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**FAST FLUX TEST FACILITY
QUARTERLY TECHNICAL REPORT
APRIL, MAY, JUNE 1967**

JANUARY 1969

**AEC RESEARCH &
DEVELOPMENT REPORT**

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UC-80, Reactor
Technology

FAST FLUX TEST FACILITY
QUARTERLY TECHNICAL REPORT
APRIL, MAY, JUNE 1967

By
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Compiled by C. P. Cabell

January 1969

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E. R. Astley

ABSTRACT

Efforts during the reporting period were directed toward the achievement of a more comprehensive conceptual design of the Split Core Concept. Seven conceptual variations of the reactor were devised. Conceptual designs were developed for structures and containment, heat removal, fuel handling, irradiation testing, and fuel examination. Studies and development supporting the conceptual design were made in core and systems analysis, in safeguards, physics, fuels, corrosion, chemistry, and metallurgy.

At the close of the reporting period, conceptual design was continuing, and comprehensive evaluation of the various conceptual alternatives had been initiated.

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18

19

CONTENTS

LIST OF FIGURES.	viii
LIST OF TABLES	xi
INTRODUCTION.	xiii
I. DESIGN AND CONSTRUCTION ENGINEERING	1.1
A. Electrical	1.1
1. Lighting.	1.1
B. Structures, Utilities, and Containment	1.1
C. Reactor Design	1.1
1. Concepts for Reactor.	1.1
D. Fuel Handling and Radioactive Maintenance	1.1
1. Radioactive Maintenance Cell Complex	1.1
E. Heat Removal.	1.7
1. Steady-State Process Design and Cost Optimization	1.7
2. FFTF Heat Transport System.	1.8
F. Irradiation Test Facilities.	1.8
1. Closed Loops	1.8
G. Fuel Examination Facilities.	1.12
1. Inert Gas Cell Examination Facilities	1.12
H. Sodium and Inert Gas Receiving and Processing Systems	1.15
1. Criticality Control	1.15
2. Inert Gas Selection	1.15
II. NUCLEAR AND SYSTEMS ENGINEERING.	2.1
A. Core and Systems Analysis	2.1
1. Core Design.	2.1
2. Core Pressure Drop	2.16
3. FFTF Digital Simulation.	2.20
4. Analysis of Loss-of-Flow Accident for Control Rod Assembly.	2.23
5. Preliminary Accident Analysis Studies	2.26
6. Hydraulics	2.31

7.	Reduced Pressure Drop Fuel Pin Bundle Design	2.33
8.	Air Dump Heat Exchanger Modeling	2.36
B.	Reactor Safeguards	2.37
1.	Control and Safety Rod Worth	2.37
2.	Scram Response Requirements.	2.37
3.	Core Damage Severity Criteria	2.39
C.	Instrumentation and Control	2.41
1.	In-Reactor Coolant Temperature Sensor Irradiation Effects	2.41
2.	Reactor Environmental Effects on Signal Cables	2.41
3.	Fuel Failure Monitor System.	2.41
4.	Neutron Sensor Environment Study	2.42
D.	Physics.	2.42
1.	Analysis of Split-Cone FTR Neutronics	2.42
2.	Considerations on the FTR Safety Rod Control System	2.47
3.	Analysis of ZPR Fission Rates and Doppler Coefficients	2.53
4.	Analysis of ZPR Spectrum Measurements	2.57
5.	FTR Phase A Critical Experiments: Analysis	2.61
6.	FTR Phase B Critical Experiments - Technical Bases.	2.62
7.	Cross Section Analyses with Optical Potentials	2.64
8.	FCC-IV.	2.65
9.	Protram ETOE.	2.68
10.	Program MC ²	2.68
11.	Shielding Code Conversions	2.69
12.	FTR Shield Studies.	2.69
13.	Large Component Cask Shielding Requirements.	2.75
E.	Fast Reactor Fuels	2.77
1.	Hardware	2.77
2.	Fuel Pin Design.	2.79

3.	Fuel Pin and Subassembly Design and Irradiation Requirements	2.80
4.	Fuel Pin Thermal Analysis	2.80
5.	Effect of BeO Addition on FFTF Flux and Fuel Subassembly Design	2.82
6.	Heat Generation in Axial Reflectors	2.85
7.	Fuel Swelling	2.88
8.	Fabrication Tolerances for the Annular Gap Between FFTF Driver Fuel Spacer Wire and Flow Duct	2.90
9.	Mixed Oxide Fuel Fabrication	2.90
10.	Mixed Oxide Fuel Irradiations	2.91
11.	MTR-ETR O/M Study Irradiation Capsules	2.94
12.	Irradiation Testing of Cermet Pins	2.94
13.	Restrictor Screening Capsule Irradiations	2.96
14.	MTR-ETR O/M Study Irradiation Capsules	2.103
III.	DEVELOPMENT	3.1
A.	FFTF Corrosion and Chemistry	3.1
1.	Analysis	3.1
2.	Contaminated Systems Study	3.4
3.	Sodium Cleaning and Decontamination Evaluation	3.5
B.	Mechanical Metallurgy	3.5
1.	Alloy Selection	3.5
2.	In-Reactor Measurements of Mechanical Properties	3.6
3.	Stainless Steels	3.8
4.	Nickel-Base Alloys	3.10
5.	Intercalibration Test of Counting Laboratories	3.14
6.	Fast Reactor Structural Materials and Fuel Cladding Studies	3.16
7.	Test Facilities	3.18
8.	Fast Neutron Mechanisms	3.20
9.	Liquid Metal Capsule Irradiations	3.22
10.	Thin Section Material	3.22

LIST OF FIGURES

1A.1	Schematic Diagram of Alternating Current Power Supply to Lighting System	1.2
1B.1	Building Plan XVI Longitudinal Section	1.3
1B.2	Building Plan XVI at Elevation 0 ft 0 in.	1.4
1B.3	Building Plan XVI at Elevation +23 ft 0 in.	1.5
1D.1	Maintenance and Decontamination Flow Sequence	1.6
1E.1	Reactor Heat Transport System Sections	1.9
1F.1	Closed Loop Piping 5 MW Loop Arrangement No. IV	1.10
1F.2	Closed Loop Re-entrant Tube Arrangement	1.11
1F.3	Closed Loop U-Tube Arrangement	1.13
1G.1	Inert Gas Examination System	1.14
2A.1	Allowable Tubesheet Exposure as a Function of Distance from the Core and Pin Reflector Thickness	2.3
2A.2	Split Core Reactor: Concept I	2.4
2A.3	PNL Split Core: Concept II	2.5
2A.4	PNL Split Core: Concept III	2.6
2A.5	PNL Split Core: Concept IV	2.7
2A.6	PNL Split Core: Concept V	2.8
2A.7	PNL Split Core: Concept VI	2.9
2A.8	PNL Split Core: Concept VII	2.10
2A.9	Cover Plan for Concept VII	2.11
2A.10	Reflector Details of Core Assembly	2.15
2A.11	Various Methods for Reducing Pressure Drops	2.16
2A.12	Hex Tubes for Conical Core FTR	2.18
2A.13	Hex Tubes for Conical Core	2.19
2A.14	Relative Flux as Function of Axial Position for Three Radial Positions	2.21
2A.15	Heat Transfer Coefficient for Flow Through Unbaffled Rod Bundles	2.22
2A.16	Model of Control Rod Assembly	2.24
2A.17	Temperatures in Control Rod Assembly for Complete Loss of Sodium Flow	2.25
2A.18	Temperature Transients for Reactivity Insertions of 3\$/sec and 6\$/sec	2.27

2A.19	Transient Response for 4\$/sec Ramp, 200 msec Trip, and 1.0 G Rod Acceleration	2.28
2A.20	Transient Response for 2\$/sec Ramp, 400 msec Trip, and 0.5 G Rod Acceleration	2.29
2A.21	Reactivity Feedback Caused by Doppler Effect for 2\$/sec Ramp, 400 msec Trip, and 0.5 G Rod Acceleration	2.30
2A.22	Friction Factor for Seven-Pin Bundle	2.31
2A.23	Pressure Drop for Seven-Pin Bundle with Liquid Sodium	2.32
2A.24	Low Pressure Drop Fuel Subassembly Design	2.34
2A.25	Possible Fission Gas Plenum Arrangement for Even Flow Distribution	2.35
2A.26	Arrangement of Fission Gas Plenum for Open Flow	2.35
2B.1	Maximum Transient "Overpower" Versus Reactivity Ramp and Scram Acceleration	2.37
2B.2	Maximum Transient "Overpower" Versus Reactivity Ramp and Scram Acceleration	2.38
2B.3	Maximum Transient "Overpower" Versus Reactivity Ramp and Scram Acceleration	2.38
2D.1	FTR Split Core Configuration	2.43
2D.2	Total Flux Versus Transverse Position	2.44
2D.3	Center Point Flux Versus Central Gap Volume	2.45
2D.4	Total ^{239}Pu Critical Mass Versus Central Gap Volume	2.46
2D.5	Schematic of FTR Split Core in R-Z Geometry	2.48
2D.6	Assumed Safety Rod Control Worth	2.50
2D.7	Results for 2\$/sec and 4\$/sec Accident Conditions	2.51
2D.8	Effect of Changing the Doppler Coefficient	2.54
2D.9	^{239}Pu Radial Fission Rate in SEFOR	2.55
2D.10	^{239}Pu Axial Fission Rate in SEFOR	2.55
2D.11	^{238}U Radial Fission Rate in SEFOR	2.56
2D.12	^{238}U Axial Fission Rate in SEFOR	2.56
2D.13	Central Neutron Spectrum of ZPR-6 Assembly 4-Z	2.58
2D.14	Central Neutron Spectrum of ZPR-3 Assembly 48	2.59
2D.15	Central Neutron Spectrum of ZPR-6 Assembly 4-Z	2.60
2D.16	Short-Range Correlation Functions	2.66

2D.17	Single-Particle Wave Functions for Neutron-Nucleus Scattering	2.67
2D.18	Split Core Reactor	2.70
2D.19	Heat Generation Rates in Shroud and Vessel	2.71
2D.20	Heat Generation in Tubesheet	2.72
2E.1	Materials Sample Basket, Seven Rod Cluster Flow Tube and Loop Adapter for Sodium Flow Test	2.77
2E.2	Seven Rod Cluster After 4 Weeks Sodium Flow	2.78
2E.3	Bottom End of Seven Rod Sodium Flow Test Cluster	2.78
2E.4	Effect of Sintering Temperature Variation	2.79
2E.5	Cladding Temperature Profile--Touching Pins	2.83
2E.6	Effect of BeO Addition on FFTF Flux and Fuel Design	2.84
2E.7	Heat Generation from Nickel Reflectors in Fuel	2.86
2E.8	Heat Generation in Nickel	2.86
2E.9	Temperature Profiles in Nickel Reflectors (top) and Adjacent Clad	2.87
2E.10	VH-4 Restrictor Screening Capsules	2.92
2E.11	Mixed Oxide Restrictor Screening Capsule	2.93
2E.12	MTR-ETR O/M Studies Capsule	2.95
2E.13	Microstructure of Preirradiated SS-PuO ₂ Cermet Pin	2.97
2E.14	Photomicrograph from Cermet 5-P-13	2.98
2E.15	Photomicrograph from Cermet Pin 5-P-14	2.99
2E.16	Transverse Section from Irradiated SS-20 vol% PuO ₂ Cermet Pin 5-P-13	2.100
2E.17	Transverse Section from Irradiated SS-30 vol% PuO ₂ Cermet Pin 5-P-14	2.101
2E.18	Macrographs, β - γ Autoradiographs and α Autoradiographs from BNW-1-36	2.102
2E.19	MTR-ETR O/M Study Irradiation Capsule	2.104

LIST OF TABLES

1H.I	Inert Gas Selections	1.16
2A.I	Reference Split Core Comparisons Concepts I, II, III and IV	2.12
2A.II	Split Core Concept Comparisons	2.13
2A.III	Concept VII Data	2.14
2A.IV	Pressure Drops for the Subassembly Design	2.17
2A.V	Heat Generation Rates	2.23
2B.I	Core Damage Severity Criteria	2.40
2D.I	Reactor Compositions	2.43
2D.II	Doppler Coefficients in ZPR-III Assemblies 47 and 48	2.57
2D.III	Comparison of Rod Calculations	2.61
2D.IV	Doppler Tdk/dT as a Function of Core Configuration	2.64
2D.V	Damage Flux Incident on Reactor Components	2.73
2D.VI	Neutron and Gamma Intensity at Sensor Location Due to Reactor Fission Events	2.74
2E.I	Fast Reactor Fuel Pin Data	2.81
2E.II	Alternate Fuel Parameters for Irradiation Testing	2.89
2E.III	Restrictor Information	2.91
3B.I	Comparison of Measurements at Various Sites	3.15
3B.II	Neutron Flux Calculated for MTR VH-2 Facility at Reactor Midplane	3.16

FAST FLUX TEST FACILITY
QUARTERLY TECHNICAL REPORT
APRIL, MAY, JUNE 1967

INTRODUCTION

This report, prepared by Battelle-Northwest under Contract No. AT(45-1)-1830 for the Atomic Energy Commission, Division of Reactor Development and Technology, describes technical progress on the Fast Flux Test Facility program during April, May, and June 1967. Monthly progress reports issued during the reporting period constitute the sources of information reported herein.

Activities reported for the subject period are arranged under three major headings as follows:

Chapter I, Design and Construction Engineering;
Chapter II, Nuclear and Systems Engineering; and
Chapter III, Development.

I. DESIGN AND CONSTRUCTION ENGINEERING

W. B. McDonald

A. ELECTRICAL

G. H. Strong

1. Lighting

A. L. Vosmer

It is currently expected that 440/277 V circuits will be used for the FFTF Lighting System. A schematic diagram is given in Figure 1A.1.

B. STRUCTURES, UTILITIES, AND CONTAINMENT

F. H. Shadel

Preliminary plans were issued for Building Arrangement Plan XVI (Figures 1B.1 through 1B.3). This arrangement houses the following systems and components which contain primary sodium within a 200 ft diam containment sphere: reactor, reactor hot cell, primary heat transport system equipment cells, primary system cleanup, six closed loop cells, irradiated fuel storage, and contaminated sodium storage.

C. REACTOR DESIGN

L. M. Finch

1. Concepts for Reactor

L. M. Finch

Seven conceptual variations of the split core design were developed for study. Details are given in Chapter II, Section A.

D. FUEL HANDLING AND RADIOACTIVE MAINTENANCE

H. D. Lenkersdorfer

1. Radioactive Maintenance Cell Complex

D. J. Lagrou and G. H. Salzano

The conceptual design of the Radioactive Maintenance System shows, in Figure 1D.1, a typical component flow sequence from

1.3

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FIGURE 1B.1. Building Plan XVI Longitudinal Section

FIGURE 1B.3. Building Plan XVI at Elevation +23 ft 0 in.

1.5

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FIGURE 1D.1. Maintenance and Decontamination Flow Sequence

the installed position to the radioactive maintenance cell complex. The cells are as follows:

- Component transfer cell: connects the reactor hot cell, containment work area, and the high bay maintenance area outside containment; transports large components such as primary pump, IHX, or FHM internals from one area to another.
- Component decontamination cell: receives large components from the upper bay area via a large component handling cask; decontaminates by spraying, flushing, and/or recirculating decontaminant, or by other methods.
- Remote maintenance cell: provides maintenance of components which cannot be decontaminated or are too highly radioactive for contact or glove box operations; includes control rod poison section replacement, primary pump repair, closed loop component repair, etc.; receives components from the high bay area via a large component handling cask.
- Contact maintenance cell: used for alpha or plutonium contamination control including containment; receives components from the high bay area via a large component handling cask.

E. HEAT REMOVAL

J. H. Westsik

1. Steady-State Process Design and Cost Optimization

H. G. Johnson

Process and cost data comparisons were made for the heat removal system considering air and steam/water heat dump concepts, number of parallel cooling circuits, and 1200 °F core coolant bulk outlet temperature capability achieved through core bypassing. These areas were compared on the basis of capital investment, operating cost, steady-state process performance, system reliability and availability. Conclusions were as follows:

- The air heat dump concept is superior in all respects to a steam/water heat dump concept.
- Cooling circuit redundancy is required to achieve acceptable system reliability and availability.
- Redundancy is costly in as much as the incremental capital investment is several million dollars for redundancy at the system design power of 400 MW.
- The 1200 °F core coolant bulk outlet temperature capability is also costly. The capability costs would require several million dollars to provide redundancy for operation at that temperature.

2. FFTF Heat Transport System

L. G. Henke

Conceptual drawings of the Reactor Heat Transport System were prepared depicting the piping and equipment arrangement for the very limited installation space in the proposed 200 ft containment sphere. Figure 1E.1 shows an arrangement utilizing two primary heat transport equipment rooms separated by 180 degrees. This arrangement is not optimum, and studies are continuing.

F. IRRADIATION TEST FACILITIES

L. M. Finch

1. Closed Loops

D. R. Doman

a. Cell Layout Studies

A closed loop cell equipment layout has been prepared (Figure 1F.1). This arrangement provides containment for the primary system components and storage vessels, and includes shielding of hot and cold traps.

b. Closed Loop Tube Layouts for Split Core Reactor

Figure 1F.2 presents design features of a closed loop re-entrant tube. A partial plan view shows the general top face

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 1 of 2

FIGURE 1E.1. Reactor Heat Transport System Sections

UPPER PLAN

LOWER PLAN

SECTION A-A

SECTION B-B

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FIGURE 1F.1. Closed Loop Piping 5 MW Loop Arrangement No. IV

FIGURE 1F.2. Closed Loop Re-entrant Tube Arrangement

arrangement and illustrates the problems in leading piping across the top face. Neutron damage or fuel meltdown may require the removal of the pressure tube. In this respect, Detail I shows an internal arrangement that allows removal of the pressure tube while maintaining nozzle piping connections.

Elevation M-M shows two possible methods for attaching piping to the nozzle spool pieces. One method incorporates a remote cut and weld arrangement which allows piping and spool pieces to be handled separately. The alternate method is a direct attachment in which the piping must be removed with the spool piece, or cut to accommodate removal. Details II and III indicate the problems involved with a restricted flow area and testing space below the reactor core because of the tapered tubes.

Figure 1F.3 shows two closed loop U-tube concepts. The partial plan views of the reactor reveal the tightness of the connections at the top face and indicate that U-tubes can be installed in only the center positions in the row of five vertical closed loops. Loops in the center position would require inlet flow to be split in half ("A" Assembly) in order to fit into the configuration, and the U-tubes with inlet piping ("C" Assembly) to be removed together as one assembly.

G. FUEL EXAMINATION FACILITIES

C. L. Boyd

1. Inert Gas Cell Examination Facility

D. J. Meyers and C. L. Boyd

An inert gas cell arrangement (Figure 1G.1), providing additional deep cell space for disassembly and handling of long sub-assemblies, is under consideration. The inert gas cell is an in-line cell bank divided into three distinct working areas. A deep cell area provides space for cleaning, storage, disassembly, reassembly, and interim examination of 35 to 40 ft long fuel and material subassemblies. The remainder of the cell bank consists of an equipment storage and transfer area and a fuel and material specimen examination area.

FIGURE 1F.3. Closed Loop U-Tube Arrangement

PLAN

B ←

SECTION A-A

SECTION B-B

SECTION C-C

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FIGURE 1G.1. Inert Gas Examination System

The cell has 5-ft thick high-density concrete walls with stainless steel line and sealed cell penetrations. The arrangement provides remotely changeable absolute filters, mercury vapor, and fluorescent lights throughout the cell bank.

The arrangement places operating stations on opposing walls at three levels in the deep cell area and at one level in the remainder of the cell bank. Viewing windows are oil filled-lead glass type with alpha seal plates, and manipulators are the sealed type. Width of the cell bank permits simultaneous operations at opposing in-cell work stations and provides sufficient space in the center of the cell for material and equipment handling.

H. SODIUM AND INERT GAS RECEIVING AND PROCESSING SYSTEMS

R. V. Dulin

1. Criticality Control

The potential for criticality in the cold traps will depend on the size and shape of the traps and the maximum amount of fuel that could credibly reach the traps. An initial review indicates that the high critical mass in an unmoderated system (>100 kg) and the nonoptimum geometry of the traps will preclude a criticality hazard. Further substantiating reviews will be made at later stages of design.

2. Inert Gas Selection

A study of inert gas selection for FFTF applications has resulted in the tentative selections shown in Table 1H.1. These selections are based on operating with vented or some failed fuel, a piped heat removal system, and a closed reactor cover with a hot cell over the reactor. A firm choice between helium or nitrogen for reactor cavity cooling requires review of bases of selection for like applications for other reactors as well as review of the most promising conceptual arrangements of the neutron shield and heat removal systems.

TABLE 1H.I. *Inert Gas Selections*

Reactor primary system	Helium (alternative: Argon)
Closed loop primary system	Helium (alternative: Argon)
Reactor secondary system	Argon
Closed loop secondary system	Argon
Fuel decay storage	Helium (alternative: Argon)
Fuel handling machine	Helium or Argon as required by reactor or closed loop
Fresh sodium storage	Helium (alternative: Argon)
Contaminated sodium dump tank	Helium
Fuel transfer cell	Argon
Fuel examination cells	Argon
Reactor cavity atmosphere	Nitrogen or Helium
Short-term irradiation facility	Helium
NaK system (if required)	Argon (alternative: Nitrogen)
Closed loop cells	Nitrogen
Reactor primary equipment cells	Nitrogen
Reactor hot cell	Nitrogen (alternative: Argon)
Maintenance and decontamina- tion cell	Nitrogen

II. NUCLEAR AND SYSTEMS ENGINEERING

D. L. Condotta

A. CORE AND SYSTEMS ANALYSIS

R. E. Peterson

1. Core Design

G. R. Waymire, L. M. Finch, and R. E. Peterson

a. Design Criteria

Following the decision to utilize the split conical core as the FFTF reference design,⁽¹⁾ further studies of geometry have been undertaken. Basic design criteria include the following:

- Provide adequate spacing at the lower tubesheet to fulfill structural and thermal-hydraulic requirements.
- Maintain tight packing and maximum pin density in the core region.
- Maintain a concentric single design fuel subassembly if possible.
- Allow adequate nozzle spacing to accommodate the FHM (probably 10 to 12 in. spacing).
- Allow adequate piping chaseways around the central closed loops (probably 16 to 18 in. minimum between center lines).
- Maintain a tube length of less than 40 ft.
- Design so that total core may be removed.
- Maintain a fuel pin outside diameter of 0.250 in.
- Maintain a fuel lattice P/D of 1.15 to 1.25.

Configuration considerations included both hexagonal and cylindrical cross sections. The cylinder appears, with the conical core, to offer little benefit from a core design standpoint. Differences in manufacturability with hexagonal and cylindrical stock are negligible. The hexagonal tube, therefore, appears to offer the best cross section.

1. BNWL-501, *Fast Flux Test Facility, Quarterly Progress Report, January, February, March 1967.*

b. Tubesheet Position

One of the parameters affecting geometry is the position of the lower tubesheet with relation to the core area. The limiting factor is the allowable fluence level on the structure.

A study was made to determine the position-time relation involved. The results, assuming an allowable fluence of 10^{21} neutrons/cm² and a plant factor of 0.6, are shown in Figure 2A.1 for different end reflector thicknesses.⁽¹⁾ These curves may be slightly conservative due to higher sodium and lower iron fractions assumed in the reflector. Based on these curves, a minimum distance of 54 in. was chosen giving a 3 to 4 yr life with an 8 in. reflector.

c. Fuel Pin Lattice

With the pin criteria stated earlier, pin arrays for various configurations were calculated. The arrays were:

4.5 in. hexagonal subassembly	217 pins
4.5 in. cylindrical subassembly	199 pins
4.0 in. hexagonal subassembly	169 pins

These arrays utilized a 0.125 in. wall for hexagonal subassemblies and a 0.080 in. wall for cylindrical subassemblies.

d. Core Geometries

To date, seven core layouts have been completed (Figures 2A.2 through 2A.9). Tables 2A.1 through 2A.III list parameters and variations thus far determined.

e. Reflector

To maintain a cylindrical vessel, the stationary radial reflector has been designed as a transition piece from the conic core geometry to the cylindrical vessel. The transition reflector is made up of two sets of wedged-shaped segments with the load carried through a weldment into the lower tubesheet (Figure 2A.10).

1. *Fluxes based on data supplied by C. A. Mansius, Reactor Physics.*

FIGURE 2A.1. Allowable Tubesheet Exposure as a Function of Distance from the Core and Pin Reflector Thickness

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FIGURE 2A.2. Split Core Reactor: Concept I

FIGURE 2A.5. PNL Split Core: Concept IV

Neg 0671900-1

FIGURE 2A.6. PNL Split Core: Concept V

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FIGURE 2A.7. PNL Split Core: Concept VI

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FIGURE 2A.8. PNL Split Core: Concept VII

2.11

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FIGURE 2A.9. Cover Plan for Concept VII

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TABLE 2A.I. *Reference Split Core Comparisons*
Concepts I, II, III and IV

<u>Description</u>	<u>Total Posi- tions</u>	<u>Driver Posi- tions</u>	<u>Closed Loops, in.</u>	<u>Subas- sembly Size, in.</u>	<u>Total Volume, liter</u>	<u>Closed Test Volume, liter</u>	<u>Driver Volume, liter</u>	<u>H/D</u>
<u>CONCEPT I*</u>								
Figure 2A.2	90	76	1-6 2-5 2-7.5	4.5 hex	1235	260	975	0.720
<u>CONCEPT II*</u>								
Figure 2A.3	86	72	5-6	4 hex	1075	263	812	0.862
<u>CONCEPT III</u>								
Figure 2A.4	84	70	4-4 1-6	4 hex	1148	208	940	0.80
<u>CONCEPT IV</u>								
Figure 2A.5	74	60	5-6	4.5 hex	1257	181	1076	0.745

<u>Description</u>	<u>H. in.</u>	<u>Fuel</u>	<u>Void</u>	<u>Iron</u>	<u>Active Sodium</u>	<u>Other Sodium</u>	<u>Core Bottom to Nozzle, ft</u>	<u>Nozzle Spacing, in.</u>	<u>Distance to Tubesheet, ft</u>
<u>CONCEPT I*</u>									
Figure 2A.2	34	0.241	0.054	0.229	0.271	0.216	35	12	5
<u>CONCEPT II*</u>									
Figure 2A.3	36	0.228	0.039	0.224	0.237	0.269	35.5	11	4.5
<u>CONCEPT III</u>									
Figure 2A.4	36	0.237	0.042	0.237	0.251	0.231	35.5	11	4.5
<u>CONCEPT IV</u>									
Figure 2A.5	36	0.202	0.035	0.202	0.237	0.321	30.5	10 min	4.5

* Closed loop volume for Concept IV was calculated on the basis of width between closest center driver positions. If the distance to the symmetrically packed area less driver volume is considered, the closed volume becomes 394 liters.

NOTE: Concepts I to III include 8 control rod positions.
 Concept IV includes 6 control rod positions.

2.12

BNWL-541

TABLE 2A,II. Split Core Concept Comparisons

<u>Description</u>	<u>Concept V</u>	<u>Concept VI</u>
Total Positions	72	80
Driver Positions	58	66
Control Positions	10	10
Closed Loop, in.	5 each, 6 in.	1 each, 6 in.; 2 each, 5.7 in.; 2 each, 5.0 in.
Subassembly Size, in.	4.5 hex	4.336 hex
Total Volume, liters	1012	937
Closed Test Volume, liters	205	122
Driver Volume, liters	807	815
H/D	0.792	0.788
H, in.	34	34
Fuel	0.233	0.228
Void	0.041	0.044
Iron	0.226	0.236
Active Sodium	0.267	0.337
Other Sodium	0.232	0.156
Core Bottom to Nozzle, ft	30.5	30.5
Nozzle Spacing, in.	10	10
Distribution to Tubesheet from Core Bottom, in.	54	54
Remarks	Number of driv- ers, low; total volume, high	Minimum closed volume with driver volume near 800 liters

TABLE 2A.III. *Concept VII Data*

Description	Value
Total Positions	83
Driver Positions	69
Control Positions	10
In-Core Open Loops	4
Closed Loops, in.	1 each, 6 in.; 2 each, 5.7 in.; 2 each, 5.0 in.
Subassembly Size, in.	4.2 in. hexagonal
Total Volume, liters	901
Split Area Volume, liters	101
Driver Area Volume, liters	800
H/D	0.796
H, in.	34
Fuel	0.244
Void	0.043
Stainless	0.267
Active Sodium	0.302
Other Sodium	0.144
Estimated Average Flux	
@ 400 MW:	0.78×10^{16}
Estimated Peak Flux	
@ 400 MW:	0.81×10^{16}
	Assumes no core zoning
Weight of ^{239}Pu , kg	510
Driver Fuel Power, MW	400
Nozzle Spacing	15% Increase/in. of Spacing Increase
Assembly Length	2% Increase/ft Decrease in Height
Split Area	18% Increase/in. of Width Increase

NOTE

1. CORE LATTICE SPACING BASICALLY SIMILAR TO DIMENSIONS SHOWN ON DETAIL (NUMBER OF SUB-ASSEMBLIES ALTERED AS SHOWN BELOW)

2. FOLLOWING IS QUANTITY AND TYPE OF VARIOUS CHANNELS:

- (A) 3 - VERTICAL CLOSED LOOPS
- (B) 10 - DRIVER FUEL AND OPEN LOOPS
- (C) 10 - INDOOR CONTROL RODS (W/ MECHANICAL HOLD-DOWN AND PRE-LOAD FEATURE)
- (D) 8 - REFLECTOR CONTROL RODS (W/ INDOOR HOLD-DOWN AND PRE-LOAD FEATURE)
- (E) 58 - REMOVABLE REFLECTORS
- (F) 6 - REMOVABLE REFLECTOR TUBES (W/ MECHANICAL HOLD-DOWN AND PRE-LOAD FEATURE)

157 TOTAL		BY DATE	CHK	BY DATE	CHK	DESCRIPTION
REV NO	01	DATE	01/10/60	DATE	01/10/60	DESCRIPTION
REFERENCE DRAWINGS		CONNECTION PPT. ISSUE NO.		REVISIONS		DATE
NEXT USED ON		DRAWING STATUS				

Neg 0671900-3

FIGURE 2A.10. Reflector Details of Core Assembly

2.15

BNWL-541

2. Core Pressure Drop

M. T. Jakub

a. Methods of Reducing Pressure Drop

Pressure drop across the FFTF core is estimated to be near 20 psi per ft. The fuel and fission gas plenum length required can result in a total core pressure drop of 100 psi or higher. Several methods for alleviating this condition, including enlarging the hydraulic diameter of the flow channel immediately above the active zone and altering the hydraulic diameter of the fuel subassembly, were investigated (Figure 2A.11).

The basic element was set at 217 wire-wrapped pins and 4.12 in. flat-to-flat dimensions to permit a comparison of the 1a, 1b, and 1c designs. Table 2A.IV presents the results.

FIGURE 2A.11. Various Methods for Reducing Pressure Drops

TABLE 2A.IV. *Pressure Drops for the Subassembly Design (400 MW, 300 °F = core delta-T, 70 driver subassemblies)*

<u>Design</u>	<u>P/D</u>	<u>Pin OD, in.</u>	<u>Wire Spacer OD, in.</u>	<u>Δp_T psi</u>	<u>% Fuel + Clad, %</u>
1a	1.145	0.240	0.035	135	66.8
1a	1.16	0.237	0.038	112	63.7
1b*	1.06	0.261	0.015*	92	78.9
1b	1.08	0.256	0.020	81	76.0
1b	1.10	0.251	0.025	74	73.0
1b	1.12	0.246	0.030	69	70.0
1c	1.145	0.240	0.035	125	66.8
1c	1.16	0.237	0.038	105	63.7

* P/D and Spacer at inlet

b. Internal Pressure Loads

By using a materials design curve for 304 SS and the elastic stress formulae in BNWL-263, the required wall thickness for the hexagonal duct can be calculated as a function of pressure drop across the wall and temperature. Figures 2A.12 and 2A.13 show the results for two different duct diameters. These curves apply to a fixed inside corner radius of 0.15 in., the maximum radius for pins approximately 0.25 in. in diameter. Any larger radius would require the deletion of six corner pins in the array.

FIGURE 2A.12. Hex Tubes for Conical Core FTR (material: 304 SS; inside corner radius: 0.15 in.; inside flat-to-flat dimension on hex tube: 4.08 in., 1200 °F)

FIGURE 2A.13. Hex Tubes for Conical Core (Material: 304 SS; inside corner radius: 0.15 in.; inside flat-to-flat dimension on hex tube: 3.59 in.)

3. FFTF Digital Simulation

C. D. Flowers and H. G. Johnson

a. Reactor Model

The core zone thermal model has been completed and is presently being debugged. The model incorporates four parallel regions through the active fuel zone. They are fuel, coolant in tubes, leakage coolant outside of tubes, and radial reflector coolant. Each of these regions has been divided into three equal-length axial nodes for simulating the core zone heat balance. Temperature dependence of coolant and fuel physical properties is accounted for in the model. Fuel temperature calculations are based on a chopped sine axial power level distribution.

b. Axial Flux Distribution

Axial flux distribution data for an 800 liter oxide core, obtained from the 2DF Code,⁽¹⁾ were used to determine the power level constants for the chopped sine distribution. Figure 2A.14 shows the relative flux as a function of axial position for three radial positions of interest. The volume averaged flux, occurring at approximately 0.68R, was used to obtain the required fuel heat generation constants.

c. Heat Transfer Coefficient

The overall heat transfer coefficient from fuel to coolant is calculated as a function of temperature and flow in the program. For the present model, fuel and coolant resistances are neglected, and lumped (or point) fuel and coolant temperatures are calculated. Results of computations are given in Figure 2A.15.

1. R. W. Hardie. Unpublished Code. Pacific Northwest Laboratory, Richland, Washington. (Personal Communication)

FIGURE 2A.14. *Relative Flux as Function of Axial Position for Three Radial Positions*

FIGURE 2A.15. Heat Transfer Coefficient for Flow Through Unbaffled Rod Bundles ($\Delta = \text{Pitch}, P/D = 1.2$)

4. Analysis of Loss-of-Flow Accident for Control Rod Assembly A. Padilla and G. Dickerson

Calculations were performed to determine the steady-state temperatures in a control rod assembly for a complete loss of sodium flow. It was assumed that the heat generated within the assembly was transferred to the outside of the hexagonal tube by conduction alone. Melting or boiling was not considered.

Figure 2A.16 shows the geometry of the control rod assembly and the model used for the analysis. Table 2A.V shows the heat generation rates used.

Calculations were carried out for the maximum heat generation rates in Table 2A.V and an assumed sodium temperature of 1000 °F flowing on the outside of the hexagonal tube. The heat transfer across the inside of the poison pin occurred by both conduction and thermal radiation. However, the model used for radiation was only approximate because each node along the inside diameter of the pin radiated to most but not all of the other nodes (Figure 2A.17).

TABLE 2A.V. Heat Generation Rates

	<u>Average</u>	<u>Maximum*</u>
304 SST	15 W/g	18.6 W/g
Sodium	15 W/g	18.6 W/g
Boron Carbide	71 W/cm ³	88 W/cm ³

* Assuming an axial peaking factor of 1.24.

Maximum temperatures attained by the stainless steel, sodium, and boron carbide are 4775, 4292, and 3799 °F, respectively. Therefore, it is concluded that the central stainless steel support rod will melt or the stagnant sodium will boil before steady-state conditions are achieved.

1.10 in.diam Structural Rod

Control Rod Assembly

Stainless Steel Type 304

Boron Carbide

Sodium

Model for Analysis

FIGURE 2A.16. Model of Control Rod Assembly

FIGURE 2A.17. *Temperatures in Control Rod Assembly
for Complete Loss of Sodium Flow*

5. Preliminary Accident Analysis Studies

A. Padilla

Preliminary studies of reactivity insertion without scram were carried out to determine transient fuel pin and coolant temperatures. The NUTIGER Code, which allows for reactivity feedbacks from Doppler, fuel expansion, and coolant density changes was used for the analysis. The effect of sintering on fuel temperatures was not considered. Parameters were:

Pin OD	0.25 in.
Cladding Thickness	0.016 in.
Fuel Cladding Gap Coefficient	1500 Btu/hr-ft ² -°F
Sodium Film Coefficient	45,000 Btu/hr-ft ² -°F
Axial Peaking Factor	1.24
Peak Linear Power	8.7 kW/ft
Steady-State Coolant ΔT	400 °F
Doppler Coefficient, $T \frac{dK}{dT}$	-0.004
Ramp Rates	3\$/sec and 6\$/sec

Figure 2A.18 shows the results of the accident analysis studies. Significant fuel melting occurs before any appreciable interaction with the coolant. For the lower ramp of 3\$/sec, the average fuel temperature is above 6000 °F (mp 4850 °F) before the coolant reaches its boiling point in about 0.95 sec.

Figure 2A.19 shows the transient fuel temperature response to a 4\$/sec reactivity ramp above for the hottest fuel pin and an average fuel pin. Figure 2A.20 gives the transient response of the average and hottest fuel pins to a 2\$/sec reactivity ramp with a 400 msec time delay before scram and a rod acceleration of 0.5 gravity. Figure 2A.21 shows that the Doppler feedbacks of the average and hottest fuel pins to the 2\$/sec ramp are almost identical.

FIGURE 2A.18. Temperature Transients for Reactivity Insertions of 3\$/sec and 6\$/sec

FIGURE 2A.19. Transient Response for 4\$/sec Ramp,
200 msec Trip, and 1.0 G Rod Acceleration

FIGURE 2A.20. Transient Response for 2\$/sec Ramp, 400 msec Trip, and 0.5 G Rod Acceleration

FIGURE 2A.21. Reactivity Feedback Caused by Doppler Effect for 2\$/sec Ramp, 400 msec Trip, and 0.5 G Rod Acceleration

6. Hydraulics

Laboratory measurements of the pressure-drop flow behavior of a seven-pin model of a segment of the proposed FFTF driver fuel assembly have been completed. Testing was performed in water at temperatures from 47 to 223 °F (10 to 106 °C) at flow-rates from 7 to 45 gal/min.

The data from the tests were used to develop a friction factor versus Reynolds number curve (Figure 2A.22). Also shown in Figure 2A.22 is the friction factor curve for a smooth tube. The difference between the curves is due primarily to the wire wraps, i.e., the wire wraps increase the pressure drop by 30 to 35%. Entrance and exit losses were found to be negligible.

FIGURE 2A.22. Friction Factor for Seven-Pin Bundle

Pressure drops were calculated for liquid sodium by using friction factors from Figure 2A.22. The calculations plotted in Figure 2A.23 give the pressure drop for 800 to 1200 °F sodium.

FIGURE 2A.23. *Pressure Drop for Seven-Pin Bundle with Liquid Sodium*

7. Reduced Pressure Drop Fuel Pin Bundle Design

C. D. Flowers and M. T. Jakub

In the present FTR pin bundle design, the fission gas plenums are hollow pin extensions located on one end of the fuel bundle. This design creates a long and compact pin bundle which results in a large coolant pressure drop through the core. A fuel design for reducing the large pressure drop while still providing the necessary fission gas plenums was investigated.

The large pressure drop through the long pin bundle can be significantly reduced by arranging the pins so that part of the fission gas plenums are located on one end of the active fuel zone and the remaining fission gas plenums are located on the opposite end of the active fuel zone. Figure 2A.24 shows a typical application of the proposed gas plenum arrangement.

To illustrate the ΔP reduction obtained by this design, two different pin bundles employing the proposed arrangement were analyzed. As an example, a 3.9 in., across-the-flats, hexagonal flow region with highly turbulent sodium coolant and circular fuel pins is assumed. The subassembly is taken to be 169 circular pins with 0.25 in. OD, and each pin is wire wrapped to space the pins 0.040 in. apart.

Assume for Case 1 an evenly distributed arrangement of fission gas plenum. Also assume that half of the pins are reversed in a pattern similar to those in Figure 2A.25. The total length of the fission gas plenum, although doubled for the coolant, remains the same for each pin. Total pressure drop equals 84.7 psi.

Assume for Case 2 a more open arrangement gas plenums, and that half of the pins are reversed in a pattern similar to those in Figure 2A.26. Total pressure drop equals 78.4 psi.

FIGURE 2A.24. Low Pressure Drop Fuel Subassembly Design

FIGURE 2A.25. *Possible Fission Gas Plenum Arrangement for Even Flow Distribution*

FIGURE 2A.26. *Arrangement of Fission Gas Plenum for Open Flow*

The sample calculations show the pressure drop for the opposite-end gas plenum regions to be approximately 85 to 95% less than the pressure drop for the gas plenums when all are located on the same end. Since the pressure drop through the active fuel region is unchanged, the overall pressure drop of the entire bundle is decreased approximately 30 to 40% by employing the proposed pin bundle arrangement.

8. Air Dump Heat Exchanger Modeling

H. G. Johnson

Crossflow and counter-current type heat exchanger models were constructed for the air dump heat exchangers. The crossflow model is quite detailed and is considered to be as exact as can be reasonably expected. It is considered to be the reference model for comparison with others.

The crossflow model features four tube passes with an egg-crate type nodal arrangement in which tube-side coolant conditions are determined at three points in each pass and shell-side coolant conditions are determined at the crossflow inlet and outlet for each tube side point. The tube side coolant flow is in the X direction, with shell-side coolant flowing in the Y direction. The X direction is positive in the direction opposite to the tube-side coolant flow direction. Air is not mixed between tube passes, and variations in the air weight flow rate with distance are neglected. Central and forward differences are used to approximate the $\partial T / \partial X$ terms for the tube-side coolant at the interior and downstream exterior points. Heat transfer is neglected in the return bends between passes. An arithmetic average shell-side coolant temperature is used at each tube-side point for use in the heat transfer driving force ($T_t - \bar{T}_s$).

The metal tube and fins are assumed to be at the same temperature as the tube side coolant. The counter-current models, constructed with four nodes, used less exact techniques.

B. REACTOR SAFEGUARDS

D. E. Simpson

1. Control and Safety Rod Worth

D. E. Simpson

A review of considerations pertinent to criteria for maximum individual safety and control rod reactivity worth has been prepared and is being reviewed internally.

2. Scram Response Requirements

D. E. Simpson

Estimates of the maximum "overpower" factor (ratio of maximum fuel energy content to the initial steady-state energy content) in a scram-terminated ramp reactivity insertion transient are shown in Figures 2B.1, 2B.2, and 2B.3 for different values of safety rod acceleration, time delay before scram, and Doppler coefficient.

FIGURE 2B.1. Maximum Transient "Overpower" Versus Reactivity Ramp and Scram Acceleration

FIGURE 2B.2. Maximum Transient "Overpower" Versus Reactivity Ramp and Scram Acceleration

FIGURE 2B.3. Maximum Transient "Overpower" Versus Reactivity Ramp and Scram Acceleration

3. Core Damage Severity Criteria

D. E. Simpson and W. E. Roake

Preliminary criteria defining levels of severity of accidental damage to fuel subassemblies in the FTR are suggested. The following levels of damage severity are proposed:

- Negligible damage: conditions the fuel pins and subassemblies can sustain several times without significant reduction in burnup capability or increase in coolant radionuclide contamination. This level corresponds to no loss of fuel or equipment, and a minor loss of operating time; significant degrading of test data is possible.
- Moderate damage: conditions the fuel pins and subassemblies can sustain without loss of integrity but requiring replacement of affected subassemblies immediately, or at significantly reduced burnup; fission gas release might occur due to localized cladding failure, but significant release of fissionable material would not occur. This level corresponds to a loss of part or all of a core load of fuel, probable loss of test data, and a time loss for major reloading.
- Gross failure: conditions the fuel subassembly can sustain without disruption of the core geometry, but with release of fission products and fissionable material. This level corresponds to loss of a complete core, loss of tests and test data, and a major outage for plant cleanup and reloading.
- Core meltdown or disruption: conditions producing a possibility of progressive or autocatalytic core compaction, up to the limits of the DBA. This level corresponds, from loss of time and equipment, up to permanent loss of the facility.

Note that the limits of "gross failure" define the circumstances beyond which autocatalytic progression of an accident must be considered possible and therefore assumed. Below these limits, such progression cannot occur.

Table 2B.I defines the maximum values of fuel pin operating parameters tentatively established as upper limits for each level of severity. These values are necessarily conservative due to the lack of currently available experimental data. One of the major objectives of the fuel development experimental program is to define these limits more accurately in terms of fuel damage and fuel failure thresholds, and for fuel behavior after failure possibly leading to autocatalytic accident progression.

The attached limits apply only to the initial mixed oxide core for FFTF. The values of the various parameters are not necessarily consistent for a given accident. Exceeding the limit for any of the parameters would constitute exceeding a given severity level.

TABLE 2B.I. Core Damage Severity Criteria

Parameter	Upper Limit for Maximum Fuel Pins			Core Melt-down or Disruption
	Negligible Damage	Moderate Damage	Gross Failure	
Maximum fuel temperature	$>T_{\text{melt}}$	T_{melt}	$<T_{\text{vapor@ 1 atm}}$	DBA
Maximum molten fuel radius	0	$\sqrt{0.1} R_f^{(1)}$	$\sqrt{0.5} R_f^{(1)}$	↓
Maximum cladding temperature ⁽²⁾				
Inner surface	1300 °F	1600 °F	1800 °F	
Outer surface	1200 °F	1400 °F	1600 °F	
Maximum cladding plastic strain	0	$\geq 0.2\%^{(3)}$	$>> 0.2\%$	↓

(1) R_f = radius of fuel pin

(2) Short duration; approximately 1 sec

(3) Based on total end-of-life strain limit of 0.2%. Thus a total strain equivalent to 0.2% would require fuel replacement

C. INSTRUMENTATION AND CONTROL

C. D. Swanson

1. In-Reactor Coolant Temperature Sensor Irradiation Effects

J. L. Jackson and C. E. Huck

Two Geminol thermocouples, one Pt-Rh-10% Pt, and one W-26% Re-W being irradiated in EBR-II failed from open circuits during calibration. All failed thermocouples were sheathed with Type 304 SS and used aluminum oxide insulators, but none used compacted type insulation presently considered for future specifications. Bare wire Geminol thermocouples with beaded aluminum oxide insulators were substituted for all thermocouples that failed.

2. Reactor Environmental Effects on Signal Cables

G. J. Dau

Refinements of test apparatus resulted in no measureable calibration shift of chromel-alumel thermocouples at 230 °C in a photon flux of 2.3×10^7 R/hr. The previously observed shift was apparently due to inadequate heat transfer from the thermocouple bead to the constant temperature bath.

3. Fuel Failure Monitor System

R. K. Bollinger

A combination of two fuel rupture monitor systems techniques is proposed: (a) delayed neutron monitoring for fission debris, and (b) monitoring for fission gas products. The dual approach is necessary because of differing capabilities and limitations inherent in the two techniques. Delayed neutron monitoring can have a very short detection delay time but can be used only when an appreciable neutron flux exists within the reactor core. Conversely, gas monitoring is applicable irrespective of the neutron flux level but the detection time is 10 to 30 times longer than for the delayed neutron technique.

4. Neutron Sensor Environment Study

R. R. Cone

A preliminary study of the neutron and gamma environment which may exist at a typical neutron sensor location in the neutron shield was conducted for reactor full power and shutdown conditions.

The Shielding Analysis Unit calculated the nuclear environment for one of the split cone concepts (a few hours after shutdown) as follows:

Thermal neutron flux	1 nv
Gamma field	10^4 R/hr

Hence, neutron test facilities for testing intermediate range instrumentation should be capable of providing a gamma background of at least 10^5 R/hr and about 1000 nv (thermal) during reactor startup conditions in order to allow for design uncertainties and errors from calculation techniques.

D. PHYSICS

P. L. Hofmann

1. Analysis of Split-Cone FTR Neutronics

L. L. Maas

Analysis of the neutronics characteristics of the FTR split-cone configuration has been started. Major emphasis has been placed on analyzing the effects of the longitudinal gap and the conical tilt angle.

a. Effect of Longitudinal Gap on FTR Neutronics

Several possible configurations for the FTR split core concept have been investigated. Figure 2D.1 shows the X-Y geometrical representation used for the two-dimensional transport theory calculations. Reactor compositions representative of those at the axial core midplane were utilized in averaging cross sections for each region in the reactor. The cross section data were generated by the FCC-IV code.

The five closed loops in the central gap region were assumed to contain a representative FBR fuel ($^{238}\text{U}/^{239}\text{Pu} = 5/1$). The closed loops and associated sodium and structure were homogenized into the center gap region.

FIGURE 2D.1. FTR Split Core Configuration

Table 2D.1 gives the compositions used for each region in the calculations. The fuel contains 85/15 wt% $^{239}\text{Pu}/^{240}\text{Pu}$. The center gap and reflector compositions were fixed for all cases, while PuO_2 and UO_2 fractions in the driver fuel were adjusted for criticality ($k_{\text{eff}} = 1.04$).

TABLE 2D.1. Reactor Compositions

	<u>Center Gap</u>	<u>Driver</u>	<u>Reflector</u>
vol% Fuel	6.5	28.0	-
vol% Void	1.0	5.0	-
vol% 304 SS	18.0	25.0	-
vol% Na	75.5	42.0	20.0
vol% Inconel	-	-	80.0

Figure 2D.2 is a plot of the total flux as a function of position in the Y-direction for 50, 150, and 250 liter central gaps in an 800 liter driver region. The flux dips in the center gap region because the fuel is much less concentrated in that region than in the driver region.

The center point flux versus the center gap volume for 600, 800, and 1000 liter driver regions is shown in Figure 2D.3. A power level of 400 MW is assumed for all cases. Note that a center point flux of $0.7 \times 10^{16} \frac{n}{cm^2 \cdot sec}$ might be attained by a 600 liter driver region with ~ 200 liter center gap, an 800 liter driver and ~ 125 liter center gap, or a 1000 liter driver with ~ 85 liter center gap.

Figure 2D.4 shows the variation in ^{239}Pu mass as a function of center gap volume for three driver volumes. The total ^{239}Pu mass includes both the center gap region and the driver region.

FIGURE 2D.2. Total Flux Versus Transverse Position

FIGURE 2D.3. Center Point Flux Versus Central Gap Volume

FIGURE 2D.4. Total ^{239}Pu Critical Mass Versus Central Gap Volume

b. Effect of Conical Tilt on FTR Neutronics

To assess the effect of the conical core tilt on the neutronics calculations, an r-z representation of the FTR was used.

A schematic of the calculational model is shown in Figure 2D.5. In this model a 100 liter central region representing the central closed test loops is surrounded by an 800 liter driver region. Between the driver region and the radial reflector is a 3.64-cm-thick annular region of sodium and stainless steel. The core region was divided into five sections, all composed of the same fuel ($\text{PuO}_2\text{-}^{238}\text{UO}_2$) and the same outer radius for the base r-z calculation. In the second case, the outer radius of the two top core regions was increased and the bottom two decreased to represent a 5.25 degree tilt angle, approximately maximum for an outside driver element in the FTR split core concept. The amount of fuel and stainless steel was maintained in all of the driver regions even though the volumes changed.

The relative total flux and fission rate as a function of axial position for the vertical core (r-z) and for the tilted conical core have been compared and show little change. There was a +0.001 change in k_{eff} in going from the vertical (r-z) core to the tilted core. On the basis of these calculations, it appears that a vertical (r-z) representation of the present FTR core is adequate for neutronics calculations.

2. Considerations on the FTR Safety Rod Control System

A. E. Waltar

Major parameters of the safety rod control system for the FTR yet to be fixed include

- The downward accelerating force on the rods (G-values),

FIGURE 2D.5. Schematic of FTR Split Core in R-Z Geometry

- The total time delay, T , between the start of an accident and the release of the rods, and
- The initial (cocked) position of the rods.

Initial studies concerning the shutdown capabilities of the FTR safety rod control system have been directed towards achieving simple, closed-form expressions for the first two items.

A PNL-II worth curve for a 2% rod (6.67%) was established by slightly modifying a perturbation code run to account for flux depression (Figure 2D.6). Reactivity versus time curves were then graphically generated by folding the worth curve into a standard acceleration curve.

Figure 2D.7 contains the principal results. The bottom cluster of curves corresponds to an initiating accident of 2\$/sec and the upper cluster results from an accident of 4\$/sec. Each curve is labeled as to the G-value of the rods, and the number in parenthesis indicates the total time delay, T . The broad horizontal line at about 2300 °K is included for generally indicating when, at some place in the core, melting might begin. However, extreme caution should be exercised when attempting to use this limit in any absolute sense. The K-III kinetics code used for this analysis will handle only one dimension for an average pin. Hence, the temperatures as plotted correspond to the centerline temperature of an average pin (averaged radially and axially). The 300 °K indicated rise in temperature is currently only an estimate as to the average pin centerline temperature corresponding to a melting temperature at the hottest point in the reactor. The determination of this absolute value must await an analysis from a 2-D kinetics code.

FIGURE 2D.6. Assumed Safety Rod Control Worth

FIGURE 2D.7. Results for 2\$/sec and 4\$/sec Accident Conditions

In any event, some general observations can immediately be made. Clearly the most effective parameter in the safety rod system contributing to a successful arrest of an accident is the total delay time, T . Even rods falling less than 1-G (free fall with viscous damping) are quite adequate for the accidents considered when total time delay is of sufficiently short duration. However, if total delay times of about 200 msec or so are unavoidable, higher G rods would certainly be desirable. Most of the curves represent the rods initially positioned with the tips 10 cm above the core top. The effect of moving the rods down to the edge and then 10 cm into the core is illustrated in the center of the figure (4\$ ramp, 1g rods, dropped 200 msec following initiation of the accident). The lower time-to-peak temperatures and the magnitude of the peak temperatures are the direct result of placing the rods in a position such that they are moving up a steeper part of the worth curve when dropped. Although not indicated on the graph, it might be pointed out that total rod movement (to reach peak temperature) was, in all cases, quite small. The maximum rod movement in any of the situations considered was only about 20 cm. This could be important from the standpoint of using short cocking springs, etc.

Thus far, only the total time, T , from the start of the accident to dropping of the rods, has been considered. This time, however, is comprised mainly of two segments: 1) the time required to reach scram conditions, t_s , and 2) the electronic delay time, t_d , required to then release the rods. Hence, by way of illustration, with an over-power factor of 1.2 (condition necessary for detection of the accident) and a ramp rate of 4\$/sec, the actual electronic delay time, t_d , allowable for the curve in Figure 2D.7 labeled 2.0G(200) is only 158 msec.

Another parameter pertinent to the total transient description, although not directly a part of the safety rod system, is the reactor Doppler coefficient. All studies illustrated in Figure 2D.7. assumed a nominal Doppler coefficient, K_D , of -0.004. To note the effect of changing the feedback reactivity resulting from Doppler broadening, two cases were rerun using a K_D of -0.002 and -0.008. The runs selected were the 1.0g(200) case from the 4\$ ramp and the 0.5g(400) case from the 2\$ ramp. These were chosen since they represented cases slightly above failure (assuming the upper fuel temperature limit to be valid). Figure 2D.8 contains these results. Although the total time to peak temperature is not appreciably altered for different values of K_D , it is clear that increasing the magnitude of the Doppler coefficient reduces the peak temperatures attained.

3. Analysis of ZPR Fission Rates and Doppler Coefficients

L. L. Maas

Radial and axial fission rate distribution calculations for ZPR-3, Assembly 47 (SEFOR), and Doppler coefficient calculations for Assembly 47 and 48 (P-1) were completed. Assemblies 47 and 48 have been selected because they are similar, in many ways, to the FTR.

For the calculation of fission rate distributions shown in Figures 2D.9 through 2D.12, transport theory calculations for regular and adjoint fluxes in 26-energy groups were performed in the radial and axial dimensions. The ^{238}U fission distributions shown in Figures 2D.11 and 2D.12 exhibit good agreement between measured and calculated fission rate distributions. The calculated ^{239}Pu fission rate, however, does not agree well with experiment. The source of this discrepancy is being studied.

FIGURE 2D.8. Effect of Changing the Doppler Coefficient

FIGURE 2D.9. ^{239}Pu Radial Fission Rate in SEFOR

FIGURE 2D.10. ^{239}Pu Axial Fission Rate in SEFOR

FIGURE 2D.11. ^{238}U Radial Fission Rate in SEFOR

FIGURE 2D.12. ^{238}U Axial Fission Rate in SEFOR

Table 2D.II gives the results of the Doppler coefficient calculations for SEFOR and P-1. For these calculations, the fast cross section calculation code FCC was used to generate 26 energy group cross sections at 300 °K and 2100 °K. These were then used in a transport theory calculation to obtain $T \frac{dk}{dT}$ from which the δk for the measured temperature change was calculated. Corrections for crystalline binding are included in the calculated values.

TABLE 2D.II. *Doppler Coefficients in ZPR-III Assemblies 47 and 48*

	Assembly 47 (SEFOR) $(T \frac{dk}{dT} / \text{kg } ^{238}\text{U})$	Assembly 48 (P-1) $\delta k/k$
Calculated	-2.38×10^{-5}	-4.96×10^{-6}
Measured	-2.17×10^{-5}	-4.89×10^{-6}

4. Analysis of ZPR Spectrum Measurements

R. W. Hardie

The central neutron spectra in two ZPR critical assembly cores were calculated and compared with recent measurements by E. F. Bennett of ANL using a proton-recoil proportional counter. All calculations were done with the FCC-IV fundamental mode code using a modified version of the Russian cross section set.

Figure 2D.13 shows the calculated and experimental spectra for ZPR-3 Assembly 48, a 420- ℓ ^{239}Pu fueled core. The spectra in ZPR-6 Assembly 42, a 1100- ℓ ^{235}U fueled core, was measured twice because of improvements in the technique. The first measurement is shown in Figure 2D.14 and the second in Figure 2D.15.

FIGURE 2D.13. Central Neutron Spectrum of ZPR-6 Assembly 4-Z

FIGURE 2D.14. Central Neutron Spectrum of ZPR-3 Assembly 48

FIGURE 2D.15. Central Neutron Spectrum of ZPR-6 Assembly 4-Z

In general, the agreement between calculation and measurement is quite good, except in the low keV region where the experiment is known to have larger errors.

5. FTR Phase A Critical Experiments: Analysis

R. A. Bennett and S. L. Engstrom

a. Reflector Control Rod Worth Calculations

The 3-D, 2 group, diffusion code WHIRLAWAY has been employed to assess axial reflector effects on the rod worth of two reflector rods located in positions P3 and P23 of ZPR-3, Assembly 48A.

To obtain the rod worths, 3-D calculations for the rod-in and rod-out case were performed and compared with comparable 2-D, WHIRLAWAY runs. In the 2-D runs, the axial buckling for the rod-out case was adjusted to give the same (approximately) k_{eff} as the 3-D rod-out case. The same axial buckling was then used in the 2-D rod-in WHIRLAWAY case.

In the following Table, 2D.III, the results of these calculations are shown. In addition, more elaborate 2DF five group transport calculations were also performed, and the results are tabulated.

TABLE 2D.III. *Comparison of Rod Calculations*

<u>Case Description</u>	<u>Code</u>	<u>k_{eff}</u>	<u>Rod Worth (% $\Delta k/k$)</u>
Core 48A no Control Rods	2DF	0.98294	
Core 48A with Control Rods	2DF	0.97474	0.834
Core 48A no Control Rods	3-D WHIRLAWAY	0.98963	
Core 48A with Control Rods	3-D WHIRLAWAY	0.98024	0.949
Core 48A no Control Rods	2-D WHIRLAWAY	0.98160	
Core 48A with Control Rods	2-D WHIRLAWAY	0.97264	0.913

If one were to adjust the 2-D transport worth of 0.834% $\Delta k/k$ by means of the 3-D result, the predicted rod worth would be $\sim 0.866\%$ $\Delta k/k$, about 5% higher than the reported measured value of 0.821% $\Delta k/k$. One possible source of this discrepancy is an overestimate in the ^{10}B absorption cross section values used in these calculations. This discrepancy is being evaluated.

b. Spatial Reaction Rate Calculations

Previous calculations of ^{10}B spatial reaction rates have shown fairly large discrepancies in comparison with experimental results. Additional calculations are now under way to resolve these discrepancies.

The ^{239}Pu fission rate calculations are in substantial agreement, thus suggesting that the calculated flux spectra are reasonably correct. Further, the ^{239}Pu fission rate, like the ^{10}B absorption rate, shows a significant contribution from the thermal and near thermal energy groups. This finding further confirms that, if the calculated ^{239}Pu fission rate is correct, then the entire calculated flux spectrum is correct.

In searching for other reasons in the discrepancy, a reevaluation of the Bondarenko ^{10}B absorption cross sections is being made. Use of more recent data yields a somewhat better agreement with experiment.

6. FTR Phase B Critical Experiments - Technical Bases

R. A. Bennett

Uncertainties in the calculated values of nuclear parameters of moderately sized fast reactors and the effect of these uncertainties on the nuclear design of the FTR are being assessed to provide technical justification for the performance of FTR critical experiments. Evaluations of the uncertainties in the FTR Doppler coefficient are in progress.

The magnitude of the negative Doppler coefficient is an important consideration in the safety analysis of the FTR. Since the FTR is relatively small, and since the split-cone driver fuel arrangement leads to rather unfavorable fuel distributions, a relatively high enrichment fuel results and the Doppler coefficient tends to be small. Suppose that for some typical fertile-to-fissile fuel ratios in the FTR, Tdk/dT is about -0.004. The real FTR Doppler coefficient may be less than this value, since it can be affected adversely by test fuel and control rod patterns.

For the clean, initial rod-free case, the Tdk/dT value was established at -0.004. This is obviously an idealized reactor model which, because of the absence of rods, has a $k_{eff} > 1$. Assuming that peripheral shim rods will be used to control the reactor, $k_{eff} = 1$ was established by inserting a poison ring between the core and reflector. This model now represents the reactor in the initial critical condition. Recalculation of the Doppler coefficient for this case resulted in a value of -0.0034 for Tdk/dT . In general, test elements would tend to be less reactive than driver fuel and, consequently, such low reactivity, high ^{238}U elements would improve (increase) the Doppler coefficient. However, high reactivity test elements might possibly be introduced, and the in-core test shim rods are provided to compensate for this type of test. This situation was simulated in another calculation where test elements (with a composition similar to that of Zone II drivers) were introduced into Zone I. The reactivity increase was about $0.018 \Delta k$, and criticality was reestablished by inserting an in-core rod bank located in Zone II. Again, the Doppler coefficient was recalculated for this reactor model and, as expected, a sharp decrease in Tdk/dT to a value of -0.0028 was observed. In Table 2D.IV, these three calculations are recapitulated.

TABLE 2D.IV. Doppler Tdk/dT as a Function of Core Configuration

	<u>Tdk/dT</u>
Rod-Free Core	-0.004
Ref1. Rods Inserted	-0.0034
Test Fuel, In-Core and Ref1. Rods Inserted	-0.0028

At the end of life, the control rods would, of course, be further withdrawn to compensate for driver fuel depletion and the presence of fission products. It is expected that, under these conditions, a further decrease in the Doppler coefficient would occur. In addition, if we now postulate errors of $\pm 25\%$ in the Doppler coefficient calculation, the Tdk/dT value might be as low as -0.002. This is the Tdk/dT value that one might want to use in reactor safeguards and dynamics studies.

Verification of the FTR kinetics parameters is an important objective of the Critical Experiment Program.

7. Cross Section Analyses with Optical Potentials

R. E. Schenter

Optical model potentials describing the elastic scattering of neutrons by nuclei can be used to obtain the angular and energy dependence of elastic scattering cross sections in previously unmeasured regions of momentum space. They also appear as input to theoretical calculations of nonelastic nuclear reaction processes which include inelastic scattering, capture, and fission. Consequently, a program has been started to calculate the optical potentials for the scattering of neutrons off nuclei that are important in fast reactor analysis. The plan is to calculate the optical potential, which is both complex and nonlocal, in terms of the fundamental nucleon-nucleon interaction and the wave function of the target nucleus.

Preliminary calculations have been made for the nuclei of oxygen and helium. Because these nuclei are tightly bound systems highly resistant to distortion by the incident neutron, simplifying approximations can be made in the optical model calculations. The essential output from these calculations is the spacial dependence of single-particle wave functions describing the elastic scattering of the incident neutron. Several forms of short range correlation in the target nucleus wave-functions have been investigated. This correlation appears in optical model calculations via the two-body correlation function of the target and is the part that takes into account the hard core properties of the nucleon-nucleon interaction. Figures 2D.16 and 2D.17 show typical results for three types of short range correlation and the resulting single-particle wave functions generated for an oxygen target. The differences between the curves in Figure 2D.17 could have important significance in nuclear reaction calculations since, for example, the nonelastic cross section is proportional to the following integral:

$$\int_0^{\infty} \text{Im } V_{\text{op}}(r_o) r_o^2 |\psi(r_o)|^2 dr_o ,$$

where $\text{Im } V_{\text{op}}$ is the imaginary part of the optical potential.

8. FCC-IV

R. W. Hardie and W. W. Little

The coding of FCC-IV, a revision of the FCC fundamental mode fast reactor code, is complete. The major features of the revision are the following:

FIGURE 2D.16. Short-Range Correlation Functions

FIGURE 2D.17. *Single-Particle Wave Functions for Neutron-Nucleus Scattering [Oxygen -16, Targets 5, 6, 7 ($E = 3.7$ MeV)]*

- The maximum number of energy groups has been increased from 26 to 40.
- A more complete scattering matrix has been added.
- An option has been introduced to read the Russian cross section data from tape.
- A flux feedback in the calculation of the elastic scattering cross section has been added.
- An option to calculate and punch perturbation cross sections has been added.
- The $1/T$ interpolation equation on temperature to compute shielded cross sections has been replaced by a logarithmic interpolation scheme.

9. Program ETOE

K. B. Stewart

The computer program ETOE was compiled and the test case successfully executed. Time requirements have been excessive since the sample isotope required 6 min of UNIVAC 1107 computer time to modify the data for use in the slowing down computer code MC². The vast majority of the time (4 min) was spent copying and sorting data from temporary drum storage to the new MC² library tape.

Conversion of ETOE to the new UNIVAC 1108 computer should not present any unusual problems. A test case has been successfully executed on the 1108.

10. Program MC²

K. B. Stewart

The fast reactor slowing down program MC² has been successfully compiled on the UNIVAC 1107 computer. MC² cannot be executed until after ETOE has produced a library tape for MC².

11. Shielding Code Conversions

D. R. Marr

Shielding codes successfully converted for use on the UNIVAC 1108 include: ANISN (one dimensional neutron transport code); MAC (one dimensional removal-diffusion code with coupled gamma calculation); RIBD (for generation of fission product inventory); and ISOSHL-D-I and -II (point kernel gamma integration programs). At the same time, a correction for calculating intensity at a point off the end of a cylindrical source was made in the ISOSHL-D integration routine.

12. FTR Shield Studies

C. A. Mansius and W. L. Bunch

The one dimension, removal-diffusion code MAC has been used to estimate the neutron and gamma flux distributions in the vicinity of a split conical core arrangement based on the concept shown in Figure 2D.18. To carry out the calculations, it was necessary to approximate the various regions by homogeneous slab shields.

a. Heat Generation Rates

Heat generation in structural components is shown in Figures 2D.19 and 2D.20. Although these values should be conservative because the one-dimensional code does not include geometric effects, the uncertainty in the calculations is not known.

b. "Damage" Fluxes

Neutron damage to structural materials is in most cases caused by the higher energy neutrons. An estimate of neutron damage flux intensities incident on various reactor components is presented in Table 2D.V for regions located radially out from the core and axially below the core. The range in values

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SK-3-12761

FIGURE 2D.18. Split Core Reactor

FIGURE 2D.19. Heat Generation Rates in Shroud and Vessel

FIGURE 2D.20. Heat Generation in Tubesheet

TABLE 2D.V. *Damage Flux Incident on Reactor Components*

<u>Region</u>	<u>Distance from Core, cm</u>	<u>Incident Damage Flux ($E \geq 0.1$ MeV)</u>
<u>A. Radially (from core)</u>		
"Slant" reflector	0	$2.6-3.5 \times 10^{15}$
"Transition" reflector	21.6	$1.2-1.7 \times 10^{15}$
"Vertical" reflector	59.7	$3.5-6.2 \times 10^{13}$
Shroud vessel	97.8	$0.6-1.2 \times 10^{12}$
Sodium annulus	102.9	$3.2-6.1 \times 10^{11}$
Reactor vessel	163.8	$1.5-8.1 \times 10^{10}$
"Blast" shield	170.8	$0.6-2.2 \times 10^{10}$
Insulation	183.5	$0.8-1.8 \times 10^9$
Concrete	214.1	$0.6-1.4 \times 10^9$
<u>B. Axially (below core)</u>		
"Pin" reflectors	0	$2.2-3.2 \times 10^{15}$
End of pins	20.3	$0.6-1.0 \times 10^{15}$
Tube sheet	137.2	$1.2-3.7 \times 10^{12}$
Inlet plenum	152.4	$2.4-4.9 \times 10^{11}$
Refractory clad	212.4	$1.3-6.2 \times 10^{10}$
Refractory material	214.9	$0.8-3.3 \times 10^{10}$
Sodium pool	253.0	$0.8-3.3 \times 10^8$
Reactor vessel	289.5	$0.3-1.1 \times 10^8$
Neutron shield	297.2	$1.0-2.4 \times 10^7$

is indicative of the coarseness of the spectral calculations in the vicinity of the assumed 0.1 MeV cutoff energy and is not intended as an estimate of the uncertainty of the calculations.

c. Reactor Instrument System Sensor Environment

An estimate was made of the radiation environment that might exist in the FTR radial shield at a potential location for a neutron sensing element in the reactor instrument system. Calculations for full power operation were based directly on the MAC results. Transient results following a shutdown are based on the fission events by delayed neutrons after a 20 milli-k scram. The equilibrium shutdown level will be established by the production rate of neutrons in the core from (α, n) events and spontaneous fission, together with the shutdown multiplication factor. Results are presented in Table 2D.VI.

TABLE 2D.VI. *Neutron and Gamma Intensity at Sensor Location Due to Reactor Fission Events*

<u>Time After Shutdown, min</u>	<u>Flux, n/cm² sec</u>	<u>Gamma Intensity R/hr</u>
0 (equilibrium operation)	10^{10}	10^5
1	10^8	10^3
4	10^7	10^2
7	10^6	10^1
10	10^5	10^0
25 (equilibrium shutdown)	10^0	10^{-5}

The gamma intensity at the sensor location will be sustained following shutdown of the reactor by the sodium radioactivity of about 10^4 R/hr from ^{24}Na and about 1 R/hr from ^{22}Na . The intensity from ^{24}Na would decay with a half-life of 15 hr. Thus, the intensity two days after shutdown would be about 10^3 R/hr, intensity four days after shutdown would be about 10^2 R/hr, etc. down to the residual intensity of about 1 R/hr from the ^{22}Na . In addition, the gamma intensity would probably be sustained during shutdowns by corrosion and

fission products in the sodium, and by activated structural and shield components in the vicinity of the sensor. No estimate was made of the magnitude of these sources. Fission product activity from within the core would not contribute to the gamma intensity at the sensor because of attenuation by the thick reflector.

13. Large Component Cask Shielding Requirements

W. L. Bunch

An intermediate heat exchanger (IHX) is potentially the most radioactive source that will be handled in the proposed large component cask for the FFTF. The cask must be several feet in diameter and tens of feet tall. Total cask weight will be strongly dependent upon the shielding. Therefore, a study estimate of requirements for potential sources of radiation within an IHX was made.

The IHX will be located in a region of negligible activation. The three sources of radiation to be associated with an IHX are (1) radioactive sodium adhering to the surfaces of the drained system, (2) corrosion products either plated on or adhering to the IHX wall surfaces, and (3) fission products retained in the IHX.

For the study, the use of four primary loops in the FTR was assumed, with each IHX containing $3.26 \times 10^6 \text{ cm}^2$ in contact with primary sodium. The bundle of tubes would form a cylindrical array about 4 ft in diameter and 16 ft long. When drained, the adherence of 0.017 lb of sodium to each square foot of surface area was assumed, leaving a total of 2310 Ci of ^{24}Na and 0.86 Ci of ^{22}Na (based on specific activities of 0.08 Ci/cm^3 and $30 \text{ } \mu\text{Ci/cm}^3$, respectively). The ^{24}Na decays with a half-life of 15 hr. Thus, removal of the IHX could be delayed to permit decay of this isotope. Calculations using

the computer code ISOSHL D indicate that less than a quarter of an inch of lead shielding would be required to reduce the gamma intensity from ^{22}Na to 100 mR/hr outside the handling cask.

It was assumed that the corrosion products from the core would be plated on the cold surfaces of the heat exchanger and would not be drained with the sodium. Removal of 118 cm^3 of fuel cladding from the core each operating month had been previously estimated. Dominant activity of the corrosion products was calculated to be from ^{58}Co , possessing a half-life of 71 days. After about a year of operation, the equilibrium activity of ^{58}Co in an IHX was calculated to be 3400 Ci. ISOSHL D calculations indicate that about three inches of lead shielding or equivalent would be required to reduce the intensity outside the cask to 100 mR/hr.

The major source of gamma radiation to be shielded by the large component cask will probably be associated with fission products plated to the IHX walls. Although the fuel is to be clad, cladding failures will probably occur. For this study, a cladding failure releasing all of the fission products from fuel generating 6 MW for 200 consecutive days was assumed. After dissipating uniformly throughout the sodium (rather than channeling into a single loop), the halogens, noble gases, and volatile solids were assumed to enter the cover gas system and the rest of the fission products to be plated to the IHX walls. The computer code RIBD was used to generate the fission product inventory, and ISOSHL D was used to estimate the gamma intensity outside the handling cask from the deposited fission products. It was found that about 8 in. of lead shielding or equivalent would be required to reduce the intensity to 100 mR/hr outside the cask within a month after the assumed cladding failure.

E. FAST REACTOR FUELS

W. E. Roake

1. Hardware

M. K. Millhollen

The seven-rod flow cluster (BNWL-501), which was previously water flow tested to measure pressure drop characteristics at various Reynolds Numbers, was removed from its hexagonal flow tube and visually examined. There was no observable dimensional or surface change so the cluster was cleaned and welded into a new hexagonal flow tube. The top of the tube was welded to an adapter assembly for the 314 Building sodium loop and the bottom to a materials specimen basket. The length of the complete assembly is 6 ft 7 in. (Figure 2E.1)

The test assembly was inserted in the sodium flow loop, and was removed from sodium flow (~ 1000 °F, ~ 25 ft/sec) after 4 weeks of test.

Visual examination revealed a dark surface coating on the sample basket and samples and on the exterior of the flow tube. A few dark areas were noted on the bottom end of the fuel rod cluster, but the fuel rod surface was generally brighter than surfaces of any other section of the assembly (Figure 2E.2).

Neg 0670943-1

FIGURE 2E.1. *Materials Sample Basket, Seven Rod Cluster Flow Tube and Loop Adapter for Sodium Flow Test*

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FIGURE 2E.2. *Seven Rod Cluster After 4 Weeks Sodium Flow*

There was no evidence of dimensional change at any point on the diameter of the fuel rods or wires. One fatigue crack was apparent in a weak section of a bottom end cap (Figure 2E.3). Wires had not loosened, and no evidence of erosion or corrosion at any point on the cluster was detected.

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FIGURE 2E.3. *Bottom End of Seven Rod Sodium Flow Test Cluster*

The complete assembly was returned to test under the same temperature and flow conditions.

2. Fuel Pin Design

G. L. Fox

A sintering model was developed which assumed a two region fuel pin, sintered and unsintered oxide, and separated by a sintering temperature T_B . The sintering computer program was run with values of T_B of 1600, 1700, and 1800 °C. Figure 2E.4 shows the variations in maximum fuel temperature and core void formation. The maximum temperature increased about 40 °F and the core central void volume decreased about 9% for every 100 °C increase in T_B .

FIGURE 2E.4. Effect of Sintering Temperature Variation

Figure 2E.4 also compares effects of two different pellet densities. The difference is only a densification effect, since the unsintered $\int k dt$ values used were for 88% theoretical density oxide.

3. Fuel Pin and Subassembly Design and Irradiation Requirements

W. J. Bailey

A literature survey was made to locate useful data (Table 2E.I) on fuel pins used in or designed for fast flux reactors. Two items--pin spacing and pin support--are of particular interest at this time because of the impending design work on FFTF fuel assemblies.

4. Fuel Pin Thermal Analysis

M. P. Burns

TIGER V, a transient heat transfer code, was used to calculate the temperature profile of an FFTF fuel pin with heat flow blocked or restricted over a portion of its surface area. For an axially oriented, long rectangular area of blocked 0.02 in. wide heat flow, the cladding surface temperature was found to increase by about 100 °F.

Two models of the fuel pin were used. In the first model, heat flow was assumed to be blocked by circular deformation of an adjacent fuel pin. For this type of blockage, axial heat flow can be neglected. The area of blocked heat flow was surrounded by another area of restricted heat flow 0.01 in. wide (circumferential distance). Heat flowed through this area into stagnant sodium before reaching the main coolant stream.

The second model was identical except that the rectangular area of blocked heat flow was assumed to be only 0.06 in. long. This case represents foreign material of low thermal conductivity lodged in a flow channel. Axial conduction was represented in a three-dimensional node structure.

TABLE 2E.I. Fast Reactor Fuel Pin Data (e.g., Pin Spacing and Support Span)

REACTOR	FFTF (PNL-III)	PFR	EBR-II	FERMI	SEFOR	RAPSODIE	BR-5
Reactor Power, (MW):							
Thermal	360	600 (design)	62.5	200	20	20	5
Electrical		250 (design)					
Pin OD, (in.)	0.250	0.230	0.174	0.158	0.890	0.264	0.197
Cladding ID, (in.)	0.218	0.200	0.156	0.148	0.810	0.228	(0.165)
Pin Separation:							
Method	wire wrap, all pins (0.031 in.)	many grids (at least 3 types)	wire wrap (0.049 in. diam.), all pins	honeycomb grids	springs	coextruded spiral fins (0.0315 in. high on all pins)	wire wrap (9 p.
Center-to-Center Spacing	0.201 in. triangular	0.290 ± 0.005 in., triangular	(0.223 tri- angular)	0.199 in., square	6 pins with C's on 2.190 in. diam. circle	(~0.296 in.) triangular	0.217 in. triangular
Minimum Rod Separation	0.031 in.	0.040 in.	0.049 in.	0.041 in.	0.205 in.	0.0315 in.	0.0200 in.
Support Span	12 in.	(~4 in.)	6 in.	Some grids re- strict movement and are 1-7/8 in. apart; others grip pins and are 7-1/2 in. apart.	~18 in.	(~4.6 in.)	(~11)
Sodium Coolant:							
Flow Direction	up	up	up	up	up	up	up
Velocity, (ft/sec)							
average	29	20 (mean)	15.8	?	~5.4		?
maximum			23.8	15.7	~8.3		15
Core Pressure Drop, (psi)	60		56	67	~50		12
Linear Power, (kW/ft):							
average	8.8 ⁽¹⁾	(9.3)	(8.25) ⁽³⁾	(4.5)	11.3	(~7.3) ⁽⁷⁾	(~3.9)
maximum	15.0	(13.7) ⁽²⁾	(12.4) ⁽⁴⁾	(7.8) ⁽⁵⁾	20.8	11.6	(5.4)
maximum (with OPF)	18.8						
Heat Flux, (10 ⁶ Btu/hr-ft ²):							
average	0.514	(~0.53)	0.619	(0.370)	0.152	(~0.37) ⁽⁸⁾	(~0.31)
maximum	0.880	(~0.78)	1.08	0.640	0.280	(~0.59)	0.44
maximum (with OPF)	1.10						
Power Peaking Factors:							
Nuclear:							
Axial	1.24	} 2.14 } (~1.59)	1.46	1.23	1.47	} 1.59	1.21
Radial	1.15		1.15	?	1.24		1.16
Engineering, Hot Spot-Hot Channel	1.20		(1.24)		1.01		
Transient Overpower (OPF)	1.25						1.40
Fuel Subassembly Data:							
Subassembly Length	~7 ft	12.5 ft	91 ²⁷ / ₃₂ in.	96 ⁹ / ₁₆ in.	114 in.	65.6 in.	32.7 in.
Pins/Subassembly	169	325	91 ³² / ₃₂ in.	140 ¹⁶ / ₁₆ in.	6	37	19
Active Fuel Length	36 3/4 in.	36 in.	13.5 in. (Mk-II)	30.5 in.	33.81 in.	13.4 in.	11 in.
Fuel Pin Length	~7 ft	9 ft	18 ³ / ₄ in.	32 ²³ / ₃₂ in.	49 ⁵ / ₈ in. (6)	19.1 in.	~11
Segmented Fuel Column		yes (Ta)	no	no	2 segments	no	no
Plenum:		spacers every 9 in.)			(2 in. gap)		
Length, (in.)	24	45	(~4)		<5	~4.4	unknown
Volume			~0.665 cm ³ (Mk-II)				unknown
Location	top	bottom	top	none	top	top	unknown
Axial Breeder Fuel* Length, (in.):							
Upper	none	18 S	18 S	17 S	5 I	~11 S	(no)
Lower	none	18 I	18 S	17 S	4 I	~11 S	(no)
Other Supporting Data:							
Coolant Temp.							
Inlet	500 °F	770 °F	705 °F	550 °F	700 °F	770 °F	810 °F
Outlet:							
average	900 °F	1060 °F	960 °F	800 °F	820 °F	930 °F	930 °F
maximum			1037 °F				
maximum (with hot channel)	1060 °F		1075 °F				
maximum (with OPF)	1200 °F						

Note - (X) means "X" is an estimated value
 *S = breeder fuel in separate pin
 I = breeder fuel integral part of core fuel pin
 (1) 867 W/g of ²³⁹Pu
 (2) 450 W/cm² or 250 W/g
 (3) 311 W/g, also 735 kW/t
 (4) 1230 kW/t
 (5) 710 W/g
 (6) ~4 in. long nickel reflector piece inside pin at each end
 (7) 410 kW/t, average
 (8) 185 W/cm²

For both models, 0.250 in. OD mixed oxide fuel pins with 16 mil cladding were operated at a heat generation rate of 15 kW/ft. The coolant bulk temperature was 700 °F.

Figure 2E.5 shows the cladding surface temperature for the first model as a function of angular displacement from the centerline of the blocked area. The maximum internal and external cladding temperatures of the second model were almost exactly the same.

These results are in general agreement with the experimental results of Ushakov et al.⁽¹⁾ For a tightly packed square rod array ($P/D = 1.0$) and an NaK flow velocity of 10.3 ft/sec, Ushakov found values for the quantity $(t_w - \bar{t}_w)k_w/\bar{q}r_2$ ranging from 0.27 to -0.2, and for θ ranging from 0 to 45 degrees. When the parameters used in this study are inserted, a peak to average circumferential temperature difference of about 140 °F is predicted. Dwyer⁽²⁾ notes, however, the maximum relative temperature difference over a 45° arc to be about 50% greater than the difference over a 30° arc, corresponding to the triangular array used in this analysis. He also notes coolant velocity to have little effect on the temperature variation.

5. Effect of BeO Addition on FFTF Flux and Fuel Subassembly Design

P. D. Cohn and W. E. McClung

Figure 2E.6 illustrates the effects of BeO addition on flux, fissile fraction, and fertile-to-fissile ratio, for the

1. P. A. Ushakov, V. I. Subbotin, B. N. Galbrianovich et al. Atomic Energy Review, vol. 13, p. 162, 1962.

2. O. E. Dwyer. Atomic Energy Review, vol. IV, no. 1, p. 48.

FIGURE 2E.5. *Cladding Temperature Profile--Touching Pins (coolant bulk temperature = 700 °F, heat generation rate = kW/ft)*

FIGURE 2E.6. Effect of BeO Addition on FFTF Flux and Fuel Design

PNL skew design.* The solid lines represent constant enrichment, and the dashed lines represent loci of fixed pin sizes.

6. Heat Generation in Axial Reflectors

R. J. Lobsinger

Inclusion of nickel or nickel-alloy reflectors at the ends of the fuel column was proposed by FFTF Reactor Physics as a means of flattening the axial power profile, reducing the critical mass, decreasing the sodium activation, and improving the shield design.

Preliminary calculations were performed to estimate the temperature distribution in these reflectors and to determine the effect on fuel pin design. Data from W. L. Bunch were used to calculate the heat generation in nickel reflectors caused by gamma heating (Figure 2E.7). Initial results predict a maximum internal temperature of 1800 °F at the center of the nickel adjacent to the fuel operating in the central zone of the reactor. Data are being refined by use of the TIGER V computer program.

The use of nickel or nickel-alloy reflectors adjacent to the fuel was proposed to flatten the axial power profile, reduce the critical mass, decrease the sodium activation, and improve the shield design. Heat will be generated in the reflectors as well as being transferred from the adjacent fuel.

The TIGER V computer program was used to calculate the temperatures in a 6 in. long nickel reflector located at the top of a fuel pin. The fuel pin was assumed to be operating at a maximum linear power of 15 kW/ft, with a sodium outlet temperature of 1045 °F. A volumetric heat generation rate of

* 91 SA core with six control positions, three packaged loops, and ten fueled tests.

$0.574 \times 10^5 \frac{\text{BTU}}{\text{in.}^3 \text{hr}}$ was assumed in the fuel adjacent to the nickel. Heat generation data for the nickel (Figure 2E.8) are based on values obtained from W. L. Bunch. Figure 2E.9 shows temperature profiles in the nickel reflector and adjacent cladding.

FIGURE 2E.7. Heat Generation from Nickel Reflectors in Fuel

FIGURE 2E.8. Heat Generation in Nickel

FIGURE 2E.9. *Temperature Profiles in Nickel Reflectors (top) and Adjacent Clad*

7. Fuel Swelling

J. E. Hanson

An in-depth review was made of swelling and growth in mixed oxide fuel due to the accumulation of gaseous and solid fission products. This analysis of existing data and their application to the FFTF driver fuel design led to the following conclusions:

- Based on the available data, a reference FFTF driver fuel design confidently expected to attain an average burnup of 45,000 MWd/tonne without cladding deformation due to fuel swelling would have:

- Cold fuel smeared density of 88% TD
- Cold fuel-cladding diametral gap of 6 mils
- Cold solid pellet density of 93% TD.

These parameters are based on a cladding inside diameter of 0.218 in. and an average burnup of 45,000 MWd/tonne at a rod power of 8.73 kW/ft (core average). Further, the premise assumes no fuel-cladding mechanical interaction until the end-of-life.

- Swelling data directly applicable to FFTF fuel design are extremely limited. The most applicable data seem to be the very limited and incomplete information obtained under the US/UK Fast Reactor Exchange Program.
- The apparent inconsistency between the British mixed oxide swelling data and the GE/APO data seems to arise from the large differences in fuel temperatures. Swelling, as manifested by cladding deformation, was concluded to be quite sensitive to temperature.
- No model development for satisfactorily describing swelling and growth in mixed oxide fuel at high burnups exists.

- The British data fail to indicate any satisfactory correlation between fuel form (vibrationally compacted versus high density and annular pellet) and swelling.
- The available data suggest that swelling, as manifested by cladding deformation, can be controlled by stronger cladding, increased fuel-cladding gap, or higher fuel temperatures.

Evaluation of the swelling expected in the peak FFTF fuel pin (15 kW/ft, 75,000 MWd/tonne burnup) indicates that the swelling will be accommodated by the same design used for the average pin. The uncertainties in this case are greater than those for the average rod since the applicable data in this region are more limited. British data at 61,000 MWd/tonne burnup indicate that 75,000 MWd/tonne peak burnup in FFTF driver fuel should be attainable without cladding deformation.

Based on the results of the study, tests involving larger fuel-cladding gaps and annular pellets were defined for the irradiation testing program. Table 2E.II lists the fuel parameters for these tests.

Testing will be accomplished in 37-pin (unencapsulated) subassemblies in EBR-II.

TABLE 2E.II. *Alternate Fuel Parameters for Irradiation Testing*

<u>Fuel Form</u>	<u>Pellet Density, % TD</u>	<u>Smeared Density, % TD</u>	<u>Fuel Cladding Gap, mils</u>
Solid Pellet	89.9	85	6
Solid Pellet	91.6	85	8
Annular Pellet	96	85	6 (pellet ID = 54 mils)

8. Fabrication Tolerances for the Annular Gap Between FFTF
Driver Fuel Spacer Wire and Flow Duct

H. G. Powers

We made a statistical analysis of the effect of variations in fuel pin, wire-wrap and subassembly flow duct inscribed circle diameter on the variation in annular gap between the outer spacer wire and driver fuel subassembly duct wall. Two assembly techniques were selected for analysis. One was a form of selective assembly wherein a change in average fuel pin diameter would be offset by using a different average wire diameter. The other technique permitted the full variation in annular gap resulting from random variation of fuel and wire diameters and subassembly duct inscribed circle diameter over the specified tolerance ranges. In the analysis, a minimum annular gap of 0.010 in. was assumed necessary for slipping the flow duct over the fuel bundle. The former technique, resulting in an annular gap of 0.015 ± 0.005 in., could be considered optimistic, while the annular gap of 0.029 ± 0.019 in. in the latter result could be considered pessimistic.

9. Mixed Oxide Fuel Fabrication

C. A. Burgess and R. E. Bardsley

Equipment modifications and relocations for establishing an integrated, mixed UO_2 - PuO_2 pellet fabrication facility have been completed. This new hood line is equipped with an automatic pellet press to facilitate fabrication of pellets for full size, EBR-II subassemblies. Each 37-pin subassembly will require 2000 pellets representative of the proposed fabrication process for FFTF driver fuel.

10. Mixed Oxide Fuel Irradiations

L. A. Pember and R. M. Crawford

Fabrication of 18 mixed oxide restrictor screening capsules to be irradiated in the VH-4 facility of the MTR was completed. These capsules are designed to operate at a power generation of 28 kW/ft. At this power generation, the fuel is expected to be molten to approximately 50% of the radius. Information from these tests will be used to determine the effectiveness of various fuel motion restrictor materials and restrictor designs. Table 2E.III specifies restrictor materials and geometries. Figures 2E.10 and 2E.11 show the capsule design.

TABLE 2E.III. Restrictor Information

<u>Capsule Number</u>	<u>Material</u>	<u>Geometry</u>	<u>Remarks</u>
BNW-1-19	Tungsten	Hole	Fixed
20	304 SS	Hole	Fixed
21	Molybdenum	Hole	Fixed
22	Tungsten	Hole	Floating
23	304 SS	Hole	Floating
24	Molybdenum	Hole	Floating
25	Nickel	Hole	Floating
26	Tungsten	Grooved	Fixed
27	304 SS	Grooved	Fixed
28	Molybdenum	Grooved	Fixed
29	Tungsten	Grooved	Floating
30	304 SS	Grooved	Floating
31	Molybdenum	Grooved	Floating
32	Nickel	Grooved	Floating
33	ZrO ₂	Solid Pellet	Floating
34	ThO ₂	Solid Pellet	Floating
35	Depleted UO ₂	Solid Pellet	Floating
36	None	---	Reference Capsule

(21) $\lambda = \frac{210}{270}$
 MATL TUNGSTEN
 (22) $\lambda = \frac{210}{270}$
 MATL 304 SST
 (23) $\lambda = \frac{210}{270}$
 MATL MOLYBDENUM
 (24) $\lambda = \frac{210}{270}$
 MATL TUNGSTEN
 (25) $\lambda = \frac{210}{270}$
 MATL 304 SST
 (26) $\lambda = \frac{210}{270}$
 MATL MOLYBDENUM
 (27) $\lambda = \frac{210}{270}$
 MATL NICKEL

28	A. $\frac{2.9}{2.78}$	MAT. TUNGSTEN
29	A. $\frac{2.9}{2.8}$	MATL 304 SST
30	A. $\frac{2.9}{2.8}$	MATL MOLYBDENUM
31	A. $\frac{2.6}{2.5}$	MATL TUNGSTEN
32	A. $\frac{2.9}{2.8}$	MATL 304 SST
33	A. $\frac{2.9}{2.8}$	MATL MOLYBDENUM
34	A. $\frac{2.9}{2.8}$	MATL NICKEL

[illegible]

GENERAL NOTES

ALL PARTS, UNLESS OTHERWISE SPECIFIED SHALL CONFORM TO THE FOLLOWING

1. TOLERANCE - FRACTIONAL $\pm \frac{1}{64}$, DECIMAL $\pm .005$ ANGULAR $0^{\circ} 30'$

2. REMOVE ALL BUGS AND BREAK SHARP EDGES TO $\frac{1}{16}$ MIN R.
3. ALL MACHINED SURFACES $\frac{1}{16}$ SURFACE ROUGHNESS IN ACCORDANCE WITH ASA B46.1-1962
4. ALL INTERNALS TO BE AS SPECIFIED OR APPROVED EQUAL-QUALITY
5. WELD ON PARTS #37 MUST CONFORM TO PT #27 MAX DIA. TOLERANCE
6. VIBRATOR OR GRIT BLAST CAPSULE NUMBERS ON AL BODY IN $\frac{1}{8}$ HIGH NUMERALS
7. STAMP WITH $\frac{1}{8}$ HIGH BY $\frac{1}{8}$ WIDE LETTERS (TOP) FOR ORIENTATION OF CAPSULE IN REACTOR
8. THIS ASSEMBLY REQUIRES $\frac{1}{2}$ GAL. FUEL

Neg 0670962

FIGURE 2E.10. VH-4 Restrictor Screening Capsules

2.92

BNWL-541

Neg 44630-3

FIGURE 2E.11. Mixed Oxide Restrictor Screening Capsule

2.93

BNWL-541

11. MTR-ETR O/M Study Irradiation Capsules

R. E. Bardsley, C. A. Burgess, and R. M. Crawford

The fuel for the MTR-ETR irradiation capsules (Figure 2E.12) consists of both high and low density UO_2 -25 wt% PuO_2 pellets (nominally 0.250 in. long x 0.212 in. diam) at three different oxygen-to-metal ratios. The test pins have a nominal 85% smear fuel density.

Test Variables:

<u>Pellet Type</u>	<u>Density</u>	<u>O/M</u>		
Solid	87.5% to 89% TD	2.000	1.963	1.939
Cored*	96% to 97.5% TD	2.000	1.963	1.939

* Pellet internal diameter nominally 0.050 in.

About 800 pellets were sintered at 1600 °C for 6 hr in argon - 8% hydrogen and cooled (<400 °C) in helium. The lower sintered densities (solid pellets) were obtained by heat treating the ball milled UO_2 - PuO_2 feed material for 1 1/2 hr in argon at 1300 °C to reduce the surface area prior to pressing.

Oxygen-to-metal ratios of 1.963 and 1.939 were obtained by H_2 reduction at 1600 °C for 8 and 16 hr, respectively. Some of the pellets were then reoxidized to obtain the 2.00 O/M by heat treating 4 hr in 800 °C argon - 8% hydrogen saturated in H_2O .

12. Irradiation Testing of Cermet Pins

L. A. Pember

Destructive examination of two PuO_2 - SST cermet pins irradiated EBR-II subassembly X009 was begun. Samples of the helium cover gas contained no fission products, an indication that cladding integrity was maintained during irradiation.

Neg 0670410-2

FIGURE 2E.12. MTR-ETR O/M Studies Capsule

Metallographic examination of two cermet pins irradiated in EBR-II to 3.28 at.% burnup of the fissile atoms was nearly completed. The principal change observed from the preirradiated steel microstructure, shown in Figure 2E.13, was an increased carbide concentration at the grain boundaries of the irradiated pin cladding, as shown in Figures 2E.14 and 2E.15. There was no indication of any PuO_2 -SS reaction during irradiation. Increased porosity of the PuO_2 particles during irradiation was noted. No corrosion was noted at the Na bond cladding interface. Striations formed on the cladding during the extrusion process are clearly visible in the transverse sections shown in Figures 2E.16 and 2E.17.

13. Restrictor Screening Capsule Irradiations

L. A. Pember

Irradiation of 18 mixed oxide restrictor screening capsules was completed in the MTR-VH-4 facility. One control capsule, BNW-1-36, was irradiated 30 min during MTR Cycle 260. This capsule was returned to PNL and examined in sufficient detail to determine that the desired extent of center fuel melting was attained. The remaining 17 capsules were irradiated during MTR Cycle 261.

Preliminary examination indicated melting in the control capsule to 44% of the fuel radius. Appreciable fuel slumping occurred during the irradiation, as illustrated in Figure 2E.17. The central void varies from a maximum diameter of 0.106 in. near the top of the fuel column to an average diameter of 0.020 in. in the bottom one-fourth of the fuel. β - γ autoradiographs (Figure 2E.18) indicate high radioactivity concentration surrounding the central cavity of the fuel in the bottom section, but little radioactivity at the center void in the upper end. This indication suggests that the fission products migrating to the molten fuel region have slumped with the fuel.

Clad-Core Bonding Longitudinal
100X

Clad-Core Bonding Transverse

100X

FIGURE 2E.13. *Microstructure of Preirradiated
SS-PuO₂ Cermet Pin*

Photomicrograph Showing Transition from Cladding to Matrix

Photomicrograph Showing Area from Center of Core

250X

FIGURE 2E.14. Photomicrograph from Cermet 5-P-13

Photomicrograph Showing Transition from Cladding to Matrix

250X

FIGURE 2E.15. Photomicrograph from Cermet Pin 5-P-14

20X

FIGURE 2E.16. *Transverse Section from Irradiated SS-20 vol% PuO₂ Cermet Pin 5-P-13*

20X

FIGURE 2E.17. Transverse Section from Irradiated SS-30 vol% PuO_2 Cermet Pin 5-P-14

Neg 0672032

FIGURE 2E.18. Macrographs, β - γ Autoradiographs
and α Autoradiographs from BNW-1-36

Appreciable erosion of the upper depleted UO_2 insulator pellet occurred during irradiation. More mixed oxide fuel migrated into the insulator pellet at the top of the fuel column than at the bottom. Molten fuel penetrated a crack in the lower depleted UO_2 pellet. No fuel movement into the plenum located at the bottom of the fuel column is indicated.

Axial expansion of the fuel was 0.122 in. or 3%. The fuel cladding gap was measured microscopically. In the cold condition, the average diametral gap was 0.001 inches. Therefore, the original 0.004 in. diametral gap had decreased to not greater than 0.001 in. during irradiation.

14. MTR-ETR O/M Study Irradiation Capsules

R. E. Bardsley, C. A. Burgess, and R. M. Crawford

Twelve NaK bonded irradiation capsules (Figure 2E.19) required for the MTR-ETR O/M irradiations were completed. Five capsules were sent to the MTR-ETR reactor site.

The following table summarizes the test pin variables:

<u>Pellet Type</u>	<u>Density, % of TD</u>	<u>O/M</u>		
Solid	87.5-89	2.000	1.963	1.939
Cored	96-97.5	2.000	1.963	1.030

Neg 0671997-1

FIGURE 2E.19. MTR-ETR O/M Study Irradiation Capsule

III. DEVELOPMENT

F. W. Woodfield

A. FFTF CORROSION AND CHEMISTRY

D. W. Shannon

1. Analysis

G. R. Bloom, T. F. Demmitt, V. P. Kelly, P. L. Koehmstedt,
H. P. Maffei, and F. A. Scott

a. Oxygen

Additional work was conducted on the PNL procedure for measuring the oxygen concentration in sodium⁽¹⁾ to determine whether oxide deposits on the outer surface of the tubes containing the sodium samples could cause erroneous analyses as the result of reduction by the sodium amalgam. Two standard sodium samples were analyzed in the presence of stainless steel rings, preoxidized by 24 hr exposure in air at 600 °F. Results obtained for these samples were within the precision limits of the method (5 ± 2 ppm). Thus, there is no significant effect of surface oxide films on samples with oxygen concentrations ≥ 5 ppm.

b. Carbon

The PNL high-temperature combustion method for total carbon measurements in sodium was described in BNWL-483. Work is in progress to refine the procedure and to improve the sensitivity and precision. These efforts have resulted in reducing the procedure blank to 6-8 ppm and the detection limit to about 5 ppm.

1. D. W. Shannon. Fast Reactor Corrosion and Chemistry Interim Technical Progress Report No. 1, pp. 3.1-3.5, October, 1966 - April, 1967, BNWL-483, June 1967.

The sodium extruder device used to provide samples for analysis was calibrated several times and the sodium delivery was found to be linearly and reproducibly related to piston displacement. The linearity for any given tube is excellent, and reproducibility between tubes is about $\pm 3\%$. This variation is acceptable for most of the analytical determinations.

c. Trace Metals

The PNL method for measuring trace concentrations of metallic corrosion product impurities in sodium involves dissolution in alcohol, hydrolysis, separation and concentration, and analysis by atomic absorption spectrophotometry.

Difficulties currently encountered with the atomic absorption spectrophotometer include high noise level and signal drift. A coordinated effort is in progress with the instrument manufacturer to attempt a resolution of these difficulties.

Initial tests have indicated that sodium present in concentrations of 1000 ppm or greater can cause interference in measurement of iron concentrations. This effect demonstrates the need for an effective impurity separation step within the procedure.

d. Analytical Chemistry Glovebox

The analytical chemistry glovebox has been assembled from a used glovebox (obtained from CANEL equipment) and a new VAC gas purification system. Assembly, installation, and leak testing of the system have been completed. Performance evaluations are being conducted. Moisture levels below 0.2 ppm in the inert gas have been achieved. Thus, the original design specification of less than 1 ppm moisture has been met.

e. Distillation - Vacuum Fusion

The best method available for measuring oxygen and sodium is the mercury amalgamation procedure. Unfortunately, this method, requiring considerable manipulation of the sodium, is undesirable for FFTF radioactive samples. These manipulations could be eliminated by using a vacuum fusion technique for oxygen measurement. In addition, vacuum fusion offers the potential of direct measurement of total hydrogen and total nitrogen compounds concurrent with measurement of oxygen. Work performed and described previously (BNWL-483) has demonstrated the feasibility of the method.

Testing during this report period has been hindered by excessive off-gassing of the sample and graphite crucible. This problem has prevented attainment of quantitative measurement of oxygen concentration. The equipment is being redesigned in an attempt to minimize this problem.

f. Radioactive Sodium Sampling and Analysis Methods

A report entitled "Sampling FFTF Sodium for Analytical Purposes," (BNWL-491) has been completed. This report defines the state-of-the-art on sodium sampling and discusses sampling in relation to FFTF requirements.

Loop fabrication is in progress. Procurement of materials is on schedule. Major components, including the control console, framework, air cooler, cold trap, and expansion tank are nearly complete.

g. 3706 Building Laboratory

One of the existing PNL laboratories was reconditioned to provide space and facilities for performing bench-scale sodium technology studies. This laboratory was required to perform

work that could not be delayed until completion of the Sodium Studies Facility. Construction work is essentially complete in this laboratory, where both radioactive and nonradioactive materials can be worked. The laboratory is presently occupied and experimental work is in progress.

2. Contaminated Systems Study

V. P. Kelly, T. J. Owen, D. W. Shannon, and U. L. Upson

a. Radioisotope Transport Loop

The Radioisotope Transport Loop (RTL) is a small-scale model of an FFTF closed loop designed to permit study of isotope transport phenomena. This loop was designed and fabricated at PNL.

Installation of the loop in the Sodium Studies Facility was completed and prestartup tests were performed. After completion of minor changes to achieve more uniform heating and better temperature control with the external heaters, the system was accepted as ready for startup.

Following an exhaustive hazards review, the loop was filled with purified sodium and operated at low-temperature. At the same time, flow was established throughout the system to begin functional testing of the components and the system as a whole.

The startup operation was conducted quickly and smoothly and no significant problems were encountered. Functional performance tests are in progress.

b. Thermal Convection Capsules

The first of four thermal convection capsules is in operation. Testing is directed toward defining major operating parameters (power input, temperature, flow) and their effects on heat transfer characteristics.

3. Sodium Cleaning and Decontamination Evaluation

W. K. Winegardner

One of the objectives of this task is to evaluate the depth of diffusion of deposited fission product activity in the stainless steel. A test system is being set up to perform exploratory tests under static conditions. Source tabs for use in this system were prepared and irradiated. To prepare these tabs, enriched uranium was sandwiched between two tabs of both lead and tin. The assemblies were then irradiated to an integrated thermal neutron dose of about 10^{16} nvt. These tabs will be used as the source of diffusing species in tests to study migration and diffusion of fission products in sodium filled capsules. Assembly of the test facility was completed.

B. MECHANICAL METALLURGY ⁽¹⁾

T. T. Claudson

1. Alloy Selection

K. R. Wheeler

An improved design for the capsule used for the irradiation of liquid metal samples is still being sought. A dye penetrant check on the vacuum cylinder weld areas has revealed the cause of sodium penetration of the vacuum annulus. A large circumferential crack in the heat affected zone adjacent to the top weld was responsible. This crack on the ID surface was probably caused by shear stresses created by differential thermal expansion between the inner and outer walls of the cylinder. A preirradiation stress relieving anneal did not sufficiently reduce in-service

1. *Although related to the FFTF, this work was not funded under the FFTF program.*

stresses to prevent failure. A new design incorporating a bellows section as part of the interior wall should eliminate the shearing stresses in the top weld region.

A program to select materials for an in-core pressure device for the FFTF is continuing. Flat, tensile, and creep-rupture specimens of 304, 316, and 348 SS, and JS-700 and Incoloy 800 alloys are being machined out of both 0.010 and 0.015 in. sheet. Stainless steel (304 and 316) specimens subjected to 1060 °F (571 °C) sodium for 30 days in an isothermal loop displayed a heavy corrosion film. The film is being examined to determine the nature of the corrosion products present. Attempts are under way to determine when and how the corrosion occurred, and how such corrosion could affect the test program.

The liquid metal capsule GEH 22-4 was discharged from the ETR after approximately 50 full power days. Although the capsule functioned adequately, a leak external to the capsule developed in the lead tube.

Capsule GEH 22-5 containing 38 nickel-base samples of alloys Hastelloy X-280, Incoloy 800, Inconel 625, In-120, and Inconel 718 is in final stages of fabrication. The specimens will be irradiated in a controlled temperature sodium environment with a sodium temperature range of 1100 to 1400 °F (593 to 760 °C).

2. In-Reactor Measurements of Mechanical Properties

E. R. Gilbert

The purpose of the in-reactor measurements program is to determine the effects of irradiation on the mechanical properties of reactor structural materials. The effort is involved in measuring creep in AISI 304 SS, nickel-base alloys, and refractory metal alloys during neutron irradiation.

In-reactor creep rupture test results on AISI 304 SS have indicated a much greater irradiation effect on rupture life at 840 °C than at lower temperatures. An in-reactor test specimen ruptured after only 6 hr under 3.52 kg-mm^{-2} (5000 psi) stress. In contrast, an unirradiated control specimen tested under the same conditions of temperature and stress did not rupture until 926 hr. The total elongation for the in-reactor specimen was about 8%, while that for the control specimen was about 28%. These data suggest about a 100 times faster creep rate during the in-reactor creep test, and an even greater difference between the rupture lives. Unfortunately the extensometer in the in-reactor creep test was a new design that failed during service. Plans to repeat these tests and to check the reproducibility of these results are being made.

A plot of the Zener-Holloman parameter-versus-stress was made on logarithmic coordinates for in-reactor and control creep data for 304 SS over the temperature range of 450 to 900 °C. The plot, except for a deviation below 500 °C, is linear for both in-reactor and control data. This deviation represents either a change in the temperature dependence or the stress dependence of the second stage creep rate.

An in-reactor creep test was conducted on hot swaged, sintered molybdenum at 580 °C and 21.1 kg-mm^{-2} (30,000 psi) stress. A creep rate of $2.4 \times 10^{-6} \text{ hr}^{-1}$ was observed for both the in-reactor test and the control test. The creep rate represents a portion of the transient stage creep since the creep rate of the control specimen is decreasing at the longer times. The in-reactor test specimen ruptured after 431 hr, whereas the control specimen has not yet failed after 2000 hr. The elongation will be measured in the postirradiation examination.

3. Stainless Steels

J. J. Holmes, R. E. Robbins, and E. R. Gilbert

The purpose of this phase of the program is to determine the combined effects of irradiation and environment on the mechanical properties of stainless steels. Radiation-induced property changes will be determined from irradiations and tests conducted at various temperatures on several alloys. Particular emphasis will be placed on determining the existence of metallurgical instabilities and the mechanisms by which they are enhanced in a nuclear environment.

Tensile specimens of 25% cold worked, AISI Types 304 and 348 SS have been irradiated at 290 °C to a fast neutron fluence of 6.0×10^{21} n/cm² ($E > 1$ MeV) and tested at temperatures to 750 °C. The irradiated yield and tensile strengths of the 304 specimens are higher than those of the unirradiated, as-fabricated material at all test temperatures. The irradiated strengths rapidly approach the unirradiated values at test temperatures above 500 °C while, at 750 °C, all strength values are about the same. Very little strain hardening was observed for the irradiated material in this range of test temperatures. The irradiated and unirradiated uniform elongations are about the same in the 300 to 700 °C test temperature range. At other test temperatures, the irradiated uniform elongation is less than that for the unirradiated specimens. The total elongation of the irradiated is less than that for the unirradiated material at all temperatures. Very little necking strain was observed in irradiated specimens.

Optical and transmission electron microscopy have been initiated on annealed AISI 304 SS specimens irradiated at 290 °C to a fast neutron fluence of 6.0×10^{21} n/cm² ($E > 1$ MeV) and then tested at room temperature, at 500, and 750 °C.

Tensile tests at temperatures from room temperature to 750 °C have been conducted on AISI Type 316 SS irradiated to a fast neutron fluence of about 1.38×10^{21} n/cm² (E > 1 MeV) in liquid sodium capsule GEH 22-3. Evaluation of the tensile data is in progress.

Out-of-reactor control tests for irradiation quadrants 201, 257, and 258 have been performed over the temperature range from room temperature to 750 °C. In-reactor quadrant 201 containing annealed AISI 304 SS tensile specimens was irradiated at 290 °C to a fast fluence of about 1.5×10^{20} n/cm² (E > 1 MeV). Quadrants 257 and 258 containing AISI 348 SS tensile specimens were irradiated to about 1.0×10^{20} n/cm² (E > 1 MeV) at 290 °C. The out-of-reactor quadrants were held at 290 °C for a period of time corresponding to the in-reactor times. Evaluation of the tensile data is in progress.

Transmission electron microscopy is being performed on annealed AISI 304 SS tensile specimens irradiated at 290 °C to a fast fluence of about 1.5×10^{20} n/cm² (E > 1 MeV), Quadrant 201. Observations of the buttonhead section of an as-irradiated specimen revealed defect clusters 50 to 100 Å in diameter. The defect density was about 1.5×10^{16} cm⁻³, and no depletion near grain boundaries or dislocations was seen. After testing at 400 °C, the clusters had increased in size and decreased in density. Dislocation loops were also observed at this temperature.

A comparison of the irradiated and thermal control data for Quadrant 201 was made. Radiation hardening increase in the yield strength was observed at 600 °C. Similar increase of the ultimate tensile strength was significant only in the

temperature range 200 to 400 °C. Unirradiated material exhibits a ductility peak at 450 °C, whereas irradiated material exhibits a minimum in ductility at this temperature.

The mockup of the sheath capsule design (for irradiations at 400 to 600 °C in the G-7 loop) was evaluated. The thermal monitors employed in the capsules were removed and examined by X-ray diffraction. Diffraction peaks for the radioactive thermal monitors were sharp with no apparent blurring due to the radioactivity. Examination of the monitors that melted during the irradiations indicate temperatures in the capsules in the range predicted by the STAC computer calculations.

4. Nickel-Base Alloys

I. S. Levy

The purpose of this program is to determine the effects of modified microstructures on the irradiation stability of nickel-base alloys. Microstructural modifications are made by preirradiation thermal or thermomechanical treatments and are evaluated by tensile tests, stress rupture tests, and microstructural examinations.

Control unirradiated, thermomechanically-pretreated Hastelloy X-280 specimens subjected to a thermal history analogous to specimens irradiated at 1250 °F (677 °C) to a fluence of 1×10^{20} n/cm² (E > 1 MeV) were tensile tested at 1350 °F (732 °C). A general increase in strength levels and a general decrease in ductility with level of cold work in the treatments was observed. Yield strength increases, relative to the standard treatment, of up to 34% (58,850 versus 44,000 psi) could be achieved with retention of 2.5% uniform elongation and 21% total elongation. However, the best combination of strength increase and ductility retention was for a

treatment that included 10% cold work followed by a 1500 °F (815 °C) age. The strength increase relative to the standard treatment was 25% (55,000 versus 44,000 psi) with retention of 4.8 uniform elongation and 26% total elongation. The ductility of specimens subjected to the standard solution treatment and, therefore, of lower strength was only a little better at 5.5% uniform elongation and 32% total elongation.

A comparison of these data with those previously obtained for unirradiated control specimens exposed at 540 °F (282 °C) was made. Exposure at the higher temperature resulted in strength increases and ductility losses relative to the 540 °F (282 °C) exposure. Of extreme importance was the observation that the magnitudes (and percentages) of the strength increases and ductility losses were far less for several of the thermomechanical treatments than for the standard solution treatment. For the standard treatment, the increase in yield strength was 15,500 psi (or 55%), and the percent uniform elongation sustained an 82% reduction from the value after 540 °F (282 °C) exposure (5.5% versus 30.5%). On the other hand, the 10% cold work, 1500 °F (815 °C) age treatment resulted in an increase in yield strength of only 5000 psi (or 10%), while the percent uniform elongation was reduced by only 40% from the value arising from the 540 °F (282 °C) exposure (4.8 versus 8.0%).

Metallography of the standard treated specimen after the 1250 °F (677 °C) exposure showed massive matrix precipitation even in the unstressed areas previously free of matrix precipitate after the 540 °F (282 °C) exposure. The precipitate was the factor accounting for the strength increase in the 1250 °F (677 °C) treatment. In the thermomechanically treated specimens, a precipitate structure developed in such a manner during the treatments (the amount depended on the severity of

the treatment) that subsequent exposure at 1250 °F (677 °C) produced less additional precipitate and, therefore, less strength increase, with the most severe treatment [20% cold work, 1500 °F (815 °C) 24 hr age] showing no strength increase. Thus, the thermomechanical treatments resulted in increased stability to a representative thermal exposure and in increased strength.

The postirradiation 1350 °F (732 °C) tensile test data for the Hastelloy X-280 specimens irradiated at 1250 °F (677 °C) showed that irradiation further reduced ductility of the standard treated material to only 1.8% uniform elongation (a 67% reduction below the 5.5% value for the unirradiated control specimen). An experimental treatment involving high temperature aging showed more than three times the ductility of the standard treatment. A treatment involving cold work and aging improved the postirradiation yield strength, relative to the standard treatment, by 28% (53,000 versus 41,500 psi) while maintaining more than three times the ductility.

Stress-rupture tests at 1350 °F (735 °C) are nearing completion on Inconel 600 and Inconel X-750 specimens held as controls for specimens irradiated at 540 °F (280 °C) to a fluence of 1×10^{20} n/cm² (>1 MeV) and previously tested. Comparison of available data for both alloys indicating a decrease in rupture life from irradiation of at least one-third was the same as found in earlier tests on Hastelloy X-280. The exact extent of this effect is dependent on the preirradiation treatment given the specimen, but a thorough analysis awaits completion of the tests.

Creep data from tests at 1350 °F (735 °C) on unirradiated, pretreated, Hastelloy X-280 specimens held as controls for

those irradiated at 540 °F (280 °C) are being examined. The data indicate that thermomechanical treatments can lead to increased rupture life by reducing creep rates. A tenfold reduction in creep rate, compared to the standard treatment, was achieved.

Specimens of Inconel 600 in various experimental heat treatments were irradiated at 1250 °F (677 °C) to a fast fluence of 1×10^{20} n/cm² (>1 MeV). These specimens and their controls were tensile-tested at 1350 °F (732 °C). The data show significant effects from both the thermal exposure and the neutron irradiation, and that magnitude of the effects is dependent upon the preirradiation treatments. Microstructural examinations performed on the control specimens confirm these observations.

A comparison of the control data for the 1250 °F (677 °C) exposure with previously obtained 540 °F (282 °C) exposure data for Inconel 600 indicates the standard treatment to have experienced an increase of 60% in yield strength and a four-fold reduction in uniform elongation resulting from the exposure at 1250 °F (677 °C). An experimental treatment, on the other hand, showed only a 30% increase in strength and only a 28% reduction in uniform elongation.

Metallography explained these effects in Inconel 600. The 1250 °F (677 °C) exposure caused precipitation hardening in the standard treated material, while the experimental treatment already displaying a significant precipitate structure caused by the treatment incurred less further precipitation and thus retained better ductility.

The effect of irradiation at 1250 °F (677 °C) on Inconel 600, while further reducing ductility, apparently

could not be attributed to hardening since yield strengths were reduced 25% or more compared to controls. The standard treatment suffered a five-fold reduction in uniform elongation (to 1.2%) and an 80-reduction in nonuniform elongation (to 0.6%), apparently the result of grain boundary embrittlement. The experimental treatment, more resistant to irradiation, ended up with 6.8% uniform elongation and 2.6% nonuniform elongation.

Stress-rupture tests at 1350 °F (732 °C) are continuing upon Inconel 600 and Inconel X-750 specimens irradiated at 540 °F (282 °C) to a fast fluence of 1×10^{20} n/cm² (>1 MeV) and their controls. The data to date indicate that irradiation reduces rupture life of Inconel X-750 by 68% (427 versus 1386 hr), and rupture life of Inconel 600 by 74% (126 versus 490 hr) in their standard-treated conditions. Some experimental treatments appear to increase the postirradiation rupture life for these alloys by about 25%. Previous work on Hastelloy X-280 showed irradiation had reduced rupture life of the standard treated material by 48% and that improvement of up to 107% could be achieved with some treatments.

5. Intercalibration Test of Counting Laboratories

R. E. Dahl

Results have been received from four of the 11 participating laboratories. Each set of data has been ratioed to the results obtained by Isochem on the same sample. Ratios were then compared to determine the laboratory differences. Results from the four laboratories, three respondents, and Isochem are presented in Table 3B.I, compared with the results of the intercalibration program conducted in 1964. These preliminary data indicate considerably less agreement, except for $^{46}\text{Ti}(n,p)^{46}\text{Sc}$, than attained previously, as evidenced by the larger standard deviation on each monitor.

TABLE 3B.I. *Comparison of Measurements at Various Sites*

	<u>^{60}Co</u>	<u>^{58}Co</u>	<u>^{46}Sc</u>	<u>^{54}Mn</u>
1964 Results				
Average*	0.996	0.99	1.04	0.99
Standard Deviation	0.032	0.026	0.043	0.99
1967 Results (Based on 4 of 11 laboratories)				
Average	0.98	0.98	1.03	1.01
Standard Deviation	0.090	0.051	0.036	0.052

* *Ratio of specific activity measurements at other sites to that of Isochem.*

Significant disagreement in weighing activated specimens, discovered in the 1964 program, is not shown by preliminary results to be a problem at present. A standard deviation of 0.43% was calculated from the 48 weights received.

To date, six of 11 laboratories have reported counting results to PNL. A spectrum has been calculated for the MTR Rabbit Facility and forwarded to all participating laboratories to aid in calculating fluence from the monitors.

Fast ($E > 1.0$ MeV) and thermal flux were calculated for all monitor materials sent to participating laboratories. The spectral-averaged cross sections ($E > 1.0$ MeV) used to calculate flux from the Ni, Fe, and Ti monitors were 92.4 mb (branching ratio of 76%), 63.8 mb, and 10.2 mb, respectively. The titanium cross section was determined by normalizing the flux from Ti to Ni. A value of 37.4 barns was used to calculate the thermal flux from the AlCo (0.1% Co) monitors. The

average flux and the percent standard deviation from 15 monitors of each type for both the 2 hr and the 8 day irradiation are listed in Table 3B.II.

TABLE 3B.II. *Neutron Flux Calculated for MTR VH-2 Facility at Reactor Midplane*

Reaction	Average Flux and % Standard Deviation for Each Monitor Set	
	2 hr Irradiation	8 day Irradiation
$^{58}\text{Ni}(n,p)^{58}\text{Co}$ $\phi(E\ 1.0\ \text{MeV})$	$2.36 \times 10^{13} \pm 2.8\%$	$2.44 \times 10^{13} \pm 0.8\%$
$^{54}\text{Fe}(n,p)^{54}\text{Mn}$ $\phi(E\ 1.0\ \text{MeV})$	$2.28 \times 10^{13} \pm 1.2\%$	$2.64 \times 10^{13} \pm 2.0\%$
$^{46}\text{Ti}(n,p)^{46}\text{Sc}$ $\phi(E\ 1.0\ \text{MeV})$	$2.36 \times 10^{13} \pm 1.2\%$	$2.20 \times 10^{13} \pm 2.0\%$
$^{59}\text{Co}(n,\gamma)^{60}\text{Co}$ (Thermal)	$1.79 \times 10^{14} \pm 2.1\%$	$1.54 \times 10^{14} \pm 2.9\%$

Discrepancies are noted in this table between the low and high exposure flux levels. Idaho Nuclear Inc., who arranged for the irradiation of these materials, indicated that differences between the low and high exposure flux levels might be explained by:

- Variation of power level
- Instability of water pressure in rabbit facility, thus causing capsule to drop from midplane position
- Disintegration of aluminum foil spacer in 8 day irradiation, thus causing a shift in positioning of the foils in the capsule.

Only the differences between individual laboratories, not the absolute value of the flux, are important to this study.

6. Fast Reactor Structural Materials and Fuel Cladding Studies

R. E. Robbins, J. J. Holmes, and R. W. Barker

The objective of this program is to determine the combined effect of environment and fast reactor irradiation on the

mechanical properties of candidate fast reactor cladding and structural alloys. The program currently in progress is specifically directed at producing a basis for selection of optimum alloys for FFTF applications and at providing a description of material behavior in fast reactor service.

Five structural material capsules were discharged from the EBR-II after about 1.7×10^{22} n/cm² total neutron fluence. These capsules contain 10 specimens of AISI 304 SST in the annealed condition and 12 specimens of 304 given a preirradiation heat treatment in the out-of-reactor hot water loop for times ranging from 339 to 3266 hr.

Electron microscopy of AISI 304 irradiated to 1.7×10^{22} in the EBR-II has continued. Examination of specimens given one hour preirradiation anneals at 900, 1100, 1200, 1300, 1500, and 1600 °F (482, 593, 648, 704, 815, and 871 °C) has revealed the following:

- After the 900 °F (482 °C) anneal, the structure is essentially identical to the as-irradiated condition and contains Frank sessile loops and voids.
- Annealing at 1200 °F (648 °C) reduces the number of Frank loops, and a dislocation network presumably consisting of interacted perfect loops begins to form.
- At 1200 °F (648 °C) and 1300 °F (704 °C), the dislocation network becomes more developed while voids remain unchanged.
- At 1400 °F (760 °C), the network anneals out and voids shrink, thus eliminating both features completely at 1500 °F (815 °C).

The change in substructure correlates very well with changes in the proportional elastic limit. Various theoretical predictions concerning the effects of loops, network, and voids

on strength have been examined with respect to these referenced microstructural features and found to be reasonably consistent with the experimental results.

Samples of 304 SS irradiated in the EBR-II to 1.7×10^{22} n/cm² (fast) have been further analyzed by transmission electron microscopy. The Frank sessile loops transform into perfect loops in the temperature range 1100 to 1300 °C. The Burgers vector of the perfect loop was determined as $\frac{a}{2} \langle 110 \rangle$. At 1200 °C, some faulted loops still remain. At 1300 °C, all loops are transformed and the loops are further developed into a nodal structure. The Burgers vectors of the nodes were analyzed and found to be consistent with the reaction

$$\frac{a}{2} [110] + \frac{a}{2} [\bar{1}01] \rightarrow \frac{a}{2} [011] .$$

At annealing temperatures greater than 1300 °F (704 °C), the dislocations anneal out by typical recovery processes. No evidence of gross grain boundary migration (i.e., recrystallization) was found.

Thin sections, cut parallel to the sample axis and near the fracture end of a tensile sample, are being studied. The microstructure is characterized by a high density of dislocations similar to that expected in a highly deformed unirradiated sample. The one significant feature thus far observed is the randomness of the voids. No evidence of dislocations sweeping up bubbles during deformation has been noted.

7. Test Facilities

R. H. Todd and J. A. Yount

An investigation into the potential fire hazards of sodium and other alkali metals disclosed that adequate fire fighting clothing is not available. The materials normally used for protecting personnel in other fire situations are dangerous

when exposed to alkali metal fires. An example of this is aluminized asbestos, which flares and burns when hit with burning NaK. Two materials used together in layers to make protective blankets and clothing appear suitable. The outer layer would consist of "pluton" cloth, made of fibrous graphite. This layer would be backed up with an insulating layer of one of the several forms of Fiberfrax, a product of the Carborundum Company. Both of these materials have an extremely high service temperature (above 2300 °F, or 1260 °C) and are known to be nonreactive when exposed to burning NaK. The graphite cloth retains some strength after direct contact but has essentially no insulation properties. Although the Fiberfrax material has excellent insulating properties, it cannot withstand direct contact with burning NaK in low density form. It is hoped that a suitable composite can be made up from these two basic materials.

The commercial solvent Dowanol "EB" has been very successful for removing small quantities of sodium from test samples and equipment.

A liquid metal metering station for handling high-purity sodium was assembled and tested. This station enables measured volumes of sodium to be transferred from shipping and storage vessels to receiving vessels without danger of contamination. Calibration tests have yielded an average error of 1 ml, with a maximum error of 3 ml in a volume range of 220 ml (minimum) to 500 ml. A sample of sodium may be taken before or after delivery of the desired volume for an analytical check on the purity of the supplied sodium.

8. Fast Neutron Mechanisms

R. P. Allen

Hydrogen permeation studies of the 18-8 type stainless steels at projected LMFBR temperatures are continuing with current emphasis on specimens of cladding thickness. A square root dependency of the permeation rate on the hydrogen partial pressure has been determined by means of gas-phase hydrogen charging techniques for diffusion membranes as thin as 0.005 in. This investigation suggests that surface absorption and desorption processes for these alloys are normally rapid compared with matrix diffusion.

Powder metallurgy techniques can be used to incorporate inert, helium-yielding particles into metallic matrices as a possible means of producing predetermined helium concentration profiles in irradiated specimens. Good particle distributions with no evidence of component-matrix interaction during sintering have been obtained for LiAlO_2 and Li_2SiO_3 in some preliminary work with nickel as the matrix material.

Steady-state hydrogen permeation studies of fast reactor alloys are continuing with current emphasis on the austenitic stainless steels. A preliminary investigation of the effect of a surface carbide layer on the effusion behavior of hydrogen from AISI 302 SS has been completed for the 250 to 600 °C temperature range. No significant reductions in permeability or other evidences of appreciable surface effects were noted, nor were there any indications of the formation of hydrocarbonaceous reaction or desorption products.

Fundamental studies of hydrogen behavior in fast reactor alloys are continuing with the use of both dynamic and steady-state hydrogen permeation techniques. During the month, a new permeation facility incorporating a high sensitivity, mass

spectrometer type hydrogen leak detector was assembled, calibrated, and placed in operation. The usable hydrogen sensitivity for permeation experiments is greater than 2×10^{-7} atm cm^3/sec for normal hydrogen backgrounds, while the time constant for pressure changes in the permeation system is less than 0.25 sec.

A series of hydrogen solubility and gas evolution studies using the new detector system was initiated to determine the effects of irradiation and prior thermomechanical treatment on (1) the occlusive capacity, and (2) the isothermal and isochronal hydrogen evolution characteristics of fast reactor alloys in the 300 to 700 °C temperature range. Planar specimens are charged with hydrogen, quenched, and the occluded hydrogen vacuum extracted under isothermal or isochronal conditions. Integration of the resulting evolution rate versus time/temperature curve yields the quantity of gas occluded for the particular charging and specimen conditions involved. The use of this procedure eliminates a number of the problems associated with the conventional Sieverts technique and permits hydrogen evolution rates (for the approximately 0.5 g samples used) as low as 0.0002 ppm/sec to be detected.

A preliminary comparison of the isochronal hydrogen evolution characteristics of representative hydrogen-charged AISI 304 and 316 SS samples was completed. For the test conditions employed ($dT/dt = 24 \text{ C min}$), the maximum hydrogen evolution rate for AISI 304 SS was approximately 60% greater than that for AISI 316 SS, and the temperature at which the maximum occurred was significantly lower (485 °C versus 520 °C). The effect of hydrogen charging conditions was also investigated, and a significant difference in isochronal evolution behavior was noted for specimens thermally charged with hydrogen and those charged as the cathode in a high intensity, hydrogen-helium glow discharge.

9. Liquid Metal Capsule Irradiations

K. R. Wheeler

Liquid metal capsule GEH 22-3, discharged from the ETR, was opened for specimen retrieval and performance evaluation. Examination of this capsule was of particular interest because of radioactivity detection in the gas inlet tubes during the last ETR cycle of capsule operation. The presence of radioactivity possibly resulted from a sodium leak and partial blockage of gas flow. The capsule, however, revealed no sodium external to its normal containment chamber. Radioactive sodium vapor escaping through a fractured thermocouple and heater cable lead is the probable cause of the detected activity.

10. Thin Section Material

A. J. Lovell

The program to select a material for an in-core pressure device for the FFTF is continuing. (Previous reports appeared under Alloy Selection.)

The investigation of the corrosion film on 304 and 316 specimens subjected to 1060 °F (571 °C) sodium for 609 hr in the isothermal loop is complete. The metallographic examination showed the film to be so extremely thin that it could only be resolved at 1000X. The results from X-ray diffraction indicate presence of a body-centered-cubic structure identifiable either as an α iron or chromium. The spectrographic analysis results indicated the manganese to be higher in the surface scraping than in the bulk alloy. The results from the microprobe indicate the film to consist of the elements Na, O, and Cr. The true nature of the corrosion film is still not known. From the information available, at least two mechanisms could account for the weight gain experienced by the specimens.

The first possibility is that chromium and manganese have formed on the surface of the stainless steel specimens in a deposition process, with the sodium and oxygen present as a contaminant remaining from the cleaning process. The second possibility is that the corrosion film is formed as a mixed Na-Cr-Mn oxide adhering to the surface of the stainless specimen, with the Cr and Mn coming from the stainless steel.

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