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TECHNICAL BASES - FFTF FUEL/CLADDING DAMAGE CRITERIA

By

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February 1970

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TABLE OF CONTENTS

I.	INTRODUCTION	I.1
	A. Damage Criteria for the FFTF Fuel Pin	I.1
	B. Fuel Pin Parameter Limits	I.5
	C. Application of Fuel Pin Damage Criteria	I.6
II.	SUMMARY AND CONCLUSIONS	II.1
III.	STEADY STATE FAST FLUX IRRADIATIONS	III.1
	A. Available Data	III.1
	B. Expected Mode of Failure	III.5
	C. Predictions of FTR Steady State Performance	III.7
IV.	EXPERIMENTAL TESTS DURING ABNORMAL FUEL OPERATION	IV.1
	Unirradiated Fuel	IV.2
	Irradiated Fuel	IV.4
V.	ANALYTICAL PREDICTIONS OF FTR FUEL PIN BEHAVIOR UNDER OFF-NORMAL CONDITIONS	V.1
	A. Hypothesized Failure Mechanisms	V.1
	B. Criteria for Cladding Failure	V.2
	C. Accident Identification for Transient Conditions	V.5
	D. Failure Predictions for Various Assumed Off-Normal Conditions	V.8
VI.	IMPACT OF THE PRESENT ANALYSES ON FUEL CLADDING DAMAGE CRITERIA	VI.1
	A. Normal Operating Zone	VI.1
	B. Operational Incident	VI.1
	C. Minor Accident	VI.3
	D. Major Accident	VI.4
	E. Summary	VI.4
VII.	MAJOR UNCERTAINTIES IN PREDICTING FUEL FAILURE	VII.1
VIII.	PROJECTED CONTINUATION OF THESE STUDIES	VIII.1
IX.	REFERENCES	IX.1
APPENDIX I.	Predictions of Deformations in FTR Fuel Pins Under Steady State Operation	AI.1
APPENDIX II.	Cladding Failure from Fission Gas Within Pellet During Uninterrupted Overpower Transient	AII.1

APPENDIX III.	Sodium Boiling Point Within Core	AIII.1
APPENDIX IV.	FTR Cladding Stress and Deformation Calculations	AIV.1
APPENDIX V.	Stress Intensity Factors	AV.1
APPENDIX VI.	Burst Strength of EBR-II Irradiated Fuel Pin Sections	AVI.1
APPENDIX VII.	FFTF Mixed Oxide Fuel Development and Evaluation	AVII.1
APPENDIX VIII.	Material Properties Section	AVIII.1
APPENDIX IX.	Effects of Reactor Transients on Cladding Performance	AIX.1

LIST OF FIGURES

III.1	Summary of Fission Gas Release Data from Mixed Oxide Fuel Pins	III.4
III.2	Projected Fuel Pin Swelling Versus Burnup for EBR-II Specimens	III.6
V.1	Relative Reactor Power Versus Time Following a 5\$/second Transient	V.9
V.2	Fraction of Fuel Molten After a 5\$/second Transient	V.10
V.3	Inner Cladding Temperature Versus Time After a 5\$/second Transient at the Top of the Fuel Column	V.11
V.4	Maximum Cladding Temperature (ID) as a Function of Percent of Fuel Melted During 5\$/second Transient and 830 lb/hour Coolant Flowrate	V.12
V.5	Internal Gas Pressure After 5\$/second Uninterrupted Transient	V.14
V.6	Variation in Failure Time Along the Fuel Column for All Plastic Cladding	V.16
V.7	Predicted Time for Failure from Fuel Melting Following a 5\$/second Uninterrupted Transient	V.18
V.8	Cladding Strain from Fuel Melting After Internal Voidage is Consumed - 5\$/second Transient	V.20
V.9	Average Cladding Temperature Versus Time After a Transient of 5\$/second (19 inches from inlet)	V.21
V.10	Average Fuel Temperature Versus Time After Uninterrupted 5\$/second Transient (19 inches from inlet)	V.22
V.11	Predicted Cladding Strain from Thermal Expansion After 5\$/second Uninterrupted Transient	V.23
V.12	Reactor Power (MW) Versus Time After a 6¢/second Power Excursion with a 10% Coolant Flow	V.26

V.13	Fuel and Cladding Temperature Versus Time After 6¢/second Uninterrupted Transient with 10% Flow	V.27
V.14	Increase in Plenum Pressure with a 6¢/second Transient with 10% Coolant Flow	V.28
V.15	Flow Coastdown - Normalized Flowrate Versus Time	V.31
V.16	Cladding Temperature History - Flow Coastdown and Pipe Rupture	V.33
V.17	Internal Pressure History with 100% Gas Release to Plenum	V.34
V.18	Pipe Rupture Accident - Normalized Flowrate Versus Time	V.36
VI.1	Oxide Fuel Damage Criteria for the FFTF Accident Severity Levels	VI.2
VII.1	Uncertainty in Maximum Cladding ID Temperature of FTR Maximum Pin Undergoing a 5\$/sec Transient	VII.2
VII.2	Uncertainty in Maximum Fuel Temperature of FTR Max-Pin-Undergoing a 5\$/sec Transient	VII.4
VII.3	Initial Gap Conductance Versus Cold Gap of Rod with Fuel Pellet 0.213 Diam and Outside Coolant Temperature = 1000 °F	VII.5
AI.1	Unirradiated Type 316 SS Thermal Expansion, Mean from RT (70 °F)	AI.7
AI.2	Radial Temperature Distribution in Typical FTR Fuel Pins (Axial Midpoint)	AI.11
AI.3	Axial Temperature Profile in an FTR Fuel Pin Operating at 4.8 kW/ft	AI.12
AI.4	Axial Temperature Profile in an FTR Fuel Pin Operating at 8.7 kW/ft	AI.13
AI.5	Axial Temperature Profile in an FTR Fuel Pin Operating at 12.2 kW/ft	AI.14
AI.6	Axial Temperature Profile in an FTR Fuel Pin Operating at 14.6 kW/ft	AI.15

AI.7	Axial Temperature Profile of the Average Cladding Temperature for FTR Fuel Pins	AI.16
AI.8	Hybrid Computer Simulation of the Expected Structure in an FTR Fuel Pin Operating at 8.7 kW/ft	AI.17
AI.9	Hybrid Computer Simulation of the Expected Structure in an FTR Fuel Pin Operating at 12.2 kW/ft	AI.18
AI.10	Hybrid Computer Simulation of the Expected Structure in an FTR Fuel Pin Operating at 14.6 kW/ft	AI.19
AI.11	FTR Axial Cladding Swelling Profile Min. Power Pin 4.8 kW/ft	AI.21
AI.12	FTR Axial Cladding Swelling Profile Avg. Power Pin 8.7 kW/ft	AI.22
AI.13	FTR Axial Cladding Swelling Profile Max. Power Pin 12.2 kW/ft	AI.23
AI.14	FTR Axial Cladding Swelling Profile 20 PCT Overpower Pin 14.6 kW/ft	AI.24
AI.15	Radial Expansion of Cladding Due to Swelling and Thermal Expansion	AI.25
AI.16	Radial Expansion of Cladding Due to Swelling and Thermal Expansion	AI.26
AI.17	Radial Expansion of Cladding (mils) Due to Swelling and Thermal Expansion	AI.27
AI.18	Radial Expansion of Cladding (mils) Due to Swelling and Thermal Expansion	AI.28
AI.19	Predicted Volumetric Swelling of Mixed Oxide Fuel	AI.31
AI.20	Predicted $\Delta V/V$ Isotropic Versus Length at 20,000 MWd/MT Peak for 4 Peak Linear Powers	AI.32
AI.21	Predicted $\Delta V/V$ Isotropic Versus Length at 40,000 MWd/MT Peak Burnup for 3 Linear Power Levels	AI.33
AI.22	Predicted Fuel $\Delta V/V$ Isotropic Versus Length at 60,000 MWd/MTM Peak Burnup for 3 Linear Powers Shown	AI.34

AI.23	Predicted $\Delta V/V$ Isotropic of Fuel at 80,000 MWd/MTM Peak for 3 Linear Powers	AI.35
AI.24	Thermal Expansion of Fuel Versus Length for Four Linear Power Levels Shown	AI.37
AI.25	Change in Pellet Radius from Thermal Expansion Plus Fuel Swelling	AI.38
AI.26	Change in Pellet Radius from Thermal Expansion Plus Fuel Swelling	AI.38
AI.27	Change in Pellet Radius from Thermal Expansion Plus Fuel Swelling	AI.39
AI.28	Change in Pellet Radius from Thermal Expansion Plus Fuel Swelling	AI.39
AI.29	Prediction of Fuel-Cladding Interaction in an FTR Fuel Pin Operating at 4.8 kW/ft	AI.40
AI.30	Maximum Change in Diameter of 0.230 in. OD FTR Fuel Pin-Cladding Swelling + Fuel - Cladding Interaction - Deform Predictions at 4 A/O B.U. Incubation	AI.43
AI.31	Cladding Stress in an FTR Fuel Pin 80,000 MWd/MTM at 12.2 kW/ft	AI.44
AII.1	Relative Reactor Power Versus Time Following a 5\$/Second Transient	AII.2
AII.2	Cold Fuel Configuration of Reference FFTF Fuel Pin	AII.3
AII.3	Assumed Power Profile of Peak Rod	AII.4
AII.4	Steady State Axial Temperature Distribution of Fuel and Cladding and Size of Central Void During Steady State Operation of Reference Fuel Pin at 12.2 kW/ft	AII.5
AII.5	Radial Temperature Profiles Following a 5\$/Second Transient at 35 inches from Bottom of Fuel	AII.7
AII.6	Radial Temperature Profiles Following a 5\$/Second Transient at 25 inches from Bottom of Fuel Column	AII.8
AII.7	Radial Temperature Profile Following a 5\$/Second Transient at 19 inches from Bottom of Fuel	AII.9

AII.8	Radial Temperature Profile Following a 5\$/Second Transient at 13 inches from Bottom of Fuel	AII.10
AII.9	Radial Temperature Profile Following a 5\$/Second Transient at 7 inches from Bottom of Fuel	AII.11
AII.10	Radial Temperature Profile Steady State at a Position 1 inch from Bottom of Fuel	AII.12
AII.11	Fraction Columnar Versus Length and Burnup	AII.13
AII.12	Fraction Melted in Excess of Fraction Columnar Versus Length and Time	AII.16
AII.13	Internal Gas Pressure After 5\$/Second Uninterrupted Transient	AII.23
AIII.1	Boiling Point of Sodium as a Function of Pressure	AIII.2
AIII.2	Coolant Pressure Versus Core Length	AIII.3
AIII.3	Boiling Point of Sodium as a Function of Core Length	AIII.4
AIV.1	0.2% Yield Strength as a Function of Temperature for the Loss of Coolant Transient	AIV.6
AIV.2	Inner and Outer Pressures During Steady State	AIV.9
AIV.2a	Temperature Histories for Pipe Rupture and Flow Coastdown	AIV.10
AIV.3	Distributions During Pipe Rupture at 35 inches up the Fuel Column for Case 1 (See Table AIV.1)	AIV.15
AIV.4	Distributions During Flow Coastdown at 35 inches up the Fuel Column for Case 1 (See Table AIV.1)	AIV.16
AIV.4a	Internal Pressure Histories	AIV.18
AIV.5	Unirradiated and End of Life Yield Strength of Solution Treated as a Function of Temperature	AIV.24
AIV.6	Unirradiated and End of Life Stress-Strain Plot of Solution Treated at 1000 °F	AIV.24

AIV.7	Cold Work Type 316 Stainless Steel 0.2% Yield Strength as a Function of Temperature	AIV.26
AIV.8	Internal and External Pressure Histories During Steady State for Solution Treated and Cold Worked CAses	AIV.29
AIV.9	Inner Pressure History for 32.5% and 80% Fission Gas Release During a 5\$/Second Reactivity Transient	AIV.30
AIV.10	Inner and Outer Surface Temperature Histories During a 5\$/Second Reactivity Transient	AIV.30
AIV.11	Distributions During a 5\$/Second Reactivity Transient at 7 inches up Fuel Column for 32.2% Fission Gas Release and Unirradiated Type 316 Stainless Steel Solution Treated	AIV.33
AIV.12	Distributions During a 5\$/Second Reactivity Transient at 19 inches up the Fuel Column for 32.5% Fission Gas Release and Unirradiated Type 316 Stainless Steel Solution Treated	AIV.34
AIV.13	Distributions During a 5\$/Second Reactivity Transient at 27 inches up the Fuel Column for 32.5% Fission Gas Release and Unirradiated Type 316 Stainless Steel Solution Treated	AIV.35
AIV.14	Various Distributions for a 15 mil Wall Thickness	AIV.48
AIV.15	The Inner and Outer Surface Hoop Stress as a Function of History Number and Different Irradiation Effects	AIV.49
AIV.16	The History of the Cladding Temperature and Yield Stress of the Cladding for a 5\$/Second Transient	AIV.54
AIV.17	The History of the Fuel Temperature for a 5\$/Second Transient	AIV.55
AIV.18	The Time History of Pressure Between Fuel and Cladding for a 5\$/Second Transient	AIV.56
AIV.19	The Time History of Inner Surface Cladding Effective Strain for a 5\$/Second Transient	AIV.57
AV.1	Stress Intensity Factor Range ΔK , psi $\sqrt{\text{in.}}$	AV.2
AV.2	Predicted Stress Intensity Factor as a Function of Time in EBR-II	AV.4

AV.3	Predicted Rate of Crack Growth Versus Stress Intensity Factor Range Using 1 GE Data Point at 74,000 MWd/MT in EBR-II	AV.6
AV.4	Crack Length (Mils) Versus Time (Days)	AV.7
AVI.1	Axial Fluence and Temperature Profile (PNL 1-3)	AVI.3
AVI.2	Axial Fluence and Temperature Profile (PNL 1-6)	AVI.4
AVI.3	Test Apparatus	AVI.7
AVI.4	Comparison of Failure Modes in PNL-1 Fuel Pins	AVI.8
AVI.5	Comparison of Microstructures in PNL-1 Fuel Pins	AVI.10
AVI.6	Extraction Replica of Specimen from Below Fuel Midplane	AVI.11
AVI.7	Extraction Replica of Specimen from Above Fuel Midplane	AVI.12
AVI.8	Irradiated in EBR-II at 8.1 kW/ft to 7900 MWd/MTM (PNL 1-6)	AVI.13
AVI.9	Irradiated in EBR-II at 8.8 kW/ft to 8200 MWd/MTM (PNL 1-3)	AVI.14
AVI.10	Microprobe Results (PNL 1-6-F)	AVI.16
AVIII.1	Unirradiated Type 316 Stainless Steel Poisson's Ratio σ Assume $\sigma = 0.3$ for All Temperatures	AVIII.2
AVIII.2	Unirradiated Type 316 Stainless Steel Modulus of Elasticity	AVIII.3
AVIII.3	Unirradiated Type 316 Stainless Steel Creep Strength	AVIII.4
AVIII.4	Temperature Dependences Produced by Swelling Equations for Solution Treated Type 304 Stainless Steel and Type 316 Stainless Steel with 20% Cold Work	AVIII.5
AIX.1	Preliminary Recovery Curves for Type 316 Stainless Steel	AIX.2

LIST OF TABLES

II.1	Summary of Failure Time for Various Off-Normal Conditions	II.4
III.1	Summary of Results of High-Burnup Fast-Flux Irradiation of Mixed-Oxide Fuel Pins	III.2
IV.1	Results from TREAT Meltdown Experiment CEN-S2 with Stainless-Steel-Clad UO_2 -Core Fuel Rods Submerged in Sodium	IV.4
IV.2	General Electric Preirradiated Transient Tests	IV.6
V.1	Cladding Failure Criteria	V.3
V.2	Summary of Biaxial Test Data	V.4
V.3	Estimate of Cladding Failure During 5\$/sec Power Transient from Fission Gas Release to Central Void	V.15
V.4	Summary of Fuel Failure Analyses for a 5\$/sec Power Transient	V.24
V.5	Predicted Failure Times at the Top of the Fuel Column for the 6¢/second Transient	V.29
V.6	Time for Cladding Failure at the Top of the Fuel During a Flow Coastdown Accident	V.32
V.7	Time to Failure, Inner Gas Pressure and Cladding Temperature Following a Pipe Rupture Accident	V.37
V.8	Summary of Estimated Failure During a 6¢/second Power Transient and Loss of Coolant Accidents	V.38
VII.1	Uncertainty in Fuel and Cladding Temperature	VII.3
AI.I	FTR Fuel Pin Parameters	AI.2
AI.II	Power and Flux Estimates for FTR Fuel Pins	AI.4
AI.III	Pertinent Heat Transfer Parameters	AI.5
AI.IV	Burnup and Linear Power Distribution Along Pin Axis	AI.30
AII.1	Steady State Power 12.2 kW/ft Peak Fuel Density = 90.5% Time = 0.281 Seconds Transient 5\$/Second	AII.20
AII.2	Steady State Power 12.2 kW/ft Peak Fuel Density = 90.5% Time = 0.293 Seconds Transient 5\$/Second	AII.20
AII.3	Steady State Power 12.2 kW/ft Peak Fuel Density = 90.5% Time = 0.313 Seconds Transient 5\$/Second	AII.21

AII.4	Steady State Power 12.2 kW/ft Peak Fuel Density = 0.5% Time = 0.313 Seconds Transient 5\$/Second	AII.21
AII.5	Steady State Power 12.2 kW/ft Peak Fuel Density = 90.5% Time = 0.321 Seconds Transient 5\$/Second	AII.22
AIII.1	Temperature Drops Through the FFTF Core	AIII.1
AIV.1	Loss of Coolant, 35 inches up, Case (1)	AIV.11
AIV.2	Loss of Coolant, 10 mil Wall, Ye - 84,000, 35 inches up, Case (2)	AIV.13
AIV.3	The Transient Temperature Histories for the Various Axial Locations	AIV.19
AIV.4	The Transient Internal Pressure Histories	AIV.19
AIV.5	6¢/sec Power Transient with a 15 mil Wall	AIV.20
AIV.6	Steady-State Temperatures and Flux at Various Axial Locations	AIV.28
AIV.7	Temperature and Pressure Histories for a 5\$/second FTR Reactor Power Transient	AIV.31
AIV.8	Solution Treated 5\$/second Transient Case Temperature and Pressure Histories	AIV.31
AIV.9	Reactivity Transient ~5\$/second S.T. Case with a 10.0 mil Wall	AIV.32
AIV.10	5\$/sec, 0% Release, 10 mil Wall	AIV.36
AIV.11	5\$/sec, 0% Release, 15 mil Wall	AIV.38
AIV.12	5\$/sec, 50% Release, 80,000 MWd/T	AIV.39
AIV.13	5\$/sec, 80% Release, 80,000 MWd/MTM ye 70,000	AIV.41
AIV.14	5\$/sec, 80,000 MWd/MTM Yield (2), Swelling Gradients	AIV.42
AIV.15	Steady State Loading Conditions at 19.8 inches up from the Bottom of the Fuel Column	AIV.45
AIV.16	Results for Steady State	AIV.46
AV.1	Shape Factor $f \left(\frac{a}{w} \right)$ for Single Edge Crack	AV.5
AVI.1	As-Fabricated Fuel and Cladding Compositions	AVI.2
AVI.2	PNL-1 Fuel Pin Burst Test Results	AVI.5

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1. INTRODUCTION

Specification of fuel damage criteria is important in evaluating and verifying the adequacy of safety margins for the FFTF. These margins are provided to meet the established safety objectives for the facility.⁽¹⁾ The criteria define transient performance limits in terms of the various fuel parameters which are expected to be among the more critical under the particular circumstances. The parameters also provide the means of correlation between experiment and the design function.

The purpose of this report is to review the parameter limits specified as preliminary design data in the FFTF design safety criteria,⁽²⁾ and to identify the bases for each of these limit values. These bases provide a logical framework for discussion of the uncertainties involved as well as emphasize the more promising options for maximizing the return on investment of development funds.

A. Damage Criteria for the FFTF Fuel Pin

The damage criteria for the FFTF first core fuel pins have been developed with due regard to the role of the cladding as an effective fission product barrier. The safety philosophy adopted for the FFTF emphasizes protection of the public through multiple levels of defense against accidental release of radioactive

material. The fuel pin cladding serves as the innermost barrier against excessive release of the radioactive fission products. Therefore, the damage criteria emphasize maintaining the integrity of this inner barrier not only over the broad spectrum of operating and off-normal conditions which might be expected over the lifetime of the reactor, but also with sufficient margin to cover those conditions considered to be of an unlikely nature.

This emphasis on the integrity of this inner barrier is not to assert that no fuel pins out of approximately 15,000 in the normal core loading will allow some leakage of fission products into the primary heat transport system. The primary system will be designed for normal operation even with one percent of the fuel pins leaking fission gasses into the coolant. However, it does emphasize that the fuel pins be designed and verified not to fail simultaneously through common or systematic failure paths or by pin-to-pin failure propagation. This, in turn, emphasizes that sufficient margin be provided between the specified safety limit and the point at which violent fuel pin failure might be expected.

The fuel pin damage criteria define the peak allowable severity for the abnormal condition corresponding to the accident categories as specified in Criteria for Accident Severity Levels - FFTF Reactor Systems.⁽²⁾

These categories are identified as:

- Operational Incident
- Minor Accident
- Major Accident
- Disruptive Accident

The damage criteria specify that an Operational Incident will result in *no significant reduction in burnup capability*. Therefore, all fuel pins not directly affected by the fault initiating the Operational Incident shall not experience any transient conditions which would prevent continued at-power performance to its goal exposure. It is expected that several Operational Incidents may be experienced while individual fuel pin assemblies are in the reactor; therefore, this criterion emphasizes that no loss of capability to reach that goal exposure applies for multiple short-term transient conditions of this magnitude.

The damage criteria specify that for a Minor Accident the consequences shall be limited as follows:

No gross loss of fuel pin integrity, but some reduced burnup capability. Fission gases and fissionable material retained within the cladding, although a limited number of pins within the safety limit may be breached allowing some nonviolent release of fission gases. Some reduction in transient capability.

Several accidents of this severity might be expected during the lifetime of the FTR and therefore any one fuel loading may experience a transient corresponding to a Minor Accident. It is an objective of the FFTF fuels development program to show that operation may continue with the fuel load even following a Minor Accident without undue risk that any subsequent abnormal condition might cause the fuel to fail prematurely or more violently as a result of preconditioning attributed to the earlier transient. Until it is verified by sufficient tests and experience that such preconditioning does not occur, it may be

necessary to remove any fuel which has experienced temperatures in the melting region and thereby avoid any possibility of preconditioning but at additional operating expense.

The damage criteria specify that for a Major Accident, the consequences shall be limited as follows:

Cladding failure may occur. Any release of fission gas and essentially solid fuel material from ruptured fuel pins is nonviolent. Overall core geometry is not disrupted, but total loss of core tests and test data. Incapable of sustaining additional transient conditions.

Accidents of this severity are not expected during the lifetime of the FTR. However, if such an accident does occur, then the objective is to show that the consequences can be mitigated below the threshold of core disruption and that the second fission product barrier, the primary system, is maintained intact. Resumption of the operation would require extensive core replacement.

The damage criteria specify that for a Disruptive Accident, the consequences shall be limited as follows:

Violent releases of fuel material contribute to destruction of the core. Possible core compaction and molten fuel-sodium interactions with resulting energy release less than the inner containment DBA.

The objective under such extreme and hypothetical conditions is to assure the integrity of the engineered safeguards features and to mitigate the release of radioactive material to the environs.

B. Fuel Pin Parameter Limits

Specification of fuel pin parameter limits corresponding to these damage criteria requires a sound understanding of fuel response under the imposed transient conditions. This necessarily requires good analytical techniques for prediction of performance based upon experimental data obtained under as near prototypic conditions as possible to limit the number of variables which might influence interpretation of the results. Present values assigned to these parameter limits as specified and discussed in this report have been derived from extensive analyses using models justified by qualitative evaluation of the available experimental data.

In deriving these values, it is apparent that the fuel pin response is considerably different for overpower conditions resulting from some reactivity insertion, than for cladding overtemperature conditions resulting from some loss-of-flow initiator (e.g., pump coastdown, flow blockage, pipe rupture). For overpower conditions, fuel damage is postulated to be quite dependent upon the percent of areal fuel melting across any fuel pin section. For overtemperature conditions, the peak temperature of the stainless steel cladding is postulated to be the prime parameter related to possible failure of that cladding by its reduced strength to contain the normal fission gas pressure. Both parameters - areal melting and cladding temperature - depend upon analysis of the relationships to operating conditions and spatial and temporal evaluations of the power or flow transient.

The fuel pin parameter limits corresponding to the boundaries separating the various accident categories represent upper

limits on each category rather than threshold values for the next higher category. For example, the values between the Minor Accident and Major Accident categories is chosen to assure that the damage criteria for a Minor Accident is not exceeded for conditions up to the parameter value. It is recognized that grey areas separate the accident categories and choosing limit values corresponds to the adaptation of conservatism in dealing with safety aspects of reactors.

C. Application of Fuel Pin Damage Criteria

The fuel pin damage criteria have application in (a) design of the reactor and protective systems, (b) establishment of the bases for performance of fuel pin transient experiments, and (c) providing a base for safety reviews and evaluation.

Design Considerations

Transient analyses to determine the required system responses to abnormal conditions depend upon the normal safety margins provided between the normal operating band and the limit parameter values for the appropriate accident category. The smaller the margin, the greater is the responsibility placed upon rapid detection and corrective response by the plant protective system. In addition, smaller margins place greater design responsibility upon inherent corrective or mitigative features. Therefore, the fuel pin parameter limits have a direct effect on design of the reactor systems, plant protective system, and heat transport systems.

Fuel Pin Development Program Considerations

The specified fuel pin parameter limits form the basis for performance of the FFTF transient fuel pin test program to confirm that the specified values do meet the damage criteria and are, thus, conservative. The TREAT experiments described in the appendix will simulate fuel melting and cladding temperatures corresponding to the limits for minor accidents and major accidents to assure that the expected damage is not exceeded.

Safety Reviews and Evaluation

Since the parameter limits form the bases for design and evaluation of the reactor response to abnormal conditions, verification that these values are conservative will be of major importance in safety reviews and evaluation of safety margins.

II SUMMARY AND CONCLUSIONS

The failure threshold for an FTR fuel pin subjected to transient conditions was considered for four specific accidents.

- A 5\$/second unterminated power excursion with the reactor initially at full power. This is about twice the ramp rate envisioned should a subassembly melt and compact to the center of the core.
- A 6¢/second unterminated power excursion at reactor startup with 10% coolant flow. Ramp rates of this magnitude are typical of those anticipated due to control rod withdrawal caused by operator error or control system malfunction.
- A flow coastdown accident with the reactor at full power due to the loss of electrical power to the coolant pumps.
- A double ended pipe rupture with the reactor at full power. Flow would be lost temporarily as the stand-pipe surrounding the break is filled but then shut-down flow would be re-established.

The fuel considered was 75 UO_2 -25 PuO_2 solid sintered pellets of 0.194 in. in diameter and 90.5% theoretical density. This fuel was clad with 20% cold worked type 316 stainless steel 0.230 in. OD x 0.015 in. wall. The original cold pellet-cladding diametral gap was 0.006 in. yielding a local smeared density of about 85.5% of theoretical. No benefit, if any exists, was taken for the dished end pellet configuration.

The transient behavior of the fuel pin was analyzed under conditions corresponding to each of the above accident modes. The specific accident determines the fuel pin parameters as a function of time. These parameters are in turn

compared to the previously established FTR fuel/cladding damage criteria once a failure criterion (such as excessive strain on the cladding) is reached. This comparison then determines whether the established damage criteria are adequate over the broad range of potential accident conditions.

The analyses of fuel-pin behavior were based on one-dimensional, plane-strain-calculations for loads, temperatures and material properties that vary with time (and temperature) over the life of the accident. Cladding failure criteria were established and the times and conditions to satisfy these criteria were determined for several cladding failure mechanisms.

The cladding failure criteria were:

- the non-elastic effective strain (ϵ_{eff}^n) exceeds 0.2%
- the yield strength is exceeded at any point in the cladding
- the yield strength is exceeded at all points in the cladding
- the stress on the inner surface of the cladding becomes tensile

The primary failure mechanisms considered were:

- coolant boiling followed by vapor blanketing of the pins and subsequent cladding melting
- increasing the internal gas pressure by heating the plenum gas
- releasing fission gas to the central void as fuel melting beyond the columnar grain zone occurs
- increasing the fuel volume due to melting assuming that the gas in the central void prevents the molten fuel from flowing into the void.

• differential thermal expansion between the fuel and the cladding assuming that the as-fabricated gap has been consumed, i.e. zero gap when the transient is initiated.

The results of the analyses are summarized in Table II-1. They indicate that the four cases can be considered as two broad categories based on the predicted failure mechanisms. The 6¢/second transient, the flow coastdown accident and the pipe rupture accident constitute one category and the 5\$/second transient constitutes the second category.

In the first category, the rising cladding temperature was the controlling factor in establishing the failure threshold regardless of the assumption made about the condition of the fuel at the start of the accident (zero burnup and 80,000 MWd/MTM with 100% steady state fission gas release failed at very nearly the same time) or the cladding failure criteria selected. Cladding temperature was controlling because the yield strength-temperature relationship employed in these analyses approached zero above about 1700 °F. No failure from any cause (in this category) was predicted below cladding temperatures of 1600 °F. Thus, there was an interplay between the increasing plenum pressure and the decreasing cladding strength due to the temperature rise. For any of the three given accident cases the rate of cladding temperature rise was rapid enough so that the fuel pin cladding failure predicted by the various criteria occurred at very small differences in time. Significantly, however, cladding failure was always predicted before the onset of coolant boiling.

The second category involving the 5\$/second power transient was more difficult to analyze due to the fact that three failure mechanisms for fuel pin cladding failure had to be considered: 1) fission gas released to the central

TABLE II.1. Summary of Failure Time for Various Off-Normal Conditions

CATEGORY ONE- (Cladding Temperature Limit)									CATEGORY TWO (Extent of Fuel Melting Limit)					
Flow Coastdown			Pipe Rupture			6t/sec - 10% Flow			5t/sec Power Transient					
Cladding Damage Criteria:			Cladding Damage Criteria:			Cladding Damage Criteria:			Fuel Damage Criteria:					
Clad Temp, °F	Time, sec	Yield, ksi	Clad Temp, °F	Time, sec	Yield, ksi	Clad Temp, °F	Time, sec	Yield, ksi	Area Melt	Time, sec	Clad Temp, °F			
1250 ⁽¹⁾	2.50	50	1250 ⁽¹⁾	0.30	50	1250 ⁽¹⁾	19.4	50	0% ⁽¹⁾	0.220	1085			
1350 ⁽²⁾	3.15	36	1350 ⁽²⁾	0.40	36	1350 ⁽²⁾	19.8	36	20% ⁽²⁾	0.252	1120			
1600 ⁽³⁾	4.35	12	1600 ⁽³⁾	1.10	12	1600 ⁽³⁾	21.4	12	50% ⁽³⁾	0.290	1190			
									70%	0.306	1220			
Failure Predictions:			Failure Predictions:			Failure Predictions:			Failure Predictions:			No Elastic Strain		
Clad Temp, °F	Time, sec	Cladding Condition	Clad Temp, °F	Time, sec	Cladding Condition	Clad Temp, °F	Time, sec	Cladding Condition	Failure Mode	Melt, %	Time, sec	Clad Temp, °F	Melt, %	Time, sec
1626	4.39	One point plastic	1636	1.16	One point plastic	1694	21.84	One point plastic	Gas to Void	>50	0.302	1210	-	-
1658	4.52	All plastic	1657	1.22	All plastic	1705	21.90	All plastic	$\epsilon = 0.2\%$	-	-	-	-	-
1663	4.55	$\epsilon = 0.2\%$	1665	1.24	$\epsilon = 0.2\%$	1706	21.91	$\epsilon = 0.2\%$	Fuel Melting	-	-	-	>70	0.310
									$\epsilon = 0.2\%$	-	-	-	-	-
									Thermal Interference	-	-	-	-	-
									$\epsilon = 0.2\%$	0	0.185	1055	0	0.152 1048
									$\epsilon = 0.5\%$	0	0.220	1085	0	0.204 1070
									$\epsilon = 1.0\%$	20	0.250	1120	15	0.242 1110

- (1) Upper Limit for an operational incident (suggested)
 (2) Upper limit for a minor accident (suggested)
 (3) Upper limit for a major accident

void as a consequence of fuel melting, 2) volume expansion due to fuel melting and 3) differential thermal expansion between fuel and cladding. Once the accident was initiated, the steady state fission gas pressure in the fuel pin plenum was found to be unimportant. Again, coolant boiling was not encountered prior to failure of the fuel pin cladding. The geometric state of the fuel at the start of the accident dominates the considerations for failure in this category. Since only little is known about the fuel under a transient of this magnitude, many assumptions were required. Hence, much of this analysis can be significantly influenced by the assumptions that were made. It is judged that the assumptions are conservative but not unrealistic. Most of the analysis involved the release of fission gas during melting as TREAT tests ⁽¹⁶⁾ had indicated that this was the primary mechanism for cladding failure for fuel that had been irradiated to an appreciable burnup prior to the transient. A detailed stress analysis was conducted (see Appendix IV) and the times for failure determined for the various cladding failure criteria. Cladding failure was not predicted prior to reaching 50% molten fuel but in each case failure was predicted to occur very shortly thereafter. On the basis of time it is difficult to distinguish between failure criteria. The significant point to be made is that cladding failure was not predicted below 50% melting.

The consideration of volume change from fuel melting yield about the same results as fission gas release during the 5\$/second transient. That is, failure is not predicted prior to 50% melting but may occur shortly thereafter.

The treatment for differential thermal expansion as a failure mechanism was factored into this study during its late stages. Unfortunately, the results indicate that

this is a more limiting case during the 5\$/second power ramp than any of the other failure mechanisms. While it is felt that some of the assumptions may be unrealistic sufficient time was not available to analyze this case in depth. A "hard ball" model was used for the initial engineering evaluation of the effects of differential thermal expansion. When this indicated a problem, a preliminary mechanics calculation was performed in which the elastic properties of the fuel and cladding were considered. As can be seen in Table II.1, 0.2% strain in the cladding is reached in about 0.185 seconds which is prior to the time that fuel melting is estimated to occur.

Since stress in the cladding from differential thermal expansion is a "secondary" (see Appendix IV) rather than a primary load, 0.2% cladding strain is not considered a reasonable failure criteria under those conditions. While the cladding may undergo strain due to differential thermal expansion, no pressure remains on the cladding to cause additional strain from this source. There are little or no data to indicate what a reasonable strain from this cause might be but 1% is thought to be conservative. It is estimated that 1% strain would not be reached until about 20% fuel melting occurs. Obviously, additional analyses are required on differential thermal expansion effects.

While this report was being published two recent reports which bear to a large extent on the problems studied herein were received at BNW:

- 1) R. R. Stewart, C. E. Dickerman, L. E. Robinson, and W. B. Doe, Studies of Fast Reactor Fuel Element Behavior Under Transient Heating to Failure, III, In Pile Experiments on Irradiated UO₂ Fuel Pins in The Absence of Coolant, ANL-7552, March 1969.*

* Distribution made in February 1970.

- 2) K. Hikido and J. H. Field, Molten Fuel Movement In Transient Overpower Tests of Irradiated Oxide Fuel, GEAP-13543, September 1969.* (Official Use Only).

Review of the work by Stewart et al., indicates that this work appears to be applicable to the loss-of-coolant accident analyses since, in general, the cladding was subjected to severe temperature excursions while the fuel remained in essentially the non-molten state. It is significant to note that cladding failure of these irradiated pins occurred only when the melting point of the cladding was exceeded. It was noted that no evidence of fuel motion as a result of fission gas release was observed in these tests. Since the fuel was not melted to any appreciable extent this is reasonable because melting had probably not extended into the region of the fuel which contained previously unreleased fission gases.

The report by Hikido and Field presents the results of experiments which are applicable to the power excursion accident analyses. This accident is characterized by relatively low cladding temperatures and the formation of molten fuel. Based on the results of these experiments it cannot unambiguously be stated that differential thermal expansion of the fuel and cladding was not a principal cladding failure mechanism. However, it must be borne in mind that the molten fuel boundary (35%) in each case extended well into the region of the fuel which still contained fission gases and, as demonstrated by this C test, the sudden release of these gases due to fuel melting appeared to be the ultimate cladding failure mechanism. Most significantly, these test results show that the differential thermal expansion of the fuel and cladding and the release of fission

* Distribution made in February 1970.

gases during an accident can be accommodated by proper fuel pin design. In the case of the Hikido and Field Tests, a hollow top reflector and hollow top insulator pellets provided a path for the ejection of molten fuel into the plenum. An additional significant benefit from this design would be the large negative reactivity feedback that occurs when the fuel moves in this fashion.*

The work described in both of these reports will be intensively reviewed in the near future using methods described in Appendix IV.

In conclusion:

- no fuel pin cladding failure criteria were exceeded from the loss of coolant and 6¢/second uninterminated power ramp accidents prior to the cladding temperature exceeding 1600 °F
- no fuel pin cladding failure criteria were exceeded from an uninterminated 5\$/second power excursion prior to exceeding 20% molten fuel
- onset of coolant boiling was not predicted until after fuel pin cladding failure was predicted from one of the above causes, hence, coolant boiling was not a fuel pin failure mechanism considered in these analyses
- these analyses show that short term cladding temperature transients up to 1250 °F maximum temperature should not be detrimental to the life of the fuel pin hence, the upper cladding temperature limit for an operational incident can be increased to 1250 °F.
- it is suggested that the upper cladding temperature limit for the minor accident should be increased to 1350 °F.

* Based on this preliminary study and the experimental work cited, a modification of the FTR driver fuel pin design to incorporate hollow top insulation pellets and reflectors appears an appropriate way to extend the cladding failure threshold for the power excursion accident.

III. STEADY STATE FAST FLUX IRRADIATIONS

A. AVAILABLE DATA

There are three primary sources of information that pertain to the performance of mixed oxide fuels under fast reactor irradiation conditions:

- Capsules irradiated in EBR-II to about 60,000 MWd/MTM
- Bare pins irradiated in DFR (U.K. Dounreay Fast Reactor) in 77 Pin subassemblies to about 70,000 MWd/MTM
- Driver fuel of Rapsodie now irradiated to about 50,000 MWd/MTM

There are also a large number of bare pins under irradiation in EBR-II that have operated successfully to about 50,000 MWd/MTM but no quantitative information concerning their performance is yet available.

Table III.1⁽⁴⁾ summarizes the results of two series (DFR and EBR-II) of irradiation tests that have evaluated the irradiation behavior of stainless clad mixed oxide fuel pins at conditions similar to that expected in the FTR. Very little quantitative information is available from Rapsodie except that their performance is "similar" to that of DFR pins. The F Series of pins shown in the table are irradiated by General Electric in the EBR-II at power levels in excess of that expected in the FTR. Maximum burnup reached 6% FIMA (Fission of Initial Metal Atoms) which very roughly corresponds to 60,000 MWd/MTM. Of the nine stainless steel clad rods containing solid pellets and the two stainless steel clad Vipac rods, there were no failures. One of two pins with annular pellets failed after achieving a peak burnup of 9.1% FIMA but the failure could have been induced by the handling that occurred during out-of-pile examination made partway through the exposure of the element.

TABLE III.1 Summary of Results of High-Burnup Fast-Flux Irradiation of Mixed-Oxide Fuel Pins⁽⁴⁾

Fuel fabrication form	Specimen	Sinter density (fraction of theoretical)	Cladding	Cladding dimensions, in.		Maximum cladding temperature, °C	Fuel length, in.	Maximum heat rate		Burnup, % FIMA		$\Delta t/L$, %	$\Delta D/D$, %	Fission-gas release, %
				Outside diameter	Wall thickness			watts/g	kw/ft	Maximum	Mean			
Vipac	F-2A*	0.838	347	0.250	0.015	590	13.5	204	14.1	5.1	4.5	0.09	0.44	58
	F-2Y	0.839	316	0.250	0.015	585	13.5	199	13.8	5.0	4.4	0.04	0.40	56
	V-021	0.817	M 316L	0.230	0.015	655	3.5	210	11.6	8.1	8.1	0.34	1.7	Failed
	V-024	0.799	M 316L	0.230	0.015	700	2.5	170	9.2	7.1	7.1	0.6	1.0	Failed
	V-035	0.823	M 316L	0.230	0.015	560	19.8	175	9.7	5.2	4.5	0.05	0.35	
						625†		170	9.4	6.9	6.1	0.025	0.7	76-85†
	V-037	0.825	M 316L	0.230	0.015	555	19.7	172	9.6	6.9	6.1	0.18	0.52	
	V-039	0.826	M 316L	0.230	0.015	560	20.0	180	10.0	5.7	5.1	0.09	0.42	
						625†		177	9.9	7.5	6.6	0.12	0.76	81
	V-152	0.847	M 316L	0.230	0.015	590	19.9†	230	13.1†	5.1	4.3	0.007	0.3	
						565†		222	12.7†	7.1	6.2†	0.42	0.63	71
	V-156	0.853	M 316L	0.230	0.015	665	3.7†	182	10.5†	5.8	5.8†		0.65	80
	V-173	0.821	M 316L	0.230	0.015	595	16.4	182	10.1	6.7	6.1	0.07	0.6	
	V-233	0.833	M 316L	0.230	0.015	570	20.1	227	12.8	6.0	5.0	0.014	0.56	
	V-258	0.816	M 316L	0.325	0.025	650	18.3	224	23.3	6.0	5.1	0.04	0.85	92
Annular pellets	V-259	0.793	M 316L	0.230	0.015	435	18.2	224	12.0	6.0	5.2	0.05	0.65	78
	V-177	0.832	Nimonic 80A	0.230	0.015	650	20.7	165	9.3	2.9	2.6	-0.05	0.25	
						645†		163	9.2	5.8	5.3		0.25	
	A-151	0.724	M 316L	0.230	0.015	570	20.1	200	9.8	5.8	5.1	0.035	0.14	
						630†		191	9.3	7.7	6.8	0.046	0.4	44
Solid pellets	A-153	0.729	M 316L	0.230	0.015	560	20.1	224	11.0	7.0	5.8	-0.025	1.3	
						625†		216	10.6	9.1	7.6	0.025	1.7	Failed
	F-2A	0.940	347	0.250	0.015	615	14.2	207	16.1	5.8	5.1	0.23	0.56	49
	F-2B	0.946	316	0.250	0.015	625	14.2	210	16.4	6.0	5.3	0.18	0.68	48
	F-2C	0.944	316	0.250	0.015	630	14.2	214	16.7	6.0	5.3	0.15	0.84	50
	F-2E	0.948	Incoloy 800	0.250	0.015	630	14.2	214	16.8	6.1	5.4			Failed
	F-2F	0.926	347	0.250	0.010	630	14.2	217	16.6	6.0	5.3	0.20	0.88	50
	F-2F	0.945	347	0.250	0.015	525	14.2	127	9.9	3.6	3.2	0.20	0.52	32
	F-2G	0.967	347	0.250	0.015	630	14.2	209	16.7	6.0	5.3	0.22	0.92	47
	F-2S	0.967	347	0.250	0.030	640	14.2	219	17.5	6.3	5.5	0.10	1.72	55
	F-2U	0.870	347	0.250	0.015	620	14.2	227	16.3	5.9	5.2	0.12	0.40	63
	F-2Z	0.872	316	0.250	0.015	615	14.2	185	16.1	5.8	5.1	0.08	0.64	58

*The specimen designations refer to the original reports,^{2,8} with the EBR-II specimens being the F series and the Dounreay specimens the V series.

†Specimen was removed from the reactor for dimensional measurements and reinserted for additional burnup.

‡Possibly failed.

It is significant that the diametral expansion of these pins was generally less than 1% (with the exception of F2-S which was axially restrained) and that fission gas release was approximately 50 to 80%. The higher fission gas release than observed in thermally irradiated fuel can be explained in part by the higher fission rate and gas formation in the center of fuel pellets irradiated in a fast flux. The gas generated toward the fuel center (within the columnar grain region) has a greater probability of release due to the increased diffusion rate and pore sweeping processes that occur at fuel temperatures in excess of roughly 1700 °C. A relationship between fission gas release and burnup is shown in Figure III.1. As can be seen, there seems to be a saturation effect at ~80% fission gas release at 60,000 to 80,000 MWd/MTM.

Because of the large gas release of fast reactor irradiated fuel, the swelling of the fuel due to the sudden heating of fission gases retained in the fuel is much reduced during a transient when compared to fuel irradiated in a thermal flux. Merckx has shown^(5,6) that a large percentage of the fuel element swelling in a fast flux is caused by the swelling of the cladding rather than the fuel. This swelling is caused by the precipitation of vacancies to form voids when stainless steel is exposed to neutrons with an energy greater than 0.1 MeV.

The V series of pins irradiated at a typical fluence burnup in Dounreay and tabulated in Table III.1 suggest a possible burnup limitation of 7% FIMA. Of the twelve pins shown from this series, the two with burnup in excess of 7% FIMA burnup failed despite the fact that the fuel was 80% smear density and peak power levels were between 9 and 11 kW/ft. These pins were clad with 316L and maximum cladding temperatures were below 700 °C. The tests were performed with downward flowing coolant which increases the potential for gas blanketing and overheating of the cladding.

FIGURE III.1. SUMMARY OF FISSION GAS RELEASE DATA FROM MIXED OXIDE FUEL PINS

A possible explanation of the failure observed by the British is suggested in an analysis performed by Baily, et al.⁽⁷⁾ Although only a few of the F2 Series of pins were used in the correlation (shown in Figure III.2) the data suggests an increased fuel swelling rate after 50,000 MWd/MTM. A possible explanation for the increased rate is that the fuel swelling rate exceeds the cladding swelling rate after a burnup of 50,000 MWd/MTM. Fuel-cladding interaction then develops after this burnup so that creep rate of the cladding now increases due to the fuel pressure. More experiments are required to assess if such a hypothesis has statistical validity, for Merckx's⁽⁶⁾ analysis of the same data indicated that all of the diameter changes in the pins were due to cladding swelling.

The design burnup of the FTR is 80,000 MWd/MTM peak. The data in Table III.1 suggests that 60,000 MWd/MTM should be readily achievable and that 80,000 MWd/MTM is a possibility. The tests now in EBR-II are designed to demonstrate that FTR fuel can achieve its goal exposure without an undue number of failures.

B. EXPECTED MODE OF FAILURE

Failure of fuel pins during steady state operation, if it should occur, is expected to be caused either by degradation of the cladding by chemical attack (both on the inside and outside) or by overstressing. The location of the failure is expected to be either near the top of the pin where the cladding temperature is highest or near the axial position operating near 8 to 9 kW/ft where the fission gas inventory of the fuel is expected to be highest. Unfortunately, most of the mixed oxide fuel pins irradiated in a liquid metal environment which have failed have not obeyed the predictions. The failures in EBR-II have occurred with encapsulated pins and failure has been attributed to gas blanketing of the cladding by a bubble of fission gas that was released toward the bottom of the pin. Whether this mode of failure should occur in a flowing sodium environment is still unknown.

FIGURE III.2. PROJECTED FUEL PIN SWELLING VERSUS BURNUP FOR EBR-II SPECIMENS (6)

Recent failures of the 77-pin subassemblies in the DFR are believed to be due to gas entrainment in the down-flowing coolant. These failures were encouraging since:

- 1) The pin failures did not propagate to adjacent subassemblies
- 2) Only a few of the pins in the subassemblies failed
- 3) These failures were detectable with acoustic boiling detectors, and
- 4) The reactor operation was continued even after cladding failure was detected.

Another potential mode of steady state failure that is currently being investigated is reaction of sodium, which could find its way into the pin through weld defects or cracks in the end plugs or cladding. It is generally believed that hypostoichiometric fuel is relatively inert to sodium and thermodynamic calculations⁽⁸⁾ suggest that hypostoichiometric fuel should not become hyperstoichiometric in the presence of stainless steel cladding over the life of the fuel element. Experimentation, however, suggests that there may be a variation in oxygen content with radius during steady state operation. If sodium comes in contact with hyperstoichiometric fuel, a low density compound is formed which has been postulated to be the cause of failure in several fast flux irradiation tests. The conditions under which sodium may react with hypostoichiometric fuel (the FTR fuel has an O/M of about 1.97) are not yet defined. We have not considered these reactions in this report.

C. PREDICTIONS OF FTR STEADY STATE PERFORMANCE

Appendix I summarizes an analysis that was performed using SINTR, EXPAND, PECT-1 and DEFORM to predict the deformations expected in typical FTR fuel pins operating at 4.8, 8.7, 12.2 and 14.6 kW/ft under steady state conditions. It was concluded that cladding swelling would constitute the bulk of the diameter changes and that cladding stresses would remain elastic to

burnups of 80,000 MWd/MTM for all power conditions examined. In this analysis the unrestrained swelling of the fuel was not sufficient to close the fuel-cladding gap and cause additional cladding stress. The predicted diameter changes are shown in Figure A.I.30 as a function of burnup and power level.

Appendix I also includes detailed swelling profiles for fuel and cladding, thermal expansion of fuel and cladding, axial and radial temperatures profiles, and simulations of fuel structure made with the Hybrid computer.

IV. EXPERIMENTAL TESTS DURING ABNORMAL FUEL OPERATION

The transients resulting from the more plausible accident possibilities, such as loss of coolant, are typically hypothesized to have initial temperature or power periods of the order of 100 milliseconds. Unfortunately there is no currently operating facility in which the temperature and/or power transients hypothesized for accidents can be imposed on a (U,Pu)O₂ fuel pin operating at typical initial steady state conditions. Power transients with periods of 100 milliseconds can, however, be reasonably well simulated in the Transient Reactor Test Facility (TREAT). The major problem is the nonrepresentative radial fission distribution due to the thermal flux.

Several summaries of transient testing of prototypic fast reactor fuel have been written by Dickerman.⁽⁸⁻¹⁰⁾ His summaries include both tests performed in-reactor and out-of-reactor.

The basic approach in the TREAT meltdown program has been to separate the various phenomena such as the physical interaction of pins in clusters, release of fission gases with different heating rates, etc. This approach is based upon the philosophy that analyses of relatively simple experiments can be synthesized and accurate predictions can be made by extrapolation of the data over the variable of interest. This approach may be contrasted with "integral experiment" philosophy of mocking-up individual fuel prototypes to obtain specific information that may be useful only to a limited number of experimenters and the understanding of the causes of the behavior remain unanswered.

Experimental investigations have been performed in TREAT on the following types of fuel: EBR-II Mark 1, Fermi Core A, EBR-II thorium-uranium, SEFOR type UO₂, EBR-II gas bonded UO₂, mixed oxide, Hallam core II UC, niobium-clad unbonded UC, and EBR-II gas-bonded US. Although the performance of carbide or

nitride fuel may be of interest in the design of subsequent cores of the FTR, this discussion will be limited to the performance of gas-bonded oxide fuel in stainless steel cladding.

Variables which have been shown or are anticipated to influence the transient behavior of mixed-oxide fuel pins include transient shape and severity, fuel-pin transient temperature distribution, amount of axial restraint, heat dissipation rate, variation in temperature and power distribution axially and radially, burnup, and fuel pin defects.

Available data pertinent to the FTR Core I design include 12 transient experiments by GE Breeder Reactor Development Operation at TREAT with stainless-steel clad $(U,Pu)O_2$ fuel pins encapsulated in stagnant NaK or sodium. The GE mixed-oxide program has concentrated on transients with initial reactor periods in the range of 100 to 300 milliseconds. In a typical experiment the TREAT reactor is pulsed from minimal power to perhaps 200 MW to give a multiple order of magnitude power pulse to the fuel pin in 3 to 4 seconds. To eliminate the higher thermal shock than would be expected in a fast reactor transient the pins are preheated to 400 to 500 °C.

UNIRRADIATED FUEL

GE Fast Ceramic Reactor Development Program (Task C)

It was inferred by GE early in their testing program that short columns of axially unrestrained unirradiated UO_2 fail by conditions which cause cladding melting.⁽¹²⁻¹⁴⁾ The appearance of the specimen indicates that considerable axial fuel movement occurs when a significant fraction of the fuel is taken to the melting point.

ANL Violent Boiling Program

Recent tests by Barghusen and Armstrong at Argonne⁽¹⁵⁾ also indicate the integrated power and peak power required to cause failure of a fuel pin similar to the FTR design. The

0.25 inch OD rods were 12 inches long, clad with 15 mil 304 stainless steel and contained 10% enriched solid pellet UO_2 bonded with helium at 15 psia. The experiment involved a series of transients at energies of 312, 481, and 495 MW-sec, corresponding to an average fission energy of 214, 330, and 340 cal/g UO_2 . Five fuel rods and four gas filled dummy rods made up a test assembly. (Results of the three tests are shown in Table IV.1.)⁽¹⁵⁾ During the third transient the center rod of the array failed at about 0.030 seconds after peak power and more than half of the UO_2 dispersed into the stagnant sodium coolant that was heated prior to test to 200 °C. The total energy input 235 cal/g UO_2 at the time of failure as indicated by a pressure pulse was less than the maximum input of the second transient. (The total energy input in the second transient was 330 cal/g UO_2 .) Only the failed rod showed any diametral expansion although all rods were slightly warped. The diameter of the pin had increased 3% and the failure was a ragged tear in the tube wall.

Another series of tests⁽¹⁵⁾ involved two transients at 344 and 669 MW-sec equivalent to fission energies of 236 and 460 cal/g UO_2 , respectively. The second transient resulted in destruction of all five fuel rods.

TABLE IV.1. Results from TREAT Meltdown Experiment CEN-S2 with Stainless-Steel-Clad UO_2 -Core Fuel Rods Submerged in Sodium(15)

	Transient Number		
	1	2	3
<u>Reactor characteristics</u>			
Integrated power, MW-sec	312	481	495
Peak power, MW	1048	1790	2310
Period, msec	90	70	63
<u>Results</u>			
Peak temperature, (a) °C	470	710	--
Maximum indicated motion, cm	2.3	3.8	7.4
Maximum piston velocity, cm/sec	1.6	8.5	193
Appearance of fuel rod	Intact ^(b)	Intact ^(b)	Split
<u>Energy calculations</u>			
Fission energy input, cal/gm UO_2	214	330	340

(a) Measured by the thermocouple located in the sodium 3-in. above the bottom autoclave head.

(b) Determined from neutron radiographs of the autoclave assembly.

This series of tests suggest that the fission energy limitation involving rods similar in design to that planned for the FTR is at or very near 340 cal/g UO_2 if the fuel has no fission gas inventory.

IRRADIATED FUEL

The failure threshold for a transient following a history of steady state operation is more uncertain. It is expected that greater fuel swelling should be expected after a period of steady state operation due to the heating of fission gases retained within the fuel. Because of the high fission gas release that has been observed in fast flux experiments, it is expected that fuel swelling in a fast reactor transient should

be less than swelling under identical transients at equivalent burnup in a thermal reactor. In TREAT the power and temperature profiles are biased toward the cooler radial portion of the pellet where a significant quantity of fission gas may be retained. Consequently the potential for fuel damage is greater for transient testing in a thermal neutron spectrum. Preirradiation to ~50,000 MWd/MT tends to reduce the bias of the temperature and power distribution due to preferential steady state burnup of the fuel in the outer radii; however, the higher fission gas inventory in the outer radii than in a fuel irradiated in a fast flux increases the likelihood of release during a power excursion.

Hanson, Rabin, and Field⁽¹⁶⁻¹⁹⁾ have reported the results of transient TREAT tests on three fuel pins that had been previously irradiated to 8,000 to 70,000 MWd/MTM in a thermal flux. The results of these tests are not directly applicable to the FTR because axial restraint prevented axial motion of the fuel during the test and the preirradiation was conducted in a thermal flux. Also, three tests are not sufficient to obtain a good statistical correlation between the energy deposition to cause failure and the burnup prior to the test. Nevertheless, these data are probably the best that are available at the present time. Two of the three pins failed (one low and one high burnup) from transients that caused melting over 45 to 65% of the fuel area. By comparison, all of the failures with unirradiated fuel corresponded to cases where over 70% of the fuel areas showed evidence of melting. Table IV.2 shows results of a portion of the transient tests performed by GE.

It was concluded from the GE tests that a lower energy threshold does exist in fuel which has been irradiated and that the release of fission gas from fuel which is melted during a transient can be one of the primary causes for cladding failure.

These results are, however, in conflict with subsequent tests performed by Dickerman, Robinson, and Stewart.⁽²⁰⁻²²⁾ Transient tests run on fuel pins which had been irradiated to 6,500, 28,000, and 56,000 MWd/MTM indicated that threshold for loss of structural integrity was apparently the cladding melting point, analogous to the tests performed on unirradiated fuel. These tests were performed in an inert-gas atmosphere and the higher cladding temperatures resulting from the lower heat dissipation may explain the discrepancy. Additionally, the extent of fuel melting in these tests was not sufficient to release fission gases still contained in the fuel.

There is also a distinct possibility that damage caused to the fuel element cladding may be cumulative. The lower fission energy required to cause failure in a third transient than had been imposed in previous transients in the Barghusen experiments described earlier supports a cumulative damage model. Also work reported by Field⁽²³⁾ in which SEFOR-type UO_2 fuel pins were repeatedly transient tested into fuel melting showed evidence of cumulative damage by radial ratchetting that led to plastic deformation of the cladding.

Other transient tests have been performed to attempt to isolate the effect of fuel density and form on the transient behavior.⁽¹⁴⁾ The power profile was not held constant in this series of tests and the results are still inconclusive.

The relationship between energy deposition to cause failure and prior burnup requires the elimination of the variation in the results caused by power profile, fuel form, initial fuel-clad gap, pellet density, cladding type and cladding temperatures as well as other variables that may not yet be isolated. In addition, tests need to be performed in a reactor with a fast flux after prior irradiation in a fast flux so that the fuel swelling caused by heating of unreleased fission gases is simulated. Due to the lack of a sufficient test facility

and the number of tests required to obtain statistically meaningful correlations, the behavior of fuel during a transient will have to be predicted by computer codes established to model expected behavior and modified to predict failure as experimental failure thresholds are established.

V. ANALYTICAL PREDICTIONS OF FTR FUEL PIN BEHAVIOR UNDER OFF-NORMAL CONDITIONS

It is difficult at this time to precisely define the conditions under which mixed oxide fuel pin failure will occur for either steady-state or transient irradiations or to define the stress and/or strain criteria for the cladding which defines its failure. There are carefully designed experiments underway that will provide positive information with regard to failure criteria and conditions before the FTR goes into operation (Appendix VII) but in the interim we must rely on analytical methods to assure the safety of the reactor design and proposed operation. In this analysis the selected criteria are believed to be highly conservative and the worst possible situations have been assumed for the failure estimates. Thus a bias has been intentionally built into this analysis that should provide an additional margin of safety.

A. HYPOTHESIZED FAILURE MECHANISMS

The driving functions that could conceivably cause cladding failure during a reactor off-normal condition include:

1. The sudden release of fission gas from the fuel into the central void of the fuel where it is heated to near the fuel melting temperature (4850 °F) causing radial expansion of the fuel and excessive strain of the cladding.
2. The release of all of the generated fission gas to the plenum where the pressure causes rupture of the upper end of the cladding during a loss of coolant accident.
3. The contact of molten fuel or fission products with the inside of the cladding causing the cladding to melt and allowing coolant to enter the pin.
4. Blanketing of the pin due to nucleate boiling of the sodium or release of fission gas at the bottom of the pin and subsequent cladding rupture due to the cladding's reduced strength.

5. A mechanical ratcheting mechanism of the fuel caused by a series of overpower transients and normal reactor shutdown which ultimately lead to excessive local cladding strain.
6. Cladding melting during a loss of coolant accident.
7. Propagation of failure due to failure of neighboring pins during either a loss of coolant accident or an overpower transient.
8. Thermal expansion of the fuel during a power transient after the fuel-cladding gap has closed.

A complete analytical capability should be able to consider any or all of the proposed failure mechanisms for various fuel assembly configurations and fuel and cladding materials. Since analytical models have not been developed to analyze all of the proposed failure mechanisms, the ability to precisely define any or all of the proposed modes of fuel pin failure quickly will probably not be possible for several years.

The approach generally used in this analysis was to calculate temperature profiles at various times for the given off-normal condition being studied, and then either use this in PECT-I⁽²⁴⁾ for analysis of stresses and strains in the cladding or hand calculate new internal pressures from fission gas release and input the temperature-pressure history into PECT-I.

B. CRITERIA FOR CLADDING FAILURE

In this analysis the five criteria shown in Table V.1 were considered.

The nonelastic effective strain limit considers only permanent deformation due to creep and plasticity strains which exceed the yield strength of the material.

The total effective strain limit considers elastic (recoverable) strains in addition to the creep and plasticity strains.

TABLE V.1. Cladding Failure Criteria

1. Nonelastic effective strain

$$\epsilon_{\text{eff}}^N = 0.2\%$$

2. Total effective strain

$$\epsilon_{\text{eff}}^T = 0.2\%$$

3. Yield strength

$$\sigma_{\text{eff}} = \sigma_y \text{ at one point}$$

4. Ductile failure

$$\sigma_{\text{eff}} = \sigma_y \text{ at every point}$$

5. Brittle failure - stress at inner surface of cladding becomes tensile.

The stress criterion based on the yield strength means that when the effective stress at any point in the cladding reaches the yield strength of the material, failure occurs.

Criterion number four states that failure occurs when the effective stress at all points in the cladding equals the yield strength. This is true ductile behavior of the cladding.

Criterion five represents an extremely brittle condition by defining failure as occurring as soon as the inner surface of the cladding reaches a tensile stress condition. This would cause a crack on the inner surface of the cladding to propagate outward.

Ordinarily, one would not seriously consider criterion five for austenitic stainless steel. However, recent experimental data obtained from burst tests of fuel pin cladding lightly irradiated in EBR-II suggests that this should be considered (Appendix VI). In summary, sections of fuel pins PNL-1-3 and PNL-1-6 which were irradiated in EBR-II to approximately

8500 MWd/MT burnup (4×10^{21} nvt ($E > 0.1$ MeV) were subjected to burst tests at 900 °F. Table V.2 summarizes the burst test results. Two points were significant:

- The brittle failure characteristic of the cladding is not entirely consistent, but
- It occurred more times than not.

TABLE V.2. Summary of Biaxial Test Data

Specimen	Pretest Heat Time, hr	Test Temp, °F	Burst Press, psi	ID Hoop Stress, psi	Lifetime	$\Delta D/D$, %
"E" Lot-Cold	4	901	7760	56,960	Burst	19.2
H ₁ Plenum (PNL 1-6)	5-1/2	902	8050	59,090	Burst	18.4
G ₁ Fueled (PNL 1-6)	7	900	3000	22,020	Burst	1.0
C ₃ Fueled (PNL 1-6)	5-1/2	897	9830	72,150	Burst	9.6
G ₂ Fueled (PNL 1-6)	3	900	3700	27,160	Burst	0.3
C ₂ Fueled (PNL 1-3)	3	1000	9100	66,790	Burst	6.7
G ₄ Fueled (PNL 1-3)	2	900	5300	38,900	Burst	0.2

The results of these tests are discussed in more detail in Appendix VI. It is significant to note that the cracks propagate from the inner surface of the cladding. This cladding failure characteristic was also observed during the postirradiation examination of mixed oxide fuel pins which were transient irradiated in treat.⁽¹⁶⁾

GE^(25,26) has reported the existence of cracks on the inner surface of a Type 316 stainless steel clad mixed oxide fuel pins that had operated in EBR-II at ~16 kW/ft to ~70,000 MWd/MTM. Another pin with Type 347 SS cladding had failed. Both pins

contained pellet fuel with a very high smeared density (94-95%) compared with 85.5% for the FTR driver fuel and both operated at a considerably higher power than the 12.2 kW/ft. maximum being considered for the FTR. Hence, the performance of the GE pins (F2 Series) is not directly applicable to the steady-state behavior of the FTR driver fuel, but the formation of cracks on the inner surface and the possible propagation of these cracks under a tensile stress state represents a situation that should be analyzed when failure under accident conditions are considered.

As noted in Appendix IV the stress on the inner surface of the cladding may be tensile at the end of life for a high power pin (operating at steady-state) that has released 80% of its fission gas and has had its effective cladding thickness reduced to 10 mils. Calculations show that this same pin still has a compressive stress on the inner surface if the cladding thickness remains at 15 mils. Obviously a tensile stress itself is not sufficient to cause failure. It can only propagate cracks that are already present or cause cracks after some fracture stress is exceeded. Appendix V discusses this subject in greater depth. Because of the present uncertainties inherent in the estimates of cladding stresses under steady-state conditions and the present lack of information to realistically evaluate crack propagation we have considered criterion 5 (tensile stress on the inner surface) to be a reasonable failure criteria under transient conditions only when relatively large compressive stresses become tensile.

C. ACCIDENT IDENTIFICATION FOR TRANSIENT CONDITIONS

The following types of transient conditions were analyzed:

- 1) An unterminated overpower transient of 5\$/sec at full coolant flow
- 2) A 6¢/sec power increase with 10% coolant flow
- 3) A flow coastdown accident from loss of offsite power
- 4) A pipe rupture accident.

To evaluate the response of the fuel pins, in terms of failure thresholds and modes of failure progression, a rather mechanistic approach has been taken. That is, various accidents are identified that are assumed to represent the more severe conditions within a given accident category. These accidents are then analyzed mechanistically to evaluate the transient consequences imposed upon the fuel pin. The consequences are specified in terms of time for a given mode of failure and the condition of the fuel and cladding at that time. These conditions will be discussed in Section VI with regard to the preliminary list of accident severity levels for the FFTF that was developed as a guide for design and was presented in Appendix A of the Criteria for Accident Severity Levels - FFTF Reactor Systems.⁽²⁾

Given any single accident condition, the consequences are design dependent. This, of course, is one of the fallacies of a mechanistic approach in that any one accident which may be identified as limiting can be eliminated from further consideration by concentrated design efforts. For example, the flow decay rate for a loss of power condition can be reduced by a different torsional inertia which is design dependent. However, the mechanistic approach is useful since it does provide a somewhat logical basis for the specified transient conditions.

The fuel assembly meltdown and collapse is unique to fast reactor systems which, theoretically, have the potential for positive reactivity feedback by collapse of the fuel pins leading to higher fuel concentration toward the core midplane. A ramp rate of 5\$/second is about twice the value calculated for gravity collapse of any single driver fuel assembly. A value of ~ 4 \$/second is conservatively estimated as a peak value for projected open loop test assemblies and advanced core materials. However, any special test configuration proposed for the FFTF test loops by a customer will be evaluated relative to the design conditions for the plant protective system (PPS). This

system will be designed to override a 5\$/second reactivity ramp without exceeding the severity of a minor accident and so this is the limiting value used in these studies. (It should be noted that the fuel damage criteria and fuel parameter limits apply to the fuel assemblies affected by the subsequent transient and not the initiating fault. For example, if cooling is lost to a single fuel assembly, the fuel assembly will eventually melt down, thus exceeding all of the listed parameter limits.)

The 6¢/second power increase accident at 10% coolant flow is a common design condition for safety systems. It occurs during reactor startup while the coolant flow is still quite low and comes about as a consequence of the continuous withdrawal of the control rods.

The loss of offsite power to the primary pumps is also a common design condition for safety systems. When this occurs flow to the subassemblies gradually "coasts down" according to the inertia in the system, e.g. the size of the flywheel on the pump motors.

The pipe rupture accident is not one that is likely to occur but is considered nevertheless. The pipe rupture case involves the flow response to a sudden double-ended pipe break at the worst location. Flow would be lost temporarily as the standpipe surrounding the break is filled but then shutdown flow would be re-established.

Another transient case that Hanson⁽³⁾ had considered previously but is not analyzed herein is a 50\$/second power transient that is the estimated ramp rate for a coherent bubble passing through the core. It turned out that the failure mode was about the same for the 5\$/second consideration but occurs in times a factor of ten shorter.

D. FAILURE PREDICTIONS FOR VARIOUS ASSUMED OFF-NORMAL CONDITIONS

In making the calculation, the one or two failure modes that were thought to be the most likely were examined in detail. Not all failure modes were analyzed for all accident conditions but, in general, the five criteria for cladding failure were examined for each case analyzed. Times to reach the various applicable cladding failure criteria are compared with times to reach selected cladding temperatures as well as the times to melt selected fractions of the fuel cross section for each transient.

The time remaining between the onset of the various types of accidents and the most applicable cladding failure criteria is determined in order to show the time available to take corrective measures before cladding failure is predicted.

1. 5\$/sec Transient

The power-time history for the 5\$/sec uninterrupted transient used in this analysis is shown in Figure V.1.⁽²⁷⁾ With this history it is now possible to estimate the rate of rise in temperature at any point in the fuel. The maximum power pin (12.2 kW/ft steady state power) was analyzed since this pin would melt first. Figure V.2 shows the fraction of fuel cross-section molten at the hottest axial position as a function of time after the start of a 5\$/sec transient. (See Appendix A.II for estimates of the temperatures and structure that exist prior to the transient.) The increase in the maximum inner cladding temperature is shown in Figure V.3 after the start of the 5\$/sec power transient. The maximum cladding temperature, which occurs at the top of the fuel column, is plotted as a function of the area fraction of fuel melted in Figure V.4.

1.a. Estimates of Failure from Fission Gas Release

The amount of gas released to the central void is predicted by determining the fraction of fuel cross section molten as a

FIGURE V.1. RELATIVE REACTOR POWER VERSUS TIME FOLLOWING A 5\$/SECOND TRANSIENT (27)

V.10

FIGURE V.2. FRACTION OF FUEL MOLTEN AFTER A 5\$/SECOND TRANSIENT

FIGURE V.3. INNER CLADDING TEMPERATURE (°F) VERSUS TIME AFTER 5\$/SECOND TRANSIENT AT THE TOP OF THE FUEL COLUMN

FIGURE V.4. MAXIMUM CLADDING TEMPERATURE (ID) AS A FUNCTION OF PERCENT OF FUEL MELTED DURING 5\$/SECOND TRANSIENT AND 830 LB/HOUR COOLANT FLOWRATE

function of time after the start of the power transient. If the molten zone remains within the columnar region, no gas is assumed to be released to the central void. If, however, the molten zone extends beyond the columnar region, all of the fission gas that has been retained in the portion of fuel melted and outside the columnar zone is assumed to be deposited within the central void and raised to a temperature equal to the fuel melting temperature. This calculation is also integrated along the pin length because the power level and gas production varies axially. No fission gas is assumed to exist in the central void prior to the transient but the pressure of the gas in the plenum is added to this pressure. The increase of gas pressure within the void from an uninterrupted 5\$/sec overpower transient is shown in Figure V.5, assuming 0, 50 and 80% gas release prior to the transient.

The calculations suggest that a rapid rise of pressure could be expected in the pin after \$1.3 to \$1.4 of reactivity addition in the core. The times to failure for the failure criteria (Table V.1) are summarized in Table V.3 for the case where the wall thickness was reduced to 10 mils. Failure is generally predicted at the top of the fuel column where the cladding temperature is the hottest and, hence, its strength is the lowest. Figure V.6 shows the variation in failure time along the length of the pin for 0, 50 and 80% release for the criteria that the cladding wall has become entirely plastic.

It is apparent from Table V.3 and Figure V.6 that for this assumed failure mode failure time is relatively insensitive to the cladding failure criteria that is selected (except for tension on the inner surface). This, of course, comes about because of the shape of the pressure-versus-time curve (Figure V.5) that indicates little increase in pressure up to about 0.26 sec; then an extremely rapid pressure rise with time as fuel is melted beyond the columnar grain structure and fission gas is released. Likewise, the percent gas released during steady state in the regions of the fuel outside the columnar

FIGURE V.5. INTERNAL GAS PRESSURE AFTER 5\$/SECOND UNINTERRUPTED TRANSIENT

TABLE V.3. Estimate of Cladding Failure During 5\$/sec Power Transient from Fission Gas Release to Central Void

Cladding Failure Criteria	Time to Failure, Seconds	Pressure, psi	Corresponding Cladding Temperature, °F		Area Fraction Molten Fuel, %
			inner	outer	
<u>Zero Percent Released*</u> (10 mil wall - 35 in. up fuel column)					
5**	0.2629	2015	1140	1084	20
3	0.2822	5878	1170	1110	40
2	0.2826	6109	1171	1111	40
4	0.2827	6147	1171	1111	40
<u>50 Percent Released*</u> (10 mil wall - 35 in. up fuel column)					
5	0.2569	1121	1131	1076	12
3	0.2891	5856	1184	1122	47
2,4	0.2897	6025	1185	1124	47
<u>80 Percent Released*</u> (10 mil wall - 35 in. up fuel column)					
5	0	800	1046	1010	0 (steady state)
3	0.3013	5557	1208	1145	56
2,4	0.3021	5766	1210	1147	57

* Fission gas released from fuel during steady state operation.

** Failure criteria as explained in Table V.1 are

- 1) $\epsilon_{\text{eff}}^N = 0.2\%$
- 2) $\epsilon_{\text{eff}}^T = 0.2\%$
- 3) $\sigma_{\text{eff}} = \sigma_y$ at one point
- 4) $\sigma_{\text{eff}} = \sigma_y$ at every point
- 5) Tensile stress on cladding interior

FIGURE V. 6 . VARIATION IN FAILURE TIME ALONG THE FUEL COLUMN FOR ALL PLASTIC CLADDING

grains is a relatively insensitive parameter compared to the delay time for the initiation of fuel melting. Furthermore, with this failure mode, cladding temperature is not a controlling factor.

1.b. Estimates of Failure from Expansion Due to Fuel Melting

One of the proposed mechanisms for fuel pin cladding failure during an overpower transient is deformation of the cladding due to the fuel volume expansion attendant with the formation of molten fuel.

In this calculation it is assumed that all of the fabricated porosity outside the columnar grain region of the fuel pellet is available to accommodate the volume increase of the molten fuel. No credit is given to the central void or the fuel-cladding gap that might be present. The fraction of fuel melted is calculated as a function of time, and the $\Delta V/V$ determined by correcting for fuel density and assuming 10% volumetric expansion of fuel above the fusion temperature (4850 °F)⁽²⁸⁾ and 5% expansion for fuel at fusion temperature. The available void outside the melted fuel or columnar region (whichever is larger) is then determined as a function of time after start of the transient. When the expansion of fuel due to melting is equal to the void available, cladding strain is expected. Figure V.7 shows that the time for the start of cladding strain from molten fuel is about 0.30 sec for a 90.5% dense fuel pellet and about 0.31 sec for an 88% dense fuel. Interestingly, this isn't much different from the failure times predicted for fission gas release during fuel melting. A time of 0.3 sec corresponds to 50 to 60% of the cross-sectional area of the fuel becoming molten at the maximum fuel temperature.

The time for failure will depend upon the amount of strain that the cladding can withstand after the internal voidage is consumed. If no axial redistribution of fuel is allowed, the cladding strain can be estimated as a function of time from

FIGURE V.7. PREDICTED TIME FOR FAILURE FROM FUEL MELTING FOLLOWING A 5\$/SECOND UNINTERRUPTED TRANSIENT

Figure V.7 and an expression relating cladding strain $\frac{\Delta r}{r}$ to fuel volume change $\frac{\Delta V}{V}$ as follows:

$$\frac{\Delta r}{r} = \frac{1}{2} \frac{\Delta V}{V}$$

Figure V.8 plots the result of this calculation for the 90.5% dense fuel. If the cladding could accommodate 1% strain instead of zero percent strain, the failure time would be extended from 0.30 to 0.34 seconds. Of course, after 0.34 sec more than 80% of the fuel would have melted and the potential for molten fuel coming in contact with the cladding becomes rather large.

1.c. Prediction of Cladding Strain from Fuel Cladding Interaction Produced by Thermal Expansion

A major uncertainty in all predictions of fuel behavior during a transient is the fuel-cladding gap during steady state operation. The swelling of the fuel and cladding during irradiation make any prediction of the fuel-cladding gap after some burnup highly speculative. Examination of irradiated fuel, however, which is fabricated with a diametral gap less than 6 mils generally indicates that the gap closes after a short irradiation period. In this calculation it will be assumed that the fuel and cladding are in intimate contact at the start of the 5\$/sec transient. The cladding expansion was determined using the average cladding temperature profile calculated with TIGER-V shown in Figure V.9. The fuel expansion was determined using the volumetric average fuel temperature (Figure V.10) calculated using TIGER-V and a fuel thermal expansion of ⁽³²⁾

$$\frac{\Delta r}{r} = \left[4.45 + \frac{5.55}{3000} (\bar{T} - 500) \right] \Delta \bar{T}$$

where $\bar{T}$ = volumetric average fuel temperature (°F)

The predicted cladding strain is shown in Figure V.11 as a function of time after an uninterrupted 5\$/sec transient. If 1% cladding strain is chosen as a failure criteria, failure can be expected after 0.240 sec.

FIGURE V.8. CLADDING STRAIN FROM FUEL MELTING AFTER INTERNAL VOIDAGE IS CONSUMED - 5\$/SECOND TRANSIENT

FIGURE V.9. AVERAGE CLADDING TEMPERATURE VERSUS TIME AFTER A TRANSIENT OF 5\$/SECOND (19 INCHES FROM INLET)

V.22

FIGURE V.10. AVERAGE FUEL TEMPERATURE (AT $(r/R)^2 = 0.5$) VERSUS TIME AFTER UNINTERRUPTED 5 ϕ /SECOND TRANSIENT (19 INCHES FROM INLET)

FIGURE V.11. PREDICTED CLADDING STRAIN FROM THERMAL EXPANSION AFTER 5\$/SECOND UNINTERRUPTED TRANSIENT

This oversimplified consideration of thermal expansion is felt to be unrealistic. As the fuel pushes on the cladding (as it does from time zero with these assumptions), the cladding will exert a stress on the fuel which will reduce the magnitude of the cladding strain shown in Figure V.11 due to elastically compressing the fuel. The approach to more realistically evaluate differential thermal expansion is discussed in Appendix A.VI.

1.d. Summary for the 5\$/sec Transient

Table V.4 summarizes the times for failure for the three primary failure modes that were considered, i.e., fission gas released to the central void, expansion from fuel melting, and differential thermal expansion of fuel and cladding.

TABLE V.4. Summary of Fuel Failure Analyses for a 5\$/sec Power Transient

Time Sec	Fraction of Fuel Molten, %	Maximum Cladding Temp, °F	Failure Mode
0.152	0	1048	Differential thermal expansion with 0.2% cladding strain.*
0.242	35	1110	Differential thermal expansion with 1% cladding strain.
0.310	56	1233	Fuel melting with 0.2% cladding strain.*
0.340	>80	1350	Fuel Melting with 1% cladding strain.
0.302	57	1210	Fission gas release on melting - 80% steady state gas release - 0.2% cladding strain.*
0.302	57	1210	Fission gas release on melting - 80% steady state gas release - cladding plastic at all points.**
0.2826	40	1171	Fission gas release on melting - 0% steady state gas release - 0.2% cladding strain.*
0.2827	40	1171	Fission gas release on melting - 0% steady state gas release - cladding plastic at all points.**

* Criteria 2 - Table V.1 - $\epsilon_{eff}^T = 0.2\%$

** Criteria 4 - Table V.1 - plastic at all points

2. 6¢/Second with 10% Flow

The power-time history for the simulation of a rod withdrawal accident at reactor startup is shown in Figure V.12.⁽²⁹⁾ It is assumed that the reactivity is increasing at 6¢/second but that the coolant flow rate is only 10% that of full flow. After about 16 seconds, the reactor power has reached about 100 MW and levels off for a few seconds at about 160 MW due to the Doppler effect. This analysis was terminated at about 22 seconds but, if unchecked, the reactor power would start to rise again (although less steeply) and would probably reach 500 MW in another ten seconds. Our analysis of fuel pin failure was not carried beyond 22 seconds.

The peak fuel and cladding temperature (the latter occurring near the top of the fuel column) were determined at various times and the estimates are plotted in Figure V.13. It is seen that the peak cladding temperature rises almost as rapidly as the peak fuel temperature. Figure V.13 includes a pin that would operate at 8.7 kW/ft under steady state conditions as well as a pin that would operate at 12.2 kW/ft.

Since the cladding temperatures are rising so rapidly in relation to the fuel temperatures, the primary failure mode considered was that due to the increasing pressure (See Figure V.14) in the fuel pin plenum due to the rising temperature. No additional fission gas would be expected to be released since the steady state fuel temperatures are never reached. Several burnup and percent steady state fission gas release cases were considered (See Appendix IV) the most severe of which was 80% released and 80,000 MWd/MTM. Table V.5 summarizes the failure times predicted for the various assumed cladding failure criteria for this worst case. Failure would be expected at the top of the fuel column.

The possibilities that the sodium would boil

FIGURE V.12. REACTOR POWER (MW) VERSUS TIME AFTER A 6¢/SECOND POWER EXCURSION WITH 10% COOLANT FLOW (28)

FIGURE V.13. FUEL AND CLADDING TEMPERATURE VERSUS TIME AFTER 6¢/SECOND UNINTERRUPTED TRANSIENT WITH 10% FLOW

FIGURE V. 14. INCREASE IN PLENUM PRESSURE WITH A 6¢/SECOND TRANSIENT WITH 10% COOLANT FLOW

causing vapor blanketing of the pin and subsequent cladding melting was considered. Since sodium boiling would not be expected until its temperature reached 1900 °F (See Appendix III), it can be seen from Table V.5 that no boiling is anticipated.

TABLE V.5. Predicted Failure Times at the Top of the Fuel Column for the 6¢/second Transient

<u>Cladding Failure Criteria</u>	<u>Failure Time, sec</u>	<u>Plenum Pressure, psi</u>	<u>ID Cladding Temp, °F</u>
<u>0 Burnup</u>			
3*,4**	22.08	207	1733
<u>80,000 MWd/MTM - 80% release</u>			
3	21.84	1149	1694
4	21.90	1155	1705
<u>50,000 MWd/MTM - 80% release</u>			
3	21.96	724	1712
4	21.96	726	1717

* Criteria 3 = one point plastic

** Criteria 4 = entire tube wall plastic

Interestingly, Table V.5 shows that failure is independent of the original condition of the fuel, i.e., zero burnup fuel pins fail at very nearly the same time as those at 80,000 MWd/MTM burnup. Actually, these results merely reflect the fact that the strength of the cladding is going to essentially zero in the neighborhood of 1700 °F (see Appendix IV). The more significant point to be made is that failure does not occur below about 1700 °F for any case considered for this type of transient. This will be discussed in greater detail later.

3. Prediction of Time to Cladding Failure During a Loss of Coolant Accident

Two types of loss of coolant accidents have been proposed; namely, a flow coastdown due to loss of electrical power to the pumps or a sudden pipe rupture causing an instantaneous loss of coolant flow. Both accidents will be analyzed in this section.

3.a. Flow Coastdown Accident

Figure V.15 shows the normalized flow rate that was assumed in the flow coastdown accident. The steady state pin power was 12.2 kW/ft since this is the hottest pin and would also contain the most gas in the plenum at the start of the transient. Two flow rates were considered--830 and 885 lb/hr.

Fuel pin cladding failure during a flow coastdown accident could conceivably be caused by

- the loss of strength of the cladding due to overheating and subsequent cladding rupture due to the pressure of gas in the pin plenum, and
- nucleate boiling of the sodium following blanketing of the pin with sodium vapor leading to cladding melting.

Early studies had focused upon the second mode of failure but these studies have shown that cladding failure should occur due to cladding overheating before coolant boiling begins (see Appendix IV).

The transient flow coastdown accident was studied using the following assumptions:

- The gas in the plenum increased in temperature at a rate equal to the coolant outlet temperature and obeyed the perfect gas law.
- There is no additional gas release from the fuel during the transient since no fuel is melted.
- The heat transfer coefficients for the cladding-coolant and the fuel-cladding interfaces remain unchanged during the transient.

FIGURE V.15. FLOW COASTDOWN - NORMALIZED FLOWRATE VERSUS TIME

The cladding temperature predicted using the TIGER V computer code and the flow coastdown assumption shown in Figure V.15 is depicted in Figure V.16 as a function of time. The estimated pressure rise is shown in Figure V.17 as a function of time for 80% steady state fission gas release. When the cladding temperature reached 1650 to 1800 °F, the fission gas pressure induced stress was found to cause failure. Table V.6 shows the predicted internal gas pressure, the inner and outer cladding temperatures and the time for cladding failure using the cladding failure criteria in Table V.1. Times to failure assuming both 32.5 and 80% steady state gas release are shown. Appendix IV summarizes the cases that were studied.

TABLE V.6. Time for Cladding Failure at the Top of the Fuel During a Flow Coastdown Accident.

<u>Time to Failure, seconds</u>	<u>Plenum Gas Pressure, psi</u>	<u>Inner Clad Temperature</u>	<u>Outer Clad Temperature</u>	<u>Criterion</u>
-	295	1025	990	Initial condition, (32.5% steady state fission release)
4.64	426.1	1685	1667	5
4.94	440.2	1756	1740	3
5.19	451.7	1814	1798	4
5.21	452.6	1818	1803	$E_{eff}^T \approx 0.2\%$
-	800	1025	990	Initial condition, (80% steady state gas release)
4.39	1124	1626	1607	3
4.52	1141	1658	1640	4
4.55	1144	1663	1646	$E_{eff}^T = 0.2\%$

The shortest failure time estimated is 4.39 seconds for the criteria of exceeding the yield point any place in the cladding. None of the criteria are exceeded until the cladding temperature exceeds ~ 1600 °F.

The time for boiling of the sodium at the cladding outside diameter assuming a pressure of 50 psia is 5.6 seconds which is in excess of the time to failure predicted in the above

FIGURE V.16. CLADDING TEMPERATURE HISTORY - FLOW COASTDOWN AND PIPE RUPTURE

V.34

FIGURE V.17. INTERNAL PRESSURE HISTORY WITH 100% GAS RELEASE TO PLENUM

analysis. Consequently, failure by fission gas pressure induced stress exceeding the strength of the cladding should be considered the more limiting case.

3.b. Pipe Rupture Accident

The flow history during a pipe rupture accident was assumed to follow the profile shown in Figure V.18. As before, the coolant flow rate prior to the failure was assumed to be 830 and 885 lb/hour and the steady state pin power was 12.2 kW/ft.

TIGER V was again used to predict the cladding temperature as a function of time. The temperature history of the cladding during a pipe rupture accident is compared with that during the flow coastdown in Figure V.16. It is seen the temperature rises much more rapidly in the case of the pipe rupture but the rate of increase approaches that of the flow coastdown after one second.

The internal gas pressure assuming 80% steady state fission gas release is compared with that predicted during a flow coastdown in Figure V.17. The higher pressure during the pipe rupture is due to the higher outlet coolant temperature.

Table V.7 shows the predicted time to failure assuming 32.5% and 80% steady state fission gas release and a burnup of 80,000 MWd/MT. In the case of the pipe rupture accident, the predicted time to failure for 80% steady state fission gas release is 1.16 seconds.

Sodium boiling would be predicted after ~3 seconds for this type of accident and thus occurs after fuel pin cladding failure.

3.c. Summary of 6¢/second Power Transient and Loss of Coolant Accidents

These three accident categories are typified by a rapid heating of the coolant and cladding. The fuel temperatures

FIGURE V.18. PIPE RUPTURE ACCIDENT - NORMALIZED FLOWRATE³ VERSUS TIME

TABLE V.7. Time to Failure, Inner Gas Pressure and Cladding Temperature Following a Pipe Rupture Accident

<u>Time to Failure, seconds</u>	<u>Pressure, psi</u>	<u>Cladding Temperature</u>		<u>Criterion*</u>
		<u>Inner, °F</u>	<u>Outer, °F</u>	
32.5% Steady State Fission Gas Release				
0	295	1025	990	
0.3	344.5	1274	1259	5
1.96	441.2	1761	1746	3
2.33	451.7	1813	1798	4
2.36	452.6	1818	1803	1 & 2
80% Steady State Fission Gas Release				
0	800	1025	990	
1.16	1129	1636	1621	5 & 3
1.22	1141	1657	1642	4
1.24	1145	1665	1649	1 & 2

* Refer to Table V.1.

do not increase sufficiently to cause concern over either thermal expansion effects or additional fission gas release during the transient. Hence, the analysis centers around determining whether the cladding fails due to internal pressure or due to sodium boiling with an attendant loss of heat transfer followed by vapor blanketing of the pin and cladding melting. The assumptions made for the analysis are believed to be conservative. Thus, the estimated failure times should be shorter than those that would be experienced should such accidents occur.

In treating the internal pressure increase with time, it is assumed that the temperature of the gas in the plenum is always the same as the maximum cladding temperature. This is conservative because the pressure rises that we have considered all occur in less than 5 seconds which is probably insufficient time to establish thermal equilibrium between the cladding and the plenum gas.

Another conservative assumption is believed to be the dependency of the yield strength of 316 stainless steel on

temperature (see Figure AIV.7 in Appendix IV). The strength goes to extremely low values in the neighborhood of 1600 °F. There is an almost complete lack of data in this temperature range so that the analysis becomes highly uncertain above a temperature of about 1600 °F. The important point here is that none of the analysis indicate failure below 1600 °F. Thus from a cladding stress point of view, any accident of this type that does not cause the cladding temperature to exceed 1600 °F will not cause pin failure. Table V.8 summarizes the estimated minimum failure times and pressure-temperature conditions for the three accident conditions considered.

TABLE V.8. Summary of Estimated Failure
During a 6¢/second Power Transient
and Loss of Coolant Accidents

<u>Time to Failure, seconds</u>	<u>Internal Pressure, psi</u>	<u>Inner Cladding Temperature, °F</u>	<u>Conditions</u>
22.08	207	1733	6¢/sec 0 Burnup
21.84	1149	1694	6¢/sec 80,000 MWd/MTM 80% Steady State Fission Gas Release
21.96	724	1712	6¢/sec 50,000 MWd/MTM 80% Steady State Fission Gas Release
4.94	440	1756	Flow coastdown 32.5% Steady State Fission Gas Release
4.39	1124	1626	Flow coastdown 80% Steady State Fission Gas Release
1.96	441	1761	Pipe rupture 32.5% Steady State Fission Gas Release
1.16	1129	1636	Pipe rupture 80% Steady State Fission Gas Release

NOTE: Since the sodium will not boil until it reaches a temperature of 1900 °F, fuel pin failure will occur before the coolant boils.

The cladding failure criteria and the assumed percent of fission gas released during steady state operation are both relatively insensitive compared with the decreasing cladding strength with temperature. This entire analysis turns out to be an interplay between the rising pressure in the plenum with increasing temperature (time) and the attendant decrease of cladding strength. As it turns out, in so far as total elapsed time for failure from the start of the transient is concerned, all other considerations are relatively unimportant.

Because of the conservative assumptions, it may be that sodium boiling would be the actual failure mechanism in an actual accident of the type considered here. However, this would take a longer time and would require higher coolant temperatures. Thus, by setting the cladding temperature limits based on the cladding stress analysis we have probably provided an additional margin of safety.

VI. IMPACT OF THE PRESENT ANALYSES ON FUEL CLADDING DAMAGE CRITERIA

The impact of the analyses discussed in this report can now be evaluated in terms of the accident severity levels that have been previously defined⁽²⁾ for the FTR. This is summarized in Figure VI-1.

A. Normal Operating Zone

The expected operation of the FTR driver fuel is discussed in Appendix I. The analyses substantiates that the fuel is expected to perform satisfactorily to 80,000 MWd/MTM at all power levels considered (4.8, 8.7, 12.2 and 14.6) including the 20% overpower case.

B. Operational Incident

As indicated in the Introduction (Section I), no significant reduction of the burnup capability is allowed for this accident severity level. With the exception of fuel-cladding differential thermal expansion, our analyses did not indicate any reduction in fuel pin capabilities if the conditions outlined in Figure VI-1 for an Operational Incident were never exceeded. Actually, it can now be suggested that the maximum cladding temperature allowable for this severity level be increased to 1250 °F. In so far as the stress analyses are concerned (see Appendix IV), this value could be increased to 1600 °F, for none of the cases considered predicted anything other than elastic stresses in the cladding below 1600 °F. In setting this cladding temperature limit other factors must be considered (see Appendix V and IX) such as the metallurgical condition of the cladding and crack propagation. Thus, for 6 such incidents (~ 120 for a 20 year reactor life and a one year pin life), each of which occurs in less than 20 seconds (the 6¢/second uninterrupted power excursion case provides the maximum time at overpower conditions), a maximum of two minutes can be considered as the longest time

TENTATIVE DATA

ACCIDENT SEVERITY LEVELS	FUEL MATRIX DAMAGE CRITERIA				CLADDING DAMAGE CRITERIA PEAK TEMP.
	CROSS-SECTIONAL AREA $\geq T_{MELT}$		PEAK FUEL TEMPERATURE		
	NEW FUEL	IRRADIATED	NEW FUEL	IRRADIATED	IRRADIATED
DESIGN BASIS ACCIDENT					
DISRUPTIVE ACCIDENT	70%	50%			1600°F
MAJOR ACCIDENT	20%	20%			(1350°F) 1300°F
MINOR ACCIDENT	0	0	5100°F	4950°F	(1250°F) 1200°F
OPERATIONAL INCIDENT					
NORMAL OPERATING ZONE					
NOTE: The changes shown on this figure represent the results of the analyses contained in the present report.					

FIGURE VI.1. OXIDE FUEL DAMAGE CRITERIA FOR THE FFTF ACCIDENT SEVERITY LEVELS

that the cladding could be at 1250 °F. No significant variation in cladding properties would be expected beyond that which will occur during steady state operation at lower temperatures.

The differential thermal expansion between the fuel and cladding was shown to be a potential problem during the 5\$/second transient when a "hard ball" model was used. However, when elasticity of the fuel pin system was considered and a more realistic cladding strain criteria ($\epsilon = 1\%$) used for this secondary type of loading, this problem disappeared. Differential thermal expansion will be analyzed in greater depth by conducting detailed analyses of the existing applicable TREAT data. Additional TREAT tests may be conducted as further assurance that this is not a problem.

The analyses of this report were conducted on the basis that the melting temperature of irradiated fuel is 4850 °F⁽²⁸⁾, however, a report* recently received at BNW has re-evaluated the available data and concluded that the reduction in mixed oxide fuel melting point at 80,000 MWd/MTM is negligible. Thus the value of 4950 °F shown in Figure VI-1 appears reasonable.

C. Minor Accident

This type of accident is defined as one in which the integrity of the fuel pin is maintained but its capability to incur additional burnup and sustain an additional transient has been impaired. Again, with the exception of differential fuel-cladding thermal expansion, our analyses did not indicate fuel pin cladding failure if the condition defined in Figure VI-1 are not exceeded. When elasticity is considered, differential fuel-cladding thermal expansion is not predicted to present a problem until 20% of the fuel has melted. This remains a matter for additional analysis (see Section VI-B, above).

* J. L. Krankota and C. N. Craig, "The Melting Point of Plutonia-Urania Mixed Oxides Irradiated to High Burnup" GEAP-13515, July 1969. Document Issued February, 1970.

As none of our analyses indicated that cladding temperature posed a failure problem below 1600 °F, it can be suggested that the 1300 °F limit imposed on cladding temperature for a minor accident could be raised to a higher value, e.g., 1350 °F. While some reduction in metallurgical properties of the cladding may occur (see Appendix IV) making it prudent to avoid additional burnup of the fuel, this reduction would not significantly affect the analyses that were conducted here because of the extremely short times involved.

D. Major Accident

It is expected that this type of accident may produce some pin failures and our analyses indicated that beyond 50% molten fuel or at cladding temperature in excess of 1600 °F, failure is predicted very shortly after either of these conditions are satisfied. Transient tests in TREAT⁽¹⁶⁾ have experimentally verified that failure can occur in fuel pins approximating the present FTR pin design when 30-50% of preirradiated fuel becomes molten. A report recently received at BNW* showed that providing internal pressure relief via an annular blanket and an annular reflector at the top of the fuel pin substantially improved the capability of mixed oxide fuel pins to sustain power transients without failure. These data will be analyzed in greater detail for possible application of this concept to the FTR fuel pin design and its possible consequences on the fuel/cladding damage criteria.

E. Summary

The proposed changes that these analyses have suggested are summarized in Figure VI-1. This is based on the failure analyses presented herein and the best estimates that are presently available to us concerning the metallurgy of the cladding.

* K. Hikido and J. H. Field, "Molten Fuel Movement in Transient Overpower Tests of Irradiated Oxide Fuel," GEAP-13543, September 1969.

VII. MAJOR UNCERTAINTIES IN PREDICTING FUEL FAILURE

The following list identifies the major uncertainties in predicting fuel performance during abnormal operation. This is by no means a complete list but identifies areas where experimentation is required to answer the more difficult critical questions.

- a) Properties of irradiated 20% cold worked 316 SS as a function of temperature and fluence.
- b) The ductility and fatigue strength expected from 20% cold worked 316 SS as a function of temperature, fluence and time in sodium.
- c) Fuel and cladding swelling in a fast reactor as a function of fluence, temperature, biaxial stress.
- d) The plastic behavior of fuel once fuel-cladding contact occurs.
- e) The effect of pellet density and initial gap size on the ability of the fuel to accommodate melting without producing appreciable cladding strain.
- f) The release of fission gas from fuel during overpower transients.
- g) The effect of power ramp on the ability of fuel to axially redistribute during an overpower transient.
- h) The tendency for failure to propagate in an assembly as a function of size of power transient and rod geometry.
- i) Whether or not damage is cumulative and if so the relationship between ramp rate, hold time at peak and zero power, and probability of rod failure.

Figure VII.1 shows the results of a study made to predict the uncertainty of maximum cladding temperature as a function of time following a 5\$/sec overpower transient. In this study TIGER V was used to predict the cladding temperature as before only the peaking factors were applied to the operating parameters in such a way to minimize and maximize the fuel and cladding

FIGURE VII.1. UNCERTAINTY IN MAXIMUM CLADDING ID TEMPERATURE OF FTR MAX. PIN UNDERGOING A 5\$/SEC. TRANSIENT

temperatures. Those peaking factors are discussed in detail in Reference 30. The factors used in this analysis are shown in Table VII.1.

Figure VII.1 suggests that the cladding temperature is predictable to ± 93 °F for the peak pin operating at 12.2 kW/ft. This produces an uncertainty in the prediction of the time to reach a cladding temperature of 1200 °F of ± 0.047 sec.

Figure VII.2 shows the uncertainty in peak fuel temperatures using the same criterion as the previous discussion. The results of the analysis shown in Figure VII.2 suggest that the peak fuel temperature is predictable during steady state operation at 4030 ± 730 °F. The uncertainty in the time to reach fuel melting is ± 0.015 sec.

TABLE VII.1. Uncertainty in Fuel and Cladding Temperature⁽³⁰⁾

A. Gap Conductance

Nominal Cold Gap 3 ± 1.5 mils

h_{gap} minimum 850 Btu/hr °F ft²

h_{gap} maximum 1350 Btu/hr °F ft²

Figure VII.3

B. Film Coefficient Coolant-Clad

$h = 37,500 \pm 12,500$ Btu/hr ft °F

C. Deviation in Coolant Flow

$\pm 8.1\%$

D. Coolant Inlet Temperature

inlet T $\pm 0.5\%$

inlet uniformity $\pm 1.67\%$

Total = $\pm 2.17\%$

E. Power Measurement $\pm 9.0\%$

The sodium material properties are included in the above calculation and are not treated separately.

FIGURE VII.2. UNCERTAINTY IN MAXIMUM FUEL TEMPERATURE OF FTR MAX-PIN-UNDERGOING A 5\$/SEC. TRANSIENT

FIGURE VII.3. INITIAL GAP CONDUCTANCE h (BTU/HR FT² °F) VERSUS COLD GAP OF ROD WITH FUEL PELLET 0.213 DIAM AND OUTSIDE COOLANT TEMPERATURE=1000°F (31)

VIII. PROJECTED CONTINUATION OF THESE STUDIES

As indicated in other sections of this report, the bulk of the effort to date has been directed toward developing certain capabilities for analyzing specific off-normal conditions in the FTR. This development was based on qualitative assessments of the available data (both steady state and transient). Highly conservative assumptions were then made in order to arrive at fuel/cladding damage criteria for the FTR. In making these analyses it became evident that several areas require considerably more study. These are outlined below.

1) Differential Fuel-Cladding Thermal Expansion

Since this was the most limiting failure mechanism of those that were considered for large reactivity transients but is not a mechanism that TREAT experimenters⁽⁹⁻²³⁾ consider to be a primary cause of failure, it becomes obvious that additional analyses are required. In addition to checking the calculations for possible error, other considerations such as fuel plasticity and creep will be examined in depth.

2) Correlation of the Analysis Techniques With Experimental Data

The analytical methods described in this report will be applied in a quantitative fashion to as much pertinent experimental information as there is readily available. While the emphasis will be on TREAT experiments, steady state irradiation data will also be examined.

3) Experimental Design

During this study it was noted that there is a decided lack of material property data for both the fuel and the cladding, especially at elevated temperatures and under irradiation

conditions. The more critical values that are required will be documented. In addition to this, it is anticipated that certain key transient experiments will be outlined that will help to better define mechanisms and cladding failure criteria.

4) Common Method of Data Reporting

Experimenters frequently report their transient data in terms of an integrated or a peak TREAT power or in terms of cal/g of fuel. While this may be a convenient way to report data, these values are not very useful for establishing fuel-cladding damage criteria. An attempt will be made to put the data on a common basis, such as, areal percent of fuel melting, fuel temperature profiles, peak cladding temperature, etc.

5) Generalized Failure Criteria

The mechanistic approach taken in this report allows fuel-cladding damage criteria to be estimated for the rather specific accident cases that were considered. It is recognized that a more general approach is desirable that would allow the reactor designer to consider any combination of reactivity transients and reduced flow conditions without going through a detailed analysis for each specific case. An attempt will be made to define an analytical approach that would yield a definition of general failure criteria.

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APPENDIX I

PREDICTIONS OF DEFORMATIONS IN FTR FUEL PINS UNDER STEADY STATE OPERATION

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I. INTRODUCTION

This appendix section summarizes the results of calculations of diameter changes in FTR fuel pins during steady state operation and briefly describes the methods employed to make the estimates.

This work essentially represents the state of development of a general Fuel Prediction and Analysis System as it existed in December 1969. This system is continually being improved as greater insight is gained into basic material properties and engineering behavior, but sufficient confidence is now placed in the system to warrant summarizing the predictions contained herein. This section is intended for internal BNW usage and is meant to be a convenient reference for selected fuel parameters and anticipated structures and deformations. No attempt is made to provide a review of the state of the knowledge that exists with regard to mixed-oxide stainless steel clad fuel nor to discuss any of the other modeling efforts that are underway at other laboratories.

II. ASSUMPTIONS

A. Fuel Parameters

The fuel pin design and irradiation conditions selected for this analysis are summarized in Table AI.I.

TABLE AI.1. FTR Fuel Pin Parameters

Item	Value	Units
Fuel Material	75% UO ₂ , 25 PuO ₂ (O/M = 1.97)	NA
Cladding Material	316 SS (20% cold worked)	NA
Cladding, OD	0.230	inches
Cladding, ID	0.200	inches
Cladding, thickness	0.015	inches
Pellet diameter	0.194	inches
Fuel/cladding Gap (diametral)	0.006	inches
Fuel length	36	inches
Plenum length (effective)	36.8	inches
Pellet density	90.5	% theoretical
Smeared density	85.0	% theoretical
Minimum power (pin peak)	4.8	kW/ft
Average power (pin peak)	8.7	kW/ft
Maximum power (pin peak)	12.2	kW/ft
20% overpower (pin peak)	14.6	kW/ft
Burnup (Maximum)	80,000	MWd/MTM

The power levels selected were based on estimates of the power in an average FTR pin, a radial peak-to-average power ratio of 1.40 and an axial peak-to-average power ratio of 1.24. Twenty percent overpower is considered to be the maximum overpower condition that is likely to be encountered in the operation of FTR. The power in an average pin was estimated by considering the following:

- a) The FTR generates 400 MW of power of which 90% or 360 MW is deposited in the fuel. The other 10% or 40 MW is deposited in the rest of the reactor components.
- b) The FTR contains 76 driver fuel subassemblies with 217 pins in each subassembly.
- c) The pins have 3 foot long fuel sections.
- d) There are three open loops each considered to be equivalent to 3/4 of a driver subassembly or collectively equivalent to 2.25 driver fuel subassemblies.

Thus

$$\text{Ave. pin power} = \frac{360 \times 10^3}{217 (78.25)^3} = 7.0 \text{ kW/ft}$$

and the peak power of an average pin is

$$7.0 \times 1.24 = 8.7 \text{ kW/ft.}$$

Also, the peak power of a pin in the center of the core is

$$8.7 \times 1.40 = 12.2 \text{ kW/ft.}$$

The 20% overpower condition is, therefore, considered to be

$$12.2 \times 1.2 = 14.6 \text{ kW/ft.}$$

A pin at the edge of the core (minimum power) is estimated to have a peak axial power of 4.8 kW/ft.

The peak flux was taken to be $7 \times 10^{15} \text{ n}/(\text{sec-cm}^2)$ of which 65% is considered to have $E > 0.1 \text{ MeV}$. Flux in the other positions is assumed to be proportional to the pin power. The axial flux profile is that of a chopped cosine curve with an extrapolation distance of 0.625 feet. A uniform fuel composition was assumed throughout the core so that the flux-to-pin-power ratio is constant for all cases. Table AI.II summarizes the power and flux assumptions.

TABLE AI.II. Power and Flux Estimates for FTR Fuel Pins

Pin Type	Pin Ave., kW/ft	Pin Peak, kW/ft	Total Flux		E > 0.1 MeV	
			ϕ_{ave} x 10^{-15}	ϕ_{max} x 10^{-15}	ϕ_{ave} x 10^{-15}	ϕ_{max} x 10^{-15}
Minimum	3.9	4.8	2.26	2.8	1.47	1.82
Average	7.0	8.7	4.03	5.0	2.62	3.25
Peak	9.8	12.2	5.64	7.0	3.67	4.55
20% Overpower	11.8	14.6	6.77	8.4	4.40	5.46

The behavior of the fuel is predicted to a burnup as high as 80,000 MWd/MTM in approximately 20,000 MWd/MTM increments. The four power levels discussed above were assumed to operate to peak burnup at the same initial power. No effort was made to analyze the effect of shuffling fuel to a higher or lower power position and no reduction in power due to burnup effects was considered.

B. Heat Transfer Parameters

Practically all of the predictions of fuel performance depend to a large extent on the heat transfer calculations since everything that is thought to influence fuel behavior is temperature sensitive. Our computer code SINTR⁽¹⁾ was used to generate the pertinent temperature information. This is a fairly standard heat transfer code that uses the integral of kdT for the fuel that was determined by Baily⁽²⁾ and Stevens^(2a) from thermal conductivity measurements of mixed oxide fuels. The code considers both the effects of temperature and fuel density on the thermal conductivity. Table AI.III summarizes the pertinent heat transfer assumptions and the coolant mass flow rates employed in this analysis. Note that the flow rate increased with pin power up to 12.2 kW/ft but did not increase beyond the 12.2 kW/ft rate for the 14.6 kW/ft case.

None of the values shown in Table AI.III were allowed to change with burnup. This is probably not completely true for such things as the fuel/cladding gap coefficient and thermal conductivity but since we have no functional relationship describing how these parameters change with burnup, we are forced, at the present time, to ignore this effect.

TABLE AI.III. Pertinent Heat Transfer Parameters

Parameter	Value	Unit
Inlet coolant temperature	600	°F
Sodium mass flow rate - 4.8 kW/ft	327	lb/hr
Sodium mass flow rate - 8.7 kW/ft	593	lb/hr
Sodium mass flow rate - 12.2 kW/ft	830	lb/hr
Sodium mass flow rate - 14.6 kW/ft	830	lb/hr
Coolant specific heat	0.305	Btu/lb-°F
Coolant-Cladding Film Coefficient	37,500	Btu/ft ² -°F-hr
Cladding conductivity	11.4	Btu/ft-°F-hr
Fuel-Cladding Gap Coefficient	1000	Btu/ft ² -°F-hr
Sintering temperature*	1700	°C
	3090	°F
Sintered density**	98	% theoretical
Equiaxed Grain Growth Temperature	1450-1700	°C
	2640-3090	°F

* Temperature for initiation of columnar grain formation.

** Density to which the columnar grains sinter.

C. Thermal Expansion

The SINTR code⁽¹⁾ also calculates the thermal expansion of the fuel and the cladding. The temperature dependence of the coefficient of thermal expansion for unirradiated Type 316 stainless steel is shown in Figure AI.1.⁽³⁾ For cladding thermal expansion we use

$$DP_{hot} = DP (1 + \alpha \Delta T)$$

where DP_{hot} = hot cladding diameter

DP = cold cladding diameter

ΔT = (ave. clad. temp. - 70) °F

α = temperature corrected coefficient

$$= \alpha_o + \alpha_s \Delta T$$

where $\alpha_o = 8.55 \times 10^{-6}$ in./in.-°F

$\alpha_s = 1.47 \times 10^{-9}$ in./in.-°F²

For fuel thermal expansion we use the expression

$$DF_{hot} = DF (1 + 1.72 \times 10^{-4} + 6.8 \times 10^{-6} T + 2.9 \times 10^{-9} T^2)$$

where DF_{hot} = hot fuel diameter

DF = cold fuel diameter

T = vol. mean fuel temp., °C

This relationship is only valid above about 500 °C.

D. Irradiation Induced Cladding Swelling

The volumetric swelling due to irradiation induced void formation was determined using the relation for volumetric swelling for cold worked 316 stainless steel:⁽⁴⁾

$$\Delta V/V = 1.01 \times 10^{-36} (\phi t)^{1.69} (e^{-7800/RT} - 5480 e^{-25300/RT})$$

AI.7

FIGURE AI.1. UNIRRADIATED TYPE 316 SS THERMAL EXPANSION, MEAN FROM RT (70°F) (3)

where ϕ = flux, neutrons/cm²-sec

t = time, sec

T = temperature, °K

R = gas constant (1.987 calories/degree mole)

It is recognized by the authors that the expression for cladding swelling is only a best fit of preliminary stainless steel swelling data and should probably be stress dependent. However, until the swelling of fuel pin cladding is better understood, this empirical expression is the best engineering approximation that is possible with the data available.

E. Unrestrained Fuel Swelling

The prediction of unrestrained fuel swelling is currently accomplished using EXPAND,⁽⁵⁾ a computer code developed at General Electric to predict the swelling of oxide fuel in a fast neutron flux. The program calculates the swelling from the accumulation of gaseous and solid fission products in 50 annuli across the pellet radius and calculates the volumetric fuel expansion by assuming that each annulus expands independent of restraint which may occur from other annuli. The solid fission product swelling is based upon an analysis of Anselin⁽⁶⁾ in which the solid fission product swelling was predicted to cause a volumetric expansion of 0.32 volume percent per atom % burnup. Gaseous expansion is calculated by assuming several models:

- 1) Fission gas bubbles within the grains are restricted by hydrostatic, surface tension, and viscous forces. The bubble size is limited by forces which are a function of the fuel temperature and the temperature gradient.
- 2) Swelling also occurs from the formation of gaseous bubbles at grain boundaries. If a central void is predicted, the size of bubbles at grain boundaries is limited by forces which are a function of the fuel temperature and the temperature gradient. If a central void is not predicted to

form, the size of bubbles at the grain boundary is limited by assuming that bubble diameters cannot exceed the assumed bubble spacing.

Only a portion of the initial porosity within the fuel is assumed to accommodate any of the fission product swelling. Porosity in fuel, which does not exceed 1265 °C, is assumed not to close and to remain in essentially an undisturbed state throughout life. Porosity that is present in the columnar region of the fuel is assumed to be swept to the fuel center to form a central void. Initial porosity between the columnar grains and 1265 °C is assumed to close before any swelling of the annuli between 1265 °C and columnar grain growth occurs. The columnar grain radius is assumed to occur at a particular temperature in the fuel that is burnup dependent.* The minimum temperature for columnar grains is calculated by:

$$T = \frac{31200.}{11.0 + \log_{10} B}$$

where T = minimum temperature for columnar grain formation, °K

B = burnup (MWd/MTM)

EXPAND allows one to calculate the isotropic volumetric expansion due to the accumulation of fission products by providing the following input: a pellet surface, temperature, pellet radius, a linear power, an activation energy for creep of the fuel matrix, an activation energy for gaseous diffusion and the burnup in MWd/MTM.

The activation energy for creep of the fuel matrix is believed to depend upon the stoichiometry of the fuel and at temperature less than 1500 °F upon the neutron flux. For

* The 1700 °C sintering temperature assumed in the SINTR code is reached at 50,000 MWd/MTM according to this expression.

the purpose of this calculation an activation energy, that is independent of neutron flux, of 60,000 calories-mole⁻¹-°C⁻¹ was selected. It is the lowest value that has been reported for UO₂ (hyperstoichiometric) and results in the maximum fuel swelling that is thought feasible. The confidence of the calculation will be improved when creep rate data for mixed oxides becomes available and the stoichiometry of the fuel during irradiation is better understood.

III. RESULTS

A. Temperature Distributions

The radial temperature profiles are shown for each of the four cases considered in Figure AI.2 and the axial temperature profiles are indicated in Figures AI.3 through AI.6. The average cladding temperature for the four power levels is depicted in Figure AI.7 as a function of axial position. The centerline temperature that is included in Figures AI.3 through AI.6 is the maximum fuel temperature that would have been achieved if the fuel had not sintered, forming a central void.

B. Fuel Structure

The structure that is anticipated in the FTR driver fuel after an appreciable burnup was simulated on the Hybrid computer. This simulation was accomplished by programming SINTR onto the Hybrid and displaying the simulated fuel structure on a cathode ray tube. The display was recorded photographically. Figures AI.8 through AI.10 show the predicted structures for the three higher power levels for a transverse section at the midpoint of the pin and for an axial section at the fuel centerline. No restructuring occurred in the low power pin. In the other three pin types, the transverse structure is simulated by concentric circles representing fuel surface, beginning of equiaxed grain growth, beginning of columnar grains and the central void. The axial section is a much foreshortened view of the entire 36 inch length of the fuel column. In this view the central

FIGURE AI.2. RADIAL TEMPERATURE DISTRIBUTION IN TYPICAL FTR FUEL PINS (AXIAL MIDPOINT)

FIGURE AI.3. AXIAL TEMPERATURE PROFILE IN AN FTR FUEL PIN OPERATING AT 4.8kW/FT

FIGURE AI.4. AXIAL TEMPERATURE PROFILE IN AN FTR FUEL PIN OPERATING AT 8.7 kW/FT

FIGURE AI.5. AXIAL TEMPERATURE PROFILE IN AN FTR FUEL PIN OPERATING AT 12.2 kW/FT

FIGURE AI.6. AXIAL TEMPERATURE PROFILE IN AN FTR FUEL PIN OPERATING AT 14.6 kW/FT

FIGURE AI.7. AXIAL TEMPERATURE PROFILE OF THE AVERAGE CLADDING TEMPERATURE FOR FTR FUEL PINS

TRANSVERSE SECTION AT 18"

LONGITUDINAL SECTION AT FUEL
CENTERLINE

FIGURE AI.8. HYBRID COMPUTER SIMULATION OF THE EXPECTED
STRUCTURE IN AN FTR FUEL PIN OPERATING AT
8.7 kW/FT

TRANSVERSE SECTION AT 18"

LONGITUDINAL SECTION AT FUEL
CENTERLINE

FIGURE AI.9. HYBRID COMPUTER SIMULATION OF THE EXPECTED
STRUCTURE IN AN FTR FUEL PIN OPERATING AT
12.2 kW/FT

TRANSVERSE SECTION AT 18"

LONGITUDINAL SECTION AT FUEL
CENTERLINE

FIGURE AI.10. HYBRID COMPUTER SIMULATION OF THE EXPECTED
STRUCTURE IN A FTR FUEL PIN OPERATING AT
14.6 kW/FT

void is depicted as the shaded region and the columnar grain zone, equiaxed grain zone and the fuel surface are depicted as dotted lines.

At the higher power levels, the layer of equiaxed grains becomes quite thin due to the fact that the temperature range over which they are formed is located in a very steep temperature gradient. Likewise, the thickness of the nonrestructured fuel layer is also quite thin at the higher power levels. The validity of the assumptions used in this simulation have been verified⁽⁷⁾ for low burnup ($\sim 10,000$) and low power (~ 9 kW/ft) but similar verification is lacking for the higher burnup and high power conditions.

C. Cladding Expansion

The cladding can increase in diameter from thermal expansion, irradiation induced cladding swelling, and cladding stresses. Figures AI.11 through AI.14 depict the axial percent increase in diameter along the rod length due only to irradiation swelling for several burnup levels for the four power cases considered. Since we assumed a constant burnup rate to flux ratio, the only variation with power is due to the slightly hotter cladding at the higher power levels. As the coolant flow was allowed to increase with increasing power (except for 14.6 kW/ft) the temperature differences are relatively small. The maximum diameter increase predicted at 80,000 MWd/MTM is about 2.5% for the 14.6 kW/ft pin. The other three power levels would be expected to show about 2% diameter increase at 80,000 MWd/MTM due to "swelling".

Thermal expansion is also a significant contributor to total cladding expansion. Figures AI.15 through AI.18 show the radial increase in cladding dimensions due to thermal expansion plus cladding swelling. Up to 12.2 kW/ft, the maximum radial expansion is estimated to be a little over 3 mils. Due to the higher temperature in the 14.6 kW/ft pins, the total radial expansion is slightly over 4 mils.

FIGURE AI.11. FTR AXIAL CLADDING SWELLING PROFILE MIN. POWER PIN 4.8 kW/FT

AI.22

FIGURE AI.12. FTR AXIAL CLADDING SWELLING PROFILE AVG. POWER PIN 8.7 kW/FT

AI.23

FIGURE AI.13. FTR AXIAL CLADDING SWELLING PROFILE MAX. POWER PIN 12.2 kW/FT

AI.24

FIGURE AI.14. FTR AXIAL CLADDING SWELLING PROFILE 20 PCT OVERPOWER PIN 14.6 kW/FT

FIGURE AI.15. RADIAL EXPANSION OF CLADDING DUE TO SWELLING AND THERMAL EXPANSION

FIGURE AI.16. RADIAL EXPANSION OF CLADDING DUE TO SWELLING AND THERMAL EXPANSION

FIGURE AI.17. RADIAL EXPANSION OF CLADDING (MILS) DUE TO SWELLING AND THERMAL EXPANSION

FIGURE AI.18. RADIAL EXPANSION OF CLADDING (MILS) DUE TO SWELLING AND THERMAL EXPANSION

The third source of cladding diameter increase that was considered with the SINTR code was stress from the thermal gradient across the wall of the cladding and from the internal pressure buildup due to the release of fission gas and gases present from fuel pin fabrication. On the basis of unirradiated material properties, the stresses remained elastic for all power levels considered to 80,000 MWd/MTM even though it was assumed for purposes of these calculations that 100% of the fission gas was released. Thus no significant dilatation would be expected from these sources of stress. There may be other sources of stress that we have not evaluated such as the stress developed from fuel cladding interaction and from a possible differential swelling across the wall of the cladding due to the thermal gradient. These are discussed later.

D. Fuel Expansion

A chopped cosine power distribution was used to predict the axial power and burnup profiles along the pin axis. The pin power was assumed to be constant throughout life. At 20,000 MWd/MTM peak burnup, the following burnup and power levels profiles were determined (Table AI.4).

Using the corresponding burnup and linear power as a function of axial position and the predictions shown in Figure AI.19, the total volumetric swelling was calculated for all four peak linear power levels at peak burnup of 20,000; 40,000; 60,000 and 80,000 MWd/MT. The volumetric swelling that was predicted is shown in Figures AI.20 through AI.23.

Estimating fuel swelling involves differences between complex exponential terms which are highly sensitive to the initial conditions assumed. The estimates indicated in Figures AI.19 through AI.23 are believed conservative in that conditions were selected that would give rise to the maximum fuel swelling that would be anticipated in the FTR. These estimates will be revised as our comprehension of fuel swelling improves

TABLE AI.4. Burnup and Linear Power Distribution Along Pin Axis

Distance, inches	Burnup, MWd/MT	Linear Power, kW/ft			
		Case No. 1	Case No. 2	Case No. 3	Case No. 4
0	11,100		4.7	6.4	8.12
3	12,180	2.9	5.3	7.5	8.89
5	13,720	3.3	6.0	8.3	10.0
7	15,180	3.6	6.6	9.3	11.07
9	16,600	4.0	7.2	10.1	12.13
11	17,900	4.3	7.8	10.9	13.07
13	18,900	4.55	8.2	11.5	13.78
15	19,600	4.7	8.5	12.0	14.31
17	20,000	4.8	8.7	12.2	14.6
19	20,000	4.8	8.7	12.2	14.6
21	19,600	4.7	8.5	12.0	14.3
23	18,900	4.55	8.2	11.5	13.78
25	17,900	4.3	7.8	10.9	13.07
27	16,600	4.0	7.2	10.1	12.13
29	15,180	3.6	6.6	9.3	11.07
31	13,720	3.3	6.0	8.3	10.0
33	12,180	2.9	5.3	7.5	8.89
36	11,100		4.7	6.4	8.12

and we are able to incorporate other phenomena such as fuel cracking and radial fuel ratcheting into our fuel performance models. It is generally felt that these phenomena also contribute to gap closure (in addition to fuel swelling) but we are not yet able to express this mathematically.

To obtain the total fuel expansion, the thermal expansion of the pellet should be added to the swelling caused by the accumulation of fission products. A temperature dependent thermal expansion for UO_2 was used for the prediction of thermal expansion:⁽⁸⁾

FIGURE AI.19. PREDICTED VOLUMETRIC SWELLING OF MIXED OXIDE FUEL

FIGURE AI.20. PREDICTED $\Delta V/V$ ISOTROPIC VERSUS LENGTH AT 20,000 MWd/MT PEAK FOR 4 PEAK LINEAR POWERS

FIGURE AI.21. PREDICTED $\Delta V/V$ ISOTROPIC VERSUS LENGTH AT 40,000 MWd/MT PEAK BURNUP FOR 3 LINEAR POWER LEVELS

FIGURE AI.22. PREDICTED FUEL $\Delta V/V$ ISOTROPIC VERSUS LENGTH AT 60,000 MWd/MTM PEAK BURNUP FOR 3 LINEAR POWERS SHOWN

FIGURE AI.23. PREDICTED $\Delta V/V$ ISOTROPIC OF FUEL AT 80,000 MWd/MTM PEAK FOR 3 LINEAR POWERS

$$\Delta L/L = 2.9 \times 10^{-9} T^2 + 6.8 \times 10^{-6} T + 1.72 \times 10^{-4}$$

where T = the volumetric average fuel temperature in °C. GE-APO⁽⁹⁾ has confirmed the applicability of this expression for mixed oxides in the 1000 °C range. This relationship is assumed to apply for mixed oxide for temperatures from about 600 °C to the melting point. The change in radius due to thermal expansion is shown as a function of fuel column length in Figure AI.24 for the cases considered. When thermal expansion effects are now added to the predicted fission product swelling, the relationships depicted in Figures AI.25 through AI.28 are obtained for estimates of the changes in fuel radius. The change in the outer radius of the fuel was calculated from the volumetric expansion $\Delta V/V$ shown in Figures AI.20 through AI.23 by assuming $\Delta r/r = 1/3(\Delta V/V)$ for isotopic swelling and $\Delta r/r = 1/2(\Delta V/V)$ for swelling with zero axial motion.

E. Fuel Cladding Interaction

Initially there is a radial gap between the fuel and cladding of 3 mils. The variation of the inside cladding diameter is typically less than ± 0.001 inches. The uncertainty in the pellet radius is ± 0.0005 inches.

Figure AI.29 shows the cladding expansion from swelling and thermal expansion added to an initial fuel-cladding radial gap of 3 mils for the pin operating at a peak linear power of 4.8 kW/ft as a function of length. Superimposed is the calculated radial expansion of the fuel from swelling and thermal expansion and the uncertainty band due to pellet diametrical tolerances. It is seen that fuel cladding interaction is only predicted for the low power fuel at 20,000 MWd/MT if the fuel is restrained axially. Maximum cladding strain should occur approximately 11 inches from the end of the fuel column.

The fuel at higher peak power levels was examined for fuel cladding interaction in a similar manner but no fuel cladding interaction was predicted to 80,000 MWd/MTM (the highest examined).

FIGURE AI.24. THERMAL EXPANSION OF FUEL VERSUS LENGTH FOR FOUR LINEAR POWER LEVELS SHOWN

FIGURES AI.25 & 26. CHANGE IN PELLET RADIUS FROM THERMAL EXPANSION PLUS FUEL SWELLING

FIGURES AI.27 & 28. CHANGE IN PELLET RADIUS FROM THERMAL EXPANSION PLUS FUEL SWELLING

FIGURE AI.29. PREDICTION OF FUEL-CLADDING INTERACTION IN AN FTR FUEL PIN OPERATING AT 4.8 kW/FT

The possibility of fuel-cladding interaction was examined for a pin operating at 12.2 kW/ft using the CYGRO-2⁽¹⁰⁾ code. This code also predicted that no interaction would occur and that the cladding stresses would be elastic at the end of life; in agreement with the SINTR/EXPAND code.

As an additional check on cladding stress, the PECT-1⁽¹¹⁾ code was run for pins operating at 14.6 and 12.2 kW/ft. These calculations showed that Type 316 stainless steel (20% cold worked) remained in the elastic state throughout its lifetime on the basis of unirradiated mechanical properties. It is assumed that PECT-1 would also have indicated elastic behavior in the lower power pins.

IV. DISCUSSION

The estimates of steady state deformations in four typical FTR fuel pins (4.8, 8.7, 12.2 and 14.6 kW/ft) indicate that irradiation induced cladding swelling will be the predominant mode of deformation. On the basis of the assumptions made, the fuel swelling never exceeds the cladding swelling to produce fuel-cladding mechanical interactions. For the low power pin, it was possible to show that the fuel and cladding almost come in contact and, if one assumes that all of the fuel swelling goes to increase the radius of the fuel (i.e., zero length change), it was shown that interaction may occur at about 20,000 MWd/MTM. This occurs because more gas is retained in the low power pin. It is important to realize that these estimates are based on unrestrained swelling of the fuel being the only mechanism for closing the fuel-cladding fabrication gap. We recognize that the fuel cracks, that cracks partially heal and that new cracks probably form as in-reactor cycling occurs. This may well constitute a more important mechanism for closing the gap than does fuel swelling, per se. However, at this point in time the relationships between gap closure

and pin power, burnup, and thermal cycles are not well established so that it is very difficult to treat this process analytically.

Calculations of cladding deformation were made using DEFORM⁽¹²⁾ in which it was arbitrarily assumed that the gap had closed at 4% B.U. The results of these calculations are shown in Figure AI.30 which plots the maximum percent $\Delta D/D$ as a function of burnup for the four power levels considered. This version of DEFORM employed a fuel swelling relationship that depends on burnup and temperature (different than EXPAND). There are reasons to believe that the calculations shown in Figure AI.30 overestimate the deformations, for analysis of GE F-2 Series irradiation data⁽¹³⁾ indicate that cladding swelling only accounts for the observed fuel pin diameter changes.

Efforts are continuing to use the formulations of EXPAND in the DEFORM code (this entails extensive reprogramming) and to allow the fuel-cladding gap to close by some mechanism other than fuel swelling as estimated by EXPAND. This capability should substantially improve our ability to analyze fuel pin irradiation data and permit a more realistic appraisal of the FTR fuel pin behavior.

The estimates of cladding stress indicated that the stress should remain elastic for all conditions to the end of life (80,000 MWd/MTM). This was based on stress from internal pressure buildup assuming 100% fission gas release and from the thermal gradient across the cladding. The expected hoop stress from these sources is plotted across the wall of the cladding in Figure AI.31 for the start of steady state and the end of life for these "usual considerations" for a pin operating at 12.2 kW/ft. The difference between start of steady state and end of life is due to the fission gas pressure buildup. As can be seen in Figure AI.31, the cladding stress is not very sensitive to fission gas release under steady state conditions.

FIGURE AI.30. MAXIMUM CHANGE IN DIAMETER OF 0.230 IN.OD FTR FUEL PIN-CLADDING SWELLING + FUEL - CLADDING INTERACTION - DEFORM PREDICTIONS AT 4 A/O B.U. INCUBATION

FIGURE AI.31. CLADDING STRESS IN AN FTR FUEL PIN
80,000 MWd/MTM AT 12.2 kW/FT

Since the cladding has a thermal gradient across it, it is also possible to consider stresses that may develop due to a gradient in the irradiation induced swelling that may occur across the wall of the cladding. Figure AI.31 also includes the results of this calculation with and without plastic flow at the end of life. This consideration far overshadows any other source of stress that we have examined and represents an area of major uncertainty in all of the calculations done to date. This raises the obvious question about the effects of stress and/or plastic flow on the stainless swelling process per se for compressive stresses may tend to reduce swelling while tensile stress may tend to increase swelling. The net effect would be to significantly reduce the magnitude of the stresses built up in the cladding but to increase the overall diameter increase of the pin due to cladding swelling. Little additional analysis can be done along these lines until more is understood about the effects of stress on stainless swelling and the nature of in-reactor stainless steel creep. Efforts are underway to measure residual stress in fast flux irradiated fuel pin cladding in order to gain insight as to the importance of this consideration on fuel pin deformations.

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APPENDIX II

CLADDING FAILURE FROM FISSION GAS WITHIN PELLET DURING UNINTERRUPTED OVERPOWER TRANSIENT

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POWER TIME HISTORY

The power time history during an uninterrupted 5\$/second transient is shown in Figure A.II.1. (Reference 1)

GEOMETRY AND POWER LEVEL

The peak power FFTF rod was chosen for analysis. The steady-state power is 12.2 kW/ft and the cold fuel configuration is shown in Figure A.II.2.

POWER PROFILE

The case considered was a fast flux irradiation of a mixed oxide fuel. No flux depression factor through the fuel was assumed. Consequently, the radial power generation was constant.

The axial power profile assumes a chopped cosine distribution which is symmetric about the pin center and an extrapolation distance of 0.625 feet. It is shown in Figure A.II.3.

FUEL SURFACE TEMPERATURE

The axial pellet temperature profiles that are estimated to exist under steady-state conditions are shown in Figure A.II.4. These temperatures were recalculated for the power transient with TIGER V using an inlet cooling temperature of 600 °F, a sodium mass flowrate of 830 lb/hour a coolant-cladding film coefficient of 37,500 Btu/ft²-hr-°F, a cladding thermal conductivity of 11.4 Btu/ft-°F-hr, and a fuel-cladding gap coefficient of 1200 Btu/hr-ft²-°F.

INTERNAL FUEL TEMPERATURES

The temperature profile within the pin assumed a two region model. The outer region below 1800 °C has a density

FIGURE A.II.1. RELATIVE REACTOR POWER VERSUS TIME FOLLOWING A 5\$/SECOND TRANSIENT (1)

FIGURE A.II.2. COLD FUEL CONFIGURATION OF REFERENCE FFTF FUEL PIN

- A. CLADDING WALL 0.015 ± 0.001 INCHES
316-20% CW STAINLESS STEEL
- B. CLADDING WALL END OF LIFE 0.010 INCHES
- C. PEAK BURNUP AT END OF LIFE 80,000 MWd/MTM
- D. FUEL IS 75 W/O UO_2 · 25 W/O PuO_2 (O/M=1.97)
WITH FABRICATED PELLET DENSITY OF
 $90.5\% \pm 2.0\%$

FIGURE A.II.3. ASSUMED POWER PROFILE OF PEAK ROD

A.II.4

FIGURE A.II.4. STEADY STATE AXIAL TEMPERATURE DISTRIBUTION OF FUEL AND CLADDING AND SIZE OF CENTRAL VOID DURING STEADY STATE OPERATION OF REFERENCE FUEL PIN AT 12.2 kW/FT

of 90.5% theoretical and has a conductivity of

$$\int_s^{1800} k dT = \frac{T^{0.613}}{(6.59 - 5.3D)} - \frac{(0.5D + 1.53)}{\exp(2.77 \times 10^{-3}T)}$$

where D = fuel pellet density

The region above 1800 °C is assumed to have a density of 98% theoretical and a conductivity of (2)

$$\int_{1800}^{T_G} k dT = 0.0228 (T - 1800)$$

Figures A.II.5 through A.II.10 show the temperature profile within the fuel as a function of time following an uninterrupted 5\$/second power excursion shown in Figure A.II.1. The axial location of each set of curves is shown on each graph.

FRACTION OF FUEL CONTAINING COLUMNAR GRAINS

The temperature which defines the outer boundary for columnar grains is expected to decrease with burnup. An empirical expression which is used to predict this temperature is

$$T_c = \frac{31200}{11.0 + \log_{10} B}$$

where

T = minimum temperature for
columnar grains (°K)

B = fuel burnup in MWd/MT.

The fraction of the fuel cross section which is above T_c at 20,000 and 80,000 MWd/MTM is plotted in Figure A.II.11. The fraction becomes less toward the ends of the pin because of the reduction in burnup toward the pin extremities.

FIGURE A.II.5. RADIAL TEMPERATURE PROFILES FOLLOWING A 5\$/SECOND TRANSIENT AT 35 INCHES FROM BOTTOM OF FUEL

FIGURE A.II.6. RADIAL TEMPERATURE PROFILES FOLLOWING A 5\$/SECOND TRANSIENT AT 25 INCHES FROM BOTTOM OF FUEL COLUMN

FIGURE A.II.7. RADIAL TEMPERATURE PROFILE FOLLOWING A 5\$/SECOND TRANSIENT AT 19 INCHES FROM BOTTOM OF FUEL

FIGURE A.II.8. RADIAL TEMPERATURE PROFILE FOLLOWING A 5\$/\$SECOND TRANSIENT AT 13 INCHES FROM BOTTOM OF FUEL

FIGURE A.II.9. RADIAL TEMPERATURE PROFILE FOLLOWING A 5\$/SECOND TRANSIENT AT 7 INCHES FROM BOTTOM OF FUEL

A.II.12

FIGURE A.II.10. RADIAL TEMPERATURE PROFILE STEADY STATE AT A POSITION 1 INCH FROM BOTTOM OF FUEL

A.II.13

FIGURE A.II.11. FRACTION COLUMNAR VERSUS LENGTH AND BURNUP

SIZE OF CENTRAL VOID

Once the fraction of the cross section composed of columnar grains is known along the length of the pin the central void volume can be determined. This volume is calculated by assuming that the fabricated porosity inside the columnar fuel region is combined to form a central hole. The size of this void is integrated over the length of the pin to determine the central void volume. The size of this void is an important prediction as it serves as the containment volume for any gases released during a power transient.

FRACTION OF FUEL RELEASING GAS TO CENTRAL VOID

Before the prediction of fuel failure due to the increase in gas pressure in the pin from an overpower transient can proceed, a model of the fuel structure and unreleased fission gas distribution within the fuel pellet is required. Since the radial distribution of fission gas within the pellet is thought to depend on both the local axial power level as well as burnup, many calculations are required along the length of the pin in order to predict both the quantity of gas released to the plenum prior to the transient and the gas released to the central void during the transient.

The following analysis assumes a two region fuel model composed of either columnar or equiaxed grains. The size of the columnar region is assumed to be dependent upon burnup and contain no fission gas that could be released during an overpower transient. This assumption is believed realistic because the columnar region is known to be constantly swept by lenticular voids which migrate to the center of the fuel and create a central void. The size of the central void which makes up the containment volume for the gases swept out of the fuel during the transient is determined by assuming that all of the initial fabricated porosity in that portion of fuel calculated to have

columnar grains is swept to the fuel center. Since the power level is assumed to decrease axially along the pin by a chopped cosine power profile, the fraction of fuel that is columnar and, therefore, the size of the central void is predicted to decrease with increasing distance from the axial position of peak power. The size of the central void is then integrated axially to predict the volume of the central void.

The fraction of fuel releasing gas to the central void is determined by finding the fraction of the fuel area melted which is in excess of the fraction which is columnar. The fuel region outside the columnar zone is assumed to have retained all of the gas generated there. The fission gas trapped in that portion of the fuel is determined and 100% of this gas is deposited in the central void when this fuel melts. This process is performed over the pin length and the total gas pressure is determined using the perfect gas law. The gas temperature is assumed to be at the fuel melting point.

The fraction melted at each axial position can be taken directly from Figures A.II.5 through A.II.10. The fraction melted is obtained along the abscissa where the fuel temperature exceeds the melting point. The fraction which is columnar is found where the time = 0 temperature profile crosses the predicted value of T_c defined in the previous equation (at burnup = 80,000). The fraction releasing gas is the fraction melted minus the fraction columnar if the result is positive. This fraction is shown in Figure A.II.12.

CALCULATION OF FISSION GAS PRESSURE

In the calculation of the fission gas pressure it is assumed that the fuel melting is 90.5% theoretical density. The total yield of fission gas is assumed to be 30% and 200 MeV are generated per fission.

A.II.16

FIGURE A.II.12. FRACTION MELTED IN EXCESS OF FRACTION COLUMNAR VERSUS LENGTH AND TIME

a. Fuel Density

$$\rho = 11.055 (0.905) = 9.97 \text{ gm/cm}^3$$

b. Moles of Metal/cm³

$$Mm^2 = 9.97 \frac{\text{gm}}{\text{cm}^3} \left[\frac{238.25}{270} \right] \left[\frac{1}{238.25} \right] = 3.69 \times 10^{-2} \frac{\text{moles}}{\text{cm}^3}$$

c. Percent Metal Fissioned at 80,000 MWd/MTM

$$Ff = 80,000 (1.22 \times 10^{-4}) = 9.76\%$$

d. Moles of Gas Generated

$$\begin{aligned} Mg &= 3.69 \times 10^{-2} [9.76 \times 10^{-2}] (f_B) (0.30) \\ &= 1.075 \times 10^{-3} \frac{\text{moles}}{\text{cm}^3} \end{aligned}$$

where f_B = burnup ratio to maximum power position.

e. Fission Gas Pressure

$$P = \frac{Mg}{Vv} Vm R T_m$$

where

P = fission gas pressure in central void

Mg = gas generation in moles/cm³

Vv = central void volume determined by integrating void volume along the pin length

Vm = Volume of fuel molten in excess of that columnar

R = gas constant in psi-cm³(mole-°K)

T_m = fuel melting temperature = 2953 °K.

PREDICTION OF PRESSURE INCREASE WITH TIME FOLLOWING
5\$/SECOND OVERPOWER TRANSIENT

Tables A.I.1 through A.I.5 show the results of the calculations previously discussed at different times following a 5\$/second transient.

Column 1 is the axial position in the pin and its distance from the bottom of the fuel column.

Column 2 is the volume fraction of fuel in the axial node.

Column 3 is the fractional area in node above the melting point (4850 °F).

Column 4 is the fraction of the total fuel volume (in entire length) which is molten.

Column 5 is the area fraction which has columnar grains.

Column 6 is the fraction of the entire column that is molten in the segment.

Column 7 is the fractional void volume outside the melt zone in the segment. This column is added over the length to obtain the total void volume available to accommodate fuel melting.

Column 8 is the percent of area in the segment that should be center void. It is found by taking the fraction of the cross section which has columnar voids (Column 5) and multiplying by (1-% density).

Column 9 is the fraction of the total fuel volume which is central void in the segment. It is found by multiplying Column 1 by Column 8. This fraction is also summed over all axial segments to obtain a center void volume.

Column 10 is the kW/ft linear power in the axial node and the moles of fission gas generation/cm³ assuming the center position reaches 80,000 MWd/MT.

Column 11 is the fraction of all the fuel releasing gas. It is found by subtracting Column 6 from Column 4. If the result is positive it is entered here. This column is then summed to obtain a total fuel volume fraction releasing gas. The total fraction is multiplied by 13.6 cm³ in pin of fuel to obtain a volume of fuel releasing gas.

Column 12. This column determines the gas generation in each axial position and this is added to find total number of moles of gas released to the central void.

Column 13 shows the calculated fission gas pressure in the node. It is found by multiplying

$$P \text{ (psi)} = (2953 \text{ }^{\circ}\text{K}) \left(\frac{82 \text{ atm}}{\text{deg mole}} \right) \left[\frac{\text{sum of Column 12}}{\text{sum of Column 9}} \right] \left[\frac{14.7 \text{ psi}}{\text{atm}} \right]$$

The pressure calculated above is added to the pressure of the released fission gas and is assumed to be acting directly upon the interior of the cladding. No credit is taken for the strength of the fuel in confining the pressure. The pressure as a function of time is shown in Figure A.II.13.

REFERENCES

1. Personal Communication with A. E. Waltar
2. E. G. Stevens, "Derivation of Sintering Correction Factor for Fast Oxide Fuels," Trans ANS, vol 11, no. 1, p. 130, June 1968.

TABLE A.II.2. Steady State Power 12.2 kW/ft Peak
 Fuel Density = 90.5%
 Time = 0.293 Seconds
 Transient \$5/second

Axial Position (inches from inlet)	Volume in Node	Area Fraction Melted	Total Fraction Melted	Area Columnar	Fraction of All Fuel Columnar	Void Outside Melt Zone	Fraction $\times$ Section Void	Total Fuel Void	Fission Rate and Gas Generation		Fraction All Fuel Releasing Gas	Moles Gas @STP $\times 10^{-5}$	Gas Pressure from Fuel
									kW/ft	moles/cc $\times 10^{-3}$			
35	0.125	0.02	0.0025	0.125	0.0156	0.0111	0.015	0.0018	8.49	0.745			750 atm
25	0.1875	0.44	0.0825	0.325	0.0608	0.0095	0.039	0.0073	11.1	0.975	0.0217	21.1	11,000 psi
19	0.1875	0.48	0.0900	0.325	0.0608	0.0088	0.039	0.0073	12.2	1.073	0.0292	31.3	
13	0.1875	0.46	0.0860	0.305	0.0572	0.0092	0.036	0.0067	11.6	1.02	0.0288	29.4	
7	0.1875	0.07	0.0130	0.075	0.0140	0.01575	0.009	0.0017	9.5	0.835			
1	0.125					0.0113			6.08	0.535			
TOTAL	1.000				0.2084	0.0656		0.0248		0.018	0.0797	81.8 $\times 10^{-5}$	
									$\times 13.6 \text{ cc} = 0.382 \text{ cc}$				

TABLE A.II.1. Steady State Power 12.2 kW/ft Peak
 Fuel Density = 90.5%
 Time = 0.281 Seconds
 Transient \$5/second

Axial Position (inches from inlet)	Volume % in Node	Area Fraction Melted	Total Fraction Melted	Fractional Area Columnar	Fraction Total Columnar	Void Outside Melt	Fraction $\times$ Section Void	Fraction Total Fuel Void	Fission Rate and Gas Generation		Fraction All Fuel Releasing Gas	Moles Gas @STP $\times 10^{-5}$	Gas Pressure from Fuel
									kW/ft	moles/cc $\times 10^{-3}$			
35	0.1250			0.125	0.0156	0.0119	0.0119	0.0015	8.49	0.745			277 atm
25	0.1875	0.335	0.063	0.325	0.0608	0.0118	0.0309	0.0058	11.1	0.975	0.0022	2.9	4070 psi
19	0.1875	0.380	0.071	0.325	0.0608	0.0105	0.0309	0.0058	12.2	1.073	0.0102	14.9	
13	0.1875	0.350	0.066	0.305	0.0572	0.0110	0.0290	0.0054	11.6	1.02	0.0090	12.5	
7	0.1875			0.075	0.0140	0.0117	0.0071	0.0013	9.5	0.835			
1	0.1250					0.0119			6.08	0.535			
TOTAL	1.000				0.2084	0.0688		0.0198		0.018	0.0214	30.3 $\times 10^{-5}$	
									$\times 13.6 \text{ cc} = 0.269 \text{ cc}$				
1	2	3	4	5	6	7	8	9	10	11	12	13	14

TABLE A.II.3. Steady State Power 12.2 kW/ft Peak
 Fuel Density = 90.5%
 Time = 0.3014 Seconds
 Transient \$5/second

Axial Position (inches from inlet)	Volume in Node	Area Fraction Melted	Total Fraction Melted	Area Columnar	Fraction Total Columnar	Void Outside Melt	Fraction x Section Void	Total Fuel Void	Fission Rate and Gas Generation		Fraction All Fuel Releasing Gas	Moles Gas @STP x 10 ⁻⁵	Gas Pressure from Fuel
									kW/ft	moles/cc x 10 ⁻⁵			
31	0.125	0.10	0.0125	0.125	0.0156	0.0102	0.015	0.0018	8.49	0.745			1145 atm 16,800 psi
25	0.1875	0.505	0.0945	0.325	0.0608	0.0084	0.039	0.0073	11.1	0.975	0.0337	32.9	
19	0.1875	0.570	0.1070	0.325	0.0608	0.0073	0.039	0.0073	12.2	1.073	0.0462	49.5	
13	0.1875	0.550	0.1030	0.325	0.0608	0.0076	0.036	0.0067	11.6	1.02	0.0422	43.0	
7	0.1875	0.140	0.0262	0.305	0.0572	0.0146	0.009	0.0017	9.5	0.835			
1	0.125			0.075	0.0140	0.0113			6.08	0.535			
TOTAL	1.0000		0.3432		0.2692	0.0594		0.0248		0.018	0.1221	125.4	
									13.6 = 0.382 cc				

TABLE A.II.4. Steady State Power 12.2 kW/ft Peak
 Fuel Density = 90.54%
 Time = 0.313 Seconds
 Transient \$5/second

Axial Position (inches from inlet)	Volume in Node	Area Fraction Melted	Total Fraction Melted	Area Columnar	Fraction of All Fuel Columnar	Void Outside Melt Zone	Fraction x Section Void	Frac. Total Fuel Void	Fission Rate and Gas Generation		Fraction All Fuel Releasing Gas	Moles Gas @STP x 10 ⁻⁵	Gas Pressure from Fuel
									kW/ft	moles/cc x 10 ⁻⁵			
31	0.1250	0.200	0.025	0.125	0.0156	0.0091	0.015	0.0018	8.49	0.745	0.0094	9.5	2580 atm 27,900 psi
25	0.1875	0.600	0.112	0.325	0.0608	0.0068	0.039	0.0073	11.1	0.975	0.0512	68.0	
19	0.1875	0.65	0.122	0.325	0.0608	0.0059	0.039	0.0073	12.2	1.073	0.0612	89.2	
13	0.1875	0.625	0.117	0.305	0.0608	0.0064	0.036	0.0067	11.6	1.02	0.0562	78.2	
7	0.1875	0.25	0.047	0.075	0.0140	0.0127	0.009	0.0017	9.5	0.835	0.0330	78.0	
1	0.1250					0.0113			6.08	0.535		37.4	
TOTAL	1.0000		0.423		0.212	0.0522		0.0248			0.2110	282.1	
									13.6 cc = 0.382 cc				

TABLE A.II.5. Steady State Power 12.2 kW/ft Peak
 Fuel Density = 90.5%
 Time = 0.321 Seconds
 Transient \$5/second

Axial Position (inches from inlet)	Volume % in Node	Area Fraction Melted	Total Fraction Melted	Area Columnar	Fraction of All Fuel Columnar	Void Outside Melt Zone	Fraction x Section Void	Frac. Total Fuel Void	Fission Rate and Gas Generation		Fraction All Fuel Releasing Gas	Moles Gas @STP x 10 ⁻⁵	Gas Pressure from Fuel
									kW/ft	moles/cc x 10 ⁻³			
31	0.1250	0.300	0.0375	0.125	0.156	0.0079	0.150	0.0018	8.49	0.745	0.0219	22.2	3600 atm 52,800 psi
25	0.1875	0.665	0.1248	0.325	0.0608	0.0058	0.039	0.0073	11.1	0.975	0.0640	85.5	
19	0.1875	0.715	0.1340	0.325	0.0608	0.0049	0.039	0.0073	12.2	1.073	0.0732	107.0	
13	0.1875	0.695	0.1305	0.305	0.0572	0.0052	0.036	0.0067	11.6	1.02	0.0697	96.6	
7	0.1875	0.460	0.0865	0.075	0.0140	0.0092	0.009	0.0017	9.5	0.835	0.0725	82.4	
1	0.1250					0.0113			6.08	0.535			
TOTAL	1.0000		0.5133		0.2084	0.0443		0.0248			0.3013	393.7	
									x 13.6 cc = 0.382 cc				

A.II.23

FIGURE A.II.13. INTERNAL GAS PRESSURE AFTER 5 $\bar{s}$ /SECOND UNINTERRUPTED TRANSIENT

APPENDIX III

SODIUM BOILING POINT WITHIN CORE

D. C. Bullington

The prediction of cladding failure requires a knowledge of the boiling point of the coolant along the axis of the fuel element. Prediction of cladding failure is based upon cladding temperatures that assume no coolant boiling. Nucleate boiling of the coolant can significantly increase cladding temperatures due to gas blanketing by the sodium vapor.

Figure AIII.1 shows the boiling point of sodium as a function of pressure from Reference 1.

Figure AIII.2 shows the assumed pressure along the fuel rod based upon the temperature drops shown in Table AIII.1.⁽²⁾

Assuming a 36 in. fuel length and a 40 in. plenum at the top of the pin, the local boiling point of sodium along the fuel column is shown in Figure AIII.3.

TABLE AIII.1. Temperature Drops Through the FFTF Core⁽²⁾

Pressure at Sodium Surface	14.7 psi
ΔP Outlet Plenum	4.0
ΔP Through Core:	
Inlet Nozzle	4.0 psi
Axial Shielding	12.0
Bundle Friction	63.0
Inlet and Exit Loss	6.0
Instrumentation	<u>15.0</u>
Total	100.0
ΔP Static Head (30 ft)	<u>12.0</u>
TOTAL	130.7 psi

FIGURE A.III.1. BOILING POINT OF SODIUM AS A FUNCTION OF PRESSURE (1)

A.III.2

A.III.3

FIGURE A.III.2. COOLANT PRESSURE VERSUS CORE LENGTH (2)

A.III.4

FIGURE A.III.3. BOILING POINT OF SODIUM AS A FUNCTION OF CORE LENGTH

REFERENCES

1. G. H. Golden, J. V. Tokar, "Thermophysical Properties of Sodium." ANL-7323, p. 102.
2. A. Padilla, Personal Communication. Based on Westinghouse Core ΔP of 100 psi.

APPENDIX IV
FTR CLADDING STRESS AND DEFORMATION CALCULATIONS
F. E. Bard

A. INTRODUCTION

The FTR cladding stress and deformation calculations are performed by code PECT-1.⁽¹⁾ Code PECT-1 offers a realistic compromise between accurate behavior description and mathematical tractability for a wide class of structural materials. The mechanical model used in PECT-1 describes the elastic, plastic and creep deformations in a long thick-walled cylinder of linear hardening material subjected to transient thermal and mechanical loading. Two assumptions, axial symmetry and plane strain, simplify the field equations of mechanics. The geometry, material and the thermal and mechanical loadings are specified in the following paragraphs.

Geometry

At the start of steady-state, denoted by SSS, the inner and outer radii are 0.100 and 0.115 in., respectively. At the end-of-life, denoted by EOL, they are 0.102 and 0.112 in. The change is due to fuel and cladding interaction on the inner surface (2 mils) and erosion of the outer surface by 3 mils.

Various axial position were chosen which are described by the following terminology:

below midplane

midplane of fuel column

between midplane and top of fuel column

top of fuel column

These locations will be given in terms of inches from the bottom of the fuel column. Four axial positions are the minimum number that should be considered.

Material and Thermal and Mechanical Loading

These will be specified for each of the following conditions:

Loss of coolant transients

6¢/second power transient

5\$/second uninterrupted transient

Steady state consideration

along with the results of each condition. The conclusions will be based on the following criteria.

Strain Criteria

"The maximum uniform elongation should be taken as the lowest value observed in annealed stainless steel at fluence and temperature of interest namely, about 0.2 percent."⁽²⁾ This criterion is based upon uniaxial tension test where the stress and strain are uniform. In a tube (fuel cladding) the stress and strain are not uniform with internal pressure, temperature or swelling gradients as the loading state. To make a comparison between the two, the effective strain is introduced

$$\epsilon_{\text{eff}} = \frac{\sqrt{2}}{3} [(\epsilon_1 - \epsilon_2)^2 + (\epsilon_2 - \epsilon_3)^2 + (\epsilon_3 - \epsilon_1)^2]^{1/2}$$

where ϵ_1 , ϵ_2 , ϵ_3 are the principal components of the strain tensor. The effective strain for a elastic material and a uniaxial test becomes

$$\epsilon_{\text{eff}}^E = \frac{2}{3}(1 + \nu)\epsilon_1^E$$

where ν is Poisson Ratio and ϵ_1 is the uniaxial elongation. In the case of a plastic material with $\nu = 0.5$ we have

$$\epsilon_{\text{eff}}^P = \epsilon_1^P$$

Two failure criteria are proposed; one based on the total effective strain ϵ_{eff}^T and the other on the non-elastic strain ϵ_{eff}^N . By the definitions of effective strain, we have $\epsilon_{\text{eff}}^T > \epsilon_{\text{eff}}^N$. Therefore the total effective strain criterion is more conservative. Also we assume that when all points of the cladding satisfy the 0.2 percent i.e.,

$$\text{criterion 1) } \epsilon_{\text{eff}}^T > 0.2\%$$

$$\text{criterion 2) } \epsilon_{\text{eff}}^N > 0.2\%$$

at all points of the cladding failure can occur. One additional strain criterion that may be used in the hoop (tangential) strain ϵ_H on the outer surface of the cladding. This pertains to displacement criteria since the change of the outer surface ΔD is given by

$$\Delta D = \epsilon_H D$$

where D is the outer diameter of the fuel cladding.

Stress Criteria

The steady-state stress criterion is normally based upon yield and ultimate strength (unirradiated) of the cladding material and is the same as used in Section III of the A.S.M.E. code. This criterion states that no part of the cladding shall become plastic i.e., failure occurs when one point (part) of the cladding became plastic or effective stress σ_{eff} , defined by

$$\sigma_{\text{eff}} = [(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2]^{1/2} / \sqrt{2},$$

is greater than the yield stress σ_y .

The transient stress criterion may be based on ductile failure. This criterion states that plastic instability shall not occur i.e., failure occurs when the effective stress equals or is greater than the yield stress at all points (parts) that fuel cladding.

A fracture criterion based on a tensile hoop (tangential) stress σ_H ($\sigma_H > 0$) on the inner surface of the cladding could be used. The tensile stress could cause a crack on the inner surface to grow outward. By reducing the wall thickness at the end of life to 10 mils we may think that there could be a crack of 2 mils on the inner surface (no crack on the outer surface, only sodium erosion). Since we have no data (see Appendix V) we will not use this criterion in the conclusions. The stress criteria are

criterion 3) $\sigma_{eff} = \sigma_y$ at one point

criterion 4) $\sigma_{eff} = \sigma_y$ at every point

criterion 5) $\sigma_H > 0$

B. LOSS OF COOLANT TRANSIENTS

Geometry

Only one axial location was considered for the loss of coolant transients. The location is at the top of the fuel column or 35 inches from the bottom. This is based on the results of the 6¢/second transients where failure occurred at 35 inches up the fuel column. (See Table A.IV.5). Both a wall thickness of 10 and 15 mils are considered for the loss of coolant transients.

Materials

Due to the uncertainty of the material properties of Type 316 stainless steel 20% cold worked two different material models will be considered and will be denoted by case (1) and (2).

1. Poisson Ratio (Figure A.VIII.1)⁽³⁾

$$\left. \begin{array}{l} \text{case (1)} \\ \text{case (2)} \end{array} \right\} \nu = 0.3$$

2. Young's Modulus E (Figure A.VIII.2)⁽³⁾

$$\left. \begin{array}{l} \text{case (1)} \\ \text{case (2)} \end{array} \right\} E = 22.12 \cdot 10^6 \text{ psi at } 1110^\circ \text{F}$$

3. Linear hardening parameter β

$$\text{case (1)} \quad \beta = 0.$$

$$\text{case (2)} \quad \beta = 0.005$$

Note: when β is zero we have an elastic perfectly plastic model. This means that the yield point, yield strength and ultimate strength are all equal.

4. Yield Strength (See Figure A.IV.1)

case (1) yield strength is based on irradiated
Type 316 SS solution treated⁽³⁾

case (2) yield strength is based on unirradiated
Type 316 SS 20% C.W.⁽²⁾

5. The creep law is

$$\dot{\epsilon}_{\text{eff}}^c = A \sigma_{\text{eff}}^n + C d_I \sigma_{\text{eff}} \phi \quad (3600 \text{ sec/hr})$$

where $\dot{\epsilon}_{\text{eff}}^c$ = effective creep strain rate, in/(in.-hr)

σ_{eff} = effective stress, psi

ϕ = total flux, (n/cm²-sec)

$$\alpha_I = \exp (1.405 - 0.0027 T_A)$$

T_A = temperature, °K

n, A, C are constants

Case (1) (See Figure A.VIII.3)⁽³⁾

$$n = 4.9$$

$$\log_{10} A = -26.4 + 0.0107 (T - 1100).$$

$$C = 0.$$

Case (2) (4)

$$n = 6.78$$

$$\log_{10} A = -5 - n(6.73 - 0.0021T)$$

FIGURE A.IV.1. 0.2% YIELD STRENGTH AS A FUNCTION OF TEMPERATURE FOR THE LOSS OF COOLANT TRANSIENT

$$C = 1.93 \times 10^{-30} \text{ cm}^2/(\text{n-psi})$$

where

T = temperature, °F

6. Dilatation (swelling) function $\theta^{(1)}$ (See Figure A.VIII.4) (5)

$$\theta = \alpha(T - 70) + C_S(\phi t / 10^{22})^{1.69} \{ \exp [-7088/(T_B + 460)] - 5480 \exp [-23000/(T_B + 460)] \}$$

where α = linear coefficient of expansion

T = temperature, °F

ϕt = fluence, n/cm² (E > 0.1 MeV)

T_B = outer surface temperature, °F

Case (1)

$$\alpha = (9 + 0.002T) \times 10^{-6}$$

$$C_S = 0.$$

Case (2) References (3)

$$\alpha = (8.55 + 0.00147T) \times 10^{-6} \text{ in}/(\text{in-}^\circ\text{F})$$

$$C_S = 0.051.$$

Note: C_S = 0 during the transients.

Thermal and Mechanical Loading

a) Steady-state Loading

The inner and outer surface cladding steady-state temperatures at 35 inches up the fuel column was

$$\text{Case (1)} \quad T_A = 1025 \text{ }^\circ\text{F}$$

$$T_B = 990 \text{ }^\circ\text{F}$$

$$\text{Case (2)} \quad T_A = 1046 \text{ }^\circ\text{F}$$

$$T_B = 1010 \text{ }^\circ\text{F}$$

The difference is due to a change in steady-state flow rate, i.e., 885 lb/hr for Case (1) and 830 lb/hr for Case (2).

The steady-state pin power was 12.2 kW/ft for both cases and inlet coolant temperature was 600 °F.

The external pressure of the cladding is 50 psi for all times and cases considered. The steady-state internal pressure histories are given in Figure A.IV.2. The differences between Case (1) and (2) at the start of steady-state (SSS) is due to the difference in SSS temperature (less off-gas for less temperature). The end-of-life (EOL) burnup is 80,000 MWd/MT in both cases; therefore, the difference in time to the EOL for Case (1) and (2). The fission gas release is considered to be 32.5% and 80% for EOL internal pressures of 295 psi and 800 psi.

b) Transient Loading

The transient temperature histories for the flow coastdown and pipe rupture are given in Figure A.IV.2a for both Case (1) and Case (2). The internal pressure obeys the perfect gas law during the transient temperature loading. This means that there is no additional gas release from the fuel during the transient, i.e., no fuel melting.

Results

All results are shown in Tables A.IV.1 and A.IV.2 for the flow coastdown and pipe rupture transient. The tables show the time, internal pressure, inner and outer surface temperature, inner and outer hoop (tangential) stress, outer effective strain, both total and plastic (non-elastic) for the various failure criteria in Table V.1.

The various distributions for the flow coastdown and pipe rupture at the top of the fuel column are shown in Figures A.IV.3 and A.IV.4, respectively. Figures are only for Case (1). Case (2) looks like Case (1) and will not be presented.

Conclusions

Temperature threshold of 1600 °F should be set on both the pipe rupture and flow coastdown. This threshold is burnup independent and satisfies all failure criteria. (It may change due to a fracture criteria and primary creep.)

FIGURE A.IV.2. INNER AND OUTER PRESSURES DURING STEADY STATE TEMPERATURE

FIGURE A.IV.2a. TEMPERATURE HISTORIES FOR PIPE RUPTURE AND FLOW COASTDOWN

TABLE A.IV.1. Loss of Coolant, 35 inches up, Case (1)

Time	Inner Press.	Inner Temp.	Outer Temp.	Hoop stress, ksi		Effective Strain %		
				Inner	Outer	Total	Plastic	
Flow Coastdown, 80% gas release, 10 mil wall								
SSS	82.52	1025	990	- 6.631	6.752	0.02675		
EOL	295	1025	990	- 4.122	8.489	0.03392	0.0025	8570 hr
4.64	426.1	1685	1667	0.005	7.225	0.02819	0.0025	Tension
4.94	440.2	1756	1740	3.762	7.115	0.02797	0.0028	Outer Plastic
5.19	451.7	1814	1798	3.100	4.563	0.04331	0.0272	All Plastic
5.21	452.6	1818	1803	3.49	4.618	0.1060	0.0903	End of computer output

Flow Coastdown, 80% gas release, 10 mil wall

SSS same as above

EOL	800	1025	990	1.444	12.65	0.0522	0.0078	Tension
4.39	1124	1626	1607	7.466	13.38	0.0537	0.0077	Outer Plastic
4.52	1141	1658	1640	9.865	11.95	0.0656	0.0245	All Plastic
4.55	1144	1663	1646	10.13	12.07	0.1943	0.1533	$\epsilon^t \approx 0.2$

Pipe Rupture, 32.5% gas release, 10 mil wall

SSS same as above

EOL	295	1025	990	- 4.122	8.489	0.0339	0.0025	8570 hr
0.3	344.5	1274	1259	0.044	5.576	0.02231	0.0025	Tension
1.96	441.2	1761	1746	0.687	6.865	0.0273	0.0032	Outer Plastic
2.33	451.6	1813	1798	3.088	4.559	0.0432	0.0271	All Plastic
2.36	452.6	1818	1803	3.494	4.611	0.1189	0.1032	End of computer output

TABLE A.IV.1 (contd)

Time	Inner	Inner	Outer	Hoop Stress,ksi		Effective Strain%		
	Press.	Temp.	Temp.	Inner	Outer	Total	Plastic	
Pipe Rupture, 80% gas release, 10 mil wall								
SSS same as above								
EOL	800	1025	990	1.444	12.65	0.0522	0.0078	Tension
1.16	1129	1636	1621	8.412	12.65	0.0509	0.0076	Outer Plastic
1.22	1141	1657	1642	10.06	11.89	0.0606	0.0198	All Plastic
1.24	1145	1665	1649	10.20	12.02	0.2511	0.2102	End of computer output
Pipe Rupture, 80% gas release, 15 mil wall								
SSS	82.55	1025	990	- 6.85	6.54	0.0259		
EOL	800	1025	990	- 1.45	10.43	0.0413	0.0038	
0.11	845.5	1109	1081	0.05	9.65	0.0379	0.0036	Tension
1.56	1165	1703	1688	4.526	9.645	0.0380	0.0047	Outer Plastic
1.77	1181	1732	1717	6.350	8.316	0.0478	0.0189	All Plastic
1.8	1184	1737	1722	6.573	8.42	0.2171	0.1884	End of computer output

TABLE A.IV.2. Loss of Coolant, 10 mil wall, Ye - 84,000, 35 inches up, Case (2)

Time	Inner Press.	Inner Temp.	Outer Temp.	Hoop Stress, ksi		Effective Strain, %		Outer Strain, %		Swelling, %	
				Inner	Outer	Total	Plastic	Hoop	Mean		
Flow Coastdown, 32.5% gas release											
SSS	140	1046	1010	- 6.33	7.614	0.0271		0.9663	0.9515		
EOL	295	1046	1010	- 3.54	7.996	0.0354	0.00795	2.007	1.986	1.034	80,000
3.73	386.4	1462	1441	0.002	6.500	0.0313	0.0099	2.523	2.503		Tension
4.952	441.1	1723	1707	1.927	5.14	0.0337	0.0169	2.872	2.849		Plastic
4.961	441.5	1725	1709	1.863	5.4	0.0347	0.0173	2.876	2.852		All Plastic
Flow Coastdown, 80% gas release											
EOL	800	1046	1010	1.904	12.88	0.0521	0.0101	2.026	1.990		Tension
4.758	1134	1675	1658	10.16	11.84	0.2163	0.1788	2.972	2.789	Due to creep	$\begin{cases} E^T > 0.2\% \\ E^P > 0.2\% \end{cases}$
4.771	1135	1678	1662	10.18	11.85	0.2334	0.1959	2.991	2.2794		
Pipe Rupture, 32.5% gas release											
EOL	395	1046	1010	- 3.54	7.996	0.0354	0.00795	2.007	1.986	The creep coefficient was mult. by 3600 during transient.	
0.1	312	1095	1075	- 0.19	5.201	0.0252	0.00795	2.074	2.057		Tension
1.827	440.7	1721	1704	2.015	4.95	0.0343	0.01799	2.869	2.846		Inner Plastic
1.858	441.4	1725	1708	1.762	5.98	0.0394	0.02035	2.880	2.852		All Plastic
Pipe Rupture, 80% gas release											
EOL	800	1046	1010	1.904	12.88	0.0521	0.0101	2.026	1.990		Tension
1.5	1120	1651	1636	10.01	11.69	0.0997	0.0637	2.842	2.760	Due to Creep	$\begin{cases} E^T > 0.2\% \\ E^P > 0.2\% \end{cases}$
1.648	1129	1667	1651	10.13	11.76	0.2176	0.1803	2.963	2.779		
1.681	1131	1670	1654	10.16	11.78	0.2511	0.2138	2.996	2.784		

AIV.13

TABLE A.IV.2. Loss of Coolant, 15 mil wall, Ye 84,000, 35 inches up, Case (2)

Time	Inner Press.	Inner Temp.	Outer Temp.	Hoop Stress, ksi		Outer Strain, %		Outer Strain, % Hoop	Strain, % Mean	Swelling, %		
				Inner	Outer	Total	Plastic					
Flow Coastdown, 32.5% gas release												
EOL	295	1046	1010	-	4.481	7.033	0.0302	0.0057	2.003	1.985	1.034	8090 hr
4.93	439.9	1717	1701		0.097	4.470	0.0225	0.0076	2.856	2.841		Tension
4.9745	442.2	1728	1712		0.175	4.359	0.0227	0.0082	2.871	2.856		Inner Plastic
4.9855	442.8	1731	1715		0.592	4.572	0.0303	0.0157	2.882	2.859		All Plastic
Flow Coastdown, 80% gas release												
EOL	800	1046	1010	-	0.83	10.15	0.04078	0.00705	2.015	1.987	1.034	8090 hr
1.5	848	1137	1106		0.39	9.66	0.0389	0.00705	2.125	2.098		Tension
4.8616	1147	1701	1684		6.50	7.96	0.0667	0.0415	2.874	2.821		Inner Plastic
4.8991	1152	1710	1693		8.2	9.56	0.2211	0.1908	3.022	2.834		All Plastic
Pipe Rupture, 32.5 gas release												
EOL	295	1046	1010	-	4.481	7.033	0.0323	0.0057	2.003	1.985	1.034	8090 hr
0.4	367	1355	1346		1.405	2.608	0.01407	0.0057	2.39	2.38		Tension
1.89	442.1	1728	1711		0.237	3.875	0.02571	0.0127	2.871	2.854		Inner Plastic
1.90	442.6	1731	1714		0.58	4.67	0.0356	0.0208	2.885	2.858		All Plastic
Pipe Rupture, 80% gas release												
EOL	800	1045	1010	-	0.83	10.15	0.04078	0.007046	2.015	1.987		8090 hr
0.1	826	1095	1075		2.608	7.37	0.03207	0.008265	2.082	2.059		Tension
1.8	1148	1701	1684		6.563	7.96	0.1633	0.1380	2.957	2.821		Inner Plastic
1.81	1152	1703	1691		5.367	8.35	0.1973	0.1709	2.996	2.830		All Plastic

Hoop stress values indicate stress reached for the criterion indicated and are not at equivalent times.

FIGURE A.IV.3. DISTRIBUTIONS DURING PIPE RUPTURE AT 35 INCHES UP THE FUEL COLUMN FOR CASE 1 (SEE TABLE A.IV.1)

Hoop stress values indicate stress reached for the criterion indicated (Table V.1) and are not at equivalent times.

FIGURE A.IV.4. DISTRIBUTIONS DURING FLOW COASTDOWN AT 35 INCHES UP THE FUEL COLUMN FOR CASE 1 (SEE TABLE A.IV.1)

C. 6¢/sec POWER TRANSIENT WITH 10% COOLANT FLOW

Geometry

The wall thickness was held at 15 mils during this analysis. Four different axial locations were considered, however.

Materials

Case 2 of the loss of coolant transients in use for the 6¢/sec Power transient with 10% coolant flow.

Thermal and Mechanical Loading

a) Steady-state Loading

The inner and outer surface cladding steady-state temperatures and flux ($E > 0.1$ MeV) at the four axial positions are the same as the 5\$/sec transient steady-state temperatures, namely

Axial Location from Bottom ~ Inches	Inner Surface Temperature $T_A \sim ^\circ F$	Outer Surface Temperature $T_B \sim ^\circ F$	Flux Neutrons per cm^2 -sec $\times 10^{-15}$
11	807	735	3.89
19	921	845	4.55
27	1007	944	3.89
35	1046	1010	2.48

These temperatures are for a steady-state pin power of 12.2 kW/ft, flowrate of 830 lb/hr and coolant inlet temperatures of 600 °F.

The external pressure on the cladding was 50 psi for all times and cases considered. The steady-state internal pressure histories are given in Figure A.IV.4a for three of the four cases considered. The other case is for zero burnup where the internal pressure is 100 psi at the start of the transient.

FIGURE A.IV.4a. INTERNAL PRESSURE HISTORIES

The transient temperatures and pressure histories are given in Tables A.IV.3 and A.IV.4. In the 6¢/sec transient no fuel melting occurs; therefore, the perfect gas law is used to calculate the internal pressure.

TABLE A.IV.3. The Transient Temperature Histories for the Various Axial Locations

Time in Seconds	<u>11 in. Up</u>		<u>19 in. Up</u>		<u>27 in. Up</u>		<u>35 in. Up</u>	
	<u>T_A</u>	<u>T_B</u>	<u>T_A</u>	<u>T_B</u>	<u>T_A</u>	<u>T_B</u>	<u>T_A</u>	<u>T_B</u>
0	600	600	600	600	600	600	600	600
15.6	600	600	600	600	600	600	600	600
17.	697	689	721	714	715	710	683	681
25.	1237	1216	1781	1754	2196	2179	2344	2342

TABLE A.IV.4. The Transient Internal Pressure Histories

Time in Seconds	<u>Zero</u>	<u>80% Release</u>	<u>50% Release</u>	<u>80% Release</u>
	<u>Burn Up</u>	<u>50,000 MWd/MT</u>	<u>80,000 MWd/MT</u>	<u>80,000 MWd/MT</u>
0	100	353	353	566
15.6	100	353	353	566
17.	108	381	381	610
25.	265	935	935	1496

Result

Table A.IV.5 gives the times, stress and strain for the different burnup, % gas release, and axial position.

The table shows that the axial position 35 inches up from the bottom will always fail first. Likewise, the 80% gas release will fail first. Hence the minimum time for failure during a 6¢/sec power transient is 21.9 second. This occurs at 35 inches up from the bottom with 80% gas release and at the end of life i.e., 80,000 MWd/Mt.

TABLE A.IV.5. 64/sec Power Transient with a 15 mil Wall

AIV.20

Time	sec	Inner Pressure psi	Inner Temperature °F	Outer Temperature °F	Inner Hoop Stress ksi	Outer Hoop Stress ksi	Outer Eff. Total Strain %	Outer Eff. Plastic Strain %	Outer Hoop Strain %	Outer Mean Strain %	Outer Swelling Strain %	Criteria	Statements
Burn up = 0, 11 inches up													
15.6	100		600	600	0.31	0.26	0.001	0.0	0.501	0.500	0.		Use this for SSS and EOL for all axial locations.
17.	108		697	689	-1.12	1.67	0.006	0.0	0.597	0.594	0.		
25.	265		1247	1216	-5.16	7.34	0.026	0.0	1.207	1.193	0.		
Burn up = 0, 19 inches up													
17.	108		721	714	-0.95	1.51	0.005	0.0	0.623	0.620	0.		3 Inner plastic Outer plastic
24.46	254.6		1711	1685	-4.71	6.80	0.024	0.0	1.802	1.788	0.		
24.59	257.		1727	1701	-2.22	6.69	0.023	0.0	1.823	1.810	0.		
Burn up = 0, 27 inches up													
17.	108		715	710	-0.57	1.16	0.004	0.0	0.618	0.615	0.		3 Outer plastic 4 & 5 All plastic
22.50	215.9		1733	1720	-1.16	3.80	0.013	0.0	1.840	1.832	0.		
22.54	216.7		1740	1727	0.80	1.97	0.027	0.020	1.857	1.839	0.		
Burn up = 0, 35 inches up													
17.	108		683	681	-0.004	0.65	0.002	0.0	0.586	0.584	0.		5 Nothing 3 & 4 Just about all plastic and end of run
21.60	198.3		1638	1636	0.55	1.30	0.004	0.0	1.720	1.717	0.		
22.08	207.2		1733	1731	0.66	1.31	0.005	0.0	1.847	1.843	0.		
50,000 MWd/MT, 80% gas release, 11 inches up													
SSS	140		807	807	0.60	0.51	0.0018	0.0	0.7195	0.7180	0		Error in outer temperature
EOL	500		807	807	3.19	2.74	0.0114	0.0026	1.246	1.236	0.516		
15.6	353		600	600	2.13	1.82	0.0086	0.0026	1.025	1.017	0.516		
25.	935		1247	1216	-0.34	11.49	0.0406	0.0026	1.740	1.712	0.516		Nothing and end of run

TABLE A.IV.5. (contd)

TABLE A.IV.5. (contd)												
Computer Time	Inner Press	Inner Temp	Outer Temp	Hoop Stress		Effective Strain		Outer Strain Hoop	Outer Strain Mean	Time, μ sec	Swelling	Statements
				Inner	Outer	Total	Plastic					
11 inches up, 50,000 MWd/MT, 80% gas release												
5050	500	807	807	3.19	2.74	0.0114	0.00265	1.246	1.236		0.516	Error
0.26	353	600	600	2.13	1.82	0.0086	0.00265	1.025	1.017	15.6		
0.29666	935	1247	1216	-0.34	11.49	0.0406	0.00265	1.74	1.712	25.		End of computer output
19 inches up, 50,000 MWd/MT, 80% gas release												
5050.	500	921	821	-12.18	16.05	0.1138	0.0576	0.8085	0.750		0	
0.26	353	600	600	6.43	-2.62	0.0466	0.0576	0.5202	0.4965	15.6		
0.2958	899.6	1713	1688	4.20	6.39	0.0785	0.0597	1.834	1.79	24.48		Plastic
0.2959	903.9	1722	1696	3.01	7.04	0.08	0.0596	1.847	1.801	24.54		All plastic
27 inches up, 50,000 MWd/MT, 80% gas release												
5050	500	1007	944	-7.34	12.08	0.0564	0.0145	1.748	1.716		0.834	
0.26	353	600	600	4.32	-0.36	0.0123	0.0145	1.342	1.333	15.6		
0.2923	755.2	1715	1702	4.07	5.0	0.0300	0.0145	2.664	2.643	22.38		Plastic
0.2924	756.9	1720	1707	3.99	5.06	0.0301	0.0145	2.670	2.649	22.44		All plastic
35 inches up, 50,000 MWd/MT, 80% gas release												
5050	500	1046	1010	-3.41	8.691	0.033	0.0034	1.439	1.419		0.4663	
0.26	353	600	600	2.89	1.098	0.0069	0.00346	0.973	0.9669	15.6		
0.2916	724.1	1712	1710	5.07	3.84	0.0156	0.00346	2.297	2.284	21.96		Plastic
0.29163	725.9	1717	1715	4.33	4.52	0.0187	0.00437	2.307	2.291	21.96		All plastic
19 inches up, 80,000 MWd/MT, 50% gas release												
8080	500	921	821	-10.09	13.97	0.1372	0.0886	0.8167	0.7477		0	
0.26	353	600	600	8.37	-4.55	0.0737	0.0917	0.5299	0.4943	15.6		
0.2958	897.6	1709	1684	6.14	4.419	0.1034	0.0917	1.838	1.783	24.48		Plastic
0.2959	903.7	1722	1696	2.40	7.062	0.1102	0.0917	1.864	1.801	24.54		All plastic
27 inches up, 80,000 MWd/MT, 50% gas release												
8080	500	1007	944	-6.21	10.97	0.0599	0.02204	2.761	2.726		1.846	
0.26	353	600	600	5.44	-1.47	0.0158	0.02204	2.355	2.344	15.6		
0.2923	752.9	1709	1696	5.20	3.86	0.0333	0.02204	3.668	3.645	22.38		Plastic
0.29238	757.2	1721	1708	2.71	6.48	0.0530	0.3305	3.704	3.663	22.44		All plastic

TABLE A.IV.5 (contd)

Computer Time	Inner Press	Inner Temp	Outer Temp	Hoop Stress		Outer Effective Strain		Outer Strain		Time, sec	Swelling	Statements
				Inner	Outer	Total	Plastic	Hoop	Mean			
11 inch												
SSS	140	807	807	0.50	0.51	0.0018	---	0.7195	0.7180		---	Error in outer surface temperature
EOL	800	807	807	5.35	4.60	0.0211	0.0065	1.862	1.844		1.123	
0.26	566	600	600	3.67	3.15	0.0166	0.0065	1.640	1.625	15.6		
0.29666	1496	1247	1216	3.7	14.97	0.0550	0.0065	2.363	2.322	25.		End of compu- ter output
27 inch												
SSS	140	1007	944	-12.11	12.03	0.0434	---	0.9043	0.8819		---	1.85
EOL	800	1007	944	-4.02	12.80	0.0667	0.0235	2.769	2.729			
0.26	566	600	600	7.01	-0.17	0.0213	0.0235	2.361	2.345	15.6		
0.2923	1208	1715	1702	8.407	6.709	0.0437	0.0235	3.688	3.656	22.38		Plastic All plastic
0.2925	1217	1729	1716	5.754	9.365	0.0908	0.0613	3.752	3.677	22.50		
19 inch												
SSS	140	921	845	-14.38	14.06	0.0509	---	0.8007	0.7745			Plastic All plastic
EOL	800	921	821	-7.863	15.77	0.1444	0.0908	0.8253	0.7492			
0.26	566	600	600	10.12	-3.43	0.0758	0.0908	0.5350	0.4952	15.6		
0.2955	1422	1692	1667	10.03	7.456	0.1129	0.0928	1.828	1.763	24.30		Plastic All plastic
0.2957	1433	1706	1680	5.691	11.64	0.1242	0.0980	1.861	1.783	24.42		
35 inch												
SSS	140	1046	1010	-6.74	7.172	0.0241	---	0.9641	0.9511			1.032
EOL	800	1046	1010	-0.83	10.15	0.0408	0.007	2.013	1.985			
0.26	566	600	600	4.844	2.02	0.0132	0.007	1.545	1.533	15.6		
0.2914	1149	1694	1692	8.517	6.086	0.0261	0.007	2.849	2.827	21.84		Plastic All plastic
0.2915	1155	1705	1703	6.585	8.376	0.0388	0.012	2.877	2.843	21.90		

D. 5\$/second UNINTERRUPTED TRANSIENT

Geometry

The wall thickness was held at 10 mils in most of the cases considered to simulate end of life conditions. Although, a 10 and 15 mil wall was used in the 0% fission gas release to show the effects of wall thickness. The various axial positions are given on Page A.IV.16. It was felt that four axial locations are a minimum number that must be considered in a power transient stress calculation.

Materials

The material that will be considered is Type 316 SS. Due to the uncertainty of the mechanical properties of 20% cold-worked Type 316 SS, two different material models will be considered. One will be called solution treated (denoted by S.T.) and the other will be called cold worked (denoted by C.W.). It is felt that FTR cladding lies between these two cases.

- a) Poisson Ratio ν (Figure A.VIII.1)

$$\left. \begin{array}{l} \text{S.T.} \\ \text{C.W.} \end{array} \right\} \nu = 0.3$$

- c) Young's Modulus E (Figure A.VIII.2)

$$\left. \begin{array}{l} \text{S.T.} \\ \text{C.W.} \end{array} \right\} E = 22.12 \times 10^6 \text{ psi at } 1110^\circ \text{F}$$

- c) Linear hardening parameter β

$$\text{S.T.} \quad \beta = 0$$

$$\text{C.W.} \quad \beta = 0.005$$

Yield Strength

S.T. yield strength is plotted in Figure A.IV.5 and shows the effects of irradiation. Also, the yield point, yield strength and ultimate strength are all equal since $\beta = 0$. See the stress-strain plot of Figure A.IV.6.

FIGURE A.IV.5. UNIRRADIATED AND END OF LIFE YIELD STRENGTH OF SOLUTION TREATED AS A FUNCTION OF TEMPERATURE

FIGURE A.IV.6. UNIRRADIATED AND END OF LIFE STRESS-STRAIN PLOT OF SOLUTION TREATED AT 1000 °F

C. W. yield strength is assumed to be independent of irradiation. Since most of the cases considered have a upper yield strength of 70,000 psi it will be assumed that all cases have this yield strength unless denoted by yield 1 or yield 2. See Figure A.IV.7.

Creep Law

The S.T. creep law is

$$\dot{\epsilon}_{eff}^C = A \sigma_{eff}^n$$

where

$$n = 4.9$$

$$\log_{10} A = -26.4 + 0.0107 (T - 1100).$$

T = temperature °F

Only secondary creep is considered (see Figure A.VIII.3).

For C.W. the creep law is

$$\dot{\epsilon}_{eff}^C = A \sigma_{eff}^n + C \alpha_I \sigma_{eff} \phi(3600 \text{ sec/hr})$$

where the thermal creep coefficients are

$$n = 6.78$$

$$\log_{10} A = -5.0 - n (6.73 - 0.0021 T)$$

The irradiation creep coefficients were assumed to be

$$C = 1.93 \times 10^{-30} \text{ cm}^2/(\text{n} \cdot \text{psi})$$

$$\alpha_I = \exp (1.405 - 0.0027 T_A)$$

$$T_A = T + 460.$$

T = temperature °F

for most of the cases considered. Although it was assumed that $\alpha_I = 1$ and $C = 10^{-29}/3$. and $10^{-28}/3$, for two cases to show

FIGURE A.IV.7. COLD WORK TYPE 316 STAINLESS STEEL 0.2% YIELD STRENGTH AS A FUNCTION OF TEMPERATURE

the effects of the irradiation creep coefficients, these variations of the irradiation creep coefficient change the end-of-life steady-state stress and deformation distribution.

Dilatation (swelling) Function. The S.T. dilatation function is considered to be only thermal expansion

$$\theta = \alpha(T - 70.)$$

where

$$\alpha = (9 + 0.002 T) \times 10^{-6}$$

$$T = \text{temperature } \sim ^\circ\text{F}$$

The C.W. dilatation function is considered to be thermal expansion and swelling

$$\theta = \alpha_T (T - 70) + C_s \left(\frac{\phi t}{10^{22}} \right)^{1.69} \alpha_I$$

where

$$\alpha_T = (8.55 + 0.00147 T) \times 10^{-6}$$

$$C_s = 0.051$$

The function α_I is temperature dependent and in most cases α_I will depend on the outer surface temperature T_B

$$\alpha_I = \exp [-7088/(T_B + 460.)] - 5480. \exp [-23,000./(T_B + 460.)]$$

Although, a few cases are considered where α_I depends on the cladding temperature distribution. Therefore, whenever there is a temperature gradient, there will also be a swelling gradient which will introduce a swelling stress much like the thermal stress in general.

During transients it is assumed that C_s is equal to zero.

Thermal and Mechanical Loading

Steady-State Loading

The inner and outer surface cladding steady-state temperatures and flux ($E > 0.1$ MeV) for the solution treated (S.T.) and cold worked (C.W.) cases as a function of axial position is given in Table A.IV.6.

TABLE A.IV.6. Steady-State Temperatures and Flux at Various Axial Locations

Axial Position from Bottom Inches	S.T. Temperature		C.W. Temperature		Flux neutrons per cm ² -sec × 10 ¹⁵
	Inner	Outer	Inner	Outer	
7	743	680			
11			807	735	3.89
19	913	830	921	845	4.55
27	987	924	1007	944	3.89
35			1046	1010	2.48

These temperatures are for a steady-state pin power of 12.2 kW/ft and coolant inlet temperature of 600 °F. The flow-rate was 885 lb/hr for S.T. and 830 lb/hr for C.W.

The external pressure of the cladding is 50 psi for all times and cases considered. The steady-state internal pressure histories are given in Figure A.IV.8. The differences between S.T. and C.W. at the SSS is due to the difference in SSS temperature. The EOL burnup is 80,000 MWd/MT in both cases.

Transient Loading

The transient temperatures and internal pressure histories for the solution treated cases are in Figure A.IV.9 and A.IV.10. (also see Table A.IV.8) and for the cold worked case are in Table A.IV.7 where we assume linear functions between time points.

Results

The results for the 5\$/second S.T. case is given in Table A.IV.9 and Figures A.IV.11, A.IV.12 and A.IV.13. The results for the 5\$/second C.W. case is given in Table A.IV.10, A.IV.11, A.IV.12 and A.IV.13.

Table A.IV.14 is the result for the 5\$/second power transient with swelling gradients during steady-state conditions.

FIGURE A.IV.8. INTERNAL AND EXTERNAL PRESSURE HISTORIES DURING STEADY STATE FOR SOLUTION TREATED AND COLD WORKED CASES.

FIGURE A.IV.9. INNER PRESSURE HISTORY FOR 32.5% AND 80% FISSION GAS RELEASE DURING A 5\$/\$SECOND REACTIVITY TRANSIENT

FIGURE A.IV.10. INNER AND OUTER SURFACE TEMPERATURE HISTORIES DURING A 5\$/\$SECOND REACTIVITY TRANSIENT

TABLE A.IV.7. Temperature and Pressure Histories for a
5\$/second FTR Reactor Power Transient

<u>Time in Seconds</u>	<u>Temperature at 11 inches Up</u>		<u>Temperature at 19 Inches Up</u>		<u>Temperature at 27 Inches Up</u>	
	<u>Inner</u>	<u>Outer</u>	<u>Inner</u>	<u>Outer</u>	<u>Inner</u>	<u>Outer</u>
0	807	735	921	845	1007	944
0.25	907	788	1039	916	1113	1017
0.28	958	819	1103	961	1174	1065
0.30	998	845	1167	1005	1227	1109
0.32	1054	878	1234	1056	1299	1166

<u>Time in Seconds</u>	<u>Temperature at 35 Inches up</u>		<u>Internal Pressure for % Fission Gas Release</u>		
	<u>Inner</u>	<u>Outer</u>	<u>0%</u>	<u>50%</u>	<u>80%</u>
0	1046	1010	140	500	800
0.25	1121	1067	140	500	800
0.28	1166	1106	4500	3200	2000
0.30	1205	1142	16800	9000	5200
0.32	1252	1187	52000	26000	10500

The pressures are obtained from Figure A.IV.11. Also
the flux is assumed to be zero during the transients.

TABLE A.IV.8. Solution Treated 5\$/second
Transient Case Temperature
and Pressure Histories

<u>Time</u>	<u>Temperature at 7 Inches Up</u>		<u>Temperature at 19 Inches Up</u>		<u>Temperature at 27 Inches Up</u>		<u>32.5% Fission Gas Release</u>
	<u>Inner</u>	<u>Outer</u>	<u>Inner</u>	<u>Outer</u>	<u>Inner</u>	<u>Outer</u>	
0	743	680	913	830	987	924	295
0.24	807	708	999	883	1069	977	295
0.28	870	741	1105	948	1162	1043	5415
0.30	913	763	1181	1000	1231	1097	14,845
0.32	970	791	1265	1063	1318	1166	23,595

TABLE A.IV.9. Reactivity Transient ~5\$/second S.T. Case with a
10.0 mil Wall

<u>Time</u>	<u>Inner Pres- sure</u>	<u>Temperature</u>		<u>Criterion</u>
		<u>Inner</u>	<u>Outer</u>	
7 inches up, unirradiated yield function, 32.5% gas release				
0	295.0	743	680	(32.5%)
0.2531	1972.0	827.6	718.8	5 & 3
0.2611	2996.0	840.2	725.4	2
0.2656	3571.0	847.3	729.1	4
0.2661	3635.0	848.1	729.5	1
19 inches up, unirradiated yield function, 32.5% gas release				
0	295	913	830	(32.5%)
0.2535	2259	1040	907.9	5 & 3
0.2563	2387	1042	909.6	2
0.2623	3155	1058	919.3	Code Stop with 75% Plastic Wall
27 inches up, unirradiated yield function, 32.5% gas release				
0	295	987	924	(32.5%)
0.2520	1828	1097	996.8	5 & 3
0.2593	2772	1114	1009	2
0.2629	3219	1122	1015	4
27 inches up, irradiated yield function, 32.5% gas release				
0	295	987	924	(32.5%)
0.2529	1940	1099	998.2	5
0.2776	5108	1156	1039.0	3 & 2
0.2821	6405	1169	1049.0	4
27 inches up, irradiated yield function, 80% gas release				
0	800	987	924	(80%)
0.2567	2054	1108	1005	5
0.2767	3554	1154	1038	2
0.2907	4604	1199	1072	3
0.3042	5616	1246	1108	4
0.3047	5653	1247	1110	1

FIGURE A.IV.11. DISTRIBUTIONS DURING A 5\$ /SECOND REACTIVITY TRANSIENT AT 7 INCHES UP THE FUEL COLUMN FOR 32.2% FISSION GAS RELEASE AND UNIRRADIATED TYPE 316 STAINLESS STEEL SOLUTION TREATED

FIGURE A.IV.12. DISTRIBUTIONS DURING A 5\$/SECOND REACTIVITY TRANSIENT AT 19 INCHES UP THE FUEL COLUMN FOR 32.5% FISSION GAS RELEASE AND UNIRRADIATED TYPE 316 STAINLESS STEEL SOLUTION TREATED

FIGURE A.IV.13. DISTRIBUTIONS DURING A 5\$/SECOND REACTIVITY TRANSIENT AT 27 INCHES UP THE FUEL COLUMN FOR 32.5% FISSION GAS RELEASE AND UNIRRADIATED TYPE 316 STAINLESS STEEL SOLUTION TREATED

TABLE A.IV.10. 5\$/sec, 0% release, 10 mil Wall

Time	Inner		Outer Temp.	Hoop Stress, ksi		Outer Effective Strain%	Outer Plastic Effective Strain%	Outer Strain%		Statements
	Press.	Temp.		Inner	Outer			Hoop	Mean	
11 inches up										
0	140	807	735	-12.65	13.48	0.0485	-	0.6807	0.6553	() are for yield 2
0.2629	2015	928.9	801.3	-3.98	42.27	0.1427	0.0029	0.849	0.7534	Tension
(0.2827)	(7375)	(967.8)	(825.1)	(50.89)	(97.69)					
0.2817	5572	961.5	821.3	32.01	79.47	0.2596	0.0049	1.003	0.8070	plastic
0.2842	7071	966.4	824.4	52.91	79.63	0.3494	0.094	1.075	0.8105	0.2% = ϵ^t
(0.2865)	(8518)	(971.1)	(827.5)	(73.78)	(98.12)	(0.4386)	(0.1263)	(1.173)	(0.8287)	
0.2850	7569	968	826.5	72.05	80.56	0.4616	0.2051	1.169	0.8108	All plastic
(0.2870)										
0.2854	7838	968.9	826.1	75.98	83.66	1.877	1.614	2.395	0.8086	End of computer output
19 inches up										
0	140	921	845	-13.7	14.45	0.0485	-	0.8004	0.7749	
0.2710	3192	1084	947.5	6.0	56.14	0.1832	-	1.057	0.9269	Tension
0.2815	5451	1108	964.4	28.64	79.27	0.2565	0.0015	1.159	0.9657	plastic
0.2841	7006	1116	970.0	53.27	79.58	0.3625	0.1070	1.246	0.9720	0.2% = ϵ^t
0.2842	7083	1116+	970.2	58.63	79.85	0.3844	0.1285	1.265	0.9721	All plastic
0.2848	7429	1118	971.5	64.89	83.85	2.105	1.841	2.756	0.9701	
19 inches up yield 2										
0	140	921	845	-13.74	14.50	0.0495		0.8011	0.7749	
0.2710	3192	1084	947.5	6.0	56.19	0.1845		1.058	0.9269	Tension
0.2821	5776	1110	965.6	31.96	82.53	0.2680	0.0029	1.172	0.9699	plastic

TABLE A.IV.10. (contd)

Time	Inner		Outer Temp.	Hoop Stress, ksi		Outer Effective Strain%	Outer Plastic Effective Strain%	Outer Strain%		Statements
	Press.	Temp.		Inner	Outer			Hoop	Mean	
0.2841	7042	1116	970.1	53.88	82.33	0.3620	0.0979	1.249	0.9745	$\epsilon^t = 0.2\%$ All plastic
0.2843	7119	1117	970.4	58.96	82.75	0.3954	0.1308	1.278	0.9745	
0.2848	7427	1118	971.5	64.22	85.96	2.101	1.830	2.755	0.9719	
35 inches up										
0	140	1046	1010	-6.33	7.61	0.0271	-	0.9663	0.9516	
0.2629	2015	1140	1084	9.30	30.02	0.0990	0.0028	1.130	1.057	
0.2822	5878	1170	1110	50.37	68.50	0.2215	0.0044	1.299	1.120	plastic
0.2826+	6109	1171	1111	54.23	68.45	0.2371	0.0202	1.312	1.121	$0.2\% = \epsilon^t$
0.2827-	6147	1171	1111	56.06	68.45	0.2508	0.0339	1.324	1.121	all plastic
0.2831	6416	1172	1112	60.35	71.45	1.783	1.557	2.65	1.121	

TABLE A.IV.11. 5\$/sec, 0% Release, 15 mil Wall

Time	Inner		Outer Temp.	Hoop Stress ksi		Outer		Outer Strain, %	Outer Strain, %	Statements
	Press.	Temp.		Inner	Outer	Effective Strain %	Plastic Effective Strain, %			
11 inches up										
0	140	807	735	-13.03	12.84	0.0446	0.0019	0.6780	0.6547	
0.2749	3759	949.3	813.7	-0.29	47.19	0.1560	-	0.8784	0.7713	Tension
0.2866	8588	971.3	827.6	33.71	79.53	0.2579	0.0031	1.009	0.8139	plastic
0.2898	10550	977.7	831.8	51.73	79.64	0.3323	0.0769	1.070	0.8186	$\epsilon^t > 0.2$
0.2908	11170	979.7	833.1	67.62	80.12	0.3969	0.1411	1.124	0.8196	all plastic
0.2910	11290									$\epsilon^p > 0.2$
0.2917	11670	981.3	834.2	72.83	83.80	1.885	1.621	2.412	0.8174	$\epsilon^t > 2$
19 inches up										
0	140	921	845	-14.37	14.05	0.0509	-	0.8007	0.7745	
0.2812	4674	1106	962.8	3.92	55.03	0.1803	-	1.070	0.9434	Tension
0.2865	8503	1124	975.3	31.51	79.39	0.2674	0.0135	1.178	0.9782	plastic
0.2891	10120	1132	981.1	49.64	79.61	0.3417	0.0863	1.241	0.9847	ϵ^t
0.2893	10190	1133	981.4	53.41	79.79	0.3553	0.0997	1.253	0.9849	all plastic
0.2895										
0.2901	10730	1135	983.3	59.28	84.20	2.074	1.809	2.743	0.9836	
27 inches up										
0	140	1007	944	-12.11	12.02	0.0435	-	0.9044	0.8819	
0.2758	3890	1168	1058	4.62	44.51	0.1467	0.0013	1.145	1.042	Tension
0.2857	8027	1189	1078	34.53	71.25	0.2311	0.0035	1.263	1.087	plastic
0.2875	9137	1194	1082	49.34	71.13	0.2929	0.06573	1.316	1.091	ϵ^t and
0.2877										all plastic
0.2883	9598	1196	1083	53.73	74.63	1.872	1.637	2.685	1.090	

TABLE A.IV.12. 5\$/sec, 50% Release, 80,000 MWd/T

Time	Inner		Outer Temp.	Hoop Stress, ksi		Outer Effec- tive Strain%	Plastic Effec- tive Strain%	Strain, %		Swell- ing	Statements
	Press	Temp.		Inner	Outer			Hoop	Mean		
11 inches up											
0	500	807	735	-3.24	11.29	0.0786	0.0406	1.510	1.462	0.8097	
0.2719	2471	944.2	810.6	5.398	42.00	0.1779	0.0415	1.693	1.572	0.8097	Tension
0.2900	6101	978.0	832.0	41.97	79.95	0.2962	0.0425	1.851	1.627		plastic
0.2939	7245	985.9	837.1	58.28	79.98	0.3683	0.1132	1.912	1.632		$0.2 < \epsilon^t$
0.2951	7574	988.2	838.6	72.05	80.47	0.4425	0.1867	1.975	1.634		all plastic
0.2953	7629	988.5	838.9	73.31	81.31	0.6225	0.3659	2.130	1.633		$0.2 < \epsilon^P$
0.2961	7882	990.3	840	76.27	83.90	2.137	1.872	3.444	1.634		
19 inches up (bad time cord 4. 0.25 not 0.025)											
0	500	921	845	-4.52	12.45	0.0769	0.0347	2.526	2.478	1.707	
0.2659	1931	1073	939.9	-0.96	37.82	0.1603	0.0360	2.715	2.608		Tension
0.2894	5920	1133	981.6	38.03	79.77	0.2954	0.0412	2.913	2.691		plastic
0.2926	6849	1143	988.7	56.69	80.17	0.3884	0.1329	2.995	2.699		all plastic
0.2928	6921	1144	989.2	58.65	81.39	0.6327	0.3761	3.206	2.697		$\epsilon^t > 70.2\%$
0.2936	7139	1146	990.9	61.06	83.79	2.027	1.763	4.416	2.699		$\epsilon^P = 0.2\%$
27 inches up											
0	500	1007	944	-4.467	12.66	0.0668	0.0239	2.768	2.728	1.846	
0.2629	1661	1139	1038	-0.316	31.64	0.1278	0.0239	2.938	2.852		Tension
0.2878	5468	1195	1082	37.97	70.96	0.2497	0.0241	3.128	2.937		plastic
0.2901	6137	1201	1087	51.98	70.69	0.3141	0.0890	3.185	2.943		all plastic
0.2901+	same										$\epsilon^T = 0.2\%$
0.2903	6191	1201	1088	53.28	71.57	0.4988	0.2728	3.344	2.941		$\epsilon^P = 0.2\%$
0.2912	6445	1204	1090	56.21	74.12	2.044	1.810	4.686	2.943		

TABLE A.IV.12. (contd)

Time	Inner		Outer Temp.	Hoop Stress, ksi		Outer Effec- tive Strain%	Plastic Effec- tive Strain%	Strain, %		Swell- ing	Statements
	Press	Temp.		Inner	Outer			Hoop	Mean		
35 inches up											
0	500	1046	1010	-1.33	9.981	0.0421	0.0088	2.013	1.985	1.032	
0.2569	1121	1131	1076	1.008	20.05	0.0799	0.0143	2.124	2.071		Tension
0.2891	5856	1184	1122	50.72	67.23	0.2293	0.0172	2.348	2.165		plastic
0.2897	6025	1185	1124	54.59	67.21	0.2474	0.0348	2.364	2.166		$\epsilon_t = 0.2\%$
27 inches up $y_e = 84,000$											
0	500	1007	944	-4.467	12.66	0.0668	0.0239	2.768	2.728	1.846	& all plastic
0.2629	1661	1139	1038	-0.32	31.64	0.1278	0.0239	2.938	2.852		Tension
0.2915	6530	1204	1090	48.96	81.68	0.2831	0.024	3.176	2.956		plastic
0.2931											$\epsilon_t = 0.2\%$
0.2932	7019	1209	1094	59.33	81.50	0.3415	0.0826	3.227	2.960		All plastic

TABLE A.IV.13. \$5/sec, 80% Release, 80,000 MWd/MTM ye 70,000

TABLE A.1V.13. \$5/sec, 80% Release, 80,000 MWd/MTM, 70,000 Plastic Effective Strain%											
Time	Inner		Outer Temp.	Hoop Stress, ksi		Effective Strain%	Plastic Effective Strain%	Strain%		Swelling	Statements
	Press.	Temp.		Inner	Outer			Hoop	Mean		
11 inches											
0	800	807	735	0.066	14.13	0.0911	0.0446	1.523	1.464	0.8097	Tension
0.3026	5882	1005	849.2	37.63	79.78	0.3004	0.0463	1.872	1.647		plastic
0.3077	7235	1020	857.7	57.08	79.83	0.3913	0.1359	1.950	1.656		$\epsilon^t = 0.2\%$
0.3088	7538	1023	859.6	68.94	80.34	0.4597	0.2037	2.008	1.658		all plastic
0.3091											
0.3099	7837	1026	861.4	73.04	83.81	2.024	1.76	3.365	1.657		end of product
19 inches											
0	800	921	845	-1.225	15.3	0.0889	0.0382	2.539	2.481	1.707	
0.2846	2736	1118	971.1	4.516	48.24	0.1976	0.0403	2.789	2.653		Tension
0.3012	5514	1171	1008	30.60	78.22	0.2930	0.0424	2.937	2.721		plastic
0.3050	6531	1184	1018	52.15	77.72	0.3997	0.1513	3.032	2.731		all plastic
0.3053											ϵ^t and $\epsilon^p = 0.2\%$
0.3061	6813	1187	1021	56.92	81.11	1.926	1.67	4.357	2.730		end of product
27 inches											
0	800	1007	944	-1.205	15.54	0.0776	0.0262	2.781	2.730	1.846	
0.2749	1796	1164	1057	-0.013	34.02	0.1380	0.0262	2.970	2.877		Tension
0.2993	5087	1225	1107	32.70	68.17	0.2449	0.0274	3.150	2.965		plastic
0.3023	5820	1235	1116	48.30	67.62	0.3197	0.1038	3.218	2.974		all plastic
0.3026											ϵ^t and $\epsilon^p = 0.2\%$
0.3035	6118	1239	1119	52.65	71.03	2.034	1.81	4.707	2.975		end of product
35 inches											
0	800	1046	1010	1.903	12.88	0.0521	0.0101	2.024	1.987	1.032	tension
0.3013	5557	1208	1145	47.46	64.84	0.2183	0.0131	2.365	2.189		plastic
0.3021	5766	1210	1147	52.15	64.73	0.2383	0.0332	2.383	2.192		all plastic
0.3023											$\epsilon^t > 0.2\%$
0.3032	6048	1213	1149	55.47	67.7	1.954	1.740	3.871	2.194		end of product

TABLE A.IV.14. 5\$/sec, 80,000 MWd/MTM Yield (2), Swelling gradients

Time	Inner Press., psi	Temperature, °F		Hoop Stress, $\sim$ ksi		Outer Effec- tive Strain $\sim$ %	Plastic Effec- tive Strain $\sim$ %	Outer Strain, $\sim$ %		Swell- ing	Statements
		Inner	Outer	Inner	Outer			Hoop	Mean		
11 inches		50%									
0	500	807	735	-50.37	51.92	0.2823	0.09671	1.657	1.507	0.8097	$\epsilon^t > 0.2\%$
0.2807	3408	959.4	819.9	-32.78	92.33	0.2302	0.4973	2.041	2.010		plastic
0.2907	6314	979.5	833.0	5.06	93.10	0.2368	0.4400	2.206	2.087		tension
0.2952	7621	983.5	838.8								$\epsilon^p > 2\%$
11 inches		80%									
0	800	807	735	-47.38	55.04	0.3077	0.1131	1.677	1.510	0.8165	ϵ^t
0.2864	3030	970.9	827.4	-37.37	91.07	0.2796	0.5532	2.059	2.054		plastic
0.2966	4653	991.2	840.6	-19.41	91.08	0.3168	0.5716	2.163	2.133		$\epsilon^p > 2\%$
19 inches		50%									
7896	757.9	922.7	842.1	-75.86	85.73	0.4559	0.1491	2.714	2.473	1.623	Inner and outer plastic surfaces
0	760	922.7	842.1	-75.99	85.69	0.4770	0.1701	2.79	2.538	1.688	
19 inches		yield (1)									
5997	578.8	922.7	842.1	-57.11	59.17	0.3122	0.1005	2.006	1.841	1.020	Inner and outer plastic surfaces
0	760	922.7	842.1	-57.89	59.20	0.5464	0.3335	2.795	2.509	1.688	
0.000062 hr	760	1046	927.7	-56.88	58.84	0.4373	0.6423	3.102	3.203		$\epsilon^p > 2\%$
19 inches		yield (1)		irradiated creep 0.1-13							
0	760	922.7	842.1	-13.56	26.10	0.7291	0.6395	2.905	2.471	1.688	$\epsilon^p > 2\%$
	760	1052	932	-21.93	33.84	0.3680	0.4665	3.226	3.210		
19 inches		yield (1)		irradiated creep 0.1-12							
1010	218	922.7	842.1	1.109	2.172	0.1578	0.1508	0.9123	0.808	0.0503	tension
0	760	922.7	842.1	4.417	9.723	1.780	1.748	3.816	2.453	1.688	$\epsilon^p > 2\%$
	5798	1181	102.3	47.92	65.18	3.094	2.877	6.128	3.335		

TABLE A.IV.14 (contd)

5%/sec		80,000 MWd/T				yield (2)		swelling gradients			
Time	Inner Press.	Temperature		Hoop Stress, $\sim$ ksi		Outer Effec- tive Strain $\sim$ %	Plastic Effec- tive Strain $\sim$ %	Outer Strain, $\sim$ %		Swell- ing, $\sim$ %	Statements
		Inner	Outer	Inner	Outer			Hoop	Mean		
35 inch yield 1 irradiated creep 0.1-12											
1010	218	1040	996.2	1.471	1.852	0.0849	0.0790	1.016	0.9586	0.029	$\epsilon^P > 2\%$ (all plastic) tension
0	760	1040	996.2	6.619	7.772	0.8686	0.8439	2.637	1.925	-0.991	
	5486	1190	1123.0	50	60.7	3.073	2.881	4.959	2.269	1.239	
35 inch yield 1 irradiated creep 0.1-13											
5050	527	1040	996.2	0.48	9.026	0.1311	0.1014	1.47	1.38	0.448	tension $\epsilon^P > 2\%$
0	760	1040	996.2	1.17	13.08	0.2314	0.1885	2.088	1.93	0.991	
	5372	1189	1122	48.99	59.56	2.357	2.169	4.338	2.267	1.238	

TABLE A.IV.15. Steady State Loading Conditions at 19.8 inches up from the Bottom of the Fuel Column

Steady state history number	Internal pressure psi	External pressure psi	Inner surface temperature °F	Outer surface temperature °F	Flux $\times 10^{-15}$ n/cm ² -sec	Time, hours	Fluence $\times 10^{-22}$ n/cm ²	Burnup MWd/Te	Inner surface swelling %	Outer surface swelling %
0	15	15	70	70	0	0	0	0	0	0
1	101	50	600	600	0	1	0	0	0	0
2	140		923	842	4.55	10	0.016	0	-	-
3	218					1010	1.654	10,000	0.067	0.502
4	373					3030	4.963	30,000	0.430	0.322
5	527					5050	8.272	50,000	1.018	0.762
6	760		923	842	4.55	8080	13.235	80,000	2.253	1.687
7	553	50	600	600	0	8080	13.24	80,000	0.50	0.50
*(1)	757.9	50	922.7	842.1	4.55	7896	12.93			
(2)	627.2	50	922.7	842.1	4.55	6430	10.53			
(3)	574.4	50	922.7	842.1	4.55	5964	9.75			

E. STEADY STATE CONSIDERATION

Geometry

Both 10 and 15 mil wall thickness will be considered. Only one axial position will be considered.

Materials

The material will be of Type 316 stainless steel 20% cold worked.

- a) Poisson's Ratio, $\nu = 0.3$
- b) Young's Modulus $E = 23.8 \times 10^6 \text{ psi}$
- c) Linear hardening parameter $\beta = 0.005$
- d) Yield Strength is yield (1) of Figure A.IV.7
- e) Creep Law is

$$\dot{\epsilon}_{\text{eff}}^c = A \sigma_{\text{eff}}^n + C \alpha_I \sigma_{\text{eff}} \phi \quad (3600 \text{ sec/hr})$$

where $n = 6.78$

$$\log_{10} A = -5 - n(6.73 - 0.0021T)$$

$$\alpha_F = 1$$

$$C_I = 0, 10^{-29}/3., 10^{-28}/3.$$

- f) Dilatation (swelling) function is the same as the 5\$/sec C.W. dilatation function.

Thermal and Mechanical Loading

The steady state loading histories are given in Table A.IV.15 for the axial location that is 19.8 inches up the fuel column. The history numbers 0 through 7 and (1), (2), (3) is the key used to present the results.

Results

Results for the steady state case is in Table A.IV.16 and Figures A.IV.14 and A.IV.15.

TABLE A.IV.16. Results for Steady State

History Number on Time	Wall Thickness, mils	Irradiation Creep Coefficient $\times 10^{30}$	Outer Yield Stress, ksi	Outer Effec- tive stress, ksi	Inner Hoop Stress, ksi	Outer Hoop Stress, ksi	Total Effective Strain, %	Plastic Effective Outer Strain, %	Outer Hoop Strain, %	Outer Mean Strain, %	Statements
0	15	0	100.5	0	- 0.015	-0.015	-	-	-	-	No Swelling Gradients (no swelling and swelling are the same)
1				0.27	0.317	0.266	0.001	-	0.5009	0.5001	
2				14.79	-15.29	14.88	0.054	-	0.7999	0.7722	
3				15.16	-14.73	15.36	0.055	-	0.8016	0.7726	
4				15.89	-13.62	16.32	0.579	-	0.8051	0.7735	
5				16.61	-12.55	17.24	0.605	-	0.8084	0.7742	Elastic Throughout
6				17.75	-10.87	18.68	0.646	-	0.8135	0.7754	
7				2.67	3.52	3.02	0.0097	-	0.5108	0.5024	
3				17.79	-17.70	17.99	0.0648	-	0.8588	0.8250	
4				32.91	-32.88	33.35	0.1199	-	1.175	1.112	
5				57.28	-58.04	57.96	0.2171	0.0086	1.692	1.580	Plastic at Outer Surface
6				100.5	-88.34	101.5	0.4079	0.0419	2.762	2.553	
7				85.18	-73.92	85.85	0.3524	0.0423	2.460	2.280	
3			58.	17.79	Same as above						Inner Surface Plastic Inner and Outer 3 pt on inner and 3 on outer plastic
4											
5			58	57.01	-57.56	57.69	0.2082	0.0006	1.688	1.58	
(3)				58.10	-57.99	58.61	0.2844	0.0728	1.974	1.828	
6				58.40	-58.85	58.60	0.5040	0.2913	2.762	2.506	
7	15	0	58	43.07	-44.46	42.94	0.4482	0.2914	2.460	2.233	

TABLE A.IV.16. (contd)

History Number on Time	Wall Thickness, mils	Irradiation Creep Coefficient $\times 10^{30}$	Outer Yield Stress, ksi	Outer Effec- tive Stress, ksi	Inner Hoop Stress, ksi	Outer Hoop Stress, ksi	Total Effective Strain, %	Plastic Effective Outer Strain, %	Outer Hoop Strain, %	Outer Mean Strain, %	Statements
0	-	-	-	0	- 0.015	- 0.015	-	-	-	-	
1	10	0	100	0.43	0.50	0.45	0.0016	-	0.5016	0.5002	No swelling gradient
2				15.2	-14.67	15.36	0.0553	-	0.7991	0.7702	
3				15.78	-13.83	16.12	0.0535	0.0040	0.8527	0.8234	
4				16.96	-12.15	17.64	0.0641	0.0024	1.132	1.096	
5				18.14	-10.50	19.14	0.0684	0.0024	1.578	1.538	
6				19.95	- 8.00	21.40	0.0750	0.0024	2.511	2.465	
7		0	100	4.13	5.51	4.71	0.017	0.0024	2.205	2.191	All Elastic
2		0	84	15.26	-14.74	15.43	0.0556	-	0.8018	0.7728	
3				17.64	-16.03	17.98	0.0719	0.0077	0.8638	0.8257	
4				30.71	-27.84	31.39	0.1375	0.0257	1.185	1.112	
5				50.91	-47.62	51.94	0.2423	0.0569	1.704	1.575	
7896 (*)				84.26	-75.86	85.73	0.4559	0.1491	2.714	2.473	Inner & outer points plastic
6			84	84.26	-75.99	85.69	0.4770	0.1701	2.790	2.538	
5			58	50.91	-47.62	51.94	0.2423	0.0569	1.704	1.575	
6430 (2)				58.20	-57.29	59.18	0.3557	0.1437	2.156	1.968	Inner & outer points plastic
6			58	59.46	-57.89	59.20	0.5464	0.3333	2.795	2.509	2 inner & 3 outer points plastic
3			58	9.04	- 6.64	9.37	0.0942	0.0613	0.8678	0.8160	
4				11.11	- 6.08	11.76	0.2030	0.1625	1.207	1.090	
5				16.55	- 9.14	17.53	0.3706	0.3103	1.755	1.537	
6	10/3			24.61	-13.56	26.10	0.7291	0.6395	2.905	2.471	
3	100			1.95	1.11	2.17	0.1578	0.1508	0.9123	0.8080	
4				3.87	2.03	4.36	0.4422	0.4281	1.409	1.081	
5				5.74	2.98	6.49	0.8808	0.8599	2.193	1.524	
6	100/3	58		8.58	4.42	9.72	1.780	1.748	3.816	2.453	

AIV.47

FIGURE A.IV.14. VARIOUS DISTRIBUTIONS FOR A 15 MIL WALL THICKNESS

FIGURE A.IV.15. THE INNER AND OUTER SURFACE HOOP STRESS AS A FUNCTION OF HISTORY NUMBER AND DIFFERENT IRRADIATION EFFECTS

F. FUEL THERMAL EXPANSION DURING A 5\$/sec POWER TRANSIENT

To better understand the fuel-cladding interaction produced by thermal expansion a detailed stress and deformation calculation was performed. The analysis is based on elasticity of the fuel during a 5\$/sec power transient. Steady-state calculations have shown that the thermal stress relaxes out of the fuel at the end of life due to creep (CYGRO calculation) and we are left with only the hydrostatic stress generated by the internal gas pressure P_A of the cladding (no fuel cladding interaction at the end-of-life). The high rate of loading during a 5\$/sec power transient will not allow the fuel to creep, hence the fuel will be assumed elastic during the transient and the stress and strain distributions are given by

$$\sigma_R^F = \frac{E^F \alpha^F}{1 - \nu^F} \left\{ \frac{T_V^F}{2} \left[1 - (R_1/R)^2 \right] - (1/R^2) \int_{R_1}^R T^F R dR \right\}$$

$$+ \frac{P_A R_1^2}{R_2^2 - R_1^2} \left\{ 1 - \frac{P_3 R_2^2}{P_A R_1^2} + \frac{R_2^2 (P_3 - P_A)}{R^2 P_A} \right\}$$

$$\sigma_\theta^F = \frac{E^F \alpha^F}{1 - \nu^F} \left\{ \frac{T_V^F}{2} \left[1 + (R_1/R)^2 \right] + (1/R^2) \int_{R_1}^R T^F R dR - T^F \right\}$$

$$+ \frac{P_A R_1^2}{R_2^2 - R_1^2} \left\{ 1 - \frac{P_3 R_2^2}{P_A R_1^2} - \frac{R_2^2 (P_3 - P_A)}{R_2 P_A} \right\}$$

$$\sigma_z^F = \frac{E^F \alpha^F}{1 - \nu^F} (T_V^F - T^F) - P_A$$

$$\epsilon_R^F = \frac{\alpha^F}{1 - \nu^F} \left\{ \frac{T_V^F}{2} \left[1 - 3\nu^F - (1 + \nu^F)(R_1^2/R^2) \right] \right.$$

$$\left. - \frac{1 + \nu^F}{R^2} \int_{R_1}^R T^F R dR + (1 + \nu^F) T^F \right\} + \frac{(1 - 2\nu^F) P_A R_1^2}{E^F (R_2^2 - R_1^2)} \\ \left\{ \frac{((1 + \nu^F) R_2^2 (P_3 - P_A))}{(1 - 2\nu^F) R^2 P_A} - \frac{[(1 - \nu^F) P_3 - \nu^F P_A] R_2^2}{(1 - 2\nu^F) P_A R_1^2} + 1 \right\}$$

$$\epsilon_{\theta}^F = \frac{\alpha^F}{1 - \nu^F} \left\{ \frac{T_V^F}{2} \left[1 - 3\nu^F + (1 + \nu^F) (R_1/R)^2 \right] + \frac{1 + \nu^F}{R^2} \int_{R_1}^R T^F R dR \right\} \\ + \frac{(1 - 2\nu)^F P_A R_1^2}{E^F (R_2^2 - R_1^2)} \left\{ - \frac{(1 + \nu)^F R_2^2 (P_3 - P_A)}{(1 - 2\nu^F) R^2 P_A} - \frac{[(1 - \nu^F) P_3 - \nu^F P_A] R_2^2}{(1 - 2\nu^F) P_A R_1^2} + 1 \right\}$$

$$\epsilon_Z^F = \alpha^F T_V^F + \frac{(1 - 2\nu^F) P_A R_1^2}{E^F (R_2^2 - R_1^2)} \left\{ - \frac{(P_A - 2\nu^F P_3) R_2^2}{(1 - 2\nu^F) P_A R_1^2} + 1 \right\}$$

where σ_R^F = radial stress component

σ_{θ}^F = tangential stress component

σ_Z^F = axial stress component

ϵ_R^F = radial strain component

ϵ_{θ}^F = tangential strain component

ϵ_Z^F = axial strain component

R = radial coordinate

R_1 = inner surface radius

R_2 = outer surface radius

T^F = the difference between the average volume transient and steady state temperature

and the fuel material coefficients are

$\nu = 0.291$ (Poisson ratio),

$E^F = 20 \times 10^6 - 0.5 \times 10^4 (T_V^* - 2000)$ psi is Young's Modulus

and the linear coefficient of thermal expansion is

$\alpha^F = (4.55 + 0.00185 (T_V^* - 500)) \times 10^{-6}/^{\circ}\text{F}.$

T_V^* = is the average volume steady state temperature.

P_A was defined as the internal gas pressure of cladding. P_3 is the pressure acting on the outer surface of the fuel and equal to P_A at the end of life or at the start of the transient, i.e., transient time, t , is zero.

We now assume at $t = 0$ that the fuel is next to the cladding. This means that the fuel tangential strain on the outer surface

$$\epsilon_{\theta}^F \Big|_{R_2} = \alpha_{TV}^F + \frac{P_A [2R_1^2 + \nu^F (R_2^2 - R_1^2)] - P_3 [R_2^2 + R_1^2 - \nu^F (R_2^2 - R_1^2)]}{E^F (R_2^2 - R_1^2)}$$

and inner surface cladding tangential strain

$$\epsilon_{\theta}^F \Big|_{R_3} = \alpha_{TV}^C + \frac{P_3 [(1 + \nu) R_4^2 + (1 - \nu) R_3^2] - P_A [(1 + \nu^C) R_4^2 + (1 - \nu) R_3^2]}{E (R_4^2 - R_3^2)}$$

are equal. Hence we can find P_3 the pressure between the fuel and cladding for any time, t

$$P_3 = P_A + (\alpha_{TV}^F - \alpha_{TV}^C) \left\{ \frac{[(1 + \nu) R_4^2 + (1 - \nu) R_3^2]}{E (R_4^2 - R_3^2)} + \frac{(1 - \nu^F) R_2^2 + (1 + \nu^F) R_1^2}{E^F (R_2^2 - R_1^2)} \right\}$$

This interface pressure P_3 will continue to build up as the temperatures increase. At time $t = t^*$ the cladding will become a plastic body, then P_3 will become a constant since the cladding is a plastic body it can no longer restrain the fuel. Hence cladding must move at the same rate as fuel or

$$\dot{\epsilon}_{\theta} \Big|_{R_3} = \dot{\epsilon}_{\theta}^F = \alpha_{TV}^F \dot{T}_V$$

and

$$\dot{\epsilon}_Z = \dot{\epsilon}_Z^F = \alpha_{TV}^F \dot{T}_V$$

where $(\dot{})$ denotes the time rate of change. One more assumption used in the following is that the volume change due to the fuel melting is not considered.

A hand calculation was performed based on the above assumption. Figure A.IV.16 shows the yield stress and temperature histories of the cladding used in the analysis. The cladding material properties used are

Poisson's ratio, $\nu = 0.3$

Young's modulus, $E = 20 \times 10^6$ psi

Linear expansion, $\alpha = (8.55 + 0.00147T) \times 10^{-6}/^{\circ}\text{F}$

Two average volume temperature histories that correspond to a 87 mil wall and a 11 mil wall are given in Figure A.IV.17. The fuel material properties are given above. The interface pressure P_3 was calculated for a 87 mil fuel wall (all of the fuel) and both 10 and 15 mil cladding wall thickness. Also an 11 mil fuel wall and a 15 mil cladding wall were analyzed. The results are presented in Figure A.IV.18. The cladding effective strain was then calculated and plotted in Figure A.IV.19.

Figure A.IV.19 shows that all three cases analyzed exceed the 0.2% strain criteria before the time of 0.25 seconds at which time gas is released to the center void. (See the 6¢/sec uninterrupted transient calculations.) Also shown is the time (0.22 second) at which melting on the inner surface of fuel will occur. For a 87 mil fuel wall, 0.2% strain occurs at 0.185 and 0.19 seconds for the 15 and 10 mil cladding wall thicknesses, respectively. To compare these results with Section V results we see that Figure V.11 shows 0.2% strain at time equal to 0.15 seconds. The difference is due to the elasticity of the fuel and cladding.

To understand how the effective strain can exceed the 0.2% criteria, one must define primary and secondary loads. An example of a primary load in a tube is internal pressure. The pressure buildup will cause the tube to become plastic and fail (burst tests) even if the pressure buildup was stopped just before the tube became plastic. Creep will cause failure (creep rupture tests). Another example of a primary

AIV.54

FIGURE A.IV.16. THE HISTORY OF THE CLADDING TEMP. AND YIELD STRESS OF THE CLADDING FOR A 5%/SECOND TRANSIENT

FIGURE A.IV.17 THE HISTORY OF THE FUEL TEMPERATURE FOR A 5 \$/SECOND TRANSIENT

FIGURE A.IV.18. THE TIME HISTORY OF PRESSURE BETWEEN FUEL AND CLADDING FOR A 5 PSI/SECOND TRANSIENT

AIV.57

FIGURE A.IV.19. THE TIME HISTORY OF INNER SURFACE CLADDING EFFECTIVE STRAIN FOR A \$5/SECOND TRANSIENT

load is the pressure in the center voids of the fuel due to gas release which begins to buildup at 0.25 seconds.

The pressure build up between the fuel and cladding due to thermal expansion is a secondary load. The reason being that the pressure will decrease as soon as the temperature increase has stopped. Suppose the tube is plastic and the temperatures are constant. Then the tube will creep and reduce the interface pressure. And the tube will then become an elastic material again and no failure.

The conclusions are based on the discussion and results and may be summarized as follows:

- 1) Thermal expansion is a secondary load.
- 2) 0.2% strain may be exceeded during a transient.
- 3) Gas release is a primary load.
- 4) Failure will occur at 0.25 seconds.

G. REFERENCES

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4. To J. M. Shivley, From E. R. Astley, "Irradiation Induced Creep Equation Recommendations" BNW/FFTF 693697, dated December 15, 1969.
5. Fast Flux Test Facility Quarterly Technical Report, September, October, November 1969, BNWL-1275. January 1970.

APPENDIX V

STRESS INTENSITY FACTORS

D. C. Bullington

The observation of intergranular cracks by General Electric⁽¹⁾ on EBR-II irradiated fuel specimens after a burnup of 74,000 MWd suggests that a model of crack initiation and growth may also be necessary to predict the performance of FFTF fuel during steady state and transient conditions. James⁽²⁾ has suggested that such a factor would predict crack growth during stress cycling such as may occur during reactor shutdown but also would provide a means of estimating the growth of stress corrosion cracks should they be identified. If cracks of some length can be predicted as a function of burnup then growth during a large application of stress as might occur during a transient can also be predicted.

a. Experimental Data

No experimental data of 316 20% cold worked stainless is known at the present time that evaluate the growth of cracks under cyclic stress in an environment typical of reactor conditions. If there is no fuel-cladding interaction it has been shown that the inner cladding surface should be under compression when the rod is producing power.* Preliminary studies of the pressure between fuel and cladding have suggested the interaction pressure is on the order of 16,000 psi. The loss of ductility of the cladding due to irradiation damage increases the likelihood of crack extension and the presence of some fission products in the vapor phase notably cesium and iodine may assist the growth of the cracks when the cladding is in a tensile condition. Figure AV.1⁽²⁾ shows experimental data of unirradiated 304 stainless steel at three temperatures 75 °F, 1000 °F and 1200 °F. Although it is recognized that such data may only be remotely applicable to irradiated 20% cold worked stainless steel after irradiation in-pile it was applied to

* See Appendix IV

FIGURE A.V.1. STRESS INTENSITY FACTOR RANGE ΔK , psi $\sqrt{\text{in.}}$ (2)

the length of cracks observed by General Electric to predict the length of a crack as a function of time.

b. Prediction of Crack Length With Time

The growth rate of cracks in metals in sheet form under cyclic conditions has been shown to be of the form

$$\frac{da}{dN} = C_1 \left[\Delta \sigma \sqrt{\pi a} \right]^n F \left(\frac{a}{w} \right)$$

Where

C, n, C_1 = material constants dependent upon environmental considerations obtained from Figure V.3

σ = the maximum stress (psi)

a = crack length (inches)

$F\left(\frac{a}{w}\right)$ = a shape factor given below (see Table AV.1)

$$F\left(\frac{a}{w}\right) = \left[1.12 - 0.23\left(\frac{a}{w}\right) + 10.55\left(\frac{a}{w}\right)^2 - 21.71\left(\frac{a}{w}\right)^3 + 30.38\left(\frac{a}{w}\right)^4 \right]$$

$\frac{a}{w}$ = ratio of crack length to specimen thickness

For the purpose of calculation the following assumptions were made with regard to power cycles of EBR-II during the time specimen F2H of General Electric was in-pile. One hundred power cycles occurred during the 284 days in-pile and no crack growth occurred during the first 50 cycles. These assumptions are consistent with EBR-II reactor power history and observations of the cladding of similar specimens in the series after 50,000 MWd/MT. ⁽³⁾

Using the previous assumptions of crack growth, the one General Electric data point and the 304 experimental data shown in Figure AV.1 (slope for 1200 °F data below $K = 10^4$), Figure AV.2 was drawn. Using Figure AV.2, the assumption that a 16,000 psi tensile stress occurs during each of the final 50 cycles that occur every 1/2.84 days followed by essentially an

FIGURE A.V.2. PREDICTED STRESS INTENSITY FACTOR AS A FUNCTION OF TIME IN EBR-II

unstressed state, and Table AV.1, Figure AV.3 was drawn which shows a prediction of the stress intensity factor K as a function of time in days after irradiation began.

TABLE AV.1. Shape factor $f(\frac{a}{W})$ for Single Edge Crack

Single Edge Notch. $K = \text{Sigma} + (\text{PI} * A) ** 0.5 * F(A/W)$

<u>A/W</u>	<u>F(A/W)</u>	<u>A/W</u>	<u>F(A/W)</u>	<u>A/W</u>	<u>F(A/W)</u>
0.01	1.1215	0.21	1.3975	0.41	2.1637
0.02	1.1222	0.22	1.4225	0.42	2.2236
0.03	1.1247	0.23	1.4485	0.43	2.2866
0.04	1.1291	0.24	1.4756	0.44	2.3529
0.05	1.1350	0.25	1.5038	0.45	2.4226
0.06	1.1425	0.26	1.5331	0.46	2.4959
0.07	1.1515	0.27	1.5635	0.47	2.5731
0.08	1.1619	0.28	1.5953	0.48	2.6543
0.09	1.1735	0.29	1.6283	0.49	2.7398
0.10	1.1864	0.30	1.6628	0.50	2.8298
0.11	1.2005	0.31	1.6987	0.51	2.9244
0.12	1.2157	0.32	1.7363	0.52	3.0241
0.13	1.2319	0.33	1.7754	0.53	3.1289
0.14	1.2492	0.34	1.8164	0.54	3.2391
0.15	1.2675	0.35	1.8593	0.55	3.3561
0.16	1.2868	0.36	1.9042	0.56	3.4770
0.17	1.3070	0.37	1.9512	0.57	3.6052
0.18	1.3282	0.38	2.0005	0.58	3.7400
0.19	1.3503	0.39	2.0523	0.59	3.8815
0.20	1.3734	0.40	2.1066	0.60	4.0302

Figure AV.3 was then used to predict crack length as a function of time using equation which predicts $\frac{da}{dN}$ and numerically integrating as a function of time. The result of this analysis is shown in Figure AV.4. What is indicated is a crack starting shortly before 30,000 MWd/MT and reaching a length of 6 mils on the cladding interior due to fuel clad reaction may be too low. It also indicates that the wall thickness may be reduced 8 mils at end of life (80,000 MWd/MT) instead of the presently assumed 5 mils.

FIGURE A.V.3. PREDICTED RATE OF CRACK GROWTH VERSUS STRESS INTENSITY FACTOR RANGE USING 1 GE DATA POINT AT 74,000 MWd/MT IN EBR-II

FIGURE A.V.4. CRACK LENGTH (MILS) VERSUS TIME (DAYS)

This analysis is highly uncertain due to lack of experimental data. The application of these equations to growth of cracks in tubular specimens creates additional uncertainty due to the bending moment present. It is recognized that the stress intensity factor should be greater for tubular specimens but there is no known way of obtaining it theoretically. (2)

This analysis does point out a potential limitation of fuel life and points out the need for experimental data to study crack growth in cold worked, irradiated 316 tubing at temperatures expected in the FFTF.

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APPENDIX VI.

BURST STRENGTH OF EBR-II IRRADIATED FUEL PIN SECTIONS

R. L. Fish, J. J. Holmes and R. D. Leggett

INTRODUCTION

In order to evaluate the effects of fuel interaction on cladding strength, burst tests were conducted on two annealed Type 304 stainless steel clad fuel pins from the PNL-Series 1 experiment which was irradiated in EBR-II.

A limited amount of information is available⁽¹⁻⁶⁾ concerning the integrity of cladding for LMFBR applications. Metallographic analyses⁽¹⁻⁴⁾ has been used to estimate the extent and character of fuel/cladding chemical interactions and a few studies have been reported on the effects of the interactions on the strength of the cladding.^(5,6)

FUEL PIN DESCRIPTION

Mixed oxide (UO_2 -25% PuO_2), stainless steel clad (0.250 in. OD x 0.016 in. wall) fuel pins were irradiated in position 6C1 during cycles 26C through 28C (excluding 27G through 28A) and accumulated about 8500 MWd/MTM burnup to the fuel (56.8 EFPD at 8.5 kW/ft peak pin power) and approximately 5.0×10^{21} n/cm² E > 0.1 MeV peak fluence to the cladding. The irradiation temperature of the cladding ranged from 800 °F to 950 °F over the fueled cladding length with an average cladding radial ΔT of about 50 °F. The irradiation was performed in sodium filled capsules of the MK-A-19 type. Fuel and cladding compositions are given in Table AVI.1.

EXPERIMENTAL PROCEDURE

Specimens 2-3/4 inches in length were cut by abrasive wheel from positions above and below the fuel midplane as well as from the plenum region. The specimen location, identification, and temperature and fluence profiles are shown in Figures AVI.1 and AVI.2 and the irradiation conditions are given in Table AVI.2. Following the cutting procedure each specimen was prepared for pressure testing by attaching Swagelok[®] fittings

TABLE AVI.1. As-Fabricated Fuel and Cladding Compositions

Fuel: (UO₂-25 wt% PuO₂) Sintered Pellets

<u>Element</u>	<u>Maximum Concentration, ppm</u>	<u>Element</u>	<u>Maximum Concentration, ppm</u>
C	25	Mn	5
Cl	10	Na	2
F	25	Ni	10
Ag	0.1	P	50
Al	1000	Pb	20
B	0.5	Si	80
Ca	1	Sn	20
Cr	25	Zn	100
Cu	200	V	20
Fe	160	K	20
Mg	5	Mo	20

Cladding: Mill annealed Type 304 SS
 Ø.250 in. OD x 0.016 in. wall

Element Concentration, %

C	0.056
Mn	1.73
P	0.038
S	0.011
Si	0.47
Ni	9.90
Cr	18.50
B	0.0005
N ₂	0.10
Ti	0.006
O ₂	0.0103
Al	0.002
Mo	0.29
Cb	0.030
Fe	balance

A.VI.3

FIGURE A.VI.1. AXIAL FLUENCE AND TEMPERATURE PROFILE
(PNL 1-3)

FIGURE A.VI.2. AXIAL FLUENCE AND TEMPERATURE PROFILE
(PNL 1-6)

TABLE AVI.2. PNL-1 Fuel Pin Burst Test Results

<u>Specimen Identification</u>	<u>Cladding Midwall Operating Temperature</u>	<u>Fluence $E > 0.1 \text{ MeV}$ n/cm^2</u>	<u>Test Temperature</u>	<u>Hoop Stress, psi</u>	<u>Strain, $\Delta D/D \%$</u>
Control			900 °F	56,960	19.2
Plenum Region					
PNL 1-6:H ₁	~890 °F	$\sim 1.2 \times 10^{21}$	900	59,090	18.4
Above Fuel Midplane					
PNL 1-6:G ₁	~920	$\sim 4.2 \times 10^{21}$	900	22,020*	1.0
PNL 1-6:G ₂	~920	$\sim 3.0 \times 10^{21}$	900	27,160*	0.3
PNL 1-3:G ₄	~945	$\sim 3.7 \times 10^{21}$	900	38,900*	0.2
Below Fuel Midplane					
PNL 1-6:C ₃	~850	$\sim 4.1 \times 10^{21}$	900	72,150	9.6
PNL 1-3:C ₂	~860	$\sim 4.3 \times 10^{21}$	1000	66,790	6.7

* Maximum values. Because of the nature of the failure, cladding perforation may have occurred at significantly lower values.

to the ends of the specimen . It is important to note that fuel material within the specimen was not removed or intentionally altered in any way in order that the effects of the fuel and fission products could be determined.

Each specimen was subsequently attached to a pressurization line within a test chamber designed to totally contain fission product release during testing. The test chamber was then placed in a furnace at 900 or 1000 °F and allowed to thermally equilibrate. A soak time of about three hours was required to stabilize the temperature. Each specimen was pressurized at 20 psi/sec until failure occurred. Failure was indicated by a drop in pressure within the specimen. Figure AVI.3 illustrates the test apparatus.

Metallographic analysis of several failed sections was performed upon completion of testing. Optical metallography was supplemented with extraction replication to detect possible carbide and sigma precipitation within the cladding. Microprobe analysis was performed to evaluate fission product penetration of the cladding.

RESULTS

Burst test results are included in Table AVI.2.

Specimens from below the midplane burst at pressures either equivalent or slightly above the unirradiated control value. All three specimens taken above the midplane but within the fuel column failed at stresses less than one-half of the control value. The fracture mode of specimen failing at reduced stresses was also greatly different than for the other specimens tested (Figure AVI.4). Stainless steel cladding burst tested at 900 to 1000 °F normally ruptures violently and produces a large amount of specimen distortion. Considerable transgranular tearing and fragmentation often occurs during a typical failure. In specimens containing fuel material above

**BIAXIAL
TEST
APPARATUS
FOR IRRADIATED
FUEL PIN
SPECIMENS**

FIGURE A.VI.3. TEST APPARATUS

ABOVE FUEL MIDPLANE

CONTROL

PLENUM

BOTTOM OF PNL-1

FIGURE A.VI.4. COMPARISON OF FAILURE MODES IN PNL-1 FUEL PINS

the midplane, the low stress failures were characterized by multiple pinhole failures along the length of the specimens rather than the expected explosive tearing. Once the pinholes penetrated the cladding, the pressurizing gas leaked out indicating failure. The values shown in Table AVI.2 for samples exhibiting this latter type of failure represent maximum stresses. It is possible that cladding perforation may have occurred at pressures substantially below those that were presumed to represent failure but not detected. A single small crack, for example, would leak gas slowly and not be detected with this method of detecting failure.

The microstructural features of the two failure types are shown in Figure AVI.5. Specimens failing explosively show the characteristic necking near the fracture and transgranular mode of failure. Specimens from below the reactor midplane failed in this fashion. Samples from above the reactor midplane, however, showed practically no local deformation and failed in a completely intergranular mode of failure. The grain boundaries appeared to be very weak as evidenced by the loss of several whole grains within the cladding during the polishing operation.

Extraction replication revealed a greatly different level of grain boundary precipitation in specimens taken from above and below the midplane within the fueled region. Below the midplane, no precipitates were observed by the extraction technique (Figure AVI.6). Similar results were obtained after aging identical material for 1300 hours at 900 °F. Above the midplane, heavy grain boundary precipitation was found (Figure AVI.7). These precipitates were identified by X-ray diffraction as $M_{23}C_6$ and sigma phase.

Metallography on these fuel pins was performed to further characterize the precipitate formation in the cladding as it varied axially. Figures AVI.8 and AVI.9 illustrate the gradual

EXPECTED FAILURE MODE

50X

OBSERVED IN TOP OF FUELED SECTIONS

75X

TOP FUEL

250X

TOP FUEL

750X

FIGURE A.VI.5. COMPARISON OF MICROSTRUCTURES IN PNL-1 FUEL PINS

A.VI.11

FIGURE A.VI.6. EXTRACTION REPLICA OF SPECIMEN FROM BELOW FUEL MIDPLANE

FIGURE A.VI.7. EXTRACTION REPLICA OF SPECIMEN FROM ABOVE FUEL MIDPLANE

CLAD. OD 795⁰F
 CLAD. ID 835⁰F
 FLUENCE (E>0.1 MeV)
 = 3.4×10^{21} n/cm²

CLAD. OD 870⁰F
 CLAD. ID 930⁰F
 FLUENCE (E>0.1 MeV)
 = 4.5×10^{21} n/cm²

CLAD. OD 880⁰F
 CLAD. ID 935⁰F
 FLUENCE (E>0.1 MeV)
 = 4.4×10^{21} n/cm²

FIGURE A.VI.8. IRRADIATED IN EBR II AT 8.1 kW/FT TO 7900 MWd/MTM
 (PNL-1-6)

A.VI.14

FIGURE A.VI.9. IRRADIATED IN EBR II AT 8.8 kW/FT TO 8200 MWd/MTM
(PNL-1-3)

increase in grain boundary precipitation in going from the lower to the upper parts of the fueled portion of these pins.

The primary difference in irradiation conditions between specimens above and below the midplane is irradiation temperature. The average irradiation temperature below the midplane was about 850 °F while above the midplane the average temperature was around 920 °F. Aging identical material out-of-pile at 800, 850 and 900 °F for 1300 hr (the time in-reactor) failed to produce substantial amounts of precipitation. This suggests either that the irradiation temperatures were much higher than those calculated or that other environmental factors present during the irradiation enhanced the grain-boundary precipitation. Possible environmental influences include:

- irradiation enhanced diffusion⁽⁷⁾ which is of sufficient magnitude to allow enough mass transport for the formation of large precipitates at 920 °F but not at 850 °F
- cladding stresses which assist precipitation
- impurity pickup in the cladding from either the capsule components or the fuel.

The possibility that the actual cladding temperatures were higher than those calculated cannot be precluded, but analysis of the fuel structure⁽⁸⁾ was in agreement with the values reported in Table AVI.2.

The results from efforts to identify a possible corrosive agent that may be causing the intergranular failure by using a shielded electron microprobe have been inconclusive. Figure AVI.10 illustrates the results of the examination of a fuel pin sample taken 1/2 inch below a burst test specimen which failed at a low burst pressure (above the midplane). As indicated by the microprobe display, uranium, plutonium, cesium, iodine, barium, and tellurium were found in a layer along the inside of the cladding. The cesium distribution shows an increased concentration at approximately 8 microns inside the cladding (the area represented by Line BB of the lower right photograph in Figure AVI.10). This is illustrated by the

FIGURE A.VI.10. MICROPROBE RESULTS
(PNL-1-6-F)

graphical plot "B" and by the higher magnification specimen current display. It is significant that there is little or no fuel associated with the cesium peak at about 30 microns inside the cladding. Plots "A" and "C" illustrate graphically the distribution of cesium in Lines AA and CC, respectively. The lower plot of Fe, Cr, and Ni shows the counting rate of these elements as scanned along Line BB. The counting rates are proportional to the respective concentrations. The inside edge of the cladding is judged to be at about 24 microns on the relative distance scale. The existence of the increased cesium concentration in this region of the cladding is thought to be significant but as this is the only area on the circumference of the cladding cross section found to have any excess cesium it is difficult to assign a major role to cesium in promoting the severe intergranular failure that was observed during burst testing.

DISCUSSION

The grain boundary failure at low stresses in specimens from above the midplane represents a marked departure from expected cladding behavior. Fast reactor irradiation of 304 stainless steel under similar conditions does not lead to reduced fracture stress or premature grain boundary failure.⁽⁹⁻¹¹⁾ Ductility is reduced somewhat but the fracture mode remains transgranular and failure proceeds by plastic instability.

The mechanism responsible for the drastic reduction in grain boundary strength is as yet unclear. The key difference between specimens from above and below the midplane appears to be the level of grain boundary precipitation. Since the failure mode is different at these two locations, it is probable that grain boundary precipitation is an underlying factor. A number of possible explanations in terms of boundary precipitates exist.

The irradiation induced precipitates may be much weaker or lack cohesion with the matrix and thereby provide an easy

path for crack nucleation. Similar precipitate morphologies created by thermal aging at temperatures near 1500 °F do not lead to low stress failure or drastically reduced ductility and grain boundary weakness. This indicates that, unless the $M_{23}C_6$ and sigma phase created during irradiation are metallurgically different than that created by thermal aging, the precipitates are not directly responsible for the loss in boundary strength.

Grain boundary precipitation changes the relative concentration of the alloy constituents near the boundary resulting in reduced corrosion resistance.⁽¹¹⁾ Corrosion or stress corrosion resulting from the loss in corrosion resistance could weaken the boundaries and produce the observed effects. The constituent causing the corrosion could result from the hot cell environment, from fission products within the cladding, or a combination of both. The actual source of the possible corrosion is extremely important in the application of these results to nuclear technology. If the corroding agent exists in the hot cell environment or affects the as-irradiated conditions of the fission products, etc., then special care must be taken in the interpretation of postirradiation examination of irradiated fuel pins to be certain that observed effects are the results of irradiation and not handling. More important, special procedures must be taken in handling of fuel pins during postirradiation examination. Alternately, if the corrodant is one of the fission products, then the cladding strength used in design must be greatly reduced from that derived from post-irradiation tensile and creep tests on nonfueled specimens. Any corrosion from the fission products may be the result of hydrolysis of such elements as Cs or Rb and may not have occurred if the irradiated fuel had been protected from moisture at all times.

CONCLUSIONS

It is concluded that:

- a) Improved procedures are required for storage and handling environments.
- b) The loss in ductility and rupture strength is associated with $M_{23}C_6$ and sigma phase along grain boundaries.
- c) Severe grain boundary attack occurred on the specimens exhibiting poor mechanical properties (cause of attack unknown).
- d) Cesium was observed in the cladding.
- e) Failures in PNL-1-3 and 1-6 specimens initiated on the inside surface of the cladding.
- f) The exact cause of the results is not yet defined. Cleaning and/or fuel environment may have contributed to the results.

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APPENDIX VII

FFTF MIXED OXIDE FUEL DEVELOPMENT AND EVALUATION

J. E. Hanson and J. W. Hagan

The FFTF mixed oxide fuel development program underway at BNW is directed toward:

- (a) Development and demonstration of fabrication processes for fuel pins and assemblies which are reliable and predictable in performance over their entire lifetime.
- (b) Demonstration of overpower and overtemperature performance capability beyond normal operation to provide adequate safety margins to withstand the abnormal consequences which may result under accident conditions.
- (c) Development of a sound analytical base for evaluation of fuel pin performance and response under normal and abnormal reactor conditions.

To accomplish these objectives, the FFTF Project has initiated a broad irradiation testing program including both steady-state irradiations and in-reactor transient testing programs. These programs are strongly integrated with analytical studies in which anticipated performance is predicted as a basis for establishing the test conditions. The test results then provide a basis for reevaluation of the analytical techniques. In addition, the BNW programs are being integrated within the overall LMFBF fuels development programs in establishing the broad technological base for mixed oxide fuels.

The specific FFTF fuels development activities related to safety are categorized according to the following program titles:

- (a) - Fuel Irradiation and Evaluation
- (b) - Steady-State and Transient Performance Limits
- (c) - Fast Flux Pin Irradiations
- (d) - Fuel Performance Analysis and Prediction
- (e) - Irradiation and Hypostoichiometric UO_2 - PuO_2 Fuel

The detailed activities under each program are given below.

The primary emphasis of the FFTF program is verification of established parameter limits corresponding to specified damage criteria for the FFTF. As such, the FFTF Project is investigating comparative effects on these limits of fast and thermal fuel irradiations, the dependence of transient results on the test fuel pin length, and possible consequences of wet and dry fuel slumping. In accordance with the program directive to use available technology, offsite LMFBR development programs are depended upon to provide basic information on such developmental questions as the following:

- (a) Evaluation of performance of defected (sodium-logged) fuel in terms of potentially reduced lifetime capability, susceptibility to gradual deterioration to gross failure, or reduced transient capability. Two offsite programs will provide transient performance data on the response of sodium-logged fuel pins.

These programs are:

- ANL - Fuel Meltdown Studies in TREAT - Transient tests are planned with sodium-logged pins in a transparent capsule to evaluate thresholds and modes of failure with subsequent tests in the Mark II test assembly which characterizes flowing sodium during the transient power runs.
- GE - Fast Ceramic Reactor Development Program PA-10 Task C - A transient test is planned which will evaluate the relative failure thresholds of normal fuel pins and sodium-logged (or bonded) fuel pins.

- (b) Evaluation of the consequences of various fuel pellet axial restraints on transient performance. This is being investigated within the following program:

- GE - Fast Ceramic Reactor Development Program PA-10 Task C - Comparative tests are being performed with pins incorporating axially restrained fuel and pins with intentionally provided paths for axial movement

of molten fuel toward the blanket regions. These results may support this mechanism as an inherent safety feature.

Complementary programs are being conducted in the following areas:

- (a) Performance of integral tests simulating loss of flow without immediate plant protective system response.

The following offsite program is directly applicable:

- GE - Fast Ceramic Reactor Development Program PA-10 Task C - A loss-of-flow test is planned in which flow will be reduced at some given rate or stopped to the test element while at-power within an in-reactor capsule. This integral test will evaluate the effects of coolant expulsion, reentry, and possible severe materials interactions.

- (b) Evaluation of the effects of fuel pellet stoichiometry on swelling and long-term performance. The following offsite program is related to this area:

- GE - Fast Ceramic Reactor Development Program PA-10 Task B - This program is investigating the stoichiometric effects upon long-term performance of defected fuel.

- (c) Investigation of the consequences of individual cladding failures during normal or near-normal operating conditions.

- GE - Fast Ceramic Reactor Development Program PA-10 Tasks B and C - As described above.
- ANL - Fuel Element Failure Propagation Program - Fuel pin failures during operation at power in a sodium package loop in ETR are planned within this program.

- (d) Evaluation of the transient performance of near-prototypic fast reactor irradiated fuel pins. The following program applies to this area.

- ANL - Fuel Meltdown Studies in TREAT - This program will perform TREAT experiments on prototypic pins irradiated in DFR (U.K.).

In addition, several LMFBR development programs are involved in evaluated potential materials interactions following fuel pin cladding failures. These data will evaluate gross overheating consequences and include the following:

- GE - Fast Ceramic Reactor Development Program
PA-10 Task C - Sudden mixing of molten materials is planned in an in-reactor capsule.
- ANL - Violent Boiling Program - This includes liquid drop experiments and TREAT autoclave experiments in which gross overheating of the fuel is performed in a sodium environment.

Further verification of the fuel performance under phototypic conditions will result from actual operation in the FFTF. It is anticipated that initial operation will include test assemblies at greater than normal powers in loops to verify the predicted safety margins. In addition, extensive post-irradiation testing will be performed on driver fuel at each significant increasing level of burnup.

The following program descriptions summarize those programs being performed by BNW within the area of FFTF fuel development and performance.

(A) FUEL IRRADIATION AND EVALUATION

This program is investigating fuel performance characteristics during fast flux irradiation as a function of various fuel pin design parameters and operating conditions. These parameters include initial radial gap between the cladding and pellets, percent cold working of the 316 SS cladding, fuel test length, and linear pin power during operation. The bulk of the steady-state irradiations will be performed in EBR-II. An assembly is planned for irradiation in the EFFBR if it should become available.

After steady-state irradiations in EBR-II, selected pins will be allocated for transient testing in TREAT. The TREAT tests will be performed to simulate the consequences under postulated FFTF accident conditions corresponding to the limit conditions within the minor or major accident categories.

(B) STEADY-STATE AND TRANSIENT PERFORMANCE LIMITS

This program will investigate the steady-state and transient performance of mixed oxide fuel pins of prototypic length (~36 in.). These investigations will involve steady-state irradiation in GETR (EBR-II cannot accommodate fuel lengths greater than ~13 in.) and subsequent transient testing in TREAT. Specific parameters in these studies include evaluation of the effects of fuel motion restrictors, comparison of 304 SS and 316 SS cladding, evaluation of the capability of fuel to continue operation after some central melting of the oxide, and the effects of power cycling. Post-irradiation examinations will provide information on plutonium and fission gas migration characteristics which in turn affect transient performance of the fuel under postulated accident conditions.

TREAT tests will be performed to simulate accident conditions as described in the foregoing program for the EBR-II pin irradiations.

Specific tests will evaluate transient performance of pins at various levels of burnup and cladding material, with and without restrictors. This program also includes a run-to-failure test in GETR at normal power levels and a long-term exposure test under 20% overpower conditions. Other tests will evaluate internal fuel movement under transient conditions after exposure in GETR and the consequences of continued operation of fuel after experiencing a transient overpower condition into the center melting range. Performance under severe power cycling conditions will be investigated. Possible dry slumping of separated pellets within a fuel pin subjected to vibrations and mechanical shocks will also be evaluated.

(C) FAST FLUX PIN IRRADIATIONS

This program is expected to provide statistically significant steady-state fast flux irradiation experience with reference and alternate $\text{UO}_2\text{-PuO}_2$ FTR driver fuel at peak, average and minimum anticipated FTR pin linear power ratings. Data obtained will be applied to developing analytic models to describe mixed oxide fuel pin behavior as a function of fuel and cladding parameters, temperature, fluence and burnup.

(D) FUEL PERFORMANCE ANALYSIS AND PREDICTION

This program provides the analytical support for the experimental programs described in the foregoing. The studies will include development and verification of models for prediction of

- Fuel cladding swelling
- Cladding failure models
- Fission gas distribution and release as a function of operating temperature and transient melting
- Fuel growth and restructuring within the cladding
- Fuel motion within the cladding

These studies will provide the bases for various transient tests in TREAT to assure that the appropriate modeling criteria are applied. In addition, the adopted models will provide the bases for the safety analyses in establishing the fuel performance limits and safety margins. All available data from U.S. and foreign programs are being evaluated and incorporated into performance analysis studies.

IRRADIATION OF HYPOSTOICHIOMETRIC $\text{UO}_2\text{-PuO}_2$ FUEL (12212/FP-6)

This program is investigating fuel performance as a function of initial oxygen-to-metal ratio of the fuel. The more important fuel properties (e.g., thermal conductivity, melting point, creep strength, etc.) are known to vary with initial O/M thus, performance of fuels with a range of oxygen contents corresponding to that anticipated from combined fissioning and fabrication effects must be understood.

APPENDIX VIII.

MATERIAL PROPERTIES SECTION

The following figures show the material properties used in the stress analyses in Appendix A. IV. The data are for unirradiated, annealed 316 stainless steel. (1-4) Unirradiated annealed properties were used because of their conservative strength values and because mechanical properties of irradiated, cold worked 316 are not presently available.

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A.VIII.2

FIGURE A.VIII.1. UNIRRADIATED TYPE 316 SS POISSON'S RATIO σ
ASSUME $\sigma = 0.3$ FOR ALL TEMPERATURES

A.VIII.3

FIGURE A.VIII.2 UNIRRADIATED TYPE 316 SS MODULUS OF ELASTICITY

FIGURE A.VIII.3. UNIRRADIATED TYPE 316 SS CREEP STRENGTH

FIGURE A.VIII.4. TEMPERATURE DEPENDENCES PRODUCED BY SWELLING EQUATIONS FOR SOLUTION TREATED TYPE 304 STAINLESS STEEL AND TYPE 316 STAINLESS STEEL WITH 20% COLD WORK

APPENDIX IX.

EFFECTS OF REACTOR TRANSIENTS ON CLADDING PERFORMANCE

T. T. Claudson and J. J. Holmes

This memo is to provide preliminary guidance on the effects of rapid temperature excursions on cladding performance. Little data is readily available in this area. However, the main problems appear to be recovery of cold work, irradiation damage removal and second phase precipitation.

Among these three problem areas, recovery of cold work is probably the most important. Recovery kinetics for a nominal 10% decrease in properties (strength) were estimated from four 90% recovery data points and a single 10% recovery point. The 90% data were plotted as a function of logarithmic time and linear temperature (Figure A.IX.1) and a straight line was fitted to the four data points. The 10% line was assumed to be parallel to the 90% line and passes through the one 10% data point. This procedure is approximate and only gives very rough indications. Much additional work is needed to fully describe the recovery kinetics. It must be emphasized that the above analysis is not based on an exhaustive literature search, but represents only a cursory study of the problem.

The effect of transients on precipitation and radiation damage are thought to be insignificant. Little damage recovery should occur during transient as long as the total time at temperature is less than about one minute. Any recovery which does occur would tend to improve ductility and should not be detrimental to cladding performance.

Second phase precipitation will occur during normal operations. Reactor transients will probably not lead to any increased precipitation but may produce some precipitate coarsening. This coarsening is not thought to be a problem.

Accurate estimation of the effects of transients on cladding performance requires much more data than is presently available. Much additional work needs to be conducted in this area.

A.IX.2

FIGURE A.IX.1. PRELIMINARY RECOVERY CURVES FOR 316 STAINLESS STEEL