ANL-75-73

ARGONNE NATIONAL LABORATORY
9700 South Cass Avenue
Argonne, Illinois 60439

**THE SAFETY CONSEQUENCES OF
LOCAL INITIATING EVENTS IN AN LMFBR**

by

R. M. Crawford,* W. W. Marr,
A. Padilla, Jr., and P. Y. Wang**

Reactor Analysis and Safety Division

December 1975

NOTICE
This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Energy Research and Development Administration, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

***Sargent & Lundy, Chicago, Illinois**

****Hanford Engineering Development Laboratory**

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

TABLE OF CONTENTS

	<u>Page</u>
ABSTRACT	9
I. INTRODUCTION AND PROBLEM DEFINITION	9
II. INITIAL FAULTS AND THEIR CONSEQUENCES	11
III. REACTOR-OPERATING EXPERIENCE	13
IV. ANALYSIS OF POTENTIAL MODES OF FAILURE PROPAGATION	17
A. Fission-gas Release	18
1. Classification of Potential Mechanisms for Fuel-failure Propagation	18
2. Analysis of Potential Modes of Fuel-failure Propagation: Thermal Effects	23
3. Analysis of Potential Modes of Fuel-failure Propagation: Mechanical Effects	29
B. Local Blockages in Pin Bundles	32
1. An Insulating Blockage	33
2. A Porous Blockage	36
3. Planar In-core Blockage	38
4. Local Coolant Boiling	40
C. Limited Thermal Interaction between Molten Fuel and Coolant	42
1. Local Pressure Generation	43
2. Mechanical Interactions between Fuel Pins	43
3. Thermal Interactions between Fuel Pins	43
4. Fuel Debris	44
V. OPERATION WITH DEFECTED FUEL	45
A. Fission Products Released from Failed Pins to Coolant	45
B. Sodium-Fuel Chemical Reaction	46
VI. SUMMARY AND CONCLUSIONS	50

BLANK PAGE

TABLE OF CONTENTS

	<u>Page</u>
APPENDIXES	
A. Effects of Dispersed Gas in the Reactor Core.	51
1. Summary.	51
2. Introduction.	52
3. Analysis	52
4. Results and Discussion	58
B. Local Heat-generating Porous Blockages.	60
1. Summary.	60
2. Introduction.	60
3. Analysis	60
4. Parametric Study	63
5. Results and Comments.	64
C. Creep Analysis of Cladding Material under Internal Pressure and Temperature Gradients.	66
1. Summary.	66
2. Introduction.	66
3. Analysis	66
4. Numerical Calculations	70
5. Conclusions	72
D. Fault Trees	73
E. Overpower Fuel Pin.	78
1. Introduction.	78
2. FFTF Parameters	79
3. Effect of FFTF Parameters	83
4. Effect of Power Level	84
5. Operation with Molten Fuel.	85
ACKNOWLEDGMENTS	87
REFERENCES	88

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1.	Potential Initial Faults and Failure-propagation Modes.	12
2.	Structure Evolution in an Oxide-fuel Pin	19
3.	Potential Mechanisms for Pin-to-Pin Failure Propagation due to Fission-gas Release in LMFBR Fuel Subassemblies	20
4.	Model for Various Release Rates	20
5.	Mean Cladding Temperatures of LMFBR Fuel Pins vs Time Subsequent to Complete Thermal Insulation at Nominal Power . . .	23
6.	Schematic Cross Section of Submerged Impinging Gas Jet	25
7.	Effect of Dispersed Gas in Sodium	27
8.	Schematic Representation of a Fuel Subassembly of the Fast Flux Test Facility (FFTF)	28
9.	Lower and Upper Gas-Liquid Interface inside an FFTF Fuel Subassembly vs Time after Simultaneous Rupture of 10 or More Fuel Pins at the Outlet Region of the Core, Assuming Zero Gas-flow Resistance.	29
10.	The Effects of Support Length L and Peak Pressure p_0 on Moment M_{max}	31
11.	The Effects of Fuel-pin Radius and Temperature on Moment: $t = 0.014$ in.	31
12.	The Effects of Fuel-pin Radius and Temperature on Moment: $t = 0.010$ in.	32
13.	Flow Passing through a Porous Bed	36
14.	Maximum Porous-bed Thickness to Produce Coolant Boiling for Various Bed Porosities and Particle Sizes	37
15.	UO_2 Particle Sizes from Interactions with Sodium	38
16.	Model of Coolant-channel Blockage	38
17.	Coolant-temperature Increase Downstream of a Blockage	39
18.	Detection of an Inlet Blockage	40
19.	Flow-blockage Model	40
20.	Bubble Growth and Collapse for Local Boiling behind a 50% Flow- area Blockage in an FFTF Subassembly.	41
21.	Velocity of Coolant Stream Required to Sweep Out Fuel Debris . .	44
22.	Fuel-element Swelling vs Time after Failure	47

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
A.1.	Effect of Fission Gas on Flow and Temperatures in Fuel Assembly at Full Power and Constant Pump Speed	51
A.2.	Two-phase Friction Multiplier: Baroczy Correlation	54
A.3.	Void Fraction vs Quality: Baroczy Correlation	54
A.4.	Slip Ratio vs Void Fraction: Baroczy Correlation	55
A.5.	Estimates of Two-phase Thermal Conductivity.	57
A.6.	Flow-pattern Prediction Chart for Vertical Flow	57
B.1.	Analytical Model of a Porous Blockage	60
B.2.	Temperature-Time Histories of Coolant Passing through a Porous Bed	65
C.1.	Loading and Geometry of a Thin-walled Cylindrical Shell.	66
C.2.	Distribution of Tangential Stress across the Wall for $\Delta T/T_0 = 0.03$	71
C.3.	Distribution of Tangential Stress across the Wall for $\Delta T/T_0 = 0.06$	71
C.4.	Effect of Temperature Gradient on Stress Ratio	71
C.5.	Effect of Temperature Gradient on the Life of the Shell	71
D.1.	Definition of Symbols for Event Presentation in Fault-tree Analysis	73
D.2.	Definition of Symbols for Logic Operations in Fault-tree Analysis	74
D.3.	Pin-to-Pin Failure Propagation.	75
D.4.	Abnormal Cladding-surface Conditions	76
D.5.	Loss of Cladding Integrity	77
E.1.	Fuel-pin-failure Model: Overenriched Fuel Pin.	79
E.2.	Westinghouse Correlations for Thermal Conductivity of Mixed-oxide Fuel	82
E.3.	Equiaxed-grain-growth Temperature	82
E.4.	Columnar-grain-growth Temperature.	82
E.5.	Fuel-cladding Gap Conductances for Peak-power FFTF Fuel Pins	83
E.6.	Comparison of Fuel Geometries in FFTF Overpower Fuel Pins.	84

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
E.7.	Molten-fuel Inventory in FFTF Overpower Pin.	85
E.8.	Initial Temperature Map for Initially Molten UO_2 Contacting Initially Solid Stainless Steel, Showing Region in Which Simul- taneous UO_2 Solidification and Steel Melting Occur	86

LIST OF TABLES

<u>No.</u>	<u>Title</u>	<u>Page</u>
I.	Brief Tabulation of Oxide-fuel-pin Failures Classified by Sponsoring Country	13
II.	Characterization of Failed Oxide Rods among World's Fast-reactor Irradiations	14
III.	Experience with EBR-II Fuel-pin Irradiations	16
IV.	Summary of Results for Insulating Blockages.	34
V.	Cladding Temperatures Resulting from Local Channel Plugging .	34
VI.	Experience with Mixed-oxide-fuel Failure.	48
E.1.	Parameters for Analysis of Molten-fuel Inventory in FFTF Overpower Fuel Pin	80
E.2.	Thermal-conductivity Correlations for Mixed-oxide Fuel.	81
E.3.	Fuel-restructuring Models	82
E.4.	Effect of FFTF Parameters	83

THE SAFETY CONSEQUENCES OF LOCAL INITIATING EVENTS IN AN LMFBR

by

R. M. Crawford, W. W. Marr,
A. Padilla, Jr., and P. Y. Wang

ABSTRACT

The principal objective of this report is to review and analyze the potential for fuel-failure propagation in an LMFBR at or near normal conditions. A coherent argument is constructed, using existing documented results where possible, and providing results of additional, new analysis where there are believed to be gaps or serious discrepancies in the current arguments.

Results are presented to support the conclusion that although some individual fuel-pin failure may occur, rapid failure-propagation spreading among a large number of fuel pins in a subassembly is unlikely in an operating LMFBR. This conclusion is supported by operating experience, mechanistic analyses of failure-propagation phenomena, and experiments. In addition, some of the consequences of continued operation with defected fuel are considered.

I. INTRODUCTION AND PROBLEM DEFINITION

The principal objective of this report is to review and analyze the possibility of propagation of fuel-pin failures in an LMFBR. This is being done to provide a visible demonstration of LMFBR safety, even though there has been no evidence of propagation of fuel-pin failure in currently operating reactors. Operating experience, experimental evidence, and upper-limit analyses (where experimental data are not available) support the conclusion that fuel-pin failure will not propagate in an LMFBR environment. Further, if propagation is hypothesized, it is most likely self-limiting and does not rapidly spread to neighboring fuel pins, nor represent a significant hazard to the public for all credible initiating events. This report considers only local initiating accidents, which occur during otherwise normal operating conditions in the reactor. Neither the origin of the accident nor the probability of its occurrence is discussed extensively. The emphasis in this report is on the consequences of certain postulated accidents, and the accident sequence is followed only to the point at which propagation ceases or a large fraction of the subassembly is

damaged severely. This report is an extension of the earlier work of Judd et al.,¹ which includes the results of recent analyses and experiments.

It is convenient to distinguish between local initiating events, in which failure is confined within the initial fuel subassembly without losing the integrity of the subassembly duct, and those hypothetical events in which it is postulated that either the subassembly boundary is breached or there is a potential for subassembly-to-subassembly failure propagation. This report considers the consequences of several initial faults, analyzes potential modes of failure propagation, and discusses the safety of operation with failed fuel.

First, initial faults and their associated modes of failure propagation are identified with the use of fault-tree analysis and safety-assurance diagrams. Three initial fault categories are postulated that characterize the results of the fault-tree analysis: (1) random fuel-pin failure, (2) debris in the primary system, and (3) an overpower in a fuel pin. The associated failure-propagation modes potentially resulting from these initial fault categories are characterized by the following general failure-propagation modes: (1) fission-gas release, (2) local blockages in the fuel-pin bundle, and (3) local molten-fuel-coolant thermal interactions. Second, reported reactor-operating experience is reviewed and summarized in order to assess the consequences of these initial faults and potential failure-propagation modes for typical reactor-operating conditions. Third, the results of parametric analyses are reported in order to demonstrate that propagation of fuel-pin failure is very unlikely for a wide range of conditions. Finally, reported information on continued reactor operation with "failed fuel" is reviewed, with particular emphasis on the potential release of fission products from the fuel into the coolant, as well as a consideration of the sodium-fuel chemical reaction, frequently referred to as sodium logging.

II. INITIAL FAULTS AND THEIR CONSEQUENCES

The high core power density, the difficulty of detecting small local fuel-subassembly faults, the high temperature at the fuel centerline, and the knowledge that individual fuel pins have failed in certain reactors (including fast reactors such as Fermi, EBR-I, EBR-II, DFR, Rapsodie, and BR-5) have led to the conclusion that the consequences of local fuel-pin failure in an operating LMFBR should be considered with respect to design, operation, and safety. Fuel-pin failures may result from random effects within the normal operating envelope.

A fuel-pin failure will release radioactive fission gases stored in the fuel-pin plenum. These fission gases will be transported to the cover gas in the reactor plenum, where instrumentation will monitor increases in the cover-gas radioactivity; appropriate action will be taken if this activity becomes excessive. However, since the reactor cover gas can be continuously purified by removing radioactive substances, accidental release of radioactive fission gas from a few fuel pins is within the reactor-design envelope. The primary concern is that some initial fault may result, directly or indirectly, in rapid propagation of an initial failure to neighboring fuel pins, thus causing excessive damage to the subassembly or reactor core. Therefore, it is of particular interest to ascertain probable modes of fuel failure, their potential for promoting failure propagation, and the safety of long-term operation with failed fuel. However, there has been no evidence of failure propagation in operating LMFBR's either in this country or abroad. Reactor-operating experience is reviewed and summarized in the following section to provide support for this conclusion and to provide assurance that all local accidents have been considered in both plant design and safeguards analysis.

A fault-tree diagram is a graphic method of tracing and analyzing the potential of an individual failure or combination of failures to produce some specific undesirable event. (Fault trees² that represent various failure propagation chains for a typical LMFBR are presented in Figs. D.3-D.5 of Appendix D.) One of the principal advantages of fault-tree analysis for studies of system safety and operation is that the analyst is forced to concentrate attention upon the fault itself; hence, there is a decreased tendency to overlook certain faults just because the system is designed safely. This method of analysis takes the viewpoint that if anything can go wrong, it will; then the consequences of that fault are developed.

Figure 1 identifies the accident categories and failure-propagation modes considered in this report. The remainder of this report reviews and summarizes reactor-operating experience, with particular emphasis upon the accident sources and failure-propagation modes identified above. Then representative cases for each accident condition are considered. The potential consequences of each case are investigated, and analyses for each case are presented in order to substantiate the lines of defense contained in the safety-assurance diagrams.⁸²

Fig. 1. Potential Initial Faults and Failure-propagation Modes

III. REACTOR-OPERATING EXPERIENCE

Although a failure propagation has never been observed in an operating fast reactor, it is necessary to show that the potential consequences are acceptable. Therefore, it must be demonstrated that fuel-failure propagation cannot occur with "reasonable" probability. Because little is known about reactor operation with failed fuel or about the potential for fuel-failure propagation, local initiating events are postulated to occur and the consequences of these accidents are carefully analyzed. To maintain the proper perspective of the analyses presented in the following sections, however, note that as of June 1974, about 22,000 UO_2 , PuO_2 , and mixed-oxide (U, Pu) O_2 fuel pins have been irradiated in the world's fast reactors. Of these, about 1% have failed (see Table I). Table II contains a more detailed characterization of failed oxide pins among the fast-reactor irradiations. More recent experience with "production" FFTF-type pins in EBR-II (more than 450 fuel pins irradiated) has resulted in one failure at 6.3 at. % burnup (PNL-10).

The first failure in a DFR multipin subassembly occurred during the startup of Run 61/3 on August 17, 1968, when the Mark IIA failed. Subsequent examination of this subassembly revealed that 20 rods had observable defects, 3 others had lost their fission gas without any detectable hole or crack, and the remaining 54 were still in satisfactory condition.³ Carelli et al.⁴ have found, as the British had found, that the cause of the failure was due to local overheating of the cladding, resulting from entrained gas bubbles in the coolant.

TABLE I. Brief Tabulation of Oxide-fuel-pin Failures
Classified by Sponsoring Country^a

Country	No. of Oxide Rods Irradiated	No. of Rods Failed
USSR	~6189	105 ± 50
UK	~800	~72
USA	~3231	44
France	~11505	23
Total	~21725	~244

^aMore recent information released at IAEA Symposium on Fuel and Fuel Elements for Fast Reactors, July 2-6, 1973, at the Palais des Congres in Brussels indicates that (1) 1500 fuel pins have gone beyond 10% burnup; (2) of the more than 70,000 oxide pins now irradiated in fast reactors, fewer than 300 have failed; (3) of the 250 subassemblies (~9200 pins) irradiated in BOR-60, some 9 or 10 subassemblies have had failed pins--one with about 25-30 pins in the one subassembly.

TABLE II. Characterization of Failed Oxide Rods among World's Fast-reactor Irradiations

Sponsoring Country	Reactor	Experiment No.	Identification	No. of Failed Rods	Type of Rig	Date Inserted	Date Removed	Fuel	Fuel Form	Oxygen-to-Metal Ratio	Rod O.D., in.	Fuel Density (Smeared, % TD)	Fuel-Cladding Gap (Diameter), mils	Cladding Material	Peak Linear Power Rating W/cm	Peak Linear Power Rating KW/t	Peak Burnup MWd/kg	Peak Burnup at, %
USA	EBR-II	XG05	GE F20	1	Capsule	9/65	3/68	(U-20Pu)O ₂	Solid pellet	2.03	0.25	93.4	1.1-5.9	S347	16.1		7.17	
		XG06	GE F2F	1	Capsule	9/65	2/67	(U-20Pu)O ₂	Solid pellet	1.98	0.25	94.8	0.4-5.6	Incoloy-800	15		5.33	
		XG05/XG06	GE F2D	1	Capsule	9/65	5/70	(U-20Pu)O ₂	Solid pellet	2.00	0.2495	94.4	1.8-4.1	S347	16.5		12.8	
		X010	GE F0M	1	Capsule	3/66	4/69	(U-20Pu)O ₂	Solid pellet	1.98	0.252	95.2	1.6-4.2	Incoloy-800	12.0		6.39	
		X011	GE F4F	1	Capsule	5/66	6/67	(U-30Pu)O ₂	Solid pellet	2.00	0.25	86.3	2.1-4.4	Incoloy-800	16.7	34		
		X011	GE F4D	1	Capsule	5/66	6/67	(U-20Pu)O ₂	Solid pellet	2.00	0.25	86.8	1.8-4.5	Incoloy-800	16.6	34		
		X027	GE 11-33	1	Capsule	11/67	5/70	(U-25Pu)O ₂	Solid pellet	1.975	0.25	92.7	2.6-4.0	S304	14-15		8.27	
		X011	ANL HOV 4	1	Capsule	5/66	6/67	(U-20Pu)O ₂	Vipak	2.00	0.295	80.0	Not Appl.	Hast-X	22.4		3.6	
		X011	ANL HOV 10	1	Capsule	5/66	6/67	(U-20Pu)O ₂	Vipak	2.00	0.295	84.8	Not Appl.	Hast-X	23.5		3.5	
		X015	NUMEC B11	1	Capsule	11/66	4/69	(U-20Pu)O ₂	Solid pellet	1.995	0.25	88	2.1	S316L	13		6.29	
		XG04G	GE F0	2	Capsule	7/65	8/72	(U17.5Pu)O ₂	Spill	2.00	0.230	92.7-92.9	1.5-3.5	S347	5.8-6.8		10.8	
		X151	GE F3B	4	Capsule	11/67	1/73	(U22.1Pu)O ₂	Spill(3) Apell(1)	1.97	0.251	86.4-92.6	3.2-3.8	S304(3) S316(1)	3.2-3.4		12.5	
		X151	GE F5	1	Capsule	5/69	1/73	(U22.2Pu)O ₂	Vipak	1.96	0.251	84.3		S316	4.1		9.6	
		X081	GE F8B	3	Capsule	4/67	10/72	(U22.1Pu)O ₂	Vipak(1) Spill(2)	1.97-2.00	0.251	84.0-86.9	4.1-3.9	S316	8.4-9.0		13.1	
		X088	WARD-1	5	Capsule	5/70	1/73	(U22.0Pu)O ₂	Spill	1.99	0.225-0.253	89.2-90.0	6.0-2.0	S316	6.3-7.0		5.8	
		X076	WARD-2	1	Capsule	4/70	1/73	(U22.0Pu)O ₂	Spill	1.99	0.220	89.3	2.0	S316	5.5		8.3	
		X142	PNL-2	1	Capsule	12/67	9/72	(U22.2Pu)O ₂	Spill	1.98	0.238	88.0	6.0	S304	4.1		8.0	
		X113	NUMEC B	3	Capsule	11/66	9/72	(U17.9Pu)O ₂	Spill(2) Dpell(1)	1.95-1.99	0.252	87.0-88.6	2.0	S316L	10.9-11.3		13.0	
		X027	GE H-33	1	Capsule	11/67	5/70	(U-25Pu)O ₂	Solid pellet	1.98	0.250	92	2.0	S304	15.4		9.2	
		X148	L8	1	Capsule	11/66	4/72	(U-20Pu)O ₂	Solid pellet	1.98	0.252	82	2.0	S316	~14		~13	
		X011	HOV 15	1	Capsule	9/67		(U-20Pu)O ₂	Vipak	2.00	0.290	80	0.0	Hast-X	21.4		3.5	
		X106	ANL O1B	1	Uncapsulated	8/68	12/71	(U-20Pu)O ₂	Spill	2.00	0.290	80.3	4.5 (avg)	S304L	15		7.5	
		X084	NUMEC D5	1	Uncapsulated	11/66	3/73	(U-20Pu)O ₂	Spill	1.98	0.252	80.7	3.0	S316L	14.8		17	
		X121	GE F10A	2	Capsule			(U-21.7Pu)O ₂	Solid pellet	1.96	0.250	88.0	4.2	S316 Annl.	15.5		5.2	
		X122	GE F10B	1	Capsule			(U-25.1Pu)O ₂	Solid pellet	2.00	0.251	91.7	4.6	S316 20%ow	15.0		5.2	
		X122	GE F10B	2	Capsule			(U-24.5Pu)O ₂	Solid pellet	1.97-2.00	0.250	91.1-90.7	4.7-5.3	S321 Annl.	15.0		5.2	
		X191	NUMEC-E	1	Uncapsulated			(U-22.0Pu)O ₂	Solid pellet	1.97	0.230	86.9	5.1	S316 20%ow	12.2		4.3	
		X193	HEDL-PNL-10	1				(U-22.2Pu)O ₂	Dished pellet	1.98	0.230	85.0	7.5	S316 20%ow	8.6		6.3	
		X186	HEDL-P-12AA	1	Uncapsulated			(U-22.0Pu)O ₂	Dished pellet	1.96	0.230	86.6	5.1	S316 30%ow	11.1		3.6	
		X114	HEDL-PNL-5A	1	Uncapsulated			(U-25.0Pu)O ₂	Solid pellet	1.96	0.251	89.0	5.6	S304 Annl.	13.7		13.1	
France Many USSR UK	Rapsodie DFR BR-5 DFR	Driver	10440013	1	37-rod S/A	1/67	5/70	(U-25.9Pu)O ₂	Solid pellet	1.975	0.264	88.5	9.05	S316	206		4.85	
		DFR 307	AEG Trefoli	3	Trefoli	1969	~1/71	(U-25Pu)O ₂	Solid pellet		0.276	86	5.9	Incoloy-800 ^a	460		32	
		DFR 366	MK 11A	23	19-rod S/A	6/58	9/61-8/66	PuO ₂	Solid pellet	2.0	0.197	~86	1.2-5.9	S321		5.5	2-6.7	
		DFR 209	MK 11B	6	77-rod S/A	4/65	8/68	(U-15Pu)O ₂	Vipak & Annular		0.230	80	NA & 4	M316	14.0		~5.0	
		DFR 241	MK 11C	14	77-rod S/A	4/66	3/69	(U-15Pu)O ₂	Vipak		0.230	73	Not Appl.	Assorted ^b	13.3		7.2 (1)	
		DFR 50/30	V024	1	Capsule		6/70 ^c	(U-15Pu)O ₂	Vipak & Annular		0.230	80 & 77	NA & 4	FV548, M316	14.1		25.5	
		DFR 50/26	V021	1	Capsule	Early 1965		Mixed oxide	Vipak	1.98	0.230	79.9	Not Appl.	M316L	11.4		7.1	
		DFR 50/6	V035	1	Single rod	Early 1965		Mixed oxide	Vipak	2.01	0.230	82.3	Not Appl.	M316L			8.1	
		DFR 50/17	A153	1	Single rod	Early 1965		Mixed oxide	Annular pellet	2.02	0.230	72.9		M316L			9.1	
		184 & 115/6	Monitors	3	Single rod			Mixed oxide	Vipak & Annular		0.325			PE16, M316	≥18		2.7-6.3	
		DFR 204/11		1	Single rod			Mixed oxide			0.230						9.3	
		DFR 255/3		2	Trefoli			Mixed oxide			0.230						~8	
				1	Trefoli			Mixed oxide			0.230						~5	
				2	Single pin			Mixed oxide			0.230						7.8-10.2	
				1	Single rod			Mixed oxide			0.230			M316			9.2	
		DFR 284/3 ^c		1	Trefoli			Mixed oxide			0.230						7.9	
		d		1-3	Trefoli	10/64	1/70	Mixed oxide	Vipak		0.230	77	Not Appl.	FV548	7.0		2.75	
				1	Trefoli			Mixed oxide			0.230							

^aLeary's references to the cladding material are self-conflicting.^bFV548, PE16, and M316 claddings were used in MK 11B; FV548 was used on Pin 1703 that failed.^cAn estimate.^dOne or more rods out of the following three: 1813, 1824, 1834.

(The DFR uses a "downflow" coolant system design, and this is postulated to be a contributing factor in these failures.) Although there were indications of hot-spot defects occurring on mutually facing surfaces of two adjacent pins, it was not believed that the defect in either pin was caused by fission-gas blanketing from the other, but that most likely both defects were caused by gas bubbles entrained in the coolant stream. The Mark IIA contained the largest number of failed pins (a total of 23) of any defected LMFBR subassembly. The British³ believe that it is encouraging "that the considerable number of closely grouped individual defects did not give rise to a cooperative gross meltout," even though the reactor continued operation at 60 MW for about 36 hr. In other cases, they believed that defected pins had been operated in the reactor for periods ranging from one day to $2\frac{1}{2}$ yr with no serious consequences, but these cases have not been documented.

In five cases where unencapsulated fuel-pin failures have occurred in EBR-II, they were characteristically pinhole failures, not visible to the eye without magnification. No massive failures have been reported in any fast-reactor oxide-fuel pin. Furthermore, no large cracks have ever been observed in the cladding gas-plenum region alone. All observed cracks or pinhole failures have occurred in the fueled region of the pins or at the interface between fueled and nonfueled regions. These observations are important in subsequent considerations of fission-gas release and in-core blockages. The unencapsulated fuel pins that have been irradiated in EBR-II to date have experienced no failures that have resulted in rapid failure propagation.⁶ This experience is summarized in Table III.

TABLE III. Experience with EBR-II Fuel-pin Irradiations

Maximum clad temperature (°F)	304 SS - Annealed															TOTAL All element density power ranges
	Power Density (kW/ft)															
	<81			81.1-84			84.1-87			87.1-90			>90			
	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	Peak Linear Power (kW/ft)	
<1050	0	2 (2.9-8.1)	14 [1] (3.0-7.3)	1 (6.1)	6 (2.7-9.4)	12 (2.5-7.1)	50 (0.8-6.8)	13 (2.7-9.0)	24 (4.7-12.0)	45 [1] (0.8-6.9)	28 (0.9-7.7)	18 (2.1-12.0)	2 (4.3-6.4)	2 (6.5-6.7)	7 (9.1-11.6)	224
1050-1200	0	4 (3.2-7.9)	3 (3.3-9.0)	0	0	3 (3.3-5.2)	0	0	5 (0.1-5.4)	0	0	3 (1.7-9.6)	0	0	2 [12] (8.1-10.7)	20
>1200	0	0	0	0	0	0	0	0	1 (4.9)	0	0	2 (1.7-5.0)	0	0	2 (5.0-5.1)	5

316 SS - Annealed																
<1050	5 (6.9-9.3)	11 (3.4-11.7)	3 (5.7-12.7)	6 (6.9-8.5)	31 [1] (3.4-14.6)	9 (3.1-13.9)	28 [1] (4.6-9.1)	34 [1] (3.4-11.5)	18 [1] (0.2-12.7)	67 (4.4-8.8)	71 [1] (3.4-14.7)	29 [3] (3.6-15.7)	4 (4.8-5.8)	2 (3.0-11.5)	3 (4.5-6.1)	321
1050-1200	0	3 (3.7-11.6)	2 (7.3)	0	10 [1] (3.7-8.4)	3 (8.3-9.0)	2 (5.6-7.0)	9 [1] (3.6-11.3)	8 (5.3-11.2)	4 [3] (4.6-7.2)	20 (7.0-10.0)	34 (1.8-11.1)	5 (4.8-5.5)	0	9 (3.8-5.8)	109
>1200	0	0	0	0	1 (1.7)	1 (3.1)	0	0	0	0	0	18 [1] (1.7-16.4)	0	0	1 (3.0)	21

316 SS - Cold Worked																
<1050	0	0	0	36 (4.4-5.3)	3 (5.5-5.6)	5 (.01)	43 (4.5-5.7)	110 (0.2-6.0)	7 (0-2.7)	39 [2] (3.2-5.8)	21 (2.4-6.2)	6 (0-6.3)	3 (5.1-5.9)	1 (3.7)	0	294
1050-1200	0	19 (0-2.9)	5 (.8-3.4)	23 (2.3-5.4)	24 (0-8.9)	28 (0-8.9)	5 (5.2-5.4)	164 (0-5.5)	39 (0-5.4)	2 [2] (5.6-7.3)	45 (0-6.2)	21 (0-3.2)	3 (4.8-7.3)	15 (0-7.1)	6 (0-8.6)	481
>1200	0	0	1 (1.8)	17 (2.4-2.5)	0	1 (2.9)	0	52 (1.2-4.3)	4 (2.7-5.1)	0	8 (2.2-2.4)	9 (1.8-5.2)	0	0	8 [1] (4.9-5.3)	100

321 SS - Annealed																
<1050	0	0	0	0	1 (8.4)	1 (9.6)	0	2 (7.5-8.3)	4 (5.0-9.3)	0	5 (7.7-9.1)	2 (9.3-10.1)	0	0	0	13
1050-1200	0	0	0	0	0	0	0	0	0	0	0	3 (5.6-9.5)	0	0	0	3
>1200	0	0	0	0	0	0	0	0	0	0	0	3 (1.7-1.8)	0	0	2 [2] (5.2-5.3)	3

Inc-800 - Annealed																
<1050	0	0	0	1 (6.5)	1 (12.0)	0	2 (6.6-11.1)	2 (0.2-4.8)	3 [2] (3.1-2.0)	2 (6.1-7.0)	1 (7.9)	0	0	0	3 [1] (0.2-6.0)	15
1050-1200	0	0	0	0	0	0	0	0	1 (8.9)	0	2 (8.1-8.7)	0	0	2 [1] (5.5-6.0)	4 [1] (0.2-5.5)	9
>1200	0	0	0	0	0	0	0	0	0	0	0	0	0	0	1 (4.8)	1

*Each block interpreted as follows: Number of pins
(BU range)

[] No. Failed

IV. ANALYSIS OF POTENTIAL MODES OF FAILURE PROPAGATION

This section presents the results of analyses for potential modes of failure propagation associated with the accident categories identified earlier in this report. The studies presented here cover a range of initial conditions relevant to current LMFBR designs, and the results are presented parametrically so that they can be applied to specific reactor conditions of interest.

A direct mode of failure propagation that may result from a random fuel-pin failure is the release of high-pressure fission-product gases stored in the fuel-pin plenum. In this case, analytical models based on the results of ex-reactor tests using an idealized pin-bundle geometry are applied to demonstrate that the thermal and mechanical consequences of fission-gas release are inconsequential, and that rapid propagation of fuel-pin failure is not expected to occur by this failure-propagation mode.

The most significant mode of failure propagation that may result from debris in the primary coolant system or material released from a failed fuel pin is the formation of localized blockages of the coolant subchannels in the pin bundle. Again, analytical models are developed for this case, supported by results of ex-reactor tests with blocked channels for coolant flow in idealized reactor geometries* and supported by ex-reactor tests with porous debris beds in order to perform parametric studies. These studies predict that neither localized coolant boiling, propagation of localized coolant boiling, subassembly-coolant expulsion, nor fuel-pin melting will occur by this mode of failure propagation in idealized reactor geometries.

Finally, operation of a fuel pin at overpower is considered. Reasons based upon experiment and analysis are presented which indicate that an early failure of such a fuel pin is not expected for nominal overpower situations. Nevertheless, it is postulated that a fuel-pin failure eventually occurs and that some of the molten fuel is instantaneously mixed with some of the coolant in adjacent subchannels. Analytical models are used to predict the most severe pressure pulse generated and the voiding pattern within the subassembly as thermal energy is dissipated from the released molten fuel. Possible coolant dryout of adjacent fuel pins, meltthrough of adjacent fuel pins, and distribution of the resulting fuel debris are briefly considered. It is concluded that pressure pulses higher than expected will not cause failure of adjacent fuel pins, and that normal coolant flow will be quickly reestablished before a meltdown of the subassembly. This analysis provides a conservative evaluation of the consequences of this accident that releases fuel from one pin. Additional analyses, experiments, and evaluation of experiments are continuing in order to demonstrate the margin of safety that exists in this analysis.

*An idealized reactor geometry is a normal reactor geometry with uniform power, normal coolant flow, and normal coolant-flow-subchannel dimensions, but without a detail consideration of the wire-wrap spacers.

A. Fission-gas Release

Release of fission gas from a cladding breach due to a random fuel-pin failure has been identified as a potential mode of failure propagation.^{1,5,7} This gas, which may be present at high temperatures and pressures in the fuel pin at end-of-life conditions, is postulated to flow around or through the fuel-pellet stack^{8,9} and out of the cladding breach and produce adverse thermal-hydraulic effects that overheat adjacent pins. Sufficient damage may occur to cause a breach in the cladding of adjacent pins; i.e., fuel-pin-failure propagation may occur. This potential mode of fuel-failure propagation is discussed in detail by van Erp et al.^{10,11,15} The following discussion is based primarily upon this work.

The fission-gas inventory at end-of-life high-burnup conditions (burnup $\approx 10^5$ MWd/T) for a fuel pin of a liquid-metal-cooled fast breeder reactor (LMFBR) is about 2.5 g or 450 cc (STP). This fission gas consists predominantly of xenon, which is stored in the fission-gas plenum as well as within the central fuel void and the fuel matrix. At high-burnup conditions, the gas may attain a maximum pressure of 55-69 bars (800-1000 psia) at end of life, depending on the power history of the fuel and the gas-plenum size. Current FFTF core conditions (600°F inlet temperature, 80,000 MWd/T) will give a maximum plenum pressure of ~550 psia at end of life. At rated conditions (792°F inlet temperature, 40,000 MWd/T), the maximum pressure is ~330 psia.

Release of fission gas in an LMFBR subassembly due to a cladding breach in one or more fuel pins can potentially result in fuel-failure propagation through two distinct mechanisms: (a) temperature transients in the cladding of adjacent fuel pins exceeding the threshold temperature for failure at the operating conditions, and (b) transient mechanical loads on adjacent fuel pins due to pressure pulses, causing deflection of the pins, which could result in brittle rupture of the cladding for high-burnup fuel or buckling of the fission-gas plenum.

Even though gas-release rates under actual conditions are limited by the internal resistance due to the presence of the fuel pellets,^{8,9,12} as shown in Fig. 2, the full range of gas-release rates that have the potential for failure propagation have been investigated. Such an approach gives results that can be used no matter where the failure occurs (e.g., near the gas plenum, where the internal resistance is minimum, or removed from the gas plenum, where the internal resistance is higher).

1. Classification of Potential Mechanisms for Fuel-failure Propagation

The various potential mechanisms for fuel-failure propagation due to fission-gas-release rates¹⁵ may be classified as shown in Fig. 3. A simple model corresponding to the different fission-gas release rates is shown in Fig. 4.

NOMINAL HEAT RATING ~ 12 KW/FT THROUGHOUT LIFE

Fig. 2. Structure Evolution in an Oxide-fuel Pin

Fig. 3. Potential Mechanisms for Pin-to-Pin Failure Propagation due to Fission-gas Release in LMFBR Fuel Subassemblies (from Ref. 15)

Fig. 4. Model for Various Release Rates

a. Temperature Transients in the Cladding of Adjacent Fuel Pins. This potential mechanism of failure propagation can follow different modes for different sizes of cladding breach and different gas-release rates as shown in Fig. 4. For easy reference, the different modes can be qualitatively subdivided as follows.

(1) Slow and Moderate Fission-gas Release. For this release mode, the major part of the fission-gas inventory is released in time periods long enough to allow the local cladding temperature to exceed its threshold value for failure. This mode of fission-gas release can cause local thermal transients of the cladding of adjacent fuel pins in five different ways:

(a) Gas-jet impingement, resulting in gas blanketing and possibly dryout at, or in the immediate vicinity of, the area of impingement.

(b) Flow starvation due to increased (two-phase) frictional pressure losses affecting one or more coolant subchannels.

(c) Gas blanketing and possibly dryout immediately downstream of the point of release, where the gas has not yet been radially dispersed.

(d) Flow stagnation in one or more coolant subchannels, immediately upstream of the point of gas release.

(e) Entrapment of gas bubbles in wakes of flow obstructions, such as partial or complete subchannel blockages or grid spacers.

(2) Fast Fission-gas Release. For this release mode, the major part of the fission-gas inventory is released during very short time periods. The potential failure mode due to entrapment of gas bubbles in wakes of flow obstructions could also occur for fast gas release, though it may be less probable than for slow gas release. However, the primary concern is that the fast release of fission gas could result in severe flow transients, giving rise to the following potential modes for fuel-failure propagation: (1) transient reversal of coolant flow, resulting in gas blanketing of fuel pins for time periods longer than the gas-release time; and (2) dynamic mechanical loads on the fuel pins and at the point of wire-wrap contact.

Two mechanisms exist for heating the cladding of an adjacent fuel pin following the breach of one or more fuel pins: (a) decrease of the heat-transfer coefficient between the cladding and the coolant, e.g., by (local) gas blanketing; and (b) increase of the coolant temperature, e.g., by (local) flow reductions caused by two-component, two-phase flow effects.

An important condition that must be fulfilled before failure propagation can occur is that the perturbation caused by the fission-gas release lasts long enough to allow the cladding of the adjacent fuel pin(s) to be heated to the threshold temperature for failure. For some gas-release

modes, the perturbation time is approximately equal to the gas-release time; however, for fast gas release, which may induce a severe transient reversal of the coolant flow in the affected subassembly, the perturbation time may be longer than the gas-release time.

The minimum perturbation time before failure propagation becomes possible is determined by the transient thermal response of the fuel pin (as determined by heat capacity and conductivity of the fuel, heat conductance of the gap between fuel and cladding, etc.), the transient mechanical properties of the cladding as a function of temperature, and the mechanical interactions between the fuel column and the cladding. The threshold temperature for fuel-pin failure for short-term transients at end-of-life conditions is often assumed to be between 850 and 900°C (1560 and 1650°F). However, transient mechanical tests performed by Barnaby et al.¹³ determined that the rupture life for solution-treated and cold-worked M316 stainless steel, irradiated over a range of temperatures to fluences of up to 1.74×10^{22} n/cm², was typically 2 min for tubes at 1000°C (1830°F) and 1000-psia internal pressure. Similar transient tests were performed at HEDL by Fish et al.,¹⁴ who internally pressurized sections of prototypic FTR cladding and heated them rapidly to simulate various transient events in a reactor. Both unirradiated and irradiated specimens were heated at ramp rates of 10 and 200°F/sec until straining or failure occurred. After heating, diameter changes were measured to estimate transient flow stress and ductility. Internal pressures to 13,500 psi were used in this work. An initial holding temperature of 700°F was maintained for a short period before initiating the temperature ramp. Fuel-pin sections with the fuel removed provided irradiated specimens with fluences ranging to 2.75×10^{22} n/cm² ($E_n > 0.1$ MeV).

Cladding failure temperatures were found to increase with decreasing internal pressure. However, strain to failure increased with temperature to about 1950°F, then decreased steadily with increasing temperature as the melting point was approached. For unirradiated cladding, the peak stable strains reached ~8.5% for the 10°F/sec ramp and ~4% for the 200°F/sec ramp. Irradiation resulted in reduction in peak stable strain by a factor of four. Peak stable diametral strain for irradiated material at 10°F/sec was ~2% and at 200°F/sec was ~1%.

These results indicate that the cladding can withstand considerably higher temperatures for short periods of time than is usually assumed. This demonstrates the margin of safety in the analyses that follow. Furthermore, mean rupture ductilities were only $2\frac{1}{2}\%$, and failures in transient tests were predominantly of the pinhole type, which gave depressurization times of 15-20 sec for hollow tubes at 1000°C (1830°F) and 15-45 min for unirradiated pins at 925°C (1700°F). Hence, on the basis of transient mechanical tests with M316 stainless steel, Barnaby¹³ concluded that failure during a transient should not lead to any further problem from gas blanketing.

because the fuel pin is not likely to fail, and even if it does, the cladding failure characteristics would not allow rapid depressurization. Furthermore,

Fig. 5. Mean Cladding Temperatures of LMFBR Fuel Pins vs Time Subsequent to Complete Thermal Insulation at Nominal Power (from Ref. 15)

when the transient thermal response of the fuel pins is considered, Fig. 5 (from Ref. 15) can be used to provide mean values of the cladding temperature as a function of time for a typical LMFBR power plant at full power [maximum linear power rating, 15.8 kW/ft; coolant temperatures, 430°C (800°F) and 615°C (1140°F), respectively, at inlet and outlet] for the case of complete thermal insulation over a sector angle of 360° of the pin circumference for the entire length of the pin. These results are for a higher power rating than anticipated in FFTF; hence, the minimum perturbation time would be even longer for FFTF

conditions. As an example of the high-power case, a minimum perturbation time of ~0.25-0.4 sec corresponding to the threshold temperature 850 to 900°C is required in the upper half of the core before failure propagation even becomes possible. Fission-gas release modes resulting in perturbation times less than minimum perturbation times discussed above need not be considered.

b. Transient Mechanical Loads on Adjacent Fuel Pins due to Pressure Pulses. This potential mechanism for failure propagation is most probable for fast fission-gas release and for end-of-life fuel, when the ductility of the cladding is reduced. This potential mode of failure propagation will be briefly discussed later and is the subject of continuing analyses and testing.

2. Analysis of Potential Modes of Fuel-failure Propagation: Thermal Effects

Various potential modes of fuel-failure propagation will now be examined, and results from pertinent experiments will be presented.

a. Gas-jet Impingement. A jet of fission gas impinging on a fuel pin can potentially cause cladding failure by local gas blanketing. For gas-jet impingement to lead to cladding failure, at least two conditions need to be fulfilled:

- (1) The jet must be able to reach the opposite fuel pin and not be deflected excessively by the coolant stream through which it has to penetrate.

(2) The jet should persist for a long enough time to exceed the minimum perturbation time.

The deflection of a gas jet by a transverse flow can be derived from the following empirical relationship due to Ivanov:^{16,17}

$$\frac{z}{d_0} = \left(\frac{\rho_c u_c^2}{\rho u^2} \right)^{1.3} \left(\frac{x}{d_0} \right)^3, \quad (1)$$

where x and z denote the horizontal and vertical coordinates, respectively, d_0 denotes the diameter of the breach, u_c and u denote velocities of the coolant and of the gas in the breach, respectively, and ρ_c and ρ denote densities of the coolant and of the gas at the throat, respectively. For large deflections of the gas jet, the coolant will not be displaced from the pin and, hence, will leave a liquid film in the impingement area.

From Eq. 1, it is found that, at full coolant velocity (~ 8.5 m/sec or 27 ft/sec) and neglecting the internal impedance for gas flow, the angle between the direction of the impinging jet and the horizontal is $>45^\circ$ for circular cladding breaches with diameters <0.25 mm and for fission-gas pressures that are <55 bar (abs).

Furthermore, if the internal flow impedance were accounted for, then the angle of impingement would be even larger for these breach sizes, since the angle is approximately inversely proportional to the pressure. It may thus be concluded that, under the foregoing assumptions of full coolant velocity, small breach sizes do not constitute a problem with respect to gas-jet impingement.

The second limiting condition is that the sonic gas jet should persist for a sufficiently long time, since the deflection becomes large shortly after the jet becomes subsonic, resulting in only small temperature rises of the cladding of the opposite pin. A further limiting consideration concerning the potential for failure propagation was suggested by Judd,¹⁸ who pointed out that fuel-failure propagation by gas-jet impingement, if at all possible, can be expected to be self-limiting. The jets would preferentially cause failure of adjacent pins in such a way that the gas coming from subsequently failed pins would be directed mainly toward the pins that have already failed.

Figure 6 is a schematic representation of a gas jet impinging on a neighboring fuel pin. Note that the impingement area can be subdivided into an impingement (spray-cooled) region and a region of the wall jets (film-cooled region).

Hoglund et al.⁷ performed gas-jet-impingement experiments in a single-channel heated test section with water as the coolant. It was

Fig. 6. Schematic Cross Section of Submerged Impinging Gas Jet (from Ref. 11). ANL Neg. No. 900-2934 Rev. 2.

concluded that spray formation is an important feature of fission-gas-jet impingement in LMFBR fuel subassemblies. The entrainment of liquid droplets at the gas-liquid interface of the jet was also confirmed in an experimental study by Bell et al.,¹⁹ in which radial profiles of the void fraction in the gas jet were measured at various axial locations. According to Bell et al., the central region of the jet with a void fraction of 100% may extend to the impingement wall for jet lengths comparable to the distances existing between adjacent fuel pins in LMFBR subassemblies and for a relatively wide range of gas-plenum pressures. If this were the case, then the cooling in the central region of the impingement area, after dryout occurred, would be solely performed by the gas

jet. Under these conditions, the gas temperature may be expected to have a significant effect. If the fission gas were to be heated on its way from the gas plenum to the cladding breach, and if it could be ejected at high temperatures, then a localized hot spot may be formed on an adjacent fuel pin. This is not expected to cause failure of the adjacent fuel pin, however, for the following reasons: (1) The flow impedance of the fission gas through the fuel-pellet stack is so great that the gas flow rate is significantly reduced; (2) the heat capacity of the fission-gas jet is so small that the maximum temperature rise of the cladding on the adjacent fuel pin for the time durations of interest is negligible; and (3) three-dimensional heat conduction from a localized cladding hot spot is sufficient to prevent cladding failure.

Kennedy et al.²⁰ used the SLADE finite-element computer code to analyze a hot spot on a fuel-pin cladding caused by fission-gas impingement. The conservative temperature profiles used for computation were chosen on the basis of an experimental study.²¹ The results showed that the calculated maximum stresses remained well below the yield strength of the cladding material.

On the basis of analyses and assumptions presented above, it is concluded that rapid failure propagation caused by the thermal effects of gas-jet impingement cannot occur in the FFTF.

b. Flow Starvation. Due to increased (two-phase) frictional pressure losses and dilution of the coolant with the subsequent loss of heat-transfer effectiveness, the release of fission gas into an LMFBR subassembly may cause flow starvation in a limited number of coolant subchannels. All other things being equal, the degree of flow starvation will be more serious for small rather than for large values of the radial dispersion rate of the gas

inside the fuel subassembly. Due to the decrease of the mass flow rate of coolant in the affected subchannels, the local coolant temperatures may increase, resulting in higher cladding temperatures, which could exceed the failure threshold. If the gas does not disperse radially, i.e., remains confined to a few subchannels, and if coolant crossflow does not take place between the subchannels of the subassembly, then the worst conditions (highest coolant and cladding temperatures) would occur at the exit of the affected subchannels.

Analytical treatment of the problem of flow starvation due to fission-gas release has been hampered mainly by the lack of data on both the radial dispersion rate of the gas and the degree of coolant crossflow.²² Experiments were performed at ANL with 19-pin (heated and unheated) test sections with wire-wrap spacers.²³ In one experiment, high-speed motion pictures were taken which showed that the gas disperses radially relatively fast. Some gas bubbles did spread to the periphery of the cross section of the 19-pin array over a distance of 8-14 in. downstream of the point of gas release. In this experiment, a test section with 18 Lucite pins and one central stainless steel pin, from which the gas was released, was used.

In subsequent experiments, a 19-pin heated test section was used. Thermocouples were mounted inside the test section at various axial locations, allowing measurement of both coolant temperatures (with time constant of ~2 msec) and pin-wall temperatures in a number of subchannels. Gas was injected through a hole in the central pin. Test runs were made for different hole sizes at various pressure differences across the hole and at various coolant flow velocities. It was found that the rise of the local coolant temperatures was highest for large breach sizes in the cladding at relatively low-pressure differences across the hole. For these latter conditions, a relatively high release rate was combined with a low velocity in the gas jet at the breach, so that the initial radial gas dispersion caused by the jet was small. The gas was then initially confined to a limited number of coolant subchannels, causing these subchannels to be more affected than when the jet dispersed the gas immediately over a larger number of subchannels. However, even for these worst conditions, the maximum increase of coolant temperature,¹⁵ extrapolated to full LMFBR power, was less than 100°C under steady-state conditions at gas-release rates up to ~1000 cc (STP)/sec.

On the basis of the foregoing experimental studies and related analyses performed at ANL, it is concluded that flow starvation due to fission-gas release in an FFTF fuel subassembly cannot cause fuel-failure propagation. To demonstrate the margin of safety that exists for typical operating conditions for an LMFBR reactor, a steady-state analysis of the effects of dispersed gas in the coolant is discussed in Appendix A; the results are summarized in Fig. 7. It is indeed evident that large amounts of dispersed gas must be present to result in cladding failure.

Fig. 7. Effect of Dispersed Gas in Sodium

during full-power operation these cracks will not extend into the central void region of the fuel. If the failure occurs in the fuel-pin plenum, the result is usually a pinhole or narrow crack in the cladding material, and the gas release is relatively slow. This is verified by current operating experience²⁴ and by biaxial creep experiments²⁵ performed at Atomics International with an unirradiated rod bundle in a flowing sodium environment similar to a typical LMFBF.

Nevertheless, if a fast high-pressure gas release from the fission-gas plenum is postulated, it is hypothesized that (1) mechanical damage may be produced and that (2) such large gas releases may reverse the coolant flow momentarily and thermally blanket the fuel assembly. Both effects are analyzed here. The potential mechanical-damage effects are considered later; the potential thermal-hydraulic effects are discussed in this section.

Early work regarding fast gas release was performed by Hori and Friedland²⁶ and by Kondo et al.²⁷ Subsequently, experiments regarding coolant flow transients induced by rapid gas release in a 19-pin test section were performed at ANL.^{28,29} On the basis of these experiments, a mathematical model²⁸⁻³⁰ was developed that treats the gas as a single "slug-type" bubble. Relatively good agreement was found between experimental and calculated values of the displacements of the upper and lower gas-liquid interfaces. The model permits extrapolation to full-size subassemblies, provided the gas-injection rate is sufficiently high to justify treatment as a single "slug-type" bubble.

c. Fast Fission-gas Release.

In this case, it is assumed that gas at a high temperature and pressure in the fuel-pin fission-gas plenum is suddenly released into the fuel assembly. First, as noted earlier, a sudden large gas release is unlikely and has never been observed in an operating fast reactor. Nevertheless, if a cladding rupture is postulated, it may be in the core, the axial-reflector region of the pin, or in the fission-gas plenum. If the cladding rupture occurs in the core or axial-reflector region, the gas flow from the plenum to the breach is severely restricted due to flow impedance in the upper axial-reflector region of the fuel pin. In addition, the gas flow is further restricted in the core region, since it must pass through many narrow cracks in the fuel, as shown previously in Fig. 2. It is anticipated that

An example of the application of the model is given using an FFTF fuel subassembly (see Fig. 8). Simultaneous rupture in the upper part of the core region of 10 or more fuel pins was considered, neglecting the flow resistance inside the fuel pins as well as that due to the breach. This is a

Fig. 8. Schematic Representation of a Fuel Subassembly of the Fast Flux Test Facility (FFTF). All dimensions in inches. (From Ref. 31.) ANL Neg. No. 900-3085 Rev. 1.

very conservative assumption, since experiments at EBR-II have indicated that there is frequently a significant difference between the initial internal

Fig. 9. Lower and Upper Gas-Liquid Interface inside an FFTF Fuel Subassembly vs Time after Simultaneous Rupture of 10 or More Fuel Pins at the Outlet Region of the Core. Assuming Zero Gas-flow Resistance (from Ref. 32). ANL Neg. No. 900-1315.

pin pressure and the pressure at the breach location. Nevertheless, the gas-release rate was assumed to be solely limited by inertial effects due to acceleration of the coolant inside the fuel subassembly, and in the lower and upper coolant plenums. The results are given in Fig. 9, where the dashed and solid curves represent, respectively, the lower and upper gas-liquid interface as a function of time. It follows from Fig. 9 that simultaneous rupture of up to 30 fuel pins in the upper core region will not lead to a gas-blanketing time exceeding ~ 0.1 sec; however, this is not true if larger numbers fail. This gas-blanketing time is less than the minimum required time for cladding failure, which was previously shown to be between 0.25 and 0.4 sec.

Therefore, it is concluded that rapid gas release, even for simultaneous failure of up to 30 fuel pins, does not lead to fuel-failure propagation due to temperature transients in the cladding.

d. Conclusion on Thermal Effects. On the basis of the various experiments and analyses performed to date at ANL and elsewhere, it is concluded that thermal transients induced in the cladding of adjacent fuel pins by fission-gas release from a limited number (i.e., less than 30) of randomly failed fuel pins are unlikely to cause rapid pin-to-pin failure propagation in an FFTF fuel subassembly.

3. Analysis of Potential Modes of Fuel-failure Propagation: Mechanical Effects

This section contains the preliminary results of analyses performed in order to investigate the potential for propagation of fuel failure caused by a sudden rupture of a fuel pin. As noted earlier, a sudden large breach of the cladding in the fission-gas plenum region of a fuel pin has never been observed under reactor-operating conditions. When purposely defected tubes have been internally pressurized until failure occurred, a significant pressure loss was observed as the pressurized gas flowed out of the plenum region into the surrounding fluid. For example, in some EBR-II experiments the pressure outside of a purposely failed pin was significantly less than the

original internal pressure. Nevertheless, if it is postulated that the pressure pulse due to the rupture is large, then the resulting damage must be investigated. The pulse may severely deform neighboring pins and, as a consequence, the coolant flow may be severely restricted. The extent of this localized deformation to adjacent pins is investigated by a bending analysis. The problem is formulated on the basis of establishing a failure criterion for a thin cylindrical shell subjected to a side-on pressure pulse. Since there is a striking similarity between the collapse-hinge form of buckling formed by blast and that formed by a statically applied bending moment, it has been suggested³³ that bending of the cylinder generates a weakness which results in a collapse-hinge failure of the cylinder. The experiments by Schuman³⁴ demonstrated the same form of buckling for dynamically loaded cylinders.

The validity of the above-mentioned bending analysis is demonstrated by comparing it with experimental results compiled by Boers³⁵ and obtained from the (gunpowder) explosion of a source located at the center of an EBR-II subassembly immersed in water. In the experiment, the pressure pulse at the duct wall was measured as a function of time. Since the rise time was very short, the pulse was characterized by the equation $p = p_0 e^{-\alpha \tau}$, where $p_0 = 3150$ psi (maximum peak pressure) and $\alpha = 17,241.4 \text{ sec}^{-1}$. From observations after the test, the first two concentric rings of fuel pins surrounding the central line source showed evidence of collapse failure due to this loading.

Failure, as predicted from the above analysis, was found to agree with the experimental observations described above. The value of α was assumed to be independent of geometry and to depend solely on the characteristics of the source. The peak pressure p_0 was computed at the radius of each concentric ring of fuel pins from the central pin by assuming that, following the explosion, the pressure of the expanding spherical wave front is inversely proportional to the radius of the sphere at any instant. In particular, the fuel pin was assumed to be simply supported with L , the distance between points of contact of the helical wire wraps and an adjacent fuel pin, equal to 6 in.

Calculations were performed to study how the source characteristics are related to collapse-hinge failure, fuel-pin geometry, and material properties. Parameters used in this study were typical for fuel elements used in current LMFBR designs.

The results of a parametric study of the effect of variation of L and of p_0 on the generated maximum moment $M_{\max}$ in the cylinder are shown in Fig. 10, where a and t are the mean radius and thickness of the cylinder, respectively, T is the temperature, p is the internal pressure (assumed to be zero) in the pin, α is the pressure-decay parameter ($\alpha = 17,500 \text{ sec}^{-1}$ was chosen for computation, which corresponded to the value given by Boers), and M_{cr} is the collapse moment induced in the cylinder. This figure reveals that dramatic increases in moment exist for a support length less than about 4 in. Beyond this length, the curves level off for $p_0 \leq 1000$ psi and $L \leq 10$ in.

Fig. 10. The Effects of Support Length L and Peak Pressure p_0 on Moment M_{max}

case with $p_0 = 500$ psi (corresponding to an internal pressure of about 1000 psi of a failed pin) and $t = 0.010$ in. No failure was predicted for this case either

(A value of 1000 psi for p_0 corresponds to about 2000 psi of plenum pressure of a failed pin.) Since the effect of finite length on ovalization of the cylinder was neglected in the analysis, it is suspected that the results predicted from the analysis are invalid for support lengths $L \leq 4$ in. (as shown by dashed lines). For the typical fuel-pin geometries considered here, $L = 6$ in. A hypothetically high pressure of 1200 psi is also included in the figure to show that the maximum moment will approach very rapidly to the collapse moment for $L > 10$ in.

The example shown in Fig. 11 illustrates how the induced moment M varies for different fuel-pin radii and temperatures (or material properties). For FFTF geometry, and a hypothetical peak pressure of 1000 psi and cladding thickness of 0.014 in., no failure ($M_{max} < M_{cr}$) was predicted for 0.08 in. $\leq a \leq 0.24$ in. and $70^\circ\text{F} \leq T \leq 1500^\circ\text{F}$. Figure 12 shows a similar

Fig. 11
The Effects of Fuel-pin Radius and Temperature on Moment: $t = 0.014$ in.

Fig. 12. The Effects of Fuel-pin Radius and Temperature on Moment: $t = 0.010$ in.

for the same range of temperatures and mean radii. However, both figures indicate that the fuel pin is weaker in resisting external dynamic pressure loadings at higher temperatures. Additional calculations were made for the case of nonzero internal pressure. By increasing the value of P , we find that M_{CR} increases while M_{max} remains practically unchanged. This implies that the results shown in Figs. 10-12 are conservative by assuming $P = 0$.

In reality, current fuel-pin plenums generally sustain internal pressures between 330-550 psia toward the end of their lifetimes. The effect of an impulse caused by these pressures has been ascertained using the present analysis. For example, assume as a conservative upper limit, a high temperature of 1500°F and a pulse having a peak pressure of $p_0 = 500$ psi (corresponding to ~1000 psi plenum pressure of a failed pin) impinging upon a neighboring fuel pin. If the pressure decay parameter $\alpha \leq 4500$ sec⁻¹, then collapse may occur. However, the possibility of obtaining an α having this magnitude or less will be quite improbable in the event of a cladding rupture.¹⁰

The results of the analysis, therefore, indicate that no severe damage to the adjacent cladding of a fuel pin can be expected from the postulated rupture of a fuel-pin plenum, provided the pin is sustaining a maximum internal pressure of less than 1000 psia when it ruptures. Upon rupture, side-on pressures greater than about 450 psia are not expected in current reactor designs.

In conclusion, this preliminary analysis indicates that propagation of a fuel-pin failure is not expected to occur by rapid fission-gas release resulting from a random fuel-pin failure. Furthermore, this failure-propagation mode has never been observed in an operating reactor. In addition, this analysis indicates that there is a large margin of safety for a wide range of postulated initial accident conditions.

B. Local Blockages in Pin Bundles

Appropriate reactor design, construction, and operation can minimize the possibility of debris in the primary coolant system or in the fuel-pin sub-assembly. Nevertheless, it has been postulated that debris in the primary coolant system may lodge in the fuel-pin bundle and, consequently, starve the coolant flow in a localized region of the core or impede the normal heat transfer from the fuel pin to the coolant. This localized undercooling may result in

fuel-pin failure or the release of fuel debris, which may cause additional blockages within the pin bundle. Consequently, the initial fuel-pin failure may propagate to other fuel pins within the fuel subassembly by means of this propagation mode.

This section of the report considers the following blockage situations and determines the consequences by analysis of an idealized geometry: (1) an insulating blockage, which adheres to a local area of the fuel pin and impedes or prevents the normal heat transfer from the fuel pin to the coolant; (2) a porous blockage, plugging several contiguous coolant subchannels in the reactor core, which impedes normal coolant flow and increases the coolant temperature; (3) a planar blockage, plugging several contiguous coolant subchannels, which prevents normal coolant flow and causes a coolant temperature increase in the blockage wake region; and (4) a localized coolant-boiling condition resulting from severe blockage. The difference between items 2 and 3 is that some coolant flows through the porous blockage whereas no coolant flows through the planar blockage.

1. An Insulating Blockage

Local blockages have been postulated as a potential mechanism for fuel-failure propagation because of the possibility of causing pin failure. The type of blockage required to produce molten fuel, as one possible trigger for failure propagation, involves either a blockage material of low thermal conductivity, which essentially insulates the periphery of the fuel pin, or a heat-generating blockage, which is discussed later. The amount of molten fuel that could be produced is strongly dependent upon the extent of the blockage. No molten fuel is expected for small blockages.

To estimate three-dimensional heat-transfer effects within the blockage region, parametric studies have been performed for representative cases. In these, a portion of the cladding surface of the hottest rod was assumed to be coated with a layer of insulating material (such as graphitic carbon) of various thicknesses. In some cases, an insulating layer of infinite thickness was also considered. It is unlikely that any organic materials, such as oil or fabric which could chemically react with sodium to produce graphitic carbon, would be accidentally introduced into the primary coolant system. Even if this did occur, however, experience at SRE, FERMI, and other places indicates that such impurities will not form thickly deposited layers (>1 mil) on the hot fuel-cladding surface. In the analytical models used in this analysis, however, a thick layer of insulating material was assumed in order to provide an upper bound of the accident severity.

The results of these blockage studies, discussed in more detail in the following, are presented in Tables IV and V. These tables indicate that a sufficiently large, thick layer of insulating material is needed to cause cladding failure. Note also that, even though the cladding may fail, the centerline

fuel temperature is still several hundred degrees below its melting point unless more than 180° of the surface is insulated. This is clearly demonstrated in Table IV. Dry NaO, which covers a 60° segment of the rod surface (equivalent to one plugged coolant channel for the entire fuel length), will not result in the production of molten fuel at the rod centerline.

TABLE IV. Summary of Results for Insulating Blockages

Blockage Angle, degrees	Blockage Length/ Pin Diameter	Maximum Temperatures, °F	
		Cladding	Fuel
60	0.5	1600 (cladding failure point)	
120	∞	2600 (cladding m.p.)	
180	∞		5000 (fuel m.p.)
360	0.9	2600 (cladding m.p.)	
360	1.5		5000 (fuel m.p.)

TABLE V. Cladding Temperatures (°F) Resulting from Local Channel Plugging

Type of Plug	Number of Channels Plugged ^a		
	1	2	4-6
Steel	1100	1300	1650
Oxide-sodium mixture	1350	1830	2730
Dry NaO ($k = 0.3$)	1530	2750	4300
Fuel-sodium mixture ^b	1460	2350	3910
Dry fuel	2080	4280	8000

^aInitial unperturbed cladding temperature $\approx 1100^\circ\text{F}$.

^b73-27% by volume.

Results for different plugging materials are presented in Table V which demonstrate that blockages by steel or sodium oxide are less severe than the insulating blockages discussed above. Since the release of fuel debris into the flowing coolant, with the subsequent formation of additional nonporous blockages sufficient to cause pin failure, is required for this failure-propagation mode to occur, and because of the conservativeness of the present analysis, it is concluded that this failure-propagation mode is unlikely.

The studies that provided the above results will now be discussed in more detail. One of the earliest studies involved analysis of an early FFTF design consisting of 0.250-in.-dia fuel pins with 0.030-in.-dia spacer wires,

where the low pitch-to-diameter ratio of 1.12 gave rise to concern over in-core blockages.³⁶ For the hottest pin, operating at 15.1 kW/ft, two types of blockages at the core center were analyzed. In the first case, 60° of the fuel-pin periphery (corresponding to complete blockage of one coolant subchannel) was insulated over a very long length. This resulted in an increase in the maximum cladding temperature of 900°F and an increase in the maximum fuel temperature of 150°F. Since the maximum cladding temperature was over 1700°F, cladding failure could be expected, but there was no molten fuel.

The second case involved complete (360°) insulation of the cladding periphery for blockages of various lengths. It was found that cladding failure (assumed to occur at 1600°F) occurred at a blockage length of 0.4 times the pin diameter. Extrapolation of the results gave cladding melting and fuel melting at L/D's (blockage length/pin diameter) of 0.9 and 1.50, respectively.

Brook analyzed local blockages of one, two, four, and six coolant subchannels in the PFR reactor.³⁷ He assumed two types of non-heat-generating blockages (stagnant sodium and sodium-sodium oxide mixtures) and two types of heat-generating blockages (sodium-fuel mixtures and dry oxide fuel). The sodium-sodium oxide mixture corresponds most closely to an insulating blockage. Brook found that a one-subchannel blockage (60° coverage of the fuel-pin periphery) did not result in cladding failure (the maximum cladding temperature was about 1400°F, starting from an unperturbed base temperature of 1150°F), but that blockage of two (120° coverage) or more subchannels did cause cladding failure. Cladding and fuel melting were obtained for the three-subchannel blockage (180° coverage) and the six-subchannel blockage (360° coverage). Brook's results are in general agreement with the previous results when allowance is made for the finite thermal conductivity of the blockage material.

Gregory and Broad considered blockages in the PFR in the region of the honeycomb grid structure.³⁸ This work is not easily applicable to wire-wrapped fuel bundles, but is referenced for possible future interest.

A more extensive analysis of insulating blockages was performed by Bell.³⁹ He determined the maximum cladding and fuel temperatures for blockage angles of 60, 120, 180, and 360° for blockage length-to-pin diameter ratios (L/D) of 1, 2, and 4. The thickness of the insulating material (charcoal) was also varied (0.005 in., 0.010 in., and ∞). He found that cladding failure could occur for all angles if L/D is greater than 0.5. Cladding melting would occur for a 120° blockage if the insulation thickness was greater than about 0.006 in., or for a 180° blockage if the insulation thickness is very thin. For fuel melting to occur, the blockage angle must be greater than 120°, since an infinite thickness of insulation for this angle produced no molten fuel. For a blockage angle of 180°, molten fuel was obtained for insulation thicknesses greater than about 0.007 in. For a 360° blockage angle and an infinite insulation thickness, the L/D required to produce molten fuel was about 0.5.

One area neglected in all the analyses of blockages that produce molten fuel is the effect of molten-fuel relocation. The molten fuel, if produced at all, would tend to slump downward along the central void, if present, and solidify in the lower region of the fuel pin.⁴⁰ Even for a 360° blockage, the length of blockage required to exceed the capacity of the central void to solidify the molten fuel produced by the blockage may be unreasonably large.

2. A Porous Blockage

Analysis of local in-core blockages has revealed several significant factors that must be considered: (1) blockage porosity, (2) axial coherence of blockage in the subassembly, and (3) stability of blockage against disruptive gas release from the fuel pin. Each will now be discussed in relation to pin-to-pin failure propagation.

First, analysis indicates that even a small amount of coolant flow (less than 5% of nominal) through a small non-heat-generating porous blockage will prevent cladding failure within the blockage region. Second, for molten fuel to be produced, the blockage in many contiguous coolant flow channels must occur at the same axial location in the subassembly. Since the reactor is designed with wire-wrapped pins with about a 12-in. pitch, it is unlikely that blockages will occur in many contiguous coolant channels at the same axial location. Third, if any blockage does occur, the cladding will fail before any molten fuel is present. If the cladding does fail, this will result in the release of fission gases, which may dislodge the blockage.

Nevertheless, it was postulated in this analysis that limited amounts of fuel debris from failed pins may aggregate to form a heat-generating blockage within the fuel subassembly, and the consequences of such an accident situation are investigated.

A thermal-hydraulic model of this postulated mechanism has been developed to determine the heat-transfer performance of liquid sodium flowing through a porous bed with internal heat generation. Parametric studies were performed to determine the sensitivity of the results over a range of values of interest. Some of these results are presented here. Figure 13 shows the percent flow rate passing through a unit length of porous bed as

Fig. 13. Flow Passing through a Porous Bed (Porous-bed height = 1 cm) ANL Neg. No. 900-1595.

a function of the average effective particle diameter D_p for various bed porosities. The porous-bed thickness that results in coolant boiling is shown in Fig. 14.

Fig. 14. Maximum Porous-bed Thickness to Produce Coolant Boiling for Various Bed Porosities and Particle Sizes. ANL Neg. No. 900-1404.

This analysis is discussed in greater detail in Appendix B. In the analytical model developed, a flat, porous bed of fuel debris having thickness L and porosity ϵ is assumed to occupy all coolant flow subchannels within the fuel subassembly. Sodium coolant enters the bed at $X = 0$ at a constant temperature T_{co} and leaves the bed at $X = L$, where $\partial T_c^2 / \partial X^2 = 0$. Energy is generated within the porous bed at a uniform rate q''' and is transferred to the coolant by convection. The transient thermal-energy equa-

tions for such a system, taking into account the thermal conductivity of both the porous medium and coolant, may be written as

$$\rho_p C_p \frac{\partial T_p}{\partial t} = k_p \frac{\partial^2 T_p}{\partial X^2} - h_v(T_p - T_c) + q'''$$

and

$$\rho_c C_c \epsilon \frac{\partial T_c}{\partial t} = \epsilon k_c \frac{\partial^2 T_c}{\partial X^2} + h_v(T_p - T_c) - G \epsilon C_c \frac{\partial T_c}{\partial X},$$

where $T = T(X, t)$ is the temperature, and the subscripts p and c refer to the porous medium and coolant, respectively. It is assumed that heat flow is one-dimensional, the fuel and coolant thermophysical properties are constant, and both the volumetric heat-transfer coefficient h_v and the volumetric heat-generation rate q''' are constant.

The computer code PORPLUG was developed to solve the above questions, and to predict the temperatures of both the porous bed and the coolant at steady state and during a transient. Particle-size distributions obtained from TREAT tests and laboratory experiments (see Fig. 15) indicated that the fuel debris could consist of particle diameters between 100 and 1000 μ (with an average, effective particle diameter between 500 and 600 μ) and a maximum bed porosity ϵ between 0.35 and 0.45. Based upon these assumptions, it was concluded that at least 2-5 g of fuel per flow channel would be required to produce coolant boiling and that steady-state temperatures within the porous medium would be attained in a few seconds after blockage initiation. If this much fuel was released in the coolant, it would be eventually detected by the delayed-neutron monitors or as increased activity in the cover gas.

Experimental evidence indicates that porous beds formed by fuel debris in sodium may have bed porosities even greater than assumed here, i.e., $\epsilon \geq 0.5$. This would reduce the possibility of overheating. Furthermore, fuel particles less than 1000 μ may be swept out of the core without forming

Fig. 15. UO_2 Particle Sizes from Interactions with Sodium. In-pile tests S3 and E2 and laboratory tests. (From Ref. 41.) ANL Neg. No. 900-75-424.

The significant parameters identified are (1) effective thermal conductivity of the coolant, (2) size of the wake region, (3) turbulence in the wake region, and (4) blockage porosity. An analysis of a case in which completely stagnant sodium is assumed to exist between fuel rods has demonstrated that there is sufficient axial heat conduction in the sodium to prevent coolant boiling, provided that the axial length of the stagnant layer does not exceed about $L/D \sim 2$. This result was obtained by assuming no radial heat conduction in the sodium.

Different types of flow blockages and their associated wakes have been identified.⁵¹ It was concluded from analysis that the hydrodynamics can be significantly different, even though the blockage area is the same.

A mechanistic analysis of a local in-core blockage was also performed wherein the significant parameters mentioned above were included. At least two different problems may

a blockage. (See Sec. IV.C.4 for additional discussion of fuel sweepout.) Therefore, rapid failure propagation by the formation of a porous bed is unlikely.

3. Planar In-core Blockage

The consequences of a hypothetical planar blockage are analyzed although it is recognized that there is no readily identifiable mechanism to form a planar blockage within the fuel region. Analyses were performed to investigate the coolant-wake region downstream from a blockage, as shown in Fig. 16, to determine whether voiding and fuel damage would occur. Such blockages have also been studied experimentally within the FFM Facility at ORNL^{42,43} and elsewhere.⁴⁴⁻⁵⁰

Fig. 16. Model of Coolant-channel Blockage

arise from a consideration of partial flow blockages: (1) There may be a significant overall coolant flow reduction in the subassembly, and (2) there may be a significant increase in the coolant temperature in the downstream wake of the blockage, which may cause local boiling without an overall reduction of coolant flow. If there is an overall reduction of coolant flow in the subassembly, as in Case 1, it will be accompanied by a coolant temperature increase sufficiently large to be detected by outlet thermocouples. This case will not be considered further here, since its occurrence can be detected. On the other hand, it has been demonstrated by analysis⁵² that large flow blockages may exist undetected within a local region of the subassembly. Even though there is an insignificant overall flow reduction, there may be significant local flow reductions and, consequently, a coolant temperature increase in the downstream wake region of the blockage. Blockages large enough to be of concern but small enough to be undetected are considered here.

This analysis considers only a planar-type blockage extending radially within the subassembly cross section near the core midplane and normal to coolant flow. This blockage wake region is assumed to extend two to three blockage diameters downstream and to be independent of the coolant velocity. As a consequence of reverse flow, coolant mixing, and turbulence in the blockage wake region, it is assumed that coolant in this region is characterized by an average temperature. Energy is continually added to the coolant by fuel pins in the recirculation region, and is continually removed by an exchange of mass and energy through the interface between the recirculation region and the main subcooled coolant flow, according to the experimental relation proposed by Kirsch and Schleisiek.⁴⁵ Enhanced coolant mixing due to wire-wrap spacers is neglected.

Figure 17 shows typical results calculated for the maximum average coolant temperature increase in the downstream blockage wake region for an FFTF fuel subassembly as a function of the number of flow channels blocked (percent blockage = $100 \times \text{number of blocked flow channels} / 434$).

Fig. 17. Coolant-temperature Increase Downstream of a Blockage

This figure demonstrates that, even with conservative assumptions, more than 20% of the fuel-subassembly cross section must be blocked with a nonporous blockage before coolant boiling can result in the blockage wake region. For this size blockage, the cladding will exceed the failure threshold temperature of 1600°F.

Thermal-hydraulic models, such as COBRA,⁵³ which include the effects of wire-wrap mixing, were also used to investigate the thermal effects of partial blockage in the subassembly. The results predict that

both the wire wrap and turbulent mixing restore the flow and reduce the temperature rise in the downstream wake region; fuel pins in adjacent unblocked flow subchannels are not significantly affected.

Even if local coolant boiling in the wake region is assumed to occur for a severe blockage, we shall show in the following paragraphs that cladding dryout is unlikely. Therefore, it is concluded that rapid fuel-failure propagation by blockage mechanisms is unlikely. Overheating of the pins, however, may lead to shortened lifetime and pin failures during continued operation.

4. Local Coolant Boiling

The consequences of local coolant boiling within the fuel subassembly are considered here. In previous sections it was shown that local in-core blockages are not expected to occur in the fuel-pin bundle; nevertheless, if it is assumed that they do occur, then the mean coolant temperature is well below the saturation temperature unless the blockage is quite large; i.e., an appreciable flow cross section must be blocked. In this section, it is postulated that a large enough blockage does occur within the core, and this blockage produces localized coolant boiling. An idealized reactor geometry is assumed. This situation was analyzed by Fauske⁵⁴ and is summarized here.

Fauske postulated an idealized planar blockage, such as shown in Fig. 16, extending radially over 50% of the cross section of an FFTF subassembly at the midcore. Such a blockage would cause sufficient flow reduction (~5%) to be detectable by sensors at the subassembly outlet as shown in Fig. 18. Although the mean temperature in the recirculation zone may be below the saturation temperature, the temperature immediately behind the nonporous blockage will exceed the boiling temperature. The axial extent of the wake is taken to be twice the width of the blockage. The axial temperature profile in the wake region is assumed to be linear. Figure 19 shows the model used in the analysis.

Fig. 18. Detection of an Inlet Blockage

Fig. 19. Flow-blockage Model.
(From Ref. 54.)

The transient boiling process is described considering the life-time of a single bubble, with the following assumptions: (1) inertia-controlled growth based upon the work of Theofanous et al.,⁵⁵ (2) isothermal collapse, on the basis that the heat capacity of the vapor content is small compared to the cladding material, (3) negligible heat generation in the fuel rods, because the bubble lifetimes are relatively short, (4) uniform pressure and temperature in the bubble, and (5) thermodynamic equilibrium of the liquid film adhering to the fuel pins with the vapor. The vapor temperature in the growing bubble is then given by

$$T_v = T_{\text{sup}} - \frac{1}{2} \frac{T_{\text{sup}} - T_N}{2D} L_B$$

and in the collapsing bubble by

$$T_v = T_{\text{sup}} - \frac{1}{2} \frac{T_{\text{sup}} - T_N}{2D} L_{B,\text{max}} = \text{Constant},$$

where T_{sup} is the incipient superheat temperature immediately behind the blockage, T_N is temperature corresponding to normal conditions downstream of the recirculation zone, D is the diameter of the blockage, L_B is the length of the bubble, and $L_{B,\text{max}}$ is the length of the bubble at rebound. If equilibrium within the bubble is assumed, the vapor pressure in the bubble is given by

$$P_v = P(T_v).$$

Solving the above equation with the standard momentum equations for the liquid slugs yields the bubble history.

Figure 20 shows typical calculated one-dimensional cylindrical-vapor-bubble growth and collapse histories for the central subchannel of the wake for various initial superheats. The values of the bubble lifetimes in Fig. 20 can be considered as upper limits, because the substantial effects of

Fig. 20

Bubble Growth and Collapse for Local Boiling behind a 50% Flow-area Blockage in an FFTF Subassembly. (From Ref. 54.)

radial subcooling in the wake and frictional and gravitational forces were neglected in the calculations. Therefore, because a thin liquid layer remains on the fuel-pin surface, local dryout is unlikely to occur during the lifetime of the bubble.

The thickness of this liquid film is ~ 0.15 mm;⁵⁶ if undisturbed, the film will maintain cooling for 0.2-0.3 sec. Even if flooding occurs within the bubble,⁵⁷ resulting in thinning and breakup of the liquid film, the temperature rise of the cladding would amount to only 30-40°F, insufficient to prevent wetting of the cladding as the bubble collapses.⁵⁸ On the other hand, if little or no superheating occurs, as may take place after the generation of the first few bubbles (hysteresis effect), the large subcooling in the wake prevents the steady-state vapor velocity from exceeding the flooding velocity. It is apparent from these calculations that dryout and release of molten fuel are very unlikely, even for a blockage large enough to be detected. (This does not, however, preclude cladding failure with the possible release of fission gas and fuel debris.) Furthermore, because the waiting time between bubbles whose growth characteristics can result in sizeable overall flow disturbances (large incipient superheat) is considerably longer than the hydraulic time constant of an FFTF subassembly, flow instability, bulk coolant boiling, and gross cladding melting are also very unlikely. These observations are in general agreement with conclusions reached in Refs. 48 and 59 based upon analysis as well as experimental data on local boiling behind blockages in a simulated LMFBR pin bundle.

The thermal effects within the blockage wake region have been investigated. It is concluded that a local blockage of sufficient size to initiate boiling will likely involve significant numbers of failed fuel rods. There is some experimental evidence that the fission-gas bubbles may be temporarily trapped in the wake downstream of the blockage. However, when the overheating due to the blockage and the gas bubbles initiates boiling, the fission gas will probably be "pumped" out of the wake and a relatively clean subcooled boiling will ensue. Calculations in a clean fuel-rod geometry, unperturbed by blockage debris or gas, indicate that the subcooled boiling in the wake will not propagate to involve the entire fuel subassembly. A previous analysis of possible fuel-failure propagation by vapor-bubble collapse (Ref. 40, pp. 7-17) suggests that any damage would be on rods already failed due to overheating, but could not dismiss the possibility of damage outside the blockage wake. Forthcoming experimental programs (FFM and possibly SLSF) are expected to verify that rapid fuel-failure propagation in local boiling situations is not expected to occur.

C. Limited Thermal Interaction between Molten Fuel and Coolant

In this section, a hypothetical accident is postulated in which one or more fuel pins are assumed to be operating with molten fuel along the interior centerline of the pin as a result of an enrichment loading error. The consequences of fuel-pin failure are investigated. These include considerations of

pressure pulses, mechanical and thermal interactions between pins, and the effects of fuel debris. Analytical models are used to perform parametric studies. Furthermore, it is postulated that one or more of these fuel pins fail, and that molten fuel is instantaneously mixed with surrounding liquid and sodium vapor. The upper-bound pressure pulse, as well as the associated voiding pattern, is predicted. It is concluded that only nominal pressure pulses are expected if molten fuel is released into the coolant and that coolant flow may be quickly reestablished without severe damage to the fuel-pin bundle.

1. Local Pressure Generation

A potential event that could trigger the propagation of failure from pin to pin is a local molten-fuel/coolant interaction (MFCI) caused by failure of one or more fuel pins operating at overpower. Note, however, that no energetic MFCI's have been observed in the TREAT experiments. A treatment by Fauske⁵⁴ explains these results and indicates that relatively mild pressures may be expected. In addition, it is shown in Sec. IV.A.2.c of this report that the fuel pins can be subjected to pressure transients much larger than that expected from an MFCI without failure. Consequently, it is concluded that local pressure generation from thermal interactions will not initiate any significant failure propagation.

2. Mechanical Interactions between Fuel Pins

Pressure pulses resulting from a local MFCI may be much lower than from a fission-gas release.⁵⁴ Mechanical interactions between fuel pins had been considered as a result of a rapid gas release from the fuel-pin fission-gas plenum region. The results of Sec. IV.A.2.c for hollow cylindrical shells indicated that the fuel pin would not be damaged by pressures up to 1000 psi from rapid gas release. When the cladding is supported internally by heavy fuel pellets, it is not expected that the cladding would fail by collapse, since the cladding responds as if it had a thicker wall. Therefore, these pressures are not expected to fail neighboring fuel pins. The effect on the subassembly duct wall is considered in Refs. 60 and 61.

3. Thermal Interactions between Fuel Pins

In this analysis, it was assumed that the molten fuel was released into subcooled sodium coolant surrounding the failed pin. As previously discussed, calculations using the COBRA code indicate that local coolant boiling prior to failure is not expected and, hence, the presence of subcooled sodium is expected initially. When the postulated release of molten fuel occurs, the fuel will be rapidly quenched and fragmented in the surrounding sodium, as observed in many TREAT tests performed at ANL.⁴¹ Typical particle-size distributions observed to result from these tests are shown in Fig. 15. If some molten-fuel particles do contact adjacent fuel pins, it would be anticipated that the coolant film remaining on these pin surfaces would

prevent severe thermal attack by the molten fuel. Therefore, severe thermal interactions between fuel pins is not expected to occur as a result of the local MFCL.

4. Fuel Debris

As discussed previously, molten fuel that is postulated to be released into the coolant rapidly quenches and fragments as shown in Fig. 15, where typical particle-size distributions are given. Much of this fuel debris, because of its small size, could be rapidly swept out of the reactor core region. The wire-wrap spacers are designed so that they produce only a minimal flow obstruction in the coolant subchannels. Figure 21 shows the minimum steady-state coolant velocity required to sweep out fuel particles of any given size. This figure indicates that adequate coolant drag forces exist that can sweep out all particles that are not lodged in the fuel-pin bundle.

Fig. 21

Velocity of Coolant Stream Required to Sweep Out Fuel Debris

Based on results of analyses and experimental information, it is concluded that neither the presence of molten fuel nor its release into the coolant is expected to occur. Nevertheless, if it is postulated that molten fuel is released into the coolant and is allowed to interact thermally with the surrounding subcooled sodium, then nominal pressure pulses may be expected. Even an upper-bound pressure pulse considered in this analysis may not result in severe damage to adjacent fuel pins, nor is the molten fuel expected to result in severe thermal damage to adjacent fuel pins. The molten fuel that is released will be rapidly quenched and fragmented. Much of this fuel debris will be swept out of the core, but it cannot be conclusively demonstrated that all fuel debris will be swept away. It is not expected that sufficient fuel debris will lodge in the pin bundle to cause additional failures.

V. OPERATION WITH DEFECTED FUEL

The preceding sections discussed fuel-pin failures that resulted in a loss of the cladding integrity. Although it has been concluded that these fuel-pin failures do not in themselves represent an immediate safety hazard, we must consider the possible consequence of continued reactor operation with some small number of defected fuel pins.

Three main areas of interest regarding failed-pin performance have been identified: (1) possibility of explosive bursting of sodium-logged fuel pin resulting from excessive internal vapor pressures of sodium, (2) leaching of fission products from the fuel by the sodium and subsequent deposition in the primary coolant system, and (3) pin diametral swelling resulting from a fuel-sodium chemical reaction (sodium logging).

The possibility of excessive fuel swelling resulting from a fuel-sodium chemical reaction is of potential concern, since it may present an inherent limitation on reactor operation for some cases by requiring immediate removal of each failed pin. Leaching of fission products into the primary coolant system may present some design problems related to maintenance; however, the system can be designed to accommodate the radioactivity of fission products released by many fuel pins, if necessary. Explosive bursting of fuel pins due to sodium vapor pressure has been suggested as a potential problem; however, the experimental evidence to date (GE vapor-pressure sodium-logging tests⁶²) indicates that the problem does not exist. Hence, this potential accident will not be discussed further. Only the remaining two problems will be considered here.

A. Fission Products Released from Failed Pins to Coolant

The release characteristics of fission products from oxide-fuel pins into the coolant via a cladding breach are a function of many variables. Some of these variables include fuel temperature, burnup, fission products involved, the characteristics of the failure (e.g., dry failure or failure where sodium can flow back into the inside of the pin), and the sodium temperature and pressure.

The fission products may be grouped according to their relative volatility, which is a fair measure of their mobility. These groupings are rare gases, halogens, volatile solids, and solids. The rare gases released from fuel are transported to the reactor cover gas (neglecting a small amount which will dissolve in the sodium) and must be given consideration relative to cover-gas handling and cleanup systems, as well as to their potential for leakage through and damage to top shield and pump seals during operation. The rare gases can be removed from the reactor cover gas and do not represent a potential accessibility problem in the primary coolant loop by comparison with the other fission-product groups; hence, they will not be treated further in the subsequent evaluations.

Operating experiences with failed fuel in sodium-cooled reactors are limited, and most available information is highly qualitative, since the accumulation of such data was incidental to operational objectives of the reactor system. In addition, the systems in which fuel failures occurred were not equipped or prepared to conduct systematic surveillance programs on fission-product behavior in the primary loop. Another principal shortcoming in available information relative to LMFBR applicability is that much of the experience has been with metal-fueled cores. The characteristics of fission products released from mixed-oxide fuel may be significantly different from those released from metal fuel, both with regard to the rate and the chemical form.

A survey⁶³ of fission-product activities observed in sodium- and NaK-cooled reactors that had experienced varying degrees of fuel failure indicates that the principal fission products observed to have been released were ^{137}Cs , $^{140}\text{Ba-La}$, $^{95}\text{Zr-Nb}$, ^{131}I , ^{141}Ce , ^{144}Ce , ^{89}Sr , and ^{90}Sr . The first three of these isotopes were detected in the only PuO_2 -fueled system reported, the Russian BR-5 reactor.

The rate of fission-product transfer from fuel to coolant depends on a large number of parameters. Important parameters governing the rate of transfer will be the chemical form of the fission-product isotope, its volatility, and its solubility in liquid sodium. For example, the release rates for typical fission-product groups will generally decrease in the order shown: halogens (^{131}I), volatile solids (alkali metals) (^{137}Cs), and solids (rare earths, alkaline earths) ($^{140}\text{Ba-La}$).

All nongaseous fission products may deposit at various locations in the primary system to various extents depending upon the deposition characteristics of the particular fission-product element (chemical form, rather than isotopic form). Some of the factors governing deposition characteristics are the chemical thermodynamic stability of the fission-product oxide relative to sodium oxide in sodium of low oxygen content (e.g., <5-10 ppm oxygen), chemical state in the fuel, solubility of the fission product or its oxide in sodium, surface temperature, the adsorption properties of the deposited species, and the deposition surface.

Although the available data are not adequate to permit firm conclusions at this time, most fuel failures are expected to result in negligible release of solid fission products or solid fuel debris. Experience in operating liquid-metal-cooled reactors strongly indicates that if solid fission products are present in the coolant, they tend to be removed by the cold traps. It is noted that the DFR has operated with vented fuel without incident since 1959. Furthermore, a delayed-neutron monitor on the exit piping of the reactor will detect excessive release of fission products and permit reactor shutdown for removal of failed assemblies, if necessary.

B. Sodium-Fuel Chemical Reaction

The rate of chemical reaction between fuel and sodium resulting in diametral swelling of the fuel pin is of interest for operation with failed fuel

Fig. 22. Fuel-element Swelling vs Time after Failure

pins. Fuel-pin swelling for various times after failure is shown in Fig. 22. Large diametral increases may cause excessive restriction of the coolant channels and tighter packing of the pins within the bundle. Failure and subsequent extensive swelling of one pin in a bundle could restrict the flow to adjacent fuel pins, causing them in turn to fail, swell, and threaten other fuel pins. Preliminary calculations

indicate that the cladding hot spot in the contact area between two fuel pins is less than 100°F above normal.⁶⁴ If such an incident did occur, a significant increase in the outlet coolant temperature could be detected by outlet thermocouples with ample time for operator action, since this rate of propagation would be relatively slow (over a period of hours) compared to other postulated modes of fuel-pin-failure propagation.

The degree of the chemical reaction between fuel and sodium and the extent of the resultant fuel-pin swelling appear to depend primarily on the quantity of "excess" oxygen available for reaction. The effects of temperature and fission-product concentration may also be important contributors.

The quantity of excess oxygen available for chemical reaction is dependent on the initial O/M ratio during fuel fabrication, the net quantity of oxygen liberated as a result of fissioning and recombination with fission products, and the quantity of oxygen leached from the fuel into the coolant. Also, since the reaction occurs mainly at the fuel surface, the degree of migration of oxygen due to fuel temperature gradients to the lower-temperature fuel surface is an important consideration.

An examination of the state of the art yields limited information about the expected behavior of defected fuel. For purposes of evaluation, the failure experience can be divided into three classes: unintentional failures, predefected failures, and sodium-bonded pins. Pertinent failure experience is summarized in Table VI.^{65,66} It is believed that encapsulated failures are atypical and that accidental failures represent realistic, lower-limit reaction rates. The results of the predefected pin-failure test (Test B9A in GETR) are believed to be nonrepresentative of an LMFBR failure. This GE test was conducted in the GETR forced-convection loop with a 0.250-in.-dia mixed-oxide pin operating at a linear pin power of 23.9 kW/ft with a sodium-coolant velocity of 10 ft/sec, values equivalent to about 160 and 35%, respectively, of typical LMFBR conditions. Diametral increases resulting from the sodium-fuel reaction severely restricted the total coolant flow rate to the single fuel pin in the annular flow channels to a considerably larger degree than would be predicted in a multichannel arrangement. This effect caused higher fuel temperatures and probably amplified swelling rates. Furthermore, the oxygen level in the sodium was not controlled during this test.

TABLE VI. Experience with Mixed-oxide-fuel Failure

Site	Type of Experiment	Cladding Material	Approx. Total BU MWd/T	Possible Failure Location	Final Failure Condition	Probable In-reactor Time after Failure	Initial Fuel O/M	Predicted Final O/M	Fuel Swelling		Capsule ΔD/D, %	Fuel-Sodium Reaction Phase
									Vol %	Pin Diameter		
GE EBR-II (F2F)	Encapsulated	Incoloy-800	58,000	Hairline crack in cladding; fabrication defect in bottom end cap weld.	Spiral cladding crack; 16-in. length (14.2-in. fuel column)	Intermediate to F2O and F4F (see below)	1.98	2.01		Up to 53 mils (0.25-in.-OD cladding)	1.2% (same as unfailed)	Yes
GE EBR-II (F4D)	Encapsulated	Incoloy-800	34,000	Fission-gas leak indicated; defect small or above sodium level	No leak at end of test; unfailed condition	Not known	2.00	2.02		<2 mils (0.25-in.-OD cladding)		No
GE EBR-II (F4F)	Encapsulated	Incoloy-800	34,000	Hairline crack in cladding	Localized zones of melted cladding	Failed near end of life	2.00	2.02		Up to 30 mils (0.25-in.-OD cladding)	0.64% (-1½ x normal)	Yes
GE EBR-II (F2O)	Encapsulated	347 SS	71,000	Cladding at fuel zone, probably tube weld, or gas bubble within sodium annulus of capsule	Longitudinal cladding rupture extending over most of fuel column	Failed early in life	2.04	2.07	11%	Up to 56 mils (0.25-in.-OD cladding)	3.5% (-1½ x normal)	Yes
GE EBR-II (FOM)	Encapsulated	Incoloy-800	60,000	Cladding cracks	Multiple axial cladding cracks	Failed early in life	1.98	2.01		Up to 50 mils (0.25-in.-OD cladding)	% N.A. (- same as unfailed)	Yes
GE GETR (B8C-1b)	Encapsulated	316 SS	113,500	Sodium-bonded pin	-	-	1.97	2.03	10%			
GE GETR (B8C-2)	Encapsulated	316 SS	57,700	Sodium-bonded pin	-	-	1.97	2.00	7%			
GE GETR (B8D-1)	Encapsulated	316 SS	38,000	Sodium-bonded pin	-	-	1.97 (40% PuO ₂)	1.99	26.2% (ΔL)			
GE GETR (B8D-2)	Encapsulated	316 SS	40,500	Sodium-bonded pin	-	-	1.98 (25% PuO ₂)	1.99	17.1% (ΔL)			
GE GETR (B8D-3)	Encapsulated	316 SS	38,500	Sodium-bonded pin	-	-	1.94 (25% PuO ₂)	1.96	14.5% (ΔL)			
GE GETR (B9A Top Pin)	Encapsulated forced convection	316 SS	9,300	Predefected	Cladding crack extending 2 in. on each side of fabricated notch defect	<9300 MWd/T	1.99	1.99+		23% in defect area (0.25-in.-OD cladding)		Yes
GE GETR (B9A-Bottom)	Encapsulated forced convection	316 SS	9,300	Predefected	Cladding crack extending 2 in. on each side of fabricated notch defect	<9300 MWd/T	1.95	1.95+		7% in defect area (0.25-in.-OD cladding)		Yes
British	Unencapsulated		Up to >7 at. %	Crack growth from interior to outside of cladding	Split cladding	Not known; in many cases, failure anticipated by dimensioning local diametral increases before ultimate rupture	Various					No
French			Out-of-pile experiment	Sodium-bonded capsule (pin)	-	-	1.98 (no fission products)	-	None			No
French			Out-of-pile experiment	Sodium-bonded capsule (pin)	-	-	1.98 (fission product mockup)	-	"Much swelling"			Yes

Four of the five unintentional failures in EBR-II showed evidence of a fuel-sodium reaction. Three of these had Incoloy-800 cladding, and one had Type 347 stainless steel cladding. Since the Incoloy has a somewhat higher strength and ductility than the stainless steel, the diametral increases of the Incoloy-clad pins are believed to represent low values for application to a stainless steel-clad system. Burnups ranged from 34 to 71 MWd/kg, and as-fabricated O/M ratios ranged from 1.98 to 2.04. Three pins exhibited 0.050-0.060-in. diametral increases; the fourth sustained a 0.030-in. increase. This latter pin had the lowest burnup of the four. However, since it apparently operated for considerably less time in a failed condition than the others, we cannot conclude that burnup level, per se, had any effect.

A general tendency for less swelling in lower-O/M fuel pins can be seen in the results of the GE-B8D sodium-bonded tests and in the aforementioned GETR B9A predefected tests. Sodium-bonded pins having O/M ratios of 1.94, 1.97, and 1.98 exhibited increases of fuel-column length of 14.5, 26.2, and 17.1%, respectively; predefected fuel pins having O/M ratio of 1.95 and 1.99 exhibited diametral increases of 7 and 23%, respectively.

In conclusion, insufficient operating experience with defected fuel exists that would allow definitive conclusions regarding this problem. Nevertheless, low levels of oxygen impurity in the sodium and low initial O/M ratios in the fuel may reduce the effects of excessive sodium-fuel chemical reaction. Even if a sodium-fuel chemical reaction does occur in some cases, the amount of swelling is not expected to result in large reductions of coolant flow. Insufficient information is available to evaluate the potential long-term consequences of operation with a small reduction in normal coolant flow.

VI. SUMMARY AND CONCLUSIONS

Fault-tree analysis has been used to identify various initial faults. These initial faults were represented by one of the following categories: (1) random fuel-pin failure, (2) operation of a fuel pin at overpower, and (3) debris in the primary coolant system. The potential consequences and failure-propagation modes that may result from these accident sources have been identified using safety-assurance diagrams (or process trees). Reactor-operating experience was briefly reviewed with emphasis upon the identification of accident sources, failure-propagation modes, and lines of defense. It was noted that there has not been any evidence of pin-to-pin failure propagation in an operating reactor. Nevertheless, analyses were performed to evaluate the potential modes of failure propagation for a wide range of conditions and to determine the sensitivity of these potential failure modes to various parameters. Experiment results were used to verify analyses whenever possible. On the basis of analyses and experiment results available to date, it was concluded that, although individual fuel-pin failures may occur, rapid failure propagation will not occur in a reactor with appropriate engineering design. Representative modes of failure propagation such as (1) fission-gas release, (2) local blockages in pin bundles, and (3) local molten-fuel/coolant interactions were investigated, and it was concluded that they will not lead to rapid failure propagation.

Uncertainties and assumptions in this analysis were identified as appropriate where there are inadequate experiment results for verification of the analytical models. The primary uncertainties result from (1) an insufficient knowledge of mechanisms of fuel-pin failure for end-of-life conditions and (2) the assumptions of an idealized pin-bundle geometry. Therefore, further analysis and experiments are continuing in order to establish conclusively the margin of safety inherent in an operating reactor.

APPENDIX A

Effects of Dispersed Gas in the Reactor Core1. Summary

The principal results of the two-phase-flow analysis and subsequent heat-transfer calculations are shown in Fig. A.1 for fission gas. These results are presented in terms of flow reduction and nominal temperature increases calculated at the active core exit in the center assembly at full-power conditions.

Fig. A.1. Effect of Fission Gas on Flow and Temperatures in Fuel Assembly at Full Power and Constant Pump Speed

For example, at a void fraction of 0.5, the increase in the coolant outlet temperature will be 200°F, while the inside-clad temperature, at the same position, increases by 210°F. Thus, if the nominal coolant outlet temperature is 1240°F and the corresponding cladding temperature is 1245°F, a void fraction of 0.5 at the core inlet will result in an outlet temperature of 1440°F and a cladding temperature of 1455°F. (The results will also be approximately valid if applied to slightly different nominal conditions.) These results show the following:

- a. Based on steady-state thermal effects (no reactivity consideration), the system can operate with very large void fractions (60% for a 400°F increase in cladding temperature--based on cladding yielding at ~1600-1650°F due to fission-gas pressure). Cladding-temperature limitations are reached well before coolant boiling could occur.
- b. The dominant thermal effect is caused by the reduction in sodium flow (two-phase pressure drop); the effect of a reduced film heat-transfer coefficient (two-phase heat transfer) is secondary.
- c. The flow will be bubbly for void fractions less than ~5%; slug flow will prevail from ~5% to 35-40% voids, with annular flow above this value (which must ultimately convert to mist flow for larger void fractions as the liquid film is sheared from the wall, forming small droplets). The 5% transition is a pessimistic lower bound (i.e., the tortuous flow path will probably provide bubbly flow for higher, but unpredictable, void fractions) based on flow in a vertical pipe.
- d. There does not appear to be any way in which fission-gas leakage could cause a system problem from the thermal-hydraulic effects of two-phase flow investigated in this study.
- e. These conclusions substantiate those obtained by APDA concerning the Fermi reactor.⁶⁷

2. Introduction

The presence of gas in the sodium flowing through the reactor may lead to reduced coolant flow, reduced heat-transfer coefficients, and increases in the temperatures of the coolant, cladding, and fuel. This appendix presents the results of a brief investigation into the quantitative aspects of the above effects for fission gas. The fission gas is presumed to leak into the sodium near the inlet region of the core from a hypothetical cladding breach.

3. Analysis

a. General Considerations and Assumptions

The Baroczy correlations⁶⁸ for the two-phase friction multiplier (ϕ^2), void fraction (α), and slip ratio were used in these calculations.

The effect of gas on the film heat-transfer coefficient has not received much attention in liquid-metal systems. (Single-component, two-phase heat transfer such as boiling has been studied more thoroughly.) In addition, it may be hazardous to even attempt to apply results from a nonmetal system such as air-water; for example, the results of Johnson and Abou-Sabe⁶⁹ show that air injection increases the film coefficient of water (at least up to a point, after which it decreases). In a liquid-metal system, however, the effect of reduced molecular conductivity (due to voids) probably outweighs the effect of increased eddy conductivity (due to increased turbulence), and the overall effect may be a reduction in heat transfer at the same (liquid) mass flow rate, even though the velocity is higher with gas present. On this basis, the normal (no gas) liquid-metal heat-transfer relationship was used to predict two-phase heat transfer, including the changes in Peclet and Nusselt numbers caused by reduced flow rate and reduced thermal conductivity. The largest uncertainty is the estimation of an effective conductivity for the two-phase flow. Several models were evaluated in order to assess the sensitivity of the results to this parameter. Although the "correct" method is still unknown, even the most pessimistic model is not as "damaging" as the effect of reduced flow.

b. Two-phase Pressure Drop

The pressure drop through the core is the sum of the following factors: friction, acceleration or momentum change, and buoyancy. The total pressure drop through the reactor core is thus

$$\Delta P_T = \Delta P_f + \Delta P_{acc} + \Delta P_{buoy} \quad (A.1)$$

For single-phase flow, ΔP_{acc} and ΔP_{buoy} are zero (negligible density change). For two-phase flow, ΔP_{acc} can be appreciable if the pressure change over the core is significant, whereas ΔP_{buoy} is negligible. The contributing pressure drops will now be considered in more detail.

(1) Frictional Pressure Drop. The frictional pressure drop with no gas is obtained from

$$\Delta P_f \cong 47(W/W_0)^{1.80}, \quad (A.2)$$

where ΔP_f is in psi, W is the sodium flow rate, and W_0 is the nominal, or design, flow rate. The "liquid-only" frictional pressure drop is the starting point for two-phase pressure drop due to friction. The Baroczy correlation⁶⁸ relates the pressure drops in terms of a two-phase friction multiplier ϕ_L^2 ; thus, with gas in the core, the frictional pressure drop becomes

$$\Delta P_f = 47\phi^2(W/W_0)^{1.80}, \quad (A.3)$$

where ϕ^2 is taken to be the average of ϕ_L^2 between the inlet and outlet of the core region. The friction multiplier is correlated by Baroczy as a function of "flowing quality" (gas to total mass flow rates) and of the so-called property index I , which was devised by Baroczy:

$$I = \left(\frac{\mu_L}{\mu_g} \right)^{0.2} \frac{\rho_g}{\rho_L}, \quad (A.4)$$

where μ is viscosity and ρ is density. The variation in gas density from inlet to outlet of the core was approximately accounted for by calculating all quantities at both the inlet and outlet. The fission-gas properties are close to those of xenon. The approximate composition of the fission gas is 82.4% xenon (by volume), 13.4% krypton, 3.8% iodine, and 0.4% bromine; the resulting molecular weight is 125 (that of xenon is 131). If one considers the inlet plenum and the "top of the active core" (hereafter called the exit; it is the boundary between the fuel and the upper reflector) as the two end points, and uses the nominal values of 80 psia and 792°F at the inlet and 33 psia and 1240°F at the exit, the following property indices (the sodium properties are $\rho_L = 53 \text{ lb/ft}^3$ and $\mu_L = 0.64 \text{ lb/ft-hr}$ at the inlet, and $\rho_L = 50.5$ and $\mu_L = 0.50$ at the exit) are obtained:

		Fission Gas	Helium
Inlet	Density, lb/ft	1.23	0.040
	Viscosity, lb/ft-hr	0.12	0.082
	I, Eq. A.4	0.032	0.0011
Exit	Density, lb/ft	0.50	0.016
	Viscosity, lb/ft-hr	0.14	0.097
	I, Eq. A.4	0.013	0.00044

Once the quality X and property index are specified, ϕ^2 is obtained from the Baroczy correlation (e.g., see Fig. 8 of Ref. 68). There is also a mass velocity effect on ϕ^2 , and the ϕ^2 obtained at $G_0 = 1.0 \times 10^6 \text{ lb/hr-ft}^2$ must be correlated by using other graphs (see Fig. 9 of Ref. 68) which show

$\phi^2(G)/\phi^2(G_0)$ as a function of X and L . The results are shown in Fig. A.2; the arithmetic average of the inlet and outlet, ϕ^2 , was the value used in Eq. A.3; this assumes a linear variation of ϕ^2 with axial position. The values for helium are shown for comparison between BOL and EOL fuel pins.

Since "flowing quality" may not be as useful as void fraction α , the correspondence between α and X was obtained, again using Baroczy's correlation. This time the liquid fraction $1 - \alpha$ is presented as a function of the property index and of the Martinelli parameter X_{tt} , defined by

$$X_{tt} = \left(\frac{1 - X}{X} \right)^{0.9} \left(\frac{\rho_g}{\rho_l} \right)^{0.5} \left(\frac{\mu_l}{\mu_g} \right)^{0.1} \quad (\text{A.5})$$

It is seen that X_{tt} is a function of the flowing quality and the square root of the property index. Since ρ_g/ρ_l varies between inlet and exit, so must the void fraction (at constant quality). These results are shown in Fig. A.3, which may also be used to relate inlet void fraction to exit void fraction. The inlet void fraction was then selected to represent the "independent" variable for subsequent analyses.

Fig. A.2. Two-phase Friction Multiplier: Baroczy Correlation (from Ref. 68)

Fig. A.3. Void Fraction vs Quality: Baroczy Correlation (from Ref. 68)

Finally, the slip ratios S were computed, since all data were available and S is another quantity of some intuitive interest. The slip ratio is the ratio of gas to liquid velocity and is obtained as follows:

$$\frac{W_g}{W} = \frac{XW_T}{(1-X)W_T} = \frac{\rho_g V_g \alpha A_T}{\rho_l V_l (1-\alpha) A_T} \quad (\text{A.6})$$

or

$$S \equiv \frac{V_g}{V_l} = \frac{\rho_l}{\rho_g} \frac{X}{1-X} \frac{1-\alpha}{\alpha}. \quad (\text{A.7})$$

The slip ratios are shown in Fig. A.4. The maximum slip displayed for helium is more satisfying than the monotonic result obtained for fission gas, since the liquid droplets must get smaller as $\alpha \rightarrow 1$ and, as drop size $\rightarrow 0$, the slip ratio $\rightarrow 1$ (drag force $\approx r$ and weight $\approx r^3$).

Fig. A.4

Slip Ratio vs Void Fraction: Baroczy Correlation (from Ref. 68)

(2) Acceleration Pressure Drop. Since the mixture density decreases from inlet to exit, the momentum flux increases, causing an acceleration pressure drop given by

$$\Delta P_{\text{acc}} = \frac{G^2}{g_c} \left(\frac{1}{\bar{\rho}_{\text{exit}}} - \frac{1}{\bar{\rho}_{\text{inlet}}} \right), \quad (\text{A.8})$$

where $G = W/A$,

$$\frac{1}{\bar{\rho}} = \bar{v} = \frac{X^2}{\alpha} v_g + \frac{(1-X)^2}{1-\alpha} v_l, \quad (\text{A.9})$$

and v is the specific volume.

(3) Buoyancy or Hydrostatic Pressure Drop (Rise). The gain in head (driving force) due to the flow of a two-phase mixture through the core is approximately

$$\Delta P_{\text{buoy}} = (\rho_L - \bar{\rho}) \frac{g}{g_0} h_{\text{eff}}. \quad (\text{A.10})$$

It may be demonstrated that this effect is unimportant by putting $\Delta\rho = 50$ and $h_{\text{eff}} = 10$ ft, giving $\Delta P_{\text{buoy}} \approx 4$ psi. For a more realistic case, say 10% average voids, $\Delta\rho \approx 50(1 - 0.9) = 5$ lb/ft³; on this basis, the buoyancy contribution to pressure rise was neglected.

(4) Total Pressure Drop. The total two-phase pressure drop is estimated from Eqs. A.1 and A.3 as

$$\Delta P_T \approx 47 \left(\frac{W}{W_0} \right)^{1.80} \phi^2(X) + \Delta P_{\text{acc}} \left[\left(\frac{W}{W_0} \right)^2, X, \alpha \right]. \quad (\text{A.11})$$

The assumption that $\Delta P_T = \Delta P_T$ (no voids) = 47 psi yields

$$47 \left(\frac{W}{W_0} \right)^{1.80} \phi^2(X) + \Delta P_{\text{acc}} \left[\left(\frac{W}{W_0} \right)^2, X, \alpha \right] = 47 \quad (\text{A.12})$$

as the final expression from which W/W_0 may be obtained as a function of α_i (using also the graphs relating α_e to α_i , X to α_i , and ϕ^2 to X). These results are presented in Fig. A.1 for fission gas.

c. Two-phase Heat Transfer

The effect of two-component, two-phase (i.e., nonboiling) flow on heat transfer in liquid metals is not well documented. As a first approach, several simple models were employed to estimate the effective conductivity. The following, liquid-only, heat-transfer equation was used:

$$\text{Nu} = 7.0 + 0.025(\text{Pe})^{0.8}, \quad (\text{A.13})$$

where

$$\text{Nu} = hDe/k,$$

$$\text{Pe} = \text{RePr} = 4WC_p/kP_W,$$

and P_W is the wetted perimeter.

To interpret the influence of gas on the Peclet number, consider the following heat balance:

$$q \approx WC_p \frac{dT}{dZ} \approx kP_W \left. \frac{dT}{dr} \right|_{\text{wall}}.$$

Thus,

$$Pe = \frac{4WC_p}{kP_W} = 4 \frac{(dT/dr)_{wall}}{dT/dZ} = 4 \frac{\text{wall gradient}}{\text{axial gradient}}$$

Fig. A.5. Estimates of Two-phase Thermal Conductivity

The effect of gas in the coolant is to reduce W and k , and, depending on which effect dominates, the Peclet number may increase or decrease. For the range of models considered for conductivity, Pe increases with gas addition (i.e., W decreases more slowly than k).

Figure A.5 shows the effective thermal conductivity for several models, including some experimental results (Petrick⁷⁰--nitrogen in NaK) that seem to represent a lower bound. The second curve was quoted by Petrick as describing the conductivity due to holes in a thin sheet (e.g.,

Swiss cheese), whereas the Maxwell relation⁷¹ applies to nonconducting spheres distributed uniformly.

d. Flow-regime Estimate

Two fairly acceptable estimators⁷² are available for predicting the two-phase flow regime for vertical upflow in pipes (most of the data for which came from air-water mixtures). One, by Griffith and Wallis,⁷³ unfortunately does not go "far enough" to cover the range of interest here. (Their data are for much lower velocities: ~0.5-5 ft/sec.) The second by Govier et al.,⁷⁴ comes closer to covering the range and was used to estimate flow regimes. Their chart is shown in Fig. A.6, where Q is the volumetric flow. For our

Fig. A.6
Flow-pattern Prediction Chart
for Vertical Flow (Ref. 74)

cases, the volume ratio ranges from ~0.1 to 100 as α varies from 0 to 1; also, the superficial liquid velocity ranges from ~30 ft/sec at $\alpha = 0$ to ~5 ft/sec for $\alpha \approx 0.9$. The slug-annular boundary shown in Fig. A.1 should be interpreted, according to Fig. A.6, as containing a transition region displaying froth and ripple flow. Also, it seems reasonable to expect a larger range of bubble flow in a wire-wrapped pin bundle than in a pipe; thus, the 5-6% void fraction for transition to slug flow may well be a lower bound.

4. Results and Discussion

One of the principal concerns over two-phase flow is its effect on the operating temperature of the fuel and cladding; of specific interest is the temperature of the cladding and the coolant temperature at the core exit. The other main concern relates more to a transient situation and involves reactivity effects due to voiding. Of necessity, any discussion relating to reactivity effects are qualitative in nature. At this point, only the thermal effects are considered.

If constant-power operation is assumed, the increase in the coolant outlet temperature is related to the flow decrease by

$$\Delta T_c = \Delta T_R \frac{1 - (W/W_0)}{W/W_0}, \quad (A.14)$$

where ΔT_c is the increase in coolant outlet temperature above the nominal and ΔT_R is the nominal coolant temperature rise through the reactor. The corresponding increase in the inside cladding temperature at the core outlet is given by

$$\Delta T_{ic} = \Delta T_c + (T_{ic}^0 - T_c^0) \left(\frac{U_0}{U} - 1 \right), \quad (A.15)$$

where T_{ic}^0 is the nominal inside cladding temperature, T_c^0 is the nominal coolant exit temperature, and U is the overall heat-transfer coefficient given by

$$\frac{1}{U} \approx \frac{1}{h_{TP}} + \frac{\Delta X}{k_{ss}}. \quad (A.16)$$

Here h_{TP} is the estimated two-phase coefficient, and ΔX and k_{ss} are, respectively, the cladding thickness and thermal conductivity. The value of U_0 is obtained by replacing h_{TP} by h_l , the liquid-only film coefficient; this gives

$$\frac{U_0}{U} = \frac{h_l/h_{TP} + h_l\Delta X/k_{ss}}{1 + h_l\Delta X/k_{ss}}. \quad (A.17)$$

Since the reactivity effects associated with single-channel voiding are thought to be unimportant, only the thermal effects need be considered. Here it is seen that the inside cladding temperature does not reach 1600°F (for a 400°F increase in cladding temperature) until the void fraction reaches ~60-65%. This hypothetical result may now be related to hypothetical leakage rates for fission gas; a nominal leak rate of 0.001 lb/sec from a single pin (0.05 lb/sec max) and a maximum of six or seven simultaneous pin failures are postulated. Thus, a maximum gas flow could be taken as 0.35 lb/sec from a single fuel assembly. At a coolant flow of 70 lb/sec, this gas represents a flowing quality of 0.005, which, from Fig. A.3, gives an inlet void fraction of 20%. Figure A.1 is then used to predict a coolant temperature rise of ~16°F and a cladding temperature increase of ~23°F, neither of which is alarming.

Further, considering that a single fuel pin cannot contain more than about 0.007 lb of fission gas, the seven fuel pins above would be empty within $\sim 0.007/0.05 = 0.14$ sec; this indicates that steady-state calculations can only be used to represent a worst case, since the maximum leak rate is no more than a short, transient "burp." To examine the issue from another point of view, how many simultaneous pin failures are required to cause a cladding-temperature problem, ignoring the fact that it is a transient effect? A cladding-temperature increase of 400°F requires ~62% inlet void and a flow fraction of ~38%; Fig. A.3 gives a corresponding quality of 0.083. The use of 90 lb/sec as a nominal coolant flow gives a required gas flow of $0.083 \times 90 \times 0.38 = 2.8$ lb/sec. This requires the simultaneous failure of 2800 fuel pins at 0.001 lb/sec, or 56 fuel pins at 0.05 lb/sec.

APPENDIX B

Local Heat-generating Porous Blockages1. Summary

The release of limited amounts of fuel debris from failed pins may aggregate to form a heat-generating blockage within the fuel subassembly. This incident, even though its occurrence is extremely unlikely, has been identified as a potential mechanism for fuel-failure propagation.⁷⁵ Consequently, a thermal-hydraulic model of this postulated mechanism has been developed to determine the heat-transfer performance of liquid sodium flowing through a porous bed with internal heat generation. A parametric study was performed to determine the sensitivity of the results over a range of parameter values of interest. Some of these results are presented here. Figure 13 (in the body of this report) shows the percent flow rate passing through a unit length of porous bed as a function of the average effective particle diameter for various bed porosities. Figure 14 shows the porous-bed thickness that results in coolant boiling. Relatively large quantities of fuel, at least 2-5 g per flow channel, would be required to produce coolant boiling, and steady-state temperatures within the porous bed would be obtained in a few seconds after blockage initiation.

2. Introduction

Fuel released from failed pins may aggregate to block coolant flow. If the blockage is in the form of a porous mass, small amounts of coolant might flow through it due to pressure gradients across the core and buoyancy forces associated with natural convection. In the present study, the heat-transfer performance of liquid sodium flowing through a porous bed with internal heat generation was investigated.

3. Analysisa. Analytical Model

Figure B.1 shows the analytical model of a flat porous bed of thickness L . The bed has a porosity ϵ , defined as the ratio of the void volume

Fig. B.1

Analytical Model of a Porous Blockage.
ANL Neg. No. 900-1598.

to the total volume. Coolant enters the bed at $x = 0$ at a constant temperature T_{c0} . Energy is generated within the bed at an arbitrary uniform rate q''' and is transferred to the coolant by convection. For such a system, the unsteady-state thermal-energy equations for the porous bed and coolant are⁷⁶

$$\rho_p C_p \frac{\partial T_p}{\partial t} = k_p \frac{\partial^2 T_p}{\partial x^2} - h_v(T_p - T_c) + q''' \quad (B.1)$$

and

$$\rho_c C_c \epsilon \frac{\partial T_c}{\partial t} = \epsilon k_c \frac{\partial^2 T_c}{\partial x^2} + h_v(T_p - T_c) - G\epsilon C_c \frac{\partial T_c}{\partial x}, \quad (B.2)$$

where $T = T(x, t)$ is the temperature, and the subscripts p and c refer to the porous bed and coolant, respectively. The terms on the left sides of Eqs. B.1 and B.2 are the rate of change of thermal energies. The first terms on the right sides are the transport of thermal energies by conduction. The value of the thermal conductivity of the porous bed, k_p , is different from that of the material from which the bed is made, and is usually of the order of 1-2% of the original value, depending on the bed porosity, particle size, etc. The second terms are the interface transport of thermal energy between the porous bed and the coolant, and h_v is the volumetric heat-transfer coefficient. The last term in Eq. B.2 is the energy transport due to bulk movement of the coolant, and G is the mass velocity of the coolant based on the void surface.

Initial and boundary conditions of the system are

$$\text{I.C. } \left. \begin{aligned} T_p(x, 0) &= T_{p0} \\ T_c(x, 0) &= T_{c0} \end{aligned} \right\} \text{ for } 0 \leq x \leq L$$

$$\text{B.C. } \left. \begin{aligned} \frac{\partial T_p}{\partial x} \Big|_{x=0} &= 0 \\ \frac{\partial T_p}{\partial x} \Big|_{x=L} &= 0 \\ T_c(0, t) &= T_{c0} \\ \frac{\partial T_c^2}{\partial x^2} \Big|_{x=L} &= 0. \end{aligned} \right\} \text{ for all } t > 0$$

Heat is conducted through the bed, and there can be heat transfer to the surroundings by radiation and convection at $x = 0$ and $x = L$. Such losses are ignored in the present study, and the temperature gradients of the porous bed are assumed to vanish at both boundaries.

b. Other Major Assumptions

Other major assumptions in the model are:

- (1) Heat flow is one-dimensional, i.e., in the flow direction only. This assumption is valid only for a thin porous blockage; i.e., the dimension of the porous bed in the flow direction is much less than the other dimensions.
- (2) All physical and thermodynamic properties of both the porous bed and coolant are constant.
- (3) The volumetric heat-transfer coefficient h_v and the volumetric heat generation rate q''' are constant.
- (4) The porous bed is isotropic.
- (5) Heat transfer by radiation is negligible.
- (6) Heat generation starts at $t = 0$, when the porous bed has already been established.

Equations B.1 and B.2 are solved by PORPLUG, an IBM 360 computer program, using an implicit numerical method.⁷⁷

c. Estimate of Flow Passing through a Porous Bed

Flow passing through a porous bed was estimated based on the assumption that the pressure gradient across the bed is the only driving force. The pressure drop across the length L in the normal flow channel with constant velocity v was taken as the pressure drop across a porous bed of the same length:

$$\frac{\Delta P}{L} = \frac{f}{D_e} \frac{\rho v^2}{2g_c} \quad (B.3)$$

A conservative flow rate is obtained based on this pressure drop because actual pressure drop should be higher than this. For a coolant passing through a porous bed of porosity ϵ and particle diameter D_p , the pressure drop was calculated by the correlation⁷⁸

$$\frac{\Delta P}{L} = \underbrace{\frac{150\mu v_0}{D_p^2} \frac{(1-\epsilon)^2}{\epsilon^3}}_{\text{Laminar}} + \underbrace{\frac{1.75\rho v_0^2}{D_p} \frac{1-\epsilon}{\epsilon^3}}_{\text{Turbulent}} \quad (B.4)$$

where v_0 is the superficial velocity (the average linear velocity the coolant would have in the bed if no packing were present).

By equating Eqs. B.3 and B.4, one can determine v_0 by an iterative procedure. The mass velocity in the porous bed can then be calculated by

$$G = \rho v_0 / \epsilon. \quad (B.5)$$

d. Estimate of Volumetric Heat-transfer Coefficient h_v

If the porous bed is made of uniform spheres of diameter D_p , the specific surface (particle surface for unit volume of the bed) is⁷⁸

$$S = \frac{6(1 - \epsilon)}{D_p}. \quad (B.6)$$

A random-packed bed may be considered to have a uniform fractional free cross section equal to ϵ . It may therefore be regarded as a single pipe with an exceedingly complicated cross section, with a hydraulic diameter given by⁷⁸

$$d_e = 4 \frac{\text{volume filled with fluid}}{\text{wetted surface}} = 4 \frac{\epsilon}{S}. \quad (B.7)$$

The surface heat-transfer coefficient was calculated by the correlation for liquid metals flowing in circular pipe with constant heat flux along the tube wall:⁷⁹

$$Nu = 7.0 + 0.025Pe^{0.8}. \quad (B.8)$$

By the definition of Nusselt number and Peclet number, we have

$$h = \left[7.0 + 0.025 \left(\frac{d_e \rho_c v C_c}{k_c} \right)^{0.8} \right] \frac{k_c}{d_e}, \quad (B.9)$$

where $v = v_0 / \epsilon$ is the average velocity based on the void cross-sectional area.

The volumetric heat-transfer coefficient h_v is related to the surface heat-transfer coefficient h by

$$h_v = hS. \quad (B.10)$$

The volumetric heat-transfer coefficient thus calculated is too large because, in a random-packed bed, not all the particle surfaces are available for heat transfer. A realistic approach is to determine the volumetric heat-transfer coefficient experimentally by, say, the so-called single-blow technique.⁸⁰

4. Parametric Study

A parametric study was performed for a porous blockage located at the center of an FFTF-type core. The following conditions and values were assumed:

$$T_{c0} = 427^{\circ}\text{C},$$

$$T_{p0} = 427^{\circ}\text{C},$$

$$q''' = 1600(1 - \epsilon) \text{ W/cm}^3,$$

$$\rho_p = 10.96(1 - \epsilon) \text{ g/cm}^3,$$

$$k_p = 0.0005 \text{ W/cm-}^{\circ}\text{C},$$

$$C_p = 0.0717 \text{ cal/g-}^{\circ}\text{C},$$

$$\rho_c = 0.8513 \text{ g/cm}^3,$$

$$k_c = 0.703 \text{ W/cm-}^{\circ}\text{C},$$

and

$$C_c = 0.3041 \text{ cal/g-}^{\circ}\text{C}.$$

The parametric study was performed in the following ranges:

$$0.25 \leq \epsilon \leq 0.50,$$

and

$$100 \mu \leq D_p \leq 1000 \mu.$$

5. Results and Comments

Figure 13 shows the flow rate passing through a unit length of porous bed as a function of particle diameter for various bed porosities. The flow is expressed as percentage of that occurring in a channel under normal operating conditions. As mentioned earlier, this is the minimum flow that would pass through the porous bed. With this flow, the thickness of porous bed that results in coolant boiling is plotted in Fig. 14. This is the maximum thickness of porous bed that could exist without causing coolant boiling to disrupt it. Based on the above information, it can be concluded that a relatively high quantity of fuel debris is required to produce coolant boiling (2-5 g per flow channel for a porous bed with porosity between 0.35 and 0.45 and an average particle diameter between 400 and 500 μ). Figure B.2 shows temperature-time histories of coolant passing through a porous bed with a porosity of 0.4 and a particle diameter of 500 μ . The thickness of the bed is 6.1 cm. Sodium enters the porous bed at 427°C and leaves at 1099°C, which corresponds to the sodium-boiling temperature at 5.2 atm (a temperature rise ΔT of 672°C). Because of the large value of the volumetric heat-transfer coefficient, the temperature history of the porous bed follows the coolant temperature closely, with a temperature difference of only a few degrees. Steady-state temperature profiles are attained in 2.34 sec after blockage initiation. Since the volumetric heat-transfer coefficient might be overestimated, it may take longer to reach cooling boiling. However, an arbitrary reduction of the heat-transfer coefficient by a factor of 100 results in a time increase of only 0.1 sec.

Fig. B.2. Temperature-Time Histories of Coolant Passing through a Porous Bed. ANL Neg. No. 900-1596.

APPENDIX C

Creep Analysis of Cladding Material
under Internal Pressure and Temperature Gradients

1. Summary

A creep analysis of a thin cylindrical shell (fuel cladding) under high internal pressure and a moderate temperature gradient has been performed, and the results are reported here. The analysis shows that the temperature gradient, even though small, has a significant effect on the creep stress (strain) and the life of the shell. The nonuniformly distributed tangential stress, induced by thermal effects, will definitely cause early failure of the shell. Under the interaction of internal pressure and temperature gradient, the shell's "failure time" can be calculated, provided that the fracture stress or a certain finite displacement can be specified on the basis of practical reasons.

2. Introduction

A long, thin-walled cylindrical shell (see Fig. C.1) was used for modeling the cladding of a fuel pin. Under the conditions of high internal pressure, moderate overall temperature level, and moderate temperature gradient, the nonlinear creep stress (strain) produced by the thermal effect as well as by the internal pressure was investigated. The elastic response of the shell was not included in the analysis since the gradient of temperature was assumed to be small. Emphasis was placed upon studying the effects caused by temperature in influencing the mechanical behavior of the shell material.

Fig. C.1. Loading and Geometry of a Thin-walled Cylindrical Shell

3. Analysis

For a long, thin cylindrical shell with $\epsilon_z = \dot{\epsilon}_z = 0$, the assumptions of incompressibility of the material, $\dot{\epsilon}_\theta + \dot{\epsilon}_r + \dot{\epsilon}_z = 0$, and coaxiality of the creep strain rate with the corresponding stress deviation, the following relations can be obtained:

$$\dot{\epsilon}_{\theta} = \frac{3}{4} \sigma_{\theta} \frac{\dot{\bar{\epsilon}}}{\bar{\sigma}} \quad (\text{C.1})$$

and

$$\sigma_z = \frac{1}{2} \sigma_{\theta}, \quad (\text{C.2})$$

where $\dot{\bar{\epsilon}}$ and $\bar{\sigma}$ are the effective strain rate and the effective stress, respectively.

For the present stress field, the effective stress is simply

$$\bar{\sigma} = \frac{\sqrt{3}}{2} \sigma_{\theta}. \quad (\text{C.3})$$

It is assumed that the material follows a nonlinear stress/strain-rate relationship,

$$\frac{d\epsilon}{dt} = \left(\frac{\sigma}{\sigma^*} \right)^n \frac{\sigma}{\lambda}, \quad (\text{C.4})$$

where σ^* (a reference stress) and n are temperature-insensitive material parameters, and λ is the so-called coefficient of viscous traction, a temperature-sensitive parameter.

Upon introduction of Eqs. C.3 and C.4 into Eq. C.1, it becomes

$$\sigma_{\theta}^{n+1} = \left(\frac{2}{\sqrt{3}} \right)^{n+2} \sigma^{*n} \lambda \dot{\epsilon}_{\theta}. \quad (\text{C.5})$$

Since the temperature at the outer surface of the cladding is lower than that at the inner surface, a radial temperature field with linear gradient is assumed in the following form (see Fig. C.1):

$$T(r) = T_0 - \frac{r}{h} \Delta T, \quad (\text{C.6})$$

where $\Delta T = T_i - T_0$, and r is the radial ordinate counted negative inward from the outer surface of the wall.

The parameter λ given in Eq. C.4 can be expressed as

$$\lambda = \lambda_0 \exp \left[\frac{Q}{R_g} \left(\frac{1}{T(r)} - \frac{1}{T_0} \right) \right], \quad (\text{C.7})$$

where Q is the activation energy of the material, R_g is the gas constant (2 cal/mole), T is the absolute temperature expressed in degrees Kelvin, and λ_0 is the coefficient of viscosity at the reference temperature T_0 . Upon substituting Eq. C.6 into the above equation, we have

$$\lambda = \lambda_0 \exp\left(\frac{Q}{R_g T_0} \frac{r}{h} \frac{\Delta T}{T_0}\right), \quad (C.8)$$

which is valid for $\Delta T/T_0 \ll 1$. By letting

$$\alpha = \frac{Q}{R_g T_0} \left(\frac{\Delta T}{T_0}\right),$$

the above equation becomes

$$\lambda = \lambda_0 \exp(\alpha r/h),$$

and Eq. C.5 becomes

$$\sigma_\theta = \left[\left(\frac{2}{\sqrt{3}} \right)^{n+2} \sigma^* \lambda_0 \dot{\epsilon}_\theta \right]^{1/(n+1)} \exp\left(\frac{\alpha}{n+1} \frac{r}{h}\right). \quad (C.9)$$

By using the definition of logarithmic strain and the incompressibility condition of the shell, we can express the radial strain rate $\dot{\epsilon}_\theta$ in Eq. C.9 as

$$\dot{\epsilon} = \frac{\dot{R}(t)}{R(t)} = -\frac{\dot{h}(t)}{h(t)}. \quad (C.10)$$

Since the shell is under high internal pressure p , the equilibrium condition states that

$$pR(t) = \int_{-h(t)}^0 \sigma_\theta dr. \quad (C.11)$$

Upon combining Eqs. C.9-C.11 and integrating, we obtain

$$h(t)^{-(2n+1)} = -\left(\frac{2}{\sqrt{3}}\right)^{n+2} \left(\frac{\sigma^*}{\sigma_{\theta_0}}\right)^n \frac{\lambda_0}{\sigma_{\theta_0}} \frac{1}{h_0^{2(n+1)}} \left\{ \frac{n+1}{\alpha} \left[1 - \exp\left(-\frac{\alpha}{n+1}\right) \right] \right\}^{n+1} \dot{h}(t) \quad (C.12)$$

with

$$\sigma_{\theta_0} = \frac{pR_0}{h_0}; \quad (C.13)$$

where $R_0 = R(0)$ and $h_0 = h(0)$.

By integrating Eq. C.12 with respect to time t and using the initial condition $h(0) = h_0$, we obtain the expression for wall thickness:

$$\left[\frac{h(t)}{h_0} \right]^{2(n+1)} = 1 - 2(n+1) \left(\frac{\sqrt{3}}{2} \right)^{n+2} \left(\frac{\sigma_{\theta_0}}{\sigma^*} \right)^n \frac{\sigma_{\theta_0}}{\lambda_0} \left[\frac{\frac{\alpha}{n+1}}{1 - \exp\left(-\frac{\alpha}{n+1}\right)} \right]^{n+1} t. \quad (C.14)$$

From the above equation, we define $t = t_c$ when $h(t)$ approaches zero:

$$t_c = \frac{1}{2(n+1)} \left(\frac{2}{\sqrt{3}} \right)^{n+2} \left(\frac{\sigma^*}{\sigma_{\theta_0}} \right)^n \frac{\lambda_0}{\sigma_{\theta_0}} \left\{ \frac{n+1}{\alpha} \left[1 - \exp\left(-\frac{\alpha}{n+1}\right) \right] \right\}^{n+1}. \quad (C.15)$$

Equation C.14 now becomes

$$h(t) = h_0 \left(1 - \frac{t}{t_c} \right)^{1/[2(n+1)]}, \quad (C.16)$$

and we have

$$R(t) = R_0 \left(1 - \frac{t}{t_c} \right)^{-1/[2(n+1)]}. \quad (C.17)$$

Hence,

$$\epsilon_{\theta} = \epsilon_n \left\{ \left(1 - \frac{t}{t_c} \right)^{-1/[2(n+1)]} \right\} \quad (C.18)$$

and

$$\dot{\epsilon}_{\theta} = \frac{1}{2(n+1)(t_c - t)}. \quad (C.19)$$

If Eqs. C.15 and C.19 are introduced into Eq. C.9, the tangential stress has the form

$$\frac{\sigma_{\theta}}{\sigma_{\theta_0}} = \frac{\frac{\alpha}{n+1}}{1 - \exp\left(-\frac{\alpha}{n+1}\right)} \left(1 - \frac{t}{t_c} \right)^{-1/(n+1)} \exp\left[\frac{\alpha}{n+1} \left(\frac{r}{h} \right) \right]. \quad (C.20)$$

From this equation, it can be seen that the maximum tangential stress occurs at the outer surface and the minimum tangential stress occurs at the inner surface. For $t < t_c$, their ratio is

$$\frac{\sigma_{\theta \text{ max}}}{\sigma_{\theta \text{ min}}} = \exp\left(\frac{\alpha}{n+1}\right), \quad (\text{C.21})$$

which is a function of α and n only.

When $\alpha = 0$, i.e., $\Delta T = 0$, Eq. C.20 degenerates into

$$\frac{\sigma_{\theta}}{\sigma_{\theta_0}} = \left(1 - \frac{t}{t_{c0}}\right)^{-1/(n+1)}, \quad (\text{C.22})$$

where t_{c0} is obtained from Eq. C.15 by setting $\alpha = 0$:

$$t_{c0} = \frac{1}{2(n+1)} \left(\frac{2}{\sqrt{3}}\right)^{n+2} \left(\frac{\sigma^*}{\sigma_{\theta_0}}\right)^n \frac{\lambda_0}{\sigma_{\theta_0}} \quad (\text{C.23})$$

and

$$\frac{t_c}{t_{c0}} = \left\{ \frac{n+1}{\alpha} \left[1 - \exp\left(-\frac{\alpha}{n+1}\right) \right] \right\}^{n+1}. \quad (\text{C.24})$$

From Eq. C.21 it is seen that, for zero temperature gradient, the radial stress is uniformly distributed across the wall. Failure will definitely occur at time $t < t_c$, when the stress in the shell reaches a certain stress level, say $\sigma_{\theta} = \sigma_f$ (fracture stress), and the corresponding time to fracture can be obtained from Eq. C.20 with $r = 0$:

$$t_f = t_c \left(1 - \left\{ \frac{\alpha \sigma_{\theta_0}}{(n+1) \sigma_f} \left/ \left[1 - \exp\left(-\frac{\alpha}{n+1}\right) \right] \right\}^{n+1} \right). \quad (\text{C.25})$$

It is also feasible to set up a displacement failure criterion, and the time to failure may be derived likewise.

4. Numerical Calculations

For illustrative purpose, the following values are chosen for computation:

$$Q = 80,000 \text{ cal/mole (for steel);}$$

$$R_g = 2 \text{ cal/mole;}$$

$$T_0 = 600^\circ\text{K (620.6}^\circ\text{F).}$$

Equation C.20 with $n = 2$ is plotted in Figs. C.2 and C.3 for $\Delta T/T_0 = 0.03$ and 0.06 , respectively. The solid curves are the tangential stresses nonuniformly distributed across the wall at different times. The broken lines are the stresses

at different times for zero temperature gradient. From these curves the tangential stress at the outer surface shows rapid increase with the increases of time and temperature gradient, whereas the simple elastic stress keeps at a constant level at all times, and the creep stress with no temperature gradient shows slow increase with the passage of time.

Fig. C.2. Distribution of Tangential Stress across the Wall for $\Delta T/T_0 = 0.03$

Fig. C.3. Distribution of Tangential Stress across the Wall for $\Delta T/T_0 = 0.06$

Figure C.4 shows that the ratio of maximum tangential stress and the minimum tangential stress is a function of both n and $\Delta T/T_0$. As the value of n increases, the curves will be asymptotic to the ordinate. A highly nonlinear viscous material will behave in the same way as plastic material, which is essentially temperature-insensitive.

Fig. C.4. Effect of Temperature Gradient on Stress Ratio

In Fig. C.5, t_c/t_{c0} is plotted against $\Delta T/T_0$ for different values of n . These curves show that the "life" of the shell (cladding) is

Fig. C.5
Effect of Temperature Gradient on the Life of the Shell

significantly shortened by the increase of ΔT , but is not much affected by the different values of n except $n = 0$. For instance, for $\Delta T/T_0 = 0.01$ and 0.03 with high internal pressure, the shell life is reduced to 72 and 38%, respectively, of its life without temperature gradient.

5. Conclusions

From the preceding analysis and the calculations for a long, thin-walled cylindrical shell (fuel cladding) under high internal pressure and moderate linear temperature gradient, the following conclusions can be drawn:

- a. The maximum tangential stress occurs at the outside surface of the cladding and increases with time. The simple elastic analysis and the creep solution for zero temperature gradient do not seem to be conservative.
- b. The maximum stress increases sharply with increase of temperature gradient. The thermally induced creep stress should not be overlooked.
- c. The ratio of maximum and minimum stress is sensitive to the material parameter n , but the life of the shell is not too sensitive to the values of n for $n \geq 2$.
- d. The life of the shell will be reduced considerably by increasing the temperature gradient.
- e. The theoretical life of the shell may never be reached. For practical purposes, the time to fracture can be estimated by specifying a certain displacement or a stress level.

APPENDIX D

Fault Trees

Physically, the fault tree appears as tree roots (events and consequences) terminating in the tree trunk (the undesirable event). Standardized symbols used in the construction of fault trees⁸¹ are shown in Figs. D.1 and D.2. The AND/OR gate is activated if any or all of the input events occur; the AND gate requires all inputs to exist simultaneously. The INHIBIT gates describe a

EVENT PRESENTATION:

THE RECTANGLE IDENTIFIES AN EVENT, USUALLY A MALFUNCTION, THAT RESULTS FROM THE COMBINATION OF FAULT EVENTS THROUGH THE LOGIC GATES.

THE CIRCLE DESCRIBES A BASIC FAULT EVENT THAT REQUIRES NO FURTHER DEVELOPMENT. THIS CATEGORY INCLUDES COMPONENT FAILURES WHOSE FREQUENCY AND MODE OF FAILURE ARE DERIVED THROUGH LABORATORY TESTING.

THE DIAMOND DESCRIBES A FAULT EVENT THAT IS CONSIDERED BASIC IN A GIVEN FAULT TREE; HOWEVER, THE CAUSES OF THE EVENT HAVE NOT BEEN DEVELOPED EITHER BECAUSE THE EVENT IS AN INSUFFICIENT CONSEQUENCE OR THE NECESSARY INFORMATION IS UNAVAILABLE.

THE OVAL IS USED TO RECORD THE CONDITIONAL INPUT TO AN INHIBIT GATE. IT DEFINES A STATE OF THE SYSTEM THAT PERMITS AN EVENT SEQUENCE TO OCCUR; AND MAY BE EITHER NORMAL TO THE SYSTEM OR BE THE RESULT OF EQUIPMENT FAILURES.

THE HOUSE INDICATES AN EVENT THAT IS NORMALLY EXPECTED TO OCCUR. FOR EXAMPLE, IT MAY BE USED TO REPRESENT THE EVENT, "TIMING PULSES PRESENT," USED STRICTLY FOR DESCRIPTION (i.e., WHAT IS GOING ON?...IN GROUND RULES OF EFFECTS).

THE TRIANGLES INDICATE TRANSFER SYMBOLS. A LINE FROM THE APEX OF THE TRIANGLES DENOTES A TRANSFER-IN AND A LINE FROM THE SIDE DENOTES A TRANSFER-OUT. REFERS TO A WHOLE SUBFAULT-TREE REPEATED ELSEWHERE.

THE DOUBLE DIAMOND IS USED IN THE SIMPLIFICATION OF THE FAULT TREE FOR NUMERICAL EVALUATION. THE EVENT DESCRIBED RESULTS FROM CAUSES THAT HAVE BEEN DEVELOPED BUT ARE NOT SHOWN ON A PARTICULAR VERSION OF THE FAULT TREE.

Fig. D.1. Definition of Symbols for Event Presentation in Fault-tree Analysis

LOGIC OPERATIONS:

Fig. D.2. Definition of Symbols for Logic Operations in Fault-tree Analysis

causal relationship between one fault and another; that is, the input event directly produces the output event if the indicated conditions are satisfied.

Specific initiating events that may lead to pin-to-pin failure propagation are identified in the fault trees shown in Figs. D.3-D.5. In particular, consider only the following accident categories: (1) loss of cladding integrity; (2) abnormal fuel and cladding temperature;* (3) abnormal cladding-surface conditions.* These accident categories characterize significant initiating events which provide an envelope of interest for analyses.

The first category, "loss of cladding integrity," shown in Fig. D.5 is characteristic of an accident in which fission gas is released into the coolant; this gas may thermally blanket or otherwise cause failure of neighboring pins, and then that initial failure may propagate. The second category, "abnormal fuel and cladding temperature," shown in Figs. D.3 and D.5, is characteristic of an accident in which various amounts of molten fuel may be present in the reactor; potentially, this molten fuel may enter the coolant. A case is postulated in which an overenriched fuel pin is accidentally misloaded into the wrong location in the reactor core; this case is considered representative of the second category. The third category, "abnormal cladding-surface conditions," shown in Fig. D.4, is considered representative of those accidents in which local in-core blockages may be formed and eventually cause fuel-pin failures. Lines of defense demonstrating that these initiating events or accident sources are unlikely are shown in the safety-assurance diagrams in Ref. 82.

*All abnormal conditions do not necessarily lead to a failure, but all failures result from abnormal conditions. In this context, only those conditions leading to a failure are considered.

Fig. D.3. Pin-to-Pin Failure Propagation

BLANK PAGE

D1

ABNORMAL
CLADDING
SURFACE
CONDITIONS

GAS
BLANKETING

DEPOSITS
ON CLAD
SURFACE

CONTINUOUS
GAS JET

GAS RELEASE
FROM FUEL
PIN

D2

OXIDE OR
HYDRIDE ETC
DEPOSITS

G
D

SEE
NOTE 2

LOSS OF
CLADDING
INTEGRITY

FISSION
GAS FLOW

L3

L2

FAULT LOOP

SOURCES OF
OXYGEN OR
WATER

FAILURE OF
COOLANT
PURIFICATION
SYSTEM

FAILURE OF
COOLANT
PURIFICATION
SYSTEM

D5

D4

- (NOTE 1) { A. DEBRIS COLLECTS IN FUEL BUNDLE.
B. MANY COOLANT CHANNELS BLOCKED AT SAME AXIAL LOCATIONS
C. NO POROSITY IN BLOCKED CHANNEL

- (NOTE 2) { A. SUCH DEPOSITS, IF THEY OCCUR WOULD BUILD UP SLOWLY OVER
LONG TIME PERIODS ONLY.
B. PERIODIC CHEMICAL ANALYSIS OF SODIUM WOULD INDICATE HIGH
IMPURITY LEVELS.
C. IF SUCH FOREIGN MATERIAL WERE IN SODIUM PARTICULATE
MATTER, SLOWLY DISSOLVING AND DEPOSITING ON COLD SUR
FACES WOULD BE MORE PROBABLE.

- (NOTE 3) { A. OIL PRESSURE ALARM INDICATES LEAK
B. SPECIAL PUMP SEAL DESIGN PREVENTS ACCESS TO SODIUM.

- (NOTE 4) { A. MASSES OF CRUD OR DEBRIS WILL BE SHEARED BY PUMP IMPELLER
ONLY PARTICLES LESS THAN 1/4 IN. CAN PASS THROUGH IHX.
C. ONLY PARTICLES LESS THAN 1/4 IN. CAN PASS THROUGH INLET
PLENUM GAS DISPERSAL-DISTRIBUTOR VESSEL WALL.
D. RADIAL DISPERSION AND DILUTION OF ALL PARTICULATE MATTER
E. PARTICULATES ARE BLOCKED IN THE SUPPORT BAR REGION OF
FUEL ASSEMBLY.
F. PARTICULATES ARE BLOCKED IN THE LOWER AXIAL BLANKET
REGION.

- (NOTE 5) { A. DE-GASIFICATION SYSTEM INOPERATIVE OR INADEQUATE.
B. ALARM INDICATES LEAK.
C. PUMP SEAL DESIGN PREVENTS OIL CONTACT WITH SODIUM.

- (NOTE 6) { A. DE-GASIFICATION SYSTEM INOPERATIVE OR INADEQUATE.
B. ALARM INDICATES LEAK.
C. PUMP SEAL DESIGN PREVENTS OIL CONTACT WITH SODIUM.

LEAKS
IN SEALS

IN-ADEQUATE
MAINTENANCE
STDS

SYSTEM
INADEQUATE

IN-ADEQUATE
START-UP
TESTS

SYSTEMS
INOPERATIVE

IN
ADEQUATE
START-UP
TEST

BLANK PAGE

ABNORMAL
CLADDING
SURFACE
CONDITIONS

DEPOSITS
ON CLAD
SURFACE

**OXIDE OR
HYDRIDE ETC
DEPOSITS**

GRAPHITIC CARBON DEPOSITS

SEE
NOTE 2

SEE
NOTE 2

2

Fig. D.4. Abnormal Cladding-surface Conditions

BLANK PAGE

APPENDIX E

Overpower Fuel Pin

1. Introduction

A fuel pin subjected to an overpower is postulated as an accident source for a potential failure-propagation mode. In fact, a combination of errors is assumed: (1) a fabrication error in the manufacture of the fuel pin and (2) a misloading error, which results in placing that fuel pin in the wrong reactor-core location. Either error, by itself, will not result in the production of molten fuel, as demonstrated by the analysis in this appendix.

Fabrication of fuel with enrichment levels (fissile plutonium fraction of the U-Pu oxide) in excess of design values is prevented by use of standardized fabrication and quality-control processes. All incoming fuel material is subjected to analysis for compliance to specified requirements. Statistical laboratory tests and analyses are also performed to validate incoming material.

Fuel-fabrication processes are usually controlled by a manufacturing-production-order (MPO) system by which specific quality-control inspections must be performed, as required by processing requirements and fabrication inspection planning, and a quality-control-verification stamp must be affixed on the MPO before the next fabrication process step can be started. Statistical laboratory tests are again used to confirm that fabrication processes are providing products that comply with specified requirements.

Therefore, since enrichment errors in excess of design values should be prevented by (1) the administrative controls outlined above, (2) statistical laboratory tests of incoming fuel materials, tests during fabrication, and testing of the fabricated fuel pin, (3) procurement controls, and (4) reactivity anomalies during fuel loading, it is concluded that fuel enrichments significantly different from design values are unlikely.

Since the failure consequences of an overpower fuel pin are thought to provide an upper-limit initiating event for pin-to-pin propagation, the hypothetical case of grossly overpower fuel is selected for further safety analysis. As shown in Fig. E.1, enrichments greater than 130% of nominal are required to produce significant quantities of molten fuel at the pin centerline. Such large enrichment errors may cause near-term fuel failure from a number of causes such as excessive vapor pressure of the fuel or excessive cladding temperature.

An analytical model (MARGE/SLUMP, an improved version of MARGE)⁶³ has been developed for predicting fuel melting in an overpower fuel pin under LMFBR steady-state conditions. A parametric analysis was performed for the nominal peak pin in the FFTF to determine the sensitivity of the results to the calculational parameters. The predicted amount and location of molten fuel were substantially different from the results of previous models, which did not consider the axial relocation of molten fuel.

Fig. E.1
Fuel-pin-failure Model:
Overenriched Fuel Pin

The analysis of the molten-fuel inventory in LMFBR overpower pins includes the following parameters specifically applicable to the FFTF reactor: fuel-restructuring temperatures and densities, fuel thermal conductivity, and fuel-cladding gap conductances. The effect of each parameter was assessed, and the molten-fuel inventory was calculated as a function of power level up to approximately twice the normal FFTF peak power.

2. FFTF Parameters

The parameters used in this study are shown in Table E.1. A coolant temperature rise of 368°F was assumed for all power levels, corresponding to 100% coolant mixing in the fuel bundle. The normal power for the peak fuel pin was taken as 13.0 kW/ft at the core axial center.

The equation for fuel thermal conductivity in Table E.1 was recommended for inclusion in the LIFE-II code⁸⁴ and is being adopted as the reference correlation in the SAID document. Other thermal-conductivity correlations available in the MARGE/SLUMP code are shown in Table E.2.⁸³ Both the General Electric and the Westinghouse correlations in Table E.2 are currently referenced by HEDL⁸⁵ up to the time that the correlation in Table E.1 was developed.

Figure E.2 compares the new Westinghouse correlation (see Table E.1), which is used in the present study with the old Westinghouse correlation (see Table E.2). The new correlation gives slightly higher values at the very low and very high ends of the temperature range. For example, at the estimated melting point of 4850°F (2850°K), the new correlation gives a thermal conductivity that is 10% higher than the old value. These higher thermal conductivities will result in lower fuel temperatures and less fuel melting.

Fuel-restructuring models that are available in the MARGE/SLUMP code are shown in Table E.3 along with the model recommended by HEDL.⁸⁵ Each of the first four models in Table E.3 is used with its corresponding thermal-conductivity correlation in Table E.2. Except for the Karlsruhe model, the HEDL model, which is used for the present study, has the lowest restructuring temperatures and densities.

TABLE E.1. Parameters for Analysis of Molten-fuel
Inventory in FFTF Overpower Fuel Pin

Pin power	Variable (up to 2 times normal power)	
Axial power factor	1.24 (chopped cosine)	
Coolant inlet temperature	792°F	
Coolant temperature rise	368°F	
Fuel melting temperature	4850°F	
Burnup	20,000 MWd/T	
Redistribution of initial fuel-cladding gap to the central void	0 and 100%	
Fuel bridge present	Yes	
Fuel thermal conductivity:	$K = \frac{1.33(1 - P)}{1 + \beta P} \frac{1}{0.78 + 0.02935T} + 6.60 \times 10^{-13}T^3$	
	K = thermal conductivity, W/cm-°K	
	P = fractional porosity	
	$\beta = 1 + 10P$	
	T = temperature, °K	
Fuel restructuring parameters:	TC = 41532/(16.08 + log _e B)	
	TE = 671.7 + 9.611 x 10 ⁷ /(1.103 x 10 ⁵ + B)	
	TC = columnar-grain-growth temperature, °C	
	TE = equiaxed-grain-growth temperature, °C	
	B = burnup, MWd/T	
	Density of columnar material, $\rho_{col} = 0.965$ TD	
	Density of equiaxed material, $\rho_{eqi} =$ as-fabricated density	
Fuel-cladding gap conductance:	Axial position from bottom of core, in.	Gap conductance, Btu/hr-ft ² -°F
	0.85	590
	6.00	970
	12.86	1530
	18.00	1630
	24.86	1370
	30.00	1000
	35.14	634

TABLE E.2. Thermal-conductivity Correlations for Mixed-oxide Fuel

Atomics International

$$K(T, P) = K(T) \frac{1 - P}{1 + (\alpha - 1)P}$$

$$K(T) = \frac{54.0}{T} + 1.749 \times 10^{-12} T^3 - 0.0179 \quad (T \text{ in } ^\circ K)$$

$0 < P < 0.10$	$\alpha = 1.5$
$0.10 < P < 0.15$	$\alpha = 2.0$
$0.15 < P < 0.20$	$\alpha = 2.4$
$0.20 < P < 0.25$	$\alpha = 2.6$

General Electric

$$K(T, \rho) = 0.011 + \frac{1}{T(0.4848 - 0.4465\rho)} \quad (T \text{ in } ^\circ C)$$

Karlsruhe

$$K(T, P) = K(T)(1 - P^{2/3}) \approx K(T)(1 - \eta P)$$

$$K(T) = \frac{41.2}{T + 4.9} + 6.55 \times 10^{-13} T^3 \quad (T \text{ in } ^\circ K)$$

$0 < P < 0.10$	$\eta = 2.5$
$0.10 < P < 0.15$	$\eta = 2.0$
$0.15 < P < 0.25$	$\eta = 1.7$

Westinghouse

$$0.95 \leq \rho < 1.00$$

$$K(T, \rho) = (3\rho - 1) \left(\frac{1}{5.75 + 0.0503T} + 2.91 \times 10^{-13} T^3 \right)$$

$$0.85 \leq \rho < 0.95$$

$$K(T, \rho) = (1 - 2.1\rho + \rho^2) \left(-\frac{1}{0.288 + 0.00252T} - 5.83 \times 10^{-12} T^3 \right) \quad (T \text{ in } ^\circ K)$$

where

K = mixed-oxide fuel thermal conductivity (W/cm-°K)

T = fuel temperature

 ρ = fractional theoretical fuel densityP = fractional fuel porosity ($P = 1 - \rho$).

Fig. E.2

Westinghouse Correlations for Thermal Conductivity of Mixed-oxide Fuel

TABLE E.3. Fuel-restructuring Models

	Fractional Fuel Density		Temp, °C	
	ρ_{eqi}	ρ_{col}	TE	TC
Atomics International	0.95	0.98	1600	1800
General Electric	0.97	0.99	1650	2150
Karlsruhe	as-fabricated	0.95	1300	1700
Westinghouse	0.97	0.99	1600	2000
HEDL (see Table E.1)	as-fabricated	0.965	1409 ^a	1598 ^a
			1177 ^b	1517 ^b

^aBased on a burnup of 20,000 MWd/T.

^bBased on a burnup of 80,000 MWd/T.

Figures E.3 and E.4 show the effect of burnup on the equiaxed- and columnar-grain-growth temperatures, respectively, in the HEDL model. For a burnup of 20 MWd/kg, the equiaxed and columnar temperatures are 1409 and 1598°C, respectively. Actually, the restructuring temperatures vary with local burnup, and there would be some axial variation. For a peak burnup of 20 MWd/kg at the axial center, the variation in the columnar and equiaxed

Fig. E.3. Equiaxed-grain-growth Temperature

Fig. E.4. Columnar-grain-growth Temperature

temperatures from the axial center to the ends of the core region is 52 and 69°C, respectively. This variation is neglected in the present study, particularly because the restructuring in the equiaxed region has no effect on the results, inasmuch as the density in the equiaxed region is the same as that in the unstructured region.

Fig. E.5. Fuel-cladding Gap Conductances for Peak-power FFTF Fuel Pins

The present study takes into account the axial variation in the gap conductance. Figure E.5 shows the gap conductance for the peak-power FFTF fuel pins at two levels of burnup: 20,000 and 80,000 MWd/T.⁸⁶ Although the average gap conductance at these two burnup levels is approximately the same (between 1100 and 1200 Btu/hr-ft²-°F), there are considerable differences in the axial variation. Less fuel melting would be expected at

20,000 MWd/T because of the much higher gap conductances near the core center. The gap conductances in Fig. E.5 for the peak-power pins are applied to overpower pins without attempting to account for the effect of the increased power level.

3. Effect of FFTF Parameters

The effects of fuel-restructuring parameters, fuel thermal conductivity, variable gap conductances, and burnup have been systematically investigated. Five cases were considered for an FFTF overpower fuel pin operating at a peak power of 19.5 kW/ft (about 50% over the normal power for the peak-power fuel pin) with a coolant temperature rise of 368°F (100% coolant mixing in the fuel bundle) and assuming that none of the initial fuel-cladding gap reaches the central void.

The results are summarized in Table E.4. In the first case, the original Westinghouse model in the MARGE/SLUMP code (the Westinghouse correlation for fuel thermal conductivity in Table E.2 and the Westinghouse fuel-restructuring model in Table E.3) was used, assuming a constant gap

TABLE E.4. Effect of FFTF Parameters

Case	Correlation for Fuel Thermal Conductivity	Fuel-restructuring Model (see Table E.3)	Gap Conductance, Btu/hr-ft²-°F	Molten-fuel Inventory, g
1	Westinghouse (see Table E.2)	Westinghouse	1100	16.6
2	Westinghouse (see Table E.2)	HEDL	1100	21.7
3	Table E.1	HEDL	1100	12.9
4	Table E.1	HEDL	20 MWd/kg (Fig. E.5)	6.7
5	Table E.1	HEDL	80 MWd/kg (Fig. E.5)	10.2

Fig. D.3. Pin-to-Pin Failure Propagation

conductance of 1100 Btu/hr-ft²-°F; a molten-fuel inventory of about 17 g was predicted. In the second case, the same fuel thermal conductivity and gap conductance were used, but the fuel-restructuring model was changed to the HEDL model in Table E.3, assuming a burnup of 20,000 MWd/T. This gave a predicted molten-fuel inventory of about 22 g, or an increase of 30% over the first case.

In the third case, the HEDL fuel-restructuring model was retained, but the new correlation for fuel thermal conductivity given in Table E.1 was assumed. The predicted molten-fuel inventory decreased by 40% to about 13 g. Therefore, the higher thermal conductivities obtained by the new correlation more than compensated for the reduced densities of the columnar and equiaxed regions in the HEDL fuel-restructuring model. In the fourth case, the effect of a variable gap conductance was assessed by using the values for 20,000 MWd/T shown in Fig. E.5 instead of the constant value of 1100 Btu/hr-ft²-°F used in the previous three cases. This resulted in a decrease in the molten-fuel inventory of almost 50% compared to the third case.

In the last case, the effect of burnup was assessed by using the variable gap conductances for 80,000 MWd/T shown in Fig. E.5. The fuel-restructuring temperatures in the HEDL model were also changed according to the values listed in Table E.3. This resulted in a predicted molten-fuel inventory about 50% higher than for Case 4. Although it would appear that the molten-fuel inventory would gradually increase as a function of burnup, more data in addition to those in Fig. E.5 are desirable to verify that the gap conductances in the central portion of the fuel pin do decrease with burnup.

Figure E.6 shows the difference in fuel configuration between Cases 1 and 4. The central void region above the molten pool is smaller in Case 4 than

Fig. E.6. Comparison of Fuel Geometries in FFTF Overpower Fuel Pins

in Case 1 because the higher gap conductances in the central portion of the fuel pin reduce the amount of fuel melting. However, the central void at the extreme ends of the fuel pin is larger in Case 4 compared to Case 1 because the gap conductances at the ends are much lower than the average value of 1100 Btu/hr-ft²-°F, and more fuel restructuring takes place.

4. Effect of Power Level

The effect of power level on the molten-fuel inventory is shown in Fig. E.7 for the two cases of 0 and 100% redistribution of the initial fuel-cladding gap to the central void. The

Fig. E.7. Molten-fuel Inventory in FFTF Overpower Pin

latter case results in a much larger central void, and substantially more molten fuel can slump downward and solidify in the lower regions of the fuel pin.

Even if the void associated with the initial fuel-cladding gap remains in the unrestructured-fuel region, the FFTF overpower pin would have to be about 40% over its normal peak power (assumed to be 13.0 kW/ft) before molten fuel would exist in the pin. For 100% redistribution of the initial gap to the central void, the power level for molten fuel to exist is increased to about 60% over the normal peak power.

Therefore, little or no molten fuel would exist in FFTF overpower pins for credible levels of overenrichment and/or loading errors up to an overpower of about 50%.

5. Operation with Molten Fuel

It was postulated that a fuel pin had been operating in an overpower condition which resulted in the production of molten fuel. Various failure mechanisms that allow the release of this molten fuel into the coolant were then investigated. No likely failure mechanism has been found for fuel pins early in the fuel cycle. Furthermore, experiments in the TREAT D-series provide support for this conclusion. Two recent in-reactor tests at ANL (D1, and D2)⁸⁷ designed to study the consequences of the release of small amounts of molten fuel into the coolant have demonstrated that cladding failure and subsequent release of molten fuel are unlikely. In these tests, which simulated FFTF normal flow and temperature conditions in the Mark-II loop, the cladding material, cladding dimensions, fuel-pellet shape, pellet diameter, and pellet density, as well as the wire-wrap material, pitch, and diameter, conformed to FFTF specifications. These tests were performed in a seven-pin test bundle in which the central pin had a 1.5-in.-long, fully enriched section at the axial midplane of a 13.5-in. fuel column. These were transient tests, and in the TREAT D2 test approximately 60-70% areal fuel melting occurred in the fully enriched section of the central fuel pin. Posttest examination of the central fuel pin showed considerable cracking of both fully enriched and normal-enrichment pellets. Generally, the molten fuel in the hot section was observed to move axially by slumping downward into a central voided region created by the fuel melting. In some instances, however, the molten fuel was observed to move radially, principally at the pellet interfaces, out to the cladding. It was observed that the molten fuel that contacted the cladding solidified without detectable damage to the cladding. This observation

is consistent with an analysis,⁸⁸ summarized in Fig. E.8, which shows that molten fuel at a temperature a few hundred degrees above the fuel melting point will not melt through the cladding as long as normal coolant flow is maintained.

Fig. E.8

Initial Temperature Map for Initially Molten UO₂ Contacting Initially Solid Stainless Steel, Showing Region (Unshaded) in Which Simultaneous UO₂ Solidification and Steel Melting Occur (Ref. 88). ANL Neg. No. 900-1904.

In view of these analyses and experiments, it is concluded that near-term failure of fresh overpower fuel pins will not occur for the conditions considered.

Of course, continued operation of these fuel pins under overpower conditions and repeated thermal cycling may eventually lead to failure before the normal design lifetime. It is also known that the life of the cladding may be reduced considerably by increasing the temperature gradient in the cladding for extended periods of time. This analysis is shown in Appendix C. However, no specific failure mechanism has been identified yet for the case of extended operation.

ACKNOWLEDGMENTS

We would like to thank the ANL staff members who provided many helpful comments and suggestions during the preparation of this work. In particular, we would like to recognize the contributions of J. B. van Erp and T. C. Chawla for their comments on the thermal effects of fission-gas release, J. R. Honekamp and T. D. Claar of the EBR-II Project for their comments on reactor operating experience and operation with failed fuel, and D. H. Lennox for review and suggestions on the entire report. Finally, we would also like to acknowledge the helpful encouragement of B. D. LaMar for this work.

REFERENCES

1. A. M. Judd, T. R. Burns, and W. W. Marr, ANL, personal communication (Dec 1970).
2. D. F. Haasl, *Advanced Concepts in Fault Tree Analysis*, presented at System Safety Symposium, Seattle, Washington (June 8-9, 1965).
3. K. M. Swanson, J. K. Butler, and B. Entwistle, UKAEA, unpublished data (May 1969).
4. M. D. Carelli, H. D. Garisch, and J. Graham, *An Analysis of the MK II-A Subassembly Failure*, WARD-4135-10 (June 1970).
5. R. D. Coffield and P. L. Wattleit, *An Analytical Evaluation of Fuel Failure Propagation for the Fast Flux Test Facility*, WARD-AT (45-1)-2171-7 (Dec 1970).
6. J. Honekamp et al., *Observation of Clad Failures in (U,Pu)O₂ Elements Irradiated in EBR-II*, presented at FTR/LMFBR Fuel Pin Performance Meeting held at HEDL (Sept 25-26, 1974).
7. B. M. Hoglund, R. P. Anderson, and L. Bova, *Experimental Study of a Gas Jet Penetrating a Liquid Coolant and Impinging on a Heated Surface*, ANL-7734 (Mar 1971).
8. H. H. Scott, USAEC, personal communication (Mar 1970).
9. M. D. Carelli, *Fission Gas Release from Failed Fuel Pins in Liquid Metal Fast Breeder Reactors*, WARD-5446 (Sept 1971).
10. J. B. van Erp, T. C. Chawla, A. M. Judd, and R. E. Wilson, ANL, personal communication (Apr 1971).
11. T. C. Chawla, *Equations of Motion of a Liquid-submerged Sonic Gas Jet Relative to Fission-gas-jet Impingement in Subassemblies of the Liquid Metal Fast Breeder Reactor*, Nucl. Sci. Eng. 53, 466-474 (1974).
12. B. Misra and R. Crawford, *Pressure Decay and Fission Gas Release Rates for LMFBR Fuel Pins*, Trans. Am. Nucl. Soc. 18, 204 (1974).
13. J. W. Barnaby, A. S. Fraser, and K. Buxton, UKAEA, unpublished data (Oct 1972).
14. HEDL *Quarterly Technical Report*, HEDL-TME-73-4, Vol. I (April, May, June, 1973).
15. J. B. van Erp, T. C. Chawla, and R. E. Wilson, *Potential Fuel Failure Propagation due to Fission-gas Release in LMFBR Subassemblies*, presented at ASME Annual Meeting, Paper 72-WA/NA-14, New York (1972).
16. Y. V. Ivanov, *Equations for Trajectories of Jets with Acute Blast*, Sovetskoe Kotloturlostroenie 8 (1952).
17. G. M. Abramovich, *Theory of Turbulent Jets*, MIT Press, Cambridge, Mass. (1963).
18. A. M. Judd, "Loss-of-Coolant Accidents in a Large Sodium-Cooled Fast Reactor," *Proc. Conf. Safety, Fuels, and Core Design in Large Fast Power Reactors: October 11-14, 1965*, ANL-7120, pp. 67-81 (1965).

✓

19. R. Bell, B. E. Boyce, and J. G. Collier, *The Structure of a Submerged Impinging Gas Jet*, J. Br. Nucl. Energy Soc. 11(2), 183-193 (1972).
20. J. M. Kennedy et al., *Stress Analysis for an LMFBR Fuel Pin with Temperature Distribution Pertaining to Fission Gas Jet Impingement*, Trans. Am. Nucl. Soc. 17, 343-344 (1973).
21. T. C. Chawla et al., "Fission-gas Release Studies," *Reactor Development Program Progress Report: August 1971*, ANL-7854, p. 8.4 (Sept 23, 1971).
22. M. Hori and A. J. Friedland, *Effect of Gas Entrainment on Thermal-hydraulic Performance of Sodium Cooled Reactor Core*, J. Nucl. Sci. Tech. 7(5), 256-263 (1970).
23. J. B. van Erp, C. J. Roop, and P. L. Zaleski, *Simulation of Slow Fission Gas Release in an LMFBR Fuel Subassembly*, Trans. Am. Nucl. Soc. 14(1), 277-278 (1971).
24. F. E. Holt, *Fuel Failure Mode Analysis: A Review of Fuel Element Cladding Failure and Consequences as a Basis for Failure Detection, Characterization and Location Systems for the FFTF Reactor*, HEDL-TME 71-84 (June 1971).
25. J. H. Shively, *Biaxial Creep and Stress Rupture of Wire-wrapped Cladding in 1200°F Sodium under ΔT Conditions*, AI-AEC-12944 (Apr 1970).
26. M. Hori and A. J. Friedland, *Analysis of Gas-release Effect in Fuel Cladding Failure*, Trans. Am. Nucl. Soc. 12(1), 333-334 (1969).
27. S. Kondo et al., *A Simulation Experiment of the Results of Fission Product Gas Release Due to Fuel Cladding Failure using Water Circulation System*, J. Nucl. Sci. Tech. 7(4), 197-209 (1970).
28. T. C. Chawla and B. M. Hoglund, *A Study of Coolant Transients during a Rapid Fission-gas Release in a Fast-reactor Subassembly*, Nucl. Sci. Eng. 44(3), 320-344 (June 1971).
29. T. C. Chawla and B. M. Hoglund, *A Study of Coolant Transients during a Rapid Fission-gas Release in a Fast-reactor Subassembly*, ANL-7651 (Jan 1971).
30. T. C. Chawla and B. M. Hoglund, *Pressure Distribution for Axisymmetric Two-dimensional Flow in a Plenum during Coolant Expulsion*, Nucl. Sci. Eng. 43, 87-90 (1971).
31. T. C. Chawla et al., *The Recovery of Coolant Flow Following Rapid Release of Fission Gas from a Postulated Multiple Pin Failure in a Liquid-Metal Fast Breeder Reactor Subassembly*, Nucl. Sci. Eng. 58, 21-32 (1975).
32. T. C. Chawla et al., *Coolant Expulsion and Reentry Following Rapid Fission Gas Release in LMFBR Subassemblies*, Trans. Am. Nucl. Soc. 14(2), 730 (1971).
33. J. H. Woodward and J. P. Anderson, "A Model for Collapse-hinge Instability of Blast-loaded Cylinders," *Developments in Theoretical and Applied Mechanics*, 4, Pergamon Press (1970).
34. W. J. Schuman, *The Response of Cylindrical Shells to External Blast Loading, Parts I and II*, BRL Memorandum Reports No. 1461 and 1560 (1963 and 1964).
35. B. L. Boers, *Explosive Technique for Pressure Pulse Testing of Reactor Components*, Trans. Am. Nucl. Soc. 12(2), 828 (1969).
36. J. J. Regimbal, Editor, *Technical Bases for FFTF Driver Fuel Instrumentation*, BNWL-916 (Oct 1968).

37. A. J. Brook, *Temperatures Associated with Local Blockages in the Prototype Fast Reactor*, AHSB(S) M 153, UKAEA, Risley (Mar 1966).
38. C. V. Gregory and S. P. Broad, UKAEA, unpublished data (1970).
39. C. R. Bell, Atomics International, personal communication (1970).
40. L. M. McWethy, P. R. Pluta, and D. B. Sherer, Editors, *Core Design Development Needs in Relation to Fuel Failure Propagation, Sodium Boiling and Clad/Fuel-sodium Thermal Interaction*, GEAP-13639-2 (Oct 1970).
41. T. R. Johnson, J. R. Pavlik, and L. Baker, Jr., *Postaccident Heat Removal: Large-scale Molten-fuel-Sodium Interaction Experiments*, ANL-75-12 (Feb 1975).
42. M. H. Fontana et al., *Effect of Inlet Blockages in 19-rod Simulated LMFBR Fuel Subassemblies*, Trans. Am. Nucl. Soc. 17, 345 (Nov 1973).
43. M. H. Fontana et al., "Effect of Partial Blockages in Simulated LMFBR Fuel Subassemblies," *Proc. Fast Reactor Safety Meeting, Beverly Hills, California, April 2-4, 1974*, p. 1139.
44. J. B. van Erp and T. C. Chawla, *Experimental Study of Coolant Cross-flow and Subchannel Flow Blockage in a Simulated LMFBR Subassembly*, Trans. Am. Nucl. Soc. 14(2), 748 (Oct 1971).
45. D. Kirsch and K. Schleisiek, *Flow and Temperature Distributions around Local Coolant Blockages in Sodium-cooled Fuel Elements*, presented at International Heat Transfer Seminar, Trogir, Yugoslavia (Sept 1971).
46. P. Basmer, D. Kirsch, and G. F. Schultheisz, *Phänomenologische Untersuchungen der Strömungsverteilung hinter lokalen Kühlkanalblockaden in Stabbündeln*, KFK 1548 (Jan 1972).
47. K. Gast and K. Schleisiek, "Local Boiling in a Partially Blocked LMFBR Subassembly," *Proc. Int. Conf. Engineering of Fast Reactors for Safe and Reliable Operation, Karlsruhe, W. Germany, October 9-13, 1972*, pp. 730-741.
48. K. Schleisiek, *Experimental Investigation of Sodium Boiling Processes Caused by Local Blockages*, Trans. Am. Nucl. Soc. 15(2), 838 (Nov 1972).
49. D. Kirsch, *Untersuchungen zur Strömungs- und Temperatur-Verteilung im Bereich lokaler Kühlkanalblockaden in Stabbündel-Brennelementen*, KFK 1794 (May 1973).
50. D. Kirsch, *The Temperature Distribution in the Recirculation Flow Downstream of Local Coolant Blockages in Rod Bundle Subassemblies*, presented at the International Meeting on Reactor Heat Transfer, Karlsruhe, W. Germany (Oct 1973).
51. J. Graham, A. A. Bishop, and J. A. Zoubek, *Hydrodynamic Characteristics of a Wake Behind a Fuel Assembly Local Flow Blockage*, Trans. Am. Nucl. Soc. 14(2), 748-749 (Oct 1971).
52. R. W. Keaten, *Evaluation of Thermocouples for Blockage Detection in LMFBR Fuel Assemblies*, Trans. Am. Nucl. Soc. 13(1), 362-363 (June 1970).
53. W. W. Marr, COBRA-3M: *A Digital Computer Code for Analyzing Thermal-hydraulic Behavior in Pin Bundles*, ANL-8131 (Mar 1975).
54. H. K. Fauske, ANL, personal communication (Aug 1974).

55. T. G. Theofanous, L. Biasi, H. S. Isbin, and H. K. Fauske, *A Theoretical Study on Bubble Growth in Constant and Time-dependent Pressure Fields*, J. Chem. Eng. Sci. 24, 885-897 (1969).
56. H. K. Fauske and M. A. Grolmes, *Analysis of Transient Forced Convection Liquid-metal Boiling in Narrow Tubes*, Chem. Eng. Prog. Symp. Series 67, 64-71 (1971).
57. H. K. Fauske, "Transient Liquid-metal Boiling and Two-phase Flow," *Proc. Int. Seminar on Heat Transfer in Liquid Metals, Trogir, Yugoslavia* (Sept 1971).
58. R. E. Henry, *A Correlation for the Minimum Wall Superheat in Film Boiling*, Trans. Am. Nucl. Soc. 15(1), 420 (June 1972).
59. K. Gast, *Prevention of Local Fault Propagation in the LMFBR*, Trans. Am. Nucl. Soc. 15(2), 838 (1972).
60. R. M. Crawford, W. W. Marr, A. Padilla, Jr., and P. Y. Wang, *Studies on LMFBR Subassembly Boundary Integrity*, ANL-75-27 (May 1975).
61. W. W. Marr et al., *Analytical Investigation of Certain Aspects of LMFBR Subassembly Failure Propagation*, ANL-76-19 (Feb 1976).
62. G. L. O'Neill et al., *Experimental Studies of Sodium Logging in Fast Ceramic Reactor Fuels*, GEAP-4283 (Sept 1963).
63. G. B. Zwetzig, *Survey of Fission and Corrosion Product Activity in Sodium or NaK Cooled Reactors*, AI-AEC-Memo-12790 (Feb 1969).
64. E. G. Stevens et al., *FFTF Fuel Pin and Subassembly Conceptual Design Methods and Data*, BNWL-1664 (June 1970).
65. L. M. McWethy, *Fuel Failure Predictions and Consequences*, GEAP-13953 (July 1973).
66. E. A. Aitken et al., "Reactions of Sodium with Mixed Oxide Fuel," *Proc. Conf. Fast Reactor Fuel Element Technology*, ANS, edited by Ruth Farnakes, New Orleans (Apr 1971).
67. M. Hori and A. Hosler, *A Study of Gas Entrainment and Absorption--Desorption in the Enrico Fermi Reactor and Their Effect on Core Performance*, APDA-LA-6 (July 1969).
68. C. J. Baroczy, *Sodifaze--A Steady-state Multi-channel Boiling Sodium Code*, AI-AEC-12804 (Feb 1970).
69. H. Johnson and A. Abou-Sabe, Trans. ASME 74, 977-987 (Aug 1952).
70. M. Petrick and K. Y. Lee, *Performance Characteristics of a Liquid Metal MHD Generator*, ANL-6870 (July 1964).
71. M. Jakob, *Heat Transfer*, Vol. 1, John Wiley & Sons, New York, pp. 83ff, (1949).
72. R. Anderson and T. Russell, Chem. Eng. 72, 99-104 (Dec 20, 1965).
73. P. Griffith and G. Wallis, Trans. ASME C83, 307 (1961).
74. G. W. Govier et al., Can. J. Chem. Eng. 35, 58 (1957).
75. A. M. Judd, T. R. Burns, and W. W. Marr, ANL, personal communication (Dec 1970).

76. W. W. Marr and M. M. El-Wakil, *Hybrid Computer Solution of Thermal Energy Equations in Porous Fuels*, Nucl. Sci. Eng. 41, 271-280 (1970).
77. W. U. Choudhury and M. M. El-Wakil, "Heat Transfer and Flow Characteristics in Conductive Porous Media with Energy Generation," *Proc. Fourth Int. Heat Transfer Conf., Versailles, France* (Aug 1970).
78. R. Byron Bird et al., *Transport Phenomena*, John Wiley & Sons, Inc., New York, N.Y. pp. 196-200 (1965).
79. M. M. El-Wakil, *Nuclear Heat Transport*, International Textbook Co., p. 268 (1971).
80. W. W. Marr, *Hybrid Computer Solution of Energy Equations in Conductive Porous Media*, Ph.D. thesis, The University of Wisconsin (1969).
81. D. F. Haasl, *Advanced Concepts in Fault Tree Analysis*, paper presented at System Safety Symposium, Seattle, Washington (June 8-9, 1965).
82. J. W. Hagan, L. W. McWethy, and C. S. Thomas, *Safety Assurance Summary for the FFTF*, HEDL-TME-75-76 (Dec 1975).
83. D. H. Thompson and W. W. Marr, *Prediction of Maximum Temperature or Melting Fraction in a LMFBR Fuel Element*, Nucl. Eng. Des. 17, 361-370 (1971).
84. R. J. Jackson, HEDL, personal communication (Mar 19, 1973).
85. D. S. Dutt, R. B. Baker, and R. J. Jackson, HEDL, unpublished data (Jan 1973).
86. R. E. Peterson, HEDL, personal communication (May 17, 1973).
87. R. G. Palm, L. W. Deitrich, W. F. Murphy, and G. S. Stanford, *TREAT Mark-II-Loop Tests D1 and D2 on Locally Overenriched Fuel Pins*, ANL-8100 (May 1974).
88. M. Epstein, *Heat Conduction in the UO₂-cladding Composite Body with Simultaneous Solidification and Melting*, Nucl. Sci. Eng. 51(1), 84-87 (May 1973).