All Run Incremental Slope (dV/dh vs Height)

TABLE V.5. Preliminary Segmentation

Segment Number	Segment (in.)	No. Data Points (Run 3)	Model
I	0.0 - 2.00	10	Nonlinear
II	2.00 - 4.62	14	Linear
III	4.62 - 6.75	14	Linear
IV	6.75 - 8.5	4	Nonlinear
V	8.5 - 15.75	11	Linear
VI	15.75 - 17.50	3	Nonlinear
VII	17.50 - 21.25	21	Linear

Two nonlinear segments, IV and VI, are readily associated in Fig. V.5 with the regions of the bolts for the side shapers and the fixtures around them. The nonlinear behavior of the volume vs height relation in these segments, and the small number of data points, are likely to lead to unreliable estimates of the functional relationship and the associated uncertainties. It is, therefore, prudent not to operate the electrorefiner with fluid levels in these regions.

The region consisting of segments II and III will contain the cadmium-salt interface during the operational life of the electrorefiner. This region, both from the engineering drawing in Fig. V.5, and an analysis of the data, shows a very complicated functional behavior. The residuals of the fit of a cumulative linear model to the data over a region consisting of segments II and III suggest that a quadratic model might explain a sufficiently large fraction of the behavior of the data over these two segments. However, the residuals of a quadratic model indicate that, although the residuals are smaller in magnitude, they are still strongly autocorrelated. Therefore, a quadratic model does not significantly improve the estimate of the functional behavior between the volume and the height of the tank over that region. Since we are, in essence, fitting analytic functions to data, the danger of overfitting lurks within each regression. This pitfall is particularly dangerous in the critical region of the cadmium-salt interface, where the tank internals exhibit significant geometric complexity. Table V.6 illustrates the effect of the

TABLE V.6. Calibrated Volume and Systematic Standard Deviation

Number of Segments in Region $2.0 \leq H \leq 6.75$ in.	Calibrated Volume $\pm$ Systematic Standard Deviation	
	H = 4.25 in.	H = 4.95 in.
-*	83.22 ± 0.63	96.99 ± 0.72
1	76.34 ± 0.78	89.57 ± 0.87
2	74.38 ± 0.26	87.54 ± 0.41
4	74.23 ± 0.20	87.45 ± 0.58

*Entire calibrated range fit as a single segment.

progressive segmentation on the estimates in region $2.00 \leq H \leq 6.75$ in. For comparison, the estimate of the volume and its systematic error variance has been chosen at two levels—at $H = 4.25$ in. (the midpoint of segment II) and at $H = 4.95$ in. (the midpoint of segment III). Clearly the estimates of the volume behave monotonically with degree of segmentation; the variances in the estimates, however, do not. Given the standard deviations in the estimates of the volumes, nothing is gained by increasing the segmentation over this region from two segments to four segments.

e. Estimation of the Calibration Function and Calibration Uncertainty

Because the tank is filled in incremental additions, any errors associated with previous additions of liquid affect the error in the total amount. The error model chosen for the electrorefiner calibration is, therefore, the cumulative model.²⁶ Although during calibration the volume of liquid added is the controlled variable and the liquid height the measured quantity, we follow common practice²⁷ and use the following model

$$V_i = \alpha + \beta h_i + \epsilon_i, \quad (5)$$

where V_i is the volume after the i -th incremental volume addition, h_i the liquid level measurement after the i -th incremental addition, and ϵ_i the cumulative error after the i -th incremental volume

addition. This form of the calibration function is also the measurement function, and consequently inversion is not necessary.

The estimated calibration functions, based on run three data, together with the random and systematic error variances for the linear segments, are shown in Table V.7. These are deemed the most appropriate calibration/measurement functions for the electrorefiner based on the water data at hand.

The above analysis has tried to eliminate the error due to irregularities in the cross-sectional area of the tank by taking into account the geometry of the tank intervals and the statistical behavior of the data. Both practical and statistical limitations preclude that every error contribution can be accounted for and classified. Consequently a significant amount of judgement is exercised in arriving at the appropriate segmentation of the calibration curve.

TABLE V.7. Estimated Calibration/Measurement Function Using Cumulative Model for Out-of-cell Volume Calibration Run No. 3 (Reference temperature 20°C)

Liquid Height, H (in.)	Number Data Points	Calibrated Volume, V (l)	Random Variance, σ_R^2 (l ²)	Systematic Variance, σ_s^2 (l ²)
$2.00 \leq H < 4.62$	14	-1.96280 $+17.9464 \times H$	8.31013×10^{-4}	$6.41259 \times 10^{-2} - 2.87980 \times 10^{-2} \times H + 6.85471 \times 10^{-3} \times H^2$
$4.62 \leq H \leq 6.75$	14	-11.3153 $+19.9846 \times H$	1.03049×10^{-3}	$5.15948 \times 10^{-1} - 1.53021 \times 10^{-1} \times H + 1.65253 \times 10^{-2} \times H^2$
$8.50 \leq H \leq 15.75$	11	-13.9485 $+20.3193 \times H$	1.06530×10^{-3}	$1.66336 \times 10^{-1} - 2.15882 \times 10^{-2} \times H + 1.19884 \times 10^{-3} \times H^2$
$17.50 \leq H \leq 21.25$	21	-15.6185 $+20.3905 \times H$	1.06903×10^{-3}	$1.60881 - 1.51891 \times 10^{-1} \times H + 4.22053 \times 10^{-3} \times H^2$

2. Calibration and Measurement Control of Electronic In-cell Mass Balances in the Fuel Cycle Facility

a. Introduction

In the Fuel Cycle Facility (FCF) the measurement of mass is the most common and basic physical measurement, and is central to the material accounting system. The instrument for determining the mass of an item is an electronic balance. Of interest are its calibration, that is an estimate of the true mass on the balance and the associated uncertainty in the estimate, and the control of that calibration, that is tests of whether the current state of the balance conforms to the state at calibration.

Although an electronic balance for measuring mass is a common instrument in laboratories and industrial facilities, its application in the FCF requires some special considerations. The balances will operate in a high radiation environment with changing temperatures and pressures. This requires that some electronic components, which could be adversely affected by such an environment, be separated from the balance and placed outside of the cell. This is nonstandard practice for commercial balances, and its consequences on the performance of the balances will be addressed. The balances will be remotely calibrated and operated, whether this introduces any significant operator bias will be investigated.

In addition, the facility is a new design and, therefore, at best, very little operational or historical data exists for anticipating performance problems which may develop during actual operation. For this reason, the procedures for calibration and control are more elaborate than for other installations at ANL-West, and will assure a clear characterization of anomalous behavior. If it transpires that a balance has stable and predictable anomalous characteristics, the detailed analysis of these characteristics may allow correction for this without immediate removal of the balance and interruption of operation.

b. General Model of a Linear Balance

Because incell installation of the balances requires tampering with the electronics of the balances and the effects of irradiation and temperature are unknown, there is no assurance that the manufacturer's calibration procedures and parameters remain valid. To overcome this we return to the model for a simple linear balance shown in Fig. V.9.

To make a measurement of a mass, a relationship between the mass placed on the balance (M) and the reading given by the balance (W), is needed as shown in Fig. V.9. Since it is not known *a priori* that the reading (W) given by the balance corresponds exactly to the mass (M) on the balance, for modern balances a linear relationship is assumed.

The linearity of FCF balances, over the range of operational measurements for material control and accountability, is a necessary condition. DOE in order 5633.3A requires that each balance must be "checked ... for linearity on each day that the scale or balance is used for accountability purposes".²⁸ Thus, the certification that the balance conforms to a linear relationship over the range of application is a critical element of calibration.

Fig. V.9. General Model of a Linear Balance

In the context of Fig. V.9, if the parameters β_0 and β_1 were known exactly, which characterize a particular balance, it would be possible to determine the true mass of the object on the balance by

$$M = \frac{W - \beta_0}{\beta_1}. \quad (1)$$

This relationship²⁹ is central to the issues of

1. Calibration,
2. Linearity checks, and
3. Estimation of the measurement error.

Because the transformation $W \rightarrow M$, as given by Eq. (1), is dependent on the parameters β_0 and β_1 , the above issues will depend on our knowledge of these parameters. Thus, preparation of the balance for the first measurement of an unknown mass revolves around determining β_0 and β_1 , and their uncertainties.

c. Calibration

In principle, calibration of a measurement instrument results in estimates of the bias, the variance in the bias estimate (the systematic-error variance), and the random-error variance.²⁷ These estimates are based on repeated measurements of standard masses on the balance. The choice of standard masses is often such that they are equally spaced over the range of application of the balance. The exact number is determined during preliminary evaluations based on a confidence coefficient,³⁰ and on a linear fit test to assure the validity of the linear model. The sequence of measurements of the mass of the standards is randomized to eliminate biases with respect to the selection process.

The parameters β_0 and β_1 are estimated by regressing the balance reading on the mass of the standard masses. This procedure gives estimates $\hat{\beta}_0$, and $\hat{\beta}_1$, so that

$$w = \hat{\beta}_0 + \hat{\beta}_1 S, \quad (2)$$

and we also obtain estimates $\text{Var}(\hat{\beta}_0)$, $\text{Var}(\hat{\beta}_1)$, $\text{Cov}(\hat{\beta}_0, \hat{\beta}_1)$.

These are used to make inferences about the true mass via (1).

d. Analysis of Calibration Measurements of In-cell Balance 400A

Mass measurements of three standard masses (10 kg, 20 kg, and 30 kg) were made in-cell on balance 400A. The masses were selected in random order and the operators were requested to simulate, as much as possible, the conditions which would exist during operation. That is, all weighings of standards are assumed to duplicate routine use of the balance with respect to load, temperature, humidity, vibration, loading procedure (such as positioning) and so forth. This is especially critical during the testing period, where primary consideration needs to be given to making certain that the effects of all process variables are taken into account, so that the error limit is representative of the mass measurement process.

It is desirable, due to the remote operation of the in-cell balances, to keep the number of measurements to a minimum. A total of 40 measurements was tentatively deemed adequate and resulted in a 98% confidence coefficient.³⁰ The results of the 40 measurements of randomly selected standard masses were analyzed with SAS,³¹ which is the analysis software for all mass measurements in FCF. The regression procedure of the balance readings W on the standard masses S resulted in an estimate of the intercept β_0 of 2.571 g, and slope β_1 of 0.9998. The p-value under the null hypothesis $\beta_0 = 0$ is 0.0065, and therefore the estimate is statistically significant. The p-value under the null hypothesis $\beta_1 = 1$ is 0.0004, and thus also statistically significant. Correction of the balance reading is, therefore, warranted.

Since bias correction of measurements has not yet been adopted by FCF operations, the only statistics pertinent for the measurement uncertainty analysis are the random error variance and the systematic error variance. To this end, the following procedure is proposed for calculating these statistics.³² This approach is dependent on the assumption that measurement errors are independent of the standard both with respect to bias and the error variance. This is not the case with respect to some of the balances. This in itself may not be sufficient to preclude this approach to estimation. However, caution in its application is warranted.

To calculate the error variances we form the following quantities. Let S_k be the mass of the k -th standard and W_{ik} the i -th measurement of the k -th standard. The k -th standard mass is estimated by the average $\bar{W}_k$ where

$$\bar{W}_k = \frac{1}{m_k} \sum_{i=1}^{m_k} W_{ik}, \quad (3)$$

and the k -th variance s_k^2 where

$$s_k^2 = \frac{1}{m_k - 1} \sum_{i=1}^{m_k} (W_{ik} - \bar{W}_k)^2, \quad (4)$$

where m_k is the number of measurements of the k -th standard. The bias in the estimate of the mass of the k -th standard is computed as

$$\theta_k = (\bar{W}_k - S_k), \quad (5)$$

One can then compute a weighted bias θ and a weighted estimate of the variance s^2 for K standards as

$$\theta = \frac{1}{n} \sum_{k=1}^K m_k \theta_k \quad (6)$$

and

$$s^2 = \frac{1}{n-K} \sum_{k=1}^K (m_k - 1) s_k^2, \quad (7)$$

where $n = \sum_{k=1}^K m_k$ is the total number of observations. Then the estimate of the random error

variance is s^2 . With regard to the systematic error variance, we use θ^2 since no bias correction is applied. However, if

$$\theta^2 < \frac{1}{n^2} \sum_{k=1}^K m_k^2 \sigma_k^2 + s^2/n \quad (8)$$

the right hand side of the above inequality is used as the systematic error variance. The σ_k is the assigned uncertainty to the k -th standard.

e. Routine Calibration and Linearity Checks

The procedure described above confirms that the assumed model is appropriate in describing the population of responses of the balance to measurements of unknown masses, and gives us the estimate of the variance which we associate with the estimated mass. The balance is out of calibration, if the new sample of responses is statistically different from that on which the calibration was based.

Thus, a calibration check can be based on periodic random measurements of the same standard weights used in the calibration. If the observed value falls within the predictive interval at some level of confidence, it can be concluded, with that level of confidence, that the population of responses has not changed. This is shown graphically in Fig. V.10.

The calibration data is used to calculate the predictive interval for the calibration. This interval is given by

$$w_j \pm t_{\alpha/2, k-2} s_j, \quad (9)$$

Fig. V.10. Predictive Interval

where w_j is the predicted value for the j -th standard weight based on the calibration curve, t is the student t -statistic, and s_j^2 is the estimate for the variance in the future observation.

The predictive interval lends itself to statistical control use.³³ The basic idea of a predictive interval is to choose a range in the distribution of W , the balance readings, wherein most of the observations will fall, and to declare that the next observation will fall in this range. If it falls outside this range there is an indication that a change in the performance of the balance has occurred, and it may be necessary to look for an assignable cause.

f. Measurement Control of In-cell Balance 400A

The predictive interval is used to signal the possibility that a shift has occurred in one or more of the population parameters, which describe the relationship of the instrument reading to the true mass. Thus, an instrument reading of a standard mass outside the predictive interval warrants a search for a possible cause for this shift and its probability.

In general, the out-of-control behavior of the balance manifests itself as an increase in the dispersion of the instrument readings about the mean, or a shift in the bias, or both. No one test can give a definitive answer as to which of these possibilities are the cause of the out-of-control behavior. The mass balances are an integral part of FCF operation, thus cessation of operation to recalibrate the balance, or remove and replace it, can incur considerable cost.

Control charts give a holistic view of the performance of the balance. If sufficient calibration checks have been accumulated before there is an indication of out-of-control balance performance, a direct test of calibration can be made.

Since the calibration check measurements form an independent sample, they will have their own regression line. One can then test whether the two regression lines, the original calibration and the one based on the current sample, are the same. That is, one tests the hypothesis

$$H_0 : \beta_{01} \text{ and } \beta_{02} \text{ and } \beta_{11} \text{ and } \beta_{12}$$

$$H_A : \text{Either } \beta_{01} \neq \beta_{02} \text{ or } \beta_{11} \neq \beta_{12}$$

$$\text{or both.}$$

If the two regression lines are the same, both the intercept and slope terms must be equal. If the regression lines are not the same, they must differ with respect to either the intercept or the slope or with respect to both.

To minimize cost of unnecessary removals of balances, a number of different tests, such as control charts, for example, are used to gain confidence that a true problem exists and give an indication as to the source. The existence of readily available modern computer software for statistical analysis,³⁴ allows quick and extensive testing of hypothesis with regard to the population parameters.

A measurement control procedure described previously was implemented during the initial startup and testing of the FCF. Over that period 45 calibration check measurements were made on in-cell balance 400A. The measurements span a period of six

months. Over this period no measurement exceeded the 3σ control limits, and only on two occasions, at measurements 28 and 30, was the 2σ control limit exceeded. A Stewhart control chart for the bias and a moving range chart for these data, generated with the SAS/QC procedures,³⁴ show that the mean bias and the variance appear to differ from those established at calibration for measurements with this instrument.

The V-mask parameters for the CUSUM chart were set to detect a change in the mean bias of 0.5σ with a Type I error probability of 0.05 and a Type II error probability of 0.2. For no change in the mean bias, at these parameters, the average run length (ARL) is 209, while with a deviation of 0.5σ the ARL is 24.

SAS/QC performs a number of runs tests, whose purpose is to detect a particular nonrandom pattern in the points plotted on the control chart. In the series of calibration check measurements, the first indication of a possible problem would have occurred at the 9th measurement. The information available at that point would not have indicated a problem. The Shapiro-Wilk statistic would have had a p-value of 0.81 indicating normality. Visually the data indicate a downward shift in bias and a variance smaller than expected. The cusum chart, however, does not indicate an out-of-control condition, for the V-mask is not crossed by the sequence of cumulative sums. This is consistent with the parameters of the V-mask, which correspond to an average run length of 24 measurements for an out-of-control condition, while there are only nine at this point. In addition to this, the test comparing the regression parameters has a p-value of 0.257 which suggests the parameters are based on samples drawn from the same population.

The next signal would occur at measurement 22. Visually the data continue to show a downward shift in the bias. At this point this is confirmed by the CUSUM chart. In addition, the p-value for the comparison of regression coefficients is 0.000077, clearly indicating that the populations are different.

The box plot in Fig. V.11 indicates a systematic trend in the bias and, therefore, the possibility of correcting these measurements via Eq. (1). The efficacy of this correction is given in the box plot in Fig. V.12.

Univariate Procedure
Schematic Plots

Variable=DEL

Fig. V.11. Box Plots of Uncorrected Bias

Univariate Procedure
Schematic Plots

Fig. V.12. Box Plots of Residuals

C. Burner Core Designs

1. Introduction

The primary focus of IFR burner core design studies in FY 94 was the development of core designs and deployment strategies for the disposition of weapons plutonium. Large quantities of weapons plutonium currently exist in US and CIS weapons stockpiles; arms reduction treaties call for the disassembly of many existing weapons. An important issue is the means to prevent material diversion and re-use to produce nuclear weapons. For U.S. arms reduction policy, the short-term plan is to rely on physical safeguards. Meanwhile, studies have been undertaken by DOE and others to determine efficient means to quickly transform the weapons-grade material into a form which is not readily usable for weapons. In most proposed techniques, the weapons material is diluted and radioactivity is introduced; thus, chemical separation and remote handling would be required to recover the weapons-grade material. This "disposition" of weapons material, however, does not remove the long-term proliferation risk because the bulk of the original material still exists and such material, even with extensive denaturing, requires active safeguarding. The only solution capable of avoiding active safeguarding in perpetuity is to transform the weapons material into something else. Thus, the only option for permanent destruction of plutonium is to "burn" (fission) it.

An intense, sustained neutron flux is required to fission significant amounts of plutonium; several possible neutron sources (e.g., a nuclear reactor, accelerators with spallation targets, etc.) have been proposed. The primary advantage of nuclear reactor systems is the demonstrated technology of fission reactors coupled to an efficient secondary system for converting the fission energy into electrical power. Any fission reactor "burns" plutonium at the same rate (1 MWt-day per gram of plutonium fissioned). However, different proposed reactor options have differing capabilities to extract all the energy from the plutonium and thereby minimize the amount sent to the repository—requiring perpetual safeguards. Of the potential reactor systems for plutonium destruction, the Integral Fast Reactor (IFR) closed fuel cycle offers several unique advantages:

1. The plutonium is repeatedly recycled to the reactor allowing eventual destruction of the entire inventory; burnups of 10-20 atm% of the reload batch can be achieved each fabrication/irradiation cycle.
2. All transuranics follow the plutonium in the recycle chemistry and 99.9% are returned to the core; this leads to a waste stream essentially free of all transuranics.
3. Because of the particularly hard neutron spectrum associated with the metallic fuel, plutonium is preferentially destroyed (fissioned), not converted to still higher actinides.
4. There is never a pure plutonium stream at any point in the fuel cycle. The plutonium is always contained in a denatured (radioactive) form; remote fuel processing techniques are utilized.

A physics study to evaluate the impact of introduction of weapons plutonium for disposition in the IFR closed fuel cycle was conducted; disposition strategies covering the entire range from pure burner (maximum destruction rate) to fissile self-sufficient (no net plutonium destruction) were investigated. Results of this study are described in Sect. C.2. Other, more detailed studies into specific aspects of burner core development (e.g., how to develop optimal configurations for a conventional burner) were completed; and the key results and conclusions of these studies are summarized in Sect. C.3.

2. Weapons Disposition Physics Study.

In the IFR closed fuel cycle, multiple recycle is employed to achieve total fission of the transuranics. The available range of destruction/production characteristics in metal fueled IFR cores provides for flexibility in plutonium inventory management strategy; the timing for plutonium destruction is a policy decision, dictated by the relative benefits of rapid inventory

reduction vis-a-vis sustained electrical power production. Conventional IFR cores maintain or can even increase the plutonium inventory (conversion ratio of 1.0-1.3); this allows sustained power production from a fixed plutonium inventory. Alternately, by removing fertile material and/or altering the neutron balance, the conversion ratio can readily be reduced. In this study, three different IFR disposition scenarios—a power production mode, a moderate destruction mode, and a maximum destruction mode—for weapons plutonium disposition were addressed.

For the power production disposition scenario, the weapons material is used for the initial fissile working inventory and upon irradiation is rapidly converted to a denatured form (reactor spent fuel); however, the inventory is conserved by use of a unity conversion ratio core design to allow sustained power production. For this option, the small, modular 840 MWt (Mod B/93) Advanced Liquid Metal Reactor (ALMR) core design, developed through a joint ANL/GE design effort, was utilized. The radially heterogeneous core layout, shown in Fig. V.13, consists of 108 driver, 42 internal blanket, 42 radial blanket, 9 control, 6 gas expansion module (GEM), and 3 alternate shutdown assemblies. The assembly lattice pitch is 16.14 cm; and the resulting diameter of the proposed configuration is 3.58 m (11.8 ft). The active height of the driver assemblies is 107 cm (42 in.). An operating cycle length of 23 months (at an assumed capacity factor of 85%) is applied, with a three batch refueling strategy.

In the moderate destruction disposition scenario, the weapons material is used for the initial fissile working inventory and upon irradiation is rapidly converted to a denatured form. By using a core design with a conversion ratio less than one, the inventory is gradually consumed through repeated recycle; and an external feed of makeup fissile material is required. Conversion ratios less than 1.0 are achieved by removing internal and radial blanket assemblies, and by altering the neutron balance to reduce U-238 capture (e.g., by increasing the leakage fraction). These partial burner core designs will be referred to as "conventional burner" designs because they utilize conventional IFR metallic fuel alloys in a homogeneous core layout. The minimal conversion ratio of conventional burners is roughly 0.5, further reductions in the conversion ratio would require transuranic enrichment levels greater than those currently being investigated in the IFR metal fuels testing program (up to 30 wt% transuranics per heavy metal). For this study, the

Fig. V.13. Heterogenous (Power Production) Core Configuration

moderate burner core was developed by modifying the conventional ALMR core. The resulting homogeneous core layout is shown in Fig. V.14; this core consists of 354 driver assemblies (two enrichment zones), 28 control assemblies, 12 GEMs, and three alternate shutdown assemblies. The larger core diameter (4.44 m, 14.6 ft) allows the core height to be reduced to 46 cm (18 in.); this enhances the axial leakage of neutrons thereby reducing the conversion ratio. An operating cycle length of 12 months (at an assumed capacity factor of 85%) is applied, with a seven batch refueling strategy.

In the maximum destruction disposition scenario, the weapons material is used for the initial fissile working inventory and upon irradiation is rapidly converted to a denatured form (spent fuel) and then destroyed as quickly as possible through multiple recycle. To achieve

Fig. V.14. Pancaked Homogeneous (Burner) Core Configuration

plutonium destruction rates more rapid than the conventional burner design, nonconventional metal fuel alloys are required; and to achieve the maximum theoretical net transuranic consumption rate of 1.0 g/MWtd, a non-uranium fuel form is required. Preliminary neutronic investigations of non-uranium core designs (called pure burners because they achieve the maximum destruction rate by totally avoiding in-situ plutonium production) indicated that the use of pure Pu/Zr fuel incurs excessive burnup reactivity losses; some diluent to replace the uranium is needed. From a wide variety of proposed materials, hafnium (Hf) was chosen as the diluent material because of its large resonance capture rate; the expected benefits of Hf introduction include:

1. The Hf acts as a fixed absorber requiring a higher plutonium inventory; since the plutonium destruction rate is fixed (1 g/MWtd), a higher inventory implies a slower fractional loss rate of the fissile inventory resulting in a reduced burnup reactivity loss rate.

2. The Hf resonance capture will provide negative Doppler reactivity feedback which otherwise decreases to near-zero when the U-238 is removed.
3. The Hf diluent decreases the fuel mass within a cladding of fixed diameter and should allow higher atom% burnups of the transuranic material at the same end of life fission gas pressure in the plenum.

The driver assembly design and power rating of the pure burner configuration is identical to the other two core designs. However, each pin now contains a mixture of binary Pu-28Zr and Hf-Zr alloy. As discussed above, it is desirable to maximize the plutonium inventory in pure burner designs; therefore, the core volume was increased by using the large homogeneous layout (Fig. V.14) and a core height of 107 cm (42 in.). Enrichment zoning is achieved by varying the Pu/Hf ratio between the inner and outer core zones. An operating cycle length of 12 months (at an assumed capacity factor of 75%) is applied, with a fourteen batch refueling strategy.

For each of the three core designs, the core performance was evaluated for two different fuel cycle models. In the first model, a once-through fuel cycle supplied by two external feed streams, weapons plutonium and depleted uranium, is applied. This calculation evaluates the core behavior when weapons plutonium is utilized to start-up the fuel cycle. In the second model, recycle of the discharged fuel is explicitly modeled and only make-up streams of weapons plutonium and depleted uranium are supplied. This calculation evaluates the core behavior after repeated recycle. For this study, a one-year cooling period and a one-year processing time (two-year turnaround) was assumed. All of the heavy metal isotopes are recycled back to the core in the recycle model. It is worth noting that a fraction of the heavy metal inventory (estimated at 0.1-0.5% for the IFR process) would be lost each recycle; these losses are important for waste management considerations, but are considered inconsequential for the evaluation of reactor physics performance. In the IFR fuel cycle processing, the rare-earth fission products are not entirely removed. This was modeled by utilizing a separate fission product lump for the rare-earths, and recycling 5% of the rare-earth discharge.

The calculated mass flows, detailed isotopic distributions, neutronic performance characteristics, and reactivity feedback coefficients for the three disposition scenarios (power production mode, moderate destruction mode, and maximum destruction mode) using both the once-through and recycle fuel cycle models are investigated in detail in Ref. 35. Some of the important results are summarized in Table V.8 and the key conclusions are discussed below.

The conventional, power production design operates at a conversion ratio (ratio of transuranic production to destruction) of 1.15, actually producing 40 kg/y of new transuranics. The moderator burner operates at a conversion ratio of 0.6 and has a net consumption of roughly 100 kg/y of transuranics. In the pure burner, no new transuranics are produced (conversion ratio of 0.0); and the maximum destruction rate of 1 g/MWtd is achieved (230 kg/y for the specified operating conditions). In the power production and moderate burner modes, conventional metal fuel enrichments of 20-30 wt% transuranics/HM (with 10 wt% zirconium) are utilized. In the pure burner design, transuranics compose 100% of the actinide mass. However, the zirconium fraction

TABLE V.8. Performance Comparison for Weapons Disposition Scenarios

Disposition Case	Conventional—Fig. V.13		Moderate Burner—Fig. V.14		Pure Burner—Fig. V.14	
	Startup	Recycle	Startup	Recycle	Startup	Recycle
Cycle Length, months	23		12		12	
Conversion Ratio ^a	1.187	1.147	0.591	0.539	0	0
Net TRU Consumption Rate, kg/y	-41	-33	95	110	<u>231</u>	<u>231</u>
Enrichment, wt%TRU/Heavy Metal	20	23	19/23	23/29	100	100
Equilibrium Loading, kg/y						
Weapons TRU	356	-	441	110	518	231
Recycled TRU	-	423	-	435	-	417
Total Heavy Metal	3760	3760	2110	2110	518	652
Burnup Reactivity Loss, \$	<u>2.12</u>	<u>0.09</u>	10.9	9.30	20.0	15.8
Peak Linear Power, W/cm	320	320	270	280	155	155
Ave./Peak Discharge Burnup, MWd/kg	106/150	107/151	118/156	118/160	420/547	334/450
Peak Fast Fluence, 10 ²³ n/cm ²	3.8	3.8	3.8	3.6	3.8	3.7
Delayed Neutron Fraction, β_{eff}	3.44E-3	3.40E-3	3.16E-3	3.12E-3	<u>1.97E-3</u>	<u>2.03E-3</u>
Sodium Void Worth, \$ ^b	6.32	6.90	1.72	2.94	<u>18.3</u>	<u>19.2</u>
Sodium Density Coefficient, cents/°K	0.16	0.17	0.05	0.08	0.46	0.48
Flooded Fuel Doppler, cents/°K	-0.06	-0.05	-0.11	-0.09	<u>-0.02</u>	<u>-0.01</u>
Radial Expansion Coefficient, cents/°K	-0.20	-0.20	-0.36	-0.35	-0.38	-0.34
Axial Expansion Coefficient, cents/°K	-0.13	-0.13	-0.12	-0.13	-0.22	-0.22
CRD Driveline Expansion, \$/cm	-0.08	-0.08	<u>-0.66</u>	<u>-0.63</u>	-0.25	-0.20

^aRatio of time-integrated transuranic production rate to transuranic destruction rate.

^bFor voiding of all active zones (driver and blanket) and their upper plena.

is increased to 28 wt% (to increase the melting point), and the TRU/(TRU+Hf) ratio ranges from 35-55 wt%. The startup conventional core has the lowest loading rate of weapons material (350 kg/y) while the pure burner has the highest loading rate (520 kg/y). However, the inclusion of numerous blanket assemblies results in a higher heavy metal loading rate (3.8 MT/y) in the conventional design.

The recycle case of the power production mode is the fuel cycle scenario utilized in the reference ALMR power production design and previous IFR fissile self-sufficient core design studies; this design has a low burnup reactivity loss (near-zero with recycle, and \$2 for the startup fuel cycle). Whereas, the burner cores (where U-238 capture is purposefully reduced or eliminated) have much larger burnup reactivity losses of \$10-20. The slow reactivity loss rate in the conventional design allows a long cycle length (23 months); conversely, in the pure burner design, the capacity factor was reduced to 75% to prevent excessive reactivity losses for a one-year operating cycle. In the pure burner, the addition of diluent allows large increases in the discharge burnup (up to 500 MWd per kg of heavy metal) with pin fission product loadings similar to the conventional pins. The combination of derated power density and high burnup target in the pure burner design requires extended core residence times, resulting in the 14 batch refueling strategy.

Isotopic changes with recycle impact several of the performance characteristics. The reduced Pu-239 content of the recycled transuranic material increases the enrichment requirement (by 3-4 wt%) in the conventional self-sufficient and burner designs, and increases the yearly loading requirements for all cases (e.g., from 520 kg/y to 650 kg/y in the pure burner mode). The resulting removal of U-238 in the conventional designs decreases the conversion ratio (e.g., from 0.59 to 0.54 in the moderate burner case). However, the displaced U-238 has been largely replaced by Pu-240 which acts as a fertile material by capture to Pu-241; the Pu-240 is a superior fertile material because its capture cross section is significantly higher than U-238 and its fast fission effect is greater. Therefore, in all of the recycle cases, the burnup reactivity loss rate is significantly reduced (by roughly \$2) compared to the startup cores.

The reactivity feedback coefficients for the startup and recycle calculations of the three disposition cases are also summarized in Table V.8. The primary safety coefficient differences between the conventional and moderate burner designs are caused by the increased leakage in the burner configuration; the pancaked geometry of the burner design increases the axial leakage probability. This enhances the negative leakage component of the sodium void worth significantly reducing the void worth from \$6.3 in the conventional core to \$1.7 in the burner core. The increased axial leakage in the burner design also leads to a significant increase in the radial expansion coefficient from -0.20 cents/°K to -0.35 cents/°K. The increase in excess reactivity and reduced core active height lead to significant increases in the control rod driveline (CRD) expansion coefficients; at 10% insertion, the CRD expansion coefficient is -0.08\$/cm in the conventional design but -0.66 \$/cm in the burner design. Although the CRD expansion coefficient improves the short-term transient response in accidents with rising outlet temperature, such large values may have an adverse impact on stability and other normal operating considerations, and on long-term accident response.

The pure burner design exhibits reactivity feedback coefficients which are substantially different from the conventional designs. Because the uranium is completely eliminated, the delayed neutron fraction decreases to 0.002, characteristic of Pu-239 fission; this implies that the nuclear chain reaction will be more sensitive to reactivity changes. The preferential absorption of low energy neutrons in the Hf and the reduction in core leakage (derated geometry) cause a high sodium void worth of \$18-\$19. Conversely, the Doppler coefficient is enhanced by the Hf. The fuel Doppler for a pure burner without fixed absorber (a core using Pu-Zr fuel alone) is zero to slightly positive, but the Hf contributes -0.02 cents/°K (roughly 1/3 the driver Doppler of a conventional metal-fueled core). Because the reactivity feedback coefficients of this pure burner design are quite different from conventional IFR cores, the transient responses are expected to differ from conventional designs.

Isotopic changes with recycle impact several of the reactivity coefficients. The increased Pu-240 concentration in the recycled composition reduces the burnup reactivity swing (and the corresponding excess reactivity) by \$2 in the conventional cores, and \$4 in the pure

burner. However, the fission threshold in Pu-240 is at a lower energy than in U-238; in addition, the threshold cross section is roughly twice as large. This enhanced threshold fission has an adverse impact on the sodium void worth resulting in roughly a \$1 increase for all three cases. The geometric expansion coefficients appear to be insensitive to the isotopic changes with recycle.

In summary, this evaluation indicated that conventional fissile self-sufficient and conventional burner IFR core designs which are fueled with weapons plutonium have performance characteristics which are comparable to conventional IFR cores (utilizing recycled fuel). Pure burner designs (non-uranium fuel) have less favorable performance characteristics and would require a significant development program for the new fuel forms.

3. Other Burner Design Studies

Several other studies were performed to investigate specific aspects of IFR burner core design. Detailed trade studies were performed to develop a favorable conventional burner core ($CR=0.65$) configuration for the Mod/B ALMR design effort. Layout development for burner designs is complicated by the impact of large reactivity changes; significant control rod movements are required during each cycle and must be accounted for in the configuration development. Thus, the enrichment split and spatial zoning were tailored to account for these rod movements and their associated power shift.

A wide variety of potential burner core deployment strategies were evaluated. The pivotal issue for the development of a favorable scenario is the specification of sensible economic criteria. If the goal is to maximize the net revenue, the "optimal" case is the construction and operation of the largest number of units; this is realized by using IFR core designs which conserve the transuranic inventory and allow sustained power production. If the goal is to rapidly destroy plutonium, there must be an economic incentive for this activity (e.g., a payment for destruction of this "waste"), because the burning activity makes the closed fuel cycle dependent on an outside source of fissile material. Furthermore, if the goal is to rapidly remove plutonium from an existing stockpile (as investigated in the DOE Plutonium Disposition Study), a new set of

questions must be asked. Use of a large number of reactors will again maximize the net revenue; however, it has been suggested that a more appropriate criteria would be to minimize the up-front capital cost. To minimize the capital cost, the number of required units (capacity) should be minimized; this implies that the target is to maximize the kg/d per MWt transuranic flow rate. This goal is achieved by operating at the lowest possible burnup (MWd/kg) and highest possible transuranic enrichment level. Trade studies were performed to develop such low burnup—high enrichment configurations. Using a lower discharge burnup vastly increases the plutonium introduction rate but implies larger mass flow rates (as compared to conventional burnup levels) in the closed fuel cycle; reduced residence time may also allow the use of less expensive structural materials which have a lower fluence limit. Increasing the enrichment level introduces performance penalties which must also be considered. Based on these considerations, a favorable strategy appears to be the initial use of a low-burnup startup core configuration which allows an existing transuranic inventory to be rapidly transformed into spent fuel and stored; the initial phase is then followed by an extension to conventional discharge burnup levels once recycle is initiated (to reduce the required back-end mass flows).

Finally, the introduction of alternative transuranic feed materials (e.g., transuranics removed from LWR spent fuel) into IFR burner core designs was investigated. The use of recycled LWR or LMR transuranics in the conventional burner design described in Sect. C.2 was compared for both startup and recycle fuel cycle models. These results indicate that the overall performance of the conventional burner design is similar for a wide variety of potential feeds. The primary performance changes are caused by differences in the enrichment requirement dictated by the fissile content of the feed material. Feeds with a lower fissile content (e.g., recycled LWR transuranics) increase the driver enrichment and transuranic loading requirements, but their higher Pu-240 content reduces the burnup reactivity loss rate. In addition, the higher Pu-240 content will increase the sodium void worth (by roughly \$1).

D. Validation and Application of a Physics Database for Fast Reactor Fuel Cycle Analysis

1. Introduction

Implementation of the Integral Fast Reactor (IFR) Physics Analysis Database (PADB) and the associated neutronics methods is a necessary part of the IFR fuel cycle demonstration in EBR-II and its adjoining Fuel Cycle Facility (FCF). This neutronics package (of methods, models, and database) serves not only the EBR-II operations and physics analysis tasks but also the FCF operations and material control and accountancy requirements. The former tasks include EBR-II planning, pre- and post-analysis of all reactor subruns, subassembly exchange worths, configuration estimated critical rod position projections, core-follow analysis, run-report generation, Technical Specifications compliance, special nuclear material requirements and EBR-II material balance area reporting. The latter tasks include (data) interfacing EBR-II with the Fuel Manufacturing Facility which produces fresh fueled subassemblies for the reactor and the FCF which will process the irradiated fuel and produce "fresh" subassemblies for reinsertion into the reactor.

The IFR Physics Analysis Database contains all the data required to model the portion of the IFR fuel cycle demonstration that is external to FCF. It is essentially all the data required to simulate the flow of material into EBR-II, the transmutation of these materials through the operating cycles of the reactor, and the flow of material from EBR-II to FCF. The simulation of the EBR-II fuel cycle is performed with the codes REBUS-3,¹⁴ RCT³⁶ and ORIGEN-RA.^{37,38} This paper presents a brief overview of the application of these three depletion codes to model and analyze the EBR-II fuel cycle, and a brief overview of the software developed to utilize the PADB to automate the input preparation and execution of each of these three depletion codes. It then summarizes results of verification and validation tests of the PADB tables and the neutronics methods and models used to determine the physical characteristics of irradiated subassemblies for demonstration of the IFR fuel cycle in EBR-II and FCF.

2. Simulation of the EBR-II Fuel Cycle

The first step in the depletion analysis is to model the entire reactor with REBUS-3. The neutronics solution uses DIF3D nodal diffusion theory³⁹ with ENDF/B-V.2 nuclear data. Although the model is full-core, three-dimensional, and includes BOC and EOC control rod and safety rod positions, it is rather coarse in some respects. Specifically, (i) each subassembly in the reactor grid is homogenized over axial segments of the hexagonal node of the model (defined as a subassembly and the intrasubassembly sodium) and (ii) the depletion chains include only the principal actinides between ^{234}U and ^{246}Cm and "lumped" fission products. Therefore, the REBUS-3 model contains no information of individual fuel elements (and thus the necessity for the RCT calculations) and contains no information for the hundreds of individual fission products and activation products which are created in the fuel and structure (and thus the necessity for the ORIGEN-RA calculations). The above limitations of the REBUS-3 model should be considered in light of the objectives of the REBUS-3 depletion calculations, namely node-average fluxes and atom densities versus depletion for each EBR-II run. The node-average fluxes are required for the subsequent depletion analysis with RCT and ORIGEN-RA. The node-average atom densities are required not only to compute the node-average fluxes but also to generate the neutronics model for subsequent EBR-II runs.

The second step in the depletion analysis is the reconstruction of the spatial flux distribution within the node (i.e., pinwise and axially within the subassembly) using the RCT code. This intranodal flux distribution is then used to "deplete" each fuel element within the subassembly. The RCT calculations utilize the same burn chain representation used in the REBUS-3 analysis, i.e., the RCT analysis refines (relative to the REBUS-3 analysis) the spatial description of the flux and atom densities but does not refine or augment the depletion chains.

The above two depletion steps are adequate to satisfy the standard operational requirements for EBR-II. However, requirements for the FCF necessitate the calculation of additional discharge and postirradiation quantities, such as fission product inventories, material activation, decay heat, and neutron source vs postdischarge time. At this stage, a third step in the analysis which includes a much more detailed depletion chain is required. This is

accomplished with a special version of the ORIGEN code called ORIGEN-RA, a zero-dimensional depletion code which solves a set of coupled depletion equations using one-group fluxes and cross sections. These one-group fluxes (by reactor run) and cross sections (fluence-spectrum weighted data for nuclides included in ENDF/B-V.2) are generated from the multigroup REBUS-3/RCT results. The detailed depletion chains are represented in an ORIGEN-RA library of materials which includes 253 light activation nuclides, 101 heavy metal nuclides and 821 fission product nuclides. The combined REBUS-3/RCT/ORIGEN-RA analyses provide all the depletion results necessary for not only the EBR-II operations and physics analysis tasks but also the FCF operations and material control and accountancy requirements.

3. Application of the PADB to EBR-II Fuel Cycle Analysis

Considerable effort has been made to define a set of database tables (and to generate the data for these tables) which would be suitable for performing the above fuel cycle analyses for EBR-II. The fruit of this effort is the IFR Physics Analysis Database. Realizing that the database could be of practical use only if it was of very high quality, considerable effort has also been made to create software to generate, maintain, verify and edit the database tables and files. To tap the power of the PADB, a set of software has also been written to utilize the database. Specifically, the codes PRPREB, PRPRCT, PRPORG and ORISOZ have been written to automate the preparation of input (i.e., to largely eliminate the necessity of input by the code user in preparing input "decks") for each step of the fuel cycle analysis of EBR-II.

The flow of data in the PADB and the steps in the fuel cycle analysis are driven by operations in FMF, EBR-II and FCF. For example, when a subassembly is manufactured in FMF for EBR-II, data for this subassembly are entered into the PADB. When fuel management is performed in EBR-II, the same processes are modeled analytically using REBUS-3/RCT; and when fuel is to be transferred from the tank of EBR-II to HFEF or FCF, the necessary database files which define the physical attributes of the irradiated fuel are generated using ORIGEN-RA in conjunction with PRPORG and ORISOZ.

The relation of the depletion codes REBUS-3, RCT and ORIGEN-RA, the PADB, and the input preparation software PRPREB, PRPRCT, PRPORG and ORISOZ is shown in Fig. V.15. Depletion analysis with REBUS-3 is performed for each EBR-II run. Input for this calculation describes every subassembly (fueled and nonfueled, fresh and previously irradiated) contained in the 637 reactor grid locations. This input (consisting of over 20,000 data records) can be generated using the PRPREB code, which requires as input only the name (i.e., run number) of the EBR-II configuration of interest; all other required data are obtained from PADB files. The depletion analysis with RCT is also performed for each EBR-II run. However, in this case only those subassemblies which contain fuel are considered. Input for this analysis (consisting typically of 2000-3000 data records) can be generated using the PRPRCT code, which again requires as input only the name of the EBR-II configuration of interest with all other required data being obtained from PADB files. The depletion analysis with ORIGEN-RA is performed as operation of the FCF requires detailed nuclide inventories, activity or heating values for a particular subassembly. The ORIGEN-RA calculations are performed for that subassembly over the EBR-II runs which span its irradiation history. Although input for these ORIGEN-RA calculations is not particularly voluminous, its preparation (particularly determining its irradiation

Fig. V.15. Relation of Depletion Codes, PADB Files and the "PRP" Software

history and the effective fluence-weighted one-group cross sections) is quite tedious and does require the use of software and the PADB. Input for the ORIGEN-RA calculations and required output files from these calculations are generated with the PRPORG and ORISOZ codes, which require as input only the name of the subassembly of interest.

4. Verification and Validation of the Fuel Cycle Analysis

Verification studies have been made of both the database files and the database software. Verification of the former is achieved primarily through utility software which update and maintain the database files. This software includes extensive checking of the data for consistency and completeness. Verification of the database software is achieved primarily through benchmark testing and independent coding. Both of these verification efforts will remain an ongoing activity throughout the period of application of the database and software.

The validation studies include tests of the applicability of these methods and models in the prediction of the characteristics of subassemblies irradiated in EBR-II. These tests involve comparing results obtained with the current methods and models with: (i) results obtained using higher-order methods and more rigorous models, e.g., continuous-energy Monte Carlo analyses, (ii) results based on independent methods and models, and (iii) experimentally measured quantities.

The REBUS-3/DIF3D code system has for many years been used routinely at ANL to perform LMR fuel cycle calculations. Verification tests (of numerical accuracy, neutronics options, isotope depletion models, etc.) of the REBUS-3 code and its associated neutronic and depletion modules are an ongoing activity. A concerted effort has recently been made to qualify this code system, in conjunction with appropriate cross-section data and reactor modeling procedures, specifically for analysis of EBR-II. Conclusions drawn from the qualification tests include:

1. Neutron fluxes and reaction rates in the core are predicted to within $\sim 2\%$ of reference results computed with continuous-energy Monte Carlo.⁴⁰ Larger errors (up to $\sim 20\%$) observed in reflector and blanket regions are not important for FCF operations and will eventually be reduced through further improvement of flux calculation methods (e.g., use of nodal transport theory and refinement of the multigroup cross-section generation procedures).
2. Depletion-dependent fluxes, reaction rates, and nuclide inventories are in acceptably good agreement with the results of procedures used historically to analyze EBR-II. These older methods, which had been compared against critical experiments and EBR-II measurements, have in the past been successfully used to perform core-follow analyses. Causes for the generally minor discrepancies between the current methods and older methods have been identified and explained.
3. As further described below, local predictions of burnup and fuel isotopics based on the use of REBUS-3/DIF3D (in conjunction with RCT) agree with measured values for small segments of irradiated fuel pins to within the measurement uncertainties.⁴¹
4. Critical control rod positions for a number of recent EBR-II runs are accurately predicted using eigenvalue differences determined with REBUS-3/DIF3D. This agreement indicates the reactivity effects associated with changes in core configuration from one run to the next are adequately simulated, and that the bias in the diffusion theory calculated eigenvalues (due to overprediction of leakage) is quite consistent from run to run.

Several tests of the RCT reconstruction method were reported in Ref. 36, where reconstructed power densities for a heterogeneous model of EBR-II were shown to differ by less than 3% from reference results (computed with a very fine mesh) for all axial positions of all core subassemblies, with the largest errors observed near the core axial boundaries. Reference 42 contains details of a more comprehensive assessment of the RCT and REBUS-3/DIF3D capabilities based on the analysis of an extensive sequence of EBR-II runs (runs 130A through 154F) and the comparison of calculated depletion-dependent characteristics against experimentally measured local values of burnup and U and Pu isotopic ratios. Discrepancies between computed and measured results were found to be smaller than the combined uncertainties inherent in the comparison, e.g., uncertainties in measurements of irradiated sample characteristics, in the EBR-II power level, and in the preirradiation compositions. These uncertainties, which are unaffected by potential refinements in the physics analyses, were estimated to be $\sim 6\%$ (1 sigma) for sample burnup, $\sim 0.2\%$ for ^{235}U , ^{238}U and ^{239}Pu mass fractions, and $\sim 1.0\%$ for ^{234}U , ^{236}U , and ^{240}Pu mass fractions.

A number of verification tests have been performed on the ORIGEN-RA code. Results of those tests indicate that:

1. Results of ORIGEN-RA calculations reproduce results previously computed at ANL with the ORIGEN code to within machine precision differences.
2. ORIGEN-RA very closely satisfies the mass conservation (or more strictly, nucleon conservation) requirement for each of the nuclide groups—heavy nuclides, fission products, and light/structural nuclides—both during and following irradiation.
3. The nuclide densities computed with ORIGEN-RA for simplified transmutation chains agree with analytically calculated values.

4. ORIGEN-RA yields actinide densities in agreement with REBUS-3 calculated values when the ORIGEN-RA data library for actinides is slightly modified to yield nuclide transmutation chains equivalent to REBUS-3.

These verification tests help demonstrate that the ORIGEN-RA code is correctly implemented and that it yields expected results based on its data libraries and problem-specific input.

Studies aimed at validation of the ORIGEN-RA code, data libraries, and models for EBR-II/FCF physics analyses have also been performed; these have so far focused on predictions of those discharge and postirradiation characteristics which are of central importance to fissile material accountability, criticality safety, heat loads in process equipment and containers, and intensity of penetrating radiation emissions. These characteristics include the principal fissile and fertile nuclide densities and reaction rates (validated by demonstrating consistency with REBUS-3/RCT results), densities of fission products by element and chemical group (validated against independent calculations, and by demonstrating insensitivity to major calculational approximations and uncertainties, e.g., neutron capture data for fission products), and decay heat (validated against published experimental and computational results, and against the U.S. decay heat standard ANSI/ANS-5.1-1979). Errors in the ORIGEN-RA predictions of these different parameters were demonstrated to be acceptably small for EBR-II/FCF applications. Examples of comparison of ORIGEN-RA predicted decay heat and the 1979 ANS decay heat standard are shown in Figs. V.16 and V.17. For decay times longer than one day following the pulse irradiation (these results are not shown), and for all decay times following the extended irradiation, the ORIGEN-RA values are within $\sim 5\%$ of the decay heat standard.

As an example of one portion of the several validation studies referenced above, results are presented in Table V.9 of a study of the nuclide distributions calculated by REBUS-3/RCT and ORIGEN-RA for a standard EBR-II driver subassembly. Although it would be possible to obtain the required detailed nuclide inventories by representing each fuel element explicitly in ORIGEN-RA (and performing multiple calculations to obtain their axial

Fig. V.16. Fission Product Decay Heat Comparison of ORIGEN-RA to ANSI/ANS-5.1-1979

Fig. V.17. ^{235}U Fission Product Decay Heat Comparison of ORIGEN-RA to ANSI/ANS-5.1-1979

TABLE V.9. Actinide Masses (g) at Discharge for Elements 209822 and 302126 in Subassembly B3509Y

	B3509Y	Element 209822				Element 302126			
	OR-RA ^a	OR-RA ^b	%Diff ^c	RCT/OR ^d	%Diff ^e	OR-RA ^b	%Diff ^c	RCT/OR ^d	%Diff ^e
²³² U*	2.55-08	3.13-08	-18.72	2.55-08	-18.72	1.96-08	29.65	2.55-08	29.65
²³³ U*	7.08-06	8.91-06	-20.55	7.08-06	-20.55	5.15-06	37.51	7.08-06	37.51
²³⁴ U	1.53-01	1.52-01	1.07	1.52-01	0.00	1.56-01	-1.63	1.56-01	-0.02
²³⁵ U	1.42+01	1.41+01	0.64	1.41+01	-0.01	1.44+01	-0.96	1.43+01	-0.04
²³⁶ U	4.91-01	4.71-01	3.41	4.74-01	0.03	5.21-01	-5.79	5.22-01	0.18
²³⁷ U*	1.35-06	1.32-06	2.14	1.35-06	2.14	1.45-06	-7.04	1.35-06	-7.04
²³⁸ U	7.75+00	7.74+00	0.24	7.74+00	0.00	7.81+00	-0.72	7.81+00	-0.01
²⁴⁰ U*	6.56-26	4.22-26	55.32	6.56-26	55.32	1.24-25	-47.03	6.56-26	-47.03
²³⁶ Np*	3.30-27	4.19-27	-21.42	3.30-27	-21.42	2.47-27	33.40	3.30-27	33.40
²³⁷ Np	1.04-02	1.04-02	0.23	1.04-02	0.04	1.09-02	-4.68	1.10-02	0.27
²³⁸ Np*	1.39-13	1.29-13	7.86	1.39-13	7.86	1.62-13	-14.08	1.39-13	-14.08
²³⁹ Np*	1.49-10	1.45-10	2.16	1.49-10	2.16	1.56-10	-4.96	1.49-10	-4.96
^{240m} Np*	5.66-28	3.64-28	55.33	5.66-28	55.33	1.07-27	-47.03	5.66-28	-47.03
²³⁶ Pu	3.35-08	4.30-08	-22.09	4.25-08	-1.00	2.48-08	35.16	2.51-08	1.26
²³⁸ Pu	3.08-04	2.89-04	6.51	2.90-04	0.20	3.52-04	-12.53	3.54-04	0.62
²³⁹ Pu	1.13-01	1.10-01	2.62	1.10-01	0.05	1.19-01	-5.20	1.19-01	0.21
²⁴⁰ Pu	1.34-03	1.17-03	15.32	1.16-03	-0.78	1.65-03	-18.57	1.67-03	1.14
²⁴¹ Pu	1.26-05	9.94-06	26.42	9.76-06	-1.76	1.75-05	-28.29	1.77-05	0.93
²⁴² Pu	7.98-08	6.07-08	31.54	5.89-08	-2.83	1.19-07	-33.14	1.20-07	0.70
²⁴⁴ Pu*	3.44-15	2.21-15	55.32	3.44-15	55.32	6.49-15	-47.03	3.44-15	-47.03
²⁴¹ Am	3.94-07	3.13-07	25.83	3.05-07	-2.63	5.46-07	-27.95	5.53-07	1.17
²⁴² Am*	1.28-14	9.82-15	30.17	1.28-14	30.17	1.89-14	-32.25	1.28-14	-32.25
^{242m} Am	1.06-09	8.18-10	30.17	7.85-10	-3.98	1.57-09	-32.25	1.59-09	1.16
²⁴³ Am	3.85-10	2.68-10	43.48	2.56-10	-4.53	6.50-10	-40.78	6.49-10	-0.14
²⁴² Cm	2.38-09	1.82-09	30.41	1.75-09	-3.67	3.52-09	-32.45	3.55-09	1.04
²⁴³ Cm	8.23-12	5.27-12	56.22	4.94-12	-6.26	1.51-11	-45.40	1.50-11	-0.52
²⁴⁴ Cm	4.18-12	2.70-12	54.88	2.52-12	-6.40	7.86-12	-46.85	7.76-12	-1.25
²⁴⁵ Cm	2.65-14	1.63-14	62.22	1.50-14	-8.03	5.37-14	-50.66	5.26-14	-2.15
²⁴⁶ Cm	5.33-17	3.21-17	66.30	2.91-17	-9.36	1.13-16	-52.89	1.10-16	-2.68
²⁴⁷ Cm*	7.23-20	4.07-20	77.53	7.23-20	77.53	1.69-19	-57.22	7.23-20	-57.22
²⁴⁸ Cm*	1.15-22	0.00+00		1.15-22		2.18-22	-47.33	1.15-22	-47.33
Total	2.274+1	2.261+1	0.572	2.260+1	-0.004	2.297+1	-1.017	2.296+1	-0.020

*Isotope not explicitly represented in REBUS-3/RCT depletion chains.

*Subassembly depletion in ORIGEN-RA. Results expressed as "per element" masses.

*Element depletion in ORIGEN-RA.

*OR-RA^a relative to OR-RA^b for this element.*Isotope mass based on REBUS-3/RCT and OR-RA^a.*RCT/OR^d relative to OR-RA^b for this element.

distributions), this is computationally impractical due to the large number ($\sim 20,000$) of fuel elements in the database. To test the accuracy of the computed distributions, a comparison was made between nuclide densities obtained from explicit pin ORIGEN-RA calculations (taken as the reference solution) and results more efficiently obtained by combining densities from a single ORIGEN-RA subassembly depletion with spatial density distributions derived from a set of RCT pin depletion calculations. The subassembly (B3509Y) selected for this study experienced relatively large flux and spectrum gradients (having been irradiated in the outermost row of the core), thereby accentuating the spatial variations from element to element. The results given in Table V.16 compare actinide masses at discharge in the highest- and lowest-fluence elements (serial numbers 209822 and 302126, respectively) in this subassembly. Note that for these two elements there are variations in nuclide masses of up to 100% for trace isotopes, ~ 20 -50% for minor isotopes, and ~ 2 -4% for important isotopes (^{235}U and ^{239}Pu). The combination of nuclide distribution factors from RCT analyses with a "homogenous" subassembly ORIGEN-RA depletion calculation generally agrees with the explicit pin ORIGEN-RA calculations to within 1% for minor isotopes and within 0.1% for important nuclides. Large differences remain for the trace isotopes not represented in the REBUS-3/RCT analyses. This good agreement is achieved despite the use of fluence-weighted cross sections in the ORIGEN-RA analysis and helps justify the fluence-weighting procedure as well as the REBUS-3/RCT spatial density distributions.

The observed good agreement between ORIGEN-RA and REBUS-3/RCT predictions of principal actinide densities, taken together with the good agreement demonstrated in Ref. 41 between REBUS-3/RCT predictions and measurements of burnup and isotope mass fractions in irradiated EBR-II fuel samples, helps confirm the accuracy of the ORIGEN-RA predictions of these quantities. It also indicates that ORIGEN-RA, which makes use of one-group fluxes and fluence-weighted one-group cross-sections derived from the REBUS-3/RCT analyses, accurately predicts the time-dependent fission rates of the principal heavy isotopes, and consequently the overall rate of fission product creation.

Assessment of the ORIGEN-RA libraries is also an important part of the validation of the code predictions. These libraries include decay parameters (decay constants, branching

ratios, energy/disintegration values), collapsed one-group cross sections, and fission product yield fractions. As part of the evaluation of these libraries, a set of ORIGEN-RA "test libraries" was developed by replacing the decay constants, branching ratios, and disintegration energies in the "standard libraries" with corresponding values derived from the ORIGEN2 data libraries^{42,43} (which were developed more recently than, and independently of, the ORIGEN-RA libraries).

Depletion and decay cooling analyses of an EBR-II subassembly, as well as of PWR and mixed-oxide LMR models, were performed using both the standard and test libraries. For these comparisons the cross section data and other case-specific input parameters were maintained the same. Conclusions from these comparisons, which revealed generally minor differences between predictions obtained with the different decay libraries, include:

1. The agreement of the time-dependent activities is very good ($\sim 1\%$ differences) for all nuclide categories (light, heavy, and fission products), suggesting the overall consistency between the two sets of decay constants is good.
2. The agreement in total decay heat, particularly in the dominant fission product component of the decay heat, is also quite good ($\sim 3\%$).
3. The short-term actinide decay heat (for cooling times less than 50 days) is underpredicted with the standard actinide library as a result of apparent errors in the decay energies for ^{237}U , ^{239}U , and ^{239}Np . Decay energies for these nuclides all appear to be low in the standard library compared to the corresponding values from the updated library, ENDF/B-V values, and values specified in the 1979 ANS decay heat standard.
4. The structural nuclide decay heat is predicted significantly larger ($> 50\%$) with the standard library for all cooling times considered. This discrepancy is again due to decay energy differences between the two libraries,

particularly for the stainless-steel activation products ^{54}Mn , ^{55}Fe , and ^{58}Co . However in this case, the standard library values are in much better agreement with the ENDF/B-V decay energies.

5. The agreement in the gamma component of the decay heat (based on ORIGEN-RA photon library estimates) is reasonably good for light nuclides and fission products ($\sim 4\%$). The agreement in actinide gamma heat is rather poor, due to differences in effective gamma disintegration energies. However, the largest differences ($\sim 60\%$) are observed at cooling times greater than 1 year, for which the actinide gamma heat is an extremely small fraction of the total actinide decay heat (and is also insignificant compared to the gamma heat from fission product decay).
6. The agreement in spontaneous fission neutron source is excellent.
7. The agreement in element masses for U, Pu, and Np is excellent. The same is true for all isotopes of these elements present in significant quantities. The differences in Am and Cm masses are on the order of 2% and appear to be due to a shorter (by 1.5%) half-life for ^{241}Pu on the test library compared to the standard library.
8. The agreement in the total lanthanide and total noble metal fission product masses is excellent. The respective decay heat contributions of these two fission product groups are also in very good agreement.

5. Conclusions

The above verification and validation studies indicate the IFR Physics Analysis Database and the software and procedures used to analyze EBR-II core physics are sufficiently accurate to satisfy operational and material accountability requirements in FCF. In particular, masses of the primary fissile and fertile nuclides in irradiated fuel elements can be computed with

an accuracy of $\sim 2\%$ or better, based on assessments of the dominant calculational uncertainties and on comparisons of predictions obtained with the REBUS-3/RCT codes against burnup measurements,⁴¹ EBR-II operational data, and more rigorous calculational models. Relative errors in the predicted amounts of trace heavy isotopes and individual fission/activation product nuclides are potentially much larger. Nevertheless, integral characteristics of the fission product inventories of relevance to FCF operations were found to be rather insensitive to the approximations and uncertainties in the ORIGEN-RA analyses used to compute detailed inventories and postirradiation characteristics. Moreover, some of these integral characteristics have been successfully benchmarked against measurements and independent calculations, as well as the 1979 American National Standard for fission product decay heat. Based on these comparisons, and on the uncertainties in EBR-II fission rates, it appears that the uncertainties in the activity and decay heat of discharged subassemblies are each on the order of 10% for the postirradiation cooling times of interest. The uncertainty in the gamma component of the decay heat is larger ($\sim 25\%$).

It has thus been demonstrated that the IFR Physics Analysis Database (and associated analysis procedures) are adequate to predict required quantities in support of early phases of FCF operations. Additional validation questions regarding these methods will likely arise after FCF provides operating experience.

E. Validation of the SE2-ANL Code Using EBR-II Temperature Measurements

The SE2-ANL code⁴⁴ is used at ANL to compute the core-wide temperature profiles in Liquid Metal Reactor (LMR) core geometries for a given flow distribution and to determine the assembly flow rates yielding a desired (assembly-wise) peak temperature distribution. SE2-ANL is a modified version of the SUPERENERGY-2 thermal-hydraulic code,⁴⁵ which is a multiassembly, steady-state subchannel analysis code based on the simplified energy equation mixing model,⁴⁶ with interfacing capability with ANL heating calculations,⁴⁷ additional models for fuel and cladding temperature calculation, and hot spot analysis capabilities.

The accuracy of the SE2-ANL code has recently been tested by comparing the predicted temperatures with measured values in the Experimental Breeder Reactor II (EBR-II). The sodium temperature distributions within two experimental subassemblies were calculated and compared to the values measured by thermal expansion difference temperature monitors (TED's) installed in these subassemblies. Subassembly outlet temperatures were calculated for three reactor runs and compared with recorded plant data for 21 coolant outlet thermocouples. The fuel surface temperatures were also calculated and compared with the values inferred from the experimental data from micrographic analysis and microprobe examinations of restructured fuel elements which had been irradiated in the EBR-II reactor.

Coupled heating, flow, and temperature calculations were performed to analyze the sodium and fuel temperatures. The heating calculations were done with coupled neutron and gamma heating calculations⁴⁸ based on three-dimensional full core models. The finite difference diffusion theory option of the DIF3D code⁴⁹ was used in calculating the neutron and gamma fluxes, and the GAMSOR code⁴⁷ was used in computing the gamma source. For the sodium outlet temperature analysis, the pin power distributions were obtained by the low-order polynomial interpolation scheme implemented in SE2-ANL. On the other hand, for the analyses of TED experiments and restructured fuel elements, they were calculated with the RCT code³⁶ to take into account the different types of pins constituting a subassembly and the intra-assembly burnup gradients. The flow rate of each subassembly was calculated using the EBRFLOW code.⁵⁰ This code is based on balances of flow rates and pressure drops in the EBR-II primary system, and on suitable input information from flow-modeling studies and engineering estimates of the leakage flow rates. The SE2-ANL calculations were performed using these power distributions and subassembly flow rates. A full core model was used in these SE2-ANL calculations for the sodium outlet temperature analysis, whereas a seven subassembly model consisting of the subassembly of interest and its six neighboring subassemblies was employed for the analyses of TED experiments and restructured fuel elements.

The sodium temperature distributions were calculated for two fueled experimental subassemblies (X494 and X495) containing 11 dummy elements, which were irradiated in EBR-II during Run 152D. These subassemblies were located at the grid positions 4C2 and 6C4 and surrounded by driver, control, and reflector subassemblies. Each dummy element contained three TED's which were located at the core center, top of the core, and top of the element. The temperature measured by a TED is the peak temperature during the reactor cycle. The SE2-ANL results were compared with these measurements. As shown in Table V.10, the maximum deviation between the calculated and measured temperatures was $\sim 15^{\circ}\text{C}$ for X494 and $\sim 20^{\circ}\text{C}$ for X495. The root mean square deviation was $\sim 8^{\circ}\text{C}$ for both subassemblies. The relative maximum deviation was $\sim 3\%$ for X494 and $\sim 5\%$ for X495, and the relative root mean square deviation was $\sim 2\%$ for both subassemblies.

The accuracy of the thermal hydraulics computational methods of SE2-ANL was also tested by repeating the same calculations using the COBRA-WC⁵¹ code which includes more detailed thermal hydraulics models. The maximum difference between COBRA-WC predictions

TABLE V.10. Deviations of Subchannel Sodium Temperatures ($^{\circ}\text{C}$) of SE2-ANL from Measured Values

Row No.	Pin No.	Subassembly X494				Subassembly X495			
		Core Center	Core Top	Element Top	Row Average	Core Center	Core Top	Element Top	Row Average
1	31					1.8	-1.4	0.3	0.2
3	24	-0.8	-10.0	-5.4	-2.3	-4.5	-6.5	-11.0	-0.5
	29	5.8	-0.3	0.8		-0.1	6.1	7.8	
	48	1.9	-0.3	0.8		3.8	-2.0	2.1	
4	7	5.1	-4.4	1.1	-5.1	-9.0	0.2	0.3	-4.8
	34	1.0	-14.7	-11.3		-9.6	-17.7	-11.3	
	53	-5.9	-13.0	-3.8		-0.5	-1.8	6.5	
5	5	-1.7	-11.0	-9.1	-1.5	-6.3	-16.0	-7.7	1.5
	19	10.2	13.3	7.3		19.7	16.7	12.8	
	44	-14.5	-4.1	-4.2		-10.4	-4.6	-1.4	
	61					5.4	4.1	5.8	
Column Average		0.2	-5.5	-3.5		-0.9	-2.1	0.4	
Root Mean Square		7.8				8.5			

and measurements was $\sim 18^{\circ}\text{C}$ for both subassemblies, and the root mean square difference was $\sim 8^{\circ}\text{C}$ for X494 and $\sim 6^{\circ}\text{C}$ for X495. The maximum difference between SE2-ANL and COBRA-WC predictions was $\sim 8^{\circ}\text{C}$ for X494 and $\sim 13^{\circ}\text{C}$ for X495, and the root mean square difference was $\sim 5^{\circ}\text{C}$ for both subassemblies. These comparison results show that the accuracy of SE2-ANL is comparable to that of COBRA-WC.

The mixed mean subassembly outlet temperatures were calculated for three reactor runs (Runs 163A, 164A, and 165A) by SE2-ANL and compared with the recorded plant data for the coolant outlet thermocouples, which are installed in the upper plenum one quarter inch above the subassembly outlet for 21 subassembly positions. The deviations of the SE2-ANL results from the measured values are shown in Table V.11 along with the measured sodium outlet temperatures.

TABLE V.11. Deviations of Subassembly Mixed Mean Outlet Temperatures ($^{\circ}\text{C}$) of SE2-ANL from Measured Values

Subassembly Position	Run 163A		Run 164A		Run 165A	
	SOT ^a	Diff ^b	SOT ^a	Diff ^b	SOT ^a	Diff ^b
1A1	455.7	4.8	455.1	3.8	454.9	4.1
2A1	450.1	8.1	449.2	7.5	451.2	8.1
2B1	476.9	-35.1	486.8	-55.3	485.7	-52.9
2C1	442.7	8.9	443.0	7.3	449.7	11.3
2E1	458.5	-0.3	457.4	-0.7	458.4	-1.8
2F1	449.0	-7.8	447.6	-7.4	448.6	-7.1
3B1	475.0	0.1	474.4	-0.9	465.3	6.7
3C1	460.7	7.8	460.5	7.2	460.1	7.6
3F1	465.2	8.7	463.1	9.2	462.9	9.2
4B1	479.1	11.9	477.8	11.9	478.2	9.4
4C3	473.1	9.3	473.3	7.8	472.2	9.0
4E1	482.2	7.6	481.2	6.6	481.8	6.3
4F1	491.3	0.6	489.8	0.5	488.1	1.4
5A4	487.8	4.3	487.2	3.7	485.6	4.1
5C2	455.7	3.5	458.0	1.1	457.0	1.6
6C4	479.6	4.9	484.6	6.2	488.5	2.6
7A3	452.6	41.6	452.2	40.6	449.5	41.2
7D4	436.2	48.2	433.4	63.3	432.1	64.6
9E4	456.7	-4.2	462.6	-9.2	460.0	-6.1
12E6	518.1	-5.8	517.7	-4.5	519.7	-5.5
16E9	433.4	-48.1	430.5	-45.2	428.7	-43.3

^aMeasured Sodium Outlet Temperature.

^bCalculated Temperature—Measured Temperature.

Excluding the four subassemblies in grid positions 2B1, 7A3, 7D4, and 16E9, the root mean square deviation between the calculated and measured temperatures were $\sim 6^{\circ}\text{C}$, and the maximum deviation was $\sim 12^{\circ}\text{C}$. The difference in the subassembly coolant temperature increment above inlet between SE2-ANL and measurement is within $\sim 10\%$ of the measured temperature increment. The unreasonably large deviations between calculated and measured outlet temperatures observed in the aforementioned four subassemblies seem to be due to either biased thermocouple readings or pronounced flow mixing effects. The thermocouples for the subassemblies 2B1, 7A3, and 7D4 are believed to be biased based on the inconsistency of the thermocouple readings with the power-to-flow balances. The thermocouple for the subassembly 16E9 is very likely affected by the coolant flowing radially outward from the low pressure plenum region.

Postirradiation examinations of two cross sections of irradiated U-10Zr pins (DP11 and DP81) at different axial elevations were also used to validate the SE2-ANL code. Thermodynamics of phase formation was used to infer from the experimentally observed radii of phase interfaces what the temperatures were at those radii. Using the phase interface radii determined by the microprobe examination and the porosity fractions in different zones determined by the micrographic analysis, the fuel surface temperatures were estimated.⁵² These temperatures were compared with the values calculated by SE2-ANL. Since the amount of sodium logging in each restructured zone cannot be determined experimentally, the upper and lower bounds of the fuel surface temperatures were estimated for physically reasonable ranges of sodium logging fraction. The effects of the data uncertainties on these bounds were also considered by performing the sensitivity and uncertainty analysis. The fuel surface temperature range was between 617°C and 643°C for the DP81 cross section, and between 617°C and 635°C for the DP11 cross section. The calculated SE2-ANL temperature was 632°C for DP81 and 622°C for DP11. These fuel surface temperatures calculated by SE2-ANL are well within the expected range of temperature inferred from the postirradiation examination of the pin cross sections.

In summary, comparisons have been made between SE2-ANL estimates and measurements (or values inferred from measured data) for the sodium temperature distributions within subassemblies, the mixed mean subassembly outlet temperatures, and the surface temperatures

of fuel elements. All the comparison results considered in this study indicate reasonably good agreement between the SE2-ANL and the measurement. In addition, the code yields results with accuracy comparable to the more detailed COBRA-WC calculations at much lower computational cost. In conclusion, the validation studies summarized in this report show that the SE2-ANL code can be used for core-wide steady-state thermal hydraulics calculations with good accuracy and low computational cost.

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VI. FUEL CYCLE DEMONSTRATION

A. Status of Facility Modifications

Fiscal Year 1994 was noteworthy for the near completion of the facility for Hot Operations. Most construction activities were completed, and preparations were well advanced for acceptance testing of major systems. Significant progress was made toward administrative closure of engineering documentation packages, and many elements of ANL's pre-Operational Readiness Review (ORR) process were started or near completion. The ORR is scheduled to start on December 1, 1994.

Details of progress on major systems are presented below.

1. Corridor Modifications

New fire-wall headers were installed in both north and south corridors, and penetrating service lines were fire-stopped and wrapped. Folding curtain walls were installed, and the air locks completed. A fire-rated window was installed in the north corridor wall. Various new fire doors were installed, and the basement corridor false ceiling was completed. A special concrete shielding plug was installed in the A4 window opening. This completed all planned work on this particular task.

2. Transfer Systems

Qualification of the OCS/PLC software for the Equipment Transfer Lock (ETL) and Small Transfer Lock (STL) began. The Platen Safety Restraint (PSR) vertical position indicator noise problem was solved by adding a shaft encoder.

3. Cask Transfer System Modification

The cask cart was assembled in place and tested for function and load capacity. New hardened bolts were installed in the cart rails. The design of a seismically-qualified (in the down position) bag sealer was finalized and fabricated. The bag feed/bleed system was installed, and the cask shield ring was fabricated.

4. Suited Entry Repair Area (SERA)

A design for the high pressure spray cleaning system was finalized, and procurement started. A new chiller was designed to service both the spray chamber dehumidifiers and the general basement area. The bag-out and shield rings were fabricated and installed in the transfer port located in the floor of the SERA. Shield doors for the manipulator repair booth, manipulator checkout window, and the personnel entry door were installed. The manipulator repair booth was bolted in place and the windows were installed. The east shield door and movable shield doors for the north and west walls were installed. A manipulator check-out pit crane design was finalized. The temporary work platform was removed from the spray chamber, and spray chamber component checkout began. Support structures for the electromechanical manipulator repair pit and spray chamber cooling system were installed. Design of the manipulator-hangers and surrogate filter housing was completed.

5. Argon Cell

Following an extensive acceptance test in air, the Fuel Cycle Facility Modifications Project reached a major milestone on December 18, 1993 when the Argon Cell was filled with argon gas for the first time in almost 20 years. The filling operation began on Friday morning and was completed by noon on Sunday. In that period, about 140,000 cubic feet (slightly over two cell volumes) of argon gas was used to replace and purge the air atmosphere. A final oxygen concentration of 225 ppm in the argon atmosphere was reached before the purge was stopped and the cell closed. The fill operation went very smoothly and the purity level reached before cell closure was better than anticipated.

An extensive effort was then mounted to locate and stop a variety of air in-leaks. Many leaks were located and fixed (e.g., dryer flange, valve packing, rotameter, manipulator tubes, and windows), but some leaks eluded detection for a long time. A major leakage path was eventually isolated in the purification system, and in-leakage was reduced to about 0.05 scfm.

6. Argon Purification System

As a safety precaution, the OCS/PLC control program was modified to require a manual reset of the hydrogen injection valves following a seismic trip. During the search for air in-leakage paths, it was determined that the dryer desiccant needed to be replaced. During the replacement operation, failure of an expandable pipe-plug caused a release of contaminated desiccant into the subcell. An extensive cleanup ensued, after which the remainder of the desiccant was removed and new desiccant installed in the dryer. Design of a redundant parallel dryer was finalized, and procurement began. Special OCS/PLC programming was written to preclude the possibility of an air-injection accident due to a faulty valve line-up. The north recirculation loop argon blower was replaced and tested. Problems with the purification system's oxygen monitors were eventually traced to internal instrument leaks and faulty range switches, which were fixed. Various other instrumentation problems were resolved and the system was declared fully operational.

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

Formal acceptance testing of this system in argon was partially completed. Additional control program tuning will be needed when real heat loads are available. A complete spare parts inventory was established for this vital system.

8. Refrigeration System

Numerous freon leaks were discovered in this system which dates back to the original facility construction. The system was extensively refurbished by the manufacturer and a control-system modification was installed to better accommodate low heat loads. However,

minor freon leaks continued to be found and repaired as necessary. A potential concern of low cooling water flow was noted and is being evaluated.

9. Argon Cell Safety Exhaust System (SES)

The formal acceptance test of this major system was completed prior to argon fill. After fill, oil levels were adjusted in the seal pots in accordance with the requirements of the Technical Safety Requirements (TSRs). Minor leakage through the normally-closed automatic dampers was discovered. One damper was replaced with a spare, but the replacement also leaked. The damper that was replaced was repaired, but testing revealed leakage at differential pressures above 1 psid. While there are no safety implications posed by this leakage, these dampers will either be replaced or a modification to vent the slow pressure buildup caused by the leakage will be made.

10. Safety Equipment Building (SEB)

All radiation monitoring was installed, tested, and placed in operation.

11. Electrical Power Systems

Division 1 of the Emergency Power System was placed in service. All OCS/PLC software was completed and tested. The proposed mobile diesel fuel tanker will be replaced with an existing underground storage tank (UST) and dedicated mobile delivery equipment.

12. Stack Monitor

Qualification of the OCS/PLC software was completed. This system is fully operational and in service.

13. Air Cell and Building Exhaust Systems

Following a trial fit-up, the cask transfer floor plugs were final-machined, and new gaskets installed. All ventilation ducts subject to positive pressure were tested and accepted. A complete building ventilation flow balance (including off-normal configurations) was performed, and dampers and registers locked in place. The flow balance was then verified by an independent contractor certified test. A problem with spurious flow alarms was eventually traced to turbulence within the building exhaust ducting, and was solved by installing low-pass filters in the flow instrumentation.

14. Radioactive Liquid Waste System (RLWS)

Those subsystems needed for washing of EBR-II subassemblies were installed, tested, and placed in operation. Design of the remainder of the liquid systems and their OCS/PLC software continued. Many of the instrumentation and control (I&C) components were procured. A final design was accepted for the ion exchange system. To improve efficiency and accelerate the construction, sub-sections (called "spools") of the complex piping runs were fabricated in the shop for field installation. Surplus contaminated lead shot was acquired (at no cost) from another INEL contractor and installed in the double-walled shielded tanks. The heavy radiation shield doors were hung, and shield testing of the tanks and shielded isolation room was completed. Large spray heads were installed in the tank lids to permit internal decontamination of the tanks. Poison rods were received for all the tanks. When it was discovered that the rods (a flexible boron-impregnated rubber material) would not fit in the poison-rod wells of the decon liquid holdup tank (which had been obtained from excess at no cost), 0.020 in. was machined from the rod diameter to make them fit. A final design review was held for the entire RLWS. At this time, it was decided to keep the sampling subsystem separate from the chemical injection subsystem because of possible alpha cross-contamination. Also, it was decided to use nonmetallic pipe sleeves in lieu of cathodic protection for buried piping. All of the decon spray chamber fixed equipment and trench/pit piping designs were finalized, and the fixed equipment was either procured or fabricated.

15. Facility Instrumentation and Control (I&C)

Electrical noise in the constant air monitors (CAMs) was determined to be the cause of frequent false high-radiation alarms. The source of this noise was eventually traced to arcing in the contacts of an air-flow relay. Arc suppressers supplied by the manufacturer were installed in all the CAM's, and no further problems were encountered. A rash of alarm horn failures in Beta CAM's was corrected by replacing the horns with 110 V models and isolating them from other circuits. Many oxygen monitors were replaced because of temperature/pressure instability. All of the OCS/PLC software for facility monitoring and logging was qualified.

16. Miscellaneous

All nonstructural modifications were completed in the roof area, and the structural modifications were started. Welding of the exit cell hatch, installation of shielding lead bricks on the hatch, and removal of the exit cell shield door will be deferred until all process equipment has been installed in the cells. Analyses were started to show that the existing Hot Repair Area (HRA) crane and exit cell hatch door drive modification are seismically adequate. All radiation monitors and alpha and beta CAMs were installed. All the criticality monitors, except those that are part of the A4 window shield plug, were installed tested, and accepted. Fabrication of miscellaneous floor hatches was started, and the shield door for subcell-D was installed.

B. Status of Process Equipment

During FY 1994, all major pieces of process equipment were installed in the argon cell. With the decision to not fabricate plutonium alloy fuel in Spring 1994, emphasis was placed on the equipment which will be used to treat the EBR-II fuel for interim storage and eventual disposal. The treatment process will utilize the vertical assembler dismantler (VAD), the element chopper, the electrorefiner and ancillary support equipment, the cathode processor, and casting furnace. Although the fabrication equipment will not be used, significant progress was achieved before the program was redirected. Problems with the remote handling systems had a significant negative impact on progress with the process equipment.

1. Vertical Assembler Dismantler (VAD)

The VAD, an existing machine, removes the individual fuel elements from irradiated subassemblies. During FY 1994, the control console was rewired and some instrumentation was replaced to meet new requirements. Several structural modifications to improve seismic restraints, modifications to the cooling system and a new work table were installed. Troubleshooting of the reactivated system was started and should be completed in early FY 1995.

2. Element Chopper

The element chopper shears the fuel elements into fuel segments which expose the fuel for dissolution in the electrorefiner. This equipment was installed in FY 1991 and tested during FY 1992. During FY 1994, the element chopper was reassembled, the operating instructions were drafted, and training of a new crew to operate the equipment was started. This equipment is ready for its final qualification, which requires chopping sodium bonded depleted uranium fuel elements.

3. Electrorefiner

The electrorefiner is the key treatment process for the EBR-II fuel. The reactive component, elemental sodium, will be reacted to a chloride salt and actinides will be collected for incorporation into an interim storage form. The electrorefining operations will occur at Workstations P3 and P4. Station P4 contains the electrorefiner assembly itself, consisting of the support structure, heater/insulator assembly, vessel, dedicated electrode mounting and positioning devices, salt and cadmium stirrer, cover gas supply/vent system and various instrumentation and chemical addition/removal systems. The electrodes are removable and are of three types: anodes, solid cathodes, and liquid cathodes. The anodes and solid cathodes are virtually identical except that the rotating shaft that penetrates into the electrolyte salt has a fuel dissolution basket lower assembly attached for the anode electrode and a mandrel lower assembly for the cathode electrode. The liquid cathode is, in many respects, similar to the other two, but is provided with

a crucible assembly that is submerged in the salt. This crucible contains a precharge of cadmium which is molten at operating temperature. Uranium, plutonium, and minor actinides are deposited into this molten cadmium. Additional details of this electrode are given below.

Station P3 contains the electrode assembly and disassembly machine (EADM). This machine provides remote handling support to the electrorefining operations. The fuel dissolution baskets containing the element segments are attached to the electrode shafts. After the electrorefining operations, the solid cathode material is harvested from the electrode mandrel, the liquid cathode material is removed from the liquid cathode crucible, and the cladding hulls are emptied from the dissolution baskets. Also, electrode assembly maintenance and other support operations such as handling and packaging electrorefiner samples are performed with this equipment.

Design, acquisition, and qualification activities related to the FCF electrorefiner system continued in FY 1994. Highlights included the shipment to ANL-West of the remaining components and systems of the electrorefiner, including the cover gas system in October, the electrode assembly and disassembly machine and fixturing in December, a new liquid level probe assembly in August, and the liquid cadmium cathode assemblies in September. Installation of equipment into the FCF argon cell includes the electrorefiner in June, the electrode storage fixtures in July, and the electrode assembly and disassembly machine in August.

Major design efforts included preparation of the fabrication packages for the liquid cadmium cathodes as well as the electrode assembly and disassembly machine fixturing for preparation and refurbishment of these devices; resumption of the design of the bulk fluid handling system that was deferred because of resource constraints during the earlier electrorefiner design phase; and design of the contact-type liquid level probe to provide materials accountability measurements of the contents of the electrorefiner.

The liquid cadmium cathode assembly (Fig. VI.1) includes a linear drive mechanism, in addition to the standard electrode assembly rotational drive (Fig. VI.2), that allows dendritic formations on the surface of the liquid cadmium in the collection crucible (Fig. VI.3)

Fig. VI.1. Liquid Cadmium Cathode Assembly Mounted in Test Stand at ANL-East
(ANL Neg. No. 18110K, Frame 5)

Fig. VI.2. Drive and Electrical Connection Components of Liquid Cadmium Cathode Assembly Without Motors Attached
(ANL Neg. No. 18110K, Frame 7)

Fig. VI.3. Crucible Assembly and Compactor of Liquid Cadmium Cathode Assembly with Aluminum Surrogates for the Ceramic Crucible and Compactor (ANL Neg. No. 18110K, Frame 8)

to be pushed down into the bulk of the fluid by a compactor. Handling of these electrodes on the electrode assembly and disassembly machine consists of removing the crucible assembly into an inverter fixture that allows transfer of the heavy-metal-laden ingot directly into the cathode processor process crucible. The same fixture allows recycle of the distilled cadmium from the receiver crucible back into the electrode crucible.

A second preliminary design review was held for the bulk fluid handling system to take into account any changes in requirements and electrorefiner design/operation strategy since the design effort was suspended after the first review in April 1991. Several important changes were made as a result of this review: the electrorefiner port for intended use was switched from

A to D (both anode ports); pump intake elevation capability was made to be continuous rather than discrete; receiver container support became an adapter for the Port A elevator and rotator assembly instead of a separate structure; and the transfer line was given a freeze seal at both ends instead of just the pump end for additional future flexibility. At the end of the reporting period, a final design review was scheduled.

The liquid level probe was designed to meet the measurement accuracy requirements necessary for adequate fissile material accountability. It relies upon the sudden drop in resistance that will be seen in a circuit comprising of a meter and leads, the probe itself, the salt and/or cadmium, and the electrorefiner vessel when the probe is driven into contact with the liquid being measured. The assembly includes a calibration standard and five probe tips for various height ranges, and a manually-operated probe drive mechanism with a scale readable to a hundredth of an inch.

During early FY 1994, the remote qualification of the electrorefiner was completed. This qualification included testing all operations using only the electromechanical manipulator, crane and masterslaves in the mockup area. Numerous minor notifications were made to both the hardware and software to improve operability. The remote qualification was completed in Spring 1994 when a integrated system test was performed at 300° C. After this test, the vessel volume was calibrated to support accountability activities.

During the summer, the equipment was transferred and placed at workstation P4 (shown in Fig. VI.4) in the argon cell. The electrical control cabinets were installed in Room 25 and the permanent cabling was installed. In-cell testing of the electrorefiner components is essentially complete and process chemical addition will start as soon as argon cell electromechanical manipulators are functional.

The EADM completed its Phase I qualification at ANL-East and was shipped to ANL-West in December 1993. Remote qualification of this equipment was completed in the ANL-West mockup shop. The equipment has been transferred and installed at Workstation P3

Fig. VI.4. Electrorefiner at Workstation P4 in the Argon Cell
(ANL Neg. No. Y12342)

in the argon cell. Final qualification of this system is waiting for resolution of the overhead handling system problems.

The liquid cadmium cathode assemblies were successfully subjected to Phase I assembly and checkout qualification testing at ANL-East. Before shipping, they also underwent the software and operational portions of the Phase II qualification since remote handling was not necessary and shipping of additional test-support hardware with the assemblies could be avoided. By the end of the reporting period, the remaining remote assembly and maintenance portion of the Phase II qualification was underway in the mockup shop at ANL-West. Room temperature testing of simultaneous functionality of both the linear and rotational drives of the electrode was also nearly completed at ANL-East in a mockup assembly area using water as a surrogate for the electrorefiner salt. Only a 24 h operational endurance test and a static thermal environment test remained to be completed at the end of the reporting period.

During the last quarter of the period, considerable effort was devoted to documenting tasks, including bringing drawings, listing specifications, and analysis packages up to date, and organizing and verifying completeness of the electrorefiner Task Record files in preparation for a close-out audit in support of the facility Operational Readiness Review.

4. Cathode Processor

Progress on the cathode processor continued in FY 1994. Four major milestones were completed. These were:

- a hot characterization test on the prototype cathode processor which reached the design process temperature (1370° C),
- the Phase II qualification tests (out-of-cell remote handling and control system checkout) on the in-cell unit at ANL-West,
- the successful hot characterization testing which simulate the cadmium and salt quantities associated with a solid cathode in the prototype cathode processor, and
- the installation of the in-cell unit into the FCF argon cell and initiation of the Phase III qualification.

The hot characterization testing on the prototype led to a number of hardware and control system software modifications. All of the successful changes were subsequently made to the in-cell control unit. These changes include:

- A modified furnace region with improved thermal performance which enabled the cathode processor to reach the design temperature of 1370° C.

- Modified coil power feedthroughs and instrument feedthrough to improve the vacuum boundary and meet the vessel leak tightness criteria after extended hot operations.
- An instrument with new well material which both seals the furnace liner during hot operations and survives the hot operations associated with processing cadmium and salt.
- A modified vacuum pump to solve problem of repeated failures.
- A modified radiation shield assembly to solve drainage and breakage problems.

Testing will continue to determine the operating parameters required to process cadmium and salts in quantities associated with the liquid cathode. This testing will also determine modifications to components in the crucible assembly required to improve drainage and collection of cadmium and salt, and provide an improved interface with in-cell handling and cleaning operations.

The out-of-cell Phase II remote qualification on the in-cell unit resulted in approximately 80 changes to the hardware to facilitate remote handling. None of these changes required major modifications to the equipment. These changes will not affect hot process operations and therefore were not duplicated on the prototype unit.

Significant progress was made on the Phase III qualification, which includes the in-cell remote handling and control system checkout. The hot qualification testing for solid cathode operations on the in-cell unit was initiated, completing an empty run (without processing materials) to 1370° C. Comparison of the results from this run to a similar run on the prototype was excellent. The excellent agreement provides evidence that the approach of developing hardware modifications and operational procedures on the prototype and transferring the successful ones to the in-cell unit can be pursued with confidence.

5. Injection Casting Furnace

The injection casting furnace was declared operational in FY 1994 and successfully cast four depleted uranium-zirconium fuel pin batches. The first two batches were completed in October and November 1993. Typical process operating parameters and process steps are shown in Fig. VI.5. The third and fourth castings were completed and demonstrated the near real time accountability system. The cast fuel slugs from these four batches will be used as the feedstock for the electrorefiner startup operations.

6. Fuel Pin Processor

The pin handling equipment and pin processor remove the quartz mold from the fuel pin, shear it to length, and measure length, weight, diameter, and straightness. The out-of-cell remote qualification and in-cell qualification of the pin handling equipment was completed. The equipment was used to demold the first three depleted uranium-zirconium castings. The fourth batch was being saved to perform an integrated test with the pin processor and element welder.

The pin processor was transferred and installed in the argon cell as shown in Fig. VI.6. Functional testing was completed in spring of 1994 when the decision was made to utilize this crew on electrorefiner tasks. Current plans do not require this equipment to operate.

7. Fuel Element Welder

The fuel element utilizes a XYZ robot and rotational actuator to fill the open cladding with a xenon tag gas, insert an end plug, weld the end plug, and to visually inspect the resultant weld. During the reporting period, the tag gas system was qualified and characterized. The entire element welder was transferred and installed in the argon cell as shown in Fig. VI.7. Empty cladding pieces were welded to demonstrate the welder's functionality in the cell. With the elimination of the need for fuel element fabrication, activities on the welder have ceased.

Fig. VI.5. Casting Furnace Depleted Uranium Casting No. 2 (November 21, 1993)

Fig. VI.6. Pin Processor Transferred and Installed in the Argon Cell
(ANL Neg. No. W12934)

Fig. VI.7. Element Welder Transferred and Installed in the Argon Cell
(ANL Neg. No. Y12346)

8. Fuel Element Settler

The element settler heats the finished elements to 500° C and uses an impacting force to settle the fuel slug to the bottom of the cladding and to develop a good sodium bond. A series of tests were performed out-of-cell to finalize the process operating parameters. The final operating sequence heats the elements to 150° C, impacts the element twice to settle the fuel slug, heats the elements to 500° C, and impacts the elements 500 times to produce a good sodium bond. These conditions were used to produce 34 depleted uranium-zirconium fuel elements to qualify this equipment. The equipment has been transferred into the argon cell and is currently on hold.

9. Fuel Element Inspection

The element inspection equipment verifies that the fuel slug is positioned properly at the bottom of the cladding and measures the sodium height above the slug to verify an adequate sodium bond has been made. The X-ray unit with the element positioning device was fabricated and qualified. The shielding that protects the X-ray equipment from the in-cell radiation sources was designed and fabricated. The equipment is ready for remote qualification.

C. Process Control and Accountability

The FCF is implementing a distributed process control system that will be integrated together to provide a process control, process monitoring, data archival, and material tracking system. All the systems are being tested with their respective hardware systems as they are activated.

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

The operator control stations (OCS) provide the principal operator interfaces to the facility systems, process equipment, and mass tracking computer. The programmable logic controller (PLC) interfaces with the field instrumentation, converts the signals to a digital signal, and provides the direct machine control.

The software for the facility system except the radioactive liquid waste system and hot repair facility was installed and is undergoing testing. The stack monitor and electrical power systems completed all phases of testing and have been declared operational. The software for the other facility systems was undergoing final independent software testing at the end of FY 1994. The software for the radioactive liquid waste system and hot repair facility was coded and is waiting for completion of the equipment installation so software testing can begin.

The OCS/PLC software for the process equipment is developed in conjunction with the machine development. The electrolyzer, EADM and cathode processor software was

independently qualified during the fiscal year. At the end of the year, the primary activity for process equipment software was incorporation of the MTG-OCS communication software into the individual system.

2. Mass Tracking (MTG) Software

The mass tracking system has two SUN Sparc station computers that operate under the UNIX operating system. Also, electronic weighing balances were installed in-cell so that new weights are established when material is transferred between containers and weights are confirmed to verify identity when items are transferred.

The major accomplishment for FY 1994 was the completion of a demonstration of the material tracking capabilities for the DOE Safeguards and Security audit. This audit witnessed the mass tracking operations during the third and fourth castings. Successful material inventories, control charts for the balance operations, and calculation of inventory difference were demonstrated. Final operating instructions were drafted, validated, and approved for material accountability. Also, operating and recovery instructions were written for the computer system and its associated databases.

Another auxiliary function of the mass tracking system is archiving process operating data. An optical disk juke box was installed and software was written to archive the individual OCS historical files automatically. Software was coded to retrieve and display the archive files; however, the retrieval system has not been tested. The final archival and retrieval system should allow the viewing of five years of data from the process equipment and facility systems.

D. Preparation for FCF Operation

A study on conformance of the facility design to the Final Safety Analysis Report (FSAR) General Design Criteria was performed and issued. Interfaces between all of the Work Packages were defined. A database of monitored signals was prepared to correlate with TSR's and the

Idaho Air Permit. A DOE audit of the FCF Nuclear Material Control and Accountability Plan was conducted in February 1994. Operations involving depleted uranium casting were observed, including a material balance close-out. No findings were issued by the audit team, but suggestions for minor improvements were given. Some open (i.e., unresolved) items remain pending the start of operations with fissile material. Startup Phases 3 and 4 were combined into a single start-up plan because of program changes directed by DOE. Most of the operating and maintenance procedures were written. Training plans, objectives, and tests were written and formal training of operations and support personnel was nearing completion by the end of October.

E. Preparation for Demonstration Experiments

The "FCF Process Operations Plan for Fuel Fabrication" was issued and covered both startup (depleted uranium and cold plutonium) operations. This document described the designated operations and the data requirements for successfully demonstrating the IFR fuel fabrication processes. With the change in program direction, the work which was outlined in the plan will not be completed; however, the document was issued for reference.

For the refining equipment, an electrorefiner out-of-cell calibration plan, an electrorefiner in-cell volume calibration plan, a chemical loading procedure, and a test plan for hot qualification of the cathode processor (solid cathode) were issued. The electrorefiner out-of-cell calibration plan was performed using water additions at room temperature to characterize the internal volume of the electrorefiner. These calibrations were completed in triplicate as shown in Fig. VI.8. During in-cell volume calibration, small increments of eutectic salt and cadmium will be added while the electrorefiner is at 410°C. The chemical addition plan details the appropriate handling operations to ensure the chemical quality. The test plan for the cathode processor hot qualification details an empty heat-up test and the first test with a simulated solid cathode. The simulated cathode will be made with cadmium, salt, and depleted uranium ingots.

The electrorefiner process chemicals were received and passed their acceptance testing. The eutectic salt, which is hygroscopic, had less than 100 ppm water.

A draft EBR-II spent fuel treatment plan has been prepared. This plan identifies the operating steps and equipment requirements for treating EBR-II fuel.

Fig. VI.8. Standardized Data for the Three Out-of-cell Electrorefiner Volume Calibration Runs (Linear Fit)

VII. LIQUID METAL REACTOR TECHNOLOGY RESEARCH AND DEVELOPMENT

A. Status of EM Pump Insulation Testing

The accelerated aging tests employing the small bar samples and the full-size coil tests have continued through FY 1994. The ovens in which the bar samples and pump coils are being tested are shown in Fig. VII.1. During this period the irradiation of the bar samples in EBR-II and some of the posttest analysis of these bar samples have taken place. The insulation testing in Ovens 3 and 4 had been terminated to accommodate the pretesting of bar samples for the EBR-II irradiations. Also, Oven 2 was shipped to ANL-West and is currently being used in the posttest examination of the EBR-II irradiation bar samples. Hence, only Oven 1 has remained in service for the long-term testing of bar samples.

Fig. VII.1. Ovens for Testing Bar Samples and Pump Coils
(ANL Neg. No. 6813)

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. Extrapolation of these data for the bonded bar samples indicates an expected time-to-failure at 538°C of over 1×10^6 h. Three bar samples failed in Oven 1 during FY 1993. One dry sample failed and two Secon samples failed, leaving two dry and three Secon samples on test after 51,286 h.

High temperature and voltage tests on full-size (23-in. dia.) EM pump coils have continued through FY 1994 to determine both the electrical and mechanical performance of prototypic coils, individually. Four full-size coils were on test at the beginning of the fiscal year. The two coils in Oven 5 previously faulted during FY 1993. Coil 16 which employed a MAP binder failed in Oven 6 on February 22, 1994 after 46,930 h. Coil 20 which employed a Secon binder failed on September 26, 1994 in Oven 8 after 48,880 h. Two coils remain under test in Ovens 7 and 8. Table VII.2 summarizes the test results with the full-size coils. All remaining coils exhibited steady leakage current during this period indicating acceptable electrical performance.

In addition, a six-month accelerated aging test on two prototypic GE-designed inner coils for Stator Segment 3 was performed in Oven 5 during FY 1994. These preliminary tests of Stator Segment 3 prototypic coils proved successful; however, during the testing one of the coils temporarily experienced high leakage currents.

In summary, Ovens 1, 7, and 8 are currently employed in long-term testing. All these tests have about 50,000 total hours. Ovens 3, 4, 5, and 6 are currently not in use. Oven 2 is at ANL-West and is still involved in the posttest examination of the bar samples irradiated in EBR-II.

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

Oven Number	Temp., °C	Continuous Voltage	Number and Types of Samples on Test		Total Hours
			Original	Remaining	
1	680	1500	5 dry and 5 Secon	2 dry and 3 Secon	51,286
2					a
3					b
4					b

^aOven 2 shipped to ANL-West for use in posttest examination of bar samples irradiated in EBR-II.

^bOvens 3 and 4 used for pretesting of bar samples subsequently irradiated in EBR-II.

TABLE VII.2. High Temperature and Voltage Tests on Full-size Coils

Oven Number	Temp., °C	Continuous Voltage	Number and Types of Samples on Test	Total Hours
5	500	1500	Coil B - Secon Coil 19 - Secon	a
6				b
7				48,154
8				49,004 ^c

^aDiscontinued due to coil failure after 35,942 h on February 12, 1993.

^bDiscontinued when Coil 16 (MAP) faulted on February 22, 1994 after 46,930 h.

^cCoil 20 faulted on September 26, 1994 after 48,884 h.

B. Status of EM Pump Bar Sample Irradiations

The objective of this program is to assess and confirm the adequacy of candidate insulation materials to perform the electrical insulating function in the EM Pump at conditions expected in the ALMR environment. The work involves the irradiation of selected EM Pump insulation (bar) samples in a representative neutron flux for a sufficient period of time and then to determine the resulting insulating performance. Exposure of selected insulation materials to a radiation field at elevated temperatures will provide some of the performance data necessary to insure that the insulation meets the physical and electrical requirements of the EM pump being designed for the ALMR.

The testing is comprised of essentially three phases: a pretesting phase, a irradiation phase, and a posttesting phase. The pre- and post-testing phases are designed to establish the effects of radiation on the physical and electrical properties of the insulating materials.

In an attempt to isolate the effects of irradiation there are essentially four groups of samples to be subjected to various combinations of pretesting, irradiation exposure, and post-testing. The four groups of samples are to be tested in the following test combinations:

- Aged, Irradiated/Temperature Tested and Aged
- Aged, Temperature Tested and Aged
- Irradiated/Temperature Tested
- Temperature Tested

The aged tests will subject the samples to an applied voltage in an air atmosphere for a specified time (i.e., up to a maximum of 500 h) at a temperature of 1346°F (730°C). These accelerated aging tests will be performed on some samples before and after irradiation. Some samples, to aid in the determination of the effects of irradiation, are not irradiated but are rather subjected to the same duration (~90 days) and temperature (~1140°F, 616°C) in a nitrogen atmosphere as those samples being irradiated (Temperature Test).

A brief status description of these three program phases is provided in the next three subsections.

1. Preirradiation Test Phase

The purpose of the pretesting is to characterize the insulation material both visually and electrically. These examinations and tests serve as the reference baseline from which the subsequent irradiation induced changes will be determined in the posttesting phase of the program. In addition the preirradiation test phase serves as a screening evaluation process for selecting the most promising insulation materials to be irradiated.

The preirradiation testing consists of the physical and electrical tests needed to benchmark the insulation samples prior to the irradiation phase of the program. These tests are essentially identical to those conducted in the postirradiation test phase and include visual examinations, photographs, physical measurements, voltage/temperature aging tests and electrical tests. The examinations and physical evaluations include determining the weights and dimensions of each bar sample. The electrical tests that were performed included both a direct current resistance test as well as an alternating current leakage current test.

The accelerated aging tests were short duration high temperature and voltage tests performed in an oven. The test setup and conditions are similar to those used in the past for the ALMR EM Pump insulation materials life testing program at ANL-East. The bar samples are placed in an oven in an air atmosphere. A high voltage (e.g., either 600 V AC or 1500 V AC depending on the bar sample type) is applied to the insulation and the leakage to ground current monitored. The temperature for these accelerated aging tests is 1346° F (730° C) with the test duration being 500 h (approximately three weeks) or 350 h (approximately two weeks) depending on the sample type.

As part of the preirradiation test phase several shorter duration (~ 1 day) aging tests were conducted. These tests were essentially conducted to check-out the performance of new combinations of insulating materials. Based on the results a selection of the most promising insulation materials was made for irradiation.

All preirradiation tests and examinations on the bar samples were completed in FY 1994.

2. Irradiation Testing

The irradiation program was conducted in the J-2 thimble of EBR-II. This irradiation facility, called the Nuclear Instrument Test Facility (NITF), is located outside the core in the shield region and is an air-cooled thimble. The 39 selected bar samples are contained in three stainless-steel canisters (13 samples in each canister) with this J-2 thimble test designated

as the NI-10 test. The canisters are filled with nitrogen gas and sealed initially at one atmosphere pressure. The temperature histories of the three canisters are recorded by two thermocouples attached to each canister. The neutron flux at the three canister locations was determined by a neutronic calculation.

The insulation bar samples experienced two EBR-II irradiation run cycles. EBR-II started up for Run No. 165A on July 29, 1993 with reactor criticality reached at 1726 h. The reactor was at full power of 62.5 MW on July 31 at 1742 h. Run No. 165A concluded at 1748 h on October 7 resulting in 66.0 Equivalent Full Power Days (EFPD) of irradiation. The second run (Run No. 166A) began on October 30 and was comprised of two run segments due to a loss of instrument air. The final segment concluded at 0427 h on December 1, 1993. Both segments of Run No. 166A provided an additional 26.34 EFPD. The total EFPD experienced by the insulation bar samples was therefore 92.34 EFPD.

The in-reactor environment that the samples experienced was a function of the location of the individual canister relative to the reactor core. The bottom canister being closest to the core experienced the highest temperature and neutron fluence with the top canister being the farthest from the core the lowest conditions. Temperature histories over the two operating cycles were obtained from thermocouples mounted outside the canister with the neutron fluences determined from calculations using the 92.34 EFPD of operation. The average canister temperatures during the steady state 100% reactor power operation and the neutron fluence values are as follows:

Canister	In-reactor Operation	Temperature	Fluence
Top	92.34 EFPD	830° F (443° C)	3.0×10^{17} n/cm ²
Middle	92.34 EFPD	997° F (536° C)	2.3×10^{18} n/cm ²
Bottom	92.34 EFPD	1137° F (614° C)	1.9×10^{19} n/cm ²

The temperature of the bar samples is estimated to be somewhat higher than the thermocouple reading due to gamma heating in the canister steel and bar sample copper. Temperatures of the bar sample copper is estimated to be 10 to 20°F (5.6 to 11.1°C) above the thermocouple readings as a result of gamma heating. The environmental conditions shown above are close to the target values of 90 EFPD, fluence of between 10^{16} to 10^{18} n/cm², and temperature of 850°F (455°C).

The NI-10 carrier assembly was removed from the J-2 thimble on December 10, 1993 and placed directly into the reactor storage pit. A radiation reading of 25 R was measured about 2 ft from the carrier bottom which indicated some decay time would be required before further work could be performed on the carrier assembly. On April 13, 1994 the lower assembly was disconnected from the upper assembly. The lower carrier was reading 40 to 70 R between 4 to 8 in. from the surface requiring remote handling in the EBR-II pentagon (a remote handling highly shielded area of the EBR-II reactor building which contains a remote manipulator). Although problems were encountered in removing two of the three canister lids, on May 16, 1994 all bar samples had been removed from the canisters and placed in the three transfer canisters to await their postirradiation tests.

In conjunction with the irradiation of the bar samples a temperature test was conducted in an oven at ANL-East. Using an identical fourth canister 13 bar samples were inserted and sealed in a nitrogen atmosphere. This canister was placed in an oven at the highest irradiation canister temperature observed in the EBR-II irradiation (1140°F, 616°C) and was operated at this temperature for the same duration as the irradiation samples (~92.5 days). A comparison of the insulation electrical performance of these samples with that of the irradiated samples should help identify the influence of the irradiation on electrical insulation behavior.

3. Postirradiation Testing

The postirradiation testing phase essentially is a repeat of the series of examinations and tests conducted during the preirradiation phase. The major difference between the two phases of course being the fact that in this testing phase the samples are slightly radioactive. The activity

level of the irradiated bar samples has been measured and the activity levels along with new air quality requirements dictated that the postirradiation tests on the activated samples be performed in the Sodium Components Maintenance Shop (SCMS) at ANL-West. An aging oven was shipped from ANL-East to ANL-West along with other testing equipment required for the postirradiation testing and examination of the insulation bar samples. In order to guarantee continuity between the pre- and post-testing procedures and results the ANL-East technician who performed the pretests was sent to ANL-West to help setup the postirradiation test program.

The postirradiation testing of the insulation bar samples is well underway. All 13 temperature test bar samples have undergone their scheduled oven aging and electrical and physical testing. These unirradiated samples have all been returned to the General Electric Co. for final disposal. Of the 39 irradiated samples the six Toshiba samples have completed their required tests and examinations. The remaining irradiated bar samples are undergoing oven accelerated aging prior to the final electrical resistance and leakage current tests.

C. Wind Effects on RVACS Decay Heat Removal System Performance

1. Introduction

One of the main safety features of current liquid-metal reactor (LMR) designs is the utilization of decay heat removal systems that remove heat by natural convection. The reactor vessel auxiliary cooling system (RVACS) is one of these passive systems. It removes decay heat by natural circulation of air in the gap between the guard vessel and a duct wall surrounding the guard vessel. The reactor heat is released to the atmosphere through multiple stacks. The RVACS performance is a function of the pressure difference between the cooling air-inlet and outlet, of the air density variation along the flow path, and of the pressure loss and heat transfer characteristics of this path. The pressure difference between the air inlet and outlet as well as the RVACS inlet temperature may be affected by wind speed and direction. The recirculation of air in the wake of a stack and the downwards bend of a stack's hot plume by wind may affect its own inlet temperature as well as that of its adjacent stacks. The objective of this work was to analyze

the effects of wind on the performance of the RVACS of an advanced liquid metal reactor (ALMR) design based on the PRISM concept.

To simplify the analysis, pressure distributions around the walls of the RVACS stacks were determined first by treating the stacks as solid closed structures, i.e., no cooling air flow was allowed in the stacks. These pressure distributions were used to qualitatively evaluate the effect of wind speed and direction, and of stack design parameters on the pressure difference between stack inlets and outlet. Then, a more realistic analysis was performed with a configuration where the reactor heat source was simulated and air flow in the stacks was allowed. Wind speeds were varied from 2.25 m/s (5 mph) to 26.8 m/s (60 mph). At the reference ALMR plant site, high winds of near 26.8 m/s (60 mph) are expected about once every four years.¹ To account for the effect of wind direction, three wind directions were analyzed, 0°, 90°, and 180°, in respect to a reference direction. In the more realistic analysis where the heat source was simulated, only two wind speeds, 13.4 m/s and 26.8 m/s, and two wind directions (0° and 90°) were considered. The numerical computations were performed with the computer code COMMIX.²

At the 1992 reference plant site of the PRISM ALMR there are three power blocks, each having three reactor facilities with one reactor per facility. Each reactor facility has four RVACS stacks that serve one reactor. Two plant configurations were considered designated here as PLAN 1 and PLAN 2. The major differences in the RVACS configuration of these two plant layouts are: (a) in PLAN 1 a refueling enclosure (above ground) has been placed on top of the reactor and the RVACS stacks extend over the top of this enclosure, (b) in PLAN 1 the steam generator facility (above ground) is placed next to the refueling enclosure, (c) in PLAN 2 there is no permanent refueling enclosure and the RVACS stacks extend only 6.8 m above ground level, and (d) in PLAN 2 the above ground steam generator facility is located a substantial distance from the reactor facilities.

To reduce computation time to reasonable levels, and because the objective of this work was to capture the major effects of wind on RVACS performance, the plant layouts were simplified. Figures VII.1 and VII.2 show the simplified structures considered in the analysis.

Fig. VII.2. Simplified Configuration PLAN 1

Fig. VII.3. Simplified Configuration PLAN 2

In both configurations, only one power block was considered, and in configuration PLAN 2 the turbine generator facility was treated as having an infinite length.

The first wind direction considered in this analysis is defined by air flow from the stacks towards the steam generator facilities. This wind direction is denoted as the 0° direction. The other two directions that were analyzed are the 90° (vertical to the first direction) and 180° (opposite to the first direction). In the reference stack design, each stack has two cooling air inlets located on the sides that are perpendicular to the 0° and 180° wind directions, and one outlet located at the top of the stack. A four-inlet design, i.e. having one cooling air inlet per stack side, was also analyzed.

2. Wind Effects on RVACS Inlet and Outlet Pressures

a. PLAN 1 Configuration

Table VII.3 shows for a 0° wind direction the average Δp s (Δp = pressure minus static pressure) at the location of stack inlets and outlets for a two-inlet and a four-inlet design. Inlet A is on the upwind stack side, inlet C on the downwind side and inlets B and D are on the other two stack sides with inlet B located 90° counterclockwise from A (see Fig. VII.2). Wind leads to a higher pressure at the upwind inlet of the upwind stack, and lower pressures (some significantly lower) at the other inlets and at the outlet of both stacks. In a two-inlet

TABLE VII.3. Average Δp (Pa) at Stack Inlet and Outlet
Locations for a 0° Wind Direction—PLAN 1

Stack Surface	Stack 1	Stack 2
A	9.07	-4.30
B	-26.92	-13.35
C	-22.58	-14.67
D	-35.11	-18.09
Top	-31.78	-27.20

design, only inlets A and C would be open. In both stacks, the pressure drop at the outlet is larger than that at the inlets. Consequently, wind should not have a negative impact on the driving pressure head of the stack. In a four-inlet design, in stack 2 the pressure drop at the outlet is larger than that at any of the inlets, and the wind effect should be a driving pressure head gain. In stack 1, there is a pressure gain at inlet A, and only inlet D has a slightly higher pressure drop than the outlet. Again, wind should not have a negative impact on the driving pressure head of the stack.

The steam generator facility is only slightly taller than the refueling enclosure. For this reason, the 0° and 180° directions are not significantly different, and for the latter direction, the pressure differences between inlets and outlet were similar to those for the 0° direction.

Table VII.4 shows for the 90° wind direction average Δp s at the location of stack inlets and outlets for the stacks of the two upwind reactor facilities. These results cover the stacks with the most adverse pressure changes. In a two-inlet design, only inlets A and C would be open (see Fig. VII.2). In the upwind stack, the outlet pressure drop is significantly greater than that at both inlets. Therefore, this stack should experience a driving pressure head gain. In stack four, the pressure drop at both inlets is larger than at the outlet. This stack should experience a driving pressure head loss (worst performer). However, in reality, stacks two and

TABLE VII.4. Average Δp (Pa) at Stack Inlets and Outlet for a 90° Wind Direction—PLAN 1

Stack	Inlet				Outlet
	A	B	C	D	
2	-80.53	-63.00	-70.88	48.97	-89.15
4	-49.34	-44.38	-56.30	-55.93	-46.67
6	-43.52	-39.70	-45.73	-44.31	-43.90
8	-34.49	-28.80	-30.14	-33.77	-29.03

four communicate (they serve the same reactor), and most, if not all, the losses of stack four would be balanced by the gains of stack two. In stacks 6 and 8, the inlet pressure drops are about equal to the outlet pressure drop. In these stacks, the wind effect on driving pressure head should be nearly neutral.

For a 90° wind, because of a large pressure gain at the upwind inlet (inlet D), a four-inlet design would significantly improve the RVACS driving pressure head of the first reactor facility (stacks 2 and 4). The effect on the RVACS of the other facilities would be nearly neutral.

In conclusion, in configuration PLAN 1, 0° and 180° degree winds should have a positive effect on the performance of an RVACS with a two-inlet stack design. For a four-inlet stack design, the effect should be positive or nearly neutral. With a 90° wind some stacks would experience gains and some losses, the overall effect should be nearly neutral and a four-inlet design would exhibit a better performance than a two-inlet design.

b. PLAN 2 Configuration

In configuration PLAN 2 (Fig. VII.3), the reactor facilities have no refueling enclosure, each stack extends above ground about 6.8 m, the stack horizontal cross section is larger than in PLAN 1, and the distances between stacks are slightly shorter than in PLAN 1. With this configuration, analyses were performed with wind speeds of 2.25 m/s (5 mph), 4.5 m/s (10 mph), 8.9 m/s (20 mph), 13.4 m/s (30 mph), and 26.8 m/s (60 mph). As would be expected, pressure differences between stack inlets and outlets varied as the square of the wind speed. Results are presented for the two largest speeds, i.e., 13.4 m/s and 26.8 m/s.

Table VII.5 shows average Δp values for an average wind speed of 13.4 m/s and wind directions of 0° and 180°. In a two-inlet design, only inlets A and C would be open. For a 0° wind direction, the upwind stacks 1, 3, and 5 should experience a driving pressure head gain. Wind leads to a significant pressure gain at the upwind inlet, while the pressure drop at the

TABLE VII.5. Average Δp (Pa) at Stack Inlets and Outlet—PLAN 2

Stack	Inlet				Outlet
	A	B	C	D	
0° Wind Direction, 13.4 m/s					
1	45.87	-46.25	-37.62	-40.13	-31.61
2	-14.12	-38.06	-37.77	-36.15	-31.73
3	49.99	-50.23	-38.08	-50.44	-33.89
4	-16.06	-32.51	-29.71	-35.35	-28.20
5	50.32	-51.92	-37.05	-51.48	-33.08
6	-17.10	-35.19	-29.27	-34.04	-27.60
180° Wind Direction, 13.4 m/s					
1	-34.58	-32.35	-25.08	-32.45	-30.81
2	-35.42	-35.95	-38.40	-34.82	-38.30
3	-30.46	-30.75	-27.24	-29.53	-28.64
4	-34.57	-34.49	-38.51	-35.47	-35.75
5	-29.55	-28.93	-28.22	-29.29	-28.25
6	-34.57	-34.49	-38.51	-35.47	-35.31
90° Wind Direction, 13.4 m/s					
2	-35.65	100.40	-35.82	-17.35	-26.01
4	-16.46	12.38	-14.70	-12.89	-10.78
6	-11.56	-7.58	-10.23	-14.62	-8.20
8	-20.18	12.51	-18.92	-16.51	-16.86
10	-15.79	-11.39	-14.09	-19.46	-13.14
12	-30.08	8.43	-26.68	-28.00	-23.52
90° Wind Direction, 26.8 m/s					
2	-180.13	398.90	-180.81	-102.13	-139.16
4	-98.31	24.38	-90.84	-83.10	-74.22
6	-77.41	-60.51	-71.74	-90.45	-63.20
8	-114.13	25.11	-108.73	-98.49	-100.04
10	-95.38	-76.66	-88.14	-111.02	-84.16
12	-156.71	7.86	-142.04	-147.91	-128.51

outlet is nearly equal to that in the downwind inlet. In the other stacks, all inlets as well as the outlet experience pressure drops. However, the outlet pressure drop is nearly equal to the same quantity at the downwind inlet while the pressure drop at the upwind inlet is significantly smaller. In these stacks, wind should either have a nearly zero effect, or should result in a small driving pressure head gain. Similar conclusions should be valid for a four-inlet design, although the driving pressure head gains could be smaller because of a larger pressure drop at the other two inlets (B and D). A 180° wind should have a nearly zero effect on the performance of both stack designs. All inlets experience about the same Δp , which is about equal to the outlet Δp .

Table VII.5 shows also average Δp values for a 90° wind direction, and for wind speeds of 13.4 and 26.8 m/s. In a two-inlet design, in all stacks the pressure drop at both inlets is higher than that at the outlet. This would reduce the stack driving pressure head. This reduction would be higher at higher wind speeds. The worst performer is the most upwind stack (stack 2). For a speed of 26.8 m/s, the difference in Δp values between inlets and outlets is 41 Pa, 42% of the stack's driving pressure head (97 Pa) in the absence of wind.

The situation should improve with a four-inlet design. The Δp value is positive at the upwind faces of stacks 2, 4, 8, and 12, and increasingly positive as we move from 12 to 2 (the most upwind stack). In the latter stack, this Δp is much larger than the absolute Δp value at the other inlets and the outlet. With four inlets, wind should result in a driving pressure head gain in this stack, which was the worst performer in the two-inlet design. In the other stacks, taking into consideration that stacks of the same facility are communicating (2 with 4, 6 with 8, and 10 with 12) the wind effect should be nearly neutral.

In conclusion, in configuration PLAN 2, taking into consideration that the stacks of the same reactor facility communicate, a 0° wind should have a positive effect on the performance of the two-inlet stacks, and a positive or nearly zero effect on the performance of the four-inlet stacks. A 180° wind should have a nearly zero effect on the stacks of both designs. A 90° wind would have a negative impact on the performance of the two-inlet design and a positive or nearly zero impact on the four-inlet design. The difference in performance between

a two-inlet and a four-inlet design seems to be more significant in configuration PLAN 2 than in PLAN 1.

3. Wind Effects on RVACS Flow

In the previous section the effect of wind speed and direction on the performance of the RVACS system was investigated by treating the RVACS stacks as closed structures, i.e., no flow was allowed through the stacks. In this section, a more realistic analysis is presented; the RVACS heat source was simulated and air flow through the stacks was allowed.

To avoid excessive computation times a simplified PLAN 2 configuration was analyzed. Only the stacks of one reactor facility were considered and all other structures above the ground were neglected. As in the previous analyses, no communication was allowed among the stacks. The RVACS heat source was represented by four walls at a constant temperature of 202°C. This is the RVACS air outlet temperature computed in Ref. 3. A very large heat transfer coefficient was used to assure that the air temperature at the outlet of the heated zone is equal to 202°C. In reality, changes in RVACS performance due to wind effects are reflected in guard vessel, sodium, and cladding peak temperature changes. In this analysis, because the air temperature at the outlet of the heated zone is kept constant, the wind effects will be reflected in stack air flow changes.

The analysis presented in the previous section indicated that wind in the 90° direction had the most adverse effect on RVACS performance. Accordingly, only two wind directions were analyzed, 0° (air flow parallel to the axis formed by the stack inlets of a two-inlet stack design) and 90°. Computations were performed with average (vertically) wind velocities of 13.4 m/s, 26.8 m/s and 0.0 m/s. The zero velocity analysis is the reference case of no wind effects.

For a 0° wind direction and a wind speed of 13.4 m/s, the air flow through the upwind stack increased (over the zero wind velocity case) by 8% while that through the downwind stack decreased by 4%. For a 90° wind direction and the same wind speed, in the two-inlet

design the air flow of the upwind stack decreased by 10% and that of the downwind stack by 4%. For the same wind direction, when two more inlets were added per stack (four-inlet design), the air flow of the upwind stack increased by 0.5% while that of the downwind stack decreased by 2%.

Increases in RVACS air flow lead to lower peak core temperatures while decreases in this flow lead to higher peak core temperatures. In a realistic simulation, where the total heat source is constant and the air temperature at the outlet of the RVACS heated zone varies, the flow changes presented above would be lower. For a constant heat source, a reduction in air flow would increase the air outlet temperature, which in turn would increase the air flow. The reverse would occur with an increase in the air flow. For a constant heat source, it was determined that because of this feedback an initial reduction of 10% in the air flow would lead to a final flow reduction of 8%. For the reactor system considered in this work, calculations have shown that a 10% reduction in the RVACS airflow would increase the peak cladding temperature by $\sim 10^{\circ}\text{C}$. Thus, an 8% reduction in the RVACS airflow would increase the peak cladding temperature by $\sim 8^{\circ}\text{C}$.

For the high wind velocity of 26.8 m/s, only the 90° wind direction was analyzed. From the three directions considered, this is the direction that has the most adverse impact on RVACS performance. In the two-inlet design, the high-speed wind reduced the flow of the upwind stack by 25% and that of the downwind stack by 13%. In the four-inlet design, the flow of the upwind stack was increased by 11.4% while that of the downwind stack was reduced by 8.4%. Feedback correction, reduces the 25% flow reduction in the upwind stack of the two-inlet design to 19%. That is, for a constant heat source the wind effect on the upwind stack of the two-inlet design would be a 19% flow reduction. Because in reality the stacks of the same reactor facility are communicating, in the two-inlet design, the net wind effect on RVACS air flow would be a reduction of about 15%, while in the four-inlet design the net effect would be nearly zero. A 15% reduction in the RVACS airflow would increase the peak cladding temperature by $\sim 15^{\circ}\text{C}$. Since 90° is the wind direction that has the most adverse impact on RVACS performance, and since in reality the wind direction fluctuates around an average direction, the most adverse wind effect should be less than 15°C .

4. Summary and Conclusions

The simplified analysis (no air flow through the stacks) indicated that wind in the 90° direction had the most adverse effect on RVACS performance. For this reason, in the more realistic analysis, where the heat source was simulated and air flow was allowed through the stacks, only 0° and 90° winds were considered. This more realistic analysis confirmed the previous conclusion that the 90° wind has the most adverse effect on RVACS performance, and in configuration PLAN 2 a four-inlet design would perform better than a two-inlet design. In a two-inlet design, for a 0° wind direction and a wind speed of 13.4 m/s, the air flow through the upwind stack increased (over the zero wind velocity case) by 8% while that through the downwind stack decreased by 4%. For a 90° wind direction and the same wind speed, the air flow of the upwind stack decreased by 8% and that of the downwind stack by 4%. When two more inlets were added per stack (four-inlet design), the air flow of the upwind stack increased by 0.5% while that of the downwind stack decreased by 2%. In a two-inlet design, the net effect on air flow of a 90° and 26.8 m/s wind would be a reduction of about 15%, while in a four-inlet design the net effect would be nearly zero. A 15% reduction in the RVACS airflow would increase the peak cladding temperature by $\sim 15^{\circ}\text{C}$. Since 90° is the wind direction that has the most adverse impact on RVACS performance, and since in reality the wind direction fluctuates around an average direction, the most adverse wind effect should be less than 15°C .

The inlets of the downwind stacks are thermally affected by the outflow of the upwind stacks. However, the effect is small. For an air temperature rise of 164°C across the RVACS flow path, the maximum inlet temperature rise is about 5°C . This would raise the peak cladding temperature by $\sim 1^{\circ}\text{C}$.

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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 1994 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, "Status of Progress in IFR Development," ASME Joint Intl. Power Generation Conf., **94-JPG-NE-14**:1-3, Phoenix, AZ (October 2-4, 1994).
2. C. E. Till, "Energy Over the Centuries: The IFR Option," Managing the Plutonium Surplus: Applications and Options Conf., 111-116, The Royal Institute of Intl. Affairs, London, England (January 24-25, 1994).

Metal Fuel Performance

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2. S. L. Hayes and G. L. Hofman, "Constituent Redistribution Modeling in U-Zr Fast Reactor Fuel," Trans. Am. Nucl. Soc., **71**:179-180, Washington, D.C. (November 13-17, 1994).
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1. J. P. Ackerman, T. R. Johnson, and J. J. Laidler, "Waste Removal in Pyrochemical Fuel Processing for the Integral Fast Reactor," Actinide Processing, Methods and Materials, Eds. B. Mishra and W. A. Averill, The Minerals, Metals and Materials Soc., 261-266, Warrendale, PA (1994).
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