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Progress Report

Advanced Fast Reactor Fuels Program

Second Annual Report

July 1, 1975—September 30, 1976

University of California

MASTER

LOS ALAMOS SCIENTIFIC LABORATORY

Post Office Box 1663 Los Alamos, New Mexico 87545

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Advanced Fast Reactor Fuels Program

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Project AL013: Advanced LMFBR Fuel Stability

Project AL002: High-Performance LMFBR Fuel Materials

Compiled by

R. D. Baker

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ABSTRACT

This is the second annual report on the Advanced Fast Reactor Fuels Program at the Los Alamos Scientific Laboratory. Results of steady-state (EBR-II) irradiation testing, off-normal irradiation design and testing, fuel-cladding compatibility, and chemical stability of uranium-plutonium carbide and nitride fuels are presented.

Most of the investigations discussed here are of the continuing type. Results and conclusions described may therefore be changed or augmented as the work continues. Published reference to results cited in this report should not be made without obtaining explicit permission to do so from the person in charge of the work.

SECTION A
PROJECT AL013
ADVANCED LMFBR FUEL STABILITY

Person in Charge: R. D. Baker
Principal Investigators: J. L. Green and H. D. Lewis

I. INTRODUCTION

The advanced Liquid Metal Fast Breeder Reactor (LMFBR) fuel stability program consists of two distinct areas of investigation:

1. fuel-cladding compatibility, and
2. process system atmosphere-fuel interactions.

The program was initiated in July 1975. Some of the planning effort for the compatibility work was accomplished during the last quarter of FY 1975. The primary goals of the program are

1. Establish the data base required for selection of the more desirable advanced cladding alloys through a series of out-of-pile fuel-cladding alloy compatibility tests.
2. Identify and determine the consequences of reactions that can occur between uranium-plutonium carbide and nitride fuel materials and impurities (H_2O , O_2 , N_2) in fuel-processing atmospheres.

The fuel-cladding compatibility tests were originally to have been performed at General Electric-Sunnyvale under the direction of Committee E of the Alloy Development Program. Because of changes in program funding, the compatibility work was reassigned to the Advanced Fuels Program and transferred to Los Alamos Scientific Laboratory (LASL) in June 1975.

II. FUEL-CLADDING COMPATIBILITY
(J. F. Andrew and H. D. Lewis)

The objective of this investigation is to determine the extent of interactions between advanced LMFBR fuels and candidate advanced cladding alloys

through a series of screening tests of 1000-h duration at 600 and 725°C. The carbide fuel, (U,Pu)C + ~10 vol% (U,Pu)₂C₃, was fabricated by LASL. The nitride fuel, (U,Pu)N, was furnished by Battelle Columbus Laboratories (BCL)*. The cladding materials for these experiments, which include both sodium-bonded and helium-bonded configurations, are 20% cold-worked Types 316 and 330 stainless steels, A286, M313, PE-16, Inconel 706, Inconel 718, and HT9. The cladding alloys were supplied by Hanford Engineering Development Laboratory (HEDL)*. The nominal compositions of the alloys are shown in Table A-I.

For the helium-bonded experiments, a fuel disk was sandwiched between two disks of cladding material, and the assembly was clamped together in a molybdenum yoke. The compatibility of all cladding alloys, except HT9, with advanced fuels will be evaluated in the helium-bonded configuration. In the sodium-bonded configuration, capsules made from all eight cladding alloys will be loaded with fuel and sodium in a helium atmosphere. The extent of fuel-cladding interaction will be evaluated primarily through use of optical microscopy, although other methods, such as electron microscopy, ion-microprobe and electron-microprobe analyses, and Auger electron spectroscopy may be used. Microstructural analyses will be performed on additional samples of the eight alloys to determine the effects of thermal aging over the 1000-h heating period.

The thermal treatments at 600°C and 725°C and metallographic examinations for all the helium-bonded carbide compatibility assemblies were completed. The fuel-cladding sample array consisted of (U_{0.8}Pu_{0.2})C + 9 vol% (U,Pu)₂C₃ (87% T.D.) in contact with the alloys listed above. At 600°C, the Inconel 706 and Inconel 718 alloys, as ion etched, showed some slight grain-boundary decoration, indicating carbide precipitation to depths of 75 μm and 25 μm, respectively. The rest of the alloys indicated no significant fuel-cladding interactions. Examination of the alloy samples subjected to the 1000-h thermal aging treatments at 600°C and 725°C showed no significant changes in microstructure.

Figures A-1 through A-9 show the microstructure of the advanced cladding alloys following compatibility testing with (U,Pu)C + 9 vol% (U,Pu)₂C₃ fuel at 725° for 1000 h. The figures are in order of increasing nickel content of the

*The cooperation of A. Bauer, W. Langendorfer, and coworkers at BCL in supplying the nitride fuel, and of J. Laidler, T. Weber, and coworkers at HEDL in supplying cladding alloys and supporting Quality Assurance documentation is gratefully acknowledged.

TABLE A-1
NOMINAL COMPOSITION OF CLADDING ALLOYS (wt%)

Alloy	Fe	Ni	Cr	C	Al	Ti	Mn	Si	Nb	Mo
Type 316 stainless steel	64	14	17.5	0.05	--	--	1.6	0.5	--	2.3
Type 330 stainless steel	42.5	35	19	0.05	--	--	1.6	1.2	--	--
A286	55.5	25	14	0.05	0.2	2.2	1.2	0.5	--	1.2
M813	39	34.5	18.5	0.05	1.5	2.4	--	--	--	4.2
PE-16	34.5	43.5	16	0.08	1.2	1.3	--	--	--	3.2
Inconel 706	37.5	41.5	16	0.03	0.2	1.5	0.1	0.1	2.9	--
Inconel 718	18.5	53	18.5	0.04	0.5	0.9	0.1	0.2	5.3	2.8
H1-9	84	0.6	12	0.20	--	--	0.6	0.3	--	0.9

alloys. Metallographic preparation of all cladding specimens included a final polish with 1- μ m diamond paste followed by an ion etch.

Figure A-1 shows the microstructure of the 20% cold-worked Type 316 stainless steel after contact with the carbide fuel at 725°C for 1000 h. There is some evidence that carbides precipitated along the grain boundaries adjacent to the fuel-cladding interface. This zone did not exceed 50-75 μ m.

Figure A-2 shows the results for A286 (a precipitation-strengthened austenitic steel). The alloy microstructure in the compatibility couple shows some decoration of the grain boundaries adjacent to the surface, indicating a slight interaction. The affected zone in A286 extends to $\sim$ 75 μ m.

Figure A-3 shows the results for 20% cold-worked Type 330 stainless steel. The alloy microstructure in the compatibility couple shows some decoration of the grain boundaries by precipitates in the interface region. The mottled region within grains near the surface is probably due to etch pits caused by polishing and ion-etching procedures. The affected zone extends to $\sim$ 75 μ m.

Figure A-4 shows the results for M813 (a precipitation-strengthened austenitic steel). This powder metallurgy product was used early in this experiment because vacuum-melted material was not yet available. The alloy microstructure in the compatibility couple shows a small affected zone of <25 μ m. Figure A-5 shows the microstructure of the vacuum-melted M813 compatibility couple. The alloy microstructure shows a slight uniform matrix discoloration indicating carburization, extending to a depth of $\sim$ 50 μ m. Also observed are slight grain-boundary decoration, indicating intergranular carbide precipitation, to $\sim$ 50 μ m and a region of strong surface interaction, $\sim$ 6 μ m thick, sufficiently adherent to pull away small pieces of the fuel from the pellet surface.

Fig. A-1.
Microstructure of 20% cold-worked Type 316 stainless steel after contact with $(U,Pu)C + 9 \text{ vol}\% (U,Pu)_2C_3$ at 725°C for 1000 h. Etched. 150X.

Fig. A-2.
Microstructure of A286 after contact with $(U,Pu)C + 9 \text{ vol}\% (U,Pu)_2C_3$ at 725°C for 1000 h. Etched. 150X.

The Inconel 706 couple (a precipitation-strengthened austenitic steel) exhibited four types of interaction zones (Fig. A-6):

- (1) an interaction layer at the interface, $\sim 8 \mu\text{m}$ thick, adherent to both the fuel and cladding,
- (2) a band of uniform matrix discoloration, indicating carburization, to a depth of $\sim 40 \mu\text{m}$ from the interface,
- (3) a zone indicating intragranular precipitation extending to $\sim 100 \mu\text{m}$, and
- (4) grain-boundary decoration, indicating a decreasing amount of carbide precipitation to a depth of $\sim 150 \mu\text{m}$.

The strong surface interaction resulted in the pulling away of a large piece of fuel from the pellet surface as seen in Fig. A-6.

Figure A-7 shows the results for Nimonic PE-16 (a precipitation-strengthened austenitic steel) with the stringers oriented perpendicular to the interface. Figure A-8 shows the results for Nimonic PE-16 with the stringers oriented

Fig. A-3.

Microstructure of 20% cold-worked Type 330 stainless steel after contact with $(U,Pu)C + 9 \text{ vol}\% (U,Pu)_2C_3$ at $725^\circ C$ for 1000 h. Etched. 150X.

Fig. A-4.

Microstructure of M813 (powder metallurgy product) after contact with $(U,Pu)C + 9 \text{ vol}\% (U,Pu)_2C_3$ at $725^\circ C$ for 1000 h. Etched. 150X.

parallel to the interface. The compatibility effects were similar in both cases. There were three types of interactions observed: (1) a 10- μm interaction layer at the interface, (2) carbide precipitation along grain boundaries extending to a depth of $\sim 75 \mu m$, and (3) precipitation, predominately near grain boundaries, of a needle-like phase having definite crystallographic orientation, to a depth of $\sim 150 \mu m$.

Figure A-9 shows the results for Inconel 718 (a high-nickel austenitic steel). The alloy microstructure shows a slight matrix discoloration, indicating carburization, to a depth of $\sim 25 \mu m$ and some carbide precipitation along grain boundaries extending to $\sim 50 \mu m$. An 8- μm surface interaction layer was observed in the carbide fuel.

A summary of the compatibility effects observed in the alloys subjected to testing in the helium-bonded configuration is listed in Table A-II. Although photomicrographs of some of the alloys showed evidence of grain-boundary precipitates up to $\sim 150 \mu m$ from the fuel-cladding interfaces, these precipitates

Fig. A-5.

Microstructure of M813 (vacuum-melted product) after contact with (U,Pu)C + 9 vol% (U,Pu)₂C₃ at 725°C for 1000 h. Etched. 150X.

Fig. A-6.

Microstructure of Inconel 706 after contact with (U,Pu)C + 9 vol% (U,Pu)₂C₃ at 725°C for 1000 h. Etched. 150X.

rapidly decreased from ~40 μm to the maximum observed depth. Based on examinations to date of specimens from these out-of-pile compatibility tests, it was concluded that none of the alloys should be excluded from the irradiation test matrix solely on the basis of compatibility effects.

The 600°C and 725°C heat-treatments for the sodium-bonded compatibility assemblies containing carbide fuel were completed. Examination of the compatibility samples will begin after completion of sodium removal from the assemblies.

The helium-bonded assemblies for the nitride fuel are in the process of assembly.

A detailed summary of the compatibility work to the end of FY 1976T is being prepared for publication as a LASL report.

Fig. A-7.

Microstructure of Nimonic PE-16 (stringers oriented perpendicular to the interface) after contact with $(U,Pu)C + 9 \text{ vol}\% (U,Pu)_2C_3$ at $725^\circ C$ for 1000 h. Etched. 150X.

Fig. A-8.

Microstructure of Nimonic PE-16 (stringers oriented parallel to the interface) after contact with $(U,Pu)C + 9 \text{ vol}\% (U,Pu)_2C_3$ at $725^\circ C$ for 1000 h. Etched. 150X.

III. CHEMICAL STABILITY (H. D. Lewis)

The purpose of the chemical stability program is to determine the conditions under which advanced LMFBR carbide and nitride fuels react with normal and off-normal process atmospheres and the consequences of those reactions. Oxidation, hydrolysis, and nitriding reactions will be considered. Pelletized and powdered fuel material samples will be used in the experiments.

The first series of experiments to be performed was designed to study oxidation effects on carbide fuel materials. Changes in sample composition resulting from varying periods of exposure to process atmospheres containing oxygen impurity levels from 10 ppm to 20% will be determined. The effects of oxygen concentration, sample aging, sudden exposure to high oxygen concentrations, and frictional or impact events on ignition temperature will be investigated. One phase of this study consists of determination of the effects of long-time exposure of carbide fuel to the "normal" argon atmospheres in

Fig. A-9.

Microstructure of Inconel 718 after contact with $(U,Pu)C + 9 \text{ vol\% } (U,Pu)_2C_3$ at $725^\circ C$ for 1000 h. Etched. 150X.

a typical glovebox used in processing carbide fuels.

One of the problems associated with these experiments is the characterization of powdered materials. Reaction rates are obviously dependent in part on particle size distribution and surface area of the materials. A Quantachrome Corporation "Quantasorb" surface-area analyzer and gas pycnometer, and the Micromeritics Instruments Corporation "Sedigraph 5000" were selected for materials characterization. These instruments were modified and checked out for operation in inert-atmosphere gloveboxes. The sedigraph and pycnometer were installed in the working glovebox and are ready for use. The surface-area analyzer will be

operational after installation of low-volume filters and piping required for the remote (glovebox) sample station. The data reduction methods to be used in conjunction with this equipment are outlined in a LASL report.¹

A DuPont 990 Thermal Analyzer was procured for determination of the effects of exposing the fuel materials to a wide range of impurity levels in simulated process atmospheres. This equipment was selected because of the conceptually simple modifications to the modular sensor heads required for operation in a glovebox, and the built-in capability to perform thermogravimetric and differential thermal analyses, as well as scanning calorimetry. After solving many cabling and calibration problems, the system was successfully checked out using reference-sample materials and was installed in the working glovebox.

The phase of the program concerned with the effects of long-term exposure of carbide fuels to normal process-system atmospheres was completed. Two-phase $(U,Pu)C + 10\% (U,Pu)_2C_3$ fuel materials, in the form of carbothermic briquettes, Spex-milled powder, ball-milled powder, and unsintered (green) and sintered pellets, were exposed to the normal process-system atmosphere in a fuel-production

TABLE A-11
SUMMARY OF
HELIUM-BONDED CARBIDE COMPATIBILITY TEST RESULTS
(1000 h)

Alloy Type	Effects of Thermal Aging		Compatibility effects ^c		
	600°C	725°C	600°C	725°C	Fuel
Type 316SS	None	Slight ^a	None	GB 50 μ m	None
A286	None	None	None	GB 75 μ m	None
Type 330SS	None	Slight ^a	M 3 μ m	GB 75 μ m M 3 μ m	None
M813 (powder-met.)	Not Tested	Slight ^b	Not Tested	GB 25 μ m	None
M813 (vac.-melt)	None	None	None	M 6 μ m GB 50 μ m	None
IN. 706	None	None	M 3 μ m GB 75 μ m	M 8 μ m M 40 μ m GB 150 μ m	3 μ m
PE-16	None	None	None	M 30 μ m GB 75 μ m NP 150 μ m	3 μ m
IN. 718	None	None	M 3 μ m GB 25 μ m	M 25 μ m GB 50 μ m	8 μ m

^aBeginning of recrystallization.

^bGrain growth, (intraparticle).

^cM - Matrix discoloration (carburization).

GB - Grain boundary decoration (carbide precipitation).

NP - New phase precipitating.

glovebox for three months. Control analyses on samples of these materials indicated that the initial oxygen concentrations were <400 ppm in the carbo-thermic reduction product and <300 ppm in the sintered pellets. The nitrogen concentrations were 26 ppm in the carbothermic product and 128 ppm in the sintered pellet samples. These materials were periodically sampled to determine the oxygen, carbon, and nitrogen contents. One-gram samples of carbo-thermic briquette, ball-milled powder, and unsintered pellet were periodically checked for weight gains. Oxygen and water-vapor impurity levels in the ambi-ent box atmosphere were continuously monitored during the time that the fuel materials were exposed. Nitrogen impurity levels were spot-checked by extract-ing atmosphere samples for chemical analyses.

In anticipation of an incident of maximum consequence, such as loss of box-atmosphere integrity, the fuel materials were contained, according to type,

in ten nickel crucibles. The crucibles were supported in a stainless steel rack resting on a copper slab placed on the floor of the glovebox. The support rack was covered with a stainless steel shroud, which prevented inadvertent contact of the glovebox gloves with the sample containers, but permitted free circulation of the box atmosphere over the samples. It was assumed, in a "worst case" analysis of the loss of atmosphere integrity, that all of this carbide fuel material would be instantaneously converted to oxide with equipartition of about 248 kcal between the nickel containers and the box atmosphere. The resultant fusion of the nickel containers would bring the copper slab to a maximum temperature of about 100°C and the box atmosphere, if contained, to a pressure of about 175 kPa (approximately 2 atm). However, the glovebox pressure relief bubbler would actuate at about 0.5 kPa, venting the overpressure into the process exhaust system.

The data obtained during the three-month exposure of the fuel materials are summarized in Figs. A-10 and A-11. The glovebox atmosphere history and weight gain determined for the carbothermic briquette, ball-milled powder, and

Fig. A-10.

Glovebox atmosphere history and weight changes of three carbide fuel materials exposed to the ambient glovebox atmosphere.

Fig. A-11.

Changes in carbon, nitrogen, and oxygen concentrations in five carbide fuel materials exposed to the ambient glovebox atmosphere shown in Fig. A-10.

unsintered (green)-pellet samples are shown in Fig. A-10. The box-atmosphere impurity levels were not recorded during the 4th, 5th, 7th, 39th, and 40th days because the monitors were totally committed to the fuel fabrication effort. Except for five relatively short periods of time during the exposure period, the box atmosphere contained <10-ppm oxygen and <10-ppm water vapor. Analysis of a box-atmosphere sample extracted on the 39th day indicated the oxygen impurity level could have been considerably higher than 100 ppm for a short time. Periodic analyses of the box atmosphere indicated that the nitrogen impurity level ranged from 200 to ~600 ppm.

The three types of carbide fuel material used to determine the weight-gain history, shown in Fig. A-10, exhibited behavior similar to that noted for UC by Meerson et al.² and Sowden et al.³ Meerson determined a weight gain of about 0.1% for UC powder after exposure to air for 60 days. Sowden developed an empirical relationship between isothermal oxidation rate and exposure temperature for oxidation of UC powder (surface area of ~1.8 m²/g) in argon at 0.1 MPa

(1 atm) containing 400- to 1000-ppm oxygen. Sowden's equation predicts 1% conversion to $UO_{2.2}$ at 25°C in 1.6 years.

The changes in oxygen, nitrogen, and carbon concentrations exhibited by the five types of carbide fuel materials are summarized in Fig. A-11. Except for the increase in carbon content indicated for the green-pellet sample and the decrease indicated for the Spex-milled powder after 12 days of exposure, the change in carbon concentrations did not exceed about 0.03%. The carbon content of the sintered pellet remained essentially constant during exposure. The indicated changes in nitrogen concentration in the various types of fuel materials were not considered significant, and nitrogen analyses were discontinued after the 50th day, except for a final analysis obtained at the end of the exposure period.

The results of oxygen analyses, shown in Fig. A-11, indicated a sharp decrease in oxygen concentration over the period from the 57th to 67th days for three of the materials. At this time, it is believed this change reflects unidentified sampling or analytical problems with this particular sample set. However, comparison of the box-atmosphere and oxygen-concentration histories indicated a weak correlation between excursions in oxygen impurity levels in the box-atmosphere and oxygen concentrations in the exposed fuel materials. This may reflect some subtle relation between changes in surface concentration of adsorbed or chemisorbed oxygen and water vapor and their analytical detectability. After 70 days of exposure, the oxygen concentration in the ball-milled powder increased to 3100 ppm, as compared to 2300 ppm in the green pellet, 1900 ppm in the Spex-milled powder, 850 ppm in the carbothermic briquette, and 330 ppm in the sintered pellet. Sowden's empirical relationship³ would predict, under the conditions previously stated for 1% conversion of UC to $UO_{2.2}$, an equivalent oxygen concentration of 1300 ppm in the UC powder. The oxygen concentration of 3100 ppm in the (U,Pu)C powder, after three months exposure, indicates a significantly higher reaction rate than for UC.

Comparison of the weight gains and oxygen-concentration histories indicated significantly higher weight gains than would be expected from the increase in oxygen concentrations determined for the ball-milled powder and green pellet. An attempt is being made to determine whether these materials adsorb significant amounts of water vapor or oxygen that is not detected in the standard procedure for oxygen analysis.

The sintered pellets showed no significant change in visual appearance over the exposure period. Figures A-12, A-13, and A-14 show photographs of cross sections of three pellets examined before exposure, after 49 days, and after 91 days, respectively. No apparent deterioration of the pellet surfaces was observed.

An important implication was derived from the exposure-effects experiment. Uranium-plutonium carbide fuel materials, even in finely divided form, can be exposed to argon containing oxygen, water vapor, and nitrogen impurity levels expected in a well-controlled glovebox atmosphere for prolonged periods without serious incident. However, direct exposure of materials having relatively high surface area, such as powder and green pellets, to such atmospheres can result in oxygen concentrations precluding their use as acceptable fuel.

The next phase of the oxidation-effects study will consist of the determination of the effects of oxygen concentration in the cover gas and in the (U,Pu)C fuel material, particle size distribution, and specific surface of the fuel material on oxidation rate and ignition temperature. The first series of experiments using the thermal analyzer will determine the effect of varying

Fig. A-12.

Cross section of sintered pellet before exposure to ambient inert atmosphere in a glovebox. As polished. 12X.

Fig. A-13.

Cross section of sintered pellet after 49 days of exposure to ambient inert atmosphere in a glovebox. As polished. 12X.

Fig. A-14.

Cross section of sintered pellet after 91 days of exposure to ambient inert atmosphere in a glovebox. As polished. 12X.

oxygen concentration in the cover gas on the ignition temperature of the various forms of fuel material. The scanning-calorimeter cell was used to scope the effects of oxygen concentrations in the range 5000 ppm to 20%. The calorimeter was successfully calibrated and checked out in a cold laboratory using the same remote cell station and control cabling to be used in the glovebox. The check-out consisted of determining the melting point of an indium reference sample heated at 5°C/min. A melting-point determination was run on the same indium sample and a tin sample after installation of the analyzer in the glovebox. A

check-out run was then performed using a ball-milled (U,Pu)C powder sample to identify the general range of equipment operating parameters to be used in ignition-temperature determinations. Data obtained in these preliminary experiments are summarized in Table A-III.

TABLE A-III

DATA OBTAINED FROM CALORIMETER CHECK-OUT

Material	Heating Rate (°C/min)	Determined Melting Point (°C)	Ignition Temp. (°C)	Cover Gas Flow cm ³ (STP)/min
Indium (Cold Lab) ^a	5	156.7	--	0(Air)
Indium (Glovebox)	5	160.5	--	5(He)
Tin (Glovebox) ^b	10	232.1	--	20(He)
(U,Pu)C + 10%(U,Pu) ₂ C ₃	10	--	270 ± 5	5(Air)

^aIndium melting point = 156.634°C (Ref. 4).

^bTin melting point = 231.968°C (Ref. 4).

The calorimeter is being used to obtain a preliminary "scan" of the range of ignition temperatures for (U,Pu)C under various oxygen concentrations and gas-flow rates and, at the same time, to establish calibration procedures for the analyzer cell, gas-flow rates, cover-gas composition, and sample-temperature measurement.

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SECTION B
PROJECT AL002
HIGH-PERFORMANCE LMFBR FUEL MATERIALS

Person in Charge: R. D. Baker
Principal Investigators: J. L. Green and J. O. Barner

I. INTRODUCTION

The primary objective of this program is the development by 1986 of reliable advanced carbide and nitride LMFBR fuel element systems having design and performance characteristics that are consistent with optimized core design criteria. Emphasis currently is placed on the study of the relative merits of nitride and carbide fuel elements under conditions that appropriately exploit the potential of these materials to operate to high burnup at moderate-to-high linear powers. The major portion of the program is the evaluation of the irradiation performance of these fuel element systems. A nationally coordinated continuing series of irradiation experiments is being carried out under steady-state conditions in fast reactor environments to assess the effects of damage and burnup on carbide and nitride fuel elements. These experiments are designed to investigate fuel swelling, interactions between the fuel and cladding and thermal bonding medium, fission-gas release, and the migration of fuel material and fission products as a function of burnup and irradiation conditions. In addition, off-normal experiments are being considered that would allow the study of the effects of rapid, overpower, reactor transients and loss of sodium bond on carbide and nitride fuel assemblies. This section describes the status of the portion of the national steady-state and off-normal test matrices assigned to LASL.

A second objective in the program is the determination of thermophysical, mechanical, and chemical properties and characteristics of plutonium-containing ceramics that are required for their evaluation and use as fuel materials.

II. STEADY-STATE (EBR-II) IRRADIATION TESTING

(J. O. Barner, N. S. DeMuth, G. R. Harry, D. E. Hill, J. F. Kerrisk,
T. W. Latimer, R. L. Petty, and P. E. Rexroth)

The purpose of the EBR-II testing program is the evaluation of the steady-state irradiation behavior of high-performance fuel element systems for application in advanced LMFBR reactors. Several series of carbide- and nitride-fueled experiments have been initiated in the past several years. The main objectives of the irradiations are: (1) the development of fuel element designs for use with each fuel type, (2) the determination of the irradiation behavior of the fuel materials, (3) a comparison of sodium and helium bonding, (4) a comparison of different cladding alloys, and (5) the evaluation of the overall irradiation performance of the fuel element systems. The majority of the experiments that have been completed have been encapsulated. All of the experiments that are currently available for irradiation or that are being designed are singly clad.

A. Experiment Description and Status

Twenty-three series of experiments have been originated. The general description of the experiments is shown in Table B-I. The detailed description and status of these series are summarized in Tables B-II to B-XVII. In order to define the status of those experiments that are undergoing post-irradiation examination, the following steps are referenced in the tables.

a. Capsule Examination

- a.1 Visual Examination
- a.2 Preliminary Measurements
(radiation measurements, etc.)
- a.3 Profilometry
- a.4 Photography
- a.5 Radiography
- a.6 Eddy Current Test
- a.7 Gamma Scan
- a.8 Cover-Gas Analysis
- a.9 Decapsulation

b. Element Examination

- b.1 Visual Examination
- b.2 Profilometry
- b.3 Photography
- b.4 Eddy Current Test
- b.5 Fission-Gas Analysis
- b.6 Sectioning
- b.7 Autoradiography
- b.8 Metallography
- b.9 Burnup
- b.10 Clad Density

TABLE B-1
DESCRIPTION OF EXPERIMENTS

Series	Fuel ^{a,b} Type	Bond Type	Cladding ^{a,c} Type	Range Peak Power (kW/m)	No. Elements Completed Irradiation	No Elements to Begin or Continue Irradiation	Comments
-----FAST PROGRAM-----							
U-1300 (encap.)	MC(1) MC+M ₂ C ₃ (8)	He(9)	SA-316SS(7) SA-INC-800(7)	50 - 70	9	--	3 elements failed. Max B.U. = 9.2 - 10.2 at.%. B.U. = 11-1/2 at.%. B.U. = 11.0 at.% (He) and 8.2 at.% (Na). 1 unirradiated element rejected for bond defect.
U-1956 (encap.)	MC(12) MC+M ₂ C ₃ (7)	He(16) Na(3)	SA-316SS(11) SA-304SS(2) SA-INC-800(6)	34 - 39	19	--	9 out of 19 elements failed. Max. B.U. = 11-1/2 at.%. B.U. = 11.0 at.% (He) and 8.2 at.% (Na). 1 unirradiated element rejected for bond defect.
U-1930, -1960 (encap.)	MC(3) MC+M ₂ C ₃ (25)	He(19) Na(9)	SA-316SS(13) SA-304SS(5) SA-INC-800(6) 20CW-316SS(1)	73 - 96	27	--	9 elements failed. Max unfailed B.U. = 11.0 at.% (He) and 8.2 at.% (Na). 1 unirradiated element rejected for bond defect.
U-5300 (no cap.)	MC(14) MC+M ₂ C ₃ (4)	Na(18)	SA-316SS(3) SA-304SS(11) SA-INC-800(4)	100 - 109	9	--	7 shrouded elements. 1 failure (U-248) in unshrouded element at 3.1 at.%. Continued irradiation of unshrouded elements uncertain.
K-1, -2, -3 (encap.)	MC(12)	Na(12)	SA-316SS(12)	80 - 130	12	--	10 elements failed. Max unfailed B.U. = 7.1 at.%. B.U. = 8.1 at.%. B.U. = 8.5 at.% (Na) and 8.2 at.% (He).
NF (encap.)	MC(1) MC+M ₂ C ₃ (7)	Na(8)	SA-316SS(6) 20CW-316SS(2)	60 - 83	8	--	3 elements failed. Max unfailed B.U. = 8.1 at.%. B.U. = 8.5 at.% (Na) and 8.2 at.% (He).
B-1, -2, -3 (encap.)	MN(16)	He(3) Na(13)	SA-304SS(7) SA-316SS(13)	79 - 118	16	--	10 elements failed (all Na bond). Max unfailed B.U. = 8.5 at.% (Na) and 8.2 at.% (He).
C-5 (no cap.)	MN(20)	Na(26)	20CW-316SS(20)	80 - 84	--	12	Being irradiated in X163.
ON-1 (no cap.)	MN(7)	Na(7)	20CW-316SS(7)	80 - 81	--	4	Being irradiated in X162.
K-4 (no cap.)	MC+M ₂ C ₃ (8) MN(8)	Na(16)	20CW-316SS(16)	79 - 89	1	15	Being irradiated in X163 and X246A.
-----CURRENT PROGRAM-----							
K-4B (no cap.)	MC+M ₂ C ₃ (4)	Na(4)	20CW-316SS(4)	77 - 80	--	4	Being irradiated in X246A.
K-6 (no cap.)	MC(10) MC+M ₂ C ₃ (34)	He(44)	20CW-316SS(44)	67 - 78	--	44	38 being irradiated in X258 and X259.
K-7 (no cap.)	MC+M ₂ C ₃ (30)	Na(11) He(19)	20CW-316SS(30)	66 - 76	--	30	19 being irradiated in X280. 3 being irradiated in X246A.
K-8 (no cap.)	MC+M ₂ C ₃ (29)	Na(9) He(20)	20CW-316SS(29)	46	--	29	19 being irradiated in X281.
K-9 (no cap.)	MC+M ₂ C ₃ (28)	Na(10) He(18)	20CW-316SS(28)	69	--	28	Fuel element production in process.
K-10 (no cap.)	MC+M ₂ C ₃ (27)	He(27)	20CW-316SS(13) PE-16(14)	69	--	27	Elements fabricated.
K-11 (no cap.)	MC+M ₂ C ₃ (27)	Na(27)	20CW-316SS(13) PE-16(14)	98	--	27	Scheduled for shipment in April 1977.
K-12 (no cap.)	MC+M ₂ C ₃ (28)	Na(28)	PE-16(28)	98	--	28	Scheduled for shipment in June 1977

^aNumbers in parentheses indicate number of elements of that type.

^bM = U_{0.85}Pu_{0.15} or U_{0.85}Pu_{0.15}.

^cSA = Solution annealed, 20CW = 20% cold-worked, 304SS = Type 304 stainless steel, 316SS = Type 316 stainless steel, INC-800 = Incoloy 800.

- b.11 Special Tests
- b.12 Data Reduction
- b.13 Report Preparation

All hot cell examinations are done by Project AL001 personnel under the guidance of Project AL002 personnel.

Table B-II describes the Series K1, K2, and K3 tests. In these experiments, single-phase, high-purity, uranium-plutonium monocarbide pellets were

TABLE B-II
SERIES K1, K2, AND K3 ENCAPSULATED CARBIDE EXPERIMENTS

Exptl. No.	Fuel Type ^a	Fuel Density (% T. D.) ^b	Bond and Diametral Gap (mm)	Clad Type ^c	Clad o. d. x i. d. (mm)	Max Linear Power (kW/m)	Max Centerline Temp (°C)	Goal Burnup (at. %)	Max Current Burnup (at. %)	Status
Series K1										
K-36B	MC	90	Na-0.38	SA-316SS	7.62 x 7.11	82 ^h	990 ^h	6	5.9 ^d	b.12 ^e
K-37B	MC	90	Na-0.38	SA-316SS	7.62 x 7.11	85	1000	6	2.9	a.7 ^{e, f, i}
K-38B	MC	90	Na-0.38	SA-316SS	7.62 x 7.11	85	1000	6	5.8	a.7 ^{e, f, i}
K-39B	MC	90	Na-0.38	SA-316SS	7.62 x 7.11	85	1000	10	8.5	a.7 ^e
K-42B	MC	90	Na-0.38	SA-316SS	7.62 x 7.11	85 ^h	1000 ^h	6	4.5 ^d	Completed ^g
Series K2										
K-49	MC	95	Na-0.51	SA-316SS	7.62 x 7.11	130 ^h	1270 ^h	5	3.7 ^d	b.12 ^e
K-50	MC	95	Na-0.51	SA-316SS	7.62 x 7.11	130	1300	6.5	3.6	a.9 ^{e, i}
K-51	MC	95	Na-0.51	SA-316SS	7.62 x 7.11	130	1300	8	3.5	b.12 ^e
Series K3										
K-43	MC	94	Na-0.51	SA-316SS	7.62 x 7.11	80	1000	8	5.6	b.3 ^{e, i}
K-44	MC	94	Na-0.51	SA-316SS	7.62 x 7.11	89 ^h	1000	8	8.3 ^d	b.12
K-45	MC	94	Na-0.51	SA-316SS	7.62 x 7.11	79 ^h	1000	5	2.4 ^d	b.13 ^e
K-46	MC	94	Na-0.51	SA-316SS	7.62 x 7.11	80 ^h	935 ^h	5	2.4 ^d	b.12 ^e

^aM = (U_{0.8} Pu_{0.2}).

Series 1 and 3 experiments are 93% enriched in ²³⁵U.

Series 2 experiments are 97% enriched in ²³³U.

^bTheoretical density of MC = 13.45 Mg/m³.

^cSA = Solution annealed.

^dMeasured using the ¹⁴⁸Nd method. Remaining values were computed from EBR-II data.
The value shown for K-39B is that prior to irradiation in X233.

^eElement cladding failure indicated.

^fDamaged during reconstitution of XI52.

^gReported in LA-4669-MS.

^hBeginning-of-life values computed using measured burnup results. Remaining values based on EBR-II power adjustment factor of 0.91.

ⁱStorage.

sodium bonded to Type 316 stainless steel cladding. In general, the operating linear power ratings of the capsules were relatively high (approximately 85 kW/m). Three tests at very high power (>125 kW/m) were included to determine the effect of high thermal stresses and high fuel temperatures on fuel element behavior. Ten of the twelve elements incurred cladding breaches. The failures are believed to be the result of the inability of the thin, solution-annealed cladding to adequately withstand the fuel-cladding mechanical interaction during irradiation. All planned postirradiation work on these elements was completed.

Table B-III describes the Series U-1300 experiments. These experiments contain two-phase, uranium-plutonium carbide fuel pellets that are helium bonded to either Type 316 stainless steel or Incoloy 800 cladding. Two methods for the accommodation of fuel swelling were investigated in this series, namely,

TABLE B-III
SERIES U1300 ENCAPSULATED-CARBIDE EXPERIMENTS

Exptl. No.	Fuel Type ^a	Fuel Density (g T.D.) ^b	Bond and Diametral Gap (mm)	Clad ^d Type	Clad o.d. x i.d. (mm)	Max Linear Power (kW/m)	Max Centerline Temp (°C)	Goal Burnup, (at. %)	Current Peak Burnup, (at. %)	Status
U93	MC+5%M ₂ C ₃	86	He-0.10	SA-316SS	7.66x6.10	59 ^g	1750	11	9.6 ^e	b.13
U94	MC	85	He-0.18	SA-316SS	7.73x6.86	70 ^g	1680	11	9.4 ^e	b.13
U97	MC+5%M ₂ C ₃	86	He-0.10	SA-INC-800	7.60x6.10	54	1750	11	9.6 ^e	b.13
U98	MC+5%M ₂ C ₃	86	He-0.18	SA-INC-800	7.60x6.86	67 ^g	1680	11	9.3 ^e	b.13 ^f
U105	MC+5%M ₂ C ₃	77	He-0.20	SA-INC-800	7.61x6.10	52 ^g	1900	11	9.9 ^e	b.13
U106	MC+5%M ₂ C ₃	77	He-0.20	SA-INC-800	7.72x6.86	60 ^g	1820	11	9.5 ^e	b.13 ^f
U110	MC+10%M ₂ C ₃	99 ^c	He-0.33	SA-INC-800	7.73x6.86	65	1960	10	9.2	b.4
U113	MC+10%M ₂ C ₃	98 ^c	He-0.28	SA-INC-800	7.61x6.10	50	1880	11	10.2	b.4
U114	MC+15%M ₂ C ₃	98 ^c	He-0.18	SA-INC-800	7.72x6.86	66	1570	10	9.5	b.5 ^f

^aM = (U_{0.85} Pu_{0.15}).

^bTheoretical density of MC = 13.45 Mg/m³.
Theoretical density of M₂C₃ = 12.72 Mg/m³.

^cCored pellet with nominal 2.0-mm-diam axial hole.

^dSA = Solution annealed.

^eMeasured using the ¹⁴⁸Nd method.

Remaining values were computed from EBR-II data.

^fElement cladding failure indicated by gamma scan.

^gBeginning-of-life values computed using measured burnup results.

Remaining values based on EBR-II power adjustment factor of 0.91.

the introduction of internal porosity by the use of either low-density solid fuel pellets or high-density cored pellets. These experiments reached their goal burnup of 10 at.% in subassembly X142 after operation at moderate linear power ratings (52-70 kW/m). Four of these experiments (all unfailed) completed postirradiation examination in prior years. One additional element was examined in FY 1976.

The Series U-1950 experiments are described in Table B-IV. In these experiments, either two-phase or single-phase carbide fuel is helium bonded to Type 304 or 316 stainless steel or to Incoloy 800 cladding. Fuel densities range from 77 to 99% theoretical. This series of experiments completed irradiation in December 1974 in EBR-II subassembly X055B after operation at relatively low linear power (38 to 44 kW/m).

Although 9 elements failed at high burnup, at least 18 of the 19 elements were intact at a burnup representative of that which is typical of elements operating at this power in a commercial LMFBR. A considerable amount of post-irradiation work was completed on this series during the year. Results are discussed in Sec. B.II.C.2.c.

The Series U-1930 and U-1960 experiments are described in Table B-V. Experimental parameters include fuel type, fuel density, bond type, and cladding type. The operating linear power ratings for the experiments are relatively high (73-100 kW/m). Of the 11 experiments listed in part A of Table B-V, fuel elements U194 and U200 had failed. Destructive examination of this group of experiments has been completed.

The experiments listed in part B of Table B-V completed irradiation in subassembly X182. These eight elements were under active hot-cell postirradiation examination during FY 1976. The results are discussed in Sec. B.II.C.2.d. The capsules that were damaged at the Hot Fuel Examination Facility (HFEF) (see Table B-V) required special decapsulation procedures to recover the element in a condition that allowed nearly normal examination procedures to be used.

The experiments listed in part C of Table B-V were used as replacement capsules in order to allow irradiation to be continued to the desired burnup in lead experiments from other series. A cursory examination was performed on these elements, and the five indicated elements were then placed in long-term storage. The sixth element in this group, U203, along with elements U260 and U262 from part D of Table B-V, was reconstituted into EBR-II subassembly X233

TABLE B-IV
SERIES U1950 ENCAPSULATED CARBIDE EXPERIMENTS

Expm't. No.	Fuel Type ^b	Fuel Density (% T. D.) ^c	Bond and Diametral Gap (mm)	Clad Type ^e	Clad o. d. x i. d. (mm)	Max Linear Power, (kW/m)	Max Centerline Temp (°C)	Goal Burnup, (at. %)	Max Current Burnup (at. %)	Status
U129	MC	86	He-0.41	SA-316SS	7.70x6.60	35	1750	11	11.2	b,9 ^f
U130	MC	77	He-0.25	SA-316SS	7.70x6.60	35 ^h	1500	11	11.7 ^g	b,12
U131	MC	85	He-0.36	SA-316SS	7.70x6.60	36 ^h	1490	11	11.4 ^g	b,12 ^f
U132	MC	85	He-0.36	SA-316SS	7.70x6.60	35 ^h	1490	11	11.3 ^g	b,12 ^f
U133	MC	85	He-0.36	SA-316SS	7.70x6.60	35	1490	11	10.5	b,6
U134	MC	85	He-0.36	SA-316SS	7.70x6.60	35	1490	11	10.9	b,6
U135	MC	85	He-0.36	SA-INC-800	7.67x6.60	36	1470	11	11.1	b,9 ^f
U136	MC	85	He-0.36	SA-INC-800	7.67x6.60	34 ^h	1470	11	11.0 ^g	b,12 ^f
U137	MC+20% M ₂ C ₃	97	He-0.36	SA-316SS	7.70x6.60	37	1440	10	10.3	b,9 ^f
U138 ^a	MC+20% M ₂ C ₃	98	He-0.36	SA-316SS	7.44x6.60	37 ^h	1440	8	7.2 ^g	b,12
U139	MC+20% M ₂ C ₃	97	He-0.36	SA-INC-800	7.72x6.60	38	1440	10	10.4	a,9 ^f
U140	MC	90	He-0.36	SA-INC-800	7.72x6.60	36 ^h	1460	10	10.7	b,12
U141	MC	91	He-0.25	SA-316SS	7.70x6.60	38 ^h	1460	10	10.5 ^g	b,12 ^f
U142	MC	91	He-0.25	SA-316SS	7.72x6.60	38 ^h	1460	11	10.7 ^g	b,12
U143	MC+20% M ₂ C ₃	96 ^d	He-0.36	SA-INC-800	7.57x6.60	39	1390	11	10.8	b,6
U144	MC+20% M ₂ C ₃	96 ^d	He-0.36	SA-316SS	7.72x6.60	39	1390	11	10.8	b,6
U145	MC	90	Na-0.76	SA-304SS	7.75x6.86	34	820	10	10.6	b,9 ^f
U146 ^a	MC+20% M ₂ C ₃	99	Na-0.76	SA-304SS	7.62x6.86	36 ^h	810	8	7.2 ^g	b,12
U147	MC+20% M ₂ C ₃	97	Na-0.76	SA-INC-800	7.72x6.86	35 ^h	810	10	10.9 ^g	b,12

^a Capsules 138 and 146 were removed at 4.0 at. % burnup for TREAT testing.
Duplicates replaced the originals.

^b M = (U_{0.85}Pu_{0.15}); U is 60% enriched in ²³⁵U.

^c Theoretical density of MC = 13.49 Mg/m³.
Theoretical density of M₂C₃ = 12.76 Mg/m³.

^d Cored pellet with nominal 2.0-mm-diam axial hole.

^e SA = Solution annealed.

^f Element cladding failure indicated.

^g Measured using the ¹⁴⁸Nd method. Remaining values were computed from EBR-II data.

^h Beginning-of-life values computed using measured burnup results. Remaining values based on EBR-II power adjustment factor of 0.91.

for additional irradiation. This element, which had a very small fuel-cladding gap, failed during this additional irradiation period.

Table B-VI describes the Series WF experiments. These sodium-bonded carbide elements were designed to evaluate the effects of (1) various amounts

TABLE D-V
SERIES U1800 AND U1900 ENCAPSULATED-CARBIDE EXPERIMENTS

Exptl. No.	Fuel Type ^a	Fuel Density (% T.D.) ^b	Bond and Diametral Gap (mm)	Clad Type ^d	Clad o.d. x l.d. (mm)	Max Linear Power, (kW/m)	Max Centerline Temp (°C)	Goal Burnup (at. %)	Max Current Burnup (at. %)	Status
A										
U187	MC+5% M ₂ C ₃	86	He-0.18	SA-316SS	7.72 x 6.60	77 ^h	1900	5	4.6 ^g	b,13
U189	MC+5% M ₂ C ₃	85	He-0.25	SA-INC-800	7.67 x 6.60	81 ^h	1900	5	4.6 ^g	b,13
U191	MC	92	Na-0.76	SA-304SS	7.72 x 6.91	84 ^h	1010 ^h	5	4.6 ^g	b,12
U192	MC	92	Na-0.76	SA-304SS	7.75 x 6.91	73 ^h	910 ^h	5	4.3	b,12
U194	MC+10% M ₂ C ₃	98	Na-0.84	SA-304SS	7.75 x 6.91	81 ^h	980 ^h	5	4.6 ^g	b,12 ^e
U195	MC+10% M ₂ C ₃	98	Na-0.84	SA-304SS	7.75 x 6.91	83 ^h	990 ^h	5	4.9 ^g	b,12
U197	MC+10% M ₂ C ₃	98	Na-0.84	SA-INC-800	7.75 x 6.91	80 ^h	1020 ^h	5	4.9 ^g	b,12
U198	MC+10% M ₂ C ₃	98	Na-0.84	SA-INC-800	7.75 x 6.91	84 ^h	1000 ^h	5	4.6 ^g	b,12
U200	MC+5% M ₂ C ₃	86	He-0.23	SA-304SS	7.32 x 6.60	83 ^h	2040	5	4.7 ^g	b,13 ^e
U206	MC+5% M ₂ C ₃	90	He-0.25	SA-316SS	7.44 x 6.58	90 ^h	2080	5	5.0 ^g	b,13
U208	MC+10% M ₂ C ₃	97 ^c	He-0.30	SA-316SS	7.44 x 6.58	87 ^h	1910	5	5.0 ^g	b,12
B										
U188	MC+5% M ₂ C ₃	85	He-0.18	SA-316SS	7.72 x 6.60	79 ^h	1900	11	10.4 ^g	b,12 ^{e,i}
U190	MC+5% M ₂ C ₃	85	He-0.28	SA-INC-800	7.67 x 6.60	80 ^h	1900	11	10.5 ^g	b,12 ⁱ
U193	MC	92	Na-0.79	SA-304SS	7.75 x 6.91	75 ^h	1000	11	9.9 ^g	b,12 ^{e,i}
U196	MC+15% M ₂ C ₃	98	Na-0.79	SA-304SS	7.75 x 6.91	80 ^h	1000	11	10.4 ^g	b,12 ^o
U199	MC+10% M ₂ C ₃	98	Na-0.79	SA-INC-800	7.75 x 6.91	84 ^h	1000	11	7.8 ^g	b,12 ⁱ
U201	MC+5% M ₂ C ₃	85	He-0.23	SA-304SS	7.32 x 6.60	81 ^h	2040	11	7.3 ^g	b,12 ^e
U207	MC+5% M ₂ C ₃	90	He-0.25	SA-316SS	7.44 x 6.58	85 ^h	2090	11	7.7 ^g	b,12 ^{e,i}
U209	MC+10% M ₂ C ₃	97 ^c	He-0.30	SA-316SS	7.44 x 6.58	91 ^h	1910	11	7.8 ^g	b,12 ^{e,i}
C										
U185	MC+10% M ₂ C ₃	97	He-0.28	SA-316SS	7.72 x 6.60	90	2190	3	2.7	a,7 ^j
U186	MC+10% M ₂ C ₃	97	He-0.28	SA-316SS	7.72 x 6.60	90	2190	3	2.7	a,7 ^j
U202	MC+5% M ₂ C ₃	85	He-0.01	SA-316SS	6.83 x 6.38	94	1270	3	2.5	a,7 ^j
U203	MC+5% M ₂ C ₃	85	He-0.05	SA-316SS	7.32 x 6.43	93	1260	3	5.2	a,9 ^e
U204	MC+10% M ₂ C ₃	97 ^c	He-0.05	SA-316SS	6.76 x 6.32	96	1130	3	2.6	a,7 ^j
U205	MC+10% M ₂ C ₃	97 ^c	He-0.08	SA-316SS	7.21 x 6.35	95	1120	3	2.6	a,7 ^j
D										
U260	MC+10% M ₂ C ₃	98	He-0.48	20CW-316SS	7.57 x 6.71	90	2400	3	2.8	b,4
U261	MC+10% M ₂ C ₃	98	He-0.38	SA-316SS	7.49 x 6.60	--	--	-	0	Reject ^f
U262	MC+10% M ₂ C ₃	96	He-0.38	SA-INC-800	7.34 x 6.60	90	2260	3	2.8	b,4

^aM = (U_{0.85}Pu_{0.15}).

^bTheoretical density of MC = 13.45 Mg/m³. Theoretical density of M₂C₃ = 12.72 Mg/m³.

^cCored pellet with nominal 2.0-mm-diametral axial hole.

^dSA = Solution annealed; 20 CW = 20% cold worked.

^eElement cladding failure.

^fEBR-II steady current test indicates capsule bond discontinuity.

^gMeasured using the ²³⁵U method. Remaining values were computed using an EBR-II power adjustment factor of 0.91.

^hBeginning-of-life values computed using the measured burnup results. Beginning values are based on EBR-II power adjustment factor of 0.91.

ⁱDamaged at EBR-II.

^jStorage.

TABLE B-VI
SERIES WF ENCAPSULATED-CARBIDE EXPERIMENTS

Expm't. No.	Fuel Type ^a	Fuel Density ^b (% T. D.)	Bond and Diametral Gap (mm)	Clad Type	Clad o. d. x i. d. (in.)	Max Linear Power, (kW/m)	Max Centerline Temp (°C)	Goal Burnup (at. %)	Max Current Burnup (at. %)	Status
W3F	MC+15% M ₂ C ₃	93	Na-0.64	SA-316SS	7.62x7.01	81	1000	10	8.1	b. 4
W4F	MC+10% M ₂ C ₃	95	Na-0.69	SA-316SS	7.62x7.01	82 ^f	950	6	5.3 ^e	b. 12
W5F	MC+10% M ₂ C ₃	91	Na-0.25	SA-316SS	6.35x5.84	60	925	10	8.0	a. 9 ^d
W6F	MC+10% M ₂ C ₃	90	Na-0.28	SA-316SS	6.38x5.87	62 ^f	925	6	5.3 ^e	b. 12 ^d
W7F	MC+5% M ₂ C ₃	90	Na-0.69	20CW-316SS	7.62x7.01	81	1000	10	8.1	b. 4
W8F	MC+5% M ₂ C ₃	94	Na-0.64	20CW-316SS	7.62x7.01	83 ^f	950	6	5.8 ^e	b. 12
W10F	MC	89	Na-0.30	SA-316SS	6.38x5.87	60	925	10	8.0	b. 4
W12F	MC+25% M ₂ C ₃	97	Na-0.33	SA-316SS	6.38x5.87	63	975	10	8.0	a. 9 ^d

^aM = (U_{0.8}Pu_{0.2}).

^bTheoretical density of MC = 13.45 Mg/m³. Theoretical density of M₂C₃ = 12.72 Mg/m³.

^cSA = Solution annealed; 20CW = 20% cold worked.

^dElement cladding failure.

^eMeasured using the ¹⁴⁸Nd method. Remaining values were computed using EBR-II power adjustment factor of 0.91.

^fBeginning-of-life values computed using the measured burnup results. Remaining values are based on EBR-II power adjustment factor of 0.91.

of sesquicarbide in the fuel, (2) linear power rating, and (3) cladding cold work on element performance. The amount of sesquicarbide reported to be in the fuel varied from 0 to 25 vol%. Three of the four elements with thin claddings and small gaps failed. The four elements with thicker claddings and larger gaps were all intact. Results are discussed in Sec. B.II.C.2.f.

Table B-VII describes the Series B-1, B-2, and B-3 experiments. These capsules were fueled with single-phase, uranium-plutonium mononitride. All the elements in Series B-1 and B-2 were sodium bonded and clad with either Type 304 or 316 welded stainless steel tubing. Operating linear power ratings for the experiments were relatively high (79-110 kW/m). Six of the nine elements failed before 6 at.% burnup was attained.

Series B-3 is similar to the B-1 and B-2 series except that three helium-bonded experiments are included and the average operating linear power ratings are slightly higher. The three helium-bonded elements did not fail at maximum burnups up to 8.2 at.%. All of the sodium-bonded elements in Series B-3 were breached, and all have completed postirradiation examination. The results are discussed in Sec. B.II.C.2.g.

TABLE B-VII
SERIES B-1, B-2 AND B-3 ENCAPSULATED-NITRIDE EXPERIMENTS

Exptl. No.	Fuel Type ^a	Fuel Density (% T.D.) ^b	Bond and Diametral Gap (mm)	Clad Type ^d	Clad o. d. x i. d. (mm)	Max Linear Power (kW/m)	Max Centerline Temp (°C)	Goal Burnup (at. %)	Max Current Burnup (at. %)	Status
Series B-1										
B-1-1	MN	94	Na-0.48	SA-304SS	7.37x6.35	79 ^g	1045 ^g	5	5.7 ^e	b, 12 ⁱ
B-1-2	MN	94	Na-0.48	SA-304SS	7.37x6.35	81	1100	9	5.7	a, 7 ^{f, h}
B-1-4	MN	94	Na-0.30	SA-304SS	7.37x6.35	85	1100	10	5.8	a, 9 ^f
Series B-2										
B-2-1	MN	95	Na-0.53	SA-316SS	8.02x6.99	98	1230	10	8.5	b, 4
B-2-2	MN	94	Na-0.51	SA-316SS	8.01x6.98	93 ^g	1230	9	5.5 ^e	b, 12 ^f
B-2-3	MN	94	Na-0.51	SA-316SS	8.00x6.99	94 ^g	1230	12	8.2 ^e	b, 12
B-2-5	MN	94	Na-0.71	SA-316SS	8.00x7.21	97	1230	12	5.6	b, 4
B-2-6	MN	94	Na-0.53	SA-316SS	8.03x7.49	109	1230	6	5.6	a, 7 ^{f, h}
B-2-7	MN	94	Na-0.51	SA-316SS	8.03x7.49	110	1230	12	5.6	a, 7 ^{f, h}
Series B-3										
B-3-2	MN	88	Na-0.23	SA-316SS	8.01x7.22	110 ^g	1235 ^g	9	2.8 ^e	b, 12 ^f
B-3-3	MN	91	Na-0.25	SA-316SS	8.01x7.22	112 ^g	1280	12	2.8 ^e	b, 12 ^f
B-3-4	MN	94	Na-0.33	SA-316SS	8.01x7.22	108 ^g	1200 ^g	12	2.7 ^e	b, 12 ^f
B-3-5	MN	90	Na-0.25	SA-316SS	8.01x7.48	118 ^g	1255 ^g	6	2.7 ^e	b, 12 ^f
B-3-6	MN	95 ^c	He-0.13	SA-316SS	8.00x6.99	102	1920	10	8.2	b, 6
B-3-7	MN	89	He-0.13	SA-316SS	8.00x6.99	102 ^g	1920	6	5.5 ^e	b, 12
B-3-8	MN	90 ^c	He-0.13	SA-316SS	8.00x6.99	97	1870	10	8.0	b, 6

^a M = (U_{0.2} Pu_{0.8}).

^b Theoretical density of MN = 14.17 Mg/m³.

^c Cored pellet with nominal 1.78-mm-diam axial hole.

^d Cladding is welded tubing. SA = Solution annealed.

^e Measured using the ¹⁴³Nd method. Remaining values were computed using an EBR-II power adjustment factor of 0.91.

^f Element cladding failure indicated.

^g Beginning-of-life values computed using measured burnup results. Remaining values based on EBR-II power adjustment factor of 0.91.

^h Storage.

The Series U-5100 singly-clad experiments are described in Table B-VIII. In this group, either single-phase or two-phase carbide fuel was sodium bonded to Type 304 or 316 stainless steel or to Incoloy 800. In seven of the elements, a perforated shroud was incorporated primarily to test the retention of fuel fragments by close-fitting tubes. A secondary objective of the shroud is to study the effectiveness of the shroud alloy as a carbon getter. The seven

TABLE B-VIII
SERIES U5100 SINGLY CLAD CARBIDE EXPERIMENTS

Exptl. No.	Fuel Type ^a	Fuel Density (γ T, D.) ^b	Bond and Diametral Gap (mm) ^c	Clad ^d Type	Clad o. d. x i. d. (mm)	Max Linear Power ^f (kW/m)	Max. Centerline Temp. (°C)	Goal Burnup (at. %)	Max Current Burnup (at. %)	Status
U241	MC	94	Na-0.46	SA-304SS	7.87x7.14	106	1065	6	3.0	a,4 ^g
U242	MC	93	Na-0.44	SA-304SS	7.87x7.14	106	1080	9	3.0	a,4 ^g
U243	MC	93	Na-0.80	SA-304SS	7.87x7.14	100	1045	6	2.9	a,4 ^g
U244	MC	93	Na-0.44	SA-304SS	7.87x7.14	106	1070	9	3.0	a,4 ^g
U245	MC	93	Na-0.81	SA-304SS	7.87x7.14	99	1050	12	2.9	b,6
U246	MC	93	Na-0.43	SA-316SS	7.87x7.14	108	1085	6	3.0 ^h	b,12
U247	MC	93	Na-0.81	SA-316SS	7.87x7.14	99	1040	6	2.9	b,6
U248	MC	93	Na-0.81	SA-316SS	7.87x7.14	98	1045	12	3.2 ^h	b,12 ^e
U249	MC	93	Na-0.43	SA-INC-800	7.85x7.14	108	1075	6	3.1	b,6
U250	MC	93	Na-0.81	SA-INC-800	7.85x7.14	98	1060	6	3.1	a,4 ^g
U251	MC	93	Na-0.81	SA-304SS	7.87x7.14	98	1055	12	3.1	a,4 ^g
U252	MC	93	Na-0.64	SA-304SS	7.87x7.14	98	1035	9	8.5	HFEF Exam
U253	MC	93	Na-0.66	SA-304SS	7.87x7.14	97	1055	9	8.4	HFEF Exam
U254	MC	93	Na-0.66	SA-304SS	7.87x7.14	96	1055	9	8.4	HFEF Exam
U256	MC+15% M ₂ C ₃	96	Na-0.61	SA-304SS	7.85x7.14	100	1025	9	8.4	HFEF Exam
U257	MC+15% M ₂ C ₃	96	Na-0.58	SA-INC-800	7.85x7.14	100	1055	9	8.3	HFEF Exam
U258	MC+15% M ₂ C ₃	96	Na-0.58	SA-304SS	7.87x7.14	100	1045	9	8.3	HFEF Exam
U259	MC+15% M ₂ C ₃	96	Na-0.58	SA-INC-800	7.85x7.14	101	1020	9	8.4	HFEF Exam

^aM = (U_{0.85}Pu_{0.15}).

^bTheoretical density of MC = 13.45 Mg/m³.
Theoretical density of M₂C₃ = 12.72 Mg/m³.

^cElements U252 through U259 contain slotted shroud tubes ~ 0.09-mm thick, made of V, Fe, 304SS, V, Ta, 304SS and 304SS respectively. The shroud thickness is not included in the bond gap value.

^dSA = solution annealed.

^eElement cladding failure.

^fComputed using an EBR-II power adjustment factor of 0.91.

^gStorage.

^hMeasured using the ¹⁴³Nd method. Remaining values were computed using an EBR-II power adjustment factor of 0.91.

elements that incorporated shroud tubes successfully attained their goal burnup of 8-1/2 at.% burnup. Two of the unshrouded elements, including one failed element, were destructively examined after ~3 at.% burnup. The remaining unshrouded elements were put into storage.

The C-5 and O-N1 Series of singly clad experiments are described in Table B-IX. Single-phase nitride fuel is sodium bonded to 20% cold-worked Type 316 stainless steel cladding in all of the fuel elements in this group. These elements are currently operating successfully in subassembly X163.

TABLE B-IX
SERIES C-5 AND O-N1 SINGLY CLAD NITRIDE EXPERIMENTS

Exptl. No.	Fuel Type ^a	Fuel Density (T, D ₂)	Bond and Diametral Gap (mm)	Clad Type	Clad o.d. x i.d. (mm)	Max Linear Power (kW/m)	Max Centerline Temp (°C)	Goal Burnup (at. %)	Max Current Burnup (at. %)	Status
C-5-1	MN	93	Na-0.51	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^{d, e}
C-5-2	MN	94	Na-0.51	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^{d, e}
C-5-3	MN	95	Na-0.51	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^{d, e}
C-5-4	MN	96	Na-0.53	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^g
C-5-5	MN	96	Na-0.51	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^f
C-5-6	MN	94	Na-0.53	20CW-316SS	7.87x7.11	81	1000	12	6.0	EBR-II, X163
C-5-7	MN	95	Na-0.51	20CW-316SS	7.87x7.11	83	1005	12	6.1	EBR-II, X163
C-5-8	MN	95	Na-0.76	20CW-316SS	7.87x7.11	80	970	12	6.5	EBR-II, X163
C-5-9	MN	95	Na-0.51	20CW-316SS	7.87x7.11	83	985	12	6.1	EBR-II, X163
C-5-10	MN	95	Na-0.51	20CW-316SS	7.87x7.11	81	965	12	5.9	EBR-II, X163
C-5-11	MN	95	Na-0.51	20CW-316SS	7.87x7.11	82	975	12	6.0	EBR-II, X163
C-5-12	MN	95	Na-0.76	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^d
C-5-13	MN	96	Na-0.76	20CW-316SS	7.87x7.11	81	960	12	6.4	EBR-II, X163
C-5-14	MN	97	Na-0.76	20CW-316SS	7.87x7.11	80	985	12	6.3	EBR-II, X163
C-5-15	MN	96	Na-0.76	20CW-316SS	7.87x7.11	80	970	12	6.4	EBR-II, X163
C-5-16	MN	96	Na-0.76	20CW-316SS	7.87x7.11	80	985	12	6.3	EBR-II, X163
C-5-17	MN	97	Na-0.76	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^e
C-5-18	MN	95	Na-0.53	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^d
C-5-19	MN	94	Na-0.53	20CW-316SS	7.87x7.11	81	975	12	5.9	EBR-II, X163
C-5-20	MN	95	Na-0.53	20CW-316SS	7.87x7.11	82	995	12	6.0	EBR-II, X163
O-N1-1	MN	91	Na-0.51	20CW-316SS	7.87x7.11	80	1000	12	6.2	EBR-II, X163
O-N1-2	MN	91	Na-0.51	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^d
O-N1-3	MN	91	Na-0.51	20CW-316SS	7.87x7.11	80	1025	12	6.1	EBR-II, X163
O-N1-4	MN	90	Na-0.51	20CW-316SS	7.87x7.11	80	1025	12	6.2	EBR-II, X163
O-N1-5	MN	91	Na-0.51	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^d
O-N1-6	MN	90	Na-0.51	20CW-316SS	7.87x7.11	---	----	--	--	Reject ^d
O-N1-8	MN	91	Na-0.51	20CW-316SS	7.87x7.11	80	995	12	6.2	EBR-II, X163

^aM = (U_{0.8}Pu_{0.2}).

^bTheoretical density of MN = 14.17 Mg/m³.

^c20CW = 20% cold worked.

^dFuel chips in bond.

^eAir in plenum.

^fPossible impurities present.

^gDamaged.

The K4 and K4B experiments are described in Table B-X. The K4 Series is composed of eight carbide-fueled elements and eight nitride-fueled elements. All of the K4 elements are sodium bonded and contain fuel-fragment-restraint devices (shrouds) which are thin-walled, perforated, metal tubes. The shroud tubes are 25% cold-worked, Type 316 stainless steel, 0.076 mm (0.003 in.) thick. The K4 Series is intended to test the fuel-shroud concept and to compare the behavior of sodium-bonded carbide and nitride elements under as nearly identical conditions as possible. Twelve of the K4 elements, six carbide and six nitride, operated successfully to an interim burnup of ~6 at.% with the seven shrouded elements from the U-5100 Series in subassembly X246. Eleven of these elements will continue irradiation in subassembly X246A. Three of

TABLE B-X
SERIES K4 AND K4B SINGLY CLAD CARBIDE AND NITRIDE EXPERIMENTS

Exptl. No.	Fuel Type ^a	Fuel Density ^b (g/cm ³)	Bond and Diametral Gap (mm)	Clad ^d Type	Clad o.d. x i.d. (mm)	Max Linear Power (kW/m)	Max Centerline Temp. (°C)	Goal Burnup (at. %)	Max Current Burnup (at. %)	Status
K4-1	MC-3 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.69	20CW-316SS	7.87x7.26	83	895	9	5.9	Interim Exam ^e
K4-2	MC-2 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.69	20CW-316SS	7.87x7.26	83	910	9	5.9	Interim Exam ^e
K4-3	MC-4 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.69	20CW-316SS	7.87x7.26	83	905	9	5.9	Interim Exam ^e
K4-4	MC-12 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.69	20CW-316SS	7.87x7.26	81	900	9	5.7	Interim Exam ^e
K4-5	MC-14 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.69	20CW-316SS	7.87x7.26	76	875	9	5.5	Interim Exam ^e
K4-6	MC-17 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.69	20CW-316SS	7.87x7.26	74	850	9	5.4	Interim Exam ^e
K4-8	MN	97	Na-0.69	20CW-316SS	7.87x7.26	77	920	9	5.4	Interim Exam ^e
K4-9	MN	97	Na-0.69	20CW-316SS	7.87x7.26	76	925	9	5.4	Interim Exam ^e
K4-10	MN	97	Na-0.69	20CW-316SS	7.87x7.26	83	990	9	5.7	Interim Exam ^e
K4-11	MN	97	Na-0.69	20CW-316SS	7.87x7.26	83	990	9	5.7	Interim Exam ^e
K4-12	MN	97	Na-0.69	20CW-316SS	7.87x7.26	80	975	9	5.6	Interim Exam ^e
K4-13	MC-6 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.69	20CW-316SS	7.87x7.26	81	900	9	0	EBR-II, X246A
K4-14	MC-18 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.69	20CW-316SS	7.87x7.26	85	915	12	6.4	EBR-II, X163
K4-15	MN	97	Na-0.69	20CW-316SS	7.87x7.26	90	945	12	5.3	EBR-II, X163
K4-16	MN	97	Na-0.69	20CW-316SS	7.87x7.26	81	950	12	5.8	EBR-II, X163
K4-17	MN	97	Na-0.69	20CW-316SS	7.87x7.26	79	935	9	5.5	Interim Exam ^e
K4B-1	MC-9 $\frac{1}{3}\text{M}_2\text{C}_3$	98	Na-0.79	20CW-316SS	7.87x7.11	78	915	12	0	EBR-II, X246A
K4B-2	MC-9 $\frac{1}{3}\text{M}_2\text{C}_3$	98	Na-0.79	20CW-316SS	7.87x7.11	79	920	12	0	EBR-II, X246A
K4B-3	MC-10 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.79	20CW-316SS	7.87x7.11	77	935	12	0	EBR-II, X246A
K4B-4	MC-10 $\frac{1}{3}\text{M}_2\text{C}_3$	99	Na-0.79	20CW-316SS	7.87x7.11	80	935	12	0	EBR-II, X246A

^aM = (U, $\frac{1}{3}$ Pu, $\frac{1}{3}$).

^bTheoretical density of MC = 13.45 Mg/m³, $\frac{1}{3}\text{M}_2\text{C}_3$ = 12.72 Mg/m³, and MN = 14.17 Mg/m³.

^cThe diametral gap shown is the difference between the fuel o.d. and the cladding i.d. The thickness of the perforated shroud tube has not been subtracted.

^d20 CW = 20% cold-worked.

^eTo be irradiated in X246A after interim examination.

the K4 elements, one carbide and two nitride, have successfully operated in EBR-II subassembly X163 with the C-5 and O-N1 elements. The four K4B Series elements were fabricated to fill the X246A subassembly in order to achieve the goal burnup on the K4 elements. They utilize the K7 Series sodium-bonded element design and are similar to the Atomics International AIR-1 Series elements.

The K6 Series elements are described in Table B-XI. The K6 Series are being irradiated in two A-19B subassemblies designated X258 (K6A) and X259 (K6B). The primary objective of this series of tests is to provide initial, statistical, behavioral data for helium-bonded carbide-fueled elements for FTR peak-power testing.

The K7 Series elements are described in Table B-XII. The majority of the K7 Series elements are being irradiated in subassembly X280. Four K7 Series elements will be irradiated in X246A. The primary objective of the K7 Series is to determine the irradiation behavior of sodium-bonded, shrouded carbide fuel elements and helium-bonded carbide fuel elements at the peak-power condition anticipated for a 127-element assembly in the Fast Test Reactor (FTR).

The K8 Series elements are described in Table B-XIII. The K8 Series elements are being irradiated in subassembly X281. The primary objective of the K8 Series is to determine the irradiation behavior of sodium-bonded, shrouded carbide fuel elements and helium-bonded carbide fuel elements at the peak-cladding-temperature conditions anticipated for a 127-element assembly in the FTR.

B. Experiment Design and Analysis

1. Experiment Design

The K9, K10, K11, and K12 Series experiments are described in Tables B-XIV, B-XV, B-XVI, and B-XVII, respectively. These fuel elements are in various stages of design or fabrication. They will all be available for insertion into EBR-II during FY 1977. All the elements of these series are 9.40-mm o.d. and would fit in a 91-element FTR assembly. The K9 Series is intended to test sodium-bonded, shrouded carbide elements and helium-bonded carbide elements under conditions that simulate the peak cladding temperatures in FTR.

The K10 Series is intended to test helium-bonded carbide elements under conditions that simulate the peak cladding temperatures in FTR. However, in

TABLE B-XI
SERIES K6 SINGLY CLAD CARBIDE EXPERIMENTS

Exptl. No.	Fuel Type ^a	Fuel Density (% T.D.) ^b	Bond and Diametral Gap (mm)	Clad Type ^{c,d}	Clad o.d. x i.d. (mm)	Max Linear Power (kW/m)	Max Centerline Temp (°C)	Goal Burnup (at. %)	Max Current Burnup (at. %)	Status
K6A-52	MC+ 9% M ₂ C ₃	81	He-0.13	20CW-316SS	7.87x6.86	73	1460	12	3.5	EBR-II, X258
K6A-53	MC+ 9% M ₂ C ₃	81	He-0.13	20CW-316SS	7.87x6.86	73	1470	12	3.5	EBR-II, X258
K6A-54	MC+ 9% M ₂ C ₃	80	He-0.13	20CW-316SS	7.87x6.86	72	1460	12	3.3	EBR-II, X258
K6A-55	MC+ 9% M ₂ C ₃	80	He-0.13	20CW-316SS	7.87x6.86	73	1460	12	3.4	EBR-II, X258
K6A-56	MC+ 7% M ₂ C ₃	80	He-0.13	20CW-316SS	7.87x6.86	73	1460	12	3.4	EBR-II, X258
K6A-57	MC+ 7% M ₂ C ₃	80	He-0.13	20CW-316SS	7.87x6.86	71	1440	12	3.2	EBR-II, X258
K6A-58	MC+ 10% M ₂ C ₃	81	He-0.13	20CW-316SS	7.87x6.86	73	1460	12	3.4	EBR-II, X258
K6A-59	MC+ 10% M ₂ C ₃	81	He-0.13	20CW-316SS	7.87x6.86	72	1450	12	3.2	EBR-II, X258
K6A-60	MC+ 7% M ₂ C ₃	80	He-0.13	20CW-316SS	7.87x6.86	72	1450	12	3.2	EBR-II, X258
K6A-61	MC+ 8% M ₂ C ₃	82	He-0.25	20CW-316SS	7.87x6.86	73	1870	12	3.8	EBR-II, X258
K6A-62	MC+ 7% M ₂ C ₃	81	He-0.25	20CW-316SS	7.87x6.86	73	1870	12	3.8	EBR-II, X258
K6A-63	MC+ 8% M ₂ C ₃	82	He-0.25	20CW-316SS	7.87x6.86	71	1850	12	3.6	EBR-II, X258
K6A-64	MC+ 6% M ₂ C ₃	80	He-0.25	20CW-316SS	7.87x6.86	73	1880	12	3.8	EBR-II, X258
K6A-65	MC+ 6% M ₂ C ₃	79	He-0.25	20CW-316SS	7.87x6.86	72	1860	12	3.7	EBR-II, X258
K6A-66	MC+ 7% M ₂ C ₃	81	He-0.25	20CW-316SS	7.87x6.86	72	1860	12	3.7	EBR-II, X258
K6A-67	MC+ 8% M ₂ C ₃	80	He-0.25	20CW-316SS	7.87x6.86	72	1870	12	3.7	EBR-II, X258
K6A-68	MC+ 13% M ₂ C ₃	81	He-0.25	20CW-316SS	7.87x6.86	71	1850	12	3.5	EBR-II, X258
K6A-69	MC+ 13% M ₂ C ₃	81	He-0.25	20CW-316SS	7.87x6.86	71	1850	12	3.5	EBR-II, X258
K6A-70	MC+ 8% M ₂ C ₃	80	He-0.25	20CW-316SS	7.87x6.86	72	1860	12	3.5	EBR-II, X258
K6A-71	MC+ 9% M ₂ C ₃	81	He-0.25	20CW-316SS	7.87x6.86	----	----	--	0	EBR-II, unassigned ^e
K6A-72	MC+ 9% M ₂ C ₃	81	He-0.25	20CW-316SS	7.87x6.86	----	----	--	0	EBR-II, unassigned ^e
K6A-73	MC+ 6% M ₂ C ₃	82	He-0.13	20CW-316SS	7.87x6.86	----	----	--	0	EBR-II, unassigned ^e
K6B-74	MC+ 1% M ₂ C ₃	88	He-0.25	20CW-316SS	7.87x6.86	70	1800	12	3.6	EBR-II, X259
K6B-75	MC+ 1% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87x6.86	68	1770	12	3.4	EBR-II, X259
K6B-76	MC+ 1% M ₂ C ₃	88	He-0.25	20CW-316SS	7.87x6.86	71	1810	12	3.5	EBR-II, X259
K6B-77	MC	89	He-0.25	20CW-316SS	7.87x6.86	67	1750	12	3.3	EBR-II, X259
K6B-78	MC	87	He-0.25	20CW-316SS	7.87x6.86	70	1790	12	3.4	EBR-II, X259
K6B-79	MC	88	He-0.25	20CW-316SS	7.87x6.86	68	1770	12	3.2	EBR-II, X259
K6B-80	MC+ 7% M ₂ C ₃	78	He-0.13	20CW-316SS	7.87x6.86	69	1430	12	3.4	EBR-II, X259
K6B-81	MC+ 7% M ₂ C ₃	78	He-0.13	20CW-316SS	7.87x6.86	67	1410	12	3.2	EBR-II, X259
K6B-82	MC+ 8% M ₂ C ₃	82	He-0.13	20CW-316SS	7.87x6.86	70	1440	12	3.4	EBR-II, X259
K6B-83	MC+ 7% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87x6.86	73	1830	12	3.7	EBR-II, X259
K6B-84	MC+ 6% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87x6.86	75	1870	12	3.8	EBR-II, X259
K6B-86	MC+ 7% M ₂ C ₃	88	He-0.25	20CW-316SS	7.87x6.86	77	1880	12	3.8	EBR-II, X259

TABLE D-XI (cont)

Exptl. No.	Fuel Type ^a	Fuel Density ^b (g T, D.)	Bond and Diametral Gap (mm)	Clad ^{c, d} Type	Clad o. d. x l. d. (mm)	Max Linear Power (kW/m)	Max Centerline Temp (°C)	Goal Burnup (at. %)	Max Current Burnup (at. %)	Status
K6B-87	MC + 7% M ₂ C ₃	89	He-0.25	20CW-316SS	7.87x6.86	72	1820	12	3.5	EBR-II, X259
K6B-88	MC + 9% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87x6.86	75	1860	12	3.7	EBR-II, X259
K6B-89	MC + 9% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87x6.86	78	1900	12	3.8	EBR-II, X259
K6B-90	MC + 9% M ₂ C ₃	88	He-0.25	20CW-316SS	7.87x6.86	73	1840	12	3.5	EBR-II, X259
K6B-91	MC + 9% M ₂ C ₃	88	He-0.25	20CW-316SS	7.87x6.86	76	1870	12	3.7	EBR-II, X259
K6B-92	MC + 9% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87x6.86	74	1850	12	3.5	EBR-II, X259
K6B-93	MC + 6% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87x6.86	74	1850	12	3.7	EBR-II, X259
K6B-94	MC + 9% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87x6.86	----	----	--	0	EBR-II, unassigned ^e
K6B-95	MC + 8% M ₂ C ₃	82	He-0.13	20CW-316SS	7.87x6.86	----	----	--	0	EBR-II, unassigned ^e
K6B-95	MC + 5% M ₂ C ₃	88	He-0.25	20CW-316SS	7.87x6.86	74	1840	12	0	EBR-II, unassigned ^e

^aM = (U_{0.8}Pu_{0.2}).

^bTheoretical density of MC = 13.45 Mg/m³ and of M₂C₃ = 12.72 Mg/m³.

^c20CW = 20% cold worked.

^dFFTF first core material, M-3-28.

^eReplacement element.

addition to the fourth-core stainless steel cladding used in the K9 Series, N-lot stainless steel and PE-16 claddings are also included. The N-lot stainless steel is included in order to compare the results from this subassembly with the results from the K7 and K8 Series, which utilize N-lot cladding. The PE-16 cladding is included in order to determine the relative degree of fuel swelling restraint by using stronger advanced claddings.

The K11 Series will test sodium-bonded, shrouded carbide elements at conditions simulating the peak power conditions in FTR. The test utilizes both fourth-core stainless steel and PE-16. The metallurgical condition (cold-worked vs solution-annealed) of the shroud tube is also a variable.

The K12 Series will also test sodium-bonded shrouded carbide elements at conditions that simulate the peak power conditions in FTR. However, in the K12 Series, the concepts of unshrouded fuel in 0.38-mm-thick PE-16 cladding and shrouded fuel in 0.30-mm-thick PE-16 cladding will be tested.

A study was performed to aid in the choice of ²³⁵U enrichments to be used in LASL Phase IA and Phase II advanced fuel experiments (experiments K7-K11). The primary objective was to minimize the number of different enrichments necessary to meet the irradiation conditions required for these experiments. Also, consideration was given to flexibility in choice of reactor location, maintaining satisfactory fluence-to-burnup ratios, and minimizing

TABLE B-XII

SERIES K7 CARBIDE EXPERIMENTS

Element Number	Fuel Type ^a	Fuel Density ^b (% T. D.)	Bond and Diametral Gap ^c (mm)	Clad ^d Type	Clad ^e o. d. x i. d. (mm)	Maximum Linear Power (kW/m)	Maximum Centerline Temp (°C)	Goal Burnup (at. %)	Maximum Current Burnup (at. %)	Status
K7-1	MC+10% M ₂ C ₃	99	Na-0.79	20CW-316SS	7.87 x 7.11	76	880	12	0.9	EBR-II, X280
K7-2	MC+10% M ₂ C ₃	99	Na-0.79	20CW-316SS	7.87 x 7.11	76	880	12	0.9	EBR-II, X280
K7-3	MC+7% M ₂ C ₃	98	Na-0.79	20CW-316SS	7.87 x 7.11	76	885	12	0.9	EBR-II, X280
K7-4	MC+7% M ₂ C ₃	98	Na-0.79	20CW-316SS	7.87 x 7.11	76	890	12	0.9	EBR-II, X280
K7-5	MC+8% M ₂ C ₃	99	Na-0.79	20CW-316SS	7.87 x 7.11	76	890	12	0.9	EBR-II, X280
K7-6	MC+8% M ₂ C ₃	99	Na-0.79	20CW-316SS	7.87 x 7.11	76	895	12	0.9	EBR-II, X280
K7-8	MC+9% M ₂ C ₃	99	Na-0.79	20CW-316SS	7.87 x 7.11	66	870	12	0	EBR-II, X246A
K7-9	MC+10% M ₂ C ₃	99	Na-0.79	20CW-316SS	7.87 x 7.11	66	880	12	0	EBR-II, X246A
K7-10	MC+10% M ₂ C ₃	99	Na-0.79	20CW-316SS	7.87 x 7.11	66	870	12	0	EBR-II, X246A
K7-11	MC+7% M ₂ C ₃	79	He-0.25	20CW-316SS	7.87 x 6.86	76	1880	12	0.9	EBR-II, X280
K7-12	MC+7% M ₂ C ₃	79	He-0.25	20CW-316SS	7.87 x 6.86	76	1900	12	0.9	EBR-II, X280
K7-14	MC+7% M ₂ C ₃	80	He-0.25	20CW-316SS	7.87 x 6.86	76	1900	12	0.9	EBR-II, X280
K7-15	MC+8% M ₂ C ₃	78	He-0.25	20CW-316SS	7.87 x 6.86	76	1900	12	0.9	EBR-II, X280
K7-17	MC+6% M ₂ C ₃	81	He-0.13	20CW-316SS	7.87 x 6.86	76	1480	12	0.9	EBR-II, X280
K7-18	MC+6% M ₂ C ₃	81	He-0.13	20CW-316SS	7.87 x 6.86	76	1490	12	0.9	EBR-II, X280
K7-19	MC+10% M ₂ C ₃	79	He-0.13	20CW-316SS	7.87 x 6.86	76	1490	12	0.9	EBR-II, X280
K7-20	MC+10% M ₂ C ₃	79	He-0.13	20CW-316SS	7.87 x 6.86	76	1490	12	0.9	EBR-II, X280
K7-23	MC+6% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87 x 6.86	76	1870	12	0.9	EBR-II, X280
K7-25	MC+6% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87 x 6.86	76	1880	12	0.9	EBR-II, X280
K7-26	MC+6% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87 x 6.86	76	1880	12	0.9	EBR-II, X280
K7-27	MC+7% M ₂ C ₃	86	He-0.25	20CW-316SS	7.87 x 6.86	76	1890	12	0.9	EBR-II, X280
K7-28	MC+7% M ₂ C ₃	86	He-0.25	20CW-316SS	7.87 x 6.86	76	1890	12	0.9	EBR-II, X280
K7-7	MC+9% M ₂ C ₃	99	Na-0.79	20CW-316SS	7.87 x 7.11	---	----	4-5	0	EBR-II - unassigned ^e
K7-16	MC+8% M ₂ C ₃	78	He-0.25	20CW-316SS	7.87 x 6.86	---	----	4-5	0	EBR-II - unassigned ^e
K7-21	MC+8% M ₂ C ₃	80	He-0.13	20CW-316SS	7.87 x 6.86	---	----	4-5	0	EBR-II - unassigned ^e
K7-30	MC+7% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87 x 6.86	---	----	4-5	0	EBR-II - unassigned ^e
K7-13	MC+7% M ₂ C ₃	80	He-0.25	20CW-316SS	7.87 x 6.86	---	----	---	---	LASL-unassigned
K7-22	MC+8% M ₂ C ₃	80	He-0.13	20CW-316SS	7.87 x 6.86	---	----	---	---	LASL-unassigned
K7-24	MC+6% M ₂ C ₃	87	He-0.25	20CW-316SS	7.87 x 6.86	---	----	---	---	LASL-unassigned
K7-29	MC+7% M ₂ C ₃	86	He-0.25	20CW-316SS	7.87 x 6.86	---	----	---	---	LASL-unassigned

^a M = U₉₂, Pu₉₄, Zr.^b Theoretical density of MC = 13.45 Mg/m³ and of M₂C₃ = 12.72/m³.^c K7-1 through K7-10 each contain a 20% cold-worked Type 316 stainless steel perforated shroud tube; dimensions are 6.55 mm o.d. x 6.40 mm i.d.^d 20CW = 20% cold-worked. Cladding is N-lot stainless steel.^e Replacement element.

TABLE D-XIII

SERIES K8 CARBIDE EXPERIMENTS

Element Number	Fuel Type ^a	Fuel Density ^b (% T. D.)	Bond and Diametral Gap ^c (mm)	Clad ^d Type	Clad o. d. x i. d. (mm)	Maximum Linear Power (kW/m)	Maximum Centerline Temp (°C)	Goal Burnup, (at. %)	Current Burnup, (at. %)	Status
K8-1	MC+9% M_2C_3	99	Na-0,79	20CW-316SS	7.87 x 7.11	46	750	12	0	EBR-II, X281
K8-2	MC+9% M_2C_3	99	Na-0,79	20CW-316SS	7.87 x 7.11	46	760	12	0	EBR-II, X281
K8-3	MC+10% M_2C_3	98	Na-0,79	20CW-316SS	7.87 x 7.11	46	750	12	0	EBR-II, X281
K8-4	MC+10% M_2C_3	98	Na-0,79	20CW-316SS	7.87 x 7.11	46	765	12	0	EBR-II, X281
K8-5	MC+8% M_2C_3	99	Na-0,79	20CW-316SS	7.87 x 7.11	46	795	12	0	EBR-II, X291
K8-6	MC+8% M_2C_3	99	Na-0,79	20CW-316SS	7.87 x 7.11	46	795	12	0	EBR-II, X291
K8-10	MC+8% M_2C_3	80	He-0,25	20CW-316SS	7.87 x 6.86	46	1485	12	0	EBR-II, X281
K8-11	MC+8% M_2C_3	80	He-0,25	20CW-316SS	7.87 x 6.86	46	1500	12	0	EBR-II, X281
K8-12	MC+6% M_2C_3	80	He-0,25	20CW-316SS	7.87 x 6.86	46	1505	12	0	EBR-II, X281
K8-13	MC+6% M_2C_3	80	He-0,25	20CW-316SS	7.87 x 6.86	46	1500	12	0	EBR-II, X281
K8-16	MC+7% M_2C_3	80	He-0,13	20CW-316SS	7.87 x 6.86	46	1190	12	0	EBR-II, X291
K8-17	MC+7% M_2C_3	80	He-0,13	20CW-316SS	7.87 x 6.86	46	1210	12	0	EBR-II, X291
K8-18	MC+8% M_2C_3	80	He-0,13	20CW-316SS	7.87 x 6.86	46	1235	12	0	EBR-II, X281
K8-19	MC+8% M_2C_3	80	He-0,13	20CW-316SS	7.87 x 6.86	46	1235	12	0	EBR-II, X291
K8-22	MC+7% M_2C_3	87	He-0,25	20CW-316SS	7.87 x 6.86	46	1470	12	0	EBR-II, X281
K8-23	MC+7% M_2C_3	87	He-0,25	20CW-316SS	7.87 x 6.86	46	1485	12	0	EBR-II, X291
K8-24	MC+7% M_2C_3	87	He-0,25	20CW-316SS	7.87 x 6.86	46	1510	12	0	EBR-II, X281
K8-25	MC+7% M_2C_3	87	He-0,25	20CW-316SS	7.87 x 6.86	46	1515	12	0	EBR-II, X281
K8-26	MC+7% M_2C_3	87	He-0,25	20CW-316SS	7.87 x 6.86	46	1510	12	0	EBR-II, X281
K8-7	MC+9% M_2C_3	98	Na-0,79	20CW-316SS	7.87 x 7.11	--	---	4-5	--	EBR-II, unassigned ^e
K8-8	MC+9% M_2C_3	98	Na-0,79	20CW-316SS	7.87 x 7.11	--	---	4-5	--	EBR-II, unassigned ^e
K8-14	MC+8% M_2C_3	81	He-0,25	20CW-316SS	7.87 x 6.86	--	---	4-5	--	EBR-II, unassigned ^e
K8-20	MC+8% M_2C_3	80	He-0,13	20CW-316SS	7.87 x 6.86	--	---	4-5	--	EBR-II, unassigned ^e
K8-27	MC+7% M_2C_3	87	He-0,25	20CW-316SS	7.87 x 6.86	--	---	4-5	--	EBR-II, unassigned ^e
K8-28	MC+7% M_2C_3	87	He-0,25	20CW-316SS	7.87 x 6.86	--	---	4-5	--	EBR-II, unassigned ^e
K8-9	MC+9% M_2C_3	99	Na-0,79	20CW-316SS	7.87 x 7.11	--	---	---	--	LASL, unassigned
K8-15	MC+8% M_2C_3	81	He-0,13	20CW-316SS	7.87 x 6.86	--	---	---	--	LASL, unassigned
K8-21	MC+8% M_2C_3	80	He-0,25	20CW-316SS	7.87 x 6.86	--	---	---	--	LASL, unassigned
K8-29	MC+7% M_2C_3	87	He-0,25	20CW-316SS	7.87 x 6.86	--	---	---	--	LASL, unassigned

^a M = $U_{0.8}Pu_{0.2}$.^b Theoretical density of MC = 13.57 Mg/m³ and of M_2C_3 = 12.78 Mg/m³.^c K8-1 through K8-8 each contain a 20% cold-worked Type 316 stainless steel perforated shroud tube; dimensions are 6.55 mm o.d. x 6.40 i.d.^d 20CW = 20% cold-worked. Cladding is N-10 stainless steel.^e Replacement element.^f Contains three fuel batches.

TABLE B-XIV
SERIES K9 NOMINAL DESIGN AND OPERATING PARAMETERS^a

Element Number	Initial Load	Replacement	Spare	Bond Type	Diametral Gap (mm)	Shroud Type ^c	Fuel Density (% T.D.)
K9-1 to K9-6	4	1	1	Na	0.76 ^b	CW	98
K9-7 to K9-10	3	--	1	Na	0.76 ^b	SA	98
K9-11 to K9-16	4	1	1	He	0.28	--	81
K9-17 to K9-22	4	1	1	He	0.15	--	81
K9-23 to K9-28	4	1	1	He	0.28	--	87

^aSubassembly Type: L-19.

Peak Linear Power: 69 kW/m (21 kW/ft).

Peak Cladding Temperature: 671°C (1240°F).

Design Burnup: 8 at.% (peak).

Fuel Type: (U_{0.8}Pu_{0.2})C + 10 ± 5 vol% (U_{0.8}Pu_{0.2})₂C₃.

Fuel Enrichment: 40% ²³⁵U.

Cladding: FFTF fourth-core 20% cold-worked Type 316 stainless steel,
9.40 x 8.38 mm for all helium-bonded elements and 9.40 x 8.64-mm-diam
for all sodium-bonded elements.

^bPellet o.d. = 7.72 mm.

^c7.93 x 7.77-mm-diam Type 316 stainless steel,
CW = 20% cold-worked,
SA = solution-annealed.

TABLE B-XV
SERIES K10 NOMINAL DESIGN AND OPERATING PARAMETERS^a

Element Number	Initial Load	Replacement	Spare	Cladding Type ^b	Diametral Gap (mm)
K10-1 to K10-4, K10-23, K10-27	4	1	1	20CW316 (Core 4)	0.28
K10-5 to K10-10, K10-24	5	1	1	20CW316 (N-lot)	0.28
K10-11 to K10-16, K10-25	5	1	1	PE-16	0.15
K10-17 to K10-22, K10-26	5	1	1	PE-16	0.28

^aSubassembly Type: L-19.

Peak Linear Power: 69 kW/m (21 kW/ft).

Peak Cladding Temperatures: 654°C (1210°F).

Design Burnup: 8 at.% (peak).

Fuel Type: (U_{0.8}Pu_{0.2})C + 10 ± 5 vol% (U_{0.8}Pu_{0.2})₂C₃.

Fuel Enrichment: 40% ²³⁵U.

Fuel Density: 87% T.D.

Bond Type: Helium.

^b9.40 x 8.38-mm-diam 20CW316 = 20% cold-worked Type 316 stainless steel.
PE-16 = solution-treated PE-16.

TABLE B-XVI
SERIES K11 NOMINAL DESIGN AND OPERATING PARAMETERS^a

Element Number	Initial Load	Replacement	Spare	Shroud Type ^b	Cladding Type ^c
K11-1 to K11-7	5	1	1	CW	20CW316
K11-8 to K11-13	4	1	1	A	20CW316
K11-14 to K11-20	5	1	1	CW	PE-16
K11-21 to K11-27	5	1	1	A	PE-16

^aSubassembly Type: L-19.
Peak Linear Power: 98 kW/m (30 kW/ft).
Peak Cladding Temperature: 593°C (1100°F).
Design Burnup: 12 at.% (peak).
Fuel Type: $(U_{0.6}Pu_{0.2})C + 10 \pm 5 \text{ vol\% } (U_{0.6}Pu_{0.2})_2C_3$.
Fuel Enrichment: 72% ²³⁵U.
Pellet Density: >98% of T.D.
Pellet Diameter: 7.72 mm.
Bond Type: Sodium

^bType 316 stainless steel, 7.98 x 7.82-mm-diam.
CW = 20% cold-worked.
A = solution-annealed.

^c9.40 x 8.54-mm-diam.
20CW316 = 20% cold-worked Type 316 stainless steel (core 4).
PE-16 = solution-treated PE-16.

TABLE B-XVII
SERIES K12 NOMINAL DESIGN AND OPERATING PARAMETERS^a

Element Number	Initial Load	Replacement	Spare	Shroud Type ^b	Cladding Type ^c	Cladding Thickness (mm)	Design Burnup (at.%)	Pellet Diameter (mm)
K12-1 to K12-9	6	2	1	--	PE-16	0.38	12	7.82
K12-10 to K12-16	5	1	1	--	PE-16	0.38	8	8.08
K12-17 to K12-22	4	1	1	CW	PE-16	0.30	12	7.82
K12-23 to K12-28	4	1	1	A	PE-16	0.30	12	7.82

^aSubassembly Type: L-19.
Peak Linear Power: 98 kW/m (30 kW/ft).
Peak Cladding Temperature: 593°C (1100°F).
Fuel Type: $(U_{0.6}Pu_{0.2})C + 10 \pm 5 \text{ vol\% } (U_{0.6}Pu_{0.2})_2C_3$.
Fuel Enrichment: 72% ²³⁵U.
Pellet Density: >98% T.D.
Bond Type: Sodium.

^bType 316 stainless steel, 8.15 x 8.00-mm-diam.
CW = 20% cold-worked.
SA = solution-annealed.

^cSolution-treated, 9.40-mm-o.d.

the reactivity burdens of the experiments. It was determined that an enrichment of 72% ^{235}U in uranium was appropriate for the high-power irradiation tests K7 and K11, and 40% ^{235}U in uranium for the low-power irradiation tests, K8, K9, and K10.

Several computer codes were acquired and implemented to aid in the structural design and analysis of fuel elements and assemblies. A single code had been sought that would perform elastic and inelastic analyses of structures subjected to thermal and mechanical conditions expected in advanced fuel elements. Flexibility to model components as plane, solid, or shell structures was desired. A brief investigation of more than 20 existing stress analysis codes was performed. Each was considered in light of its capabilities, limitations, documentation, ease of use, and availability. Based on the above criteria, no single code was identified that would meet all program requirements.

Five codes with somewhat different capabilities were selected and implemented on the LASL computers. One code, SABOR,¹ is capable of treating linear-elastic response of structures composed of two-dimensional thin-shell elements. Three codes--TSAAS,² PLACID,³ and CREEP-PLAST⁴--for analyzing two-dimensional structures under mechanical and thermal loading were selected. Although these codes have some common features and capabilities, each has unique features that are applicable to fuel element and assembly design and analysis. Test problems have been successfully executed with each of the codes. One code, EPACA,⁵ which is capable of analyzing three-dimensional structures, was acquired and implemented on the LASL computers.

2. Evaluation of Temperatures in Irradiation Experiments

Calculations of fuel-element temperatures in three advanced-fuel subassemblies have been performed with the SATEMP code for beginning-of-life conditions in EBR-II. The subassemblies are the U-5100 Series elements in X156, the K4/U-5100 Series elements in X246, and the C5/ON1/K4 Series elements in X163. These elements were all irradiated as wire-wrapped elements in E-19B subassemblies. Fission rates and sodium coolant flow were obtained from EBR-II postrun information. These data were reduced by 9% to obtain the best estimate of actual EBR-II operating conditions.

The U-5100 elements in X156 contained sodium-bonded carbide fuel, 7 were shrouded, 11 were unshrouded and 1 was a dummy element. Figure B-1 shows the subassembly layout and the sodium coolant temperatures at the core outlet.

The sodium coolant flow rate was taken as 3762 g/s. Table B-XVIII lists the peak linear power and peak fuel temperature of each element along with the maximum and minimum cladding inside-surface temperatures at the core outlet.

The K4/U-5100 Series elements in X246 contained shrouded, sodium-bonded carbide and nitride fuels. The subassembly contained the seven shrouded elements from X156 along with six new shrouded, carbide-fueled elements (K4-1 to K4-6) and six new shrouded, nitride-fueled elements (K4-8 to K4-12 and K4-17). Figure B-2 shows the subassembly layout and the sodium coolant temperatures at the core outlet. The sodium coolant flow rate was taken as 2864 g/s. Table B-XIX lists the peak linear power and peak fuel temperature of each element along with the maximum and minimum cladding inside surface temperatures at the core outlet. The fissile atom content of the U-5100 Series elements in X246 was reduced by 3% to account for burnup that occurred while these elements were in X156.

Fig. B-1.
Beginning-of-life conditions for U-5100 Series in subassembly X156, run 63A, core location 4B1.

Fig. B-2.
Beginning-of-life conditions for K4/U-5100 Series in subassembly X246, run 78B, core location 6B1.

TABLE B-XVIII
BEGINNING-OF-LIFE CONDITIONS FOR U-5100 SERIES
ELEMENTS IN SUBASSEMBLY X156^a

Element Number ^b	Peak Linear Power (kW/m)	Peak Fuel Temp. (°C)	Cladding Inside Surface Temp. at Core Outlet (°C)	
			Maximum	Minimum
U241	106.4	1065	625	580
U242	106.4	1080	630	585
U243	99.5	1045	605	565
U244	106.3	1070	610	575
U245	98.9	1050	615	570
U246	108.1	1085	635	585
U247	99.2	1040	595	560
U248	97.7	1045	620	570
U249	107.7	1075	605	575
U250	97.6	1060	625	600
U251	98.0	1055	620	590
U252	98.1	1035	590	560
U253	96.6	1055	630	610
U254	96.4	1055	630	605
U256	99.9	1025	610	570
U257	99.5	1055	630	620
U258	99.5	1045	625	600
U259	100.6	1020	580	560

^aCore location 4B1 for Run 63A.

^bElement U255 was defective and replaced by a dummy element.

TABLE B-XIX
BEGINNING-OF-LIFE CONDITIONS FOR K4/U-5100
SERIES ELEMENTS IN SUBASSEMBLY X246^a

Element Number ^b	Peak Linear Power (kW/m)	Peak Fuel Temp. (°C)	Cladding Inside Surface Temp. at Core Outlet (°C)	
			Maximum	Minimum
U252 ^b	69.9	885	600	530
U253 ^b	66.2	880	610	580
U254 ^b	65.9	890	610	600
U256 ^b	68.8	855	580	535
U257 ^b	67.1	875	600	580
U258 ^b	66.8	875	605	575
U259 ^b	68.3	850	570	525
K4-1 ^b	83.1	895	585	540
K4-2 ^b	83.0	910	605	555
K4-3 ^b	83.4	905	590	550
K4-4 ^b	81.0	900	605	550
K4-5 ^b	76.1	875	590	540
K4-6 ^b	74.1	850	570	530
K4-8 ^c	76.8	920	575	535
K4-9 ^c	76.2	925	590	540
K4-10 ^c	83.2	990	620	590
K4-11 ^c	82.7	990	620	585
K4-12 ^c	80.2	975	615	580
K4-17 ^c	78.7	935	595	540

^aCore location 6B1 for Run 78B.

^bCarbide-fueled element.

^cNitride-fueled element.

The C5/ON1/K4 elements in X163 were sodium bonded with 18 containing nitride fuel and 1 containing carbide fuel. The 16 C5 and ON1 Series elements are unshrouded, whereas the 3 K4 elements have perforated shroud tubes. Figure B-3 shows the subassembly layout and the sodium coolant temperatures at the core outlet. The sodium coolant flow rate was taken as 3127 g/s. Table B-XX lists the peak linear power and peak fuel temperature of each element along with the maximum and minimum cladding inside-surface temperatures at the core outlet.

Beginning-of-life and lifetime-average power profiles were obtained for elements B-3-6, B-3-7, and B-3-8 from burnup values and irradiation history. The burnup of B-3-7 had been measured, and nominal EBR-II burnup values were used for the other two elements. These power profiles, along with the sodium flow rates were used to determine the beginning-of-life and lifetime-average peak cladding temperatures. These values are presented in Table XXI. The linear powers and sodium flow rates were relatively constant throughout the operation of these elements. The temperatures presented should, thus, be representative of the entire irradiation period.

3. Fuel Temperatures and Gap Sizes in Helium-Bonded Carbide Fuel Elements

During the design and safety analysis of helium-bonded elements, an estimate of the maximum fuel temperature is required. Beginning-of-life fuel temperature calculations at LASL account for conduction through the gas in the fuel-cladding gap and radiation from the fuel surface to the cladding inside surface. The fuel and cladding are assumed to be concentric, and their diameters are corrected for thermal expansion. Fuel centerline temperatures calculated in this manner should represent the maximum temperatures attained in the element before fuel pellet

Fig. B-3.
Beginning-of-life conditions for C5/ON1/
K4 Series in subassembly X163, run 78B,
core location 7D4.

TABLE B-XX
BEGINNING-OF-LIFE CONDITIONS FOR C5/ON1/K4 SERIES
ELEMENTS IN SUBASSEMBLY X163^a

Element Number	Peak Linear Power (kW/m)	Peak Fuel Temp. (°C)	Cladding Inside Surface Temp. at Core Outlet (°C)	
			Maximum	Minimum
C5-6	80.9	1000	615	585
C5-7	82.9	1005	620	595
C5-8	80.2	970	585	545
C5-9	82.8	985	600	555
C5-10	80.9	965	580	550
C5-11	82.3	975	600	550
C5-13	80.5	960	590	550
C5-14	79.9	985	615	590
C5-15	80.1	970	600	550
C5-16	79.6	985	615	595
C5-19	80.6	975	605	555
C5-20	82.0	995	620	585
ON1-1	79.9	1000	605	555
ON1-3	79.5	1025	620	615
ON1-4	79.6	1025	615	585
ON1-8	80.0	995	580	545
K4-14 ^b	84.9	915	600	550
K4-15	79.6	945	575	535
K4-16	80.9	950	590	540

^aCore location 7D4 for Run 78B.

^bCarbide-fueled element.

TABLE B-XXI
CALCULATED PEAK CLADDING TEMPERATURES
OF HELIUM-BONDED NITRIDE FUEL ELEMENTS (°C)

Element Number	Beginning of Life	Lifetime Average
B-3-6	676	666
B-3-7	690	682
B-3-8	654	645

cracking and rearrangement or swelling reduces the gap size, and thereby, the temperature drop across the gap.

As a check on the method, maximum fuel temperatures were calculated for a series of gas-bonded (U,Pu)C test elements in which central thermocouples were used to measure fuel temperatures during irradiation.⁶ The calculations were complicated by the fact that the irradiation was performed in a thermal reactor, which resulted in radial variation of the fuel volumetric power generation. Data on the power as a function of radius were not presented in Ref. 6, but two parameters were given that allowed the radial variation to be estimated.

These are

(1) edge/center power generation ratio = 3.5, and

$$(2) \quad \frac{4\pi \int_{T_s}^{T_c} k dT}{\chi} = 0.685$$

In the latter expression, k is the fuel thermal conductivity, T is the temperature and subscripts s and c refer to fuel surface and center, respectively. The quantity χ was not specifically defined, but it is assumed to be the power generated in a unit axial length of fuel (average linear power). The conductivity integral can be related to the volumetric power generation by the following,

$$\int_{T_s}^{T_c} k dT = \int_0^a \frac{\int_0^r q'(\eta) \eta d\eta}{r} dr, \quad (1)$$

where $q'(r)$ is the volumetric power generation (W/cm^3) at radius r , and a is the fuel surface radius. It was assumed that the relative radial power generation could be represented by the function,

$$\frac{q'(r)}{q'(a)}, = A + B \left(\frac{r}{a}\right)^n, \quad (2)$$

where A, B, and n are constants. Using the data from Ref. 6, the following values were obtained for the constants: A = 0.2857, B = 0.7143, and n = 3.75.

Calculations were performed for four of the eight test elements from Ref. 6 with the relative power generation given by Eq. (2). The other elements were not considered because the authors⁶ felt that their measured maximum fuel temperatures were not reliable owing to fuel melting or faulty instrumentation. Results of these calculations are compared with the measured temperatures in Table B-XXII. The calculations were performed for solid pellets, while the fuel pellets in the test elements had a 2.2-mm-diam central hole to accommodate the thermocouples. The calculated central temperatures should be ~25°C above the temperatures at the fuel-thermocouple interface. The agreement is quite good for L4-2 and L4-7, fair for L4-1, and poor for L4-3. It is difficult to see how the central temperatures of L4-3 and L4-1 could be similar when L4-3 has a larger gap and a lower thermal conductivity gap gas than L4-1. Contamination of the gap gas by gaseous impurities liberated from the fuel was reported as possible, but no postirradiation gas analyses were available for the four test elements in Table B-XXII.

TABLE B-XXII
COMPARISON OF MEASURED AND CALCULATED FUEL TEMPERATURES

	Test Element			
	L4-1	L4-2	L4-3	L4-7
Initial Radial Gap ^a (mm)	0.025	0.125	0.040	0.125
Gas Gap ^a	He	He	Ar	He
Average Linear Power ^a (kW/m)	94.7	92.8	101.0	94.5
Cladding o.d. Temp. ^a (°C)	470	500	470	460
<u>Calculated Temp. (°C)</u>				
Cladding i.d.	552	579	556	542
Fuel Surface	757	1290	1288	1264
Fuel Center	1105	1600	1625	1580
Radial Gap at Temp. (mm)	0.021	0.091	0.0099	0.089
Maximum Measured Fuel Center Temp. ^a (°C)	1260	1580	1280	1550

^aReported in Ref. 16.

This same calculational method was used to determine the relationship between the initial gap size and the maximum fuel temperature for 9.40-mm-o.d., helium-bonded carbide fuel elements under consideration for irradiation. Table B-XXIII shows element parameters used in the calculations. The calculations were performed with PINTEMP,⁷ allowing conduction and radiation across the gas gap. The gap size was corrected for fuel and cladding thermal expansion. Thus, the fuel temperatures calculated represent the maximum beginning-of-life conditions and do not account for changes in gap size as a result of fuel cracking and rearrangement or fuel swelling.

The maximum fuel-centerline temperature and the fuel-surface temperature at the same axial location are shown in Fig. B-4 as a function of initial gap size. The maximum fuel-centerline temperature occurred just above the core midplane. The limit for the initial gap size can be taken as the gap size at which the maximum fuel temperature reaches the fuel solidus temperature (2290°C). Table B-XXIV shows the gap sizes for this condition. The largest initial gap size allowable for the nominal conditions (81% T.D. fuel and 98.4 kW/m peak linear power) is 0.350 mm. Actual conditions can differ from the nominal because of a variation in fuel density or a variation in linear power. The actual density of a nominal 81% dense fuel can be between 78% and 82% T.D. Fuel at the lower density limit will have a lower thermal conductivity, but it will also have a lower peak linear power because the fissile atom density is reduced. The linear power variation across the subassembly is difficult to assess at this time, but a variation of $\pm 5\%$ (93.5 to 103.3 kW/m) was assumed

TABLE B-XXIII

HELIUM-BONDED-ELEMENT TEST PARAMETERS

- Peak Linear Power - 105 kW/m (32 kW/ft) and 98.4 kW/m (30 kW/ft).
- Fuel Density - 81% and 87% theoretical density.
- Cladding - 9.40 mm (0.370 in.) o.d. by 8.38 mm (0.330 in.) i.d.
- Maximum Cladding Temperature - 650°C.
- EBR-II Location - Row 5.
- Gap Gas - 93% He plus 7% Xe.
- Initial Diametral Gap - 0.127 mm (5 mils) to 0.356 mm (14 mils).

Fig. B-4.

Maximum fuel temperatures for 9.40-mm-diam helium-bonded carbide fuel elements.
(a) 81% T.D. fuel, (b) 87% T.D. fuel.

TABLE B-XXIV

INITIAL GAP SIZES FOR MAXIMUM FUEL TEMPERATURE
AT THE SOLIDUS TEMPERATURE

<u>Peak Linear Power</u>	<u>81% T.D.</u>	<u>87% T.D.</u>
98.4 kW/m	0.350 mm	0.368 mm
105 kW/m	0.323 mm	0.343 mm

for this study. Considering the worst case (78% T.D. fuel at 103.3 kW/m peak linear power), the largest initial gap size allowable is 0.318 mm. The anticipated fuel-diameter tolerance for helium-bonded elements is ± 0.025 mm and the cladding-inside-diameter tolerance should be ± 0.013 mm. To ensure a maximum initial gap of 0.318 mm in an element, the nominal initial gap should be no larger than 0.279 mm.

4. Analysis and Design of Dished Fuel Pellets

The use of fuel pellets with dished ends has been proposed as a design variable for helium-bonded advanced fuel element irradiations. The function of dished (concave) ends on fuel pellets is to reduce axial changes in the fuel-column length during irradiation. The combined action of axial fuel-column expansion and fuel-cladding mechanical interaction can lead to axial stresses on the cladding. In addition, changes in the fuel-column length influence the system reactivity.

The radial temperature gradient that exists in a fuel element during normal steady-state operation leads to changes in the shape of a fuel pellet with free surfaces.^{8,9} Pellet ends that are flat at room temperature become convex at operating conditions. For this reason, the free thermal expansion of a stack of pellets with flat ends would be greater than that which is computed on a plane strain basis.⁸ Since the radial center of the fuel is hotter than the edge, the center will be more plastic and will swell more during irradiation.¹⁰ Some of the swelling can be accommodated by the radial gap, but axial expansion of the central core of fuel pellets due to swelling can also occur. Thus, there are both rapid (thermal expansion) and slow (swelling) processes in which the central core of a fuel pellet expands more than the edges in the axial direction.

If no fuel-cladding mechanical interaction existed in an element, the axial fuel-column-length changes would have little influence on element lifetime. Reactivity effects would then control the allowable length changes. But fuel-cladding mechanical interaction does occur, particularly in helium-bonded elements. Radial expansion of the fuel from both thermal expansion and swelling causes radial forces on the cladding. Forces from axial expansion of the fuel will then be transmitted to the cladding. The friction coefficient between the fuel and cladding, in addition to the axial expansion of the fuel, will determine the axial cladding deformation. A more severe problem could arise if a physical bond exists between the fuel and cladding, i.e., if the

fuel adheres to the cladding. In this case, no axial sliding would occur, and the axial cladding deformation would be a maximum. During postirradiation examinations, some fuel-element sections have been split axially so that the fuel could be removed and the inside surface of the cladding examined. In a number of helium-bonded elements, the fuel was found to be bonded to the cladding.

During postirradiation examinations of helium-bonded elements, evidence of greater expansion and swelling in the central core of fuel pellets has been observed. Figure B-5 shows betatron radiographs at the top of the fuel column from two helium-bonded carbide fuel elements, U131 and U132, from subassembly X055B. The axial interpellet spacing caused by swelling of the central core of the fuel pellets is evident. The pellet separations occurred in the upper 30% of the fuel column in U131 and in the upper 40% of the fuel column in U132.

Figure B-6 shows a betatron radiograph of the top of the fuel column from the helium-bonded carbide element U201 (90 kW/m) from subassembly X182. There is no pellet separation along the fuel column, but the central core of the top fuel pellet (which was axially unrestrained) has expanded upward approximately 0.36 mm and moved the top insulator pellet upward. Figure B-7 shows a magnified view of the same phenomenon in a photomosaic of a longitudinal section from the top of the element.

Figure B-8 shows a photomosaic of a longitudinal section at the interface between the first and second pellets from the top of the irradiated, helium-bonded carbide element used in the UL-3 transient overpower test. The central core of the second pellet, which was restrained to some extent by the fuel pellet above it, expanded upward about 0.2 mm. This expansion was not evident in the radiographs of the element. It is not known whether this expansion occurred during the steady-state irradiation in X055, during the UL-3 transient overpower test, or possibly during both periods.

The postirradiation examination evidence has shown that axial expansion of the central core of fuel pellets in helium-bonded elements occurs when the pellets are relatively unrestrained, i.e., near the top of the fuel column. In elements such as U131 and U132, which operated at very low power, the fuel-cladding mechanical interaction may be small enough to allow upward sliding of the pellets and formation of axial gaps at the pellet edges as a result of swelling of the pellet core (see Fig. B-5). In the lower half of the fuel column, which is restrained by the cumulative effect of fuel-cladding mechanical

U131

U132

U201

Fig. B-5.
Betatron radiographs of helium-bonded
carbide elements U131 and U132.

Fig. B-6.
Betatron radiograph of helium-bonded
carbide element U201.

Fig. B-7.
Photomosaic of a longitudinal section from the top of the fuel column from helium-bonded carbide element U201.

Fig. B-8.
Photomosaic of a longitudinal section at the interface between the two top-most pellets from irradiated, helium-bonded carbide element used for the UL-3 test.

interaction of the pellets above, neither upward swelling of pellet cores nor edge gaps have been observed. These observations indicate that the axial forces transmitted to the cladding will be small near the top of a fuel column, where axial expansion of the fuel-pellet central cores can occur. Axial forces on the cladding will increase going down the fuel column and reach a maximum, where any axial expansion of the fuel-pellet central core is restrained by the fuel column above.¹¹

To design a useful dished end for a fuel pellet, both the depth and the diameter of the dish should be considered. Based on the above discussions, an upper limit for the dish depth of 0.38 mm is reasonable. The diameter of the dish should cover the area of the pellet that shows significant swelling. Examination of the swelled regions in U201 and two other elements, U189 (81 kW/m, 0.25-mm-diam gap) and U187 (77 kW/m, 0.18-mm-diam gap), indicated that the diameters of the swelled central regions of the fuel pellets were 3.94 mm to 4.80 mm. Therefore, a dish diameter of 4.57 mm seemed to be appropriate. The LASL pellet design utilizes a chamfer, which results in a diameter

of 5.84 mm on the flat pellet surface. Therefore, the annular flat pellet surface with the dish and the chamfer would have a width of 0.63 mm. This dimension appears adequate for support of the pellet stack.

Both the dish diameter (4.57 mm) and the dish depth (0.38 mm) can be achieved by defining the dish as a spherical segment of radius 7.06 mm. This design would reduce the fuel smear density by 2.54%. An increase in the fuel pellet density could be used to offset this reduction in smear density. This design should be considered as the largest dish needed to eliminate axial fuel-column expansion. For testing the efficacy of dished pellets, a range of dish sizes could be employed.

C. Postirradiation Examination Results

As indicated in the status columns of Tables B-II through B-VIII, most of the experiments are in various stages of their postirradiation examination. The final results for those elements that have completed the examination sequences will be reported in topical reports. This section will describe significant examination results, although complete results may not be available for particular elements. This was done to avoid undue delays in reporting interesting results before topical reports are issued. Any trends indicated in the following discussions should be considered to be preliminary, because the complete examination results may alter any initial interpretations.

During FY 1976 and 1976T, a total of 52 experiments were processed to various degrees through the postirradiation examinations. These examinations ranged from the initial examinations of 14 elements from subassembly X233 through the partial examination of 18 elements that began their examination sequence in prior years and have not yet been completed, to the completion of examination sequences for 20 elements that were also started in prior years.

Because the examination sequences involved one or more elements from each of the series as described in Tables B-II through B-VIII, the following discussions will summarize the extent to which each major examination sequence was involved in the overall program. Significant findings from individual series will be discussed in Sec. B.II.C.2. (Preliminary Interpretations.)

1. Examination Sequence

a. Gamma Scan. Gross gamma scans were performed during the year on 14 elements from subassembly X233. These elements were: K39B, W3F, W5F, W7F, W10F, W12F, B-1-4, B-2-1, B-2-5, B-3-6, B-3-8, U203, U260, and U262.

Multispectral gamma scans were also performed on K39B, W5F, W12F, and B-1-4. Significant gamma-scan observations will be discussed in Sec. B.II.C.2.

b. Profilometry. Optical profilometry measurements were made on the 14 capsules and the 9 unbreached elements from subassembly X233 (W3F, W7F, W10F, B-2-1, B-2-5, B-3-6, B-3-8, U260, and U262). Mechanical profilometry measurements were made on seven breached elements: U129, U135, U136, U137, U141, U145, and U190. Micrometer measurements at 12.7-mm intervals were made on element U193 because of its severely bent condition (damaged during an HFF incident). Profilometry measurements were waived for elements U114 and U188 because of their badly ruptured condition. The results of the fuel element profilometry measurements are summarized in Table B-XXV.

c. Decapsulation. Twenty-seven experiments were decapsulated during the year. Of these, 16 were classified or treated as breached experiments. The breached elements were: U114, U129, U135, U136, U137, U139, U141, U145,

TABLE B-XXV
SUMMARY OF PROFILOMETRY MEASUREMENTS

<u>Element</u>	<u>Fuel Density (%T.D.)</u>	<u>Cladding Thickness (mm)</u>	<u>Burnup (at.%)</u>	<u>Max Average $\Delta D/D$ (%)</u>
U129 ^a	86	0.55	11.37	2.80
U135 ^a	85	0.54	11.20	2.83
U136 ^a	85	0.54	11.00	2.05
U137 ^a	97	0.55	10.00	1.80
U141 ^a	91	0.55	10.50	2.40
U145 ^a	90	0.45	10.39	2.72
U190	85	0.54	10.50	3.31
U260	98	0.43	2.8 ^b	0.27
U262	96	0.37	2.8 ^b	0.24
W3F	93	0.31	8.1 ^b	0.43
W7F	90	0.31	8.1 ^b	0.17
W10F	89	0.26	8.0 ^b	0.28
B-2-1	95	0.52	8.5 ^b	0.51
B-2-5	94	0.40	5.6 ^b	0
B-3-6	95 ^c	0.51	8.2 ^b	2.47
B-3-8	90 ^c	0.51	8.0 ^b	2.31

^aBreached element.

^bNominal EBR-II burnup value.

^cCored fuel pellets.

U188, U190, U193, U199, U203, W5F, W12F, and B-1-4. Two TREAT experiments, UL-1 and UL-2, were included in the unbreached category, along with the nine unbreached elements from subassembly X233.

d. Breach Point Detection. Most of the 16 breached elements that were decapsulated had obvious breach points. Some required a helium pressurization test in order to locate the breach point; other breach points were not located. Visually detected breach points occurred in the following elements: (1) U129 (helium-bonded carbide, 86% T.D.) had a longitudinal crack, approximately 1.6 mm in length, located at 171 mm from the bottom of the element; (2) U135 (helium-bonded carbide, 85% T.D.) had a transverse crack located at 140 mm from the bottom of the element; (3) U137 (helium-bonded carbide, 97% T.D.) had a longitudinal crack, approximately 6.4 mm in length, located at 197-203 mm from the bottom of the element; (4) U139 (helium-bonded carbide, 97% T.D.) had a longitudinal crack extending from 163 mm to 221 mm from the bottom of the element, and it also had a hot spot with a corresponding transverse crack at 248 mm from the bottom of the element; (5) U145 (sodium-bonded carbide, 90% T.D.) had a longitudinal crack at 184 mm from the bottom of the element and possibly a second breach point with a deposit of unidentified material (probably caused by gas blanketing) just above the breach point; (6) U188 (helium-bonded carbide, 85% T.D.) had a large, spiraling rupture that extended from 58 mm to 224 mm from the bottom of the element with tiny globules associated with branching portions of the crack (probably caused by gas blanketing); (7) U114 (helium-bonded carbide, 98% T.D., cored) had ruptures that extended from 32 mm to 89 mm and again from 216 mm to 298 mm from the bottom of the element with evidence of associated gas blanketing; (8) W5F (sodium-bonded carbide, 91% T.D.) a complete circumferential break at 61 mm from the bottom of the element and secondary transverse cracks at 33 mm and 51 mm from the bottom of the element; (9) W12F (sodium-bonded carbide, 97% T.D.) had a complete circumferential break at 94 mm from the bottom of the element; (10) B-1-4 (sodium-bonded nitride, 94% T.D.) had multiple transverse cracks at 23, 41, 53, 102, 121, and 142 mm from the bottom of the element.

The breach points in four elements could not be detected visually, and three of these elements were subjected to a helium pressurization test. Element U136 showed no evidence of bubbles in the room-temperature alcohol-bath test at helium pressures up to 3.45 MPa (500 psig), but a small pressure drop was observed in the pressurized element at ~150°C. This suggests that very

small failure points may be open at operating temperatures. Element U141 (helium-bonded carbide, 91% T.D.) showed bubbles coming from what appeared to be a small transverse crack at 185 mm from the bottom of the element. Element U193 (sodium-bonded carbide, 92% T.D.) had to be heated during the helium pressurization test. At a pressure of 1.38 MPa (200 psig), small drops of molten sodium were observed coming from what appeared to be a cladding pinhole that was located at 114 mm from the bottom of the element.

Two elements, U190 and U199, although indicated as unbreached by gamma-scan and fission-gas measurements, were treated as breached elements because they were damaged at HFEF. Element U199 was found to be completely broken near the major damage point in the capsule. Since there was no evidence of element breaching before the incident and no fission gas was found in the capsule plenum, it is presumed that the element break was caused by the incident after the sodium had solidified. No breach points were found in element U190.

e. Sectioning. Twenty-five elements were sectioned during the year, and appropriate samples from 21 elements were submitted for burnup and cladding-density determinations and metallographic examinations. The other four elements (U114, U245, U247, and U249) were sectioned only for storage and eventual recovery purposes. The sectioned elements included: U129, U133, U134, U135, U136, U137, U140, U141, U143, U144, U145, U146A, and U147 from Series U-1950, U188, U190, U193, and U199 from Series U-1930 and 1960, B-3-6 and B-3-8 from Series B-3, and UL-1 and UL-2 from TREAT experiments. The metallographic samples from elements U129, U135, U137, and U145 were stored pending the completion of examinations on higher priority elements.

f. Fission Gas Release. Fission-gas-release calculations were completed for 19 elements. These data are included in Sec. B-II.D.1. Some of the release values are only approximate because final burnup values were not available. Special fission determinations were made for the TREAT experiments, UL-1 and UL-2, because of their very small burnup values.

g. Metallography. Fifty-five new samples were submitted during the year for metallographic examination. These samples were from 17 of the sectioned elements. During the year 61 metallography samples were completed; these included 21 samples from 8 of the newly sectioned pins, and the remaining 40 samples were from 12 pins that had been sectioned in prior years. Thirty-one samples from nine newly sectioned pins are ready for metallographic examination. The completed samples were from elements U98, U106, U130, U131, U132,

U136, U138A, U140, U141, U142, U146A, U147, U188, U196, U201, U207, U209, UL-1, UL-2, and UL-3. The uncompleted samples were from elements U133, U134, U143, U144, U190, U193, U199, B-3-6, and B-3-8. In addition, microprobe studies were made on eight samples: four from U131, one from U132, one from U142, one from U201, and one from U209. Sequential grinding techniques were used on four metallographic samples in order to study the development of the detected breach points. The results of these examinations are discussed in Sec. B.II.D.6.

h. Burnup Determinations. Burnup analyses were made on 20 samples, both from elements sectioned during the year and from a few elements that had been sectioned in FY 1975.

i. Cladding Density Measurements. Cladding density measurements were made on 64 samples from 16 elements. An additional 54 samples were submitted for analysis from 10 newly sectioned elements.

2. Preliminary Interpretations

The completed elements, as indicated in Tables B-II to B-VIII, are in the final data-reduction stages. The remainder of the elements are either in various stages of examination or in long-term storage. Preliminary interpretations have been made for some elements, even though the examination sequences have not been completed. Final conclusions must await the outcome of the data reduction and completion of all examinations.

a. Series K1, K2, and K3. The irradiation of all elements in these series has been completed. The final element, K39B, completed its irradiation in subassembly X233. It showed evidence of cladding breach at a burnup of approximately 8.5 at.%. The capsule was examined but, because of the probability of a large cladding breach, the capsule was given a low examination priority. It has been placed in long-term storage with other elements from these series.

b. Series U-1300. One breached element from this series was decapsulated during the year. Element U114 (helium-bonded carbide, 98% T.D. cored fuel) had an extensive longitudinal breach, which nearly covered the length of the fuel stack. One breach extended from 32 mm to 89 mm and again from 216 mm to 298 mm from the bottom of the element. A second major breach occurred on the opposite side of the element and extended from 140 mm to 171 mm from the bottom of the element. There was considerable evidence of gas blanketing associated with these breaches. Because of this severely damaged condition,

all destructive examination plans for element U114 were cancelled. The final two unexamined elements in this series, unbreached elements U110 and U113, will probably be examined in FY 1977.

c. Series U-1950. The postirradiation examinations of ten elements in this series were completed. These included: (1) unbreached elements U130, U138A, U140, U142, U146A, and U147, and (2) breached elements U131, U132, U136, and U141. The other five breached elements in this series (U129, U135, U137, U139, and U145) were decapsulated; their breach points are described in Sec. B.II.C.1.d. The remaining four unbreached elements in this series (U133, U134, U143, and U144) have been sectioned, and appropriate samples have been submitted for metallographic, burnup, and cladding-density analyses. Elements U133 and U134 were originally set aside for a proposed off-normal TREAT test, but plans for this test were later cancelled.

The metallographic examinations of the elements in this series showed some unusual effects. The most striking effect was a grain-boundary separation (or creep cavitation) in the cladding of samples from breached elements U131 and U132, and to a lesser extent in unbreached element U142. The details of this effect are discussed in Sec. B.II.D.6. The effect was not encountered in the other examined elements of this series.

Most of the examined samples from the helium-bonded elements showed evidence of reaction zones between the fuel and cladding. These zones (up to approximately 50 μm in thickness) were typical of (U,Pu)C-stainless steel interactions and were composed mostly of plutonium and nickel (determined by electron microprobe analysis).

Most of the examined samples also showed numerous cladding inclusions in the Type 316 stainless steel cladding. Electron microprobe analysis indicated that the inclusions in at least two samples contained titanium. A chemical analysis of one sample of cladding confirmed that the composition was typical of a 300-series stainless steel. The question of the titanium source in the Type 316 stainless steel cladding has not been resolved. There was no evidence, however, that these inclusions contributed to the grain-boundary separation effect.

Deposits in fuel-cladding gaps and in some fuel cracks were found in many samples. Electron microprobe analyses showed that most of the deposits contained plutonium and some fission products. An unusual finding in one section of U131 was the relatively large amounts of barium (up to 40 wt%) with small

amounts of strontium and cerium found in fuel-cladding gaps and in some fuel cracks.

Metallographic examinations of the two sodium-bonded elements that were examined (U146A and U147) did not show any unusual effects. Element U146A still had fuel pellets with little porosity and minor cracking after 7.2 at.% burnup. Element U147 (10.6 at.% burnup) showed much more cracking of the fuel pellets.

d. Series U-1930 and U-1960. Eleven elements (U188, U190, U193, U196, U199, U201, U203, U207, U209, U260, and U262) from these series received some amount of hot-cell examination. The examinations have been completed for all elements except U203, U260, and U262.

Breached element U196 (sodium-bonded carbide, 10.4 at.% burnup) showed metallographic evidence of an extensive carburized zone in the cladding. A small cladding sample was submitted for relative carbon concentration measurements across the cladding thickness using an ion probe; the results were compared with metallographic and microhardness measurements.

Samples from the crack regions of element U201 (helium-bonded carbide, 7.3 at.% burnup) indicated a brittle-type intergranular fracture. This was confirmed on samples from the same regions by a fractographic study using a scanning electron microscope. An additional longitudinal metallographic sample, which was taken from the top of the fuel stack (including the upper insulator pellet), showed the following effects: (1) some cladding from the inner surface was missing in an irregular pattern to a depth of more than 50% of the cladding thickness in the vicinity of the tungsten insulator pellet; (2) the cylindrical surface of the insulator pellet was coated with an unknown material having at least two distinct layers; (3) portions of the hold-down spring were also covered with an unknown material; and (4) the center of the uppermost fuel pellet was swelled markedly towards the insulator pellet, and one fuel chip in this region contained a second phase. A microprobe study showed that the layered deposits on the tungsten pellet and on the Nb-1% Zr spring wire were predominantly iron, chromium, and nickel. The layer adjacent to the insulator pellet showed evidence of diffusion between the outer coating and the insulator. The fuel chip was composed mainly of plutonium (little or no uranium) and fission products (neodymium, cerium, praseodymium, palladium, rhodium, and a small amount of ruthenium). No explanation was found for the missing portions of the cladding. Estimates have been made for the temperature of the tungsten

insulator pellet during normal steady-state operation. These calculations have shown that the surface temperature of the insulator pellet should have been well below the melting point of stainless steel.

Element U207 (helium-bonded carbide, 7.4 at.% burnup) had a longitudinal cladding crack at 112-127 mm from the bottom of the element. Both a metallographic examination in the crack region and a fractographic study using a scanning electron microscope showed the cladding crack to be an intergranular, brittle-type fracture. Other metallographic specimens had areas of fuel-cladding interaction that were typical of helium-bonded carbide elements at this burnup level.

Element U209 (helium-bonded carbide, 7.5 at.% burnup) contained high-density (97% T.D.) annular fuel pellets. It experienced a transverse cladding breach at 159 mm from the bottom of the element. A longitudinal metallographic sample that contained the breach point revealed a reaction zone, in which some melting may have occurred, in the immediate vicinity of the cladding breach, and a similar zone a short axial distance below the breach. Both reaction zones consumed approximately one-half the cladding thickness and also extended into the fuel. An electron microprobe study of these zones showed the presence of both fuel and cladding constituents. In addition, uranium and plutonium were found along the cladding cracks and, for a short distance, along the outer surface of the cladding. This evidence suggests that the element experienced gas blanketing between the capsule and element after being breached, which resulted in a significant temperature increase in the breached region.

Five of the elements (U188, U190, U193, U199, and U209) were severely damaged in a handling incident at HFEF, and required a special decapsulation procedure. This consisted of making precise cuts through the capsule wall above and below the dented portion, removing the top and bottom capsule sections, and finally sliding off or slitting the damaged capsule section. All but U199 were successfully decapsulated using this technique. Element U199 was found to be completely broken near the major dent in the capsule. Since there was no evidence of element breaching before the incident, and no fission gas was found in the capsule plenum, it was presumed that the element break was caused by the incident after the sodium had solidified. The break in U199 (sodium-bonded carbide, 7.8 at.% burnup) precluded further decapsulation efforts. The element was sectioned with the capsule in place for metallography.

Metallographic examination of the fuel showed comparable behavior to other sodium-bonded carbide elements.

Element U188 (helium-bonded carbide, 10.4 at.% burnup) had a large rupture, which extended from 58 mm to 224 mm from the bottom of the element. Many tiny globules of unidentified material existed along the cracks in the lower 25 mm of the breached region. Metallography showed evidence of gas blanketing and subsequent fuel-cladding melting at some axial locations.

Element U190 (helium-bonded carbide, 10.5 at.% burnup) was not breached, but it was treated as such during hot-cell examination because of its damaged condition. Metallography of this element showed behavior typical of helium-bonded carbide elements.

The breach point in U193 (sodium-bonded carbide, 9.9 at.% burnup) could not be visually detected. It was necessary to heat the element during the helium pressurization test. At a pressure of 1.38 MPa (200 psig), small drops of molten sodium were observed coming from what appeared to be a pinhole located at 114 mm from the bottom of the element. The breach point was reached by a sequential grinding operation.

Elements U203, U260, and U262 received their final irradiation in sub-assembly X233. Element U203 (helium-bonded carbide, 5.2 at.% burnup) was breached, but its breach point was not located visually. Elements U260 and U262 (helium-bonded carbides, 2.8 at.% burnup) were unbreached. They were decapsulated, but their destructive examination was delayed pending the processing of higher priority experiments.

e. Series U-5100 and K4. Subassembly X246, which contained the final 7 elements from Series U-5100 plus 12 elements from Series K4, was disassembled at HFEF-N on September 14, 1976. The seven U-5100 elements contained fuel restraint devices in the form of perforated shroud tubes; the K4 elements also contained shroud tubes of a more recent design. The seven U-5100 elements plus one selected K4 element will be returned to LASL for examination. They will be the first sodium-bonded, shrouded carbide experiments to be examined.

All elements were given a visual examination upon disassembly and appeared to be in good condition. There was some cladding discoloration in the fuel stack regions and some bright spots at the apparent contact points with adjacent wire wraps. The individual wire wraps appeared reasonably tight and of uniform pitch. Only slight bowing of a few elements was noted. The surface of the U-5100 elements was relatively shiny, even in the discolored zones,

whereas the K4 elements had a duller or frosty appearance. This effect was probably due to the difference in cladding materials (solution-annealed Type 304 stainless steel or Incoloy 800 for the U-5100 elements and 20% cold-worked Type 316 stainless steel for the K4 elements). The remainder of the preliminary examinations at HFEF-N (neutron radiography, weight, length, bow, and profilometry) will be made before shipment to LASL or before reconstitution.

Nine of the U-5100 elements without shroud tubes were placed in long-term storage. Three of these elements (U245, U247, and U249) were badly damaged in an incident at the LASL hot cells. These elements were sectioned and stored for possible fuel recovery operations. A fission-gas sample was taken from element U245 before sectioning. Fission-gas samples were also taken from unbreached elements U242 and U251 for comparison with U246 (displaced fuel pellets) and U248 (breached). The fission-gas-release values of 1.7% for U242 and 1.8% for U251, as compared to values of 15.3% for U246 and 12.8% for U248, support the theory that the latter two elements operated at somewhat higher temperatures. It is believed that both U246 and U248 operated unbonded for an undetermined length of time. The actual release value for U248 may have been somewhat greater than the measured value, since some fission gas was undoubtedly expelled through the cladding breach after expulsion of the bond sodium.

f. Series WF. Five elements in this series received their final irradiation in subassembly X233. The two breached elements, W5F and W12F, were fission-gas sampled and decapsulated. Both elements were found to be broken into two pieces and had transverse breaks similar to W6F. All three of these elements had thin-walled cladding (0.25 mm) and relatively small diametral gaps (0.25-0.33 mm). Element W5F was broken at 61 mm from the bottom of the element. A secondary break occurred at 51 mm from the bottom of the element while the pieces were being rinsed with Dowanol-EB for sodium removal. Another transverse crack, which extended approximately halfway around the circumference, occurred at 33 mm from the bottom of the element. Element W12F was broken at 94 mm from the bottom of the element. For comparison, location of the break in W6F was at 57 mm from the bottom of the element. The breaks in these three elements occurred near or slightly below the core midplane, which was located at ~89 mm from the bottom of the elements, because of tensile specimens located below the fuel elements.

The three unbreached elements (W3F, W7F, and W10F) have been decapsulated and are ready for fission-gas sampling and sectioning. Elements W3F and W7F

were part of an experiment that compared the behavior of solution-annealed and 20% cold-worked Type 316 stainless steels. The details of this comparison are discussed in Sec. B.II.D.3.

g. Series B-1, B-2, and B-3. The irradiation of all 16 elements in these series and postirradiation examinations of 8 elements that exhibited severe element-capsule interactions have been placed in long-term storage. The remaining five elements (B-1-4, B-2-1, B-2-5, B-3-6, and B-3-8) received their final irradiation in subassembly X233. Breached element B-1-4 has been fission-gas sampled and decapsulated. Its breach points are described in Sec. B.II.C.1.d.

The two helium-bonded nitride elements (B-3-6 and B-3-8) containing high-density annular fuel pellets exhibited marked cladding deformations in the fuel region, although neither element was breached. (See Sec. B.II.D.5) Both elements were fission-gas sampled and sectioned. Appropriate samples were taken from each element and submitted for burnup, metallographic, and cladding-density analyses. The remaining two unbreached elements (B-2-1 and B-2-5) will be examined in FY 1977 pending the processing of higher priority experiments.

D. Discussion of Postirradiation Results

1. Fission-Gas Release for Advanced Fuels Irradiated in EBR-II.

a. Helium-Bonded Carbide Elements. The percentages of fission gas released have been determined for 49 helium-bonded carbide fuel elements irradiated in EBR-II. These results and a partial description of the experimental parameters for these fuel elements are given in Table B-XXVI. For fuel elements that contained low-density (77-91% T.D.) fuel, the amount of fission-gas release appeared to be markedly affected by the smear density. In general, fuel elements that had smear densities of $\leq 81\%$ theoretical released significantly higher percentages of fission gas than elements that had smear densities of $\geq 82\%$ theoretical. Figure B-9 shows the fission-gas release vs burnup for fuel elements that contained low-density fuel and had smear densities of $\leq 81\%$ theoretical. These elements, operated at 50-90 kW/m, showed an increase in fission-gas release as burnup accumulated to about 7 at.%. Four of the thirteen elements released significantly less fission gas than the curve in Fig. B-9 indicates; no satisfactory explanation for these differences has been found. Similar fuel elements operated at 35-45 kW/m released fission gas at a level of about 75% of that released by the higher power fuel elements.

TABLE B-XXVI

FISSION-GAS RELEASE FOR HELIUM-BONDED CARBIDE
FUEL ELEMENTS IRRADIATED IN EBR-II

Element No.	Fuel Density (%T.D.)	Fuel Cladding Gap (mm)	Smear Density (%T.D.)	Peak Linear Power (kW/m)	Peak Burnup (at.%)	Fission Gas Release (%)
SMP-1	85	0.13	82	54	2.0	4
U187	86	0.18	81	77	4.6	20
U200 ^a	86	0.23	80	83	4.7	18
U189	85	0.25	79	81	4.8	19
U206	90	0.25	83	90	5.0	10
U101 ^a	85	0.02	84	77	5.2	11
U203 ^a	85	0.05	84	93 ^c	5.2 ^c	9
U96	85	0.18	81	68	7.0	34
U99	85	0.37	76	64	7.0	14
U92	85	0.20	81	69	7.1	37
U104 ^a	77	0.20	72	62	7.3	37
U201 ^a	85	0.23	79	81	7.3	19
U207 ^a	90	0.25	83	94	7.4	15
U98 ^a	86	0.18	82	65	9.3	12
U94	85	0.18	81	70	9.4	35
U106 ^a	77	0.20	72	59	9.5	25
U97	86	0.10	83	55	9.6	13
U93	86	0.10	83	59	9.6	16
U105 ^a	77	0.20	72	52	9.9	34
U188 ^a	85	0.18	80	79	10.4	41
U141 ^a	91	0.25	84	40	10.5	9
U190	85	0.28	78	80	10.5	26
U140	90	0.30	82	38	10.7	11
U142 ^a	91	0.25	84	43	10.7	7
U136 ^a	85	0.36	76	36	11.0	27
U133	85	0.36	76	38 ^c	11.1 ^c	27
U134 ^a	85	0.36	76	38 ^c	11.2 ^c	21
U135 ^a	85	0.36	76	38 ^c	11.2	32
U132 ^a	85	0.36	76	38	11.3	30
U131 ^a	85	0.36	76	38	11.4	29
U129 ^a	86	0.41	76	38 ^c	11.4	24
U130	77	0.25	70	39	11.7	43
U79	98	0.18	93	62	2.3	<1
U78 ^a	98	0.13	94	92	3.2	<1
U80	98	0.15	93	92	3.3	<1
U87	99 ^b	0.15	94	77	3.3	3
U208 ^a	97 ^b	0.30	79	87	5.0	10
U107 ^a	98 ^b	0.18	85	72	6.9	<1
U109 ^a	98 ^b	0.36	80	67	7.0	7
U138A ^a	98 ^b	0.36	88	44	7.2	5
U108 ^a	98 ^b	0.36	80	70	7.3	2
U112 ^a	98 ^b	0.18	85	74	7.4	<1
U029 ^a	97 ^b	0.30	79	92	7.5	15
U111 ^a	98 ^b	0.25	79	57	7.6	2
U114 ^a	98 ^b	0.18	85	66 ^c	9.5 ^c	13
U137 ^a	97	0.36	87	40 ^c	10.0	9
U139 ^a	97 ^b	0.36	87	44 ^c	10.2 ^c	18
U143	96 ^b	0.36	77	38 ^c	10.9 ^c	3
U144	96 ^b	0.36	77	39 ^c	11.1 ^c	5

^aFailed.^bCentral hole in fuel pellets.^cEstimated from EBR-II data.

B-9.

Fission-gas release vs peak burnup for helium-bonded carbide fuel elements containing 77-86% T.D. fuel with smear densities $\leq 81\%$ T.D.

Figure B-10 shows the fission-gas release vs burnup for fuel elements that contained low-density fuel and had smear densities of $\geq 82\%$ theoretical. These elements also showed an increase in fission-gas release until a burnup of about 7 at.% was reached, but at a much lower release level than that of low-density-fueled elements of $\leq 81\%$ smear density. Three high-burnup fuel elements that operated at 35-45 kW/m released fission gas at a level of about 65% of that released by the elements operated at 50-95 kW/m.

Figure B-11 shows the fission-gas release vs burnup for fuel elements that contained high-density (96-99% T.D.) fuel. The three elements that released more than 10% of their total fission gas generated all showed evidence of gas blanketing between the fuel element and capsule. The average amount of fission gas released by these high-density-fueled elements was about 5% at 10 at.% burnup. No significant difference in fission-gas release was found for those elements that contained fuel pellets with axial central holes.

B-10.

Fission-gas release vs peak burnup for helium-bonded carbide fuel elements containing 85-91% T.D. fuel with smear densities $\geq 82\%$ T.D.

B-11.

Fission-gas release vs peak burnup for helium-bonded carbide fuel elements containing 96-99% T.D. fuel.

b. Sodium-Bonded Carbide Elements. The percentages of fission gas released have been determined for 28 sodium-bonded carbide fuel elements irradiated in EBR-II. These results and a partial description of the experimental parameters for these fuel elements are given in Table B-XXVII. The percentage of fission gas released as a function of burnup for fuel elements irradiated at 70-110 kW/m is shown in Fig. B-12. This figure shows that the failed elements and the unfailed element that lost its sodium bond released considerably more fission gas than the unfailed elements in this power range. Figure B-13

B-XXVII

FISSION-GAS RELEASE FOR SODIUM-BONDED CARBIDE
FUEL ELEMENTS IRRADIATED IN EBR-II

Element No.	Fuel Density (%T.D.)	Fuel Cladding Gap (mm)	Smear Density (%T.D.)	Peak Linear Power (kW/m)	Peak Burnup (at.%)	Fission Gas Release (%)
K46 ^a	94	0.51	81	80	2.4	13
K45 ^a	94	0.51	81	80	2.7	6
U245	93	0.81	73	101 ^c	2.9 ^c	<1
U246 ^b	93	0.43	82	109	3.0 ^c	18
U242	93	0.44	82	107 ^c	3.0 ^c	2
U251	93	0.81	73	109 ^c	3.1 ^c	2
U248 ^a	93	0.81	73	109	3.2	16
U192	92	0.76	73	73	4.3	10
U191	92	0.76	73	84	4.5	3
K42B	90	0.38	81	85	4.5	8
U194 ^a	98	0.84	76	81	4.6	21
U198	98	0.84	76	84	4.8	9
U195	98	0.84	76	83	4.9	9
U197	98	0.84	76	80	4.9	15
W2X	88	0.79	69	67	5.3	3
W4F	95	0.69	77	82	5.3	8
W6F ^a	90	0.28	82	62	5.3	5
W8X	88	0.48	78	73	5.4	4
W8F	94	0.64	78	83	5.8	7
K36B ^a	90	0.38	81	82	5.9	16
U146A	99	0.76	78	37	7.2	<1
U199	98	0.79	77	84	7.8	10
W5F ^a	91	0.25	83	60 ^c	8.0 ^c	10
W12F ^a	97	0.33	86	63 ^c	8.0 ^c	3
K44	94	0.51	81	80	8.3	19
U193 ^a	92	0.79	72	75	9.9	43
U196 ^a	98	0.79	77	97	10.4	37
U147	97	0.76	77	37	10.6	2

^aFailed.

^bBond loss.

^cEstimated from EBR-II data.

B-12.

Fission gas release vs peak burnup for sodium-bonded carbide fuel elements operated at 70-110 kW/m.

B-13.

Fission gas release vs peak burnup for sodium-bonded carbide fuel elements operated at 35-65 kW/m.

shows the lower percentages of fission gas released from unfailed elements operated at 35-65 kW/m. Failed elements in this power range also released more fission gas than unfailed elements; one failed element released an unusually high 80% of its fission gas.

The most likely explanation for this difference in fission-gas release between failed and unfailed elements is that some fraction of the fuel in the failed elements operated without a sodium bond after failure. Also, since most of these fuel elements were encapsulated, gas-blanketing in the element-capsule annulus because of fission-gas release out of the fuel element is also likely. The higher fuel temperatures that result from operating without a sodium bond in the fuel element or capsule would produce larger fission-gas releases. Evidence of localized overheating of part of the fueled region has been observed in some of the fuel elements; element U145, which released 80% of its fission gas, was one of these.

A second possibility is that the same mechanism contributed to both the cladding failures and the increased gas release. The failure mechanism for sodium-bonded elements has not been identified, but is believed to be related to fuel-cladding mechanical interaction.

c. Helium-Bonded Nitride Elements. The percentages of fission gas released for three helium-bonded nitride fuel elements irradiated in EBR-II are listed in Table B-XXVIII. Comparison of these three elements indicate

TABLE B-XXVIII

FISSION-GAS RELEASE FOR HELIUM-BONDED NITRIDE
FUEL ELEMENTS IRRADIATED IN EBR-II

Element No.	Fuel Density (%T.D.)	Fuel Cladding Gap (mm)	Smear Density (%T.D.)	Peak Linear Power (kW/m)	Peak Burnup (at.%)	Fission Gas Release (%)
B-3-7	89	0.15	85	102 ^b	5.5 ^b	22
B-3-8	90 ^a	0.15	80	101 ^b	8.0 ^b	13
B-3-6	95 ^a	0.15	85	110 ^b	8.2 ^b	8

^aCentral hole in fuel pellets.^bEstimated from EBR-II data.

that fission-gas release was most influenced by the fuel density. Comparison of B-3-6 and B-3-7 with carbide elements of comparable fuel and smear densities (Figs. B-11 and B-10, respectively) indicate that these nitride elements released more fission gas than the average values obtained for carbide elements.

d. Sodium-Bonded Nitride Elements. The percentages of fission gas released for eight sodium-bonded nitride fuel elements irradiated in EBR-II are listed in Table B-XXIX. The only unfailed element for which the fission-gas release was determined had about one-fourth the average fission-gas release of similar sodium-bonded carbide elements (Fig. B-12) at its burnup of 8.2 at.%. The failed sodium-bonded nitride elements released from <1 to 11% fission gas. The B-3 Series elements that were irradiated to 2.7-2.8 at.% burnup released

TABLE B-XXIX

FISSION-GAS-RELEASE FOR SODIUM-BONDED NITRIDE
FUEL ELEMENTS IRRADIATED IN EBR-II

Element No.	Fuel Density (%T.D.)	Fuel Cladding Gap (mm)	Smear Density (%T.D.)	Peak Linear Power (kW/m)	Peak Burnup (at.%)	Fission Gas Release (%)
B-3-5 ^a	90	0.25	85	118	2.7	7
B-3-4 ^a	94	0.35	85	109	2.7	11
B-3-2 ^a	88	0.24	83	110	2.8	7
B-3-3 ^a	91	0.25	85	116	2.8	7
B-2-2 ^a	94	0.52	81	93	5.5	5
B-1-1 ^a	94	0.54	79	79 ^b	5.7 ^b	<1
B-1-4 ^a	94	0.37	83	91 ^b	5.8 ^b	2
B-2-3	94	0.53	81	97	8.2	4

^aBreached.^bEstimated from EBR-II data.

about the same average amount of fission gas as carbide elements of that burnup. The B-1 and B-2 Series elements that were irradiated to 5.5-5.8 at.% burnup released significantly less fission gas than comparable carbide elements at that burnup.

2. Maximum Mechanical Strain in Helium-Bonded, Low-Density Carbide Fuel Elements

A study was made of the maximum mechanical cladding strain of helium-bonded carbide fuel elements that contained 77-91% T.D. fuel pellets. Only those fuel elements that were unbreached or those with such small breach regions that their locations could be detected only by pressurization tests were included.

Axial plots of the average diameter of each fuel element were determined from four profilometry traces taken at 45° intervals. The maximum average-diametral increase from these plots was then divided by the average diameter in the upper plenum region of each of the fuel elements ($\times 100$) to determine the maximum average $\% \Delta D/D$ values. At the location for which the maximum average $\% \Delta D/D$ was found, the $\% \Delta D/D$ as a result of cladding swelling was estimated from cladding-density measurements, and the difference, or mechanical strain, was determined. These results, along with some experimental parameters of the fuel elements, are shown in Table B-XXX. The Series 1950 elements (see Table B-IV) incurred a proportionately greater amount of cladding swelling than the other fuel elements of approximately equivalent burnup because of their higher fluence-to-burnup ratios.

The maximum mechanical strain as a function of peak burnup is shown in Fig. B-14. This figure shows that the mechanical strain at the point of maximum $\% \Delta D/D$ correlates with the fuel-element smear density. Fuel elements with smear densities of 70-80% T.D. had approximately one-half the mechanical strain at 10-11 at.% burnup as those with smear densities of 81-84% T.D. Some effect of cladding thickness on the maximum mechanical strain is also indicated in Fig. B-14. Within the two smear-density groupings, the fuel elements with the thinner (0.36-0.43 mm) cladding had, in general, higher maximum mechanical strains than those with thicker claddings.

3. Comparison of Profilometry Results of Unshrouded, Sodium-Bonded, Carbide Fuel Elements Clad in Solution-Annealed and in 20%-Cold-Worked Type 316 Stainless Steel.

In the WF Series of sodium-bonded carbide fuel elements, two elements were clad in 20% cold-worked Type 316 stainless steel. One of these elements,

TABLE B-XXX

MAXIMUM MECHANICAL STRAIN FOR HELIUM-BONDED CARBIDE FUEL ELEMENTS
CONTAINING FUEL OF 77-91% THEORETICAL DENSITY

No.	Fuel Density (%T.D.)	Smear Density (%T.D.)	Cladding Thickness (mm)	Peak Linear Power (kW/m)	Peak Burnup (at.%)	Max Av $\Delta D/D$ (%)	Cladding Swelling $\Delta D/D$ (%)	Max Mech. Strain (%)
U187	86	81	0.56	77	4.6	0.36	0.09	0.27
U200 ^a	86	80	0.36	83	4.7	0.31	0	0.31
U189	85	79	0.53	81	4.8	0.36	0.36	0
U206	90	83	0.43	90	5.0	0.48	0.16	0.32
U99	85	76	0.43	64	7.0	0.82	0.49	0.33
U96	85	81	0.43	63	7.0	1.08	0.29	0.79
U92	85	81	0.43	69	7.1	1.54	0.38	1.16
U134	77	72	0.38	62	7.3	0.52	0.43	0.09
U98 ^a	86	82	0.38	70	9.3	2.59	0.79	1.80
U94	85	80	0.43	70	9.4	2.10	1.30	0.83
U106 ^a	77	72	0.43	63	9.5	1.08	0.28	0.80
U93	86	83	0.78	59	9.6	1.69	0.25	1.44
U97	86	83	0.75	55	9.6	1.77	0.58	1.19
U105	77	72	0.76	52	9.9	0.97	0.54	0.43
U141 ^a	91	84	0.55	40	10.5	2.40	1.31	1.09
U140	90	81	0.53	38	10.7	2.45	1.22	1.23
U142	91	84	0.56	40	10.7	2.70	1.35	1.35
U136 ^a	85	76	0.53	36	11.0	2.05	1.49	0.56
U132 ^a	85	76	0.55	37	11.2	2.51	1.72	0.79
U131 ^a	85	76	0.55	38	11.4	2.69	1.95	0.74
U130	77	70	0.55	37	11.7	2.24	1.45	0.79

^aBreached element.

Fig. B-14.

Maximum mechanical strain vs peak burnup for helium-bonded carbide fuel elements containing fuel of 77-91% theoretical density.

W8F, was removed from EBR-II at 5.8 at.% burnup along with a similar element, W4F, which was clad in solution-annealed Type 316 stainless steel. Comparison of the profilometry traces of these two fuel elements showed a slightly larger average diametral increase in W4F and, in addition, larger and more numerous ovalities.¹²

At an estimated 8.1 at.% burnup, a similar pair of fuel elements, W3F and W7F, completed irradiation. Comparisons of the profilometry traces of W3F, which had solution-annealed Type 316 stainless steel cladding, and W7F, which had 20% cold-worked Type 316 stainless steel cladding, have shown the same effects as those observed at the lower burnup.

The maximum average diametral increases and the maximum observed ovalities of W3F, W4F, W7F, and W8F are listed in Table B-XXXI. Cladding-immersion-density measurements on W4F and W8F have shown that the average diametral increases of these fuel elements can be accounted for by cladding swelling. The linear increase with burnup in the maximum $\Delta D/D$ of W3F and W7F compared with the maximum $\Delta D/D$ of W4F and W8F indicated that these diametral increases were also due to cladding swelling. At least up to 8 at.% burnup, the use of 20% cold-worked Type 316 stainless steel appears to effect an improvement in the cladding behavior of unshrouded, sodium-bonded, carbide fuel elements by virtue of the significantly smaller size and number of cladding ovalities compared with solution-annealed Type 316 stainless steel.

4. Cladding Ovalities in Advanced Fuel Elements

In a previous report¹³ a model was presented that related ovalities in advanced fuel elements to nonaxisymmetric fuel-cladding mechanical interaction that results from fuel cracking and wedging during irradiation. The measured

TABLE B-XXXI
PROFILOMETRY RESULTS FOR UNSHROUDED, SODIUM-BONDED
CARBIDE FUEL ELEMENTS CLAD IN TYPE 316 STAINLESS STEEL

No.	Fuel Density (%T.D.)	Cladding Condition ^a	Cladding Thickness (mm)	Fuel Cladding Gap (mm)	Peak Linear Power (kW/m)	Peak Burnup (at.%)	Max ΔD (mm)	Max $\Delta D/D$ (%)	Max Ovality (mm)
W4F	95	SA	0.31	0.69	82	5.3	0.023	0.30	0.09
W8F	94	20CW	0.31	0.64	83 ^b	5.8 ^b	0.008	0.10	0.03
W3F	93	SA	0.31	0.64	81 ^b	8.1 ^b	0.033	0.43	0.14
W7F	90	20CW	0.31	0.69	81 ^b	8.1 ^b	0.013	0.17	0.05

^aSA = solution-annealed; 20CW = 20% cold-worked.

^bEstimated from EBR-II data.

shapes of ovalities were shown to agree with calculated shapes, and stresses associated with ovalities were calculated as a function of ovality size. No attempt had been made to calculate ovality sizes for specific designs or operating conditions because of the random nature of fuel cracking and rearrangement. Even though specific calculations cannot be made, qualitative relations can be inferred between ovality size and some of the design parameters and operating conditions of elements. In order to compare these relations with data from test elements, some measure of ovality size in test elements must be established. To this end, the maximum ovality size observed in 19 sodium-bonded and 17 helium-bonded elements was used. It is recognized that the maximum ovality size may not be the optimum size parameter to use, but at this time no better choices are known.

There are two reasons to expect ovalities to increase in size during irradiation: (1) a longer irradiation period will allow more chance for the formation of large ovalities by continued cracking and wedging of fuel, and (2) once an ovality forms, fuel swelling will tend to increase the ovality size. The maximum ovality size is shown in Fig. B-15 for the groups of sodium- and helium-bonded elements as a function of the peak burnup in the fuel element. There is considerable scatter in the data, but a trend to larger ovalities with higher burnup is particularly apparent for the sodium-bonded elements.

The data in Fig. B-15 were separated into four cladding thickness ranges for the sodium- and helium-bonded elements. There was no apparent influence of cladding thickness on the maximum ovality size. It can be shown that, if a constant force is applied to the cladding inside surface, the resulting ovality size is a strong function of cladding thickness.¹³ The lack of dependence of maximum ovality size on cladding thickness means that the mechanism accounting for ovalities must be one where strains or deformations are controlling, and forces accommodate to the strains. This is in line with the mechanism proposed here, where ovality sizes are determined by the strains resulting from differential thermal expansion or fuel swelling. Thicker cladding is stronger than thinner cladding in the sense that it will require larger forces for a given deformation (ovality size), but the fuel is essentially incompressible and can accommodate the forces required.

Based on the argument that ovality sizes are strain controlled, it should also follow that ovality sizes are independent of cladding material or cladding metallurgical condition (e.g., annealed or cold-worked). A meaningful

Fig. B-15.
Cladding ovalities in advanced fuel elements.

comparison among the sodium-bonded elements is not possible since essentially all the data are from annealed Types 304 or 316 stainless steel. One pair of carbide elements (W4F and W8F) may serve as a counter example to the postulated independence of metallurgical condition. The elements are essentially identical except that W4F was clad in annealed Type 316 stainless steel and had relatively large ovalities, whereas W8F was clad in 20% cold-worked Type 316 stainless steel and had very small ovalities. The maximum ovalities in these two elements are shown in the open squares in Fig. B-15a. Any superiority of cold-worked cladding in restraining ovalities is difficult to establish based on only these two elements, especially with the data scatter evident in Fig. B-15. If cold-worked cladding, with its higher yield strength, consistently exhibits smaller ovalities, this may indicate that some of the ovality deformation measured at room temperature in annealed cladding is inelastic strain. A resolution of this question must await the examination of additional sodium-bonded elements with cold-worked cladding.

About half the helium-bonded elements represented in Fig. B-15b were clad in annealed Types 304 or 316 stainless steel while the other half were clad in annealed Incoloy 800. Although elements clad in Incoloy 800 tended to have smaller maximum ovalities, the difference between the two groups of claddings was not significant.

The maximum ovality sizes of the sodium- and helium-bonded elements were examined as a function of initial gap sizes and peak linear powers. No correlation between ovality size and initial gap size was found over a range of

diametral gaps from 0.23-0.85 mm for sodium-bonded elements and 0.02-0.37 mm for helium-bonded elements. No correlation was found between ovality size and peak linear power over a range of 70-100 kW/m for sodium-bonded elements and 40-90 kW/m for helium-bonded elements.

If fuel element failure is related to large ovalities, then failed elements should tend to show larger maximum ovalities than unfailed elements. There was a tendency for failed sodium-bonded elements to show larger maximum ovalities than unfailed elements at the same burnup, but the difference was not significant (see Fig. B-15a). There was no significant difference between the maximum ovality sizes observed in failed and unfailed helium-bonded elements (see Fig. B-15b).

5. Maximum Diametral Increases of Helium-Bonded Nitride Fuel Elements B-3-6, B-3-7, and B-3-8.

Three helium-bonded nitride fuel elements, B-3-6, B-3-7, and B-3-8, have attained estimated burnups of 5.5-8.2 at.% in EBR-II. These fuel elements contained fuel of 89-95% T.D. and had fuel-cladding gaps of 0.15 mm. The fuel pellets of two elements (B-3-6 and B-3-8) contained annular holes of 1.8 mm diameter.

To compare the maximum $\% \Delta D/D$ values obtained for these nitride fuel elements with those for carbide fuel elements of somewhat similar design, the maximum $\% \Delta D/D$ values for all unfailed carbide fuel elements that contained solid and annular fuel pellets of 95-99% T.D. or contained solid fuel pellets of 86% T.D. with a small gap size (0.10 mm) were compiled. These results, along with some experimental parameters of the fuel elements, are listed in Table B-XXXII. The maximum average $\% \Delta D/D$ values are plotted as a function of peak burnup in Fig. B-16. The nitride fuel elements had larger maximum $\% \Delta D/D$ values than any of the carbide elements at the 8 at.% burnup level.

A direct comparison of the diametral increases in identical helium-bonded carbide and nitride fuel elements from these data was not possible, since there were no carbide fuel elements that had the combination of annular pellets with 90-95% fuel density and a small (0.15-mm) gap to compare with the nitride fuel elements that attained 8 at.% burnup. In addition, differences in cladding swelling owing to differences in cladding type and composition will not be known until the fuel elements are destructively examined. The closest direct comparison involving these elements was between the nitride elements B-3-6 and B-3-8, which operated at approximately 100 kW/m and had calculated

TABLE B-XXXII

MAXIMUM AVERAGE $\Delta D/D$ OF UNFAILED, HELIUM-BONDED, NITRIDE AND CARBIDE FUEL ELEMENTS HAVING HIGH-DENSITY FUEL (<94% THEORETICAL) OR SMALL (0.10-0.13 mm) FUEL-CLADDING GAPS

No.	Fuel Density (%T.D.)	Fuel Cladding Gap (mm)	Smear Density (%T.D.)	Peak Linear Power (kW/m)	Peak Burnup (at.%)	Max Av $\Delta D/D$ (%)
NITRIDE						
B-3-7	89 ^a	0.15	85	102 ^b	5.5 ^b	1.01
B-3-8	90 ^a	0.15	81	97 ^b	8.0 ^b	2.31
B-3-6	95 ^a	0.15	85	102 ^b	8.2 ^b	2.47
CARBIDE						
U79	98	0.18	93	62	2.3	0.20
U80	98	0.15	93	92	3.3	0.76
U87	99	0.15	94	77	3.3 ^b	1.09
U138	97	0.36	87	44 ^b	3.6 ^b	0.13
U208	97 ^a	0.30	79	87	5.0	0.65
U86	99	0.25	91	61	6.2	1.13
U138A	98	0.36	88	44 ^b	7.2 ^b	0.95
U110	99 ^a	0.33	81	65 ^b	9.2 ^b	1.94
U93	86	0.10	83	59	9.6	1.69
U97	86	0.10	83	55 ^b	9.6 ^b	1.77
U113	98 ^a	0.28	79	50 ^b	10.2 ^b	1.90
U143	96 ^a	0.36	77	38 ^b	10.9 ^b	2.15
U144	96 ^a	0.36	77	39 ^b	11.1 ^b	1.78

^aAnnular fuel pellets.

^bEstimated from EBR-II data.

Fig. B-16.

Maximum cladding $\Delta D/D$ vs peak burnup for helium-bonded nitride and carbide fuel elements having high-density fuel or small fuel-cladding gaps.

beginning-of-life central fuel temperatures of 1700-1725°C, and the carbide elements U110 and U113, which operated at 65 and 50 kW/m, respectively, and had calculated beginning-of-life central fuel temperatures of 1800-1850°C. The determination of whether the higher maximum $\Delta D/D$ values for the nitride elements are the result of greater fuel swelling or whether they are the result of other factors, such as higher cladding swelling or the employment of smaller fuel-cladding gaps, will be determined after these fuel elements are destructively examined. It should be emphasized, however, that the maximum $\Delta D/D$ values for the nitride elements and the high-density-fueled carbide elements were significantly higher than for carbide fuel elements containing fuel of 77-86% T.D. and having smear densities less than 81% T.D.¹⁰

6. Cladding Cracks and Grain-Boundary Separations in U-1950 Series Elements

A series of cladding cracks and grain-boundary separations, which might have been caused by creep-rupture behavior, were observed in three elements from the U-1950 Series (Table B-IV) during postirradiation examinations. Two of the three elements in which this phenomenon was seen (U131 and U132) were failed helium-bonded elements. The cracking and grain-boundary separation in the cladding were associated with failure sites. The third element (U142) was an unfailed helium-bonded element in which an external crack, partially through the cladding, was associated with voids and grain-boundary separation.

One small failure site was observed in U132 during a helium pressurization test of the element. It was located about 33 mm below the axial midplane of the fuel column. A metallographic sample (U132-L) taken at the failure site was examined at four slightly different levels. Figure B-17 shows a photomosaic of the entire as-polished section of surface A (the first level examined). The failure region is located at 0° orientation (the top of the photomosaic). The cladding failure is not adjacent to any prominent feature in the fuel. The large gap between the fuel and cladding from 170° to 260° orientation (measured clockwise from 0°) indicates that this level is at a pellet-pellet interface and has exposed part of a fuel-pellet chamfer. Figure B-18 shows a high-magnification photomosaic of the cladding failure region from surface A in the etched condition. The larger cracks are all intergranular. As one moves circumferentially away from the center of the photomosaic, the intergranular cracks and grain-boundary separation or cavitation degenerate into a series of bubbles or voids along the grain boundaries. At the edges of the photomosaic, most of the isolated voids or bubbles are at triple points.

Fig. B-17.

Photomosaic of surface A, U132-L, as-polished.

The cracks and grain-boundary separation are oriented in a radial direction.

Figure B-19 shows a photomosaic of the entire as-polished section of surface B from U132-L. Surface B is 0.62 mm below surface A. The cladding failure is at 350° orientation and is adjacent to a small radial fuel crack. The apparent shift in failure location from surface A to surface B may only be a result of an uncertainty in orienting individual samples. Figure B-20 shows a high-magnification, as-polished photomosaic of the cladding failure region from surface B. The larger intergranular cracks are more pronounced at this level, but the general behavior is similar to that

Fig. B-18.

Photomosaic of cladding failure region from surface A, U132-L etched.

Fig. B-19.
Photomosaic of surface B, U132-L, as
polished.

Fig. B-20.
Photomosaic of cladding failure region
from surface B, U132-L, as polished.

at surface A. Figure B-21 shows a high-magnification photomosaic of a region of the cladding at this level, centered at a 20° orientation. This region is at the end of a radial fuel crack and contains a number of isolated voids or bubbles in the cladding.

Figure B-22 shows a photomosaic of the entire as-polished section of surface C from U132-L. Surface C is 0.65 mm below surface B. The behavior of the cladding is very similar to that in surface B; however, the major crack at the inner surface of the cladding is in a direct line with a small fuel crack. Figure B-23 shows a high-magnification, as-polished photomosaic of the cladding failure region from surface C. Some fuel-cladding interaction is apparent in this photomosaic, as is the alignment of the fuel crack and inner cladding crack.

Figure B-24 shows a photomosaic of the entire as-polished section of surface D from U132-L. Surface D is 0.68 mm below surface C. The fuel and cladding are in intimate contact throughout most of the circumference. The cladding grain-boundary separation effect is not apparent in this figure. A high-magnification, as-polished photomosaic of the cladding section that contained the defect area (at approximately 0° orientation in Fig. B-24) is shown in Fig. B-25. The grain-boundary separation effect is still present, although it

Fig. B-21.
Photomosaic of cladding voids from sur-
face B, U132-L, as polished.

Fig. B-22.
Photomosaic of surface C, U132-L, as
polished.

Fig. B-23.
Photomosaic of cladding breach region, U132-L, surface C, as polished.

Fig. B-24.
Photomosaic of surface D, U132-L, as
polished

Fig. B-25.
Photomosaic of cladding breach region,
U132-L, surface D, as polished.

is less extensive than in previous surfaces. This figure also shows marked zones of fuel-cladding interaction, which are similar in appearance to those found at surface C. At this point, the normal metallographic coverage, which includes ion etching as one of the steps, was performed on this sample. After ion etching of the cladding section that contained the defect area, there was a dramatic increase in visible grain-boundary separations. Figure B-26 shows a high-magnification, ion-etched photomosaic of the same cladding area as shown in Fig. B-25. As a result of the ion-etching operation, many more voids or bubbles became visible.

Three other metallographic samples were taken from U132. Some slight indications of cladding grain-boundary separation were seen at the outside cladding surface in section U132-N (taken 150 mm below the axial midplane of the fuel column) in the etched condition. No evidence of cladding cracks or grain-boundary separation were seen in the other two sections.

Two different failure sites, both small, were seen in U131 during a helium-pressurization test. Metallographic samples were taken at both locations. The sample taken at the upper failure site (section U131-H) was 3 mm below the axial midplane of the fuel column. This sample was sequentially ground and examined metallographically at five surfaces (surfaces A through E). No

Fig. B-26.

Photomosaic of cladding breach region, U132-L, surface D, ion etched.

evidence of cracks, of the grain-boundary separation, or void or bubble formation was found at any of these surfaces in the as-polished condition. If a cladding breach was present in this sample, it was not found either because of insufficient grinding or because it was so small in axial extent that it was contained between the surfaces that were examined. The spacing between surfaces was 0.61 to 0.70 mm. The normal metallographic coverage was performed on U131-H at surface E. After the cladding was etched, the incipient formation of voids or bubbles in the grain boundaries at the inner cladding surface was detected near a large fuel crack.

The lower failure site of U131 (section U131-L) was located 55 mm below the axial midplane of the fuel column. This sample was sequentially ground and metallographically examined at four surfaces, A through D, which were located 0.62 to 0.68 mm apart axially. The observed behavior in this sample was very similar to that in U132-L. Intergranular cladding cracks, grain-boundary separation, and voids were seen at each level. The cladding cracks were adjacent to a large fuel crack at surfaces B and C, while the levels examined at surfaces C and D appeared to be at or near a pellet-pellet interface. Figure B-27 shows a photomosaic of the entire as-polished section of surface C from U131-L. The alignment of the fuel and cladding cracks, and the large fuel-cladding gap that is indicative of the fuel-pellet chamfer near a pellet-pellet interface are evident. No evidence of cladding cracks, voids, or grain-boundary separation was seen in the other metallographic samples taken from U131.

Fig. B-27.

Photomosaic of surface C, U131-L, as polished.

Three metallographic samples were taken from the unfailed, helium-bonded element U142. In the normal metallographic examination of section U142-M2, taken 4 mm above the axial midplane of the fuel, an external cladding crack was observed in the as-polished condition (see Fig. B-28). Even though the crack appears to be more than halfway through the cladding, fission products had not been released from the element. In the ion-etched condition, the cladding in the vicinity of the crack showed definite evidence of the grain-boundary separation effect (or creep

cavitation) that had been previously discovered in samples from sibling elements U131 and U132 in this series (see Fig. B-29). Although the effect was not as pronounced in element U142 as in the breached elements U131 and U132, it was apparent that element U142 was on the verge of being breached. The fuel and cladding were in intimate contact in this sample throughout the fuel-cladding circumference with a pronounced interaction zone of approximately 50 μm . The two other metallographic samples from U142 (taken near the top and bottom of the fuel column) showed no evidence of cladding cracks or the grain-boundary separation effect seen in section U142-M2.

One unusual observation that might be related to the cladding cracking and grain-boundary separation was made at surface D of U131-L. Unusual deposits were noted at several locations in cladding grain-boundary separations and in the one major cladding crack present at this level. An electron microprobe examination of this surface showed that the deposits contained plutonium and some fission products. A somewhat surprising result of this examination was the presence of large amounts of barium (up to 40 wt%), with lesser amounts of strontium and cerium, in most of the cladding crack and in the fuel-cladding gap. Such concentrations were also widespread in the fuel cracks at this surface.

Fig. B-28.
Cladding cracks on o.d. of sample U142-M2, as polished.

Fig. B-29.
Cladding grain-boundary separation effect in sample U142-M2, ion etched. (Two stains from the etching process are seen near the fuel-cladding interface.)

Metallographic examination was completed on a number of other elements from the U-1950 Series. An examination of samples from the helium-bonded elements U130, U136, U138A, U140, and U141, and from the sodium-bonded elements U146A and U147 (three samples from each element) showed no indications of cladding cracks, grain-boundary separation or voids. Other elements from the U-1950 Series are still undergoing examination.

There were a number of common features in the behavior of the cladding from elements U131, U132, and U142. The cladding cracks were all intergranular and showed associated areas of grain-boundary separation (cavitation) and grain-boundary voids or bubbles. In these areas, the cracking and grain-boundary separation were almost exclusively oriented in the radial direction and the grain-boundary voids or bubbles that were away from triple points were mostly on radially-oriented grain boundaries (see Figs. B-18, B-20, B-23, B-26, and B-29). There were no indications of twin-boundary cavitation. There were also areas with only voids or bubbles in the claddings of U131 and U132. The areas showing cracks, cavitation, and voids or bubbles were all localized circumferentially. At each level examined, there was no single crack through the cladding. At two of the levels from U132-L and U131-L, the cladding cracks and grain-boundary separation were adjacent to radial fuel cracks. Also, one level from U132-L and two levels from U131-L appeared to be at or near fuel pellet-pellet interfaces.

The observed behavior of the cladding in these elements is most readily accounted for by creep-rupture.¹⁴ The intergranular cracks, the grain-boundary separation (cavitation), and the grain-boundary voids are all common phenomena observed during creep-rupture of metals. In creep-rupture testing, it has been observed that the majority of voids form on grain boundaries that are normal to the direction of the applied tensile stress.^{15,16} Because most cracking, grain-boundary separation, and void formation are along radial grain boundaries in those claddings, the tensile stress causing creep-rupture is most likely in the circumferential direction in the vicinity of these areas. Two important variables affecting creep-rupture are the cladding fluence and temperature. Cladding fluence was approximately 9×10^{22} nvt ($E > 0.1$ Mev). Two effects of irradiation that have been noted are that helium bubbles on the grain boundaries can serve as nucleation sites for cavities,^{16,17} and that helium aids grain-boundary cracking by reducing the amount of grain-boundary sliding needed to form a void.¹⁷ Estimated maximum cladding temperatures near the crack locations were in the 500-550°C range. This is below the temperature range where grain-boundary sliding becomes important,¹⁷ and also below the range where creep cavitation is expected to occur in Type 316 stainless steel.¹⁸

Cladding behavior similar to that observed in U131, U132, and U142 has been seen in oxide elements.^{19,20} It has been attributed to thermal creep of the cladding. In one case, the localized nature of the creep region was ascribed to local overheating of the cladding, which resulted from contact with an adjacent element in the subassembly.¹⁹

The reason for the localized nature of the creep-rupture in the U131, U132, and U142 claddings has not been determined. Two possible causes are that the crack regions in these elements were at higher temperatures or at higher stress levels (or both) than adjacent areas of the cladding. The elements in subassembly X055B were encapsulated and irradiated in an A-19 subassembly. Cladding overheating and occasional melting have been observed in encapsulated elements.²¹ This was attributed to gas blanketing of the element by fission gas released into the element-capsule annulus from cladding failure points below the region that was gas blanketed. No failure points were observed below the locations where the creep-rupture behavior was found in U131, U132, or U142. Also, it seems unlikely that the elements would exhibit a similar and just sufficient amount of cladding overheating to cause the creep-rupture behavior when it had not been observed previously in advanced fuel elements. Locally

high circumferential tensile stresses might be caused by local fuel-cladding mechanical interactions, such as those in the vicinity of radial fuel cracks^{22,23} or pellet-pellet interfaces.²⁴ The high neutron fluences on these claddings may have contributed to the observed behavior; similar stress levels in cladding with a lower fluence may not show the same creep-rupture phenomena.

III. OFF-NORMAL IRRADIATION DESIGN AND TESTING (N. S. DeMuth and J. F. Kerrisk)

A. Design of Sodium-Bond-Loss Experiments

The use of sodium as the thermal bond between fuel and cladding results in significantly lower fuel operating temperatures than are obtained in gas-bonded elements. As a result of various reactor- and element-initiated off-normal events, the high-conductivity sodium bond can be displaced from the fuel-cladding annulus and replaced by a mixture of plenum and fission-product gases. A resultant reduction in thermal conductance between the fuel and cladding will produce fuel overheating. Fuel melting is a credible circumstance if the sodium bond is voided rapidly from a large fraction of the fuel-cladding annulus.

An important consideration in the application of sodium-bonded fuels is the effect of bond loss on the operational capabilities of the elements. Several fuel-element concepts, which include shrouded and unshrouded carbide and nitride fuels in two different cladding sizes, are currently being investigated in steady-state tests. Some experiments to evaluate the consequences of bond loss have been conducted.²⁵ However, the results of these experiments are directly applicable only for short times (up to a few hours) after bond loss from unirradiated carbide fuels. From these and other experiments, a qualitative understanding of fuel behavior during and immediately following bond loss has been gained, but this is not sufficient to evaluate the performance limitations as a result of bond loss from the several advanced-fuel-element designs being considered.

The feasibility of several experimental concepts for simulating bond loss from advanced fuel elements was investigated. As a result of this investigation, a test was devised in which forced ejection of the sodium bond from the fuel would occur during initial full-power operation of the elements. The neutron flux at the design power level will initiate the bond-loss event, and the sodium from the fuel-cladding annulus will be relocated to the element

cladding below the fuel region. This test has the following advantages: (1) the materials and element designs are typical of those in steady-state irradiation tests; (2) elements can continue to operate until substantial additional burnup is attained or a cladding breach occurs; (3) the test complexity is reduced considerably by the absence of external instrumentation leads for initiating the bond-voiding event; and (4) no connection between the fuel region and flowing sodium coolant is required for bond voiding.

The design of this test (designated LOB-1), which will investigate the behavior of advanced fuel elements during and following a loss-of-sodium-bond (LOB) event, was completed. Technical feasibility for performing the proposed test in EBR-II was established, and the Approval-in-Principle was granted by ERDA/RDD. Development and testing of the voiding devices and element fabrication will continue into the next fiscal year. The status of these activities is described in the following sections.

1. Voiding-Device Design and Operation

The conceptual design for a device that will produce rapid voiding of the sodium bond at or near the design power rating of the elements is shown in Fig. B-30. Fission energy generation in enriched (in ^{235}U) uranium is used to heat a section of tubing that contains a low-melting-point alloy. As the reactor power level increases during startup, the fission-energy generation in the fission heater increases, and the temperature of the reservoir entry tube containing the transition section increases. The fission heater is sized such that the temperature of the transition section will approach its melting point as the element reaches its design power level. Separation of the transition section to connect the sodium-bonded, fueled region with the evacuated reservoir is induced by the plenum gas pressure, which is about 200 kPa for nominal operating conditions. This pressure forces the bond sodium into the reservoir through an orifice and allows complete voiding of the fuel-cladding annulus in about one second. A stainless steel screen, which is interposed between the orifice and the fueled region, will prevent fuel chips from blocking the orifice and reducing the sodium flow rate into the reservoir.

Several factors were considered in the selection of materials for use in the voiding devices. Because of a combination of high-temperature properties, welding characteristics, fabricability, and availability, tantalum was chosen for the reservoir material in the voiding devices. Criteria established for the material used in the transition section included a melting point below

Fig. B-30.
Voiding device for LOB-1 test.

900°C, chemical and mechanical stability in sodium and in vacuum for temperatures slightly in excess of the EBR-II coolant inlet temperatures, and acceptability for use with tantalum. A copper-manganese alloy (60% Cu-40% Mn) that melts at 870°C was selected for use in the transition section. Since copper has limited solubility in liquid sodium, tests are planned to verify the stability of the Cu-Mn alloys in contact with sodium at temperatures expected before reactor startup. Laboratory tests of prototype entry tubes are planned to establish the temperature and reliability of joint separation for the expected operating pressure in LOB-1 elements.

Heat-transfer calculations were performed to determine system temperatures as a function of voiding-device design parameters. Important parameters included the system dimensions, the materials used, and the quantity of ^{235}U in the device. A two-dimensional (r, z) finite-difference

mesh was constructed over the upper portion of the voiding device for use with the CINDA heat-transfer code. Figure B-31 shows a scale layout of this region with the various material boundaries indicated. The thermal boundary conditions used in the calculations are shown for each surface. Surfaces marked $Q = 0$ are adiabatic surfaces, those marked with Q_r radiate to a heat sink at 371°C, and that marked Q_s denotes heat transfer to the sodium coolant at 371°C. These surfaces are at sufficient distances from the main heat-flow paths so that the actual boundary conditions do not significantly influence the uranium and transition-material temperatures.

Fig. B-31.

Material configuration and operating temperatures in voiding device.

The voiding-device dimensions chosen for these calculations are shown in Fig. B-31. The weight of enriched uranium (93% ^{235}U) used in these calculations was 0.157 g, and the fission rates were average values from Run 81B data for Row 6 in EBR-II. Gamma heating for stainless steel was taken from the EBR-II Guide, while gamma heating in tantalum was estimated as 2.6 times the value for stainless steel. The calculated transition-material temperature was 910 $^{\circ}\text{C}$ and the peak uranium temperature was 1060 $^{\circ}\text{C}$. Figure B-31 shows the tantalum wall temperature as a function of axial location for this calculation. There is little radial temperature variation in the axial region between 0 and 7 mm.

The quantity of uranium required will depend mainly on the core location in EBR-II chosen for the experiment, i.e., the fission rate, and the dimensions of the tantalum tubing which contains the uranium. For these calculations, the tantalum tubing dimensions were 2.54-mm o.d. and 0.254-mm thickness. Fabrication tests are required to determine whether reliable transition joints

can be made with this thin-wall tubing. Increasing the tantalum wall thickness to 0.508 mm would increase the amount of uranium required to about 0.225 g.

The maximum uranium temperature before bond voiding will depend on the heat-transfer coefficient between the uranium heater and the tantalum walls. In these calculations, perfect thermal contact was assumed to exist between the uranium and the tantalum. Additional calculations were performed to determine the effect of lower heat-transfer coefficients on the maximum uranium temperatures. The results of these calculations are listed in Table B-XXXIII. For a heat-transfer coefficient of $0.57 \text{ W/cm}^2 \cdot ^\circ\text{C}$ ($1000 \text{ Btu/h} \cdot \text{ft}^2 \cdot ^\circ\text{F}$), the maximum uranium temperature was increased to 1095°C , which is 35°C over that for perfect thermal contact. This is in the range of expected contact coefficients, since initial dimensional tolerances will ensure a tight fit between the uranium and tantalum, and the uranium will expand more rapidly than the tantalum during heating. For a heat-transfer coefficient of $0.11 \text{ W/cm}^2 \cdot ^\circ\text{C}$ ($200 \text{ Btu/h} \cdot \text{ft}^2 \cdot ^\circ\text{F}$), the maximum uranium temperature approaches its melting point. This value would represent very poor contact between the uranium and tantalum. If local uranium melting were to result from poor thermal contact between the uranium and tantalum, the molten uranium would resolidify upon contacting the tantalum and provide an increased heat-transfer coefficient. Thus, any uranium melting owing to poor thermal contact would be temporary and self-correcting.

In EBR-II, the uranium fission heater will be located about 20 mm above the bottom of the core before voiding, while after voiding it will drop to about 150 mm below the bottom of the core. Because of the increased fraction of lower energy neutrons in the reflector region below the core, there will be a higher fission rate in the ^{235}U after it has dropped to the bottom of the

TABLE B-XXXIII

EFFECT OF URANIUM-TANTALUM HEAT-TRANSFER
COEFFICIENT ON THE MAXIMUM URANIUM TEMPERATURE

Heat-Transfer Coefficient ($\text{W/cm}^2 \cdot ^\circ\text{C}$)	Maximum Uranium Temperature ($^\circ\text{C}$)
∞	1060
0.57	1095
0.11	1130

reservoir than when it is in the core.^{26,27} The ratio of the fission rate after voiding to the rate before voiding will range from about 1.4 for operation in Row 4 of EBR-II to 1.6 for operation in Row 7.

In addition to a change in fission rate, the uranium fission heater will experience a change in environment after it drops to the bottom of the reservoir. Estimates of the temperature and heat content of the uranium fission heater in its tantalum cup, the rate at which sodium will enter the voiding-device reservoir, and the vaporization and condensation rates of sodium entering the voiding-device reservoir indicate that the sodium will quickly cool the uranium and tantalum. The sodium will provide a high-conductivity path to the tantalum capsule wall. Peak uranium temperatures in this configuration after voiding were estimated to be about 500°C.

To help ensure that bond voiding occurs "at or near" initial full-power operation in EBR-II, compensation for uncertainties in fission rates, sub-assembly positioning, element operating temperatures, and heat-transfer calculations must be inherent in the device design. The fission heaters can be designed to produce transition-joint separation at fission rates that correspond to a reactor power level between 54 and 57 MW. The peak linear power of the experiments during the initial irradiation period will be about 10% greater than the steady-state design value. In the absence of uncertainties, bond voiding would occur at rated element power, and the element would then operate slightly above this power level for a short period of time. There will be an inherent uncertainty of ± 10 to 15% in the linear power level at which voiding occurs.

2. Voiding-Device Testing

A test of the voiding-device operation is planned for the Omega West Reactor (OWR) before fabrication of devices for LOB-1 elements. OWR is an 8-MW thermal reactor, which can accommodate the requirements of the voiding-device test in an in-core experiment position. Design measurements for the test vehicle include provisions for heating to melt sodium in the voiding-device at the start of the test and for cooling to remove the fission and gamma energy generated during irradiation. Temperatures around the voiding device must be monitored and controlled to simulate the conditions expected in EBR-II.

A preliminary design of the OWR test vehicle was completed. The voiding device will be contained in a short section of 7.87-mm-o.d. Type 316 stainless

steel cladding. Two sheathed thermocouples will be brazed into the top end plug of the cladding to monitor the sodium temperature in the vicinity of the voiding-device screen. The cladding and welded end plugs will act as the inner (secondary) containment of the uranium. Helium gas flowing in an annular space between the cladding and a flow shroud tube (12.7-mm o.d. x 0.89-mm wall) will be used to cool the cladding during the test. The voiding-device temperature will be controlled to simulate temperatures in EBR-II by varying the helium flow rate. The entire voiding device, cladding, and flow shroud assembly will be contained in a 50.8-mm-o.d. by 0.89-mm-wall stainless steel capsule. This capsule will act as the primary containment for the uranium and will be cooled by the reactor cooling water.

During the OWR test of the voiding device, some method of detecting sodium entry into the evacuated reservoir is needed. A thermocouple will be located in the bottom end plug of the cladding, about 2 mm below the bottom of the voiding-device reservoir. When the uranium fission heater and sodium contact the bottom of the reservoir, this thermocouple will experience a temperature rise as a result of the increased heat generation in its vicinity. The magnitude of the temperature rise will depend on the gamma heating in the adjacent tantalum and stainless steel as well as the fission heating in the uranium. Depending on the gamma-heating rates, temperature increases at this thermocouple were calculated to be between 50 and 100°C. The time constant of the thermocouple-end plug system is about 20 s.

Even if the voiding device used for the OWR test is a duplicate of the device that will be used in EBR-II, the thermal flux of OWR will result in a significant flux depression in the uranium fission heater. An estimate of the self-shielding in the uranium indicates that the surface-to-volume average flux ratio is about 5 in OWR. A series of voiding-device heat-transfer calculations were performed to determine the effect of different flux depressions in the uranium. The results indicate that the transition-joint temperature is not significantly affected by variations in the flux depression in the uranium. The temperature of the transition joint is primarily controlled by heat losses up the sodium-filled tantalum tube. This tube has about a 4-to-1 length-to-diameter ratio and is composed of high-thermal-conductivity materials. The system allows radial redistribution of any nonuniformities in the heat flow and results in similar axial temperature gradients for a wide range of flux depressions.

3. Experiment Plan for Sodium-Bond-Loss Testing

A description of experiments planned for near-term bond-loss testing is given in Table B-XXXIV. The first four experiments will provide a direct comparison of shrouded and unshrouded carbide and nitride fuel element behavior that results from rapid bond loss. The design variables (fuel density, diameter, ^{235}U enrichment, shroud characteristics, and cladding dimensions) for carbide-fueled experiments are similar to those in Phase I and IA steady-state irradiations tests (AIR-1 and K-7). The design variables for Experiments 3 and 4 are commensurate with those planned for future steady-state irradiations testing. To accommodate the larger-diameter nitride fuel in Experiment 3, the shroud tube will be fabricated from excess K4 Series shroud material. This material is 25%-cold-worked Type 316 stainless steel, and the dimensions are 6.80-mm o.d. by 6.64-mm i.d. The materials and design variables for LOB experiments should reproduce, as closely as possible, those that are planned

TABLE B-XXXIV
PROPOSED EXPERIMENTS FOR LOB-1

FUEL: $(\text{U,Pu})\text{C} + 10\% (\text{U,Pu})_2\text{C}_3$, 98% T.D.; and $(\text{U,Pu})\text{N}$, 93-98% T.D.

MAXIMUM CLADDING TEMPERATURES: 580°C for 75 kW/m and 620°C for 90 kW/m.

GOAL BURNUP: $\sim 0.06^a$ and ~ 3.0 at. %.

ELEMENT DIAMETER: 7.87 mm.

TEST VEHICLE: Mark A-19B with unencapsulated elements.

Expt. No.	Fuel Type	^{235}U Enrich. (%)	Fuel Diam (mm)	Shroud Material	Diametral Gap (mm) ^b	Cladding Type	Cladding Wall (mm)	Peak Linear Power (kW/m) ^d
1	C	72	6.35	316SS	0.76	20CW316	0.38	75
2	C	72	6.35	None	0.51	20CW316	0.51	75
3	N	70-75	6.55	316SS	0.56	20CW316	0.38	75
4	N	70-75	6.55	None	0.30	20CW316	0.51	75
5	C	93	6.30	316SS	0.81	20CW316	0.38	90
6	C	93	6.30	None	0.56	20CW316	0.51	90
7	C	72	6.35	Mo	0.76	20CW316	0.38	75
8	C	72	6.35	316SS	0.76	20CW316	0.38	75
9	C	72	6.35	None	0.51	PE-16	0.38	75
10	C	72	6.35	316SS	0.76	20CW316	0.38	75
11	C	93	6.30	316SS	0.81	20CW316	0.38	90

^aGoal burnup for Experiments 10 and 11.

^bFuel-cladding diametral gap.

^c20CW316 = 20% cold-worked Type 316SS; PE-16 = solution-treated PE-16.

^dOperation for initial irradiation period will be at 1.1 times listed value.

for steady-state irradiations. If nitride fuel with the appropriate characteristics is not available, the test will be conducted with only the carbide-fueled experiments listed in Table B-XXXIV.

Experiments 5 and 6 are included to simulate the behavior expected following bond loss from 9.40-mm-o.d. elements with shrouded and unshrouded carbide fuels operating at a peak linear power of 99 kW/m. The steady-state linear power in Experiments 5 and 6 is about 10% lower than that in the large-diameter elements, while the cladding heat flux is about 10% higher. The objectives of these tests are to assess the expected consequences of bond loss from 9.40-mm-o.d. high-power elements and to evaluate the effect of different steady-state power on fuel and shroud behavior in 7.87-mm-o.d. elements.

Transient heat-transfer calculations were performed to compare temperatures that result from bond loss in 9.40-mm-o.d. elements operating at a peak linear power of 99 kW/m with those in 7.87-mm-o.d. elements operating at 90 kW/m. The one-dimensional (radial) heat-transfer calculations used a model developed by Kerrisk²⁸ for the CINDA code. In this model, the fuel, shroud, and cladding are assumed to be concentric. After the steady-state temperature distribution is calculated with the sodium bond intact, the transient calculation is initiated by replacing the sodium-bond conductivity with that of the plenum gas, which is a mixture of helium and xenon tag gases. In these calculations, the fuel and shroud were allowed to slump if the fuel surface reached the solidus temperature of the mixed-carbide fuel.

The calculated maximum temperatures that resulted from bond loss in large-diameter (9.40-mm) elements operating at 99 kW/m are compared in Table B-XXXV with those in 7.87-mm-o.d. elements operating at 90 kW/m and 75 kW/m. Calculations were performed for the element midplane, where the element power is greatest, and the top of the fuel column, where the coolant temperature is highest. The maximum temperatures for the 9.40-mm-o.d. element operating at 99 kW/m are in good agreement with those for the 7.87-mm-o.d. element operating at 90 kW/m. Since the maximum fuel surface temperatures for all cases considered were less than the solidus temperature of the mixed carbide (2290°C), slumping was not permitted to occur. Because of similarities in predicted fuel behavior and maximum temperatures, the use of 7.87-mm-o.d. elements operating at 90 kW/m in LOB-1 should provide a good simulation of the behavior expected in large-diameter elements operating at 99 kW/m.

TABLE B-XXXV
CALCULATED MAXIMUM TEMPERATURES FOLLOWING SODIUM BOND LOSS

Element Description	Location	Temperature (°C)			
		Cladding i.d.	Shroud	Fuel Surface	Fuel Centerline
9.40 mm o.d. 99 kW/m	Core Centerline	540	2155	2215	2545
	Top of Core	595	2040	2205	2455
	Core Centerline	540	--	2180	2510
	Top of Core	595	--	2145	2385
	Core Centerline	540	2160	2215	2530
	Top of Core	595	1945	2210	2450
7.87 mm o.d. 90 kW/m	Core Centerline	545	--	2175	2490
	Top of Core	585	--	2085	2310
	Core Centerline	540	1980	2190	2455
	Top of Core	600	1815	2110	2310
	Core Centerline	540	--	2105	2360
	Top of Core	595	--	1920	2120
7.87 mm o.d. 75 kW/m	Core Centerline	540	--	2105	2360
	Top of Core	595	--	1920	2120
	Core Centerline	540	1980	2190	2455
	Top of Core	600	1815	2110	2310
	Core Centerline	540	--	2105	2360
	Top of Core	595	--	1920	2120

No information currently exists on the consequences of molten fuel and stainless steel shroud material slumping to contact the element cladding. A potential consequence of such an event is the formation of low-melting eutectics between fuel and shroud components. Operation after the bond-loss event with the fuel and shroud material resolidified against the cladding may result in accelerated chemical interactions, which could produce cladding breaches. Although the probability for this occurring may be low, the impact on selection of promising element concepts for advanced development could be large. Based on results of steady-state irradiations, shrouded fuels currently appear to offer greater potential than unshrouded fuels. Continued operation of seven Series U-5100 elements with iron, stainless steel, vanadium, and tantalum shrouds has tentatively shown that the particular material used for shroud tubes may be of secondary importance to the successful operation of the concept.

Experiment 7, which has a "high-melting" shroud was included to demonstrate the viability of the shrouded-fuel concept, in case the behavior in elements with stainless steel shrouds is unacceptable. Several candidate materials were considered. Those that melt at temperatures greater than the fuel have the potential for reducing both fuel-cladding chemical and mechanical interactions following bond loss. Molybdenum or molybdenum alloys satisfied the criteria for melting point, fast-neutron absorption, fuel and sodium compatibility, and fabricability. For these reasons, molybdenum is planned for use in Experiment 7.

Experiment 8 will be designed to void the sodium bond from only the upper portion (about one-half) of the fuel column. This will be accomplished by loading more sodium into the element than the reservoir can accommodate, so that sodium will be forced down toward the reservoir from above the fuel column and fill the lower fuel-cladding annulus. Experiment 8 is intended to simulate a cladding breach in the fueled length or rapid growth of fission gas bubbles and/or bond boiling. The objectives of this test are to assess the consequences of substantial but incomplete bond loss on element performance, to compare the fuel and shroud behavior with that in elements that undergo complete bond voiding, and to determine the behavior of sodium remaining in the lower part of the bond gap. The results of Experiment 8 will aid in evaluating the behavior of two carbide-fueled elements [U-248 (breached) and U-246 (unbreached)] which had no sodium in their fuel-cladding annuli after steady-state irradiation in EBR-II.

The operating conditions and design variables for Experiment 9 are similar to those for Experiment 2, except that an advanced alloy (solution-treated PE-16) cladding is used. This experiment will provide a direct comparison of fuel-cladding interactions in cold-worked Type 316 stainless steel and PE-16 claddings after bond loss.

The objectives of Experiments 10 and 11, which duplicate Experiments 1 and 5, are to determine the short-term consequences of bond loss from elements with shrouded carbide fuel and to characterize the fuel, shroud, and cladding condition at the start of post-LOB irradiation. After a short irradiation period of between one and three full-power days, these experiments will be removed from the subassembly and destructively examined. This interim examination after the initial irradiation period will allow verification of bond

voiding, removal of the two experiments for destructive examination, and non-destructive examination of elements that are continuing irradiation.

The irradiation testing of experiments listed in Table B-XXXIV, as unencapsulated elements in a Mark A-19B subassembly, was found to be technically feasible by EBR-II Project. For this irradiation, the desired element temperatures and subassembly outlet temperatures will be obtained by orificing coolant flow at the subassembly entrance and by blocking the outlets of vacant flow channels. Small holes (~ 0.7 mm) in the blocking plates will permit sodium to fill vacant channels during loading and to drain during subassembly-removal operations. Operation during the initial irradiation period at peak linear powers that are 10% greater than those listed in Table B-XXXIV is required to ensure operation of the voiding device at or near the design power rating of the elements. Steady-state cladding temperatures during this portion of the test will be maintained at approximately 580 and 620°C for low- and high-power experiments, respectively.

B. Series UL Transient Tests

The Series UL tests were a joint effort of Los Alamos Scientific Laboratory and Gulf United Nuclear Fuels Corporation to simulate transient overpower operation of advanced LMFBR fuel elements. The objective of the tests was to determine the effect of prior steady-state irradiation on the failure threshold of helium- and sodium-bonded fuel elements containing uranium-plutonium carbide fuel. A description of the test elements, the test data, and much of the postirradiation examination data was given in a previous report.²⁹ This report describes an analysis of the fuel element temperatures during the transients and presents some additional postirradiation examination data. Table B-XXXVI summarizes the Series UL test data previously presented,²⁹ while Fig. B-32 shows a sketch of the fuel element, inner capsule, and TREAT capsule assembly.

1. Series UL Inner Thermocouple Locations and Time Constant

During the early postirradiation examination of the Series UL tests, it became apparent that the measured temperatures from the inner thermocouples did not match their calculated responses. The inner thermocouples (TC 2, TC 4, and TC 6 in Fig. B-32) terminate in the annulus between the heat sink and the fuel-element cladding. Two potential problems with the calculations were that the time constant of the ungrounded-junction thermocouples was unknown, and that the actual location of the thermocouple junctions was also

TABLE B-XXXVI
SERIES UL TEST DATA

	TEST			
	UL-1	UL-2	UL-3	UL-4
Fuel	$(U_{0.85}Pu_{0.15})C + 10-20 \text{ V/o } (U_{0.85}Pu_{0.15})_2C_3$			
Bond Material	He	Na	He	Na
Diametral Bond Thickness (mm)	0.25	0.77	0.25	0.77
Cladding Material ^a	316SS	304SS	316SS	304SS
Cladding Thickness (mm)	0.56	0.38	0.56	0.38
Burnup (at.%)	0	0	3.56 ^b	3.62 ^b
Reactor Energy (MW/s)	159	161.5	157	156
Peak Reactor Power (MW)	106	104	101.5	101
Fission Energy (J/g fuel)	912	967	866	891
Peak Fission Power (kW/m)	239	233	221	217
Peak Fuel Temperature (°C)	2300	1500 ^c	1700-2200	1400 ^c
Peak Cladding Temperature (°C)	850	1000	800-900	950
Posttransient Condition	unfailed	unfailed	failed	unfailed

^aSolution-annealed cladding.

^bIrradiated in EBR-II at 36 kW/m in subassembly X055.

^cCalculated assuming no sodium-bond boiling.

unknown. Thermocouple-junction locations were determined from radiographs and other dimensional measurements of the thermocouples and heat sinks from the four Series UL capsules. Table B-XXXVII lists the measured locations of the thermocouple junctions with respect to the centerline of the fuel in the element. The range associated with each value represents an uncertainty resulting from the ability of the thermocouples to move slightly within their heat sink grooves. The effect of the uncertainty on the calculated temperatures was estimated for the UL-4 test. For a variation of ± 0.25 mm around the nominal location, the effect on the calculated temperatures was less than $\pm 15^\circ\text{C}$. For calculational purposes, an average of the radial locations of the three thermocouples in each capsule was taken as the nominal thermocouple location. The average radial positions were 7.25 mm for the UL-1 capsule, 7.03 mm for the UL-2 capsule, 6.36 mm for the UL-3 capsule, and 6.35 mm for the UL-4 capsule. The differences in thermocouple junction locations led to significantly different thermal responses of the thermocouples, particularly

Fig. B-32.
Series UL capsule schematic.

for the UL-1 and UL-2 capsules as compared to the UL-3 and UL-4 capsules.

An estimate of the thermocouple time constant was obtained from measurements on spare thermocouples from the lot that was purchased for the Series UL tests. The measurements were made by using a flash laser to heat the outside surface of the thermocouple sheath while the thermocouple-junction temperature was recorded on an oscilloscope. Heat losses from the thermocouples after the laser heating were small enough so that the laser heating amounted to a step increase in the sheath temperature. The time constant was taken as the time required for the junction temperature to reach 63% of its final value. When the thermocouple sheath was heated from the side, time constants from 84 to 125 ms were measured for three thermocouples. The average time constant was 105 ms. When the thermocouple sheath was heated from the end (tip) only, time constants from 197 to 204 ms were measured for one thermocouple. The average was 201 ms. The difference was consistent with the fact that for thermocouples heated on the end, the heat flow path to the junction is longer than when they are heated on the side. The actual heating of the thermocouple sheath in the inner capsule was different from either of

TABLE B-XXXVII
RADIAL LOCATION OF THERMOCOUPLE JUNCTION
RELATIVE TO CENTERLINE OF FUEL (mm)

	Top of Fuel Column TC-2	Fuel Midplane TC-4	Bottom of Fuel Column TC-6
UL-1	7.66 ± 0.09	7.21 ± 0.23	6.87 ± 0.24
UL-2	7.12 ± 0.21	7.07 ± 0.24	6.01 ± 0.20
UL-3	6.65 ± 0.17	6.06 ± 0.17	N.A. ^a
UL-4	6.10 ± 0.23	6.62 ± 0.20	6.32 ± 0.15

^aThermocouple damaged during removal; measurements not available.

the two cases examined here. Since the thermocouples were surrounded by a high-thermal-conductivity medium (NaK and nickel) in the inner capsule, it was felt that the side-heating time-constant measurements more nearly represented the inner capsule condition. For this reason, thermocouple time constants were taken as 105 ms for all calculations.

2. UL-1 Test Analysis

Test UL-1 was designed to produce central melting of the fuel in the unirradiated helium-bonded element. Fuel, cladding, thermocouple, and capsule temperatures were calculated for the UL-1 test using the actual reactor power profile, shown in Fig. B-33. For these calculations, the thermocouple junction was assumed to be located at the average radial position of the three inner thermocouples in the UL-1 capsule. Figure B-34 shows the measured inner thermocouple temperatures compared with the calculated thermocouple temperature (solid line). The agreement is generally good. Figure B-35 shows the calculated fuel-centerline and cladding-inside-surface temperatures during the transient. The calculated maximum fuel-centerline temperature was 2295°C at 4.3 s into the transient. This temperature is just at the solidus temperature of the fuel. The calculated maximum cladding temperature was 855°C, also at 4.3 s into the transient. The calculations indicated that no fuel-cladding mechanical contact occurred during the transient.

The postirradiation examination of the element from the UL-1 test was completed, but only a preliminary analysis of the data has been made. The element was unfailed after the transient. No signs of fuel melting were seen in the posttransient metallography or radiography. Some fuel-pellet cracking was the only observable result of the transient.

Fig. B-33.
Reactor power as a function of time in test UL-1.

Fig. B-34.
Measured and calculated inner thermocouple temperatures in test UL-1 (* = near top of fuel column, + = axial midplane of fuel, □ = near bottom of fuel column, — = calculated temperature).

3. UL-2 Test Analysis

Test UL-2 was designed to produce boiling and ejection of the sodium bond in the unirradiated sodium-bonded element, followed by fuel melting. An examination of the measured thermocouple temperatures from this test gave no indication of any perturbations that might be associated with element bond boiling or ejection. The posttest radiograph showed the sodium level at its expected location in the element, indicating no loss of sodium from the bond region. Thus, the evidence available to date indicates that the element sodium bond was not ejected during the transient, and that, if sodium-bond boiling occurred, it was confined to a very short time during the peak temperatures in the bond region.

Fuel, cladding, thermocouple, and capsule temperatures were calculated for the UL-2 test using the actual reactor power profile and assuming no element bond boiling or ejection. The reactor power as a function of time for this test was similar to that for the UL-1 test (see Fig. B-33). As in the case of the UL-1 test, the thermocouple junction was assumed to be located at the average radial position of the three inner thermocouples in the UL-2

Fig. B-35.
Fuel and cladding temperatures in test UL-1 (* = fuel centerline, □ = cladding inside surface).

capsule. Figure B-36 shows the measured inner thermocouple temperatures compared with the calculated thermocouple temperature (solid line). The agreement is quite good. Figure B-37 shows the calculated fuel-centerline and cladding-inside-surface temperatures during the transient. The maximum fuel-centerline temperature was 1510°C at 4.2 s into the transient. The maximum cladding temperature was 1000°C at 4.3 s into the transient. The maximum fuel temperature for the UL-2 test was much lower than that in the UL-1 test, while the maximum cladding temperature for the UL-2 test was higher.

This was due to the presence of liquid sodium in the bond region throughout the transient. Had bond boiling and ejection occurred as expected, peak fuel and cladding temperatures for the two tests would have been similar. The maximum element sodium-bond temperature calculated was 1040°C at 4.3 s. This was about 100°C above the calculated boiling temperature of the sodium in the element.

The postirradiation examination of the element from the UL-2 test was completed but only a preliminary analysis of the data has been made. The element was unfailed and most of the fuel pellets were not cracked after the transient.

4. UL-3 Test Analysis

Test UL-3 was designed to duplicate test UL-1 with an irradiated element. This element failed as a result of the transient, exhibiting three axial cladding cracks in the lower half of the fuel column. Postirradiation data from this element have already been reported.²⁹

An analysis of the UL-3 test results is difficult because the state of the irradiated helium-bonded element at the start of the transient is uncertain. Both fuel and cladding properties change with irradiation; but the most significant uncertainty is with the fuel-cladding heat-transfer coefficient or gap conductance after 3.56 at.% burnup. Work with oxide fuel elements has indicated

Fig. B-36.

Measured and calculated inner thermocouple temperatures in test UL-2 (* = near top of fuel column, + = axial midplane of fuel, □ = near bottom of fuel column, — = calculated temperature).

Fig. B-37.

Fuel and cladding temperatures in test UL-2 (* = fuel centerline, □ = cladding inside surface).

that this parameter is a function of the fuel-cladding gap size and gap-gas composition, and, after the fuel-cladding gap has closed, it is also a function of the contact pressure between the fuel and cladding.³⁰ But the fuel-cladding contact pressure during a rapid transient without fuel melting, such as the UL-3 test, is mainly caused by differential thermal expansion of the fuel and cladding. Fuel temperatures, which control the fuel expansion, are strongly influenced by the rate of heat loss from the fuel, i.e., the gap conductance. Thus, the gap conductance and the fuel-cladding contact pressure are strongly interrelated parameters that must be consistent in any calculation. In addition to the uncertainty in these parameters, the expected fuel-cladding mechanical interaction requires an analysis of the inelastic cladding behavior during the transient. This is a difficult calculation, which can only be performed with many approximations at this time.

Data from the UL-3 test have been analyzed with two goals in mind: (1) to choose the gap conductance to match the calculated and measured thermocouple temperatures, and (2) to choose the cladding mechanical properties so that the calculated inelastic tangential strain matches the value estimated from the

cladding-diameter change that resulted from the transient. The overall objective of the analysis is to show that the fuel-element behavior can be calculated with reasonable values for the element parameters.

The gap conductance used is based on a model for oxide elements. The total gap conductance (h) is considered to be the sum of two parts: a conductance across the open gap (h_g), and a conductance resulting from contact between the fuel and cladding (h_c). The open gap conductance used was

$$h_g(\text{W/cm}^2 \cdot ^\circ\text{C}) = 0.42 - 8.37 \Delta r \text{ ,}$$

where Δr is the radial gap in cm. The term proportional to Δr approximates the effect of gap size. It includes an evaluation of the gap-gas composition. The constant term is the maximum value of h_g at gap closure ($\Delta r = 0$). It accounts for a minimum residual gap as a result of surface roughness and the temperature-jump effect between the gap gas and the solid surfaces. The contact gap conductance used was

$$\begin{aligned} h_c(\text{W/cm}^2 \cdot ^\circ\text{C}) &= 4.2 (P_c / \sigma_y) \text{ for } P_c \leq \sigma_y \\ &= 4.2 \qquad \qquad \text{for } P_c > \sigma_y \text{ ,} \end{aligned}$$

where P_c is the contact pressure between the fuel and cladding and σ_y is the yield strength of the cladding at the temperature of the cladding inside surface. The contact pressure was calculated assuming no friction between the fuel and cladding.

The cladding deformation was calculated using the method outlined by Schwiebert,³¹ which considers the elastic, plastic, and creep behavior of hollow cylinders subjected to time-dependent, axisymmetric pressure and to thermal-stress loading. The cladding-deformation calculation is coupled to an unsteady-state, heat-transfer calculation of the fuel-element and capsule temperatures. The cladding mechanical properties that most affect the calculated inelastic strain are the cladding yield strength and the fraction of stressed area represented by the elastic domain,³¹ or the strain-hardening parameter.³² The yield strength of the cladding, which was solution-annealed Type 316 stainless steel with a fluence of 3×10^{22} nvt ($E > 0.1$ Mev), was estimated from the data of Fish and Holmes.³³ Table B-XXXVIII shows the yield strength as a

TABLE B-XXXVIII
ASSUMED UL-3 ELEMENT CLADDING YIELD STRENGTH

Temperature (°C)	Yield Strength (MPa)
0	830
200	790
400	760
600	730
800	700
1000	510
1200	270
1400	120
1600	90
1800	60

function of temperature. The strain-hardening parameter represents the ability of the cladding to support some elastic behavior beyond the yield strength. Values between 0.01 and 0.1 have been used for cladding in oxide modeling efforts.^{30,34} A value of 0.044 was used for these calculations. In the UL-3 calculations, creep was not considered.

Figure B-38 shows a plot of the measured thermocouple data from test UL-3. The behavior of TC 2 (near the top of the fuel column) is different from TC 4 (axial midplane of the fuel) and TC 6 (near the bottom of the fuel column). Both TC 4 and TC 6 show a drop in temperature between about 4.5 and 5.2 s. This was attributed to a gas blanketing of the thermocouples following cladding failure and the subsequent release of fission gas from the element.²⁹ Gas blanketing of TC 2 was apparently not significant.

Two calculations of the fuel, cladding, and capsule temperatures were performed using the actual reactor-power profile. The reactor power as a function of time for this test was similar to that for the UL-1 test (see Fig. B-33). For both calculations, the thermocouple junction was assumed to be located at the average radial position of the two inner thermocouples that could be measured in the UL-3 capsule. In the first calculation, the NaK bond surrounding the element was assumed to remain in place throughout the transient. This calculation represented the behavior in the vicinity of TC 2. Figure B-39 shows a plot of the measured TC 2 data and the calculated thermocouple temperature (solid line) while Fig. B-40 shows a plot of the fuel-centerline and cladding-inside-surface temperatures from the calculation.

In the second calculation, the NaK bond surrounding the element was assumed to be ejected between 4.5 and 5.2 s. This represents the behavior in the vicinity of TC 4 and TC 6. Figure B-41 shows a plot of the measured TC 4 and TC 6 data along with the calculated thermocouple temperature (solid line) for this calculation. Figure B-42 shows a plot of the fuel-centerline and cladding-inside-surface temperatures.

An examination of the thermocouple-temperature plots (Figs B-39 and B-41) indicated that the agreement between the calculated and measured temperatures was generally good. The fuel-centerline temperature was only slightly affected by the NaK-bond ejection (compare Figs. B-40 and B-42). The maximum fuel temperature of 1740°C at 4.3 s was the same in both calculations. The maximum fuel temperature during steady-state irradiation in subassembly X055 was ~1350°C. The effect of the NaK-bond ejection on the cladding temperature is very significant. Without NaK ejection, the peak cladding temperature was 930°C at 4.3 s, while with NaK ejection, the peak cladding temperature was 1160°C at 5.2 s. The cladding solidus temperature was approximately 1425°C. A fuel-cladding interaction, ascribed to cladding melting, was seen in two metallographic sections taken from the lower half of the UL-3 element.²⁹ These calculations indicated that, although significant cladding overheating occurred, cladding temperatures remained below the melting range. The peak cladding temperature during steady-state irradiation was ~570°C.

Before the transient, the average cladding diameter of the UL-3 element was relatively constant at 7.725 mm in the fuel region. After the test, the cladding diameter varied from 7.725 mm near the top of the fuel column to about 7.795 mm at 30 to 40 mm below the axial fuel midplane. Data below this point could not be measured owing to the cladding cracks present. The inelastic tangential strain calculated from this diameter change is 9×10^{-3} . The inelastic strain calculated, assuming the NaK bond remained in place, was 8.7×10^{-3} .

5. UL-4 Test Analysis

Test UL-4 was designed to duplicate test UL-2 with an irradiated element. This element was unfailed after the transient, but a sodium plug, containing about 80% of the sodium originally loaded into the element, was found solidified in the plenum region of the element during the postirradiation examination.²⁹ The bond displacement was explained by fission-gas release from the

Fig. B-38.
Measured inner thermocouple temperatures in test UL-3 (* = near top of fuel column, + = axial midplane of fuel, □ = near bottom of fuel column).

Fig. B-39.
Measured and calculated inner thermocouple temperatures in test UL-3. No NaK bond ejection (* = near top of fuel column, — = calculated temperature).

Fig. B-40.
Fuel and cladding temperatures in test UL-3. No NaK bond ejection (* = fuel centerline, □ = cladding inside surface).

Fig. B-41.
Measured and calculated inner thermocouple temperatures in test UL-3. NaK bond ejection from 4.5 - 5.2 s (+ = axial midplane of fuel, □ = near bottom of fuel column, — = calculated temperature).

fuel, while the fact that the sodium remained in the plenum and solidified rather than running back into the bond region was explained by surface effects.²⁹

Data from the UL-4 test have been analyzed in an attempt to determine when fuel-element sodium-bond displacement by fission gas occurred. In one approach, a series of heat-transfer calculations of the thermal behavior in the capsule during the transient was performed. Sodium-bond displacement was simulated by replacing the conductance of the element sodium-bond region by a lower conductance at specific times during the transient. The resulting temperatures calculated at the thermocouple locations were compared with measured temperatures during the transient. If fission gas completely replaced liquid sodium, the bond conductance would decrease by a factor of 2000. Large conductance changes (greater than a factor of 20) were not compatible with the measured thermocouple temperatures unless they occurred after approximately 5.5 s into the transient. As shown in Fig. B-43, power generation occurred between 2.5 and 4.4 s into the transient. Thus, if complete displacement of the bond sodium

Fig. B-42.

Fuel and cladding temperatures in test U-3, NaK bond ejection from 4.5-5.2 s, (* = fuel centerline, $\square$ = cladding inside surface).

Fig. B-43.

Reactor power as a function of time in test UL-4.

occurred, it happened after the transient power generation was complete. Smaller changes in the bond conductance (less than a factor of 10) could have occurred during power generation. Smaller conductance changes were possible if only partial bond replacement occurred. A small amount of sodium remaining in the bond region could have provided conduction paths in parallel to the fission gas, and thus have increased the overall conductance of the bond region significantly. It was estimated that the sodium plug in the plenum of UL-4 contained at least 80% of the sodium originally loaded into the element, but no estimates of the quantity of sodium actually in the bond or its distribution were made. The net result of this analysis was that a unique bond displacement time could not be determined.

In a second approach to determining the bond-displacement time, the measured thermocouple temperatures were examined for systematic perturbations that could be related to changes in conductance of the element sodium bond. Curves of temperature and first derivative of temperature as a function of time for tests UL-2 and UL-4 were examined. No systematic perturbations were observed in the UL-4 data. Random perturbations in the UL-4 data were similar to perturbations seen in data from the UL-2 test, where no bond displacement occurred. Thus, the thermocouple data did not contain recoverable information about the bond displacement time.

The attempt to determine when sodium-bond displacement occurred in test UL-4 resulted in only limited conclusions. If complete bond displacement occurred, it took place well after the power-generation portion of the transient was over. If enough liquid sodium remained in the bond region to provide some parallel conduction paths, bond displacement could have occurred earlier in the transient.

For the purpose of estimating fuel and cladding temperatures during the transient, heat-transfer calculations that assumed no sodium-bond boiling or displacement were performed. The fuel-centerline and cladding-inside-surface temperatures calculated in this manner are shown in Fig. B-44. The maximum fuel temperature during the transient was 1415°C compared with approximately 750°C during steady-state irradiation in subassembly X055. The peak cladding temperature during the transient was 950°C compared with ~550°C during steady-state irradiation. In Fig. B-45, the calculated inner-thermocouple temperatures (solid line) are compared with the inner-thermocouple temperatures measured during the transient. For these calculations, the thermocouple junction

Fig. B-44.

Fuel and cladding temperatures in test UL-4 (* = fuel centerline, $\square$ = cladding inside surface).

Fig. B-45.

Measured and calculated inner thermocouple temperatures in Test UL-4 (* = near top of fuel column, + = axial midplane of fuel, $\square$ = near bottom of fuel column, — = calculated temperature).

was assumed to be located at the average radial position of the three inner thermocouples in the UL-4 capsule. The calculated temperature was in good agreement with the measured temperatures of the upper and midplane thermocouples. The offset in the lower thermocouple temperature was probably related to an instrumentation problem, since the lower thermocouple was indicating essentially the same temperature as the other two thermocouples on another readout instrument just before the transient.

IV. ADVANCED LMFBR FUEL PROPERTIES (W. M. Olson)

The measurement of LMFBR fuel properties was restricted to continuation of vapor-pressure work during FY 1976. Vapor pressures over the U-Pu-C system were to be measured by the Knudsen effusion method. The nature of the vapor species and the pressures relative to known materials were to be determined with a magnetic-sector spectrometer (Hitachi RMU-6). The vapor-pressure-measurement effort was terminated on September 30, 1976 because of changes in program funding.

The Knudsen-effusion section of the vapor-pressure apparatus was modified and installed in a containment hood so that radioactive samples could be studied. A new vacuum system was installed, consisting of stainless steel components and metal seals, ion pumps for evacuation of the ion source, flight-tube and electron-multiplier sections, and a fast turbomolecular pump for evacuation of the Knudsen-cell section during heatup. The new vacuum system minimized problems associated with component outgassings and eliminated problems associated with backstreaming of diffusion-pump oil. The electron-bombardment power-supply control system was modified in an attempt to prevent recurring heater burnout.

The spectrometer was calibrated using an NBS gold sample as the primary standard. The vapor pressure over gold at 1206 K is 54.72 μ Pa. At 1206 K (corrected) the measured peak height, for the gold sample at maximum instrument sensitivity, was 44 recorder-chart divisions, indicating a useful working range of about 1 μ Pa to 10 Pa. The apparatus was calibrated for uranium using a uranium metal sample contained in a graphite cup and tungsten Knudsen cell. The vapor pressure of carbon-saturated uranium is well known, and thus the uranium pressure over the mixed carbides could be determined without recourse to estimated ionization efficiencies. Similar calibration techniques were planned for plutonium, using a two-phase monocarbide-sesquicarbide sample. The calibration runs on the uranium samples indicated that the measurement sensitivity was about one-tenth that observed for the gold sample. However, the detectibility of the vapor species was satisfactory, and the sensitivity adequate for determination of vapor pressures of uranium-plutonium carbides.

During FY 1976T, PuC and (U,Pu)C samples were being prepared for pressure measurements, but the measurements were not initiated. The remaining time was devoted to equipment cleanup and documentation of the equipment modifications required to put the mass spectrometer system into working order. The equipment was "moth-balled" for installation in the new plutonium facility.

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