

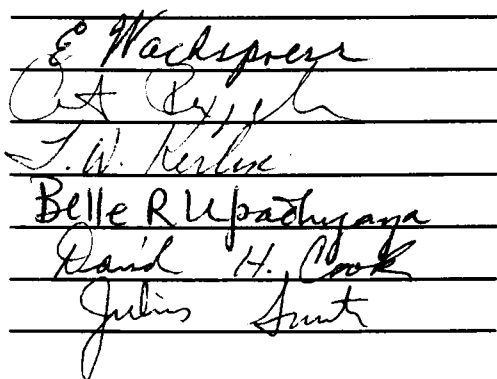
To the Graduate Council:

I am submitting herewith a dissertation written by Tanju Sofu entitled "*A Model for the Analysis of HFIR Reactivity Transients and Fuel Damage Propagation*." I have examined the final copy of this dissertation for form and content and recommend that it be accepted in partial fulfillment of the requirements for the degree of **Doctor of Philosophy**, with a major in **Nuclear Engineering**.



H. L. Dodds, Major Professor

We have read this dissertation
and recommend its acceptance:



Accepted for the Council:



Associate Vice Chancellor and
Dean of the Graduate School

**A MODEL FOR THE ANALYSIS OF
HFIR REACTIVITY TRANSIENTS
AND FUEL DAMAGE PROPAGATION**

A Dissertation Presented for the
Doctor of Philosophy Degree
The University of Tennessee, Knoxville

Tanju Sofu
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ABSTRACT

A coupled neutronics and core thermal-hydraulic performance model has been developed for the analysis of high flux research reactor reactivity insertion accidents. The model is developed specifically for the ORNL's High Flux Isotope Reactor (HFIR) but can be adapted to other aluminum clad, plate-type core geometries. The model includes point reactor kinetics for neutronics, a one-dimensional, non-homogeneous, equilibrium two-phase flow and heat transfer model with provision for subcooled boiling in the coolant channels, and a spatially averaged, one-dimensional heat conduction model for the fuel plates. In addition, the feedback from core regions other than the fuel elements is included by employing a lumped parameter approach with a one node representation of the metal and a two-node representation of the coolant in these non-fueled regions. The safety system response is incorporated in the model by approximating the movement of the safety quadrants with a second order differential equation. This basic model is used to describe the transient behavior for both nominal and hot-spot conditions of the inner and outer fuel elements, both at the beginning and at the end of the fuel cycle.

A direct numerical solution of the multiple point difference approximation to the equations representing the core dynamics model have been used in an implicit fashion. In this scheme, the partial differential equations of the model are discretized in space and then combined with the rest of the dynamic equation set. This leads to a large initial value problem of ordinary differential equations. A variable-order, variable-time-step time-advancement-scheme based on the forward and backward Euler method is used to solve this initial value problem.

The model is used to analyze a series of HFIR reactivity transients associated with malfunction of the reactor control system, formation of voids in the regions with positive reactivity coefficients, and pump start in a cold loop. Each transient is studied for four operating states of the reactor which are characterized by distinct initial conditions and safety system settings. The results of this analysis indicate that, in addition to the unprotected sequence for uncontrolled shim withdrawals, the worst credible reactivity accident for the HFIR is due to optimum amount of voids in the target region. For this case, a partial hot-plate melting is predicted in Mode 1, at full power, and at the end of cycle conditions.

The analysis is supplemented with a study for the consequences of local hot-plate melting by examining the initial disruption modes of the core at the onset of melting. Scoping calculations are performed to assess the potential for molten fuel entrainment at the expected hot-channel conditions. The potential consequences of partial flow blockages are also addressed by examining the dynamics of molten fuel accumulation as it moves into the subcooled water at the channel outlet and then cools. It is concluded that the fuel melting will be limited to the hot-plate and can be tolerated without challenging the overall mechanical integrity of the rest of the core. In summary, the HFIR can withstand the maximum credible reactivity accident with only a small amount of fuel melting.

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1. INTRODUCTION

1.1. BACKGROUND AND PURPOSE

The Oak Ridge National Laboratory's High Flux Isotope Reactor (HFIR) is a high-performance flux-trap type research reactor.¹ It is primarily designed for the production of trans-uranium isotopes, neutron scattering, and material irradiation research. After the shutdown of the HFIR in November, 1986 due to embrittlement concerns for its pressure vessel, some of the design parameters were changed in order to ensure the safe operation of the reactor with the existing pressure vessel. Primarily, the steady-state power of the reactor for normal operation has been lowered from 100 MW to 85 MW to reduce the neutron damage, and the operating pressure has been reduced from 750 psi to 468 psi at the vessel inlet. In parallel with these changes, a whole series of design modifications was required, and the safety limits and limiting-safety-system settings were redefined.

In order to document these changes and their effects on reactor operation, the HFIR Safety Analysis Report (SAR) is currently being updated as part of long term program to improve the safety of the reactor. In parallel with these efforts, the dynamic response of the HFIR to reactivity induced accidents has been previously analyzed using the HFIR core kinetics code, REACC.^{2,3} The results of this preliminary analysis indicated that the worst credible reactivity accident for the HFIR would be due to the insertion of optimum amount of void to the target region. For **Mode 1**, at full power and at the end of fuel cycle, a local melting at the hot-spot is predicted for this accident. Since the design requirements and transient acceptance criteria require that nominal fuel structural integrity and a coolable geometry should be maintained for the limiting faults, a further analysis of this sequence is needed to examine the potential for propagation of hot-plate melting.

REACC is a single-phase, lumped-parameter model that was developed primarily for the preliminary analysis of reactivity transients for the new operating conditions. The REACC model is based on a one node representation of an average fuel plate, and a two node representation of an average coolant channel. Since the thermal-hydraulic model in REACC is limited to single-phase flow, the transients that involve boiling are not adequately

analyzed. As an approximation, when the incipient boiling heat flux corresponding to the steady-state operation is exceeded, the heat transfer coefficient is reset to zero until the power level drops to a point where rewet of the plates is assumed to occur. This approach leads to extremely conservative results and includes several unverified assumptions, such as the rewet criteria. Furthermore, several key dynamic parameters such as pressure, coolant velocity, density, heat transfer coefficient, and incipient boiling heat flux are treated as constants in REACC which inhibits the proper analysis of large reactivity insertions in which significant changes in these variables could occur. In addition, since it is based on a lumped parameter model, the extent of hot-plate melting and propagation of the local heatup to the rest of the core cannot be predicted accurately with REACC.

Therefore, the need exists for an improved computer model that is valid for the highly specialized HFIR geometry and operating conditions. There is also a need to establish a technical basis for understanding the initiators which lead to fuel melting, the phenomenon of fuel melting, and its consequences in order to reevaluate core-damage criteria and to assure that the HFIR core is designed to avoid or mitigate such accidents to the greatest extent possible.

Consideration of fuel element failure propagation is part of the overall safety approach for the HFIR. As boiling starts in the hot-channel and the fuel approaches the melting temperature, the structural integrity of the fuel elements can be lost through a number of disruption modes. Fuel plates are subject to thermal and mechanical stresses due to initial coolant and core heatup, coolant hydrodynamic forces (particularly those that occur with high steam velocities after the initiation of boiling), internal fission gas pressure as the fission gas bubbles coalesce and grow with the change of phase, and the possibility of aluminum-fuel and aluminum-water chemical reactions. Once the fuel damage begins at the hot-spot, melting and deformation of the fuel can cause further reactivity additions due to core reconfiguration and can cause flow blockages in narrow coolant channels. Thus, a detailed core damage evaluation is necessary to investigate the possibilities for fuel damage propagation due to postulated partial or full flow blockages.

The purpose of this work is to develop an improved distributed parameter computer

model for the analysis of the rapid reactivity transients of the HFIR which would incorporate neutronics, two-phase thermal-hydraulics and structural behavior of the core, and provide a perspective on the issue of initial fuel damage propagation. The envelope of reactivity excursions considered as accident initiators is based on the existing information in the HFIR Accident Analysis Report⁴ as well as ongoing work on the safety analysis report.

Even though the model is developed specifically for HFIR core, it can be adapted with some modifications to other U-Al dispersion fueled core geometries and used for a large spectrum of safety analysis work for other existing high flux research reactors. Among these are BNL's *High Flux Beam Research Reactor*, INEL's *Advanced Test Reactor*, and SRS's *K- Reactor*. In addition, this model can be utilized as a tool for the design of the *Advanced Neutron Source Reactor* and the heavy water version of the *New Production Reactor*.

1.2. DESCRIPTION of the MODEL AND ORIGINALITY of THIS WORK

The mechanics and thermal hydraulics of the rapid reactivity accidents for research reactors with aluminum-based fuels are quite different from those encountered in power reactors and relatively little has been done to develop an analytical basis for fuel element failure propagation. The existing computer codes are, in general, developed for commercial power reactors with geometries and operating conditions significantly different from those of HFIR and have limited applicability for research reactors. A review of the major computer codes for transient reactor analysis is provided in Appendix A. For the type of transients of interest in this work, it would be necessary to modify these codes to a great extent for flow in narrow rectangular channels with high coolant velocities and high heat fluxes, and for the highly subcooled core conditions of the HFIR.

Furthermore, most currently used numerical schemes are designed primarily for the analysis of milder system transients with relatively longer periods. During HFIR transients, however, the time to boiling is of the order of milliseconds which leads to significant pressurization of the coolant channels, and poor heat transfer for the voided regions leads to the prediction of the hot-spot melting in less than 200 msec.

In this work, an improved dynamic model of the HFIR core has been developed that incorporates neutronics, two-phase thermal-hydraulics and structural behavior of the HFIR core for the analysis of these rapid and large reactivity insertions exceeding protective system response. This new model also provides an initial perspective on the issue of fuel damage propagation. Such an integral approach for the HFIR core in particular, and for aluminum dispersion fueled research reactors in general, is new and introduces several challenges. The lack of phenomenological models related to core melting has required a multistep approach developing neutronics, thermal-hydraulics, and structural models first, and combining them in a consistent manner to provide an integral core dynamics model after gaining sufficient insight to the phenomena.

The model developed includes point kinetics for the neutronics, a simplified two-fluid, two-phase fluid-dynamics, and one-dimensional heat conduction through the fuel plates. The point kinetics approximation is expected to be sufficient for the small HFIR core with high power density and a smooth flux shape which is not expected to distort significantly during the transients.

Various models for non-equilibrium two-phase flow have been surveyed and a novel simplified model is proposed for this analysis. A review of the two-phase models surveyed can be found in Appendix B. The basic field equations of the fluid dynamics model used in this work can be characterized as a non-homogeneous, equilibrium two-phase flow model (with provision for subcooled boiling) based on mass and energy split between the phases. This customized representation of the fluid dynamics, which is expected to describe the flow and heat transfer conditions in the HFIR coolant channels quite well, has not been borrowed from any other model and, to the best of author's knowledge, has not been published anywhere else.

The difficulty in solving the general transient fluid dynamics equations arises from the strong coupling between the conservation equations. To simplify the solution schemes, often different levels of approximations are introduced to the field equations to decouple the momentum and energy equations. In this work, a direct numerical solution of the multiple point difference equation approximation to the conservation equations representing fluid

dynamics has been used in an implicit fashion. Contrary to common approximations used in most computer models such as momentum integral, single mass velocity, and channel integral methods,^{5,6} this approach is the most direct and detailed representation of channel behavior.

One dimensional, time-dependent, spatially averaged heat conduction through the fuel plates and oxide layer at each axial location is coupled to the fluid dynamics model via the heat flux at the fuel element outer surface. The flow and heat transfer regimes considered are the single-phase liquid flow, subcooled boiling, bulk boiling, film boiling, and single-phase vapor flow. In addition, a separate mode of heat transfer is provided for the single-phase liquid flow regime when the fuel surface temperature is higher than the liquid saturation temperature. Appropriate correlations are described for friction factor, heat transfer coefficient, void fraction, incipient boiling and critical heat fluxes in each boiling regime. As a significant improvement over the previous analysis tool, REACC, this new model permits the determination of the survivability of the core through various transients based on the critical heat flux criteria rather than the more conservative incipient boiling criteria.

The base model is first used to describe a nominal fuel-assembly which consists of one nominal fuel plate and one nominal coolant channel for both the inner and outer fuel elements that are connected at the inner and outer plena. Nominal conditions are defined by a core operating with a nominal power density, at an average heat flux, with manufacturing tolerances of zero, and with no uncertainties in the operational characteristics.

The nominal conditions are then modified, considering such factors as oxide film build-up on the fuel plate surfaces, fuel plate deflections induced by differential pressures and temperatures, fuel plate thermal expansion, radiation swelling, coolant flow mal-distribution within the fuel elements, fuel segregation, and fuel-cladding non-bonds. The uncertainties in heat transfer and burnout characteristics, plant operating conditions, and power density distributions throughout the fuel cycle are also considered. The simultaneous use of all these factors describes the hot-channel and hot-plate conditions in a hot-assembly for either the inner or the outer fuel elements.

The dynamic behavior of the reactor core is conservatively assumed to be dependent

only on the nominal conditions and the nuclear feedback from the hot-assembly is neglected. In addition, the feedback from regions other than fuel elements are included by modeling these regions using a lumped-parameter approach with a one node representation of the metal and a two node representation of the coolant. Reactivity changes due to safety system action are approximated by a second order differential equation which describes the movement of the spring loaded scram quadrants.

A unique finite difference scheme is used to solve the equations representing the HFIR core dynamics model. Spatial semi-discretization of the partial differential equations describing the nominal and hot-assembly fluid dynamics leads to a large initial value problem of ordinary differential equations. The resulting nonlinear ordinary equation set is then coupled with the neutronics and heat conduction equations, and solved simultaneously in the time domain using an efficient variable-order, variable-time-step time-advancement scheme based on the forward and backward Euler method (FEBE).⁷

To be effective, the time advancement scheme must be able to cope with severe stiffness originating from the real and complex eigenvalues of the Jacobean matrix. Most widely used stiff-solver algorithms have a narrow stability margin due to the size of the problem. The time advancement scheme used in this work tackles this problem by using a generally lower-order algorithm whose accuracy is offset by the use of an extrapolation technique in addition to classical implicit iterations. Compared to solution schemes commonly described in the literature and used in major existing computer models, this fully implicit treatment is more rigorous and avoids numerical instabilities typically encountered due to explicit or semi-implicit coupling between the thermal, hydraulic, neutronic, and structural models. However, it requires a significant amount of computer time.

In addition to reactivity transients, the model developed in this study may be used as a tool to support the analysis of many other transients of safety significance as well as transients expected during normal operation. In particular, it may be used for the analysis of transients initiated by perturbations in flow rate, inlet temperature, pressure (as is the case for loss of pumping), partial loss of heat sink, and core flow blockage accidents by defining an appropriate boundary and initial condition set for each case.

In summary, the original contributions of this dissertation can be itemized as follows:

1. A computer model which incorporates the neutronic and two-phase thermal-hydraulic behavior of uranium-aluminum fueled high-flux research reactor cores is developed and applied for analysis of rapid reactivity transients of the HFIR.
2. A unique non-homogeneous, equilibrium two-phase flow and heat transfer model with a provision for subcooled boiling is developed and incorporated to the model.
3. A novel finite difference scheme based on semi-discretization of the partial differential equations representing the fluid dynamics model is used and the combined equation set is solved in the time domain in a fully implicit fashion.
4. The structural behavior of the fuel at the onset of fuel melting is also modeled and coupled with the neutronics and thermal-hydraulics models. Such an integral approach for a plate-type U-Al dispersion fueled research reactor is new and requires simultaneous consideration of several factors such as thermal and mechanical stresses, fuel swelling potential and kinetics, and coolant hydrodynamic forces.

1.3. OVERVIEW

A brief descriptions of the HFIR reactor core, safety system, and modes of operation related to the reactivity transient analysis are provided in Chapter 2. The HFIR design parameters, nuclear, thermal-hydraulic and operational characteristics supporting the descriptions provided in this chapter are presented in several tables in Appendix C.

The mathematical models that describe the dynamic response of the HFIR during reactivity transients, and the flow and heat transfer conditions that would occur in the HFIR coolant channels are discussed in Chapter 3. A discussion of the point kinetics approximation and feedback mechanism is provided in Section 3.1. The fluid dynamics model is presented in Section 3.2. Flow and heat transfer regimes are discussed separately providing the necessary field equations for each regime. The appropriate correlations are defined for the

estimation of parameters of interest such as the friction factor, heat transfer coefficient, critical and incipient boiling heat fluxes, void fraction, and slip ratio in terms of local and instantaneous values of state variables, physical properties of the coolant, and heat flux. Finally, the heat conduction model is presented in Section 3.3. The derivation of average fuel and cladding temperatures, boundary and interface conditions, and surface temperatures are discussed for two separate heat conduction models.

The physical modeling of the HFIR coolant channels and fuel plates is presented in Chapter 4. As indicated earlier, the HFIR transients are analyzed for both "nominal" and "hot" conditions. First, the nominal operating conditions are described for an average coolant channel and average fuel plate in Section 4.1. The uncertainties in the model and operating conditions are considered in hot-assembly modeling as presented in Section 4.2. The feedback from other regions is described in Section 4.3. Reactivity changes due to safety system action are discussed in Section 4.4.

Chapter 5 presents the method of solution for the equation set describing the HFIR dynamic model. Discretization of the partial differential equations and characteristics of the combined equation set are discussed in Section 5.1. The fundamental aspects of the time advancement scheme are described in Section 5.2.

The results of this study are presented in three chapters. First the results of model verifications are presented in Chapter 6. Partial comparisons of the fluid dynamics and heat transfer variables of the new model are provided by comparing results against a RELAP5/Mod2⁸ model for three simplified cases. To improve readability, only a few figures for key state variables are displayed in this section for the comparisons with the RELAP code. The remaining comparison plots are presented in Appendix D. The neutronics modeling of the core and the feedback mechanisms are verified using an earlier HFIR core model, REACC.²³ Finally, the overall neutronics and thermal-hydraulic behavior is compared with experimental SPERT-II data.⁹

Secondly, the results of the analysis for HFIR reactivity accidents are presented in Chapter 7. These are the accidents associated with the control system, accidents associated

with the target voids, and cold water accidents. Each category is analyzed for both the inner and outer assembly separately, both at the beginning and at the end of fuel cycle conditions. At the end of this chapter, the worst credible reactivity accident is identified as the bounding case and it is used as the basis for the subsequent parametric studies on the uncertainties in the initial conditions. All transients analyzed in Chapter 7 are followed up to the point of fuel damage and classified as "damaging" or "non-damaging" based on the results for average plate temperatures.

After identifying the accident initiators which lead to HFIR fuel damage, the phenomena of fuel melting and its consequences are studied in Chapter 8. The conditions at the onset of fuel melting, likely initial disruption modes and their effects on the transient being analyzed are investigated for the worst credible accident of the HFIR. The questions concerning whether or not the hot-plate melting is self-limiting and overall core coolability are addressed in this chapter. Finally, the conclusions of this work as well as the recommendations for some model improvements are presented in Chapter 9.

Two literature reviews for existing transient analysis codes and two-phase flow models are provided in Appendices A and B, respectively. The general characteristics of the HFIR core and several design parameters are summarized in Appendix C. The tables in this appendix primarily support the brief description of the HFIR provided in Chapter 2. Similarly, additional plots which support the comparisons of the model with the RELAP code are presented separately in Appendix D. The initial steady-state conditions of state variables in coolant channels and fuel plates are presented in Appendix E. Finally, a complete set of transient results for all state variables during the Mode-1 optimum void accident are presented in Appendix F for both beginning and end of cycle conditions.^a

^a A complete listing of the several computer modules developed in this work and associated subroutines may be obtained directly from the author. Please forward any request to Tanju Sofu, Argonne National Laboratory, Reactor Analysis Division, Argonne, Illinois 60439.

2. HIGH FLUX ISOTOPE REACTOR and REACTIVITY TRANSIENTS

2.1. DESCRIPTION of the HFIR

Some of the important features of Oak Ridge National Laboratory's High Flux Isotope Reactor (HFIR) that are pertinent for transient studies are summarized in this section. A detailed description of the HFIR systems may be found in several references.^{1,4}

2.1.1. Reactor Core

The HFIR is characterized by a very high power density in a small core volume and a maximized heat transfer surface area to core volume ratio. In pursuit of the high power density without large power generation, the HFIR has been designed with a compact core containing fully enriched uranium which is burned up rapidly. The core consists of four concentric regions, as shown in Figure 1. The four regions include an island region at the center in which target rods are placed, the fuel elements, the control elements, and the beryllium reflector regions.

The target contains Pu^{242} and other trans-uranium isotopes and is positioned at the center of the core. The fueled region consists of two annular regions of fuel plates (inner and outer fuel elements) that are held between concentric aluminum cylinders called side plates. The fuel plates are curved in the shape of an involute (as shown in Figure 2) to maintain a constant hydraulic head in each coolant channel, and they fill the two core annuli completely. The plates are of a sandwich type consisting of a U_3O_8 -Al cermet with type 6061 aluminum cladding. Fuel loading in the plates is graded in the radial direction to minimize power peaking, and the inner plates contain boron in the form of B_4C as a burnable poison. The control elements are located between the fuel and reflector regions.

The reactor core is contained in a 7.9 ft diameter pressure vessel which is located in an 18 ft diameter cylindrical pool. The top of the pressure vessel is 17 ft below the water

HFIR CORE

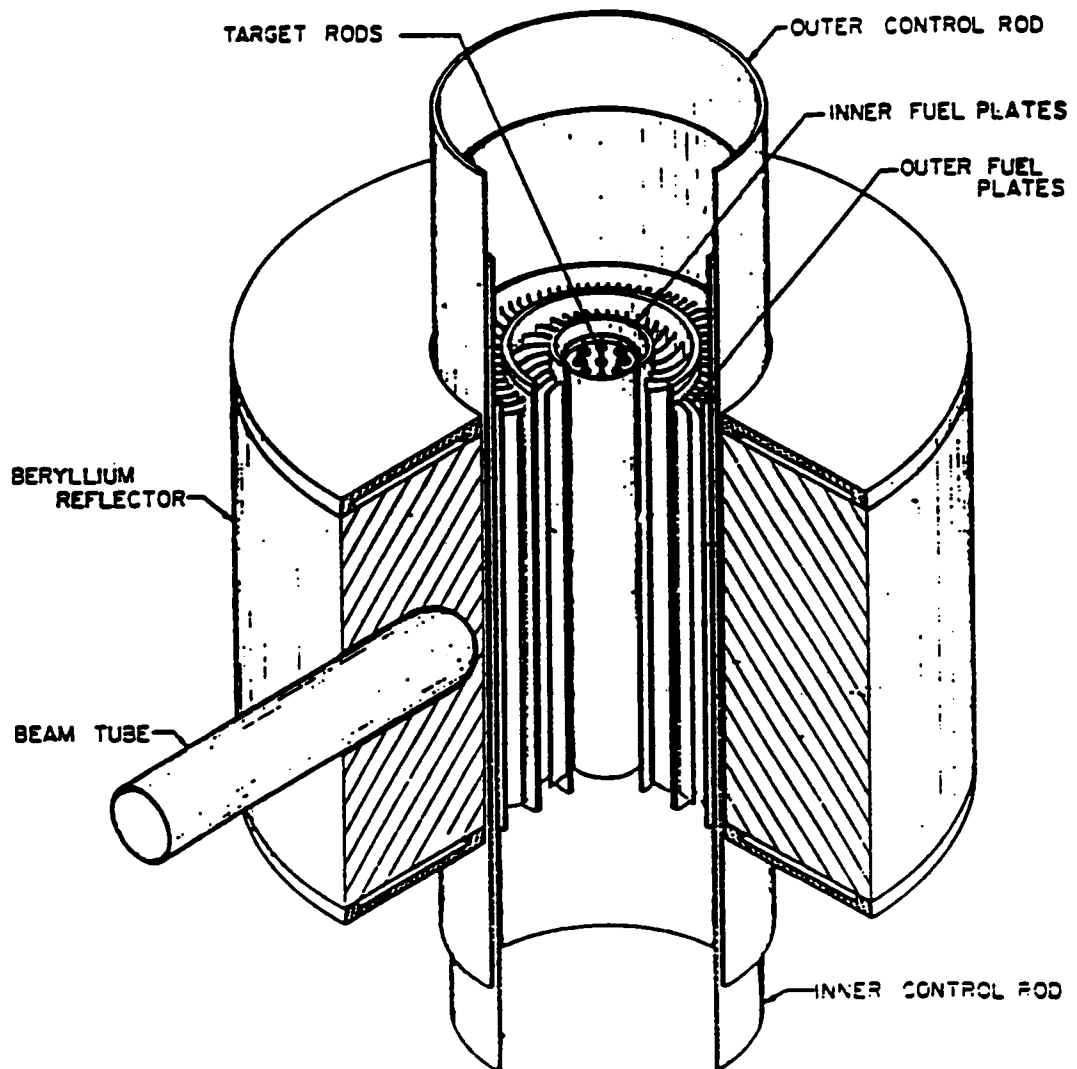


Figure 1 : Schematic representation of the HFIR core.

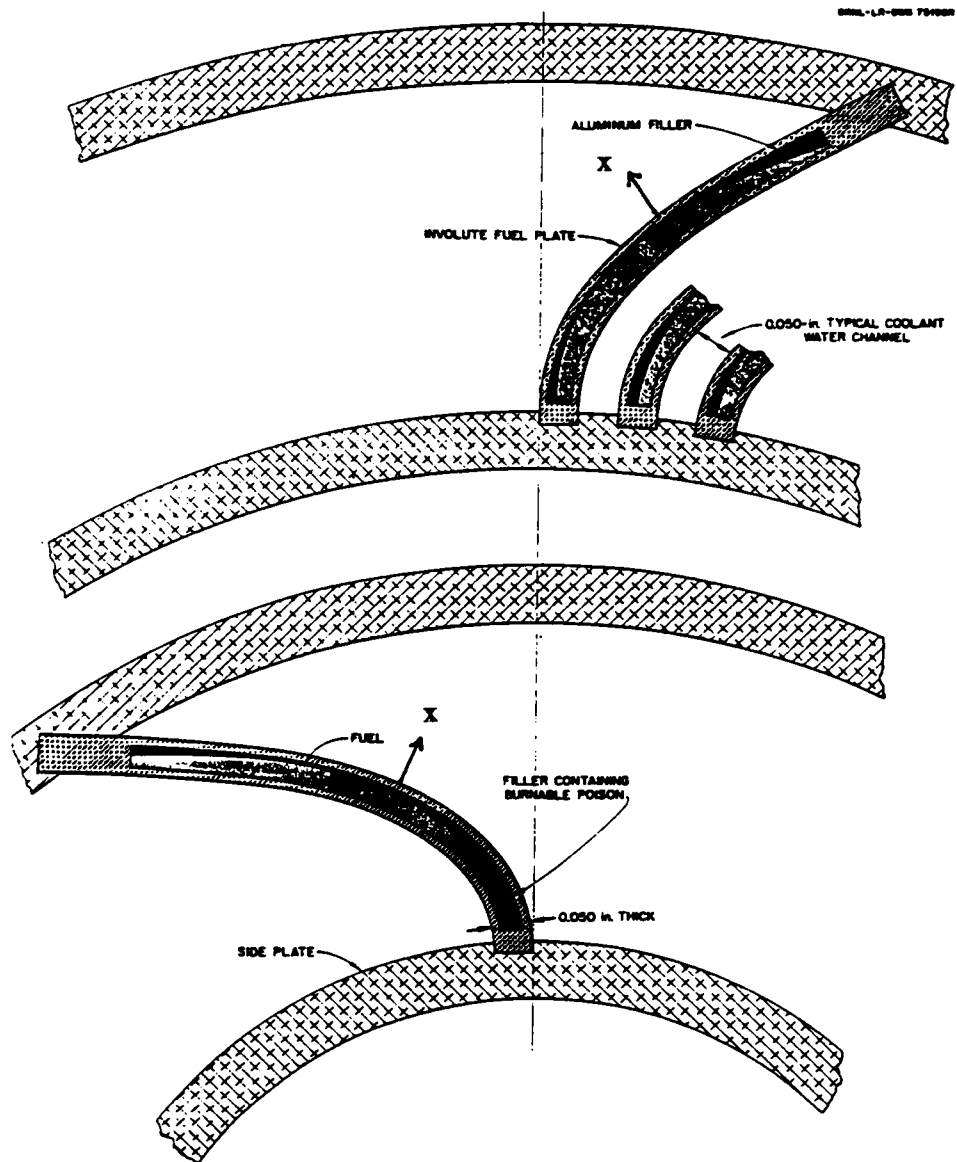


Figure 2 : Schematic cross section of the HFIR fuel assembly.

level. The HFIR design consists of a cold, solid, and moderately pressurized primary coolant system. Pressure is maintained by an off-line pressurizer pump and letdown control valves. Primary coolant flow is provided by large AC-powered pumps. During depressurized conditions, these pumps are driven by small DC-powered pony motors which can supply sufficient forced convection after reactor is shut down. The primary coolant enters the vessel through two pipes above the core, passes downward through the core, and exits the vessel through a single pipe beneath the core. The flow rate is about 16,000 gpm, of which about 13,000 gpm flows through the fuel region.

The reactor was originally designed to have a maximum steady-state operating power of 100 MW giving a peak unperturbed thermal neutron flux of 5×10^{15} n/cm²-sec. The designed maximum power density is 4.3 MW/l and peak heat flux is 2×10^6 Btu/hr-ft². However, as mentioned in Chapter 1, concerns about embrittlement of the pressure vessel restrict the current operation to 85 MW.

Major characteristics of the HFIR core are summarized in Tables C1 to C12 in Appendix C. In these tables, "*original design*" values refer to those before the shutdown of the reactor, and the "*current conditions*" refer to current operating parameters which are modified to ensure the safe operation of the reactor with the existing pressure vessel. Also, in these tables, the abbreviations "BOC" and "EOC" refer to "*beginning of fuel cycle*" and "*end of fuel cycle*" conditions, respectively.

2.1.2. Safety System

The HFIR safety system is designed to monitor key nuclear and process parameters and to automatically initiate reactor shut down to assure no fuel damage. This design meets the general requirements of commercial nuclear power reactors in the area of reactor protection systems.^b Seven scram conditions provide safe shut down in all modes of operation. These conditions and corresponding nominal values are:

^b 10 CFR 50, Appendix A, Criterion 20.

- i. Percent power to percent flow ratio (> 1.25)
- ii. Heat power (> 102 . MW)
- iii. Inlet coolant temperature (> 130 °F)
- iv. Coolant pressure (< 368 psig)
- v. Coolant Flow (< 1900 gpm)
- vi. Rate of power rise (> 20 %/sec)
- vii. Failed fuel detector counts ($> 120\%$ of normal background)

Three independent channels of each nuclear and process instrumentation (for flow, temperature, power, and neutron flux) are used for the HFIR control and safety systems, with the concurrence of two out of the three safety channels required to initiate a scram.

The reactivity is controlled by a system through the action of electric motors on five control element drives.¹⁰ The control elements are in the form of two concentric cylinders which are located between the outer fuel elements and reflector regions of the core as shown in Figure 3. This configuration has been chosen in order to minimize the perturbation of the power distribution caused by the withdrawal of the control elements as the burn-up of the core occurs. Both cylinders contain a 22 inch long *black* region with high neutron absorption characteristics ($\text{Eu}_2\text{O}_3\text{-Ta-Al}$), a 5 inch long *gray* region (Ta-Al), and a 22 inch long *white* region with very little neutron absorption characteristics (Al).

The inner control element, which is a complete cylinder, is moved upward into the reactor core to decrease reactivity. It has no rapid shutdown feature and is used for both shim and regulation. Since the gravity and the water flow tend to push it from the core, in case of a scram, all movements of this cylinder are stopped by breaking the electrical drive motor circuit (in order to limit the reactivity addition associated with a runaway control element accident).

The outer control cylinder is divided into four quadrants that are individually spring loaded and released, and moved downward into the reactor to decrease reactivity. These quadrants are used for both shim control and safety. The safety action results in fast scram by interrupting the power to the safety quadrant magnets. The release of the magnets which

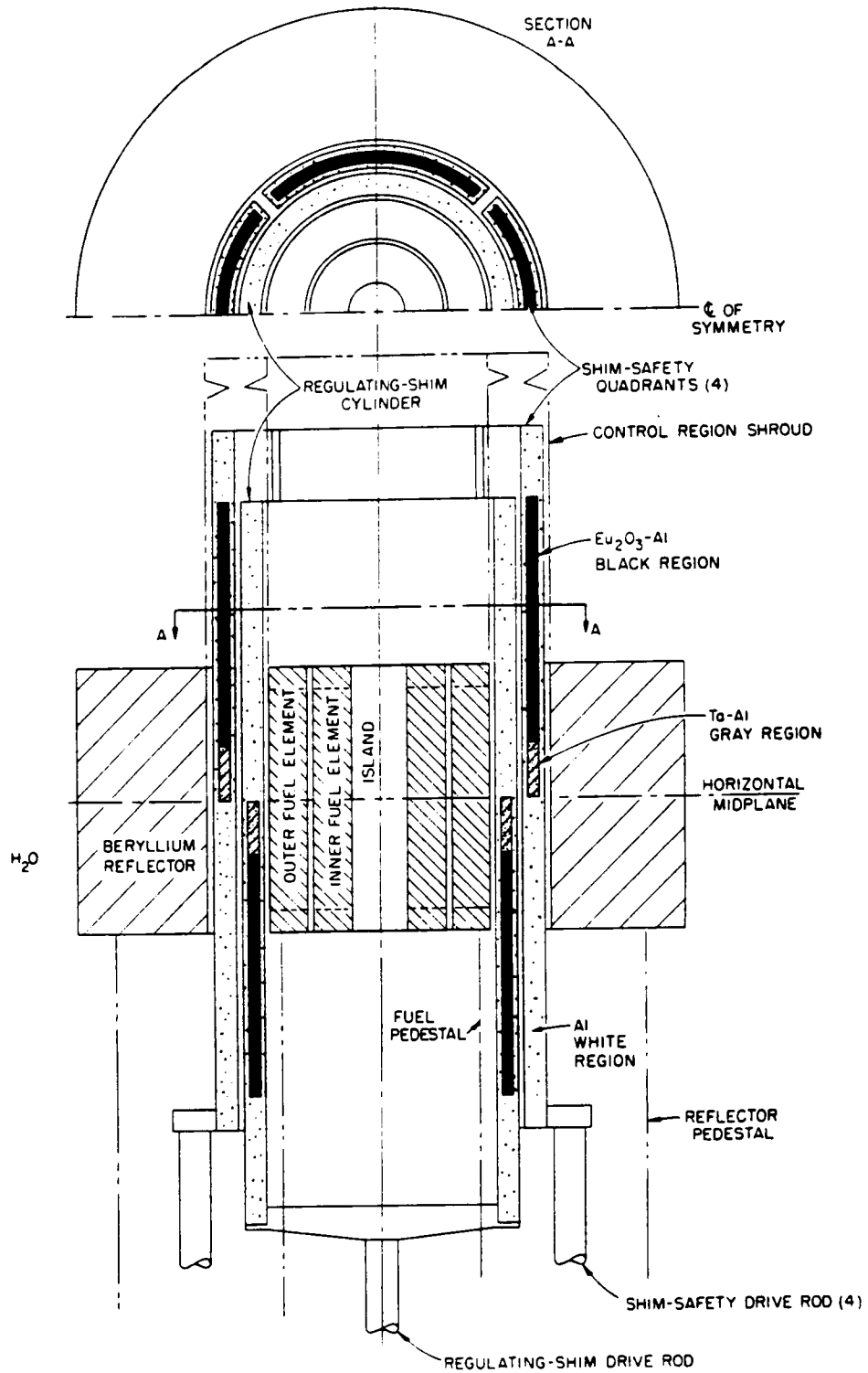


Figure 3 : Schematic representation of the HFIR safety system.

hold the scram latches in position results in a very fast insertion of the quadrants under the acceleration of springs as well as gravity and hydraulic forces. Although the reliability associated with the rapid insertion is very high, in this analysis only three of four safety quadrants are considered for scram for certain transients.

Normally, both control cylinders are operated symmetrically in order to maintain the symmetry of the power and the poison distribution in the vertical and azimuthal directions.

2.1.3. Modes of Operation

There are three modes of operation of the reactor: **Mode 1**, which is the normal sequence for the routine start-up and operation at full power; **Mode 2**, which permits operation up to 2.5 MW without pressurization but with shutdown coolant flow; and **Mode 3**, which permits operation up to 100 kW with neither pressurization nor coolant flow. These three modes are provided in order to facilitate various tests such as power and flux distribution investigations, as well as normal full power operation.

Mode 1 is the normal operating sequence which is used for start-up and operation at full power. Two conditions are provided, "*start-up*" and "*run*." The "*start-up*" must proceed automatically since no provisions are made for the manual start-up. The increase in power during start-up is terminated by the servo insertion of the regulating rod when the reactor achieves approximately 10% of steady-state full power. At this power, the regulating rod will be near its insertion limit. This is desired because it provides the maximum allowable positive reactivity available to the servo for the use in overriding Xenon poisoning. When this phase is completed, the reactor remains under the regulating system control until the operator manually requests a change ("*run*" condition). The operator may then raise the reactor power by increasing the setting of the servo demand. No increase can occur unless the control cylinders are symmetrically arranged. For this mode, the level trip is proportional to the coolant flow rate.

Mode 2 provides for low-power operation at 10% shutdown coolant flow and low pressure while the vessel head is open. The reactor power level is limited to 2.5 MW

consistent with the available coolant flow and low pressure. The control system remains in the "start" condition, but the automatic group withdrawal of the shims is blocked so that manual operation is necessary. **Mode 3** permits very low power operation with no forced flow and low pressure (vessel head open). For this case, the maximum power level is limited with 100 kW.

2.2. HFIR TRANSIENTS

Although a range of hypothetical accidents is known to cause HFIR fuel element failure, only an envelope of reactivity excursions are considered in this study as accident initiators. The envelope of reactivity transients is based on existing information as documented in the "*HFIR Accident Analysis Report*",⁴ as well as ongoing work for the "*HFIR Safety Analysis Report (SAR)*" which is currently being written to properly document the many changes made after the shutdown and their effect on the reactor.¹¹ The SAR is expected to be published later this year.

Other types of accident initiators that may lead to fuel melting, such as loss of coolant accidents (LOCA) and station black-outs, are omitted from the scope of this dissertation primarily due to the out-of-vessel characteristics of these transients. Also, the logical development of the accident code methodology for reactivity transients is uniquely different. LOCA and station blackout transients typically occur at decay heat power levels; therefore, the pre-melt coolant response is usually independent of reactivity feedback. Since these transients lead to fuel melting at low power levels, transient pressurization effects can often be ignored. Also, the period of these transients is considerably longer.

For reactivity insertion accidents, on the other hand, reactivity feedback from coolant and fuel temperature changes have a substantial effect on the power from the very early stages of the transients. In addition to steam void reactivity feedback for transients with boiling, fuel plate thermal expansion is a significant factor in terminating the resulting power excursions. Therefore, the fuel element structural behavior needs to be properly modeled. The time to boiling is typically on the order of milliseconds which leads to local pressure fluctuations in the coolant channels and possibly flow reversals. The high surface heat flux

eventually causes voiding of the entire channel, and poor heat transfer for the voided regions can lead to hot-spot melting in less than a second.

The model developed in this study may be used for the analysis of many transients of safety significance as well as transients expected during normal operation. In particular, it is useful for control rod drop/ejection accidents, cold water accidents, and the analysis of other reactivity anomalies. Although not considered in this dissertation, appropriate applications of the model could also include the analysis of transients initiated by perturbations in flow rate, inlet temperature, and pressure as is the case for loss of pumping, partial loss of heat sink, and core flow blockage accidents.

Various reactivity parameters for the HFIR core are provided in Table C13-C16 in Appendix C. Although there are several ways of adding reactivity to the HFIR core, only those accidents that are feasible from the standpoint of the initiating mechanisms are considered in this study. The accident sequences are identified in probabilistic risk assessment (PRA) work^{12,13} which is performed to help define the initiators to be covered in the updated safety analysis report (SAR). Four general types of reactivity accidents are considered in this work; namely, accidents associated with

- i. the control and safety system,
- ii. voids in the target region,
- iii. experimental facilities, and
- iv. cold water accidents.

Basically two types of control system accidents are considered: Uncontrolled control element withdrawals, and mechanical failure of control element drive rods.¹⁴ In the first case, all five control elements (inner shim-regulating cylinder, and outer shim-safety quadrants) are driven out of the reactor at the maximum speed of the shim drives, or the shim-regulating cylinder is driven out with a faster regulating drive speed. For the "*end of fuel cycle (EOC)*" conditions, the maximum reactivity addition due to runaway control elements would be much smaller simply because, by definition, control elements are assumed fully withdrawn at the end of core life.

As a second type of control element accident, the mechanical failure of the control element drive mechanisms for the inner regulating cylinder is considered. In this event, a hydraulic cylinder which is normally used to balance the downward forces due to gravity and coolant flow is expected to limit the reactivity addition rate. In another case of interest, the attachment between a safety quadrant and its drive rod is assumed to break, resulting in dropping the safety quadrant out of the core until the guide bearings hit the bottom of their track.

Variations in the target region moderator density can also add reactivity. The temperature and void reactivity coefficients of the target region are positive for the initial changes in density since the flux trap is over-moderated.¹⁵ As the water density decreases, the decrease in neutron absorption is more effective than the decrease in moderation. Therefore, voids in this area will add large amounts of reactivity to the core. Early reactivity accident studies report that, with a simulated 300 g Pu target, the maximum Δk associated with the island voids is 0.016 with a corresponding void fraction of 42%.¹⁵ For larger void fractions the island becomes under-moderated.

Three sources for formation of voids in the flux trap has been suggested in previous work.¹⁴ These are the formation of a vortex, the sweeping in of air with the coolant flow, and flow blockage with subsequent steam formation. Of these, the first two cases seem to be more serious because the associated reactivity insertion rates are faster. However, they are less likely as suggested by PRA findings.^{11,12}

Both the moderator and the fuel temperature coefficients of reactivity for the HFIR fuel assembly are negative; therefore, the worst cold-water accident would occur if cold water enters only in the fuel region when the coolant in the core is hot. In **Mode 3** operation with no forced flow, it is possible that the coolant in the heat exchangers would be at lower temperatures than that in the reactor vessel. If the primary coolant pumps were suddenly turned on (contrary to normal operating procedures), the cold water would be injected into the core. As an extreme case in which the cold water enters into the **fuel region only**, a substantial amount of reactivity would be inserted to the core. In **Mode 3**, the minimum required time for insertion of that amount would be minimum 0.030 seconds assuming that

all three primary coolant pumps are turned on simultaneously.¹⁴ During **Mode 1** and **Mode 2** operation, however, cold water accidents would be less serious due to low temperature differences of already circulating coolant.

The fourth class of reactivity accidents are associated with the experiments. The reactivity contribution of the typical Pu^{249} target was estimated to be no more than 0.01 dk/k.¹⁴ Therefore, as a worst case, the reactivity insertion associated with experiments would be 0.01 dk/k if the target were to fall completely out of the core with the maximum flow rate through the island. Since this insertion rate is not any more severe than those associated with voids in the target region, there is no need to study target related reactivities separately. The other experimental facilities in the HFIR are located sufficiently far from the core so that they are not likely to cause any detectable change in reactivity as reported in early HFIR criticality experiments.¹⁶ In brief, transients covered in this category are not considered in this work.

Results for each individual initiating event as identified above are presented in Section 6.2. In addition to these initiating mechanisms and associated reactivity insertion rates, there are some other parameters which affect the severity of the transients to be analyzed. These parameters, such as the time in fuel cycle, the initial position and reactivity worth of the safety plates, the initial steady-state power, and the coolant velocity should also be taken into consideration. Each particular transient is studied for both the beginning (BOC) and the end of cycle (EOC) conditions.

3. MATHEMATICAL MODELING

In this chapter, the mathematical models describing the dynamic response of the HFIR during reactivity transients, as well as the flow and heat transfer conditions which would occur in HFIR coolant channels are discussed. In order to establish the performance of the fuel assembly under transient conditions, mathematical models are developed in which the neutronics, thermal, and hydraulic behavior are considered in a consistent manner to provide an integrated core dynamics model. Appropriate correlations are used for the estimation of parameters of interest (such as the critical heat flux and the heat transfer coefficient) in terms of state variables and the physical properties of the coolant.

The point kinetics equations are used to model the neutronics behavior. A one-dimensional, non-homogeneous, equilibrium two-phase model with subcooled boiling is used for thermal-hydraulics in which the equations of mass, momentum and energy conservation are solved to give axial variations in fluid enthalpy and mass velocity. The fuel plates are modeled as a series of azimuthal nodes at a number of axial levels in which heat transfer is treated by a one-dimensional (in a direction perpendicular to the flow) heat conduction equation.

Once the local enthalpy and the local mass velocity at each node are established, suitable correlations are used to define the heat transfer regime. Basically, this involves a comparison of the local heat flux through the fuel plate surface at any time with the critical heat flux for these same fluid dynamic conditions. If the heat flux exceeds the critical heat flux, then overheating of the fuel plate occurs. The extent of this overheating is established using the appropriate correlations for the heat transfer coefficient.

3.1. NEUTRONICS MODEL

The neutronics model is developed to compute the power behavior of the reactor using a space-independent point kinetics approximation. This approximation is adequate for the cases in which the spatial distribution of the neutron flux remains relatively unchanged

throughout the transient.

3.1.1. Derivation of Neutronic Equations

A number of methods have been developed over the years for solution of the time-dependent neutron diffusion equation describing spatial neutron kinetics:¹⁷

$$[-M + F_p]\Phi(r, E, t') + S_d = \frac{1}{v} \frac{\partial}{\partial t} \Phi(r, E, t) \quad (1)$$

where M is an operator to account for removal and scattering, and F_p is the prompt fission operator. The delayed neutron source term, S_d , is a convolution integral over the flux history. The methods for the solution of this equation range from direct calculation of the full energy-space-time dependent diffusion equation to point kinetics equations with appropriate evaluation of nuclear parameters and reactivity.¹⁸ The various methods may be classified into two groups: direct integration methods and factorization methods.

The point kinetics approximation falls into the factorization methods category along with two other approximations; quasi-static and adiabatic methods. In these methods, the total neutron flux $\Phi(r, E, t)$ is factorized into an "amplitude" function, $\phi(t)$, and a "shape" function, $\psi(r, E, t)$, in such a way that the time dependence of the shape function accounts only for the relatively slow space-energy variations. This factorization leads to point kinetics equations in terms of amplitude function and an equation for the shape function.¹⁹

$$\frac{d\phi(t)}{dt} = \frac{[\rho(t) - \bar{\beta}(t)]}{\Lambda(t)} \phi(t) + \sum_k \lambda_k C_k(t) \quad (2)$$

$$[-M + F_p]\psi(r, E, t) + \frac{S_d[\phi(t'), \psi(r, E, t')]}{\phi(t)} = \quad (3)$$

$$\frac{1}{v} \left[\frac{d\phi}{dt} \cdot \frac{\psi(r, E, t)}{\phi(t)} + \frac{\partial}{\partial t} \psi(r, E, t) \right]$$

Here, the point kinetics parameters are calculated by inner product integrals using the shape function and its initial steady-state adjoint.

$$\rho(t) = \frac{\langle \psi^*(r,E,t)[-M+F]\psi(r,E,t) \rangle}{\langle \psi^*(r,E,t)F\psi(r,E,t) \rangle} \quad (4)$$

$$\bar{\beta}_k(t) = \frac{\langle \psi^*(r,E,t)F_{dk}\psi(r,E,t) \rangle}{\langle \psi^*(r,E,t)F\psi(r,E,t) \rangle} \quad (5)$$

$$\Lambda = \frac{\langle \psi^*(r,E,t)\frac{1}{\nu}\psi(r,E,t) \rangle}{\langle \psi^*(r,E,t)F\psi(r,E,t) \rangle} \quad (6)$$

where F is the total fission operator, and F_d is the delayed fission operator. C_k 's are calculated from balance equations (usually six groups) for delayed neutron precursors.

Various approximations can be used for solution of the remaining shape equation, Equation (3). In the standard quasistatic approach, the time derivative of the shape function is neglected, leading to a more simplified form of the shape equation.²⁰ This scheme may be improved, however, by approximating the time derivative of the shape function with a first order difference operator over a much larger time interval than what is used for solution of the point kinetics portion.^{21,22} In the adiabatic approach, both time derivative terms are neglected in the shape equation. Also, by neglecting the time delay in the precursor distribution profile, the shape equation may be reduced to an eigenvalue equation as shown below:

$$[-M + \frac{1}{k}F]\psi(r,E,t) = 0 \quad (7)$$

In this approach, the coupling between the point kinetics equations and the steady-state shape equation is established through continuous updates of the point kinetics parameters by averaging with flux shape, $\psi(r,E,t)$.

3.1.2 Point Kinetics Approximation

The point kinetics approximation is obtained by assuming that the changes in the shape function is generally negligible during the transient, and the changes in the neutron flux may be expressed by the changes in the amplitude function only. This approximation is expected to be sufficient for transients of research reactors that have small cores, high power densities and a smooth flux shape which is not expected to distort significantly during a transient. The obvious advantage of using point kinetics for neutronics behavior of the reactor is that it limits the number of equations to be solved by eliminating the space dependency and it allows for implicit coupling of the neutronics equations with the thermal-hydraulic equations as discussed in Section 5.2. As a result, very little additional effort is required in programming and the computer time remains reasonable.

In this work, the equations representing the point reactor kinetics are used in slightly different form. These equations are the basis of an earlier analog computer model.¹⁴

$$\frac{dP_f}{dT} = \frac{(1-\beta)k_{eff}-1}{l^*}P_f + \sum_{i=1}^I \lambda_i C_i \quad (8)$$

$$\frac{dC_i}{dt} = \frac{\beta k_{eff}}{l^*}P_f - \lambda_i C_i \quad (9)$$

where $i=1,\dots,I$, and

P_f is fission power,

l^* is the prompt neutron lifetime,

C_i is the effective concentration of the i^{th} delayed neutron precursors group,

λ_i is the decay rate of the i^{th} group delayed neutron precursors,

β the effective delayed neutron fraction, ie

$$\beta = \sum_{i=1}^I \beta_i \quad (10)$$

where β_i is the delayed neutron fraction for the i^{th} precursor group.

The total reactor power, P , is the sum of prompt fission power, P_p , and the decay heat power. The prompt fission power is due primarily to fission fragment kinetic energy while the decay heat power is due to radioactive decay of fission products. This fuel region after-heat is assumed to have a maximum value of 7% of the steady-state power.¹⁴ Prompt neutron lifetime and the effective delayed neutron fraction for the HFIR for the beginning of the fuel cycle conditions has been determined as 35×10^{-6} sec and 0.0070, respectively.¹⁶ At the end of the fuel cycle, when the control rods are not as effective to separate the fuel from reflector, the prompt neutron lifetime is expected to double to 70×10^{-6} sec.¹⁶ The effective delayed neutron fraction is not expected to change significantly at the end of fuel cycle and a value of $\beta=0.0071$ is suggested.¹⁴ Three sets of neutronic data for the delayed neutron precursors were available from previous transient studies for the HFIR core. These data are shown in Table 1.

Table 1 : Delayed neutron data. In the calculations, delayed neutron fractions are normalized to yield $\beta=0.0070$ for BOC, and $\beta=0.0071$ for EOC conditions.

Delayed Neutron Group	Six Group Model ²³		Five Group Model ²⁴		Two Group Model ²⁵	
	β_i	λ_i	β_i	λ_i	β_i	λ_i
1	.000295	3.01	.00017	3.87	.00208	.02523
2	.000809	1.14	.00083	1.40	.00492	.566
3	.002767	.301	.00205	.311		
4	.001367	.111	.00122	.114		
5	.001531	.0305	.00163	.02578		
6	.000229	.0124				

All three sets are examined to determine their effect on a sample transient. When six group data²³ were used, the peak power was 1146.03 MW. The results of the five²⁴ and two group²⁵ neutron precursor data for the same transient were found to be 1146.69 MW and 1146.27 MW, respectively. Since the results for each one of these data sets are in good agreement, the classical six group data for delayed neutron precursors are built into the model and used throughout this work.

The changes in neutron multiplication, k_{eff} , is calculated using the following expression:

$$k_{eff} = k_0 + k_{ext} + k_{scr} + k_{fdb} \quad (11)$$

where k_0 , k_{ext} , k_{scr} and k_{fdb} represents initial, external, scram, and thermal-hydraulic feedback reactivity contributions, respectively. A simplified block diagram illustrating the complex feedback mechanism is presented in Figure 4. The initial reactivity corresponds to the assumed state reactor power at time equal to zero. The external reactivity additions are discussed in Section 6.2. Reactivity control by means of control elements are characterized by a scram signal, a delay between signal and release, a linearly decreasing acceleration of the safety quadrants, and reactivity worth curve as a function of position. The variations in k_{fdb} due to temperature feedback throughout the core are accounted for by considering separate regions each with individual reactivity coefficients, heating and heat transfer rates as shown below:

$$\begin{aligned} k_{fdb} = & \alpha_{fm} [\bar{T}_f^{avg}(t) - \bar{T}_f^{avg}(0)] + \\ & \alpha_{fw} [\rho_m^{avg}(t) - \rho_m^{avg}(0)] + \alpha_{tm} [T_{tm}(t) - T_{tm}(0)] \\ & + \alpha_{tw} [T_{tw}(t) - T_{tw}(0)] + \alpha_{aw} [T_{aw}(t) - T_{aw}(0)] \\ & + \alpha_{cm} [T_{cm}(t) - T_{cm}(0)] + \alpha_{cw} [T_{cw}(t) - T_{cw}(0)] \end{aligned} \quad (12)$$

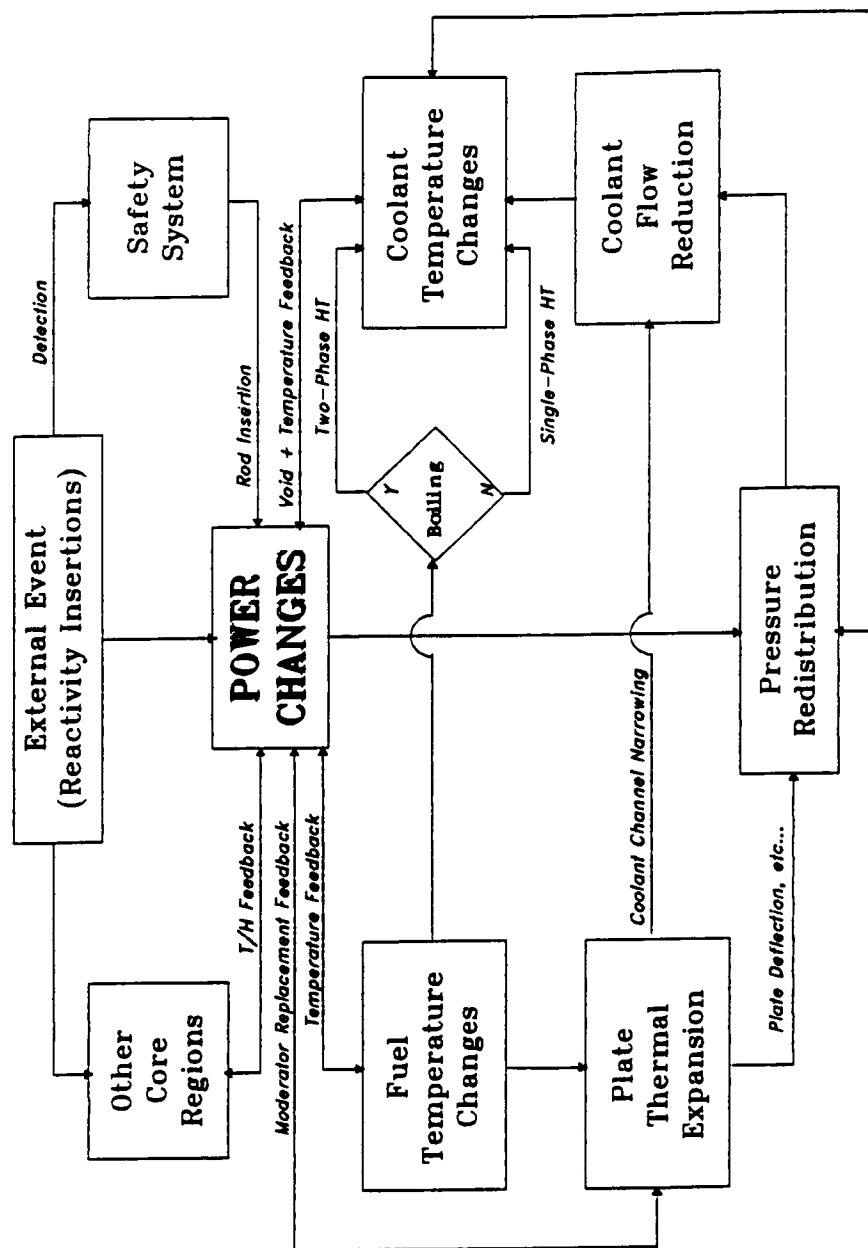


Figure 4 : Nominal-feedback-reactivity calculations: Simplified block diagram.

where α_{fm} is the temperature reactivity coefficient for fuel plates,
 α_{fw} is the reactivity coefficient for water density changes in fuel region,
 α_{tm} is the temperature reactivity coefficient for target rods,
 α_{tw} is the temperature reactivity coefficient of water in target region,
 α_{aw} is the temperature reactivity coefficient of water in annulus between target rods and fuel assembly,
 α_{cm} is the temperature reactivity coefficient for control plates,
 α_{cw} is the temperature reactivity coefficient of water in control region,
 T_f^{avg} is the fuel plate temperature averaged in both azimuthal and axial directions,
 ρ_m^{avg} is the coolant mixture density averaged over all axial nodes,
 T_{tm} is average target rod temperature,
 T_{tw} is average coolant temperature in the target region,
 T_{aw} is average coolant temperature in the water annulus,
 T_{cm} is average control element temperature,
 T_{cw} is average coolant temperature in the control region.

These variables are discussed in detail in the following sections.

The reactivity temperature coefficients used in the calculations to determine nominal-feedback-reactivity were obtained in large part from HFIR critical experiments.¹⁶ In these experiments, the temperature coefficients for the target region and for the fuel region were determined as a function of moderator temperature up to the normal full power moderator temperature of the reactor.¹⁴ Coefficients for the other regions were obtained from normalized calculations.¹⁴ Since it was not possible to obtain experimental coefficients for the end of cycle conditions, calculations were used to predict the ratio of BOC to EOC coefficients for the fuel region. The fuel plate expansion coefficient of reactivity is also included in the calculations by combining this value with the fuel plate temperature reactivity coefficient. The value of this coefficient was determined from the fuel region void coefficients measured in the HFIR critical experiments. The experimental and calculated values for the reactivity coefficients are presented in Table 2.

The fuel region moderator reactivity coefficient is quite sensitive to temperature

Table 2 : HFIR reactivity coefficient input data.¹⁴

Reactivity Coefficient (dk/k/F) for :		Metal	Water
Nominal Channel / Nominal Fuel	BOC	-2.88×10^{-5}	-15.66×10^{-5}
	EOC	-1.44×10^{-5}	-7.83×10^{-5}
Hot-Channel, Hot-Plate		0.0	0.0
Target Region		0.0	0.0
Annulus Region		NA	5.76×10^{-5}
Control Region		0.0	4.86×10^{-5}
Reflector Region		0.0	1.98×10^{-5}

changes. As shown in Figure 5, the coefficient becomes more negative as the moderator temperature increases indicating a strong fluid density feedback mechanism. This effect is combined with reactivity variations resulting from possible void formation in the fuel-region coolant, and is modeled by introduction of a new density reactivity coefficient for two-phase mixture. This modified density reactivity coefficient data is built into the model as a reactivity table and is calculated through interpolations between tabulated values defining reactivity as a function of mixture density.

3.2. FLUID DYNAMICS MODEL

Because of the sensitivity of feedback to the moderator density, choosing an appropriate thermal-hydraulic model for the HFIR and for the type of transients to be analyzed is essential. The analysis of transient two-phase vapor-liquid flow and heat transfer phenomena is the subject of current research. Considerable interest is directed toward the development of improved forms of governing equations and improved numerical techniques

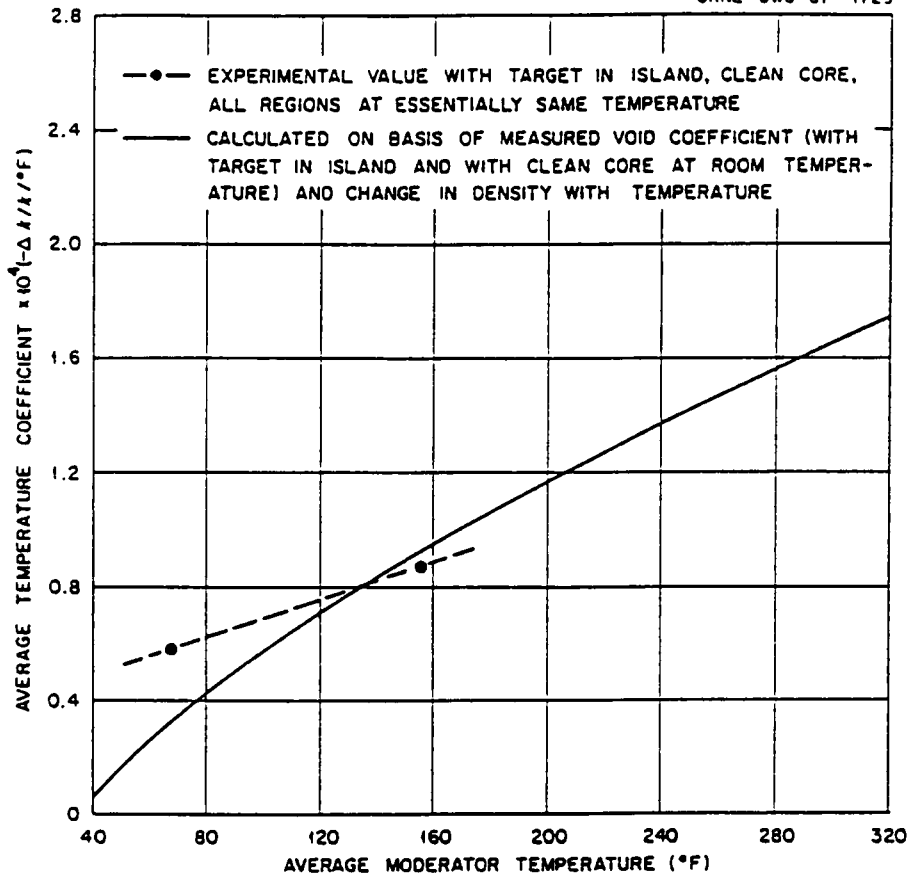


Figure 5 : Moderator temperature coefficient of reactivity vs. moderator temperature for a clean core with target in the island.

for their solution. A review of two-phase flow models can be found in Appendix B.

Many transient two-phase flow analyses in current use are based on a single-fluid model (three conservation equations). Among these, probably the simplest model is the *homogeneous-equilibrium model* (HEM) in which the two-phase fluid is viewed as a homogeneous mixture of vapor and liquid, generally with the phases in thermal equilibrium.^{26,27} This homogeneous model gives surprisingly good results particularly for high pressure and high flow rate conditions at which HFIR operates. However, for the transients in which very rapid pressure and flow changes are encountered, further modeling sophistication is needed to account for thermal non-equilibrium and non-homogeneity.

Various approaches are being taken to obtain improved models in which the differing phasic velocities are appropriately modelled. Some current single-fluid models are based on the use of the *slip ratio* to model the unequal phasic velocities (*Slip Model*).²⁶ In the *Drift Flux Model*,²⁸⁻³¹ an extra conservation equation for vapor mass is added, and the differing phase velocities are represented in terms of a "drift flux" or a "vapor drift velocity."^c

The two-fluid models which are based on six equations with an equation for the conservation of mass, momentum, and energy written for each phase, provide a reasonably complete and sufficiently accurate description of the two-phase flow. In addition, these conservation equations must be supplemented by macroscopic constitutive equations and by equations which describe the interfacial transfer conditions for mass, momentum and energy. Although the two-fluid model has a wider range of validity than single-fluid models, the simultaneous solution of these six conservation equations is time consuming and may encounter numerical instability due to complex characteristics. Furthermore, determining the parameters defining interfacial exchange is very difficult since little is known about the flow in narrow HFIR coolant channels under high heat fluxes. Instead of pursuing the usually impractical extreme of tracking the interface separating the volumes occupied by the different phases, the number of equations can be reduced for the particular transients of interest in this work.

^c Slip and drift flux models can also be justified as particular cases of two-fluid models.

3.2.1. Two-Phase Flow Equations

The fluid dynamics model used in this work can be characterized as non-homogeneous, equilibrium two-phase flow with provision for subcooled boiling which is based on a mass and energy split between the liquid and vapor phases. Since the coolant flow in the HFIR core is a collection of single channel parallel flows that are coupled through each fuel plate and by common bulk boundary conditions on each end, the conservation equations can be reduced to one dimension neglecting variations across the channel span. The liquid and vapor continuity equations, respectively, are

$$\frac{\partial}{\partial t}[(1-\alpha)\rho_l] + \frac{\partial}{\partial z}[(1-\alpha)\rho_l v_l] = -\dot{\Gamma}_g \quad (13)$$

$$\frac{\partial}{\partial t}(\alpha\rho_g) + \frac{\partial}{\partial z}(\alpha\rho_g v_g) = \dot{\Gamma}_g \quad (14)$$

the liquid and vapor thermal energy equations, respectively, are

$$\frac{\partial}{\partial t}[(1-\alpha)\rho_l H_l] + \frac{\partial}{\partial z}[(1-\alpha)\rho_l H_l v_l] = (1-\alpha)\left(\frac{Dp}{Dt} + v_l F_w\right) + \dot{q}_{wl} + \dot{q}_{sl} \quad (15)$$

$$\frac{\partial}{\partial t}(\alpha\rho_g H_g) + \frac{\partial}{\partial z}(\alpha\rho_g H_g v_g) = \alpha\left(\frac{Dp}{Dt} + v_g F_w\right) + \dot{q}_{wg} + \dot{q}_{sg} \quad (16)$$

and the equation for mixture momentum is

$$\begin{aligned} \frac{\partial}{\partial t}[(1-\alpha)\rho_l v_l + \alpha\rho_g v_g] + \frac{\partial}{\partial z}[(1-\alpha)\rho_l v_l^2 + \alpha\rho_g v_g^2] + \\ \frac{\partial p}{\partial z} + F_w - [(1-\alpha)\rho_l + \alpha\rho_g]g = 0 \end{aligned} \quad (17)$$

In these equations

ρ is density,

v is velocity,
 α is void fraction,
 $\dot{\Gamma}$ is vapor generation rate,
 H is enthalpy,
 p is pressure,
 F_w is wall friction term,
 $\dot{q}_s$ is surface heat transfer rate per unit volume,
 $\dot{q}_v$ is volumetric heat generation rate, and
 g is gravity.

Subscripts l and g refers to liquid and vapor phases respectively. The substantial derivative of the pressure in the energy equations is given by the following relation:

$$\frac{Dp}{Dt} = \frac{\partial p}{\partial t} + v \frac{\partial p}{\partial z} \quad (18)$$

Basically, the conservation equations for mass, energy, and axial momentum are derived by considering the transport in and out of a control volume.^{32,33} Analyses applied to one-dimensional geometries employ phasic quantities which are averaged over time and over the flow cross sectional area.

3.2.2. Friction Model

In the energy equations, the terms with wall friction represent viscous dissipation. In these equations, the mechanical energy exchange between the phases is neglected. Also, the covariance terms are assumed negligible. In the momentum equation, the wall friction loss, F_w , is represented by the Darcy formula³⁴

$$F_w = f \frac{|G_m| G_m}{2 D_e \rho_m} \quad (19)$$

where D_e is the equivalent diameter of the coolant channel, G_m is the mass flux for the two-phase mixture, and ρ_m is the two-phase mixture density. The mixture quantities are given by:

$$G_m = (1-\alpha)\rho_l v_l + \alpha\rho_g v_g \quad (20)$$

$$\rho_m = (1-\alpha)\rho_l + \alpha\rho_g \quad (21)$$

For single-phase liquid and vapor flow in coolant channels with smooth walls, the friction factor, f , is often expressed by the McAdams correlation⁵ for high Reynolds numbers ($3.10^4 < Re < 1.10^6$):

$$f_{sp} = 0.184 Re^{-0.2} \quad (22)$$

For $Re < 3.10^4$, the following Blasius correlation is suggested:⁵

$$f_{sp} = 0.316 Re^{-0.25} \quad (23)$$

The liquid and vapor phase Reynolds numbers are discussed in the following sections. Probably the most accurate correlation for the friction factor which includes surface roughness effects has been derived by Colebrook:³⁵

$$\frac{1}{\sqrt{f_{sp}}} = -2 \ln \left[\frac{e}{3.7D_e} + \frac{2.51}{Re\sqrt{f_{sp}}} \right] \quad (24)$$

However, this transcendental expression requires an iterative solution and is often too costly to be used in transient calculations. Instead, a somewhat simpler form in terms of channel relative roughness as suggested by Waggner³⁶ is used as the base correlation throughout this work.

$$f_{sp} = 0.0055 \left[1 + \left(2 \cdot 10^4 \frac{e}{D_H} + \frac{10^6}{Re} \right)^{\frac{1}{3}} \right] \quad (25)$$

A comparison of these correlations has been performed and good agreement exists between Waggner and Colebrook formulations for typical HFIR coolant flow conditions.

The general approach for formulating the two-phase friction is to relate it to friction

factors defined for single-phase flow with the same mass flux. In a two-phase boiling channel, the friction pressure gradient is given by:

$$f_{tp} = \phi_{tp}^2 f_{sp} \quad (26)$$

Probably the best known two-phase friction multiplier is due to Martinelli-Nelson.³⁷ However their correlation does not support a wide range of operating conditions. Instead, the two-phase friction multiplier, ϕ_{tp}^2 , is calculated in this work using the Jones correlation.³⁸ Jones included flow rate effects to the Martinelli-Nelson two-phase friction multiplier as follows:

$$\phi_{tp}^2 = \Omega(p, G_m) \left[1.2 \left(\frac{\rho_l}{\rho_g} - 1 \right) x^{0.824} \right] + 1 \quad (27)$$

where, for $G_m < 200$,

$$\Omega(p, G_m) = 1.36 + 0.0005p + 3.6 \cdot 10^{-4} G_m - 2.5704 \cdot 10^{-6} p G_m \quad (28)$$

and for $G_m > 200$,

$$\Omega(p, G_m) = 1.26 - 0.0004p + \frac{33.05556}{G_m} + \frac{0.077778p}{G_m} \quad (29)$$

In the correlations above, p is in psia.

3.2.3. Flow and Heat Transfer Regimes

The heat transfer model is based on a "flat fuel plate" and "rectangular coolant channel" geometry. The heat transfer from fuel to coolant is assumed to take place by conduction, convection, radiation and direct deposition of gamma and neutron energy in the fluid. The volumetric heat sources per unit volume per unit time, $\dot{q}_{vg}$ and $\dot{q}_{vn}$, are direct coolant heating due to gamma rays and neutron slowing-down. The net convective heat sources per unit time and per unit volume from the fuel element outer surface to the coolant is represented by $\dot{q}_{sl}$ and $\dot{q}_{sg}$ for liquid and vapor phases, respectively. These surface heat sources depend on the heat transfer mechanism considered in each of the assumed flow

regimes as discussed in detail in the "heat conduction model" (Section 3.3).

In this work, the flow regimes considered are *single-phase liquid flow*, *subcooled boiling*, *bulk boiling*, *film boiling* and *single-phase vapor flow*. In addition, a separate mode of heat transfer is provided for single-phase liquid flow when the fuel element surface temperature is higher than the liquid saturation temperature. The flow and heat transfer regimes which may occur in a HFIR coolant channel under high and low heat fluxes are shown in Figure 6 and 7.

Water enters the coolant channel from the top of the fuel assembly in a subcooled state. At low heat fluxes (Figure 6), water near the surface of the fuel plates reaches the saturation temperature and can nucleate vapor bubbles while the bulk liquid temperature is still below the saturation temperature. When this subcooled boiling regime begins, the turbulence caused by the vapor bubbles slightly improves the heat transfer. As the liquid moves downward in the channel, the bulk liquid temperature continues to rise until it reaches the saturation temperature. At this point the number of vapor bubbles increases significantly, and as they detach from the surface they coalesce into larger bubbles, starting the bulk boiling regime. In this regime, the surface temperature does not increase significantly due to improved heat transfer and constant fluid temperature. As boiling continues, bubbles tend to merge into a vapor core in the coolant channels and the liquid film at the fuel surface gets thinner. Continued evaporation in the channel eventually causes a liquid deficiency and a dryout condition. Above this point, the flow is mostly vapor and the liquid deficient heat transfer causes the surface temperature to increase significantly, possibly compromising the thermal limits for the fuel plates.

At high heat fluxes, on the other hand, the increased surface vapor generation rate in the subcooled or bulk boiling regimes may cause a film blanketing of the heat transfer surface leading to a departure from nucleate boiling (DNB). (Figure 7) The effects of this transition to post-DNB heat transfer is identical to the dryout condition. The heat flux value at this point is called the **critical heat flux (CHF)**; this phenomena is discussed in Section 3.2.4

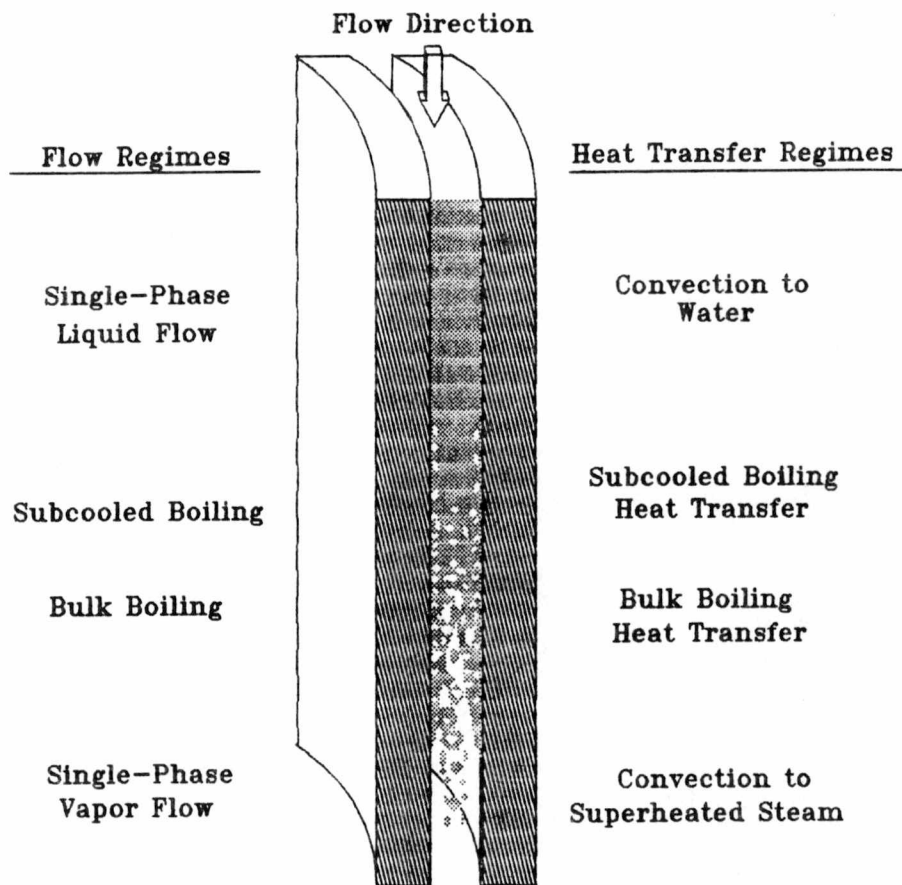


Figure 6 : The two-phase flow and heat transfer regimes which may occur in the HFIR coolant channels under low heat fluxes during a typical low-power, low-flow transient.

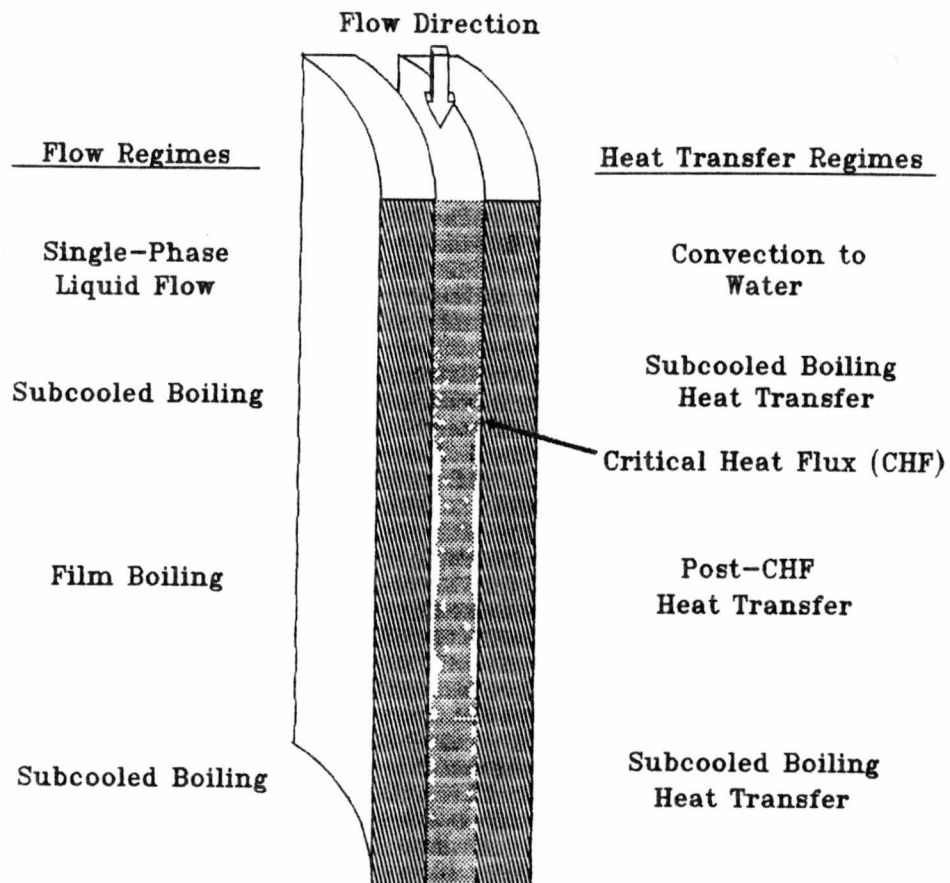


Figure 7 : The two-phase flow and heat transfer regimes which typically occur in the HFIR coolant channels under high heat fluxes during a Mode 1 full power reactivity transient.

in greater detail. Subsequently, film boiling dominates the post-CHF flow and heat transfer.^d

3.2.3.1. Single-Phase Liquid Flow

For *single-phase liquid flow*, by setting $\alpha=0$, the fluid dynamics equations reduce to:

$$\frac{\partial \rho_l}{\partial t} + \frac{\partial}{\partial z}(\rho_l v_l) = 0 \quad (30)$$

$$\frac{\partial}{\partial t}(\rho_l H_l) + \frac{\partial}{\partial z}(\rho_l H_l v_l) = \frac{Dp}{Dt} + v_l F_w + \dot{q}_w + \dot{q}_{sl} \quad (31)$$

$$\frac{\partial}{\partial t}(\rho_l v_l) + \frac{\partial}{\partial z}(\rho_l v_l^2) + \frac{\partial p}{\partial z} + F_w - \rho_l g = 0 \quad (32)$$

where the surface heat transfer term is given by

$$\dot{q}_{sl} = \frac{2}{a} h_{NB} (T_s - T_l) \quad (33)$$

Here a is coolant channel thickness,

T_s is fuel element surface temperature,

T_l is bulk liquid temperature, and

h_{NB} is single-phase liquid heat transfer coefficient.

For the heat transfer coefficient in the single-phase liquid flow regime, either the well known Dittus-Boelter correlation³⁹ or a relation recommended by Gambill and Bundy⁴⁰ is used. The Dittus-Boelter heat transfer correlation is a simple and proven correlation for a wide range of operating conditions. It is given by the following relation

^d Transition boiling which may be encountered in certain forced convection conditions is not considered in this work.

$$h_{NB} = 0.023 Re^{0.8} Pr^{0.4} \left(\frac{k_l}{D_e} \right) \quad (34)$$

The Gambill-Bundy relation, which is a modified version of the Hausen correlation,⁴¹ takes into account the conditions in the thermal intake region. It is reported to be better suited for high velocity water flow in narrow HFIR coolant channels under high heat fluxes.⁴⁰

$$h_{NB} = 0.116(Re^{\frac{2}{3}} - 125)Pr^{\frac{1}{3}} \left(1 + \frac{D_e}{h} \right)^{\frac{2}{3}} \left[\frac{(\mu_l)_{bulk}}{(\mu_l)_{surf}} \right]^{0.14} \left(\frac{k_l}{D_e} \right) \quad (35)$$

Both relations are built into the model and the switch between these two correlations is established through input data. In these correlations, *Re* is the liquid *Reynolds number*, *Pr* is the liquid *Prandtl number*, *k_l* is the liquid thermal conductivity, *h* is the coolant channel length, and *μ_l* is liquid viscosity. Subscripts "*bulk*" and "*surf*" indicate that liquid properties are to be evaluated at the bulk and film temperatures, respectively. These parameters are determined from the following definitions and correlations:

$$Re = D_e \frac{\rho_l v_l}{\mu_l} \quad (36)$$

$$Pr = \frac{c_{pl} \mu_l}{k_l} \quad (37)$$

$$k_l = c_{k0} + c_{k1} H_l + c_{k2} H_l^2 + c_{k3} H_l^3 \quad (38)$$

where

$$\begin{aligned} c_{k0} &= 9.208332762 \cdot 10^{-5} \\ c_{k1} &= 1.628148046 \cdot 10^{-7} \\ c_{k2} &= -3.735555323 \cdot 10^{-10} \\ c_{k3} &= 1.425185097 \cdot 10^{-13} \end{aligned} \quad (39)$$

The liquid viscosity is calculated from⁴²

$$\mu_l = 1.6222 \cdot 10^{-3} [1 + 4.0094 \cdot 10^{-8} (p - p_{sat})(T_l - 89)] 10^{\frac{445}{T_l - 208}} \quad (40)$$

where

$$p_{sat} = 3206.2 - 6483.29(705.4 - T_l)(1 - 2.9 \cdot 10^{-4} \frac{705.4 - T_l}{1012 - T_l}) \quad (41)$$

The liquid temperature, T_l , and the liquid specific heat, c_{pl} , for the subcooled water are determined iteratively from the liquid enthalpy, H_l , using Newton-Raphson scheme as follows:⁴²

$$T_l^{(n+1)} = T_l^{(n)} + \frac{H_l - H_l^{(n)}}{c_{pl}^{(n)}} \quad (42)$$

$$H_l^{(n)} = c_{c1} + c_{c2} T_l^{(n)} + \frac{c_{c3}}{802 - T_l^{(n)}} + \frac{c_{c4}}{T_l^{(n)} - 750} \quad (43)$$

$$c_{pl}^{(n)} = c_{c2} + \frac{c_{c3}}{(802 - T_l^{(n)})^2} - \frac{c_{c4}}{(T_l^{(n)} - 750)^2} \quad (44)$$

where superscript (n) is the iteration index. Here, the correlation coefficients are

$$\begin{aligned} c_{c1} &= -47.61 + 0.001618p \\ c_{c2} &= 0.96566 - 4.9842 \cdot 10^{-6}p \\ c_{c3} &= 8843.4 + 8.2445p \\ c_{c4} &= -20392 + 6.6112p \end{aligned} \quad (45)$$

Finally, the equation of state describing the liquid density, ρ_l , closes the set. It is given by the following expressions;⁴² for $H_l \leq 280$

$$\rho_l = C_{\rho 1} + C_{\rho 2} H_l^2 + C_{\rho 3} H_l^4 \quad (46)$$

and for $H_l > 280$

$$\rho_l = C_{\rho 4} + \frac{C_{\rho 5}}{H_l - C_{\rho 6}} \quad (47)$$

where the correlation coefficients are the following:

$$\begin{aligned} C_{\rho 1} &= 62.4 + 2.14 \cdot 10^{-4} p \\ C_{\rho 2} &= -8.73 \cdot 10^{-5} + 1.438 \cdot 10^{-9} p \\ C_{\rho 3} &= 2.32 \cdot 10^{-10} - 6.2 \cdot 10^{-15} p \\ C_{\rho 4} &= 92.924 + 5.761 \cdot 10^{-4} p \\ C_{\rho 5} &= 39440.2 + 1.6386 p \\ C_{\rho 6} &= 1377.35 + 0.035704 p \end{aligned} \quad (48)$$

In all of the correlations above, *British* units are assumed, and p is the system pressure in psia.

3.2.3.2. Single-Phase Liquid Flow with $T_s > T_{sat}$

The void profile during a hypothetical boiling situation in HFIR coolant channels is illustrated in Figure 8. In a heated channel, boiling begins with the onset of nucleation of vapor bubbles at the fuel element outer surface under high heat fluxes. This point is marked as Z_O in Figure 8 and approximated in the model as the point where the fuel surface temperature exceeds the corresponding liquid saturation temperature. At this point, the bulk water is still below saturation temperature, vapor bubbles continuously grow and collapse giving a small non-zero void fraction that may be neglected.

In this work, a separate heat transfer mode is provided for this special single-phase liquid flow regime when the fuel element surface temperature is higher than the liquid saturation temperature. In this regime, the single-phase field equations (30)-(32) still apply;

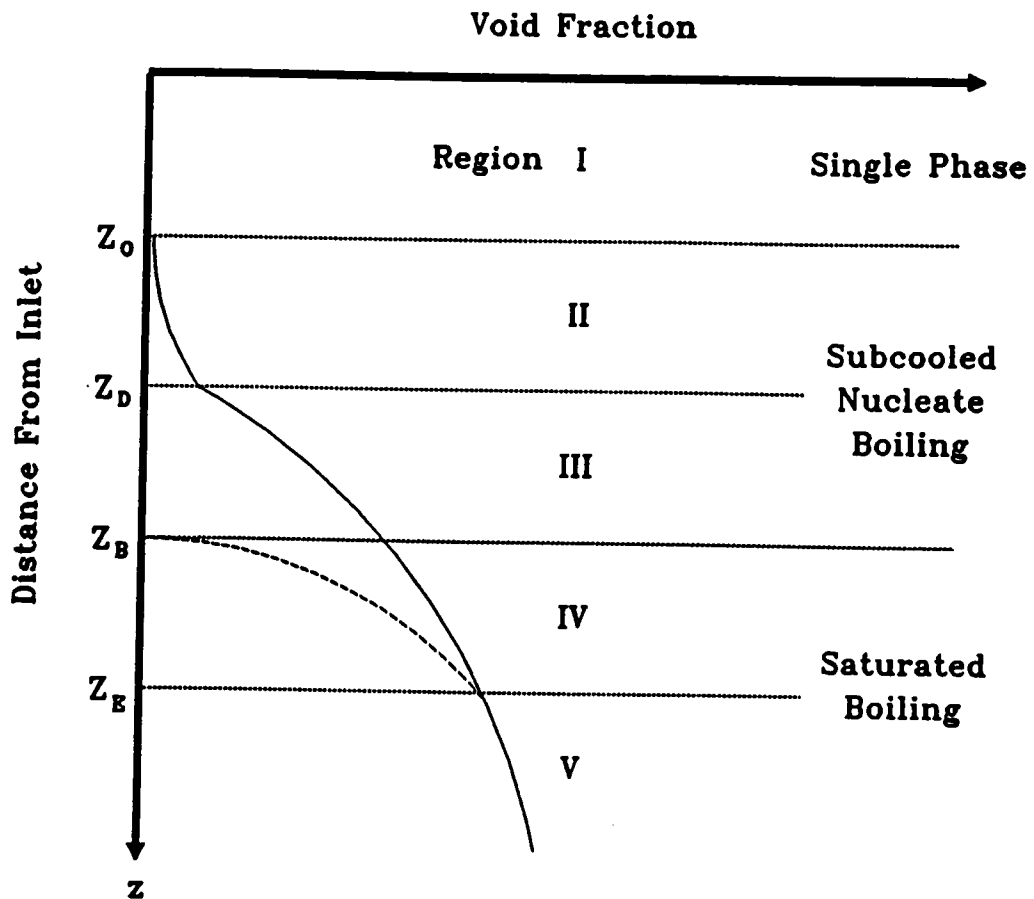


Figure 8 : Formation of voids in a heated channel. The solid line indicates true void profile including subcooled boiling, while the dashed line indicates saturated boiling void profile.^{46,5}

however, the surface heat source is now given by⁴³

$$\dot{q}_{st} = \frac{2}{a} [h_{NB}(T_s - T_l) + h_B(T_s - T_{sat})] \quad (49)$$

where the boiling heat transfer coefficient, h_B , is calculated from

$$h_B = 0.05358 e^{p/630} (T_s - T_{sat}) \quad (50)$$

which is Thom's version⁴⁴ of the Jens-Lottes correlation.⁴⁵

3.2.3.3. Subcooled Boiling

An important feature of the two-phase flow model used in this study is the thermal non-equilibrium process due to subcooled forced convection boiling. In such a system, heat is being added to a subcooled fluid as it flows through a heated channel and, as shown in Figure 7, vapor bubbles and liquid below saturation can be found simultaneously at a given flow cross section.

The boiling flow in Figure 8 can be analyzed in five regions.^{46,5} In Region I, no vapor is present and normal forced convection cooling prevails as discussed in the previous section. At point Z_O , the first vapor bubbles appear and the subcooled boiling regime actually begins with this onset of nucleation at the fuel surface under high heat fluxes. In Region II, however, the bulk of the liquid is well below saturation temperature and the thickness of the superheated liquid layer close to the wall is small. Bubbles collapse before they grow larger, giving no measurable increase in void fraction.

As the water heats up, bubbles can grow larger and begin to detach from the wall surface. This point is indicated as Z_D in the figure. Once this point is reached, bubbles detach continuously and condense slowly as they flow causing a significant increase in the void fraction. In this analysis, the subcooled boiling regime is considered after point Z_D is reached.

Most often, this point is formulated by correlations considering a heat balance and a

temperature distribution in the single-phase liquid away from the wall. However, these correlations have limited validity for HFIR operating conditions which are characterized by a very high coolant velocity in narrow coolant channels. In this study, an alternative approach due to Levy⁴⁷ is used which considers both thermal and hydrodynamic factors to formulate the bubble departure dynamics. Levy introduces a dimensionless size parameter determined from a force balance acting on a vapor bubble

$$Y_B^+ = 0.015 \frac{\sqrt{\sigma D_e \rho_l}}{\mu_l} \quad (51)$$

In terms of this parameter, the differential temperature between the saturated film and the bulk liquid at point Z_D is calculated from the following: For $0 < Y_B^+ < 5$

$$\Delta T_d = T_{sat} - T_l = \frac{q/A}{h} - Q Pr Y_B^+ \quad (52)$$

for $5 < Y_B^+ < 30$

$$\Delta T_d = \frac{q/A}{h} - 5Q \left[Pr + \ln \left[1 + Pr \left(\frac{Y_B^+}{5} - 1 \right) \right] \right] \quad (53)$$

and for $Y_B^+ > 30$

$$\Delta T_d = \frac{q/A}{h} - 5Q \left[Pr + \ln(1 + 5 Pr) + 0.5 \ln \left(\frac{Y_B^+}{30} \right) \right] \quad (54)$$

where

$$Q = \frac{q/A}{\rho_l c_{pl} \sqrt{\frac{F_w D_e}{4 \rho_l}}} \quad (55)$$

These equations are used to determine the subcooled fluid conditions at the position where

vapor bubbles first leave the heated surface. With this correlation, it was reported that buoyant forces play a negligible role even at the low mass flow rates.⁴⁸

Assuming that vapor enthalpy, H_g , remains constant during the subcooled boiling regime, the vapor energy balance, equation (16), is:⁶⁷

$$H_g \left[\frac{\partial}{\partial t} (\alpha \rho_g) + \frac{\partial}{\partial z} (\alpha \rho_g v_g) \right] = \alpha \frac{Dp}{Dt} + \alpha v_g F_w + \dot{q}_{vg} + \dot{q}_{sg} \quad (56)$$

Using vapor continuity, this equation reduces to:

$$H_g \dot{\Gamma}_g = \alpha \frac{Dp}{Dt} + \alpha v_g F_w + \dot{q}_{vg} + \dot{q}_{sg} \quad (57)$$

where

$$\dot{\Gamma}_g = \frac{\dot{q}_B - \dot{q}_l}{H_{fg}} \quad (58)$$

Here, $\dot{q}_B$ is the boiling component of surface heat flux, and $\dot{q}_l$ is the condensing heat flux. With these, equation (57) yields the following for the vapor surface heat source term

$$\dot{q}_{sg} = H_g \frac{\dot{q}_B - \dot{q}_l}{H_{fg}} - \alpha \frac{Dp}{Dt} - \alpha v_g F_w - \dot{q}_{vg} \quad (59)$$

The total surface heat transfer is given by:

$$\dot{q}_s = \dot{q}_{sg} + \dot{q}_{sl} \quad (60)$$

which is the sum of the boiling and non-boiling components of the surface heat transfer:

$$\dot{q}_s = \dot{q}_{NB} + \dot{q}_B \quad (61)$$

Therefore, the liquid phase surface heat transfer term from equation (60) becomes:

$$\dot{q}_{sl} = \dot{q}_s - \dot{q}_{sg} \quad (62)$$

$$\dot{q}_{sl} = \dot{q}_{NB} + \dot{q}_B - \dot{q}_{sg} \quad (63)$$

$$\dot{q}_{sl} = \dot{q}_{NB} + \dot{q}_B - H_g \frac{\dot{q}_B - \dot{q}_I}{H_{fg}} + \alpha \frac{Dp}{Dt} + \alpha v_g F_w + \dot{q}_{vg} \quad (64)$$

or, after arranging:

$$\dot{q}_{sl} = \dot{q}_{NB} + \frac{H_g \dot{q}_I - H_f \dot{q}_B}{H_{fg}} + \alpha \frac{Dp}{Dt} + \alpha v_g F_w + q_{vg} \quad (65)$$

Substituting this expression back into liquid energy balance, equation (15), the liquid energy equation becomes:

$$\frac{\partial}{\partial t} [(1-\alpha)\rho_l H_l] + \frac{\partial}{\partial z} [(1-\alpha)\rho_l H_l v_l] = \quad (66)$$

$$\frac{Dp}{Dt} + v F_w + \dot{q}_v + \dot{q}_{NB} + \frac{H_g \dot{q}_I - H_f \dot{q}_B}{H_{fg}}$$

In brief, the field equations describing the subcooled boiling flow regime are given by equations (13), (14), (66), and (17). For equation (66) above, the boiling and non-boiling heat fluxes are derived in Section 3.3.2 and given by the following relations:

$$\dot{q}_B = \frac{2}{a} (h_B + \frac{1}{2} h_{NB}) (T_s - T_{sat}) \quad (67)$$

$$\dot{q}_{NB} = \frac{1}{a} h_{NB} (T_s + T_{sat} - T_l) \quad (68)$$

where the heat transfer coefficients, h_{NB} and h_B , have been previously defined. And the condensing heat flux, $\dot{q}_I$, is calculated from:

$$\dot{q}_l = \frac{2}{a} h_l (T_{sat} - T_l) \quad (69)$$

where h_l is described by the Hancox-Nicoll correlation⁴⁹

$$h_l = 0.4 Re_l^{0.662} Pr_l \frac{k_l}{D_{eq}} \quad (70)$$

To calculate the local void fractions in the subcooled boiling region, first the subcooled flow quality is determined by a profile-fit approach as suggested by Levy.^{47,50} It is assumed that at the point of bubble departure, Z_D , the vapor quality will be negligible. However, the corresponding local equilibrium quality which is calculated from a heat balance assuming thermal equilibrium, will have a negative value

$$x_e(Z_D) = -c_{pl} \frac{\Delta T_d}{H_{fg}} \quad (71)$$

where ΔT_d is given by equations (52), (53), and (54). Levy suggests a simple exponential relation for the subcooled flow quality profile in which the flow quality is equal to zero at $z=Z_D$ and approaches the equilibrium quality asymptotically:

$$x(z) = x_e(z) - x_e(Z_D) e^{\left[\frac{x_e(z)}{x_e(Z_D)} - 1 \right]} \quad (72)$$

The corresponding subcooled void fraction is then obtained by assuming that the relationship between α and x is the same as it would be under thermal equilibrium conditions. This relationship is discussed in the following section.

3.2.3.4. Bulk Boiling

As the coolant heats up, the rate of bubble formation increases and dominates the heat transfer. When the bulk water temperature reaches saturation temperature (point Z_B in Figure 8), the correlations developed for estimating the heat flux associated with incipient

boiling are still applicable. The void fraction continues to increase approaching the thermal equilibrium condition at Z_E . At this point, the thermodynamic non-equilibrium history is completely lost. In this work, the bulk boiling regime is initiated when the bulk liquid temperature reaches the saturation temperature.

In this flow regime, the fluid dynamics equations (13) to (17) as well as the auxiliary equations developed in the section for subcooled boiling apply except that for the thermal equilibrium conditions the following will apply:

$$H_l = H_f \quad (73)$$

$$T_l = T_{sat} \quad (74)$$

$$\rho_l = \rho_{sat} \quad (75)$$

Void fraction is an important parameter in predicting reactivity feedback, and hence reactor power as well as for predicting flow rates during transients. In general, the void fraction of a given flow depends on the flow conditions and the distribution of the phases. Several correlations have been proposed throughout the years to predict the void fraction under a variety of conditions.⁵¹ The correlations which are of interest in this work are those which cover high-pressure, high-velocity, co-current downflow of the HFIR coolant channels.⁵²⁻⁵⁶ Since several of these correlations are flow regime dependent, the relatively simple Dix model is preferred in this work. The Dix model is essentially a drift flux model²⁸ developed for analysis of boiling water reactors at operating conditions.⁵⁷ A comparison of the void fraction predictions from various void models were performed in Reference 51 using steam-water test data representing several areas of interest for the thermal-hydraulic analysis of reactor coolant channels. A good agreement was reported for the Dix model with high-pressure, high-flow experimental data in these comparisons.⁵¹

The Dix model is based on the volumetric flow ratio which is defined as

$$\beta = \frac{1}{1 + \frac{1-x}{x} \frac{\rho_g}{\rho_l}} \quad (76)$$

In terms of this quantity, the drift flux parameters C_o and V_{gj} are given by^{54,57}

$$C_o = \beta \left[1 + \left(\frac{1}{\beta} - 1 \right)^{(\rho_g/\rho_l)^{0.1}} \right] \quad (77)$$

$$V_{gj} = 2.9 \left[\frac{\rho_l - \rho_g}{\rho_l^2} \sigma g \right]^{1/4} \quad (78)$$

The void fraction may then be predicted from the void drift model as follows:

$$\alpha = \frac{1}{C_o \left(1 + \frac{1-x}{x} \frac{\rho_g}{\rho_l} \right) + \frac{V_{gj}}{xG} \rho_g} \quad (79)$$

Since the drift flux parameters are constant (not functions of void fraction) at a given set of conditions, the Dix model is a non-iterative correlation which provides a distinct advantage in terms of computation times.

The slip ratio, which is defined as the ratio of vapor velocity to liquid velocity, is calculated using the Bankoff-Jones correlation based on the local void fraction.^{38,58}

$$S = \frac{1-\alpha}{K-\alpha} \quad (80)$$

where

$$K = K_s + (1-K_s)\alpha^R \quad (81)$$

$$K_s = 0.71 + 0.29 \frac{p}{p_c} \quad (82)$$

$$R = 3.448275 - (1.875 \cdot 10^{-4} - 5.85 \cdot 10^{-7} p) p \quad (83)$$

Here, p_c is the critical pressure (3206.2 psia).

The liquid phase properties are those of pressurized water as discussed in Section 3.2.3.1, and the vapor properties are described in the following section.

3.2.3.5. Single-Phase Vapor Flow

In this flow regime, the void fraction is unity and the governing field equations become

$$\frac{\partial \rho_g}{\partial t} + \frac{\partial}{\partial z}(\rho_g v_g) = 0 \quad (84)$$

$$\frac{\partial}{\partial t}(\rho_g H_g) + \frac{\partial}{\partial z}(\rho_g H_g v_g) = \frac{Dp}{Dt} + v_g F_w + \dot{q}_{vg} + \dot{q}_{sg} \quad (85)$$

$$\frac{\partial}{\partial t}(\rho_g v_g) + \frac{\partial}{\partial z}(\rho_g v_g^2) + \frac{\partial p}{\partial z} + F_w - \rho_g g = 0 \quad (86)$$

where the surface heat source $\dot{q}_{sg}$ is given in terms of Dittus-Boelter heat transfer coefficient³⁹ for superheated steam:

$$\dot{q}_{sg} = \frac{2}{a} h_{DB} (T_s - T_g) \quad (87)$$

where

$$h_{DB} = 0.023 Re^{0.8} Pr^{0.4} \frac{k_g}{D_e} \quad (88)$$

The vapor phase Reynolds number, Prandtl number and other physical properties are calculated from following definitions and correlations⁴³

$$Re = D_e \frac{\rho_g v_g}{\mu_{gs}} \quad (89)$$

$$Pr = c_{g1} + c_{g2} \left[\exp[-c_{g3}(T_g^* - T_{sat})^{0.75}] - 1 \right] \quad (90)$$

$$\begin{aligned} k_g = & 1.6 \cdot 10^{-7} [(17.6 + 0.00587 T_{gc} + 1.04 \cdot 10^{-4} T_{gc}^2 - 4.51 \cdot 10^{-8} T_{gc}^3) + \\ & (103.51 + 0.4198 T_{gc} - 2.771 \cdot 10^{-5} T_{gc}^2) \frac{\rho_{gs}}{62.4} + \\ & 2.1482 \cdot 10^{14} T_{gc}^{-4.2} (\frac{\rho_{gs}}{62.4})^2] \end{aligned} \quad (91)$$

$$\begin{aligned} \mu_{gs} = & 4.916 \cdot 10^{-6} + 1.5195 \cdot 10^{-8} T_g^* + 3.8 \cdot 10^{-7} \rho_{gs} + \\ & 1.168 \cdot 10^{-8} \rho_{gs}^2 + 2.823 \cdot 10^{-11} \rho_{gs}^3 \end{aligned} \quad (92)$$

where

$$c_{g1} = \exp[0.014705(2.1645p - 1.6095 \cdot 10^{-3} p^2 + 5.545 \cdot 10^{-7} p^3)^{\frac{1}{2}}] \quad (93)$$

$$c_{g2} = c_{g1} + 0.12 - \exp(-6.895 \cdot 10^{-6} p) \quad (94)$$

$$c_{g3} = 0.0379(1 - 3.12 \cdot 10^{-4} p) + \frac{0.05318}{1 + 2.927 \cdot 10^{-6} (p - 3206)^2} \quad (95)$$

$$T_g^* = \frac{1}{2}(T_s + T_{sat}) \quad (96)$$

$$T_{gc} = \frac{(T_g^* - 32)}{1.8} \quad (97)$$

$$\rho_{gs} = \left[\frac{1}{\rho_g^{sat}} + 0.5682 \exp(5.757 \cdot 10^{-4} p^{0.9}) \frac{T_g^* - T_{sat}}{p} \right]^{-1} \quad (98)$$

In all of the equations above, p is in psia.

3.2.4. Critical Heat Flux

The dependence of the two-phase heat transfer regimes on heat flux is presented qualitatively in Figure 9. For low heat fluxes, as boiling continues, bubbles tend to merge into a vapor core in the coolant channels and the liquid film at the fuel surface gets thinner (also see Figure 6). At the point where this liquid film is depleted, a dryout condition occurs and the liquid deficient heat transfer causes the fuel temperature to rise significantly. Therefore, dryout is considered as a special case of critical heat flux when the surface heat flux is low.

However, when the imposed heat flux is higher (such as is the case for HFIR coolant channels during full-power, full-flow transients), it is possible that, at the earlier stages of the boiling, the vapor generation rate near the fuel element outer surface becomes high enough to create a vapor film which separates the liquid from the fuel surface (also see Figure 7).

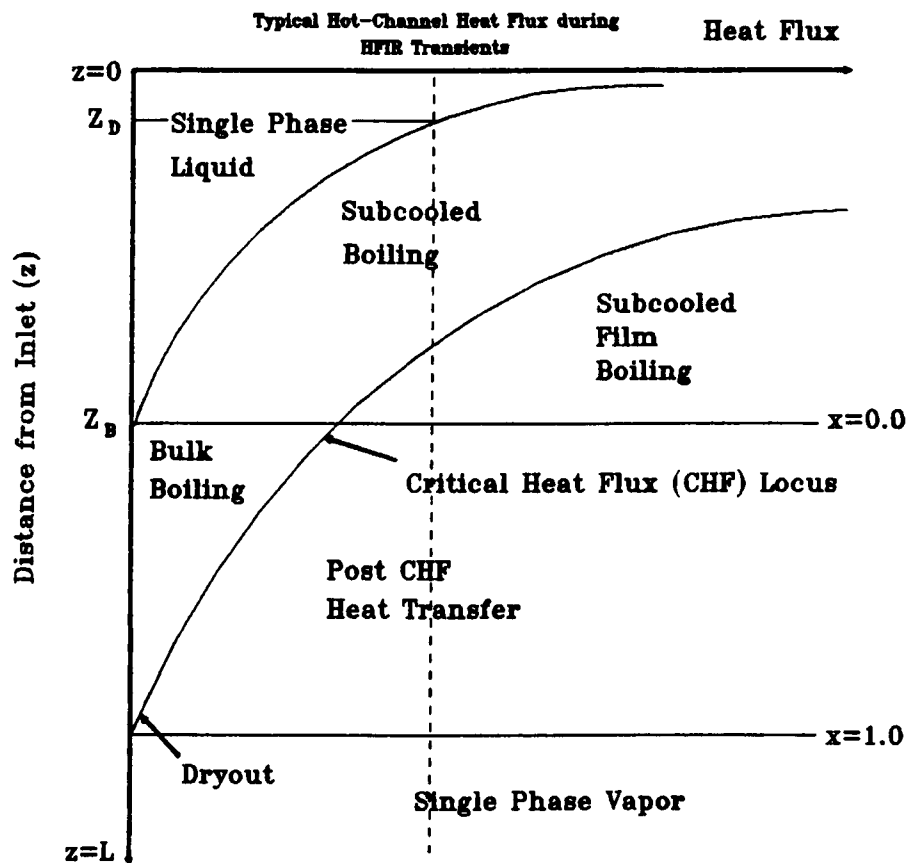


Figure 9 : Dependence of heat transfer regimes on surface heat flux and critical heat flux (CHF) locus.

This phenomenon is mostly referred to as "departure from nucleate boiling (DNB)" and marks a transition to film boiling. As indicated in Figure 9, the situation in the HFIR hot-channel is such that critical heat flux is typically exceeded during the subcooled boiling regime for Mode 1, full-power transients.

The highly subcooled, high-velocity coolant flow in HFIR channels delays the boiling incipience substantially. Therefore, the margin between the *incipient boiling* and *departure from nucleate boiling* heat fluxes is quite small and the two curves marking the transition to subcooled boiling and film boiling in Figure 9 are closer than usual.^e After the critical heat flux is reached, transition to film boiling causes the heat transfer coefficient to deteriorate significantly resulting in a rapid and substantial increase in fuel temperature.

3.2.4.1. Critical Heat Flux Correlation

The critical heat flux correlation used in this model, called the Bowring correlation, contains four optimized pressure parameters.⁵⁹ This correlation has a wider range of applicability in terms of mass flux than many other available correlations such as Biasi⁶⁰, which is utilized in RELAP, and the CISE-4 correlations.⁶¹ The Bowring correlation is described by the following equations in SI units:

$$\dot{q}_{CHF} = \frac{A - B h_{fg} x}{C} \quad (99)$$

where

$$A = \frac{2.317 \frac{h_{fg} DG}{4} F_1}{1 + 0.0143 F_2 D^{1/2} G} \quad (100)$$

^e For low-power and low-flow operation of Mode 2 and 3, however, there may be a significant separation of the incipient boiling point and burnout point.

$$B = \frac{DG}{4} \quad (101)$$

$$C = \frac{0.077F_3DG}{1+0.34F_4\left(\frac{G}{1356}\right)^n} \quad (102)$$

$$n = 2.0 - 0.5p_R \quad (103)$$

$$p_R = 0.145p \quad (104)$$

Here, the critical heat flux is in W/m², and p is in MPa. All other quantities are in standard SI units. For $p_R < 1$ MPa the coefficients F_1 to F_4 are given by:

$$F_1 = \frac{p_R^{18.942} e^{20.89(1-p_R)} + 0.917}{1} .917 \quad (105)$$

$$F_2 = \frac{1.309 F_1}{p_R^{1.316} e^{2.444(1-p_R)} + 0.309} \quad (106)$$

$$F_3 = \frac{p_R^{17.023} e^{16.658(1-p_R)} + 0.667}{1.667} \quad (107)$$

$$F_4 = F_3 p_R^{1.649} \quad (108)$$

For $p_R > 1$ MPa, the coefficients are given by:

$$F_1 = p_R^{-0.368} e^{0.648(1-p_R)} \quad (109)$$

$$F_2 = \frac{F_1}{p_R^{-0.448} e^{0.245(1-p_R)}} \quad (110)$$

$$F_3 = p_R^{0.219} \quad (111)$$

$$F_4 = F_3 p_R^{1.649} \quad (112)$$

3.2.4.2. Post CHF Heat Transfer: Film Boiling

During a typical HFIR transient, the flow regime of the post-DNB region allows the existence of a vapor film which separates the wall from the bulk of the liquid. This film boiling regime has been modeled analytically by allowing the vapor film to exist continuously on the fuel plate surface. Generally, post-CHF heat transfer correlations assume a coolant at the saturation temperature. Since during HFIR transients, CHF is exceeded in the subcooled boiling region, a mechanistic approach which recognizes the departure from thermodynamic equilibrium is needed.

In this work, the model developed by Tong and Young⁶² and used in RELAP5-Mod2⁸ is adapted to simulate film boiling heat transfer conditions. This model allows separate heat transfer modes from the fuel plate surface to liquid and vapor phases:

$$\dot{q}_{sl} = \frac{2}{a}(1-\alpha)h_l(T_s - T_l) \quad (113)$$

$$\dot{q}_{sg} = \frac{2}{a}\alpha h_g(T_s - T_g) \quad (114)$$

The Bromley-Pomeranz correlation is used for film boiling heat transfer to the liquid phase.⁶³

$$h_l = 0.620 \left(\frac{D_e}{\lambda_c} \right)^{0.172} \left[\frac{k_g^3 \rho_g (\rho_l - \rho_g) h_{fg} g}{D_e \mu_g (T_s - T_l)} \right]^{1/4} \quad (115)$$

where

$$\lambda_c = 2\pi \left[\frac{\sigma}{g(\rho_l - \rho_g)} \right]^{1/2} \quad (116)$$

Here, λ_c is the spacing between the bubbles leaving the film, which is the wavelength of a Taylor-type instability at the vapor-liquid interface.⁶⁴ The Dougall-Rohsenow correlation is used for film boiling heat transfer to the vapor phase:⁶⁵

$$h_g = 0.023 \frac{k_g}{D_e} \left[\frac{\rho_g [\alpha v_g + (1 - \alpha) v_l] D_e}{\mu_g} \right]^{0.8} \left(\frac{\mu_g c_{pg}}{k_g} \right)^{0.4} \quad (117)$$

In these correlations, the liquid and vapor phase thermal properties are those of pressurized water and superheated steam as discussed in Sections 3.2.3.1 and 3.2.3.5.

During HFIR transients, post-dryout heat transfer may be encountered only for low-power, low-flow conditions in which case the liquid exists only as droplets carried by the vapor, and the vapor phase receives the heat from the wall. This heat transfer regime is modeled by single-phase vapor flow and is discussed in Section 3.2.3.5.

As will be shown later in this work for certain HFIR transients, after CHF is exceeded, poor heat transfer conditions in the film boiling and single-phase vapor flow regions result in a rapid and significant increase in fuel temperature with fuel melting conditions occurring in less than a second. At these extremely high temperatures, radiation heat transfer also becomes significant. In this work, a separate post-CHF heat transfer mode is provided as an option for radiative heat transfer to the vapor phase as follows

$$(\dot{q}_{vg})_{rad} = e_g (T_s^4 - T_g^4) \quad (118)$$

where ϵ_g is the Stefan-Boltzmann constant. This heat transfer component is added to the vapor phase volumetric heat generation rate, $\dot{q}_{vg}$.

3.2.5. Numerically Convenient Set of Differential Equations

By summing up phasic equations, namely equations (13)+(14) and (15)+(16), we have

$$\frac{\partial}{\partial t}[(1-\alpha)\rho_l + \alpha\rho_g] + \frac{\partial}{\partial z}[(1-\alpha)\rho_l v_l + \alpha\rho_g v_g] = 0 \quad (119)$$

$$\frac{\partial}{\partial t}[(1-\alpha)\rho_l H_l + \alpha\rho_g H_g] + \frac{\partial}{\partial z}[(1-\alpha)\rho_l H_l v_l + \alpha\rho_g H_g v_g] = \quad (120)$$

$$\frac{Dp}{Dt} + [(1-\alpha)v_l + \alpha v_g]F_w + \dot{q}_v + \dot{q}_s$$

Defining the following quantities for the two-phase mixture

$$\rho_m = (1-\alpha)\rho_l + \alpha\rho_g \quad (121)$$

$$G_m = (1-\alpha)\rho_l v_l + \alpha\rho_g v_g \quad (122)$$

$$\rho_m^* = \frac{G_m^2}{(1-\alpha)\rho_l v_l^2 + \alpha\rho_g v_g^2} \quad (123)$$

$$\left(\frac{G_m}{\rho_m}\right)^* = (1-\alpha)v_l + \alpha v_g \quad (124)$$

$$h_m = \frac{(1-\alpha)\rho_l H_l + \alpha\rho_g H_g}{\rho_m} \quad (125)$$

$$h_m^* = \frac{(1-\alpha)\rho_l H_l v_l + \alpha \rho_g H_g v_g}{G_m} \quad (126)$$

and substituting these variables into mixture differential equations, we obtain the following equation set:

$$\frac{\partial \rho_m}{\partial t} + \frac{\partial G_m}{\partial z} = 0 \quad (127)$$

$$\frac{\partial}{\partial t}(h_m \rho_m) + \frac{\partial}{\partial z}(h_m^* G_m) = \frac{Dp}{Dt} + \left(\frac{G_m}{\rho_m}\right)^* F_w + \dot{q}_v + \dot{q}_s \quad (128)$$

$$\frac{\partial G_m}{\partial t} + \frac{\partial}{\partial z}\left(\frac{G_m^2}{\rho_m^*}\right) = -\frac{\partial p}{\partial z} - F_w + \rho_m g \quad (129)$$

For the energy equation, expanding the terms with partial derivatives

$$\rho_m \frac{\partial h_m}{\partial t} + h_m \frac{\partial \rho_m}{\partial t} + h_m^* \frac{\partial G_m}{\partial z} + G_m \frac{\partial h_m^*}{\partial z} = \frac{Dp}{Dt} + \left(\frac{G_m}{\rho_m}\right)^* F_w + \dot{q}_v + \dot{q}_s \quad (130)$$

and using the mixture continuity condition, we get

$$\rho_m \frac{\partial h_m}{\partial t} - (h_m - h_m^*) \frac{\partial G_m}{\partial z} + G_m \frac{\partial h_m^*}{\partial z} = \frac{Dp}{Dt} + \left(\frac{G_m}{\rho_m}\right)^* F_w + \dot{q}_v + \dot{q}_s \quad (131)$$

Note that for single-phase flow and two-phase flow with slip ratio of unity (uniform phasic velocities), the second term on the left hand side vanishes.

In the continuity equation, expanding the time derivative using the chain rule gives

$$\frac{\partial \rho_m}{\partial t} = \frac{\partial \rho_m}{\partial h_m} \frac{\partial h_m}{\partial t} + \frac{\partial \rho_m}{\partial p} \frac{\partial p}{\partial t} \quad (132)$$

and defining the following variables

$$R_h = \frac{\partial \rho_m}{\partial h_m} \quad (133)$$

$$R_p = \frac{\partial \rho_m}{\partial p} \quad (134)$$

we obtain the following for mixture continuity:

$$R_h \frac{\partial h_m}{\partial t} + R_p \frac{\partial p}{\partial t} + \frac{\partial G_m}{\partial z} = 0 \quad (135)$$

Combining this with the mixture energy equation and arranging, we get two new equations: One with a time derivative of pressure only, and one with a time derivative of mixture enthalpy

$$\frac{\partial p}{\partial t} = \frac{c^2}{\rho_m} \left[-\rho_m \frac{\partial G_m}{\partial z} - R_h (C_{mech} + C_{therm}) \right] \quad (136)$$

$$\frac{\partial h_m}{\partial t} = \frac{c^2}{\rho_m} \left[-\frac{\partial G_m}{\partial z} + R_p (C_{mech} + C_{therm}) \right] \quad (137)$$

where

$$C_{mech} = \left(\frac{G_m}{\rho_m} \right)^* \left(\frac{\partial p}{\partial z} + F_w \right) \quad (138)$$

$$C_{therm} = (h_m - h_m^*) \frac{\partial G_m}{\partial z} - G_m \frac{\partial h_m^*}{\partial z} + \dot{q}_v + \dot{q}_s \quad (139)$$

$$c^2 = \frac{\rho_m}{\rho_m R_p + R_h} \quad (140)$$

When British units are used for the variables in these expressions, the final equation set with dimensional stability can be recast as follows:

$$\frac{\partial G_m}{\partial t} = - \frac{\partial}{\partial z} \left(\frac{G_m^2}{\rho_m^*} \right) - g_c \frac{\partial p}{\partial z} - F_w + \rho_m g \quad (141)$$

$$\frac{\partial p}{\partial t} = \frac{c^2}{\rho_m g_c} \left[-\rho_m \frac{\partial G_m}{\partial z} - R_h (C_{mech} + 778.16 C_{therm}) \right] \quad (142)$$

$$\frac{\partial h_m}{\partial t} = \frac{c^2}{\rho_m g_c} \left[-1.285 \cdot 10^{-3} \frac{\partial G_m}{\partial z} + R_p (1.285 \cdot 10^{-3} C_{mech} + C_{therm}) \right] \quad (143)$$

with

$$C_{mech} = \left(\frac{G_m}{\rho_m} \right)^* \left(\frac{\partial p}{\partial z} + \frac{F_w}{g_c} \right) \quad (144)$$

$$C_{therm} = (h_m - h_m^*) \frac{\partial G_m}{\partial z} - G_m \frac{\partial h_m^*}{\partial z} + \dot{q}_v + \dot{q}_s \quad (145)$$

$$c^2 = \frac{\rho_m g_c}{\rho_m R_p + R_h} \quad (146)$$

where g_c is the conversion factor. This is the equation set that is actually solved in the analysis. Note that for single-phase liquid flow and single-phase vapor flow, setting $\alpha=0$ and $\alpha=1$ respectively, equation set (141) to (143) automatically yields the field equations describing the corresponding flow regime. For the subcooled boiling flow regime, a modified version of the liquid energy equation is used as described in Section 3.2.3.3. During subcooled boiling flow, the surface heat transfer term is calculated considering simultaneous steam formation and condensation with appropriate heat transfer correlations for each. In other words, the accuracy of the subcooled boiling formulation of this model is limited by the accuracy of equation (58) and supporting correlations given by equations (50) and (70). In the bulk boiling region, thermal equilibrium between the phases allows the use of the mixture equations given above. This formulation allows the user to choose simpler hydrodynamic models such as homogeneous flow and/or thermal equilibrium without any additional modelling modifications. These options may also be used in combination.

3.2.6. Boundary and Initial Conditions

Two types of boundary conditions are provided in the model for analysis of coolant flow in parallel rectangular HFIR channels: In the first option, the inlet flow rate and the outlet plenum pressures are specified, and in the second option, the inlet and outlet plenum pressures are specified (two-pressure boundary condition). In addition, the inlet enthalpy and the axial linear heat generation rate need to be provided as initial conditions. Form losses at the channel inlet and outlet and any sudden area changes are discussed in Section 3.2.6.2.

3.2.6.1. Boundary Conditions

In principle, a single boundary condition should be applied at each end of a flow channel for a well-posed two-phase flow problem since conditions at each end affect the flow characteristics within the channel.⁵ In both options provided in this analysis, the lower plenum pressure is specified as the boundary condition for the outlet end of the coolant channels. For the inlet end of the channels, either the coolant flow (inlet velocity) or the upper plenum pressure is specified.

Each option has its restrictions and advantages. Specifying the inlet flow rate is provided as an option to analyze nominal single-channel flow as a modeling aid. Using this option for flow in multiple parallel channels which are connected at the inlet and outlet plena is not a realistic boundary condition. In multi-channel flow, the coolant flow depends on the pressure difference between the inlet and outlet plena, form losses at the inlet and exit, and friction losses inside the channel. The usual boundary conditions for these situations is that the pressure drop in each coolant channel should be equal. This, however, requires additional iterations to adjust the flow distributions in each channel.^f

Although this type of boundary condition is a common and reasonable assumption for most thermal transients, for rapid reactivity accidents where a power burst occurs, the core inlet and outlet pressures will depend on the power conditions and will not be known in advance. However, the results of this analysis indicate that even during the most severe reactivity transient, the sudden increase in power creates only mild pressure spikes for which the two-pressure boundary condition seems adequate. In addition to pressure and flow conditions, inlet enthalpy and axial heat generation rate should also be provided. In all transients (except the cold water accidents), the inlet enthalpy is assumed to be constant since the total transient time is always much shorter than the primary coolant cycle time. In general, boundary conditions can be specified as a function of time.

3.2.6.2. Pressure Drop at Sudden Area Changes

As discussed in the previous section, the coolant flow depends on the form losses at the channel inlet and exit, and friction losses inside the channel. In addition, sudden changes in flow area inside the coolant channels, such as hypothetical flow blockages after fuel melting is initiated, should be properly modeled. In the analysis, it is necessary to evaluate integrated momentum and kinetic energy before and after the area changes. These, however, cannot be evaluated with complete accuracy since the detailed velocity distributions and the extent

^f In calculations, the additional iterations required to determine flow distributions between coolant channels for two-pressure boundary conditions are performed only at every basic time step at which state variables are reported. This basic time step is typically larger than the one which the time advancement scheme chooses.

of equilibrium between the phases are not well known. In this work, the approach discussed in Reference 34 is used, which is based on an adiabatic flow and constant pressure across the area change.

Coolant flow at the channel inlet is shown in Figure 10 as a typical case for sudden contraction. As shown in the figure, sudden contractions result in *vena contracta* of area smaller than the available flow area after the contraction (*Section 0* in Figure 10). However, the losses from the contraction to the vena contracta are negligible and the pressure losses can be approximated by the following expression for two-phase flow:⁶⁶

$$\Delta p = 1.2 \left[1 - \left(\frac{A_1}{A_2} \right)^2 \right] \frac{\rho_f v_f^2}{2} (1 - \alpha) \quad (147)$$

Here, subscripts 1 and 2 refer to the values of variables at the corresponding sections in Figure 10. Since for all the reactivity transients of interest, coolant enters the channels at a subcooled state, the pressure losses at the channel inlet are calculated using the following relation for a single-phase:

$$\Delta p = 1.4 \left[1 - \left(\frac{A_1}{A_2} \right)^2 \right] \frac{\rho_f v_f^2}{2} \quad (148)$$

Coolant flow at the channel exit is shown in Figure 11 for a typical case of sudden expansion. In this case, the pressure rise for two-phase flow is formulated as follows:⁶⁶

$$\Delta p = G_{mf} A_1 \left[\frac{(1-x)^2}{\rho_f} \left(\frac{1}{(1-\alpha_1)A_1 A_2} - \frac{1}{(1-\alpha_2)A_2^2} \right) + \frac{x^2}{\rho_g} \left(\frac{1}{\alpha_1 A_1 A_2} - \frac{1}{\alpha_2 A_2^2} \right) \right] \quad (149)$$

where the relationship between void fractions α_1 and α_2 is established by the following:

$$\frac{1}{\alpha_2} = \frac{p_2}{p_1} \frac{1-\alpha_1}{\alpha_1} \left(\frac{A_2}{A_1} \right)^{0.2} + 1 \quad (150)$$

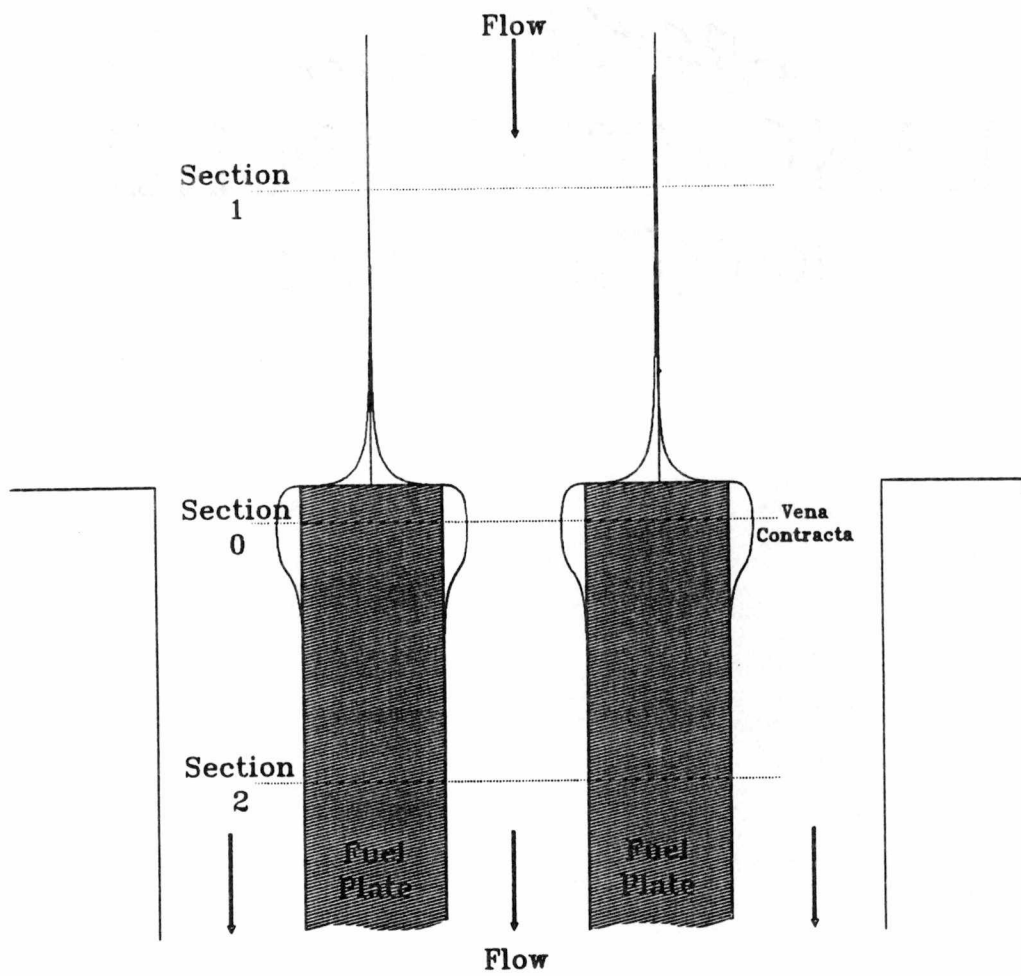


Figure 10 : Flow at the channel inlet: Sudden contraction.

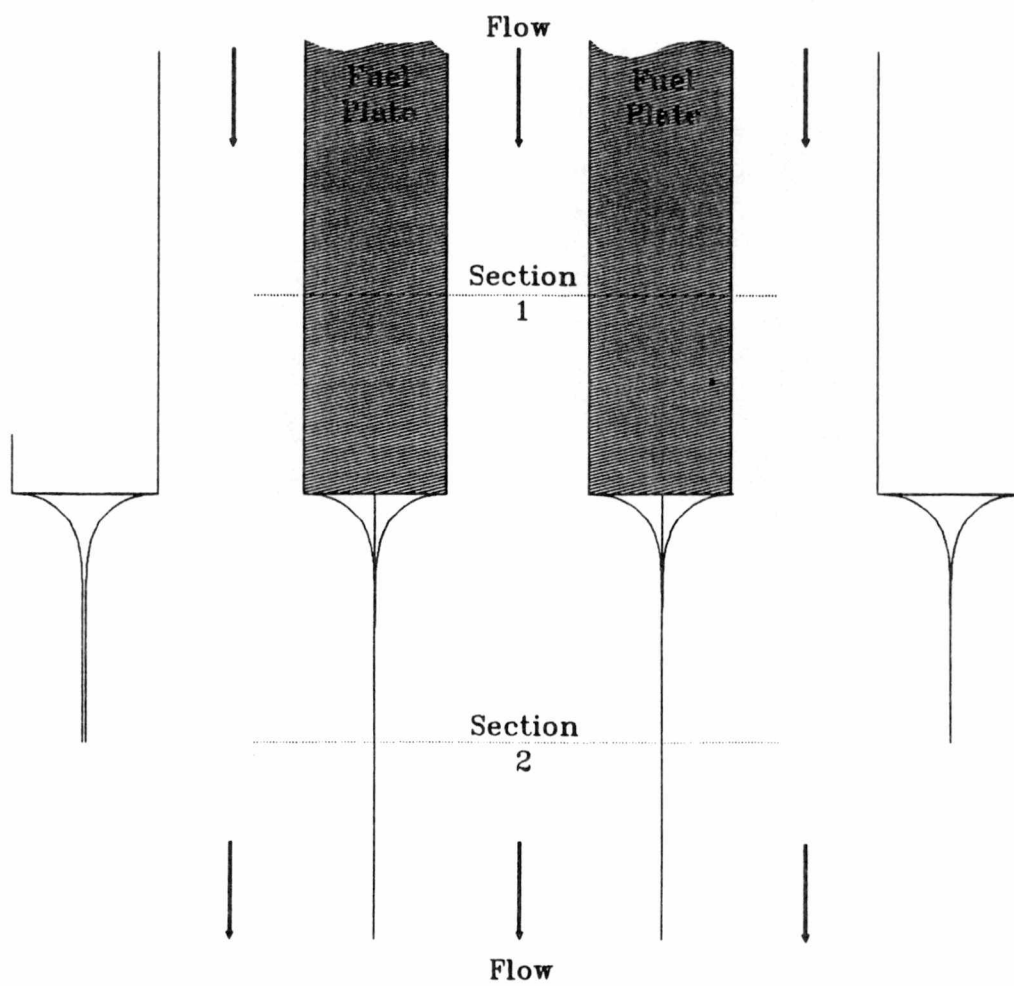


Figure 11 : Flow at the channel exit: Sudden expansion.

For transients with single-phase flow at the channel exit, the pressure drop is calculated from the simpler Bernoulli equation as follows:

$$\Delta p = \frac{\rho (v_{f1}^2 - v_{f2}^2)}{2} - \frac{\rho (v_{f1} - v_{f2})^2}{2} \quad (151)$$

3.2.6.3. Initial Conditions

Usually the transients are expected to be initiated from a steady-state condition. In general, the initial condition set is specified as an input to the model. Considering the substantial number of state variables to be specified, however, an option is provided in which the transient solution algorithm is used to guide the system to a steady-state. In this option, the program is run from a properly posed arbitrary initial condition set without any feedback effects until steady-state is reached for a specified initial power level, power density distribution, inlet enthalpy, and boundary condition set. At the end of this steady-state run, the values of state variables are stored in a restart file. The new initial condition set is then introduced to the program using this restart file for subsequent non steady-state runs.

The basic concept of steady-state is that the state of the system does not change with respect to time without a perturbation. In calculations, this is assured by monitoring the time derivatives of key state variables until they approach zero. Since, however, an arbitrary initial value set must be provided to guide the transient solution to a steady-state, it may not be possible to achieve an absolute steady-state. Therefore, the initial arbitrary data are expected to be close to the corresponding steady-state values. And, of course, the numerical schemes used to solve the differential equations are not exact; therefore, it is only possible to calculate absolute steady-state within a small limit of precision.

3.3. HEAT CONDUCTION MODEL

The heat conduction equation is derived by considering a heat balance in a volume bounded by a smooth surface during a small time interval. The time rate of change of the

energy within the volume is equal to the difference in the amount of energy which is produced within the volume and the amount of heat that is flowing through the bounding surface. The resulting equation is

$$\rho c_p \frac{\partial T}{\partial t} = \nabla \cdot (k \nabla T) + Q \quad (152)$$

where c_p = specific heat of the material,
 ρ = material density,
 k = thermal conductivity,
 Q = heat source density.

In general, the quantities c_p , ρ , and k are functions of temperature T as well as space variables.

The heat conduction model used in this work is based on a one-dimensional flat fuel plate geometry. When the involute shape of the fuel plates is ignored and a rectangular geometry is employed, the heat conduction equation becomes

$$\rho c_p \frac{\partial T(x,t)}{\partial t} = \frac{\partial}{\partial x} \left[k(x,t) \frac{\partial T(x,t)}{\partial x} \right] + Q(x,t) \quad (153)$$

where x indicates the azimuthal direction in Figure 2.

The analytical solution of this equation for homogeneous materials and regular geometries is well known. The heat conduction model of this work uses such an analytical approach to obtain the average fuel temperature, average clad temperature, and fuel plate surface (oxide layer) temperatures. This analytical approach is similar to the heat conduction model implemented in BEAGL code.^{43,67} Since the reactivity feedback from fuel plates is calculated using average temperatures, and the detailed flux distribution within the fuel plates is not known, a differencing scheme is not essential for this analysis. The analytical treatment limits the number of equations to be advanced in time domain, and therefore allows implicit coupling of the heat conduction model with the rest of the equation set describing the HFIR core without a significant increase in computation time. Furthermore, in this method mesh

spacing effects typically encountered in finite difference schemes are not of concern because of this analytical treatment. On the other hand, the model has the following limitations:

i. Volumetric heat sources are assumed piecewise constant in both fuel and cladding regions. Since the detailed flux distribution within the fuel plates is not known, this approximation does not pose a serious limitation for the thermal analysis of the very thin fuel plates.

ii. The thermal properties of the fuel meat and cladding are considered constant in space. In other words, each subregion is considered homogeneous. Thermal properties, however, are a function of average temperature in each subregion and are re-evaluated every time-step.

iii. Heat conduction in the axial direction is assumed negligible. This assumption is the most significant one and has important consequences insofar as fuel damage propagation is concerned. This issue is discussed in Section 6.3.3 in greater detail.

The heat conduction model through the fuel plates is coupled to the fluid dynamics model via the heat flux at the fuel element outer surface. The heat flux formulation is discussed in Sections 3.3.2 and 3.2.3, and enters into the heat conduction model as a boundary condition.

A section of a typical fuel plate is sketched in Figure 12. In the following section, the transient heat conduction equation is derived for the clad and fuel regions. The boundary and interface conditions are discussed in subsequent sections. At the end of this chapter, an alternative single-region heat conduction model is presented in which separate fuel and cladding regions are not considered. This simplified scheme further limits the number of differential equations to one for each node. The dynamic behavior of the core for nominal conditions are calculated based on the three region heat conduction model. In the hot-assembly modeling of the core, however, the single region heat conduction model is preferred for most cases since the computer time becomes prohibitive when the three region model is used.

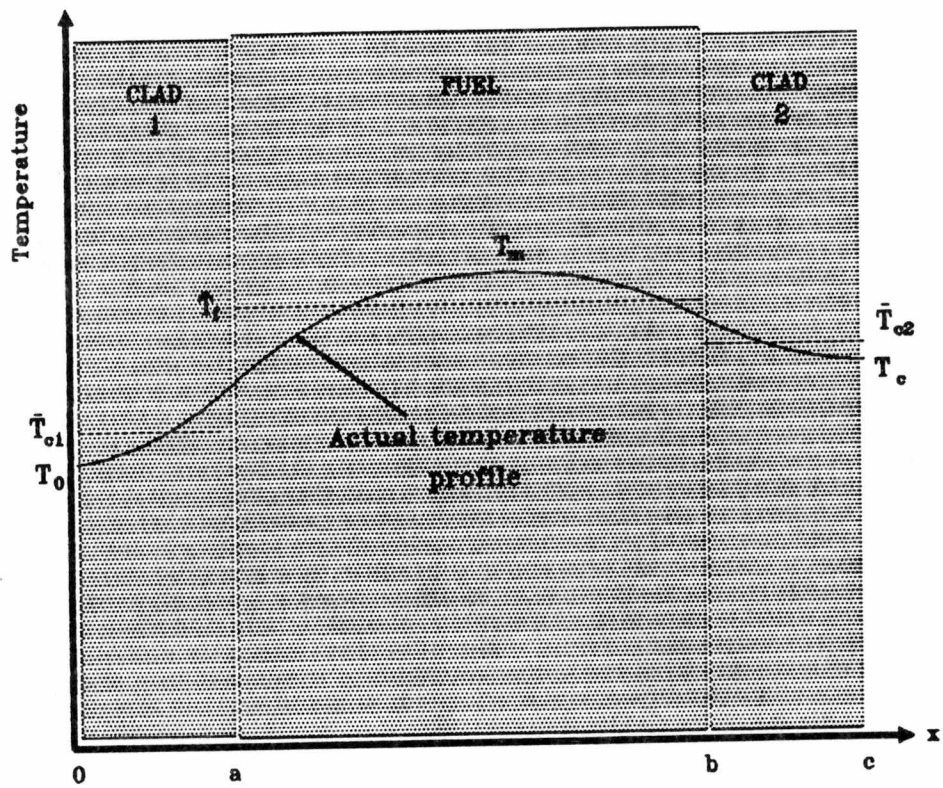


Figure 12 : Typical fuel plate temperature distribution.

3.3.1. Average Temperatures

The transient heat conduction equation is given by Equation (153) at any point of interest within fuel element. The corresponding equations in each subregion of the fuel plate shown in Figure 12, namely clad region 1 (from $x=0$ to $x=a$), fueled region (from $x=a$ to $x=b$), and the clad region 2 (from $x=b$ to $x=c$) are, respectively

$$\rho_c c_{pc} \frac{\partial T_{c1}(x,t)}{\partial t} = \frac{\partial}{\partial x} \left[k_c \frac{\partial T_{c1}(x,t)}{\partial x} \right] + Q_c \quad (154)$$

$$\rho_f c_{pf} \frac{\partial T_f(x,t)}{\partial t} = \frac{\partial}{\partial x} \left[k_f \frac{\partial T_f(x,t)}{\partial x} \right] + Q_f \quad (155)$$

$$\rho_c c_{pc} \frac{\partial T_{c2}(x,t)}{\partial t} = \frac{\partial}{\partial x} \left[k_c \frac{\partial T_{c2}(x,t)}{\partial x} \right] + Q_c \quad (156)$$

where subscripts c and f indicate clad and fuel materials, respectively. Temperatures T_{c1} and T_{c2} refer to the first (left) and second (right) cladding regions.

To derive the time-dependent heat conduction equation for the average clad temperature in region 1, we integrate equation (154) from $x=0$ to $x=a$ and divide the resulting equation by a :

$$\begin{aligned} \frac{1}{a} \int_0^a \left[\rho_c c_{pc} \frac{\partial T_{c1}(x,t)}{\partial t} \right] dx = \\ \frac{1}{a} \int_0^a \left[\frac{\partial}{\partial x} \left[k_c \frac{\partial T_{c1}(x,t)}{\partial x} \right] \right] dx + \frac{1}{a} \int_0^a Q_c dx \end{aligned} \quad (157)$$

Assuming that the cladding density and heat capacity are constant in space within that region,

and defining the average clad temperature as follows,

$$\bar{T}_{cl}(t) = \frac{1}{a} \int_0^a T_{cl}(x,t) dx \quad (158)$$

equation (157) reduces to:

$$\rho_c c_{pc} \frac{d\bar{T}_{cl}(t)}{dt} = \frac{1}{a} \left[(k_c \frac{\partial T_{cl}}{\partial x} |_a - (k_c \frac{\partial T_{cl}}{\partial x} |_0) \right] + \langle Q_c \rangle \quad (159)$$

Similarly, to derive the time-dependent heat conduction equation for the average fuel temperature, we integrate equation (155) from $x=a$ to $x=b$ and divide the resulting equation by $(b-a)$:

$$\begin{aligned} \frac{1}{b-a} \int_a^b \left[\rho_f c_{pf} \frac{\partial T_f(x,t)}{\partial t} \right] dx = \\ \frac{1}{b-a} \int_a^b \left[\frac{\partial}{\partial x} \left[k_f \frac{\partial T_f(x,t)}{\partial x} \right] \right] dx + \frac{1}{b-a} \int_a^b Q_f dx \end{aligned} \quad (160)$$

Again, assuming that the fuel region density and heat capacity are constant in space within that region, and defining the average fuel temperature as follows

$$\bar{T}_f(t) = \frac{1}{b-a} \int_a^b T_f(x,t) dx \quad (161)$$

equation (160) becomes

$$\rho_f c_{pf} \frac{d\bar{T}_f(t)}{dt} = \frac{1}{b-a} \left[(k_f \frac{\partial T_f}{\partial x} |_b - (k_f \frac{\partial T_f}{\partial x} |_a) \right] + \langle Q_f \rangle \quad (162)$$

Finally, to derive the equation for the average clad temperature in region 2, we first integrate equation (156) from $x=b$ to $x=c$ and divide the resulting equation by $(c-b)$. Defining the average clad temperature as follows

$$\bar{T}_{c2}(t) = \frac{1}{c-b} \int_b^c T_{c2}(x,t) dx \quad (163)$$

and assuming that clad density and heat capacity are constant in space within that region, we obtain

$$\rho_c c_{pc} \frac{d\bar{T}_{c2}(t)}{dt} = \frac{1}{c-b} \left[\left(k_c \frac{\partial T_{c2}}{\partial x} \right) \Big|_c - \left(k_c \frac{\partial T_{c2}}{\partial x} \right) \Big|_b \right] + \langle Q_c \rangle \quad (164)$$

3.3.2. Boundary and Interface Conditions

Continuity of heat flux at $x=a$ and $x=b$ imposes the following interface conditions:

$$-k_f \frac{\partial T_f(x,t)}{\partial x} \Big|_a = -k_c \frac{\partial T_{c1}(x,t)}{\partial x} \Big|_a \quad (165)$$

$$-k_f \frac{\partial T_f(x,t)}{\partial x} \Big|_b = -k_c \frac{\partial T_{c2}(x,t)}{\partial x} \Big|_b \quad (166)$$

And the boundary conditions at the fuel element outer surface are given by the following relations:

$$-k_c \frac{\partial T_{c1}(x,y)}{\partial x} \Big|_0 = q_{s0}'' \quad (167)$$

$$-k_c \frac{\partial T_{c2}(x,y)}{\partial x} \Big|_c = -q_{sc}'' \quad (168)$$

where q_{s0}'' and q_{sc}'' are the surface heat flux terms at the left and right boundaries,

respectively.

In general, the surface heat fluxes are calculated from the clad surface temperatures, T_0 and T_c , and bulk coolant temperatures, T_{11} and T_{12} , using various heat transfer correlations which depend on the flow regimes as defined in Section 3.2.3.

For single-phase liquid flow where T_0 and T_c are less than the saturation temperature at the corresponding local instantaneous pressure, the surface heat flux for the left and right boundaries are respectively given by

$$q''_{so} = h_{NB}(T_0 - T_{11}) \quad (169)$$

$$q''_{sc} = h_{NB}(T_c - T_{12}) \quad (170)$$

where T_{11} is the left boundary bulk liquid temperature, and T_{12} is the right boundary bulk liquid temperature. The single-phase heat transfer coefficient, h_{NB} , is described by equations (34) or (35).^{39,40}

For single-phase liquid flow where surface temperature exceeds saturation temperature, the surface heat fluxes are computed from

$$q''_{so} = h_{NB}(T_0 - T_{11}) + h_B(T_0 - T_{sat}) \quad (171)$$

$$q''_{sc} = h_{NB}(T_c - T_{12}) + h_B(T_c - T_{sat}) \quad (172)$$

where h_B is the Thom heat transfer coefficient and given by equation (50).⁴⁴

For the two-phase subcooled boiling regime where the coolant film temperature is at saturation while the bulk coolant temperature is still below the saturation temperature, equations (171) and (172) above still apply. However, in this regime, the surface heat flux is taken as the sum of the separate heat transfer terms to the liquid and vapor phases, i.e., q''_{sl} and q''_{sg} . These contributions for any surface are defined by the following relationships

$$q''_{sl} = h_{NB}(T_l^* - T_l) \quad (173)$$

$$q''_{sg} = h_B(T_s - T_{sat}) + h_{NB}(T_s - T_l^*) \quad (174)$$

where T_l^* is the film temperature and given by

$$T_l^* = \frac{T_s + T_{sat}}{2} \quad (175)$$

Here T_s is the surface temperature (T_0 for left, T_c for right boundary), T_l is the liquid phase temperature (T_{l1} for left, T_{l2} for right boundary), and T_{sat} is the saturation temperature of the coolant. The assumption here is that the portion of the convective heat flux $h_{NB}(T_s - T_l)$ entering the film above the film temperature produces voids while the rest produces convective heating of the liquid.⁴³

For the two-phase saturated boiling regime, the surface heat transfer terms for the left and right boundaries are, respectively

$$q''_{so} = (h_{NB} + h_B)(T_0 - T_{sat}) \quad (176)$$

$$q''_{sc} = (h_{NB} + h_B)(T_c - T_{sat}) \quad (177)$$

And finally, for the single-phase vapor flow regime, the surface heat fluxes are computed from the following:

$$q''_{so} = h_{DB}(T_0 - T_{sat}) \quad (178)$$

$$q''_{sc} = h_{DB}(T_c - T_{sat}) \quad (179)$$

where h_{DB} is Dittus-Boelter heat transfer coefficient for superheated steam as described by equations (88) to (98).³⁹

3.3.3. Surface and Interface Temperatures

The evaluation of both the surface and interface heat fluxes in equations (159), (162), and (164) involve surface temperatures, T_0 and T_c , and interface temperatures, T_a and T_b . In general, these temperatures can be expressed in terms of the average clad and fuel temperatures, and the bulk coolant temperature

$$T = c_0(t) + c_1(t)\bar{T}_f(t) + c_2(t)\bar{T}_{cl/c2} + c_3(t)T_f(t) \quad (180)$$

This definition of surface and interface temperatures is formally exact as long as the weight factors c_0 to c_3 are calculated properly as a function of time. However, to calculate the time-dependent coefficients rigorously is impractical. Instead, it is assumed that a steady-state approximation for these coefficients is applicable for the very thin and high thermal conductive fuel plates of the HFIR.

For steady-state, equations (154), (155) and (156) for clad region 1, fueled region, and clad region 2 reduce to

$$\frac{d}{dx} \left[k_c \frac{d}{dx} T_{cl}(x) \right] = Q_c \quad (0 < x < a) \quad (181)$$

$$\frac{d}{dx} \left[k_f \frac{d}{dx} T_f(x) \right] = Q_f \quad (a < x < b) \quad (182)$$

$$\frac{d}{dx} \left[k_c \frac{d}{dx} T_{c2}(x) \right] = Q_c \quad (b < x < c) \quad (183)$$

And the corresponding solutions are

$$T_{cl} = \frac{Q_c}{2k_c} x(a-x) + \frac{T_a - T_0}{a} x + T_0 \quad (184)$$

$$T_f = \frac{Q_f}{2k_f}[(b^2-x^2)-(b+a)(b-x)] - \frac{T_b-T_a}{b-a}(b-x) + T_b \quad (185)$$

$$T_{c2} = \frac{Q_c}{2k_c}[(c^2-x^2)-(c+b)(c-x)] - \frac{T_c-T_b}{c-b}(c-x) + T_c \quad (186)$$

Similar to what is done in equations (157) and (160), if we integrate these solutions within their respective range and divide by the total volume of the subregion, we obtain the following steady-state averages

$$\bar{T}_{c1} = \frac{Q_c}{12k_c}a^2 + \frac{T_a+T_0}{2} \quad (187)$$

$$\bar{T}_f = \frac{Q_f}{12k_f}(b-a)^2 + \frac{T_b+T_a}{2} \quad (188)$$

$$\bar{T}_{c2} = \frac{Q_c}{12k_c}(c-b)^2 + \frac{T_c+T_b}{2} \quad (189)$$

The surface and interface temperatures are then obtained from the steady-state solutions (equations (184) to (186)), averages (equations (187) to (189)), and interface and boundary conditions (equations (65) to (68)) as follows:

$$T_0 = \bar{T}_{c1} + \frac{q''_0}{2k_c}a + \frac{Q_c}{6k_c}a^2 \quad (190)$$

$$T_a = \frac{\frac{Q_c a + Q_f (b-a)}{6} + \frac{k_c}{a} \bar{T}_{c1} + \frac{k_f}{b-a} \bar{T}_f}{\frac{k_c}{a} + \frac{k_f}{b-a}} \quad (191)$$

$$T_b = \frac{\frac{Q_f(b-a) + Q_c(c-b)}{6} + \frac{k_c}{c-b} \bar{T}_{c2} + \frac{k_f}{b-a} \bar{T}_f}{\frac{k_c}{c-b} + \frac{k_f}{b-a}} \quad (192)$$

$$T_c = \bar{T}_{c2} + \frac{q''_s}{2k_c}(c-b) + \frac{Q_c}{6k_c}(c-b)^2 \quad (193)$$

Note that, since the surface heat fluxes depend on the surface temperatures as discussed in Section 3.3.2, further algebraic manipulations are needed for T_0 and T_c expressions.

Finally, the interface heat flux terms appearing in the time-dependent average fuel and clad equations (equations (159), (162), and (164)) are treated in a similar manner. It is assumed that a steady-state approximation is applicable for these terms, and the gradients are resolved using the steady-state solutions (equations (184) to (186)) and the above expressions for T_0 , T_a , T_b , and T_c .

It should be noted that, in this methodology, the coefficients c_0 to c_3 in equation (180) and, therefore the surface and interface temperatures, are still functions of time since both thermal properties and heat generation rate are time dependent and they are updated at each time step.

A similar version of this approximation for fuel rods in cylindrical geometry is implemented in the BEAGL code and reported to yield satisfactory results even for fairly rapid control rod accidents of commercial power reactors.⁴³ Also, the validity of the approximations used in this model is verified by partial comparisons of the code results with results from RELAP5/Mod2 heat conduction model.⁸ These comparisons are presented in Section 6.1.1.

3.3.4. Single-Region Model

In addition to the three-region model as described above, a single-region heat conduction model is studied and implemented in the hot-assembly model as an option for the following reasons:

i. The previous model leads to three differential equations for each fuel node. Analysis of hot assembly behavior requires simultaneous solution for nine fuel plates and considering three equations for each fuel plate at each node results in prohibitively long computer times.

i. Due to the unavailability of the detailed flux distribution within the fuel plates, separate clad and fuel heat generation rate data were not obtainable.

ii. Since fuel plates are very thin and highly thermally conductive, the difference between the average fuel and clad temperatures is not significant.

iii. Similar thermal characteristics of the fuel and clad material makes a homogenized single-region analysis more practical. In addition, since reactivity feedback from the fuel plates is calculated based on a single-region model, a single-region heat conduction is appropriate.

iv. And finally, the single-region heat conduction model does not depend on a steady-state approximation for evaluation of the thermal gradients at the fuel-clad interface which may be unreasonable under extreme circumstances such as film boiling on one side of the fuel plate while the other side is subcooled.

In the single-region model, the fueled region is assumed to extend from $x=0$ to $x=c$ (Figure 12). In this case, the time-dependent heat conduction equation for the fuel region, equation (155), is still applicable. In order to derive the time-dependent heat conduction equation for the average fuel temperature, we integrate this equation from $x=0$ to $x=c$ and divide the resulting equation by c .

$$\frac{1}{c} \int_0^c \left[\rho f_f \frac{\partial}{\partial t} T_f(x,t) \right] dx = \quad (194)$$

$$\frac{1}{c} \int_0^c \left[\frac{\partial}{\partial x} \left[k_f \frac{\partial}{\partial x} T_f(x,t) \right] \right] dx + \frac{1}{c} \int_0^c Q_f dx$$

Again, assuming that fuel density and heat capacity are constant in space throughout the fuel plate, and defining the average fuel temperature as follows

$$\bar{T}_f(t) = \frac{1}{c} \int_0^c T_f(x,t) dx \quad (195)$$

equation (194) reduces to:

$$\rho f_f \frac{d\bar{T}_f(t)}{dt} = \frac{1}{c} \left[\left(k_f \frac{\partial T_f}{\partial x} \right) \Big|_c - \left(k_f \frac{\partial T_f}{\partial x} \right) \Big|_0 \right] + \langle Q_f \rangle \quad (196)$$

The boundary conditions for the single-region model are

$$-k_f \frac{\partial}{\partial x} T_f(x,t) \Big|_0 = q''_{s0} \quad (197)$$

$$-k_f \frac{\partial}{\partial x} T_f(x,t) \Big|_c = -q''_{sc} \quad (198)$$

where surface heat fluxes are discussed in Section 3.3.2. Substituting these expressions back into equation (196), we get the time dependent average fuel temperature equation as follows

$$\rho f_f \frac{d\bar{T}_f(t)}{dt} = -\frac{1}{c} (q''_{sc} + q''_{s0}) + \langle Q_f \rangle \quad (199)$$

Again, it is assumed that a steady-state approximation is applicable for the surface temperatures, T_0 and T_c which are needed to calculate surface heat fluxes. For steady-state, equation (155) reduces to

$$\frac{d}{dx} \left[k_f \frac{d}{dx} T_f(x) \right] = Q_f \quad (200)$$

and the corresponding solution becomes

$$T_f = \frac{Q_f}{2k_f} [x(c-x)] + \frac{T_c - T_0}{c} x + T_0 \quad (201)$$

Similar to what is done in equation (160), we integrate this solution for the whole fuel and divide it by c to obtain the following steady-state average

$$\bar{T}_f = \frac{Q_f}{12k_f} c^2 + \frac{T_c + T_0}{2} \quad (202)$$

The surface temperatures are then obtained from the steady-state solution (equation (201)), average temperature (equation (202)), and boundary conditions (equations (197) and (198)) as follows

$$T_0 = \bar{T}_f + \frac{q_{so}''}{2k_f} c + \frac{Q_f}{6k_f} c^2 \quad (203)$$

$$T_c = \bar{T}_f + \frac{q_{sc}''}{2k_f} c + \frac{Q_f}{6k_f} c^2 \quad (204)$$

Since the surface heat fluxes depend on surface temperatures as discussed in Section 3.3.2, further algebraic manipulations are needed on T_0 and T_c expressions. For example, for single-phase liquid flow, using equations (169) and (170) for surface heat fluxes, we obtain

$$T_o = \frac{\frac{Q_f}{3}a + \frac{2k_f}{a}\bar{T}_f - h_{NB}T_{11}}{\frac{2k_f}{a} - h_{NB}} \quad (205)$$

$$T_a = \frac{\frac{Q_f}{3}a + \frac{2k_f}{a}\bar{T}_f + h_{NB}T_{12}}{\frac{2k_f}{a} - h_{NB}} \quad (206)$$

3.3.5. Material Thermal Properties

In the model, the fuel and clad material thermal conductivity, density and heat capacity are considered as functions of temperature and updated at each time step. Unlike water and steam thermal properties, these fuel element thermal properties are calculated by interpolation between tabulated data in the code. Also, instead of using two separate tables and interpolation schemes for density and heat capacity, a single combined table is supplied for the *volumetric heat capacity*, ρc_p , for both fuel and cladding.

Cladding material (6061/1100 Aluminum) thermal conductivity and volumetric heat capacity are presented in Table 3 and Table 4, respectively.⁶⁸ The fueled region (mainly the mixture of aluminum and U_3O_8) thermal conductivity and volumetric heat capacity are presented in Tables 5 and 6, respectively.⁶⁸

Table 3 : Cladding thermal properties: Thermal conductivity.

Temperature (F)	Thermal Conductivity (Btu/s-ft-F)
82.4	0.0291
442.4	0.0321
802.4	0.0315
1204.	0.0276

Table 4 : Cladding thermal properties: Volumetric heat capacity.

Temperature (F)	Volumetric Heat Capacity (Btu/ft ³ -F)
80.	36.3
439.	39.9
979.	45.6
1160.	47.7

Table 5 : Fuel thermal properties: Thermal conductivity.

Temperature (F)	Thermal Conductivity (Btu/s-ft-F)
80.3	0.02844
260.3	0.02819
900.	0.02792
1204.	0.02773

Table 6 : Fuel thermal properties: Volumetric heat capacity.

Temperature (F)	Volumetric Heat Capacity (Btu/ft ³ -F)
80.	32.1
439.	35.6
979.	40.5
1204.	45.4

4. PHYSICAL MODELING OF THE REACTOR CORE

HFIR fuel element heat transfer characteristics have been studied extensively for steady-state operations by both Hilvety and Chapman,⁶⁹ and McLain.⁷⁰ These studies considered the nuclear, hydraulic, heat transfer, mechanical, and the metal corrosion factors in a consistent manner to provide an integral thermal-hydraulic model of the reactor at various steady-state operating conditions. Their results indicate that the steady-state operating power level could be raised to 139 MW at the startup, and to 153 MW at the end of a 100 MW fuel cycle without any damage to the fuel elements. Their models for the nominal coolant channel, nominal fuel plates, hot-channel, and hot-plate are used as the basis for this analysis.

First, the nominal operating conditions are modelled for an average coolant channel and average fuel plate for both the inner and outer elements. These nominal conditions are then modified, considering such factors as oxide film buildup on the fuel plates, fuel plate thermal expansion and radiation swelling, coolant flow distribution within the fuel elements, fuel segregation, and fuel-cladding non-bonds. The uncertainties in plant operating conditions, heat transfer and burnout characteristics, and power density distributions throughout the fuel cycle are also considered. The simultaneous use of all of these factors describes the hot-channel and hot-plate conditions.

In this work, the dynamic behavior of the reactor is conservatively assumed to be dependent only on the nominal conditions. The local deviations from the average conditions in the hot-plate and hot-channels are neglected insofar as the nuclear feedback is concerned. The feedback from regions other than fuel elements is included by modeling these regions using a lumped-parameter approach with a one node representation for both metal and coolant. Reactivity change due to scram is approximated by a 2nd order differential equation.

4.1. NOMINAL CHANNEL AND FUEL PLATE MODEL

The mathematical models presented in the Chapter 3 are first used to describe the nominal conditions defined by a core operating with a nominal power density, at an average

heat flux, with manufacturing tolerances of zero, and with no uncertainties in the operational characteristics. The nominal core operating parameters are presented in Table 7. The coolant inlet temperature is assumed to be constant since the total transient time is always much shorter than the primary coolant cycle time.

The *nominal channel* is assumed to have a uniform thickness at the average value specified for each fuel channel prior to the reactor operation. The walls of the nominal coolant channel are assumed to be *nominal fuel plates* having uniform thickness at the average value, with nominal fuel loading. The geometric characteristics of the nominal channel and nominal plate are summarized in Tables 8 and 9 for inner and outer fuel elements, respectively. The nominal conditions are used primarily to determine the feedback between neutronics and thermal-hydraulics.

The thicknesses of the coolant channels and fuel plates during reactor operation is determined considering such effects as fuel plate thermal expansion during the transients, radiation swelling, and increases in the fuel plate thickness due to oxidation of the aluminum clad surface throughout the fuel cycle. The thickness of the nominal coolant channel, therefore, is given by

$$a = a_i - \delta_t - \delta_r - \delta_o \quad (207)$$

where a_i is the initial coolant channel thickness prior to the reactor operation, δ_t is the increase in the fuel plate thickness due to heating, δ_r is the increase in the fuel plate thickness due to radiation, and δ_o is the total increase in fuel plate thickness due oxide formation on the fuel plate surface (two times the oxide layer thickness at one surface). These effects are described in the following sections.

4.1.1. Fuel Plate Thermal Expansion

Aluminum expands upon an increase in temperature resulting in a slight decrease in coolant channel thicknesses. In addition to the obvious hydraulic impacts of this behavior, the reactivity feedback due to fuel plate thermal expansion has a significant effect in

Table 7 : HFIR nominal operating parameters.^{1,70}

Steady-state reactor power (MW)	85
Fraction of power produced in fuel elements	0.975
Fraction of power produced in the coolant	2.95×10^{-2}
Vessel inlet plenum pressure (psia)	468
Vessel outlet plenum pressure (psia)	360
Inlet flow rate (ft/s)	51
Core inlet temperature (°F)	120
Total heat transfer area (ft ²)	428.8
Maximum EOC oxide layer thickness (inch)	0.003
Fuel plate thermal expansion coefficient (in/in-°F)	13.5×10^{-6}
Maximum increase in fuel plate thickness due to radiation swelling at the EOC (%)	0.001

Table 8 : Nominal Channel geometry for the inner fuel elements.^{1,70}

Inner side plate outer diameter (inch)	5.443
Outer side plate inner diameter (inch)	10.074
Total flow area (ft ²)	0.3919
Single channel flow area (ft ²)	1.146×10^{-3}
Coolant channel and fuel plate thickness (inch)	0.050
Coolant channel and fuel plate width (inch)	3.3
Coolant channel and fuel plate length (inch)	24
Hydraulic diameter (ft)	8.21×10^{-3}
Heat transfer area (ft ²) (one side of plate only)	5.5×10^{-1}
Fuel plate active length (inch)	20
Clad thickness (inch)	0.010

Table 9 : Nominal Channel geometry for the outer fuel elements.^{1,70}

Inner side plate outer diameter (inch)	11.746
Outer side plate inner diameter (inch)	16.622
Total flow area (ft ²)	0.75443
Single channel flow area (ft ²)	1.022×10^{-3}
Coolant channel and fuel plate thickness (inch)	0.050
Coolant channel and fuel plate width (inch)	2.944
Coolant channel and fuel plate length (inch)	24
Hydraulic diameter (ft)	8.19×10^{-3}
Heat transfer area (ft ²) (one side of plate only)	4.9×10^{-2}
Fuel plate active length (inch)	20
Clad thickness (inch)	0.010

terminating the excursions during the HFIR transients.⁸

The average thermal expansion coefficient of the 6061 aluminum for the temperature range of interest is reported to be 13.5×10^{-6} in/in-°F.⁷¹ Since the contribution of both surfaces of the fuel plate decreases the coolant channel thickness, the relation describing fuel thermal expansion becomes

$$\delta_t = \alpha_{th} a (\bar{T}_f - T_{ref}) \quad (208)$$

where α_{th} is the aluminum thermal expansion coefficient, $\bar{T}_f$ is the average fuel temperature, and T_{ref} is initial steady-state reference temperature. The contribution due to the partially restrained thermal expansion of the plate along the width and length, called the Poisson effect, would further increase the fuel plate thickness by a factor of 1.3.⁷⁰ This factor is included in hot-plate modelling of the HFIR core. Also, any possible deformation of the nominal fuel plate under these thermal stresses is ignored.

4.1.2. Radiation Swelling

Radiation swelling of the fuel plates reduces the coolant flow area by increasing the plate thickness, and by causing a longitudinal deflection of the plates. Available experimental data regarding the growth of the fuel plates due to radiation indicate that, by the end of fuel cycle at the HFIR operating conditions, the volume of the fuel plate for the core average conditions will increase by about 0.5%.⁷⁰ Assuming that the volumetric increase is isotropic and homogeneous throughout the nominal plate, the increase in fuel plate thickness due to radiation swelling may be approximated as

$$\delta_r = \frac{a^2}{a+h+w} \frac{dV}{V} \quad (209)$$

where h is the fuel plate length, w is the plate width, and V is the fuel plate volume. For a 0.5% increase in the volume, this formula yields a 0.001% increase in fuel plate thickness.

⁸ This latter effect is included in moderator reactivity coefficient as discussed in Section 3.1.2.

Alternatively, if we assume that the change in the fuel plate volume is solely due to the change in fuel plate thickness, the increase in the plate thickness becomes 0.25 mils. In the analysis, the first value is used for nominal channel model, while the second more conservative value is used for the hot-assembly model at the end of fuel cycle conditions.

Similar to the thermal deflections, the longitudinal deflection of the plates due to swelling is expected to create very little buckling of the fuel plates which would likely be in a sinusoidal shape creating no net change in flow area. Hence, these deflections are not included in this analysis.

4.1.3. Oxide Film Formation

The corrosion of aluminum is of special concern for aluminum base dispersion fuels at the end of the fuel cycle. Aluminum does not corrode significantly in water at or below 210 °F. However, above 250 °F, the corrosion attack is rapid and penetrates the grain boundaries.⁷² While the water temperature during steady-state HFIR operation is generally low, the surface temperature of the fuel plates under the high heat fluxes is higher than 210 °F and may increase even more as the oxide layer grows or as crud deposits on the fuel. This oxide layer decreases the coolant channel thicknesses and increases the fuel plate temperatures because of its low thermal conductivity. The additional temperature rise, in turn, drives the formation of more oxide.

Several tests have been performed to determine the rates and effects of corrosion.⁷³⁻⁷⁷ The oxide layer is made up with the monohydrate of aluminum oxide, called boehmite, and it has a thermal conductivity of 1.3 Btu/hr-ft²-F/ft. The density of the material is reported to be 188.53 lb_m/ft³.⁷⁸ For short periods of time (up to 20 days) under steady-state conditions, oxide formation occurs at a linear rate. After the thickness of the oxide layer becomes 2 or 3 mils, the rate of oxide formation on the aluminum decreases and some oxide spalls off from the surface. Therefore, it is conservatively assumed that after the oxide layer reaches 3 mils, no more oxide is formed on the fuel plate. The thermal cycling seems to enhance this spalling process of the oxide in some of the tests, but this phenomenon appears to be

somewhat inconsistent and no allowance is made for it in this work.^h Oxide buildup is accounted for only at the end of fuel cycle conditions since no oxide buildup is expected for fresh fuel.

4.1.4. Power Density Distributions

The power density distributions in the HFIR fuel elements were calculated by Cheverton and Sims for a fuel assembly having a life of 2500 MW-days.⁷⁹ The power distributions for the inner fuel element at the beginning and at the end of fuel cycle conditions are presented in Tables 10 and 11, respectively. The distributions for the outer fuel element at the beginning and end of fuel cycle conditions are presented in Tables 12 and 13, respectively. The end of fuel cycle distributions are calculated based on 25 days reactor operation at 100 MW.

These data are first used to determine area weighted averages in the radial direction. Then, the remaining data which give one-dimensional power density distributions in 27 axial locations are lumped into the number of nodes used for this analysis representing the source term multipliers for these nodes. The source term multipliers are relative values only and are used to describe the characteristic shape of the power distribution for the inner and outer fuel elements at the beginning and at the end of the fuel cycle. For the ten-node representation of the fuel plates and coolant channels, these normalized source term multipliers are presented in Table 14. The fraction of the power produced in the fuel elements and coolant are defined as input, and same power shape is assumed to apply for both the fuel and the coolant nodes.

4.2. HOT-CHANNEL AND HOT-PLATE MODEL

In the hot-assembly model of the HFIR, the nominal conditions which are described in Section 4.1 are modified considering such factors as coolant flow distribution within the

^h The reactivity feedback effects regarding the sudden spalling of the oxide layer off the fuel plate surface are not considered in this work.

Table 10 : Inner Fuel Element BOC power density distributions.^{70,79}

Distance from core center (cm)	Distance from core centerline (cm)						
	7.14	8.00	9.00	10.00	11.00	12.00	12.60
25.4	0.678	1.109	1.379	1.515	1.470	1.344	1.186
24	0.722	0.741	0.738	0.771	0.809	0.830	0.837
22	0.815	0.753	0.738	0.767	0.802	0.830	0.824
20	0.924	0.848	0.810	0.821	0.850	0.889	0.893
18	1.031	0.952	0.904	0.917	0.939	0.980	0.988
16	1.130	1.043	0.990	1.007	1.027	1.067	1.077
14	1.227	1.126	1.068	1.089	1.107	1.146	1.162
12	1.312	1.198	1.137	1.163	1.179	1.217	1.241
10	1.387	1.264	1.200	1.233	1.241	1.280	1.316
8	1.447	1.322	1.255	1.285	1.294	1.334	1.383
6	1.493	1.372	1.306	1.332	1.339	1.380	1.445
4	1.520	1.398	1.338	1.369	1.372	1.410	1.478
2	1.532	1.408	1.350	1.386	1.388	1.422	1.490
0	1.533	1.403	1.349	1.386	1.388	1.420	1.485
-2	1.523	1.393	1.335	1.372	1.375	1.418	1.464
-4	1.494	1.368	1.305	1.342	1.345	1.380	1.425
-6	1.448	1.324	1.256	1.289	1.295	1.330	1.372
-8	1.384	1.264	1.193	1.221	1.235	1.283	1.312
-10	1.312	1.195	1.127	1.150	1.167	1.212	1.248
-12	1.235	1.120	1.057	1.077	1.097	1.144	1.177
-14	1.148	1.039	0.982	1.000	1.023	1.069	1.098
-16	1.050	0.952	0.903	0.920	0.945	0.988	1.011
-18	0.944	0.859	0.820	0.839	0.864	0.904	0.916
-20	0.819	0.762	0.735	0.755	0.783	0.816	0.812
-22	0.709	0.668	0.667	0.698	0.735	0.762	0.753
-24	0.706	0.678	0.680	0.713	0.749	0.760	0.762
-25.4	0.703	1.028	1.231	1.342	1.319	1.216	1.078

Table 11 : Inner fuel element EOC power density distributions.^{70,79}

Distance from core center (cm)	Distance from core centerline (cm)						
	7.14	8.00	9.00	10.00	11.00	12.00	12.60
25.4	0.358	0.850	1.170	1.265	1.198	1.019	0.824
24	0.363	0.558	0.634	0.654	0.662	0.645	0.592
22	0.422	0.571	0.633	0.657	0.665	0.657	0.609
20	0.497	0.648	0.688	0.725	0.733	0.727	0.685
18	0.568	0.735	0.772	0.808	0.821	0.815	0.766
16	0.629	0.814	0.853	0.885	0.898	0.894	0.842
14	0.684	0.886	0.929	0.955	0.973	0.966	0.909
12	0.730	0.950	0.999	1.032	1.041	1.029	0.970
10	0.768	1.006	1.065	1.099	1.100	1.083	1.024
8	0.801	1.054	1.124	1.160	1.152	1.129	1.069
6	0.826	1.094	1.176	1.215	1.198	1.166	1.108
4	0.848	1.126	1.213	1.246	1.228	1.195	1.139
2	0.866	1.145	1.231	1.263	1.243	1.211	1.159
0	0.877	1.147	1.234	1.269	1.248	1.215	1.168
-2	0.868	1.134	1.229	1.261	1.242	1.210	1.168
-4	0.854	1.114	1.210	1.242	1.222	1.193	1.159
-6	0.833	1.087	1.176	1.208	1.190	1.167	1.134
-8	0.809	1.054	1.132	1.166	1.153	1.133	1.093
-10	0.778	1.012	1.079	1.112	1.108	1.092	1.044
-12	0.741	0.960	1.014	1.052	1.053	1.043	0.987
-14	0.692	0.895	0.943	0.983	0.991	0.983	0.924
-16	0.631	0.813	0.861	0.906	0.918	0.913	0.852
-18	0.552	0.707	0.770	0.821	0.836	0.831	0.774
-20	0.452	0.595	0.677	0.731	0.746	0.738	0.689
-22	0.395	0.552	0.626	0.671	0.676	0.662	0.610
-24	0.392	0.557	0.635	0.665	0.668	0.647	0.593
-25.4	0.356	0.829	1.129	1.217	1.145	0.971	0.780

Table 12 : Outer fuel element BOC power density distributions.^{70,79}

Distance from core center (cm)	Distance from core centerline (cm)						
	15.15	16.00	17.00	18.00	19.00	20.00	21.00
25.4	1.580	1.512	1.394	1.254	1.119	0.719	0.298
24	0.970	0.934	0.770	0.660	0.542	0.442	0.293
22	0.943	0.860	0.758	0.650	0.533	0.425	0.291
20	0.961	0.885	0.796	0.691	0.578	0.467	0.323
18	1.043	0.978	0.881	0.781	0.669	0.562	0.429
16	1.146	1.075	0.977	0.878	0.769	0.669	0.552
14	1.239	1.165	1.064	0.964	0.864	0.775	0.670
12	1.320	1.247	1.143	1.045	0.946	0.875	0.787
10	1.388	1.319	1.214	1.117	1.027	0.973	0.898
8	1.448	1.382	1.279	1.180	1.101	1.073	1.019
6	1.499	1.437	1.337	1.239	1.168	1.169	1.127
4	1.539	1.480	1.382	1.284	1.222	1.246	1.224
2	1.558	1.500	1.404	1.306	1.242	1.285	1.308
0	1.559	1.500	1.405	1.306	1.242	1.285	1.308
-2	1.543	1.483	1.389	1.291	1.222	1.237	1.225
-4	1.510	1.448	1.353	1.255	1.174	1.150	1.117
-6	1.448	1.387	1.286	1.190	1.101	1.067	0.998
-8	1.379	1.311	1.209	1.112	1.021	0.968	0.882
-10	1.303	1.232	1.129	1.030	0.937	0.868	0.769
-12	1.222	1.149	1.045	0.944	0.846	0.767	0.661
-14	1.135	1.060	0.957	0.854	0.750	0.662	0.555
-16	1.044	0.969	0.865	0.760	0.649	0.552	0.438
-18	0.951	0.876	0.770	0.662	0.544	0.433	0.304
-20	0.845	0.783	0.675	0.567	0.449	0.321	0.174
-22	0.741	0.700	0.616	0.517	0.403	0.282	0.138
-24	0.744	0.695	0.613	0.521	0.420	0.296	0.112
-25.4	1.342	1.294	1.173	1.016	0.848	0.451	0.034

Table 13 : Outer fuel element EOC power density distributions.^{70,79}

Distance from core center (cm)	Distance from core centerline (cm)						
	15.15	16.00	17.00	18.00	19.00	20.00	21.00
25.4	1.131	1.294	1.284	1.243	1.141	0.805	0.415
24	0.746	0.757	0.723	0.704	0.690	0.643	0.439
22	0.718	0.759	0.722	0.709	0.702	0.693	0.495
20	0.739	0.808	0.794	0.785	0.786	0.791	0.586
18	0.808	0.896	0.884	0.877	0.867	0.879	0.664
16	0.883	0.980	0.967	0.959	0.943	0.957	0.729
14	0.951	1.057	1.043	1.034	1.015	1.026	0.787
12	1.011	1.124	1.111	1.101	1.078	1.087	0.838
10	1.064	1.183	1.170	1.159	1.136	1.142	0.883
8	1.110	1.236	1.220	1.208	1.185	1.191	0.923
6	1.147	1.275	1.259	1.249	1.224	1.232	0.951
4	1.172	1.299	1.283	1.270	1.241	1.243	0.963
2	1.185	1.312	1.294	1.277	1.243	1.243	0.965
0	1.189	1.316	1.295	1.278	1.243	1.243	0.966
-2	1.186	1.312	1.294	1.277	1.243	1.243	0.965
-4	1.173	1.299	1.284	1.268	1.240	1.243	0.965
-6	1.152	1.276	1.264	1.246	1.222	1.236	0.956
-8	1.117	1.239	1.223	1.210	1.186	1.200	0.930
-10	1.075	1.190	1.174	1.162	1.139	1.152	0.891
-12	1.024	1.131	1.114	1.102	1.081	1.095	0.846
-14	0.965	1.062	1.092	1.033	1.014	1.030	0.796
-16	0.896	0.986	0.965	0.954	0.938	0.958	0.741
-18	0.818	0.896	0.875	1.862	0.849	0.874	0.677
-20	0.750	0.811	0.778	0.764	0.752	0.786	0.603
-22	0.724	0.767	0.728	0.714	0.702	0.686	0.513
-24	0.738	0.766	0.732	0.724	0.700	0.653	0.414
-25.4	1.090	1.245	1.231	1.189	1.086	0.739	0.350

Table 14 : The normalized source term multipliers for the ten-node representation of the fuel plates.

Node Number	Inner Fuel Elements		Outer Fuel Elements	
	BOC	EOC	BOC	EOC
1	0.003543	0.003291	0.003550	0.003229
2	0.092051	0.086308	0.085610	0.089128
3	0.119178	0.114900	0.115382	0.116144
4	0.143065	0.139973	0.148841	0.138510
5	0.155982	0.154571	0.171369	0.149225
6	0.153420	0.154293	0.166471	0.149259
7	0.135630	0.140989	0.137481	0.139068
8	0.109946	0.116430	0.101033	0.120928
9	0.083963	0.086048	0.067500	0.091398
10	0.003223	0.003199	0.002762	0.003109

fuel elements, fuel segregation, and fuel-cladding non-bonds. In addition, the uncertainties in plant operating conditions, in power density distributions throughout the fuel cycle, and in heat transfer and burnout characteristics are considered. The simultaneous use of these factors in a consistent manner with the steady-state hot-spot analyses^{69,70} describes the hot-channel and hot-plate conditions. These local deviations from the average conditions in the hot-plate and hot-channels are neglected in feedback modeling. Hence, no advantage is taken of negative reactivity feedback associated with strongly weighted moderator density changes in these hot-regions.

4.2.1. Description of the Hot-Assembly

The hot assembly is defined as a series of fuel plates and coolant channels as shown in Figure 13. In general, nine fuel plates and nine coolant channel are considered to represent either inner or outer fuel elements. Conservatively, a periodic boundary condition is used connecting the first plate to both first and ninth coolant channels. Unlike the steady-state hot-spot analyses, the hot-assembly of this work contains two hot-channels (channels 4 and 5 in Figure 13), four nominal channels (channels 2, 3, 6, and 7), and three cold-channels (channels 1, 8, and 9).

The nominal channels are described as those having nominal BOC thicknesses of 0.050 inch and formed by nominal fuel plates having nominal BOC thicknesses of 0.050 inch as described in Section 4.1. The nominal total heat transfer area from the fuel bearing portion of these plates is 428.8 ft². These dimensions vary from these nominal values⁸⁰ due to fabrication tolerances as summarized in Table 15. The hot and cold-channels are defined respectively as the narrow and wide channels with channel width being the minimum and maximum allowed by the specifications.⁸⁰ Simultaneous consideration of the hot and cold channels in the same assembly allows the modeling of flow mal-distributions, reducing the flow to the hot-channels, and therefore creating the least favorable situation as far as fuel damage is concerned.

Several uncertainties that are considered in hot-assembly modeling are presented in Table 16. When describing the hot and cold channels, the coolant channel narrowing due to

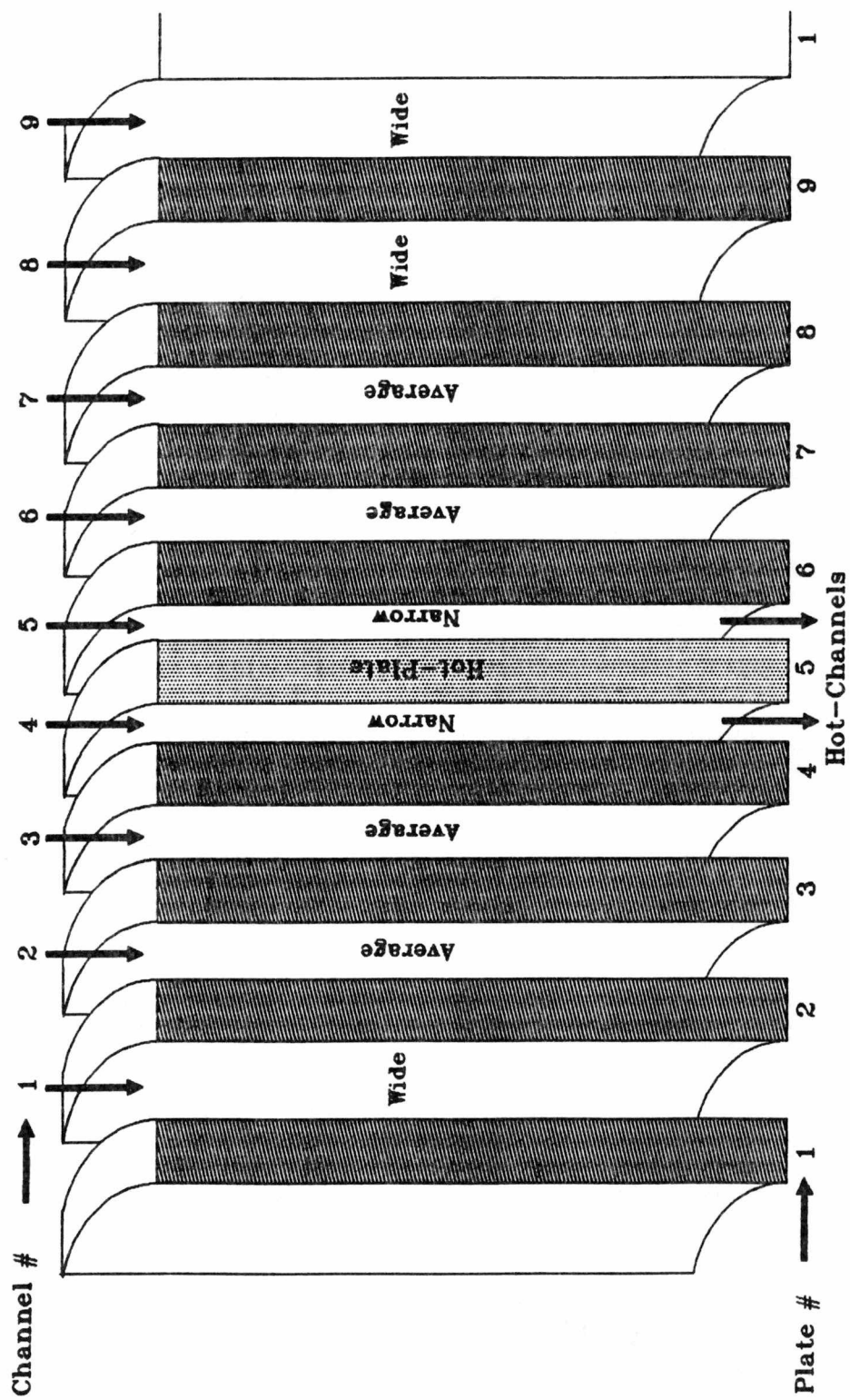


Figure 13 : Schematic of the hot-assembly modeling of the HFIR core.

Table 15 : HFIR Fuel Element Dimensions and Tolerances (for both inner and outer fuel elements).

Maximum fuel plate thickness (inch)	0.051
Minimum coolant channel thickness averaged across width of plate at any given elevation (inch)	0.044
Maximum coolant channel thickness averaged across width of plate at any given elevation (inch)	0.056
Minimum heat transfer area (total) (ft ²)	410.3
Maximum diameter of non-bond between fuel and clad (in)	0.0625
Maximum EOC oxide layer thickness (inch)	0.0039
Minimum EOC oxide layer thickness (cold-channel) (inch)	0.0
Maximum increase in fuel plate thickness due to radiation swelling at the EOC (inch)	0.00025
Minimum increase in fuel plate thickness due to radiation swelling at the EOC (cold-channel) (inch)	0.0

Table 16 : Uncertainty factors considered in hot-assembly modeling.

Tolerance on total fuel loading within an individual fuel plate (%)	1
Tolerance on fuel loading within a 0.078125 inch diameter spot (%)	30
Average tolerance on fuel loading within a rectangular area of 0.078125x0.5 inch ² (%)	10
Uncertainty in fuel plate thermal expansion (including Poisson effect)	2.0
Uncertainty in coolant channel thickness due to radiation swelling	1.002
Uncertainty in reactor power level	1.02
Uncertainty in heat transfer area	1.045
Uncertainty in power density distributions	1.10
Uncertainty in fuel loading for nodes with high power density in the hot-plate	1.10
Uncertainty in fuel loading for nodes with low power density in the hot-plate	0.92
Uncertainty in inlet coolant temperature	1.01
Uncertainty in the friction factor	1.05
Uncertainty in heat transfer correlation	0.90
Uncertainty in the burnout correlation	0.935

plate thermal expansion and radiation swelling is modified using separate uncertainty factors for each contribution. An uncertainty factor of 2.0 is applied to hot-channels for plate thermal expansion. The uncertainty for the decrease in the hot-channel thickness due to radiation effects is determined from the ratio of unidirectional azimuthal swelling to isotropic swelling, as explained in Section 4.1.2. In defining the cold channels, both thermal expansion and radiation swelling effects as well as oxide build-up are conservatively ignored.

Steady-state hot-spot analyses considered a series of fuel plate deflections due to differential pressures between the neighboring channels, temperature differences of adjacent plates, temperature difference between fuel plate and side plates, and radiation swelling.^{69,70} The two relevant modes of deflection for this analysis are longitudinal buckling of the plates due to differential temperatures between fuel plates and side plates, and radiation swelling. Possible deformation of the fuel plates under these stresses are ignored in this analysis since the data regarding these thermal deflections are inconclusive, particularly under transient conditions.⁷⁰ Furthermore, some experimental data for steady-state conditions indicate that the fuel plates may buckle into a sinusoidal shape, therefore creating no net change in the coolant flow area throughout the channel. However, thermal deflections may affect the results for the hot assembly significantly and the validity of neglecting these effects needs to be investigated.

4.2.2. Uncertainties in Operating Conditions

The reactor core process instrumentation incorporates three separate and redundant channels which provide signals proportional to reactor heat-power, reactor vessel inlet temperature, and primary coolant flow rate. A number of uncertainty factors related to these reactor operational parameters are considered in this work.

The reactor heat-power is computed from the differential temperature between the reactor vessel inlet and outlet, and from the primary coolant flow rate. An uncertainty factor of 1.02 is assigned for this parameter.⁶⁹ The uncertainty in the inlet temperature is 1.01 since this quantity can be measured with 1% uncertainty. The operating pressure of the reactor is defined in Reference 69 as the static pressure in the upper (inlet) plenum of the reactor

vessel, and no uncertainty factor assigned to it since it is a reference quantity.

The inlet and outlet plenum pressures are given for the hot-assembly calculations as boundary conditions. The total and individual flow rates for each channel are calculated dynamically based on the local and instantaneous fluid dynamics parameters. Since flow in the hot-assembly is basically pressure driven flow, the flow rate in each channel depends on the pressure losses. A conservative estimate of the losses at the coolant channel inlet and outlet are determined by modeling these sections as abrupt area changes as described in Section 3.2.6.2. The pressure losses due to drag forces inside the channel are calculated by the Waggenger formula for single-phase flow.³⁶ For two-phase pressure drop, the Jones correlation is used.³⁸ The details of these calculations and major assumptions in these relations are discussed in Section 3.2.2. The data taken during the pre-critical operation of the HFIR indicate that the absolute surface roughness to equivalent diameter ratios vary from 7.9×10^{-4} to 11.7×10^{-4} .⁷⁰ For this study, this ratio is assumed to be 12.0×10^{-4} and an uncertainty factor of 1.05 is assigned to it as suggested in Reference 70.

4.2.3. Fuel Loading Tolerances

Similar to the dimensional variations, the uranium distribution in the fuel plates may vary from the specified values. The *hot-plate* (plate 5 in Figure 13) is defined as the fuel plate which has the maximum "average" fuel concentrations permitted in the specifications. In addition, fuel loading can become segregated within local areas creating *hot-spots* within the hot-plate. In order to reduce the uncertainties regarding hot-plate and hot-spots, fuel plates are carefully inspected during the fabrication process for mal-distribution of the fuel loading.

It is specified that⁸⁰ the tolerance on the fuel loading within an individual fuel plate would be within $\pm 1\%$ range. It is also specified that the tolerance on fuel segregation within a $5/64$ inch diameter spot would not exceed 30%. In a larger rectangular area of $5/64 \times 1/2$ in², this loading tolerance becomes $\pm 10\%$. The fuel segregation uncertainty factor for the whole node is determined by normalizing these values and multiplying it with the ratio of the segregated volume to the node volume.

As a separate measure of fuel loading tolerances, it is stated that the average amount of uranium within any given area measuring 0.5 inch or more along the length of the fuel plate and across the full width of the plate must be within 10% of the nominal design value.⁸⁰ The total fuel loading in any given plate, however, must be within the 1% of the nominal value as stated in the previous paragraph. In this analysis, the implementation of this tolerance is done by assigning an uncertainty factor of 1.10 for the normalized source term multipliers of nodes with highest power density (in ten node representation of the hot-plate, these are the fourth, fifth, sixth, seventh, and eighth nodes), and an uncertainty factor of 0.92 for the nodes with lowest power density (in ten node representation of the hot-plate, these are first, second, third, ninth, and tenth nodes).

In addition to the fuel loading tolerances, a separate uncertainty factor is assigned to the power density distribution in the HFIR fuel plates. The power density distributions were computed by Cheverton and Sims⁷⁹ and are listed in Table 14. An uncertainty factor of 1.10 has been assigned to these densities when determining the hot-plate power density distributions to account for the uncertainties in these calculations.⁷⁰

4.2.4. Heat Transfer Tolerances

Tolerance in the total heat transfer area is determined from the ratio of the minimum and the nominal values of the heat transfer area. In the HFIR Functional Description,¹ these two areas are reported 410.3 ft² and 428.8 ft², respectively. Therefore, an uncertainty factor of 0.957 is assigned for this parameter. In addition, a separate uncertainty factor is used to define the uncertainty in the heat transfer correlation. Since Gambill and Bundy estimated that the heat transfer correlation due to Hausen⁴¹ is accurate to within 10%, an uncertainty factor of 0.90 is assigned for it.⁴⁰ The same uncertainty factor is assumed applicable for the Dittus-Boelter and post-critical-heat flux heat transfer correlations. Similarly, since the root-mean-square error of Bowring's burnout correlation⁵⁹ is 7% for 3800 data points,⁵ an uncertainty factor of 0.93 is assigned for it. All these uncertainty factors are used for modeling fluid flow in the hot-channels only.

The variations in the heat transfer at the different locations on the surface of the fuel

plates are also accounted for by considering the presence of non-bonds (blisters) between the fuel bearing material and the Al clad. The maximum possible diameter allowed by the present fuel fabrication tolerances is reported to be 1/16 inch.⁸⁰ The uncertainty factor associated with these non-bonds was determined using the HEATING6 code as a function of radial distance from the center along the width of the fuel plates.⁶⁹ In this work, the average value of the heat-flux peaking factor over the width of the hot-plate is approximated as 1.2 for both the inner and outer fuel elements. A more recent calculation of the tolerances regarding non-bonds and fuel segregation indicates that this value for the non-bond uncertainty factor is conservative. Similar to the situation for the fuel segregation tolerances, the non-bond uncertainty factor for the whole node is determined by normalizing the heat-flux peaking factor, then multiplying it with the ratio of the blister area to the node heat transfer area.

4.3. MODELING OF THE OTHER CORE COMPONENTS

The variations in neutron multiplication resulting from the temperature feedback throughout the core are accounted for by dividing the core into separate regions with individual reactivity coefficients, heating, and heat removal rates. In addition to the fuel elements which are described in preceding sections, the other core components are the target region, the water annulus, the control/safety plates, and the reflector region.¹⁴

The heat transfer model used for these regions is shown in Figure 14. A lumped parameter approach is used to model these regions with a one-node representation for the metal and a two-node representation for the coolant as described in Figure 14. Similar to the fuel element model, the heat transfer from the metal to coolant in these regions is assumed to take place by conduction, convection, and direct deposition of gamma and neutron energy in the water. Since the steady-state hot-spot analysis^{69,70} indicates that the fuel elements will reach burnout during a transient well before the other metal components, no uncertainty analysis are supplied for these regions.

The **target region** consists of 31 target rods with 20 inches of active length. The heat transfer characteristics of this region are evaluated at the target midplane using the average

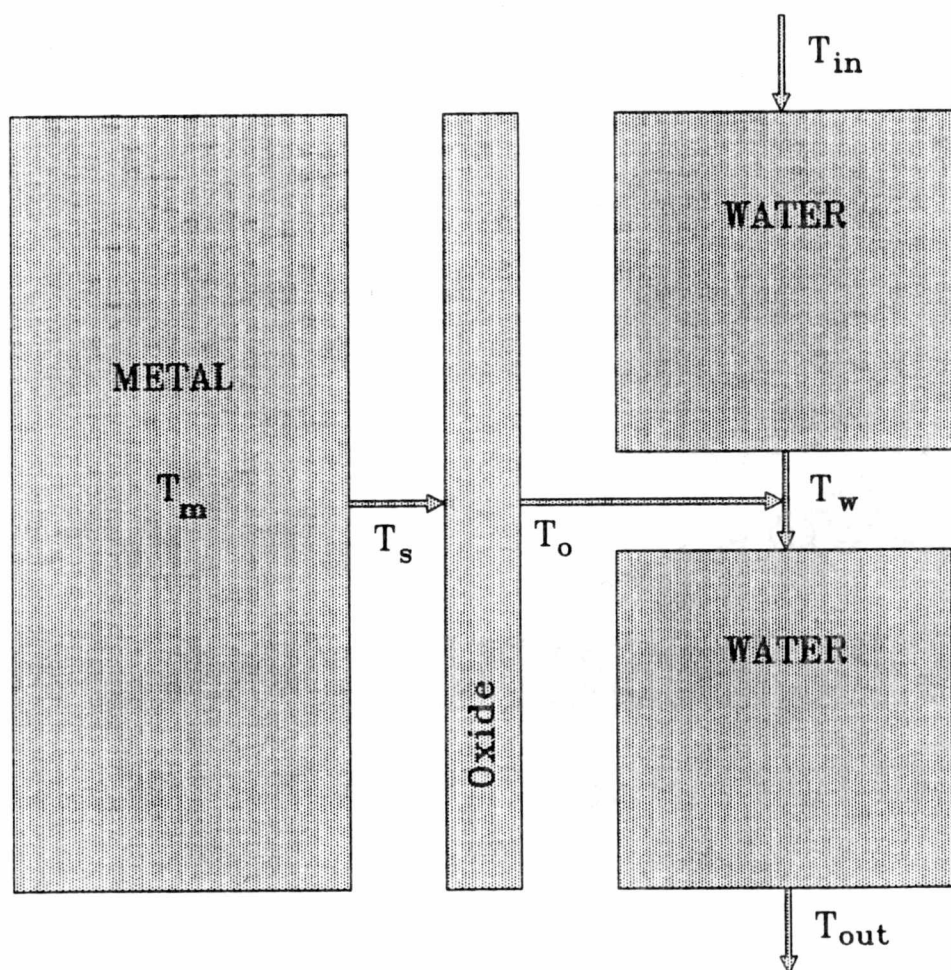


Figure 14 : Schematic of the thermal-hydraulic model for the core regions other than fuel elements.

heat flux value. Since the temperature coefficient of reactivity for this region is positive, it is conservatively assumed that there is no oxide film build-up on the target rods.

It was felt necessary to include a separate region to account for the annular space between the target rod bundle and the inner fuel element due to the low water velocity in this region. Although some small amount of heat is transferred to this **water annulus** from the target and fuel regions, it is negligible in comparison to the internal heat generation in the coolant.

The control region is assumed to consist of 20 inch long aluminum cylinders with no oxide film. Since the temperature coefficient of the region is positive, the absence of an oxide film will give the maximum adverse effects. The heat transfer parameters are calculated using average values.

A provision for incorporating the dynamic response of the reflector region in the model is made and the equations representing this region are included in the code. However, the beryllium reflector is not covered as a separate region due to unavailability of temperature coefficient data. The temperature feedback from this region is expected to be very small and is included by modifying the control region reactivity coefficient.¹⁴

The thermal hydraulic equations for the target assembly are:

$$C_{tm} \frac{dT_{tm}}{dt} = a_t P - \lambda_t (T_{tm} - T_{ts}) \quad (210)$$

$$\lambda_t (T_{tm} - T_{ts}) = \lambda_o (T_{ts} - T_{tw}) \quad (211)$$

$$\lambda_o (T_{ts} - T_{tw}) = h_t A_t (T_{ts} - T_{tw}) \quad (212)$$

$$C_{tw} \frac{dT_{tw}}{dt} = b_t P + h_t A_t (T_{ts} - T_{tw}) + \frac{2C_{tw}}{\tau_t} (T_{tm} - T_{tw}) \quad (213)$$

$$C_{tw} \frac{dT_{tw}}{dt} = b_f P + h_f A_f (T_{to} - T_{tw}) + \frac{2C_{tw}}{\tau_f} (T_{tw} - T_{tout}) \quad (214)$$

where T_{tm} is the average metal temperature for the target region,

T_{ts} is the target rods surface temperatures,

T_{to} is the oxide-water film interface temperature,

T_{tw} is the bulk water temperature in the target region,

T_{tin} is the water inlet temperature for the target region,

T_{tout} is the water outlet temperature for the target region,

C_{tm} is the total heat capacity for the target rods,

C_{tw} is the total heat capacity of water in the target region,

P is the overall power generated during the transient,

a_f is the fraction of power produced in the target rods,

b_f is the fraction of power produced in water in the target region,

λ_f is average heat transfer resistance coefficient for the rods,

λ_o is heat transfer resistance coefficient for the oxide layer,

h_f is the heat transfer coefficient,

A_f is the nominal heat transfer area for the target rods,

τ_f is the coolant residence time in the target region.

Since it constitutes a very thin layer at the surface, the total heat capacity of the oxide film is ignored.

The water annulus between the target and fuel regions which is characterized by a low water velocity is modeled by two convective equations:

$$C_{aw} \frac{dT_{aw}}{dt} = b_a P + \frac{2C_{aw}}{\tau_a} (T_{ain} - T_{aw}) \quad (215)$$

$$C_{aw} \frac{dT_{aout}}{dt} = b_a P + \frac{2C_{aw}}{\tau_a} (T_{aw} - T_{aout}) \quad (216)$$

where T_{aw} is the bulk water temperature in the annulus,
 T_{ain} is the water inlet temperature for the annulus,
 T_{aout} is the water outlet temperature for the annulus,
 C_{aw} is the total heat capacity of water in the annulus,
 P is the overall power generated during the transient,
 b_a is the fraction of power produced in water in the annulus,
 τ_a is the coolant residence time in the annulus.

And, similar to target region, the thermal-hydraulic equations for the region occupied by the control plates are:

$$C_{cm} \frac{dT_{cm}}{dt} = a_c P - \lambda_c (T_{cm} - T_{cs}) \quad (217)$$

$$\lambda_c (T_{cm} - T_{cs}) = \lambda_o (T_{cs} - T_{co}) \quad (218)$$

$$\lambda_o (T_{cs} - T_{co}) = h_c A_c (T_{co} - T_{cw}) \quad (219)$$

$$C_{cw} \frac{dT_{cw}}{dt} = b_c P + h_c A_c (T_{co} - T_{cw}) + \frac{2C_{cw}}{\tau_c} (T_{cin} - T_{cw}) \quad (220)$$

$$C_{cw} \frac{dT_{cout}}{dt} = b_c P + h_c A_c (T_{co} - T_{cw}) + \frac{2C_{cw}}{\tau_c} (T_{cw} - T_{cout}) \quad (221)$$

where T_{cm} is the average temperature of the control elements,
 T_{cs} is the control element surface temperature,
 T_{co} is the oxide layer temperature,
 T_{cw} is the bulk water temperature in this region,
 T_{cin} is the water inlet temperature for this region,
 T_{cout} is the water outlet temperature for this region,
 C_{cm} is the total heat capacity of the control elements,

C_{cw} is the total heat capacity of water in this region,
 P is the overall power generated during the transient,
 a_c is the fraction of power produced in the control elements,
 b_c is the fraction of power produced in water in this region,
 λ_c is heat transfer resistance coefficient for the plates,
 λ_o is oxide layer heat transfer resistance coefficient,
 h_c is the film heat transfer coefficient,
 A_c is the nominal heat transfer area for the control elements,
 τ_c is the coolant residence time in this region.

Similar to the case with the target region, the total heat capacity of the oxide film is neglected.

The data for the thermal hydraulic parameters for the core components other than fuel elements are listed in Table 17.^{3,14} These data are taken or deduced from early versions of the steady-state hot spot analysis.⁶⁹ The inlet temperature of the coolant for all regions are assumed to be constant since the total transient time is always much shorter than the primary coolant cycle time.

4.4. SAFETY SYSTEM MODEL

The safety system plays an important role in the dynamic modeling of the HFIR. Reactivity control by means of safety quadrants is characterized by a level or rate trip signal, a delay between signal and actual release of the control elements, a linearly decreasing initial acceleration of the control elements, and the differential reactivity worth of the safety plates as a function of plate position.

The spring loaded safety quadrants are designed to have an initial acceleration of $4g$, where g is the gravitational acceleration. This initial acceleration is assumed to decrease linearly to a value of g and remain at this value for the rest of the insertion process. This is a conservative approximation since the effect of the hydrodynamic forces is neglected in determining the final acceleration after the first six inches of travel.

Table 17 : Basic thermal-hydraulic parameters for the modeling of core components other than fuel region.

Parameter	TARGET	ANNUL US	CONTROL
Heat transfer resistance term for the metal, λ_m (W/°C)	9.881×10^3	NA	4.577×10^5
Heat transfer resistance term for the oxide, λ_o (W/°C)	∞	NA	∞
Heat transfer area x heat transfer coefficient, hA (W/°C)	2.282×10^4	NA	4.580×10^4
Total heat capacity for the metal (J/°C)	2.830×10^3	NA	2.488×10^4
Total heat capacity for the water (J/°C)	4.934×10^3	1.260×10^4	1.946×10^4
Fraction of power produced in the metal	9.23×10^{-3}	NA	6.79×10^{-3}
Fraction of power produced in the water	1.56×10^{-3}	2.86×10^{-3}	3.06×10^{-3}
Coolant residence time for full flow (s)	0.04166	0.2514	0.0901
Inlet temperature (°C)	48.9	48.9	48.9
Temperature reactivity feedback coefficient for metal (dk/k/°C)	0.0	NA	0.0
Temperature reactivity feedback coefficient for water (dk/k/°C)	5.76×10^{-5}	4.86×10^{-5}	1.98×10^{-5}

Letting S denote the rod displacement in inches, the governing equations describing the safety quadrant movement are:

$$\frac{d^2S}{dt^2} = 4g - \frac{1}{2}gS \text{ in}^{-1} \quad \text{for } \frac{d^2S}{dt^2} > g \quad (222)$$

$$\frac{d^2S}{dt^2} = g \quad \text{thereafter} \quad (223)$$

The change in reactivity generated by this motion of the safety quadrants is described by a typical S-shaped curve of reactivity. The differential reactivity worth of the safety quadrants as a function of plate position is presented in Figure 15 and taken from Reference 14. These data were determined for the four shim-safety quadrants with the inner regulating cylinder held stationary. In calculations, these data points are approximated by the dashed-lines conservatively, minimizing the area under the curves, and therefore the negative reactivity. These dashed lines are then integrated to give a negative reactivity contribution as a function of control quadrant position. The integral equations are:

$$R = 5.56S^2 \quad (0'' < S < 9'') \quad (224)$$

$$R = 450 + 100(S-9) \quad (9'' < S < 18'') \quad (225)$$

$$R = 1350 + 100(S-18) - 8.33(S-18)^2 \quad (18'' < S < 24'') \quad (226)$$

$$R = 1650 \quad (24'' < S) \quad (227)$$

where R is the reactivity in cents, S is the distance the safety quadrants are inserted. At the beginning of fuel cycle, the safety and regulating cylinders are assumed symmetric and the distance the safety quadrants are withdrawn for the initial criticality is taken 18.54 inches as an extreme value.⁸¹ At the end of fuel cycle, the safety quadrants are assumed fully withdrawn to simulate the least favorable situation.

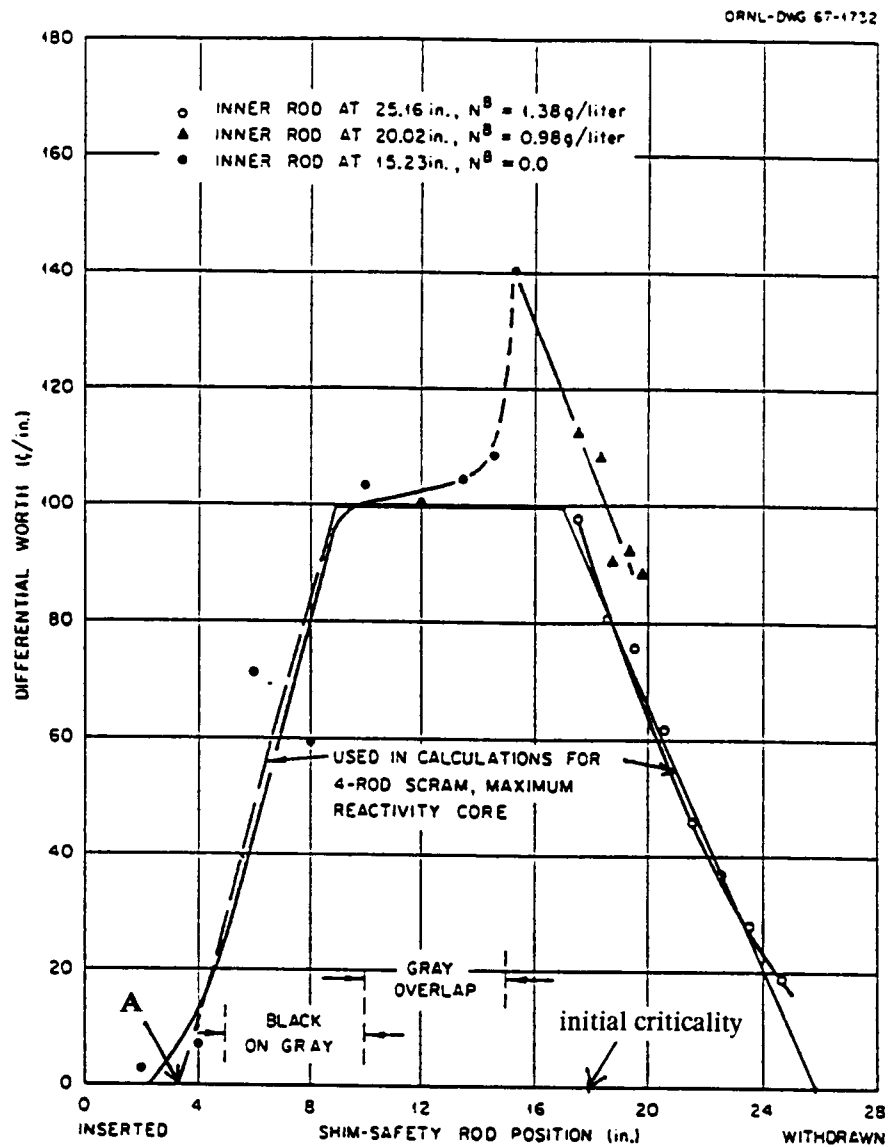


Figure 15 : Differential safety quadrant worth from Eu_2O_3 -Ta-Al rods in HFIR Criticality Experiments-3 with target.

In the HFIR Functional Description,¹ the *time of flight* for the safety quadrants during a scram is specified as 250 msec. In order to determine if this specification is met with the model used in this analysis, the total time of flight is investigated. Since the reactivity worth curves are conservatively approximated by solid lines in Figure 15, no reactivity change is associated with the last 3.5 inches of travel of the safety quadrants. In other words, the negative reactivity from the safety plates attains its maximum value when the plates are inserted 23.5 inches (point A in Figure 15) and remains the same for the rest of the transient as if the safety plates are fully inserted. The time required to reach this point from the fully withdrawn initial position is calculated as 229.8 msec. However, aside from the interpretation of reactivity worth curves, the total time of flight for safety quadrants is defined as the time difference from fully withdrawn to fully inserted positions. Since each plate consists of a 22 inch long *black region* and a 5 inch long *gray region*, the total distance travelled by the quadrants during scram is 27 inches. Calculations indicate that the total time required to travel this distance using this model is 248 msec which meets the specification.

The other safety system specifications of the HFIR are summarized in Table 18. To minimize unnecessary shutdowns due to electrical noise, the rate trips are RC networks rather than a differential amplifier. The selected RC time constant (0.25 sec) is reported to provide a noise threshold which is equivalent to a 5 MW step in power.¹ Large and rapid reactivity increases, therefore, are not expected to cause a rate trip until the power has increased by 5 MW. This 5 MW delay is also implemented in the rate trip algorithm. In the HFIR Functional Description, the reported latch release time is 10 msec for Mode 1 and 2, and 15 msec in Mode 3.¹ More recently, however, the safety system was modified by adding smoothing capacitors to the signal amplifiers due to a need for noise reduction in the HFIR instrumentation channels.²⁵ In addition, the rate trip was removed from Modes 2 and 3. Since both changes slowed the safety system response, a 0.04 msec additional delay time is added to the specified value for Mode 1 and 2. For Mode 3, the total delay for latch release is conservatively assumed to be 45 msec.

Table 18 : HFIR core scram characteristics and safety system settings for Mode 1.

Scram Characteristics	Mode 1	Mode 2	Mode 3
Level trip (MW)	12-102	3.25	0.130
Rate trip (MW/s)	17	N/A	N/A
Minimum Change in Power for rate trip (MW)	5	N/A	N/A
Latch release time (s)	0.010	0.010	0.015
Additional delay time due to smoothing capacitors (s) ²⁵	0.00004	0.00004	0.030

5. METHOD OF SOLUTION

A novel finite difference scheme is used to solve the equations representing the HFIR core dynamics model which is presented in the previous chapters. The nonlinear partial differential equations of the fluid dynamics model are discretized only in space leading to a large initial value problem of ordinary differential equations. The resulting equation set is then combined with point kinetics and the heat conduction equations, and solved simultaneously in the time domain. This fully implicit treatment avoids numerical instabilities typically encountered in other methods which employ so called "semi-implicit" coupling between the thermal, hydraulic, neutronic and structural models.

5.1. SEMI-DISCRETIZATION OF PARTIAL DIFFERENTIAL EQUATIONS

An unconventional semi-discretization technique is used to handle the equations representing the fluid dynamics and heat conduction models which are discussed in Sections 3.2 and 3.3. In this approach, the coolant channels and fuel plates are divided into a series of control volumes. Each control volume is assumed to store mass, energy and momentum for each phase.

The basic conservation equations of the fluid dynamics model all express the time rate of change of one of the variables in terms of spatial changes in the others. Generally, these equations can be expressed as first-order equations of the form

$$\frac{\partial u_i}{\partial t} = \sum_k [A_k \frac{\partial u_k}{\partial z} + B_k u_k] \quad (228)$$

where u_k are the state variables other than time, t , and space, z . A_k and B_k depend on z , t , and u_k ($i=1,..K$). When expressed in traditional finite difference form, the time rate of change can be approximated by the spatial change observed in the previous time step. That is, for a particular variable we write

$$\frac{u_i^{j,n+1} - u_i^{j,n}}{\Delta t_{n+1}} = \sum_k \left[A_k \left(\frac{u_k^{j+1,n} - u_k^{j-1,n}}{\Delta z_j} \right) + B_k (u_k^{j+1,n} - u_k^{j-1,n}) \right] \quad (229)$$

where notation $u_k^{j,n}$ indicates the value of variable u_k at axial location j and time step n . With this approach, the values of independent variables at time period $(n+1)$ can be directly determined from the values at time n . This approximation requires that the time step be restricted so that

$$\Delta t \leq \frac{\Delta z}{c + |v|} \quad (230)$$

where v is the fluid velocity and c is the sonic velocity. The general condition for stability and convergence of finite difference schemes of this nature are discussed by Fox⁸² and Richtmyer.⁸³

The time step restriction imposed on the explicit formulation described above may lead to excessive computing times as well as numerical instabilities. These difficulties can be circumvented by going to a fully implicit method. In the implicit formulation, spatial changes are written in terms of state variable values at the current time step. That is, we write equations of the form

$$\frac{u_i^{j,n+1} - u_i^{j,n}}{\Delta t_{n+1}} = \sum_k \left[A_k \left(\frac{u_k^{j+1,n+1} - u_k^{j-1,n+1}}{\Delta z_j} \right) + B_k (u_k^{j+1,n+1} - u_k^{j-1,n+1}) \right] \quad (231)$$

which results in a non-linear algebraic equation set ($i=1,..K$). This approach requires the solution of a set of simultaneous equations covering all the variables at all spatial locations. However, the time step limitation of the explicit formulation is removed, and the accuracy of the solution requires that

$$\Delta t \leq \frac{\Delta z}{|v|} \quad (232)$$

Since the solution of state variable u_i requires knowledge of itself, traditionally iterative

schemes are employed in the implicit approach. Procedures have been developed for minimizing the effort involved in solving these simultaneous equations.⁸⁴

The partial differential equations representing the fluid dynamics model are developed in Section 3.2.5 and are repeated here.

$$\frac{\partial G_m}{\partial t} = - \frac{\partial}{\partial z} \left(\frac{G_m^2}{\rho_m} \right) - \frac{\partial p}{\partial z} - F_w + \rho_m g \quad (233)$$

$$\frac{\partial p}{\partial t} = \frac{c^2}{\rho_m} \left[-\rho_m \frac{\partial G_m}{\partial z} - R_h (C_{mech} + C_{therm}) \right] \quad (234)$$

$$\frac{\partial h_m}{\partial t} = \frac{c^2}{\rho_m} \left[-\frac{\partial G_m}{\partial z} + R_p (C_{mech} + C_{therm}) \right] \quad (235)$$

Considering the vertical flow channel in Figure 16 that is divided into a number of thermal-hydraulic cells, these equations above can be discretized in the axial direction as follows:

$$\frac{dG_m^j}{dt} = - \frac{1}{\Delta z_j} \left(\frac{G_m^{j^2}}{\rho_m^{*j}} - \frac{G_m^{j-1^2}}{\rho_m^{*j-1}} \right) - \frac{1}{\Delta z_j} (p^{j+1} - p^j) - F_w^j + \rho_m^j g \quad (236)$$

$$\frac{dp^j}{dt} = \frac{c^{j^2}}{\rho_m^j} \left[-\rho_m^j \frac{1}{\Delta z_j} (G_m^j - G_m^{j-1}) - R_h^j (C_{mech}^j + C_{therm}^j) \right] \quad (237)$$

$$\frac{dh_m^j}{dt} = \frac{c^{j^2}}{\rho_m^j} \left[-\frac{1}{\Delta z_j} (G_m^j - G_m^{j-1}) + R_p^j (C_{mech}^j + C_{therm}^j) \right] \quad (238)$$

where $j=1,...,J$, J being the total number of finite difference cells in the channel (here referred

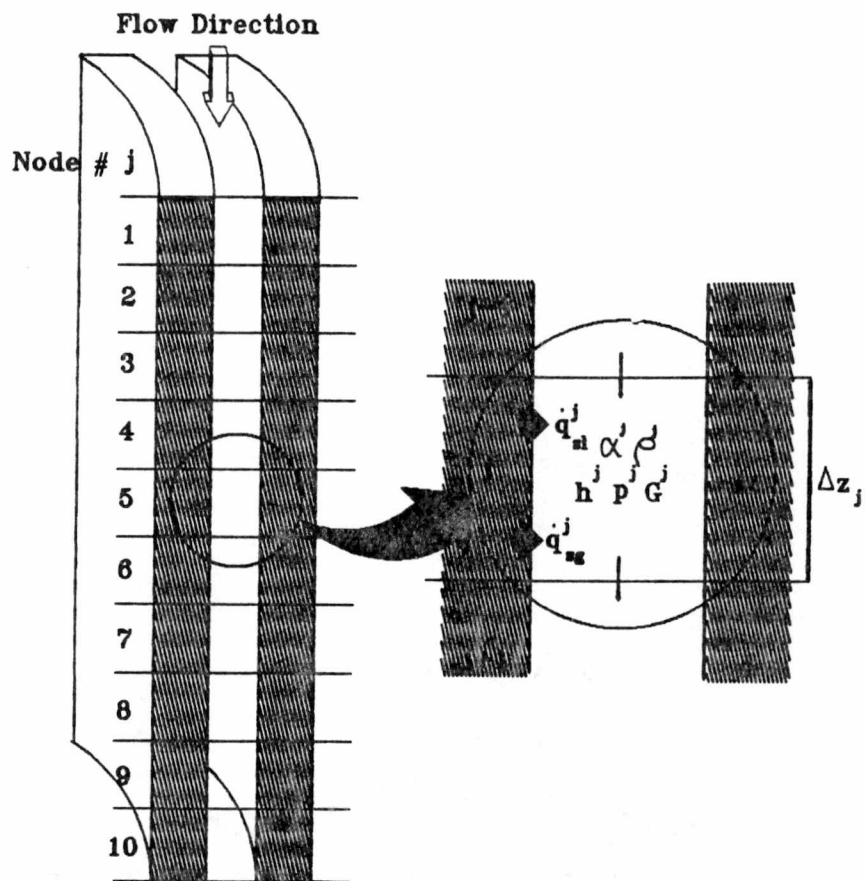


Figure 16 : A discretized flow channel.

as nodes).

Similarly, components of the fuel plate heat conduction model (clad region 1, fueled region, and clad region 2) are modelled by a series of azimuthal nodes at a number of axial levels. Heat transfer within an individual fuel node is treated by a one-dimensional heat conduction equation to yield average temperatures for each region:

$$\rho_c c_{pc} \frac{d\bar{T}_{cl}(t)}{dt} = \frac{1}{a} \left[(k_c \frac{\partial T_{cl}}{\partial x})|_a - (k_c \frac{\partial T_{cl}}{\partial x})|_0 \right] + \langle Q_c \rangle \quad (239)$$

$$\rho_f c_{pf} \frac{d\bar{T}_f(t)}{dt} = \frac{1}{b-a} \left[(k_f \frac{\partial T_f}{\partial x})|_b - (k_f \frac{\partial T_f}{\partial x})|_a \right] + \langle Q_f \rangle \quad (240)$$

$$\rho_c c_{pc} \frac{d\bar{T}_{c2}(t)}{dt} = \frac{1}{c-b} \left[(k_c \frac{\partial T_{c2}}{\partial x})|_c - (k_c \frac{\partial T_{c2}}{\partial x})|_b \right] + \langle Q_c \rangle \quad (241)$$

Again, considering the vertical fuel elements in Figure 16 that are divided into a number of finite difference cells, these equations above can be re-written for each cell in the axial direction as follows:

$$(\rho_c c_{pc})^j \frac{d\bar{T}_{cl}^j(t)}{dt} = \frac{1}{a} \left[(k_c^j \frac{\partial T_{cl}}{\partial x})|_a^j - (k_c^j \frac{\partial T_{cl}}{\partial x})|_0^j \right] + \langle Q_c^j \rangle \quad (242)$$

$$(\rho_f c_{pf})^j \frac{d\bar{T}_f^j(t)}{dt} = \frac{1}{b-a} \left[(k_f^j \frac{\partial T_f}{\partial x})|_b^j - (k_f^j \frac{\partial T_f}{\partial x})|_a^j \right] + \langle Q_f^j \rangle \quad (243)$$

$$(\rho_c c_{pc})^j \frac{d\bar{T}_{c2}^j(t)}{dt} = \frac{1}{c-b} \left[(k_c^j \frac{\partial T_{c2}}{\partial x})|_c^j - (k_c^j \frac{\partial T_{c2}}{\partial x})|_b^j \right] + \langle Q_c^j \rangle \quad (244)$$

Either fluid dynamics or heat conduction equations, when organized in a vector form,

constitute sparse band structured or tridiagonal matrices which can be exploited mathematically to improve convergence and computational efficiency of the time step advancement. For large nuclear systems analysis codes, these properties provide indispensable mathematical conveniences. Sequential iteration schemes as those used in RELAP,⁸⁵ TRAC,⁸⁶ and PHOENICS⁸⁷ take advantage of these properties and perform additional inner iterations over the components of the solution vector at a fixed point in time.

However, after combining the fluid dynamics and/or heat conduction equations with each other and the remaining system modeling such as neutronics equations, these diagonal matrix properties are lost. To overcome this difficulty, a semi-implicit coupling scheme is utilized in the aforementioned computer codes. In a semi-implicit scheme, even though implicit techniques are used within each module, data exchange between the modules is explicit. The order of block execution dictates the time levels of feedback data between models. In the RELAP code, for instance, the equations representing heat conduction and convection are advanced in time first, using reactor kinetics power and hydrodynamic variables from the previous time step. Then equations representing the fluid dynamics are advanced using both new and old heat transfer variables to compute heat transferred into a hydrodynamic volume, and so on...

Even though this semi-implicit scheme has proven to be reasonably accurate and numerically more efficient than the fully implicit schemes for slow system transients, it often causes numerical instabilities for rapid thermal transients which involve large reactivity changes. Indeed, early numerical experiments using HFIR's RELAP5/Mod2 model which is primarily developed for simulation of LOCA's exhibited some catastrophic behavior during the analysis of reactivity anomalies.⁸⁸ Semi-implicit coupling between the thermal-hydraulic and point kinetics modules effectively reduces the reactivity feedback to a linear approximation which is believed to cause meaningless results for these rapid transients with strong feedback.ⁱ

ⁱ In addition to the anticipated problems due to semi-implicit coupling, the use of large control volumes also might have led to the instabilities observed in the solution in that analysis. In RELAP, a well-posed model is obtained by introducing higher order artificial viscous dissipative terms to the basic two-fluid field equations. However, it has been shown

Therefore, a fully implicit numerical solution scheme is utilized in this work for the solution of the whole equation set describing the point reactor kinetics, fluid dynamics, heat conduction, fuel element structural behavior, other core components, and the safety system models. When this equation set is organized in a vector form, it constitutes sparse but irregular matrices. With these non-diagonal matrices, a simultaneous block iteration scheme should be used.

5.2. TIME ADVANCEMENT SCHEME

Spatial semi-discretization (discrete in space, continuous in time) of the non-linear partial differential equations of the fluid dynamics model leads to a large initial value problem in ordinary differential equations (ODE's). The resulting ordinary differential equation set (equations (236) to (238)) combined with equations describing point kinetics (equations (8) and (9)), heat conduction (equations (242) to (244)), and core structural models (equations described in Chapter 4) are then solved simultaneously in the time domain.

5.2.1. Initial Value Problem

In general, the non-linear ordinary differential equation set describing the HFIR core model is represented by the following vector formulation

$$\frac{d\bar{u}}{dt} = \bar{f}(\bar{u}, t) \quad (245)$$

where $\bar{u}$ and $\bar{f}$ are vectors of length N (total number of equations in the set). For this ODE system we consider the initial value problem, where $\bar{u}(t)$ is to be computed for $t_0 \leq t \leq T$, where $t_0 \leq T \leq \infty$. The value of T might not be known in advance.

that, unless small size control volumes are used, the kinematic viscosities required to secure a well-posed problem may be appreciably greater than the true value for the fluid considered.⁸⁹ It has also been shown that the systems with these higher order terms could generate long wave instabilities even when the final system is well-posed.⁹⁰ These arguments are discussed in Appendix B in detail.

Linear multistep methods for the solution of this problem requires approximate values at $t=t_0, t_1, t_2, \dots$ where

$$t_n = t_{n-1} + \Delta t_n \quad (246)$$

with some step size Δt_n . These values are calculated according to a formula of the form

$$\bar{u}^n = \sum_{j=1}^{K_1} \alpha_j \bar{u}^{n-j} + \sum_{j=0}^{K_2} \beta_j \Delta t_n \left(\frac{d\bar{u}^{n-j}}{dt} \right) \quad (247)$$

where

$$\bar{u}^k = \bar{u}(t_k) \quad (248)$$

$$\frac{d\bar{u}^k}{dt} = \bar{f}(\bar{u}^k, t_k) \quad (249)$$

and the α_j and β_j are fixed coefficients associated with the particular method to be used. A classical example is that of the Adams method where $K_1=1$ and $\alpha_1=1$. Equation (247) is used to calculate $\bar{u}^n$ when previous approximate values of $\bar{u}$ and its time derivative are known. Special considerations, not discussed here, are necessary to obtain the several values needed at the beginning to make the equation applicable. The approximation is of order q if it yields a $\bar{u}^n$ which is exact within the truncation error that is in the order of Δt_n^{q+1} as Δt approaches to infinity.

The method is explicit if $\beta_0=0$ and implicit otherwise. In the implicit case, the term

$$\beta_0 \Delta t_n \left(\frac{d\bar{u}^n}{dt} \right) \quad (250)$$

makes the method a system of (generally) non-linear equations, and they are solved by making a first guess at $\bar{u}^n$ with an explicit method. Then an iterative method is utilized to find the "true" solution. Two basic iterative methods are *functional iterations* and *Newton's method*. If we denote the right hand side of equation (247) by $g(\bar{u})$, the equation reduces to

$$\bar{u} = \bar{g}(\bar{u}) \quad (251)$$

If the iterates are denoted by $\underline{u}_m$, then the method of functional iteration is based on the formula

$$\bar{u}_{m+1} = \bar{g}(\bar{u}_m) \quad (252)$$

while Newton's method is based on

$$\bar{u}_{m+1} = \bar{u}_m - \left[I - \frac{\partial \bar{g}}{\partial \bar{u}} \Big|_m \right]^{-1} [\bar{u}_m - \bar{g}(\bar{u}_m)] \quad (253)$$

where I denotes a $N \times N$ identity matrix. In the case of Newton's method, if we let W denote the inverse matrix used and notice that in the case of functional iteration $W=I$, then both methods take the form

$$\bar{u}_{m+1} = \bar{u}_m + W[\bar{g}(\bar{u}_m) - \bar{u}_m] \quad (254)$$

Of these, choosing the more costly Newton's method mostly gives benefits which are often well worth its cost.

To be effective, the numerical scheme must be able to cope with severe stiffness originating from the real and complex eigenvalues of the Jacobean matrix, $\delta f / \delta \underline{u}$. Classical methods fail on stiff problems simply because only a bounded portion of the negative real axis of complex space lies in the *absolute stability region*, S_a . Perhaps the most widely known stiff solver scheme is due to Gear.⁹²⁻⁹⁶ Gear defines a notion of "stiff stability" which ensures that the error amplification matrix is stable over most of the negative real half plane. In order to achieve this, Gear's scheme uses a high order algorithm which rejects the functional iteration as a corrector method and includes the use of Newton's method as a fundamental part of the algorithm.

As with Gear's scheme, however, most widely used stiff solvers have limited applicability (narrow stability margin) due to the size of the problem. It is well known that the higher the order of algorithm, the smaller the stability margin. In this work, an efficient

scheme based on the forward and backward Euler method (FEBE) is used.⁷ FEBE tackles the stability problem by using a low-order algorithm whose accuracy is offset by the use of an extrapolation technique in addition to classical implicit iterations.

5.2.2. Description of the FEBE's Solution Scheme

FEBE is a variable order, variable time step method which has been used in Europe successfully for simulations of thermal-hydraulic transients.^{7,97} The time advancement procedure in FEBE for the numerical solution of the nonlinear partial differential equations can be viewed as a combination of iteration and extrapolation techniques.⁷

In FEBE, the extrapolation scheme^{98,99} is based only on that part of the Jacobean which is used in classical iteration techniques. Therefore, updating the Jacobean for a sub-step of the extrapolation scheme and solving the resulting matrix equation involves the same amount of computational effort as one classical iteration step. However, by combining the extrapolation scheme with classical implicit iterations, the information about the time evolution of the solution can be used to obtain a converged solution with significantly less computational effort than required by classical iteration techniques alone. Also, updating the matrix in each sub-step makes the scheme more robust than classical extrapolation techniques which update the full Jacobean at the beginning of a basic step. In brief, by combining extrapolations and iterations, FEBE's time advancement scheme takes advantage of better economy relative to iteration methods, and the improved robustness relative to the standard extrapolation methods.

Traditional iterative schemes which iterate the solution vector at fixed points in time do not provide information that can be used for mathematically sound error control. Convergence of the iteration only insures an accurate solution of the discrete linear equation set. However, no information is provided about the magnitude of the time discretization error and the appropriateness of the time step size. At best, the number of iterations required to converge to a solution can be used as a measure of the time discretization error.

In a traditional extrapolation scheme, on the other hand, the basic time step, Δt , is

successively subdivided into k sub-steps of length $\Delta t/k$ ($k=1,2,3,4,6,8,12\dots$) such that on each sub-interval the equation set is solved to yield initial entries for the construction of an extrapolation tableau.⁹⁷ From this tableau, the information needed for the control of error and step size can be extracted. With this method, only the first step of a Newton iteration is needed and the reduction of the linearization error is left to the extrapolation scheme. The Jacobean is reevaluated for each basic time step and kept constant for the sub-steps.

By combining an extrapolation scheme with classical implicit iterations, FEBE adapts the order and time step size control to the development of the solution. In the combined iteration and extrapolation scheme, at each $t^l = t^n + \Delta t/k$ ($k=1,2,\dots; l=1,2\dots k$), a backward Euler step (only the initial implicit predictor step of the equation (247)) is performed. This expression gives rise to an asymptotic expansion in powers of Δt , containing error contributions from the discretization of the time derivatives and the linearizations of the matrices. The introduction of an extrapolation scheme reduces both error contributions. In addition, information from the extrapolation tables can be used for control of the time step size. In comparison with traditional extrapolation schemes, updating the system matrix in every sub-step results in improved robustness and accuracy even when the system matrix is determined only by parts of the Jacobean.

5.2.3. Summary of Usage of FEBE

The communication between the main program and the time advancement scheme, FEBE, is summarized here. FEBE solves the initial value problem for stiff or non-stiff systems of first order ODE's of the form as presented in equation (245):

$$\frac{d\bar{u}}{dt} = \bar{f}(\bar{u}, t) \quad (255)$$

where $\bar{u}$ is the state variable vector of length N , carrying initial values at t on input and solution at $t+\Delta t$ on output:

$$\bar{u} = [u(1), u(2), \dots, u(n)]^T \quad (256)$$

It uses a forward and/or backward euler scheme for discretization of time derivatives in

conjunction with local rational or polynomial extrapolation as described above.⁹⁷⁻⁹⁹

The communication between the FEBE and the main program is primarily established through a subroutine which provides the differential equation set to be discretized for time advancement. This user-supplied subroutine, f , is used to evaluate the vector function $f(\underline{u}, t)$ of equation (255) for a given value of t and a given vector $\underline{u}$. The formal parameter sequence must be:

$$f(t, \underline{u}, d\underline{u}, *) \quad (257)$$

where the vector $d\underline{u}$ is supposed to carry the derivatives $d\underline{u}(1)/dt$, $d\underline{u}(2)/dt$, ..., $d\underline{u}(N)/dt$ on return.

The next step is to determine whether or not the problem is stiff. Stiffness occurs when the Jacobean matrix, $\partial f / \partial \underline{u}$, has an eigenvalue whose real part is negative and large in magnitude compared to the reciprocal of the time span of interest. In the case of stiffness (as is the case for the model used in this analysis), implicit discretization may be selected (via input parameter) for either all equations or just the subset requiring increased stability.

The main program calls FEBE once for each point in time at which answers are desired. Before the first call to FEBE input has to be provided for several variables and parameters. These are N , $\underline{u}$, t , Δt , Δt_m , GRESCH, ECKSCH, EPS, CLIMV, TOP, TIP, TSE, NIMAX, INX, OUT, and OUTIMP. Their meanings are as follows:

N is the number of first order ordinary differential equations.

$\underline{u}$ is the state variable vector of length n which carries the initial values of $u(i)$, $i=1,2,\dots,N$ at t . After a successful return from FEBE, it carries the solution at $t+\Delta t$ on output.

t is the initial time (usually zero). After a successful step FEBE replaces t by $t+\Delta t$.

Δt is the time step suggested by the user. FEBE may perform a smaller step than

suggested and will replace Δt by a suggestion for the size of the next integration step.

Δt_m is the upper limit of Δt . Values of Δt larger than Δt_m will be reduced to Δt_m . It is suggested that the length of the desired output interval is to be used for this variable.

GRESCH is the lower limit of Δt . If values of Δt smaller than GRESCH are suggested, FEBE will print an error message. Near discontinuities (cf. ECKSCH) GRESCH is temporarily set to $\min(\text{GRESCH}, \text{ECKSCH} * 1.d-2)$.

ECKSCH = If discontinuities in the derivatives du/dt are crossed, a logical variable (HXX in common-block /CIMEX3/ in the source code of FEBE) must be set true in the user-supplied subroutine evaluating $f(u,t)$. In this case FEBE will reduce the crossing step size to a value smaller or equal to ECKSCH and will switch off various devices requiring continuity. It is suggested that a value 0.d0 will be adequate for this variable if there will be no discontinuities, otherwise 1.d-6 should be small enough.

EPS is the upper bound for the local relative error in $u(i)$, $i=1,2,\dots,N$, to be tolerated in each time step. EPS must not be smaller than 1.d-8. It is suggested that 1.d-4 has been a useful value in many problems.

CLIMV is a vector of length n carrying the lower bounds for the local absolute errors $\text{EPS} * \text{SI}(i)$, $i=1,2,\dots,N$, to be considered significant. $\text{SI}(i)$ is the maximum absolute value of $u(i)$ encountered in $(t, t + \Delta t)$. Suggested value is $\max(\text{EPS} * u(i), \text{EPS})$. The suggested value may, however, be too large for $u(i)$ having very small absolute values for most of the problem time.

TOP is a logical vector of length N . If $\text{TOP}(i)$ is set true, then equation i will not be integrated.

TIP is a logical vector of length N . If $\text{TIP}(i)$ is set true, then equation i will be discretized implicitly.

TSE is a logical vector of length N for which the following options are available:

TSE(i)	TIP(i)	
true	false	equation i will be discretized explicitly
false	true	equation i will be discretized implicitly

NIMAX is the maximum number of equations to be discretized implicitly. It is suggested that NIMAX is set equal to N, and use smaller values only in case of storage limitations.

INX is a logical variable which must be set true for the first integration step as well as for the first integration step following a change in N, Δt , EPS, CLIMV, TOP, TIP, TSE, NIMAX.

OUT is a logical variable. If it is set true, then the values of various internal parameters, variables and indices of FEBE are printed for control purposes.

OUTIMP is also a logical variable. If it is set true the values of various internal variables of subroutine IMPHF (called by FEBE) are printed for control purposes.

To continue the integration after a successful return from FEBE, only the time at which another return is requested should be restated. No other parameters need to be reset.

FEBE is based on a multistage one-step method. Therefore, the user may change his ODE's, as well as any of the aforementioned variables and parameters between any two steps by simply paying attention to the proper use of parameter INX. Changes within a step are generally not permitted. In order to avoid having to change the dimension of the arrays, whenever N changes, each of them is in a separate common-block. In the calling program the dimension of the arrays has to be at least $7*N$ for block CIME30, $NIMAX*NIMAX$ for block CIME28 and N for blocks CIME19,...,CIME27,CIME31,...,CIME39. Subroutine IMPHF uses the parameter RUNKON. RUNKON is the smallest positive number that satisfies " $(1.d0 + runkon) .gt. 1.d0$ " on the computer. It should be verified whether the value, already given in the data-statement of IMPHF, applies to particular computer system to be

used.

The final version of the code which contains both nominal and hot-assembly models is very long running. Its use in a production mode is practical only on large, fast computers. The HFIR dynamics model along with the time advancement algorithm, FEBE, are compiled on a IBM Reduced Instruction Set Computer (RISC/6000) workstation. On this machine, a typical simulation of a 100 msec boiling case employing 540 ODE's for the hot-assembly only requires approximately 1 hour of CPU time. For the nominal assembly coupled with the point neutronics including the scram model and lumped models for core regions other than the fuel elements, a typical simulation of a 100 msec case employing 141 ODE's requires approximately 10 minutes CPU time.

6. CODE VERIFICATION

Before introducing the results of this study regarding the HFIR reactivity transients and fuel damage propagation, results of the model verifications are presented in the following sections. The HFIR core dynamics model, FDP, is first verified against RELAP5-Mod2.⁹ Since the RELAP5 model used does not include neutronic effects, only the verification of fluid dynamics variables has been feasible. To improve readability, only a few key figures are presented in this section to support the text. More RELAP comparisons are presented in Appendix D. The neutronics and feedback behavior of the model are verified against an earlier single-phase lumped-parameter code, REACC.^{2,3} At the end, an attempt has been made for comparison of the overall neutronics and thermal-hydraulic behavior of the model with the experimental SPERT-II⁹ test results.

6.1. PARTIAL COMPARISONS with RELAP5/Mod2

RELAP5/Mod2 is a PWR system transient analysis code which is primarily designed for simulation of reactor thermal-hydraulics. It employs a one-dimensional, full non-equilibrium, six-equation, two-fluid two phase model in which six flow regimes are modeled for pre- and post-CHF heat transfer. Interfacial drag formulation and wall friction are also calculated based on the flow regime. A boiling curve is used in the code to govern the selection of the heat transfer correlations in each boiling regime. The heat structure model is based on a finite difference solution of the one-dimensional heat conduction equation. A point kinetics model is provided to compute the power behavior. These models are coupled semi-implicitly in the program. Since transient conditions can be modeled up to the point of fuel damage, this version of RELAP is not adequate for the complete fuel damage propagation analysis. However, comparisons for some slow transients can be used to verify the basic thermal-hydraulic components of the principal analysis code, FDP.

RELAP's hydrodynamic model and the associated numerical scheme are based on the use of fluid control volumes, called nodes, and junctions to represent the spatial character of the flow. The fluid scalar properties such as pressure and density are represented by the

average fluid conditions in each node and they are viewed as being located at the control volume center. The fluid vector properties, i.e. velocities, are located at the junctions and are associated with the mass and energy flow between the control volumes.

The nodalization diagram of the RELAP model for the HFIR core is shown in Figure 17.^j A description of each of the hydrodynamic components and heat structures shown in the diagram is included in Table 19. The nodalization scheme in Figure 17 describes the flow in the pressure vessel in a similar fashion as it is used for the hot-assembly modeling of this work (Section 4.2.1). Nine parallel coolant channels and nine fuel plates are modeled to represent the flow between the inlet and outlet plena using periodic boundary conditions. The pressure vessel upper and lower plena are modeled as "time-dependent volumes" to specify fluid conditions (specifically temperature and pressure) at a pressure boundary. Coolant channels are modeled as a series of 10 volumes and 9 junctions describing "pipes." Fuel plates are modeled as "heat structures" with six mesh intervals. This structure is preferred over a simpler single channel model because it provides a tool for verification of the boundary conditions used and plenum effects, in addition to verification basic hydrodynamic variables.

Even though RELAP's⁸ semi-implicit scheme is accurate and efficient enough for the analysis of slow system transients, it may result in numerical instabilities for very fast transients which involve large reactivity changes and strong reactivity feedback. That is because the semi-implicit coupling between the thermal-hydraulic and point kinetics modules effectively reduces the reactivity feedback to a linear approximation. Indeed, early numerical experiments with a simplified RELAP model which is developed for comparisons with the REACC code²³ exhibited some catastrophic behavior during the analysis of reactivity anomalies.⁸⁸ Therefore, the RELAP model used for comparison purposes does not include the reactor kinetics module. Instead, the power excursion data are taken from the new model, FDP, and introduced to the RELAP model as a "*power table*." Since the RELAP5 model does not include neutronic effects, only verification of the thermal-hydraulics model is possible.

^j This RELAP5/Mod2 model of the HFIR core is slightly different version of the model developed by Dr. J. D. Freels for the HFIR coolant channel blockage analysis.

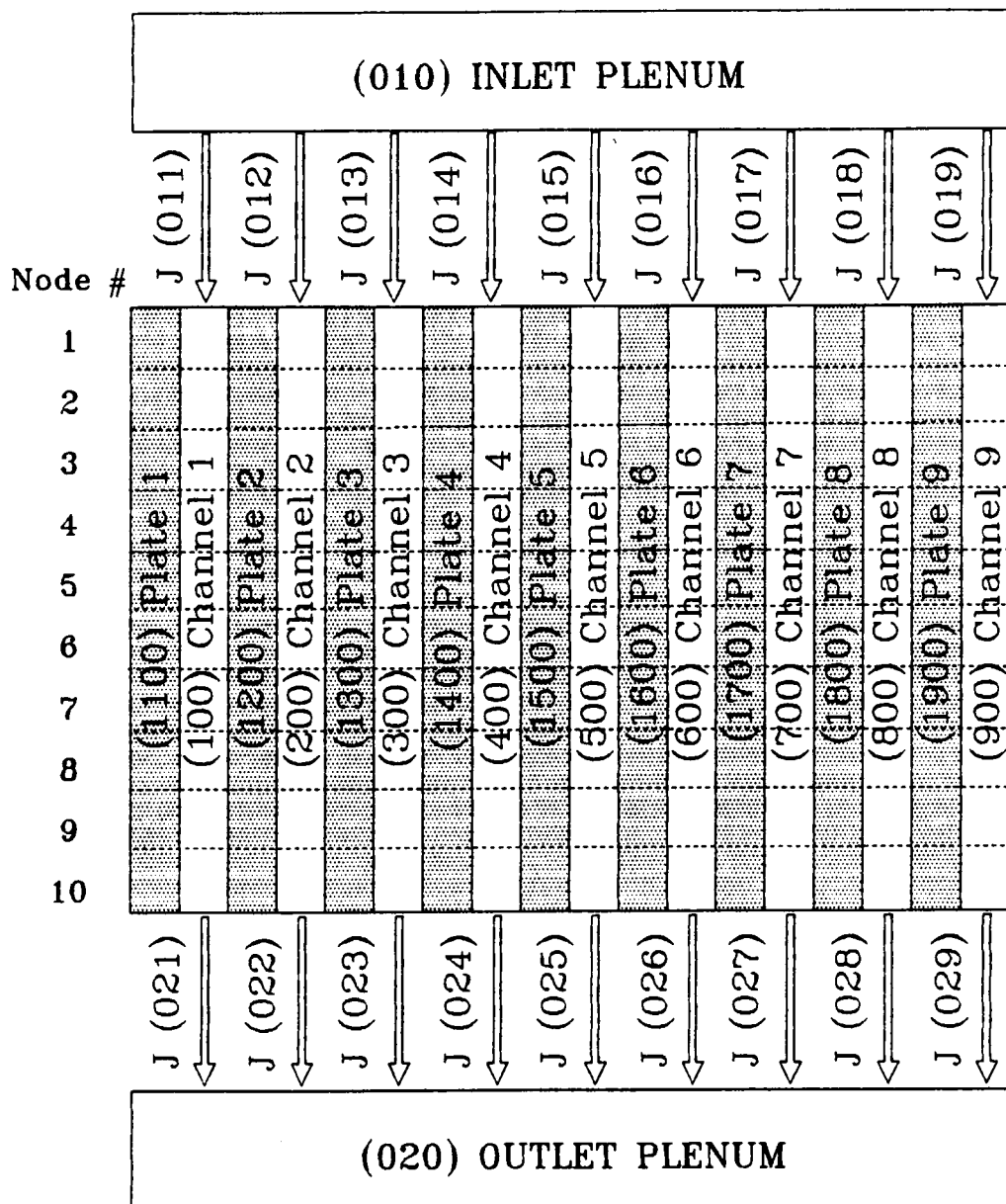


Figure 17 : RELAP5/Mod2 nodalization diagram for the HFIR core model.

Table 19 : RELAP5/Mod2 hydrodynamic and heat structure component descriptions for the HFIR core model.

Component	Description	Type	Vol No
010	Upper (inlet) plenum	TMDPVOL	1
011	Junction between inlet plenum and channel 1	SNGLJUN	---
012-019	Same as above for channels 2 to 9	SNGLJUN	---
021	Junction between channel 1 and outlet plenum	SNGLJUN	---
022-029	Same as above for channels 2 to 9	SNGLJUN	---
020	Lower (outlet) plenum	TMDPVOL	1
100	Channel 1 (10 nodes, 9 junctions)	PIPE	10
200-900	Channels 2 to 9	PIPE	10/each
1100	Fuel plate 1 (10 nodes and 6 mesh intervals)	Heat structure	10
1200-1900	Fuel plates 2 to 9	Heat structure	10/each
Total			92 node 90 hs.

Similar to the case with FDP, the steady-state conditions for the RELAP model are obtained by running an accelerated transient from arbitrary initial conditions with no perturbation until the time derivatives approach zero. Since the formulation of the two models are not identical, an exact match for the initial conditions has not been feasible. In particular, with the RELAP model, flow rates are found to be lower than the actual values after steady-state is reached. Since coolant channels are connected to upper and lower plena through normal junctions, inflow and outflow depend on the pressure difference between the upper and lower plenums and the channel friction losses (pressure driven flow). Therefore, by adjusting the channel surface roughness, the channel flow is adjusted to match the value determined by FDP calculations. However, the surface roughness determined by this adjustment scheme was found to be about five times smaller than the suggested value for the HFIR core.

The two-pressure boundary condition formulation is used for verification of boiling cases. In addition, in order to verify the effect of different boundary conditions on results, the inlet flow rate and outlet plenum pressure combination (Section 3.2.6.1) is also studied for the single-channel single-phase comparisons. For these transients in which the inlet flow rate is specified, the channel inlet junctions which connect the upper plenum to the coolant channels are converted from "single junctions" to "time dependent junctions."

The process of preparing a consistent code model input involves a large number of calculations, and numerous modeling assumptions must be made in the course of developing data. To be consistent in comparisons, similar set of correlations are preferred. In order to improve readability, only a few figures for the key state variable comparisons are presented in the following sections. Additional comparison results with RELAP5/Mod2 are provided in Appendix D.

6.1.1. Single-Phase Flow Comparisons

The comparisons for the single-phase liquid flow are presented in Figures 18 to 21.^k For these comparisons, the inlet flow rate and outlet plenum pressure are specified as boundary conditions. A relatively long period reactivity transient is studied to avoid localized boiling in the coolant channels. This reactivity increase results in a rapid change in power which results in a ten-fold increase from initial steady-state with a proportional rise in fuel and coolant temperatures. A mild primary pressure spike accompanies the power excursion as shown in these figures.

The power excursion data are tabulated and introduced to the RELAP model as a general table. In these figures, the data symbols indicate RELAP results while solid lines indicate results from this model (marked as FDP throughout these comparison plots). For these comparisons only, the Dittus-Boelter correlation is preferred for consistency instead of Hausen correlation for the single-phase heat transfer coefficient. The agreement between the results have been satisfactory. Additional single-phase flow comparisons are provided in Appendix D.

6.1.2. Two-Phase Flow Comparisons

Two separate set of comparisons are provided for the boiling cases: Subcooled boiling and bulk boiling. For both comparisons the two pressure boundary condition is used in which inlet and outlet plenum pressures are specified. Milder reactivity transients are used to avoid CHF situations. A systematic comparison for the CHF have not been made with RELAP code because of the differences in formulation of critical heat flux and post-CHF heat transfer correlations between the two models. RELAP's HFIR model utilizes a modified CHF correlation due to Costa for the flow in HFIR channels.¹⁰⁰ During the dissertation write-up, however, a qualitative comparison is performed for the optimum void accident

^k Single-phase vapor flow comparisons are not performed since it is not encountered in the analysis of HFIR reactivity transients.

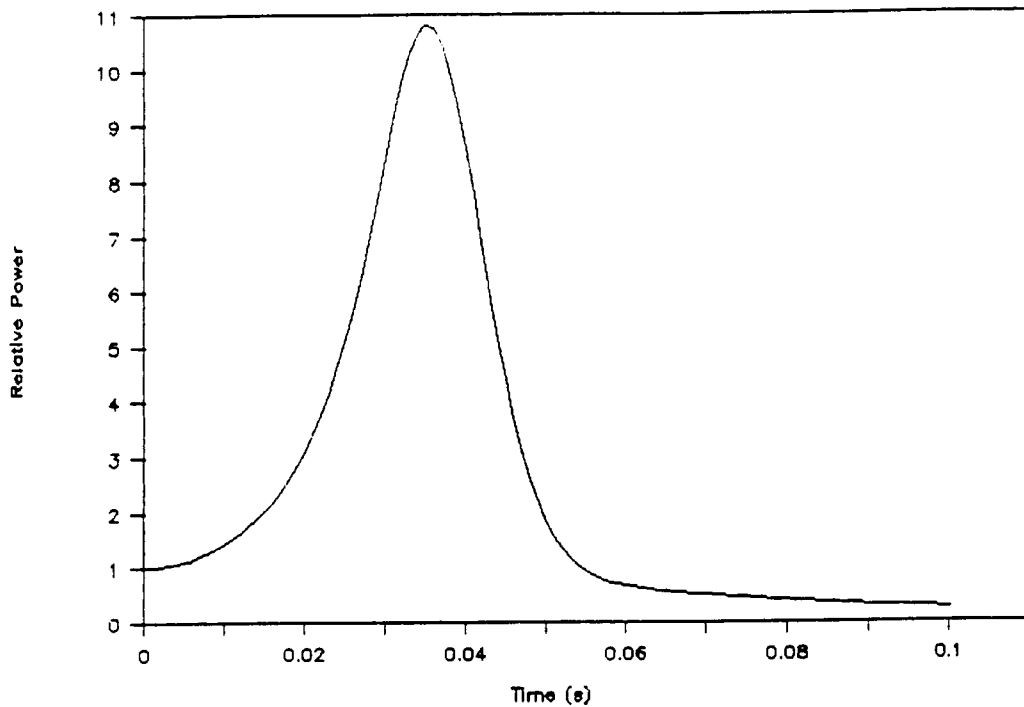


Figure 18 : Power excursion for RELAP5/Mod2 single-phase flow comparisons.

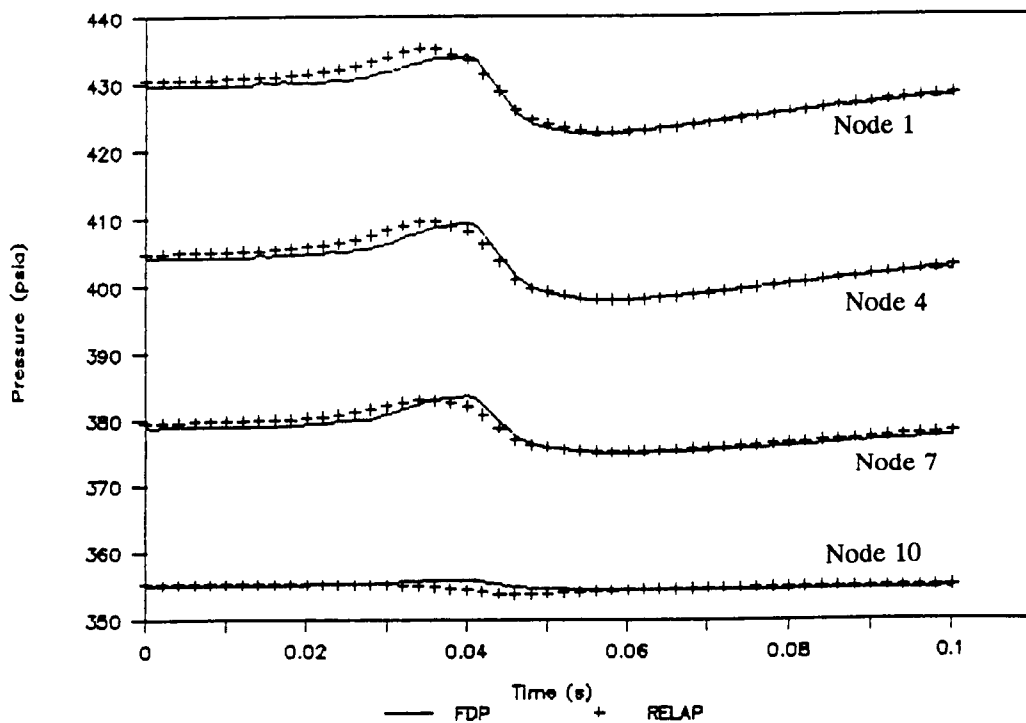


Figure 19 : Channel pressure comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for single-phase flow.

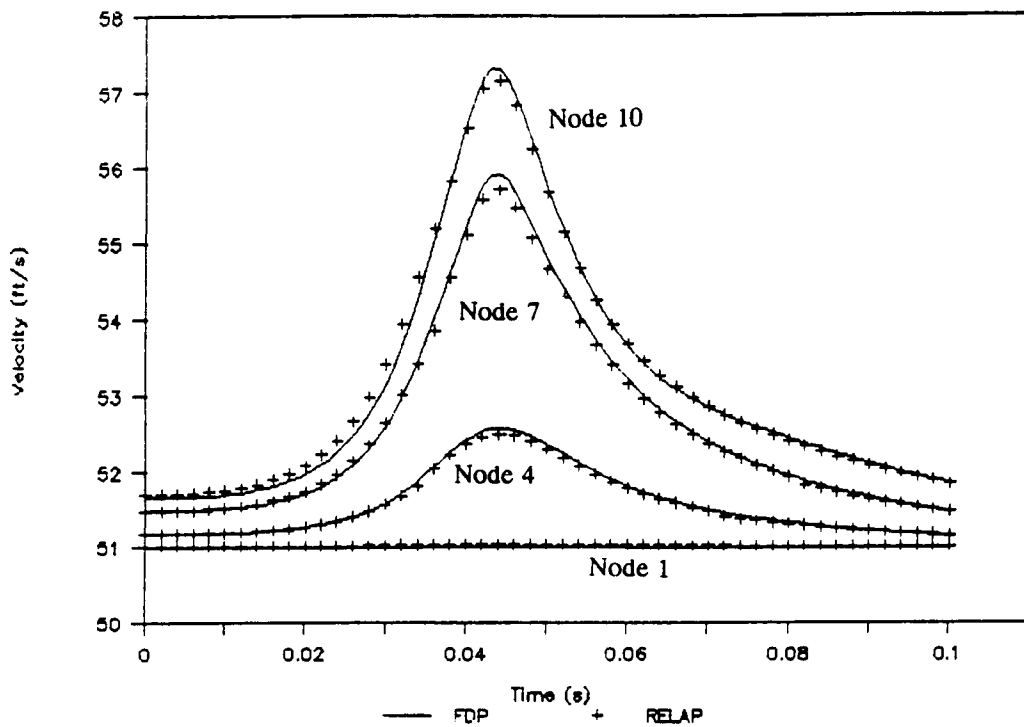


Figure 20 : Coolant velocity comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for single-phase flow.

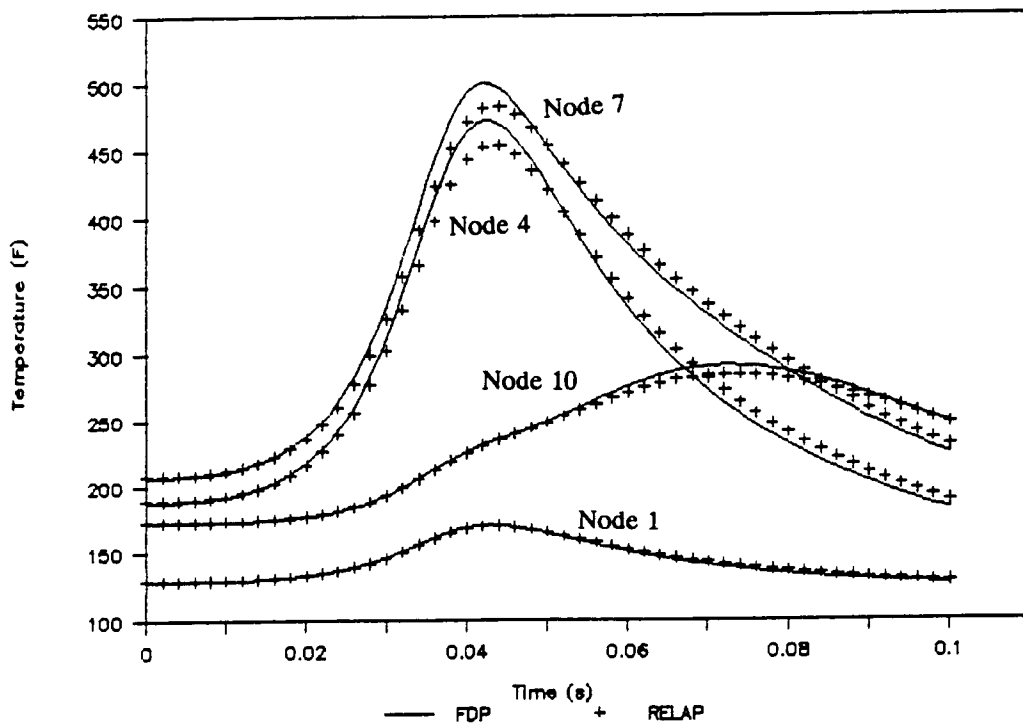


Figure 21 : Fuel temperature comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for single-phase flow.

during which CHF is exceeded for the Mode 1, EOC, full-power transient. Though it is not shown here, a satisfactory agreement is obtained.

In addition to the choice of a mild reactivity transient, coolant channel geometry and fluid flow characteristics are also modified to initiate boiling conditions earlier during the transient without large power generations and high surface heat fluxes. To accomplish this, mainly coolant inlet enthalpy is raised closer to saturation enthalpy and axial power density distributions are modified to give a flat power generation to avoid localized heat-flux peaking. Additional subcooled boiling comparisons are provided in Appendix D.

6.1.2.1. Subcooled Boiling

Subcooled boiling comparisons are presented in Figures 22 to 27. Despite the modifications in coolant channel descriptions and inlet conditions, a wide range of subcooled flow condition comparisons has not been feasible. Since the margin between the incipient boiling heat flux and critical heat flux is quite small, any boiling initiation during a thermal transient immediately leads to a CHF situation in the narrow coolant channels with high coolant velocities. During the transient selected for comparison, the power increases 1.55 times in about 100 msec and settles to an equilibrium state. This marginal increase in power does not create significant changes in the channel pressure nor the coolant velocities. However, a closer inspection of the individual nodal velocities and pressures reveals that the characteristics of these state variables for the two models are significantly different, particularly in the subcooled boiling region. The RELAP results exhibit chaotic oscillations during a small time frame in which the boiling incipience is realized. This discrepancy is primarily attributed to the differences in the momentum equation formulation of the two models.

The RELAP model uses two separate momentum equations along with constitutive equations describing the momentum transfer between the two phases. The model developed in this work, however, is based on a single mixture momentum equation in which the differing phasic velocities are approximated by Bankoff-Jones correlation for slip.^{38,58} The two-momentum equation formulation of the RELAP model provides a more detailed analysis tool

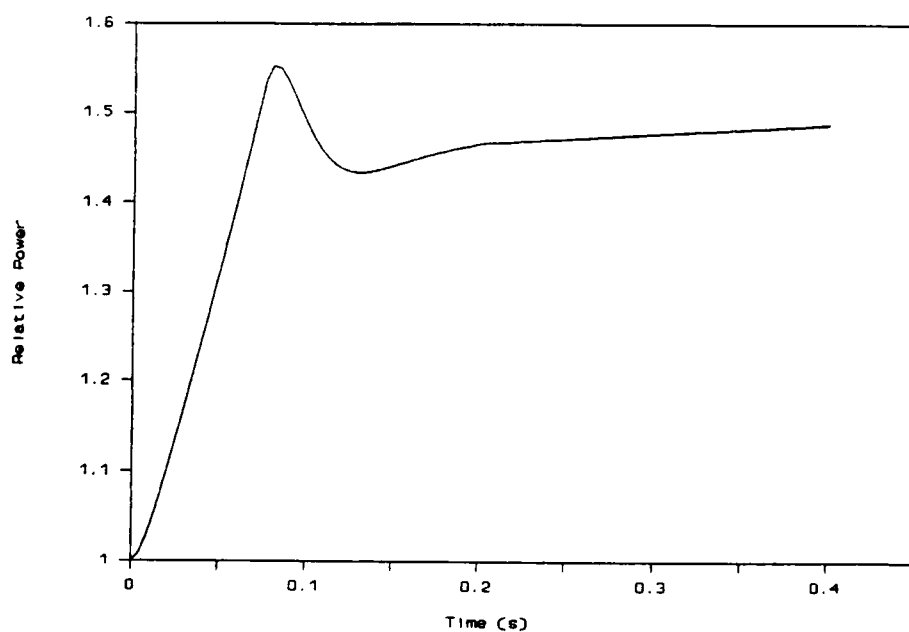


Figure 22 : Power excursion for RELAP5/Mod2 subcooled boiling flow comparisons.

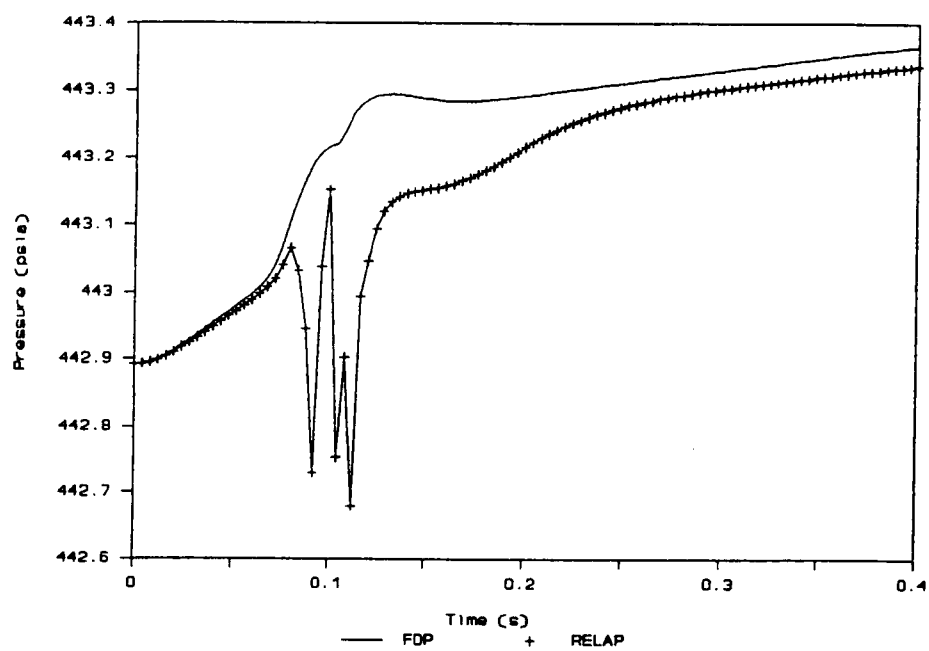


Figure 23 : Inlet (1st) node pressure comparisons with RELAP5/Mod2 for subcooled boiling flow.

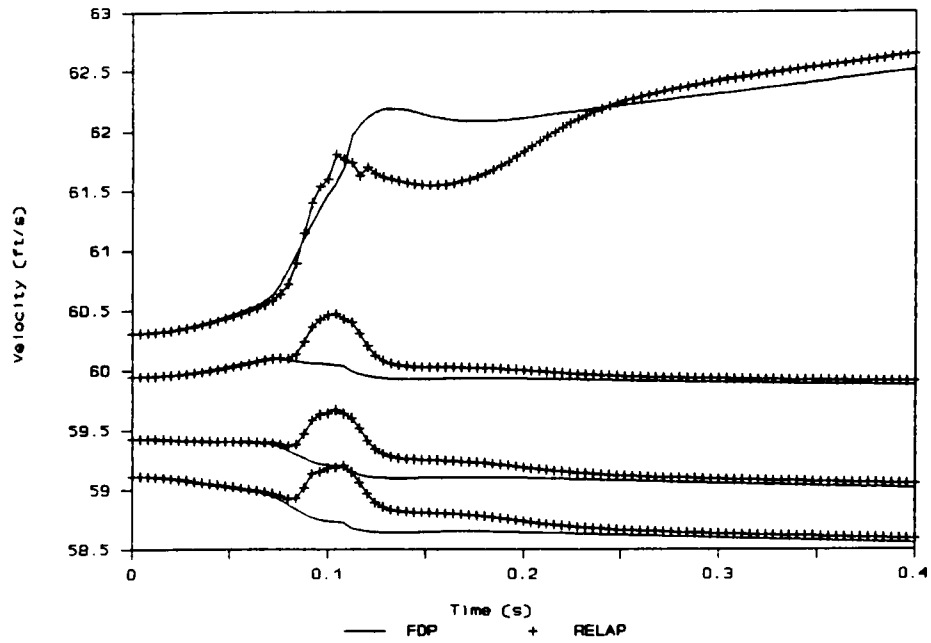


Figure 24 : Liquid velocity comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for subcooled boiling.

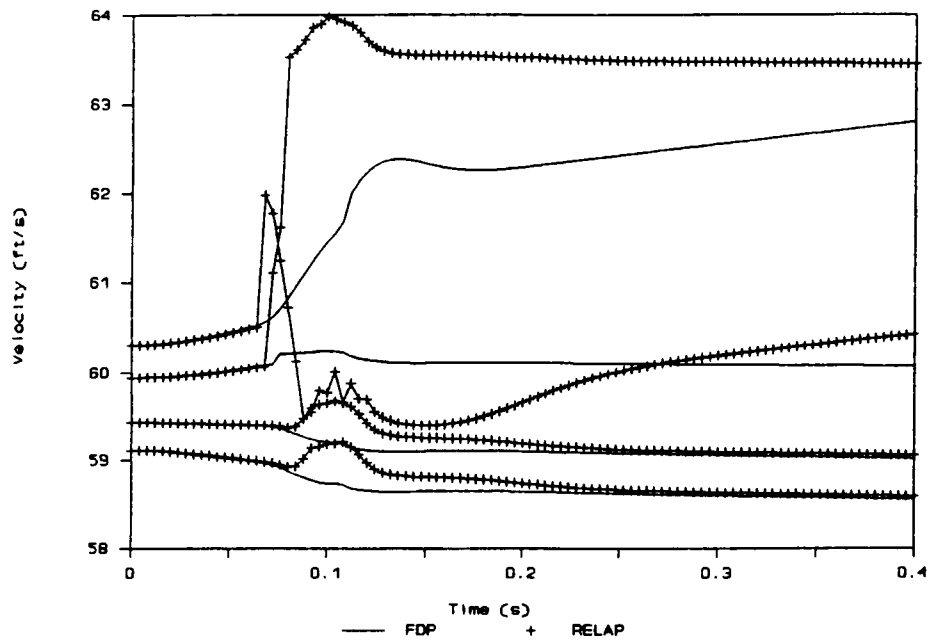


Figure 25 : Vapor velocity comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for subcooled boiling.

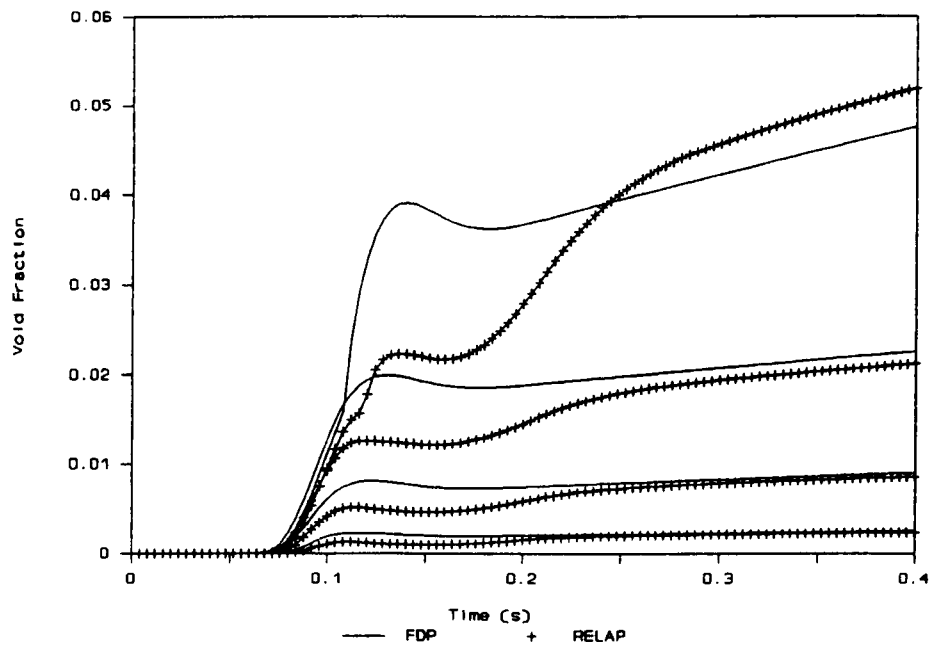


Figure 26 : Void fraction comparisons with RELAP5/Mod2 for last four nodes for subcooled boiling flow.

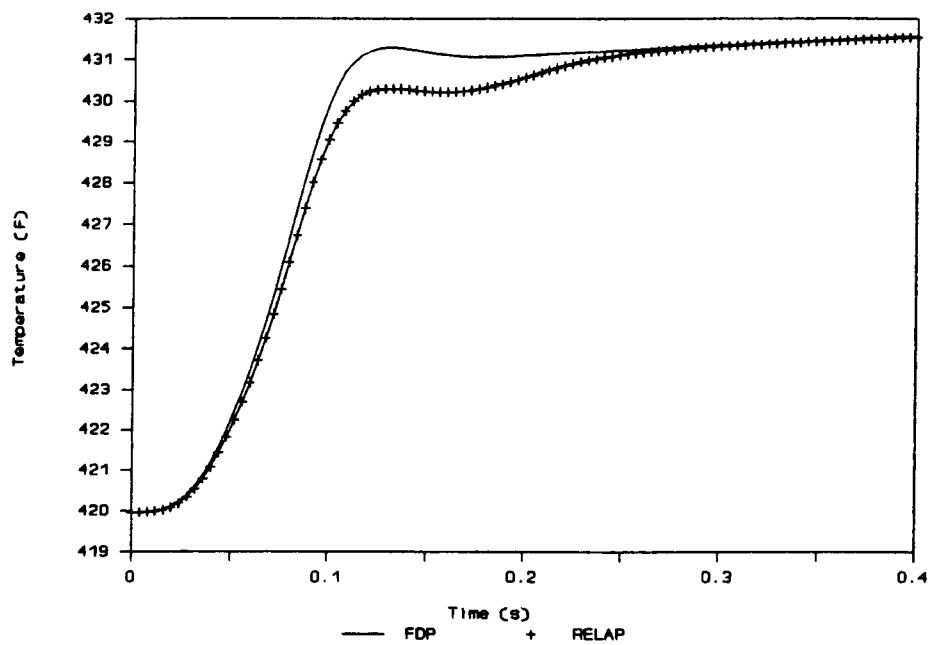


Figure 27 : Outlet node bulk coolant temperature comparisons with RELAP5/Mod2 for subcooled boiling flow.

and the oscillations observed during the subcooled boiling initiation are possibly due to boiling instabilities. The amplitude of these oscillations is not substantial, however, and good agreement is obtained between the two models for the time at which subcooled boiling is initiated. Furthermore, once this transitory region is passed, the long term agreement between the results again becomes satisfactory.

This transitory behavior of the hydraulic variables is similar for related quantities such as void fraction and phasic densities in a similar fashion during the transition to subcooled boiling. The thermal variables, however, largely remain unaffected and, overall, the results between the two models are in fair agreement.

6.1.2.2. Bulk Boiling

Despite extensive efforts, it has not been possible to observe a bulk boiling regime in HFIR-like coolant channels during a thermal transient. Therefore, a different kind of situation is studied as the transient initiator in which a 30 psia pressure drop is considered in the system pressure over a 100 msec. It should also be pointed out that the initial conditions are chosen such that subcooled boiling is observed in the channel before the initiation of the transient. The results of the RELAP comparisons for this case are presented in Figure 28 and 29. Due to the small perturbation, variations in state variables are marginal and an effective comparison between the two models is not feasible in this boiling regime. Since the main focus of the current model has been the analysis of rapid thermal transients, these bulk boiling comparisons are not of particular importance. Furthermore, the studies presented in Section 6.2 indicate that the bulk boiling regime is not realized during the reactivity transients analyzed in this work.

6.2. COMPARISONS with the REACC CODE

The neutronic behavior of the HFIR core model and the feedback mechanism is compared using an earlier HFIR core model, REACC.^{2,3} REACC is a single-phase, lumped-parameter model which was used for the preliminary analysis of reactivity transients for new operating conditions.³ The REACC's model is based on a nodal representation of the fuel

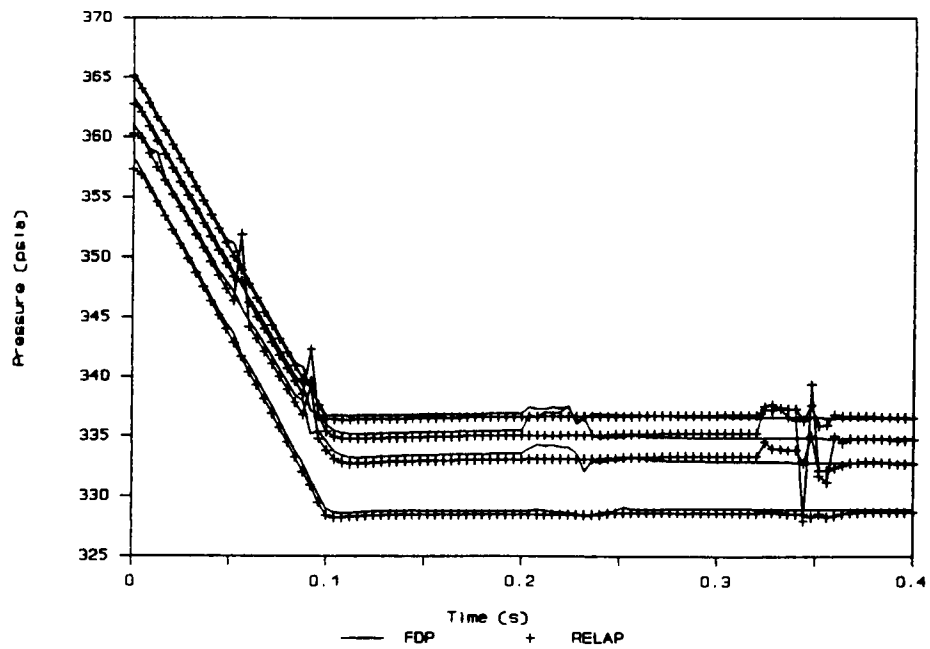


Figure 28 : Channel pressure comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for bulk boiling.

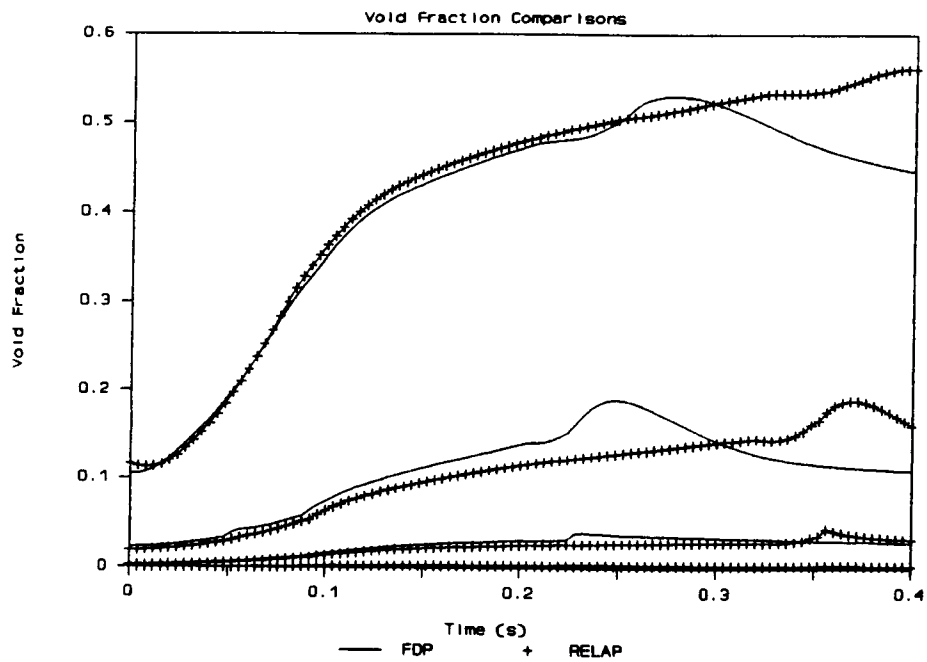


Figure 29 : Void fraction comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for bulk boiling.

and an average coolant channel. Point reactor kinetics is used to model the neutronic behavior of the core. The heat transfer from the fuel to coolant is assumed to take place by conduction, convection, and direct deposition of the neutron and gamma energy in the water. Three sets of data were provided for both beginning and end of fuel cycle; nominal core, hot-streak, and hot-spot conditions. Since REACC's thermal-hydraulic model is limited to single-phase flow, the transients which involve boiling is not adequately analyzed. As an approximation, when the incipient boiling heat flux (a constant parameter in the model) is exceeded, the heat transfer coefficient is reset to zero until the power level drops to a point where rewet of the plates is assumed to occur.

For these comparisons, the REACC regular input data deck is slightly modified using the initial steady-state temperature distributions from the new model, FDP. To minimize the distributed parameter effects, an axially flat power distribution is chosen in the new model. Reactor scram is also considered since the safety system models used in both codes are essentially identical. This also facilitates verification of the safety system response of the new model.

A moderate reactivity transient is chosen to avoid localized boiling in the coolant channels which REACC code is not designed to handle. 1.7 \$ worth of reactivity is introduced to the core in 30 msec for the Mode 1, full-power case at the beginning of cycle conditions. The results of the comparisons with the REACC code are presented in Figures 30 to 34. In these figures, the solid lines represent results from the new model, FDP, and the data points represents REACC results. As seen in these figures, there is a good agreement between the FDP and REACC results for the neutronic power. The comparisons in Figures 31 to 34 are presented in terms of axially averaged values because REACC does not provide space dependent temperatures or heat fluxes. Since REACC is based on a lumped-parameter model in which several key quantities such as coolant density, heat transfer coefficient, etc... are treated as constants, and since it includes many conservative features such as zero post-CHF heat transfer, a close agreement between the thermal-hydraulic results is not expected. Furthermore, since REACC comparisons are primarily performed for verification of the neutronic behavior and thermal-hydraulic feedback mechanisms, an agreement on the time at which the thermal-hydraulic quantities reach their peak value is more important than the

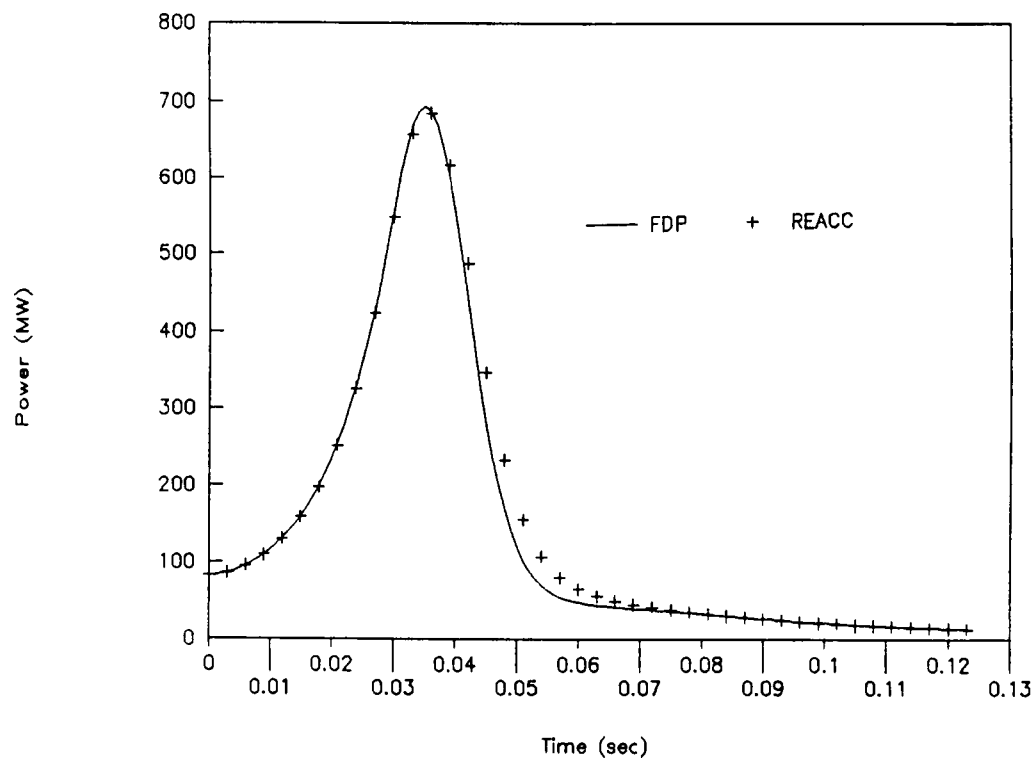


Figure 30 : REACC comparisons: Power excursion.

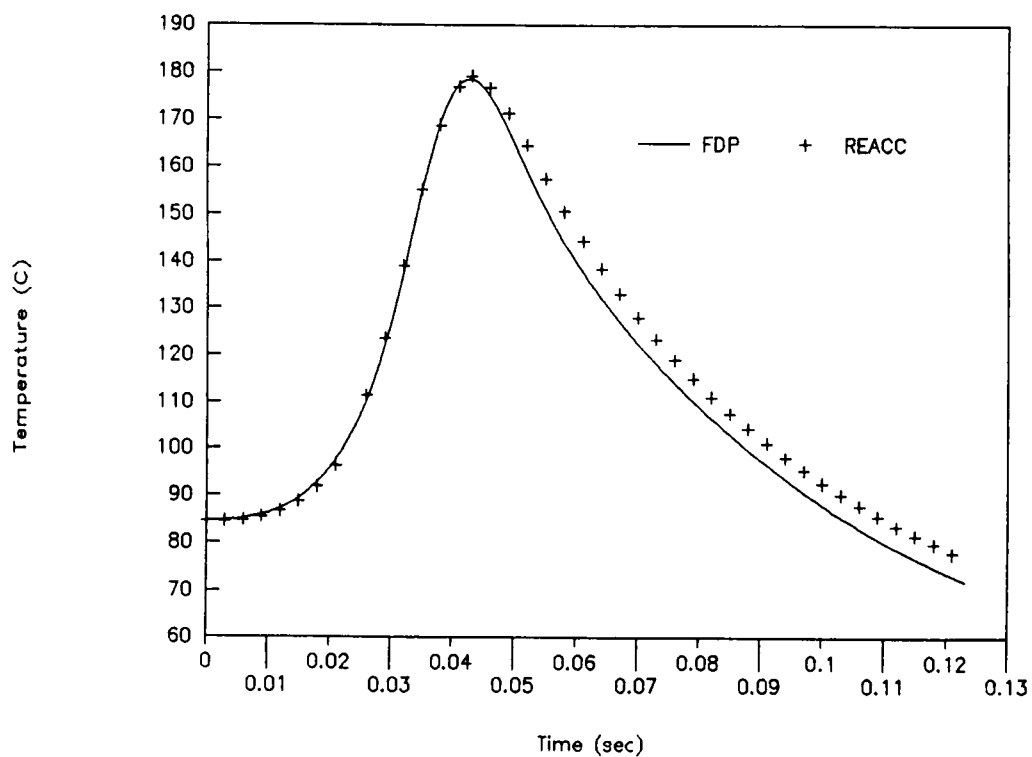


Figure 31 : REACC comparisons: Axially averaged nominal fuel temperatures.

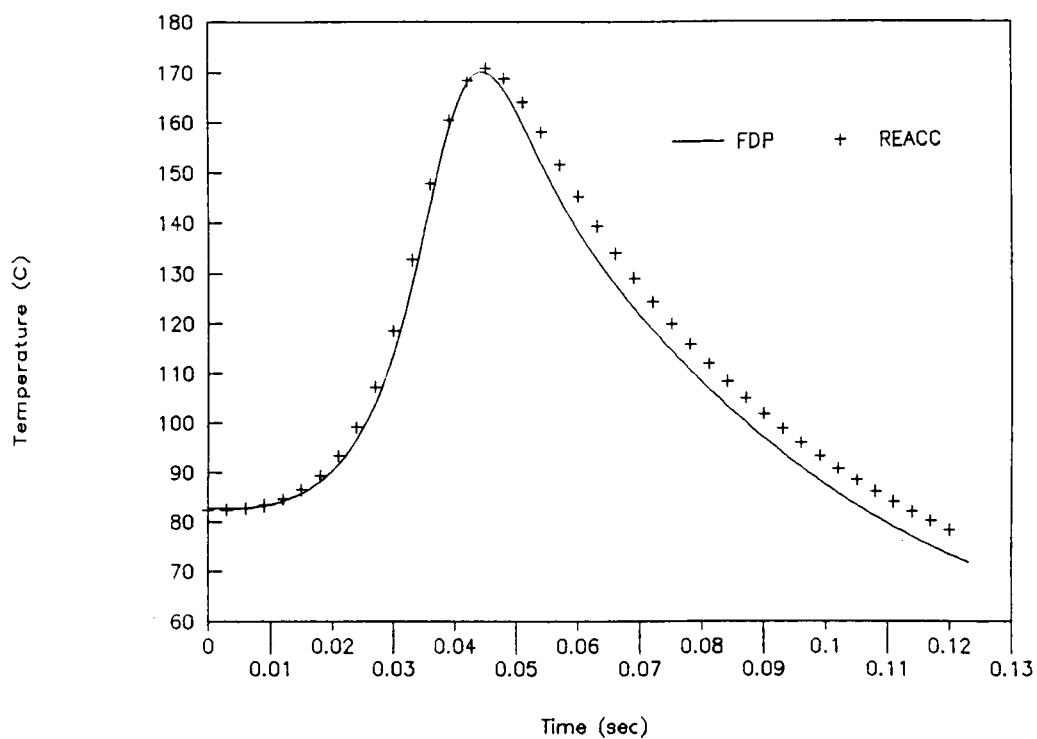


Figure 32 : REACC comparisons: Axially averaged surface temperatures.

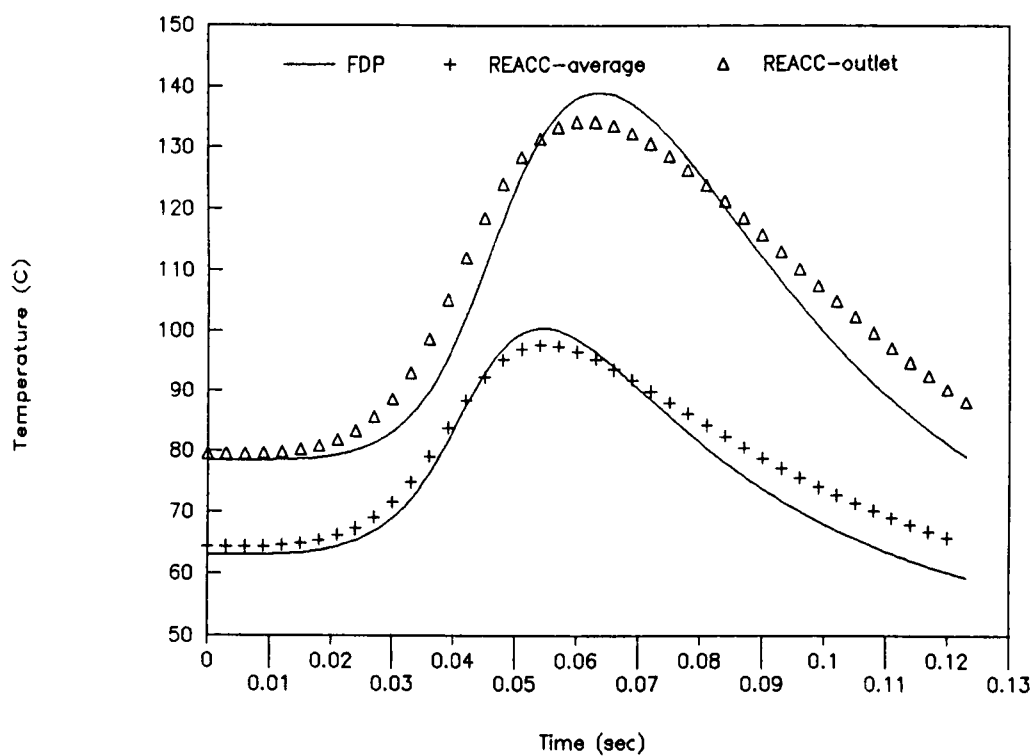


Figure 33 : REACC comparisons: Axially averaged bulk coolant temperatures.

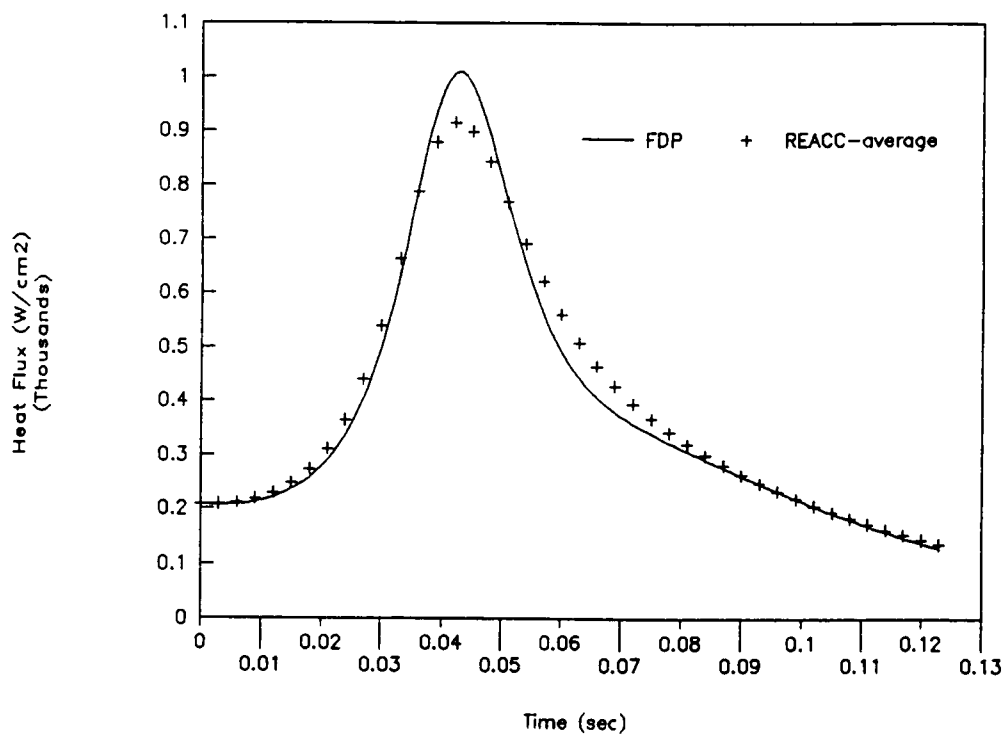


Figure 34 : REACC comparisons: Axially averages surface heat fluxes.

corresponding peak values themselves. Therefore, the agreements between the FDP results and REACC results on thermal quantities are considered satisfactory. The relatively large discrepancies in the coolant temperatures are attributed to the constant heat transfer model of the REACC code.

6.3. SPERT BENCHMARKS

A series of comparisons have been previously performed for verification of the earlier HFIR core kinetics code, REACC, against Short Period Excursion Reactor Transients, SPERT-II.⁹ The results of these comparisons were published and good agreement between the calculated and experimental results were reported.¹⁴⁴ In an attempt to verify the overall thermal-hydraulic behavior of the new model, this earlier comparative work is repeated here.

The new HFIR transient model with appropriate SPERT input data is used to calculate specific SPERT-II cases. In order to facilitate these comparisons, several components of the new code such as various feedback mechanisms are removed from the model, and only a nominal single-channel analysis is performed. Several comparisons are performed for the SPERT-II transients with different step reactivity insertions, coolant inlet enthalpies, and flow rates. Relatively long period transients have been selected to avoid destructive tests. The results of these comparisons are presented in Figures 35 and 36. In all cases considered here, the initial steady state power is assumed to be 0.5 MW. The results for the transients with different step reactivity insertions exceeding prompt criticality exhibit good agreement as seen in Figure 35. For all cases shown in Figure 35, the inlet flow rate is 14 ft/sec and the inlet coolant temperature is 82 °F. Similarly, calculated power and SPERT-II data are in good agreement for transients with different coolant inlet velocities for a specified reactivity insertion and coolant inlet temperature. For the transients in Figure 36-(a), a step insertion of 1.16\$ worth of reactivity is considered with the coolant inlet temperature at 82 °F. For all the cases considered above, the coolant is sufficiently subcooled such that thereactivity insertions do not cause boiling in the channel. The agreement between the calculated and the experimental results is satisfactory for these single-phase flow transients.

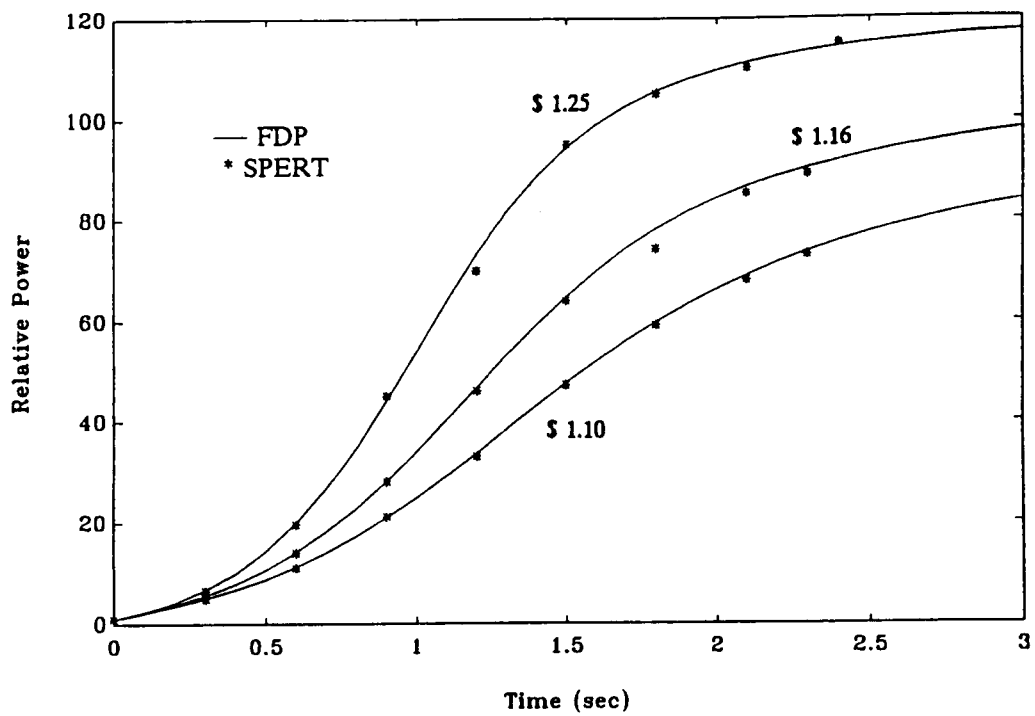


Figure 35 : Spert-II comparisons: Calculated and measured powers for different step reactivity insertions.

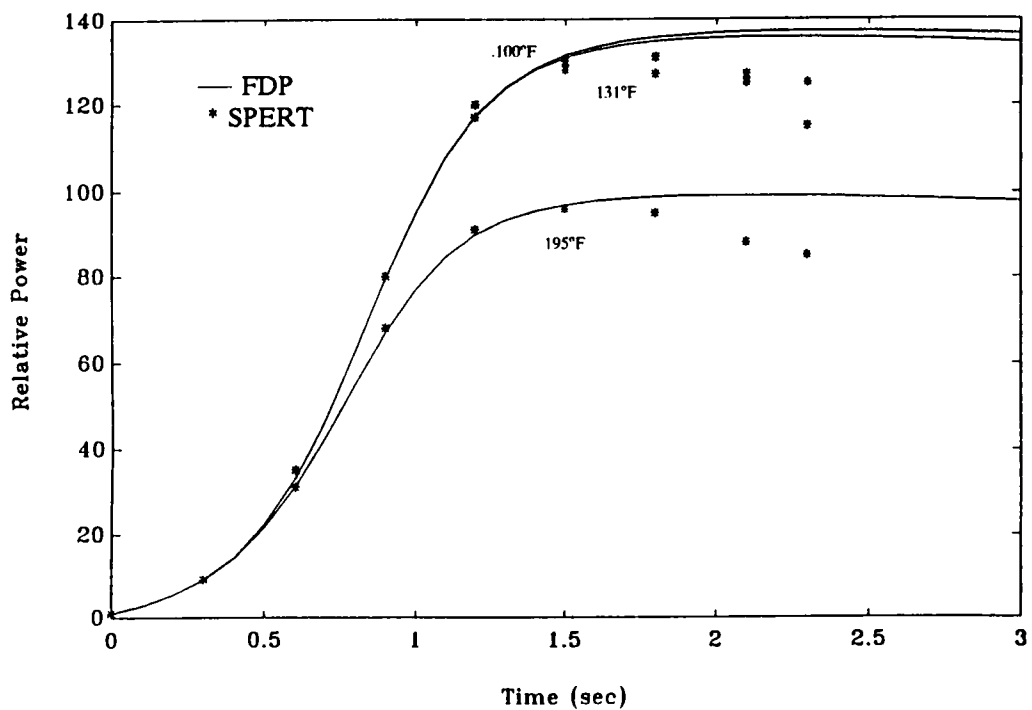
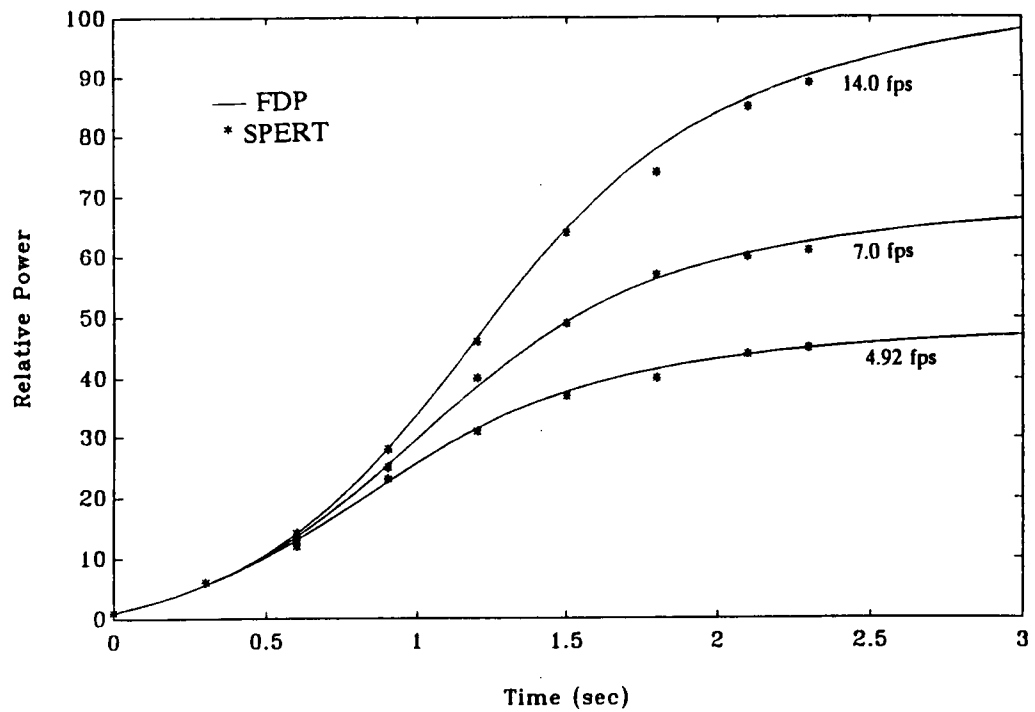


Figure 36 : Spert-II comparisons: Calculated and measured powers for; (a) different coolant inlet velocities, (b) different inlet temperatures.

A series of other SPERT-II tests are also studied to compare the behavior of the model for the cases which involve boiling in the coolant channels. In these cases, a step insertion of 1.38 \$ reactivity is considered for different coolant inlet temperatures with the inlet flow rate 14 ft/sec. These comparisons are presented in Figure 36-(b). As seen in the figure, the discrepancy between the calculated and experimental results becomes larger as the power approaches its peak. At this point, void reactivity feedback brings the reactor power down as observed in test results. Calculated results, on the other hand, exhibit a milder turn, perhaps due to a weaker reactivity feedback coefficient. It should be pointed out that the model developed in this work indicates only mild subcooled boiling for these cases. Due to unavailability of information regarding the extent of boiling in these transients and void reactivity feedback for SPERT-II reactor, more detailed comparisons which involve boiling has not been possible.

7. ANALYSIS OF HFIR REACTIVITY ACCIDENTS

A brief description of the HFIR transients and associated mechanisms which increase or redistribute core reactivity were initially provided in Chapter 2. In this section, the analysis of these reactivity transients is presented. The initiating events, expected frequency, and associated reactivity insertion rates are identified and reported along with the results for each transient. It should be noted, however, that it is not intended to investigate all possible HFIR reactivity transients in this work. Only the power excursions that can be triggered by credible events are considered. A thorough analysis of HFIR reactivity transients is currently underway as a separate task by other analysts as part of the HFIR safety analysis update studies.

HFIR PRA analysis^{12,13} is used as a tool to provide a basis for defining the frequencies of each initiating event. The transients are classified based on their relative frequency as events with moderate frequency, infrequent events, and limiting faults. The events with moderate frequency are defined as those which can be expected in any given year with an overall frequency of greater than 1.0×10^{-1} /year. The infrequent events are defined as those which can be expected during the lifetime of the plant. These events are expected to occur with frequencies between 1.0×10^{-1} /year and 1.0×10^{-4} /year. And finally, the limiting faults are events which are not expected to occur during the plant lifetime, but are included in the analysis to ensure that the consequences of the most severe initiating events are known. These events are characterized by the frequencies between 1.0×10^{-4} /year and 1.0×10^{-7} /year. The events with frequencies less than 1.0×10^{-7} /year are not considered in this work. In addition to initiating events, some unprotected sequences are also considered regardless of their frequency classification.

The design requirements and transient acceptance criteria generally impose that the fuel (and cladding) structural integrity needs to be maintained throughout the design lifetime for events with moderate frequency. For the infrequent events, the requirements reduce to the maintaining the fuel integrity during such an event only. For the limiting faults, however, some local fuel melting is generally acceptable as long as a coolable geometry is maintained.

The transients considered in these sections are studied up to the point of fuel damage. The questions which relate to maintaining core coolability are addressed in Section 6.3.

The envelope of the HFIR reactivity transients covered in this work is studied in three sections based on the initiating events as described in References 11 and 14. These sections are for control system related accidents, cold water accidents, and accidents associated with target voids. Each transient under these headings is further categorized according to mode of operation and the initial conditions in each mode such as initial reactor power and coolant flow rate. Basically, five distinct reactor operating states are identified. These are;

- i. **Mode 1**, full-power, full-flow,
- ii. **Mode 1**, low-power, full-flow,
- iii. **Mode 1**, low-power, low-flow,
- iv. **Mode 2**, and
- v. **Mode 3**.

Each of these five operating states are described by particular initial conditions and associated safety system settings as shown in Table 20.¹ The typical initial steady-state settings for some key state-variables along the axial length of the coolant channels and fuel plates are presented in Appendix E for each operating state.

As described in Section 2.1.3, **Mode 1** is the normal pressurized operating mode at full coolant flow and also at 10% of full-flow. At full-flow, the power can range from zero to 85 MW. The power level at 10% flow can range from zero to 10 MW (power outage condition). **Mode 2** provides for low-power operation at 10% shutdown coolant flow and low-pressure. In this mode, the reactor power level is limited to 2.5 MW consistent with the available coolant flow and low-pressure. **Mode 3** permits very low-power operation (100 kW) with no forced flow and the low-pressure (vessel head open). **Mode 3** is not considered in this analysis with the exception of the cold water accident since it requires special modeling

¹ The values presented in Table 20 are for nominal operating conditions and they are different from the *Limiting Safety System Settings*.

Table 20 : Operational characteristics and safety system settings for five distinct operating states of the HFIR.^{1,3}

	Mode of Operation				
	Mode 1			Mode 2	Mode 3
	Full-Power, Full-Flow	Low-Power, Full-Flow	Low-Power, Low-Flow		
Maximum power (MW)	85	10	10	2.5	0.1
Coolant Flow (gpm)	16,000.	16,000.	1,900.	1,900.	---
Nominal Channel flow rate (ft/s)	51	51	5.1	5.1	---
Inlet vessel pressure (psia)	468	468	250	15-250	15
Inlet temp. (F)	120	120	120	120	212
Power level trip set point (MW)	102	102	12	3.25	0.130
Rate trip set point (MW/sec)	17	17	17	Disconnected	
Scram delay (msec)	10.04	10.04	10.04	10.04	45.

for the natural circulation loops. No limits are given in the technical specifications or operating procedures for temperature, pressure, or flow for **Mode 3**, so the primary temperature is assumed to be 212 °F, and pressure is assumed to be atmospheric as initial conditions during the cold water accident. It should be noted that the parameters regarding the initial conditions in each operating state are **not**, in general, consistent with "*HFIR Transient Descriptions*" which is used as the basis for safety analysis report update studies.¹¹

The trip set points for **Mode 1** are much higher than those for **Mode 2 and 3** allowing for larger power overshoots during an excursion. However, **Mode 1** can not be assumed to be the bounding case since the thermal-hydraulic conditions in **Mode 1** are typically better than **Mode 2 and 3** conditions.

During a typical HFIR transient, the rapid reactivity insertion results in rapid power changes with a proportional increase in fuel and coolant temperatures accompanied with a primary pressure spike. Normally, when reactor power exceeds 110% of the limit for that particular operating state, a reverse of the shim elements will be initiated automatically using the shim drive motors. In addition, a fast reverse of the shim-safety plates using the air motors is expected in **Mode 1** if the flux-to-flow rate ratio reaches a value of 1.1. These two reverse modes were provided to counter a shim withdrawal event and to match the power to the available coolant flow during a primary pump coastdown.¹¹ Although the failure frequency for the shim reverse is quite small (9.2×10^{-3} per demand) and it clearly provides some degree of redundancy, this action is not typically fast enough to counter rapid reactivity insertions. Therefore, the shim reverse is conservatively omitted in this analysis.

The only effective protection against rapid reactivity accidents is the reactor safety system. The reactor scrams when power exceeds 120% of the allowable power in each operating state. In addition, a rate trip is actuated as a separate scram mode when power rate of change is higher than 17 MW/sec in **Mode 1**. However, in order to eliminate unnecessary scrams due to small power spikes during routine operation, the rate trip is actuated only when it is accompanied by at least a 5 MW increase in power. The failure frequency for these reactor trips are assumed to be bounded by the frequency for a pressure actuated trip which is 6.1×10^{-5} per demand.^{11,12} Any sequence which would include failure of the trips, therefore,

would have insignificant frequency. Nevertheless, some unprotected transients are also considered in this work.

For the beginning of cycle (BOC) conditions, the safety and regulating control elements are assumed symmetric and the distance the plates are withdrawn from the core for initial criticality is conservatively taken as 18.54 inches as an extreme case.⁸¹ For the end of cycle (EOC) conditions, plates are assumed fully withdrawn to simulate the least favorable case. The reactivity worth of the control plates as a function of plate position is discussed in Section 4.4.

Since a typical reactivity transient may be limiting at any initial condition set such as initial power, coolant inlet temperature, system pressure, and coolant flow rate, wide ranges of parametric analyses are needed to determine the bounding initial conditions. This, however, requires more comprehensive work. Therefore, after analyzing the basic features of the reactivity insertion accidents, the worst credible accident is identified and further studied at the end of this section to determine the limiting factors.

7.1. ACCIDENTS ASSOCIATED with CONTROL SYSTEM

As described in Section 2.1.2, the HFIR control system is composed of two concentric cylinders which are withdrawn evenly in opposite directions to increase the reactivity of the core. The outer shim-safety cylinder is divided into four quadrants each of which is driven by a shim motor. The inner shim-regulating cylinder is a single piece with no scram function. This cylinder can be moved with shim motors in parallel with the safety quadrants, but it is also connected to the servo system which adjusts the position of the cylinder up to an inch to counter the changes in power demand. The shim drive speed for the HFIR control elements is 0.008 ft/sec. The regulating drive speed for the inner shim-regulating cylinder is 0.021 ft/sec.

Basically two types of control system accidents are considered: Uncontrolled control element withdrawals and mechanical failure of control element drive rods.¹⁴ The reactivity addition rates associated with each case are calculated using the differential reactivity worth

curve presented in Figure 15. The soluble poison is not used in HFIR except for emergencies; therefore, poison dilution events are not of concern.

7.1.1. Uncontrolled Withdrawal of Control Elements

Perhaps the simplest scenario regarding accidents associated with the control elements is commonly known as a startup accident. A failure within the control system or an operator error may result in an uncontrolled withdrawal of the control elements out of the core. The frequency of an uncontrolled shim withdrawal event is estimated to be 4.0×10^{-2} /year; therefore it is considered infrequent. The unprotected sequence for this event has a frequency of 2.4×10^{-6} /year which classifies it as a limiting fault.¹¹

The thermal-hydraulic conditions and safety system settings for this transient depend on the mode of operation. It is necessary to consider withdrawals in all modes of operation over a range of initial powers for both the beginning and the end of fuel cycle conditions.

7.1.1.1. Withdrawals With Maximum Shim Drive Speed

In **Mode 1** at full-power with the control rods initially at the clean (BOC), symmetrical, critical core position, the total differential worth of all five control elements is about 1.8 \$/in (Figure 15). For this initial rod position, the reactivity insertion rate associated with all five control elements being withdrawn at the maximum shim drive speed is presented in Figure 37. At the end of fuel cycle, the maximum reactivity addition due to runaway control elements would be much smaller simply because, by definition, control elements are assumed fully withdrawn at the end of core life for **Mode 1** full-power transients.

In **Mode 2**, the control system remains in the **start** condition, but the automatic group withdrawal of the shims is blocked so that the manual operation is necessary. Therefore, an uncontrolled withdrawal of control elements can only be due to an operator error. For **Mode 1** low-power and **Mode 2** transients, the differential worth of the control elements is at its maximum (2.0 \$/in) which results in a faster reactivity insertion rate as shown in Figure 38. In addition, since the margin to withdraw is larger, the total amount of reactivity to be

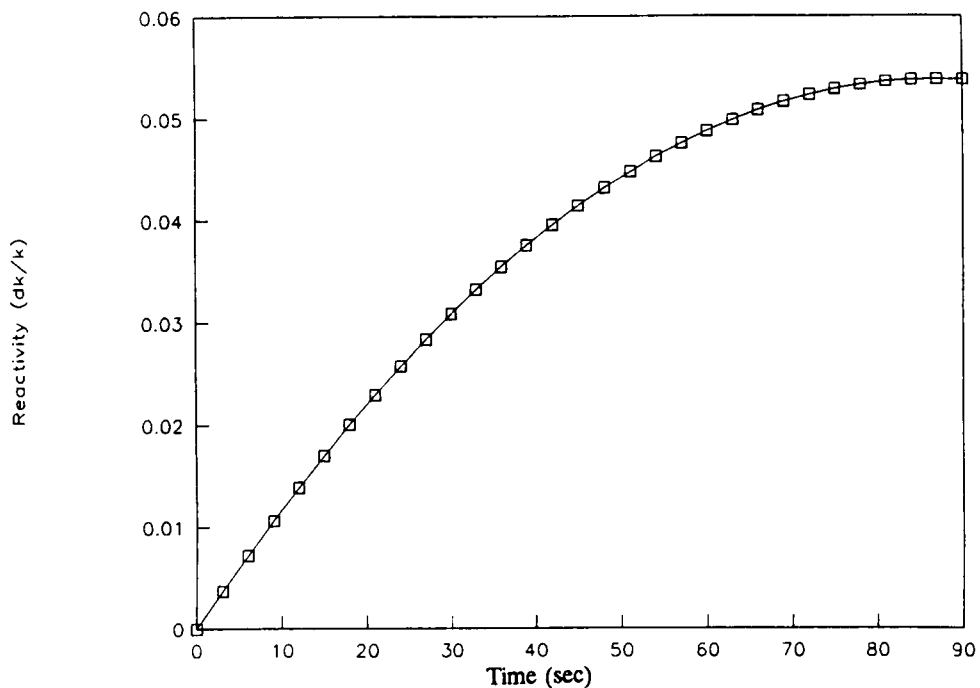


Figure 37 : Reactivity insertion rate for uncontrolled withdrawal of all five control elements at maximum shim drive speed in Mode 1.

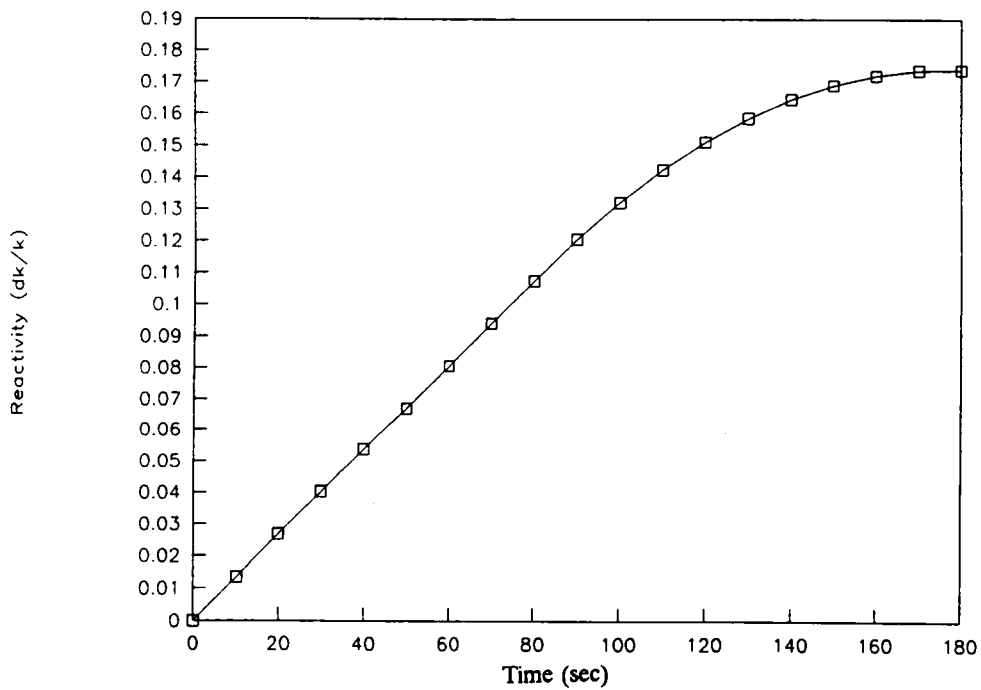


Figure 38 : Reactivity insertion rate for uncontrolled withdrawal of all five control elements at maximum shim drive speed during Mode 1 - low-power, and Mode 2 transients.

inserted is also larger for these low-power transients.

The results for these uncontrolled shim withdrawal sequences are presented in Tables 21 and 22. The peak powers in each operating state and time integrated power are presented in Table 21. In this and subsequent tables for other reactivity transients, the "differential time integrated power" indicates the extra energy released due to the reactivity insertion. In other words, it is the difference between the total energy released during the transient and the energy which would be released if no reactivity addition had occurred. The peak average fuel temperatures for nominal and hot-plate, and peak bulk coolant temperatures for the nominal and hot-channels are presented in Table 22. Since the reactivity insertion rate is relatively small for this transient, the rate trip is not triggered except in Mode 1, low-power, full-flow cases, and peak values are dominated by those at the moment of reactor scram due to the level trip. This typical behavior is displayed in Figures 39 to 42 for the BOC, Mode 1, full-power transient. The results in these figures are for the inner assembly nominal-channel and nominal-fuel plate only. As seen in these tables and figures, the peak values are well within tolerable limits and, therefore, this sequence does not constitute a threat to fuel integrity for the design lifetime in all modes of operations.

7.1.1.2. Withdrawals With Shim and Regulating Drives

In another case of interest, both inner and outer cylinders are considered to be driven out of the core with the maximum speed of the shim drives while the inner regulating cylinder is driven out with the regulating drive speed which is superimposed on the shim drive speed. Since the regulating drive is 2.6 times faster than shim drive, this transient results in a faster reactivity insertion rate than the previous case. However, since the servo system can move the inner shim-regulating cylinder only up to an inch, the additional regulating drive speed makes its impact for the first 3 seconds only, and the combined reactivity insertion rate curve is not significantly different from the previous cases in the long term as shown in Figures 43 and 44.

The results for this sequence are presented in Tables 23, 24 and Figure 45. As seen in Figure 45, the increase in the reactivity addition rate triggers the rate scram much sooner

Table 21 : Peak powers and energy released during an uncontrolled shim withdrawal accident in which all five control elements are driven out of the core with the maximum shim drive speed.

OPERATING STATES	Peak Power (MW)	Time Integrated Power (MW-sec)	Differential Time Integrated Power (MW-sec)
BOC, Mode 1, Full-Power, Full-Flow	102.0	123.6	10.9
BOC, Mode 1, Low-Power, Full-Flow	36.4	65.3	30.3
BOC, Mode 1, Low-Power, Low-Flow	12.0	11.0	0.96
BOC, Mode 2	3.29	3.38	0.42
EOC, Mode 1, Low-Power, Full-Flow	29.8	53.3	21.3
EOC, Mode 1, Low-Power, Low-Flow	12.0	9.50	0.80
EOC, Mode 2	3.25	3.15	0.37

Table 22 : Peak temperatures for uncontrolled shim withdrawal accident in which all five control elements are driven out of the core with the maximum shim drive speed.

OPERATING STATES		Peak Average Fuel Temperatures (F)		Peak Bulk Coolant Temperatures (F)	
		Nominal Plate	Hot Plate	Nominal Channel	Hot Channel
BOC, Mode 1, Full-Power, Full-Flow	Inner	225.7	268.3	173.9	195.8
	Outer	232.6	277.9	173.9	196.0
BOC, Mode 1, Low-Power, Full-Flow	Inner	160.7	176.8	139.8	147.9
	Outer	163.5	180.6	139.8	148.0
BOC, Mode 1, Low-Power, Low-Flow	Inner	209.3	238.8	180.6	199.1
	Outer	213.2	244.3	180.6	199.3
BOC, Mode 2	Inner	145.9	155.7	136.6	142.4
	Outer	147.2	157.6	136.6	142.5
EOC, Mode 1, Low-Power, Full-Flow	Inner	156.1	171.3	139.4	147.6
	Outer	155.7	170.3	139.4	147.6
EOC, Mode 1, Low-Power, Low-Flow	Inner	218.5	251.7	191.1	214.5
	Outer	219.1	252.2	191.1	214.6
EOC, Mode 2	Inner	148.6	159.5	139.4	146.6
	Outer	148.6	159.3	139.4	146.6

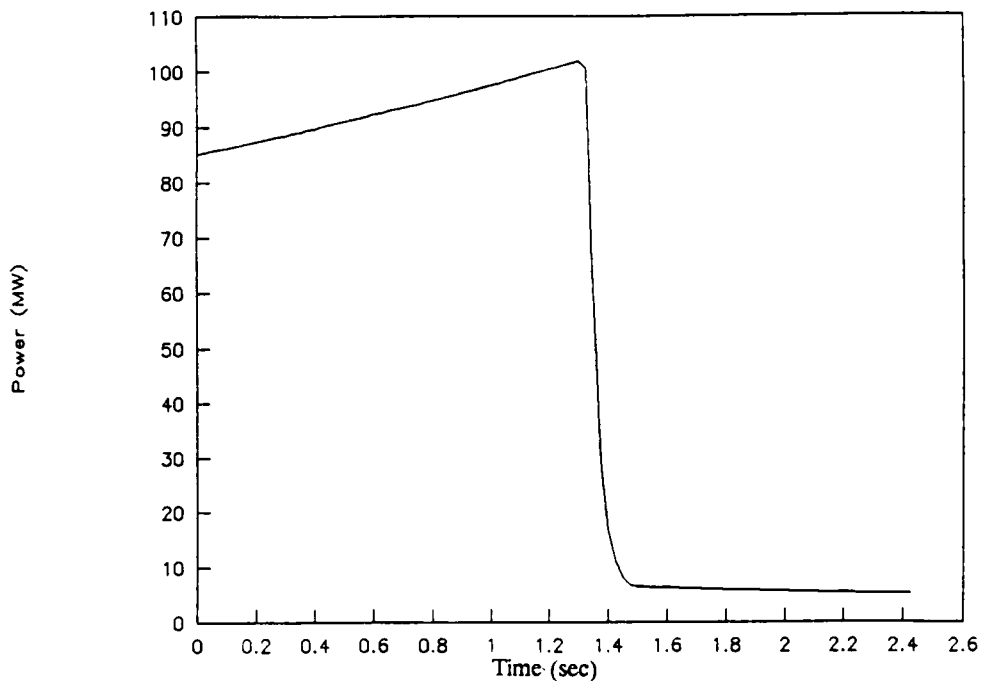


Figure 39 : Power excursion for uncontrolled shim withdrawal of all five control elements at maximum shim drive speed for BOC, Mode 1, full-power transient.

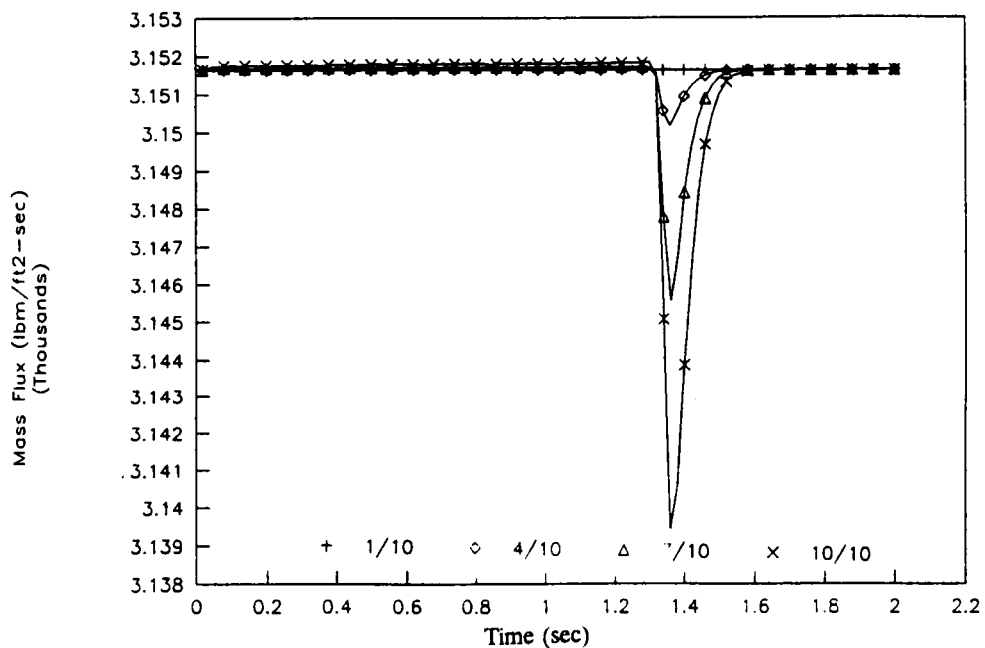


Figure 40 : Inner-element nominal-channel mass fluxes for nodes 1, 4, 7, and 10 during uncontrolled shim withdrawal of all five control elements at maximum shim drive speed for BOC, Mode 1, full power transient.

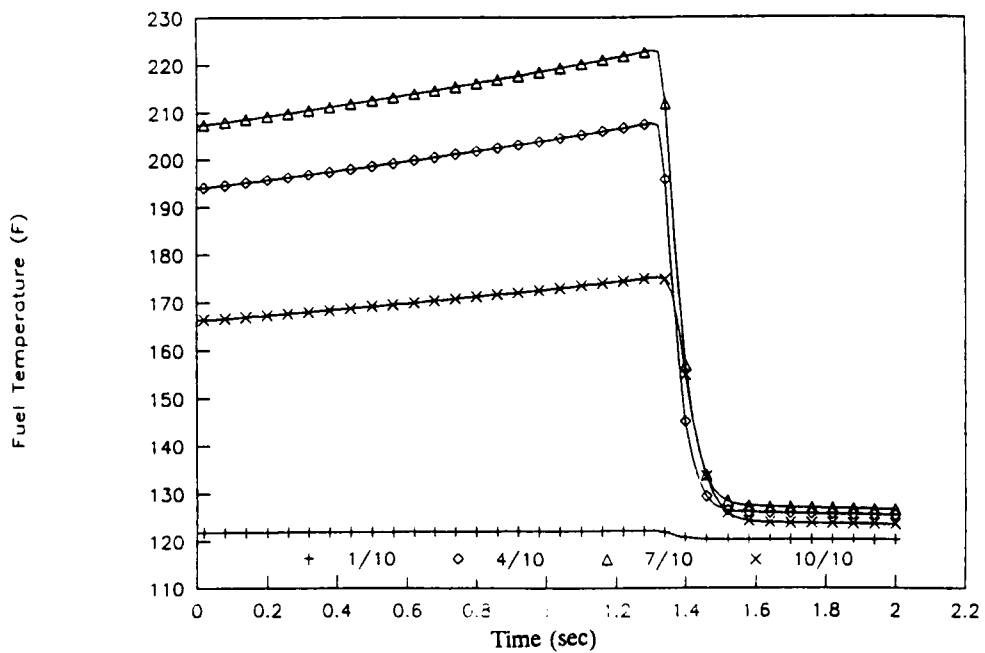


Figure 41 : Inner-element nominal-plate average fuel temperatures for the 1, 4, 7, and 10th nodes during uncontrolled shim withdrawal of all five control elements at maximum shim drive speed for BOC, Mode 1, full power transient.

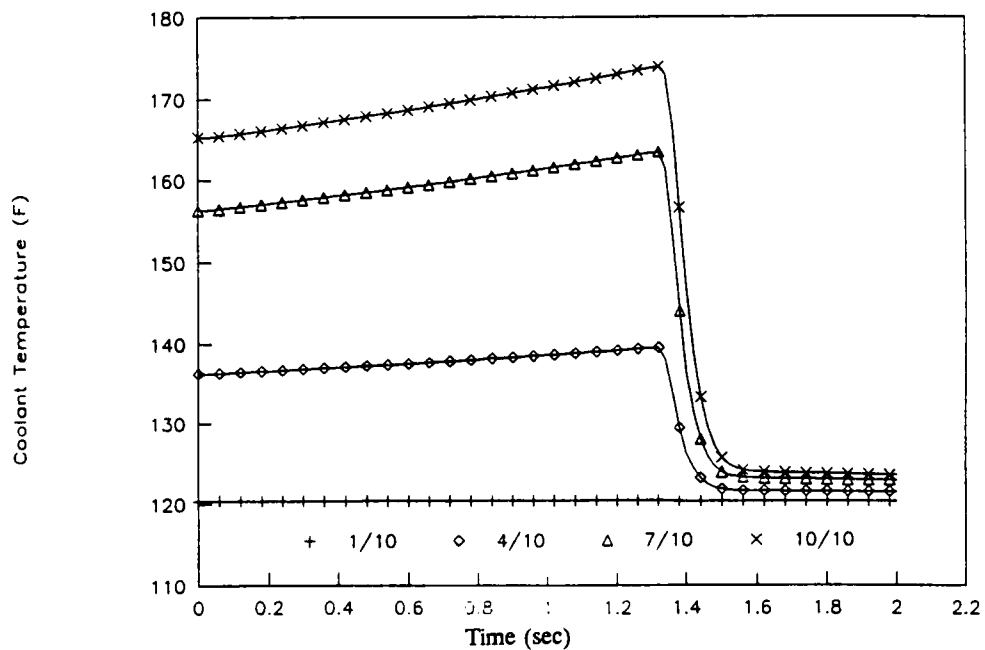


Figure 42 : Inner-element nominal-channel bulk coolant temperatures for the 1, 4, 7, and 10th nodes during uncontrolled shim withdrawal of all five control elements at maximum shim drive speed for BOC, Mode 1, full power transient.

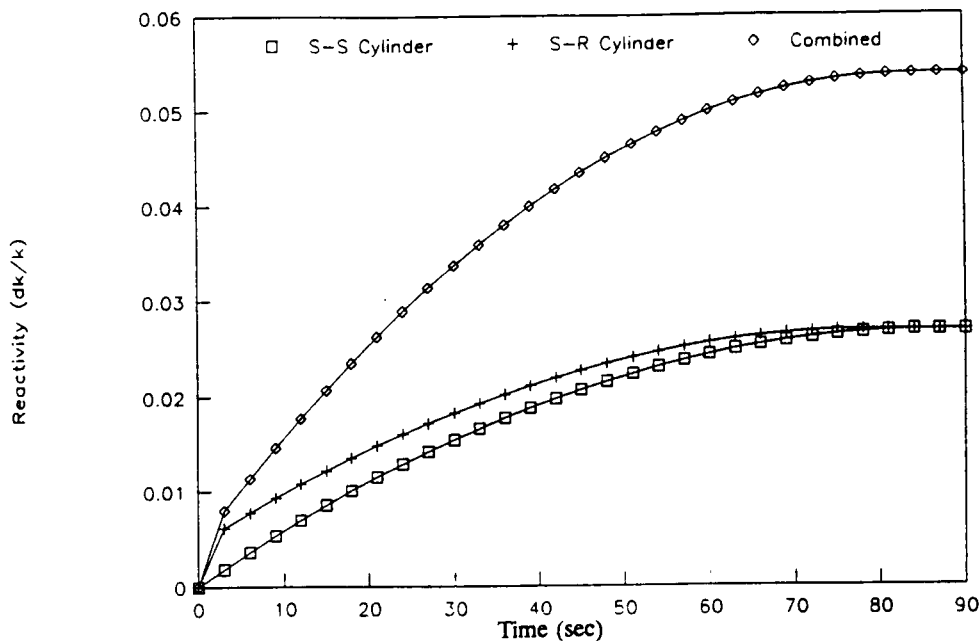


Figure 43 : Mode-1, full-power reactivity insertion rate for uncontrolled withdrawal of outer shim-safety (S-S) elements with maximum shim drive speed and inner shim-regulating (S-R) cylinder with regulating drive speed added to the shim drive speed.

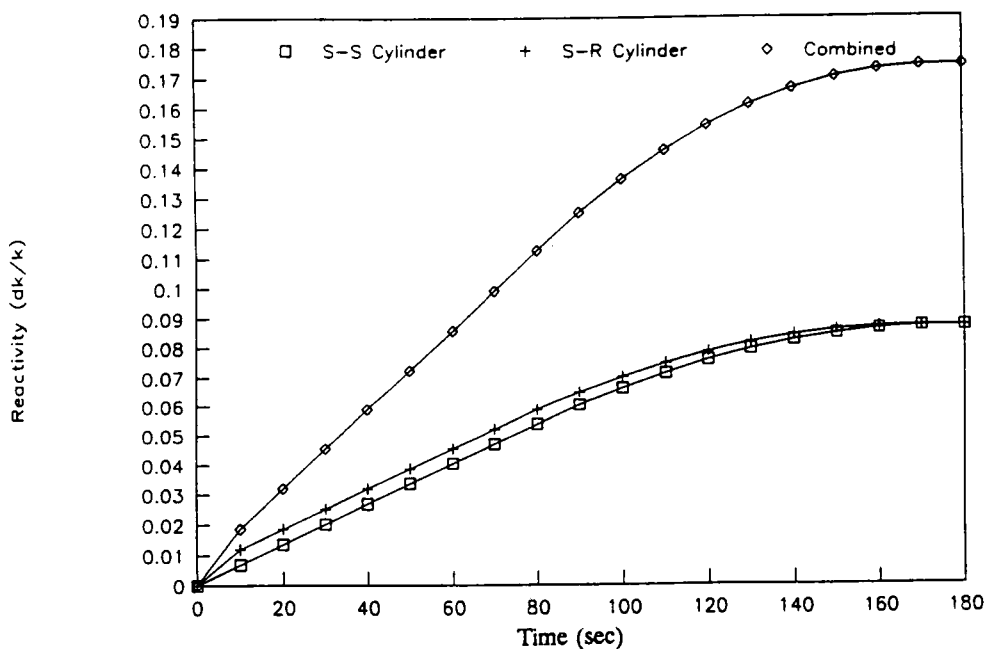


Figure 44 : Reactivity insertion rate during low power transients for uncontrolled withdrawal of outer shim-safety (S-S) elements with maximum shim drive speed and inner shim-regulating (S-R) cylinder with regulating drive speed added to the shim drive speed.

Table 23 : Peak powers and energy released during an uncontrolled S-S withdrawal with maximum shim drive speed and S-R withdrawal with regulating drive speed added to shim drive speed.

OPERATING STATES	Peak Power (MW)	Time Integrated Power (MW-sec)	Differential Time Integrated Power (MW-sec)
BOC, Mode 1, Full-Power, Full-Flow	90.4	19.0	0.61
BOC, Mode 1, Low-Power, Full-Flow	15.1	9.5	1.72
BOC, Mode 1, Low-Power, Low-Flow	12.0	5.03	0.46
BOC, Mode 2	3.31	1.62	0.21
EOC, Mode 1, Low-Power, Full-Flow	15.0	9.30	1.70
EOC, Mode 1, Low-Power, Low-Flow	12.0	4.36	0.37
EOC, Mode 2	3.34	1.66	0.22

Table 24 : Peak temperatures during an uncontrolled S-S withdrawal with maximum shim drive speed and S-R withdrawal with regulating drive speed added to shim drive speed.

OPERATING STATES		Peak Average Fuel Temperatures (F)		Peak Bulk Coolant Temperatures (F)	
		Nominal Plate	Hot Plate	Nominal Channel	Hot Channel
BOC, Mode 1, Full-Power, Full-Flow	Inner	214.3	252.9	167.7	187.4
	Outer	220.6	261.6	167.7	187.6
BOC, Mode 1, Low-Power, Full-Flow	Inner	137.4	145.3	128.6	132.6
	Outer	138.6	147.0	128.6	132.6
BOC, Mode 1, Low-Power, Low-Flow	Inner	206.4	235.0	178.0	196.6
	Outer	210.6	240.9	178.1	196.8
BOC, Mode 2	Inner	144.8	154.2	135.7	141.5
	Outer	146.1	156.1	135.7	141.5
EOC, Mode 1, Low-Power, Full-Flow	Inner	138.9	147.5	130.0	134.8
	Outer	138.7	147.2	130.1	134.8
EOC, Mode 1, Low-Power, Low-Flow	Inner	215.0	247.1	187.7	210.2
	Outer	215.4	247.4	187.7	210.2
EOC, Mode 2	Inner	147.7	158.4	138.5	145.4
	Outer	147.6	158.1	138.5	145.4

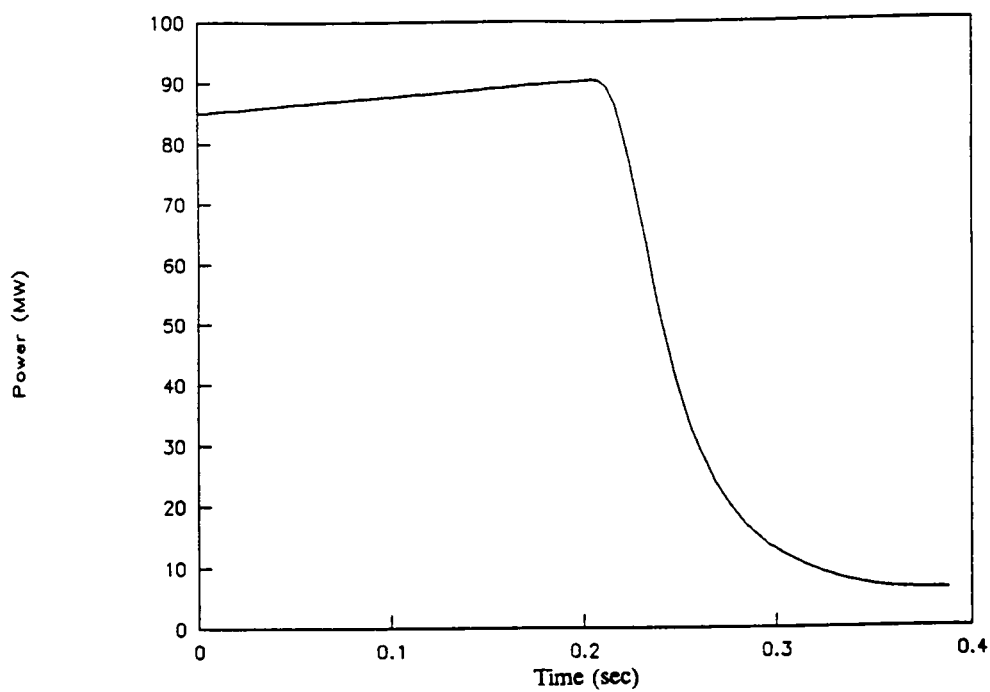


Figure 45 : Power excursion during uncontrolled S-S withdrawal with maximum shim drive speed and S-R withdrawal with shim+regulating drive speed for BOC, Mode 1, full power transient.

than the previous cases resulting in an even milder protected transients compared to the previous sequence. It has been observed, however, the time rate of change of power in the Mode-1, full-power case is very close to 17 MW/sec. Therefore, this sequence is also analyzed with the rate trip deactivated along with two other low-power cases and the results are presented in Tables 25, and 26, and in Figures 46, and 47. In Mode 1, low-power, full flow, there is significant time available for trip on power level which results in a large power overshoot. However, deactivating the rate trip for the low-power cases is clearly very conservative since the time rate of change of power far exceeds the 17 MW/sec limit long before power reaches the level scram set point as shown in Figure 47. Nevertheless, none of these cases present a notable threat to the reactor core and, in most cases, the safety system action terminates the transients before any possible power overshoots. Therefore, other combinations of protected shim withdrawal accidents from symmetrical or asymmetrical positions are not expected to be damaging.

A single shim withdrawal event will result in a reactivity insertion rate smaller than the full shim withdrawal. However, it will most likely create local power peaking in the core. This local power may reach high levels before the core average power reaches the trip set-point creating localized boiling and burnout conditions. Therefore, single shim withdrawal is not necessarily bounded by the full withdrawal event. On the other hand, since single withdrawal creates shim asymmetry which triggers an alarm signal, this event is less likely than the full withdrawal.¹¹ Since local power peaking cannot be modeled properly with the space independent point kinetics equations, this and other similar sequences are not studied in this work.

As seen in Figures 37, 38, 43 and 44, the total amount of reactivity involved in the shim withdrawal accidents is substantial. Therefore, for the unprotected sequences in which the scram system fails (ATWS sequence), uncontrolled shim withdrawals result in nominal plate meltdown. As an example, for the BOC Mode 1 full-power case, the peak power is ~600 MW at about 30 seconds as shown in Figure 48. At this point the thermal variables are in equilibrium, and the nominal fuel temperatures at the aluminum melting point.

Table 25 : Peak powers and energy released during an uncontrolled S-S withdrawal with maximum shim drive speed and S-R withdrawal with regulating+shim drive speed. Rate scram deactivated.

OPERATING STATES	Peak Power (MW)	Time Integrated Power (MW-sec)	Differential Time Integrated Power (MW-sec)
BOC, Mode 1, Full-Power, Full-Flow	102.2	58.0	5.27
BOC, Mode 1, Low-Power, Full-Flow	118.8	152.2	119.7
EOC, Mode 1, Low-Power, Full-Flow	117.2	110.7	83.2

Table 26 : Peak temperatures during an uncontrolled S-S withdrawal with maximum shim drive speed and S-R withdrawal with regulating+shim drive speed. Rate trip deactivated.

OPERATING STATES		Peak Average Fuel Temperatures (F)		Peak Bulk Coolant Temperatures (F)	
		Nominal Plate	Hot Plate	Nominal Channel	Hot Channel
BOC, Mode 1, Full-Power, Full-Flow	Inner	225.6	268.2	173.7	195.8
	Outer	232.5	277.9	173.8	196.1
BOC, Mode 1, Low-Power, Full-Flow	Inner	243.5	288.8	183.9	207.9
	Outer	251.6	299.7	184.0	208.2
EOC, Mode 1, Low-Power, Full-Flow	Inner	250.7	298.9	193.8	222.7
	Outer	249.1	296.9	193.9	222.7

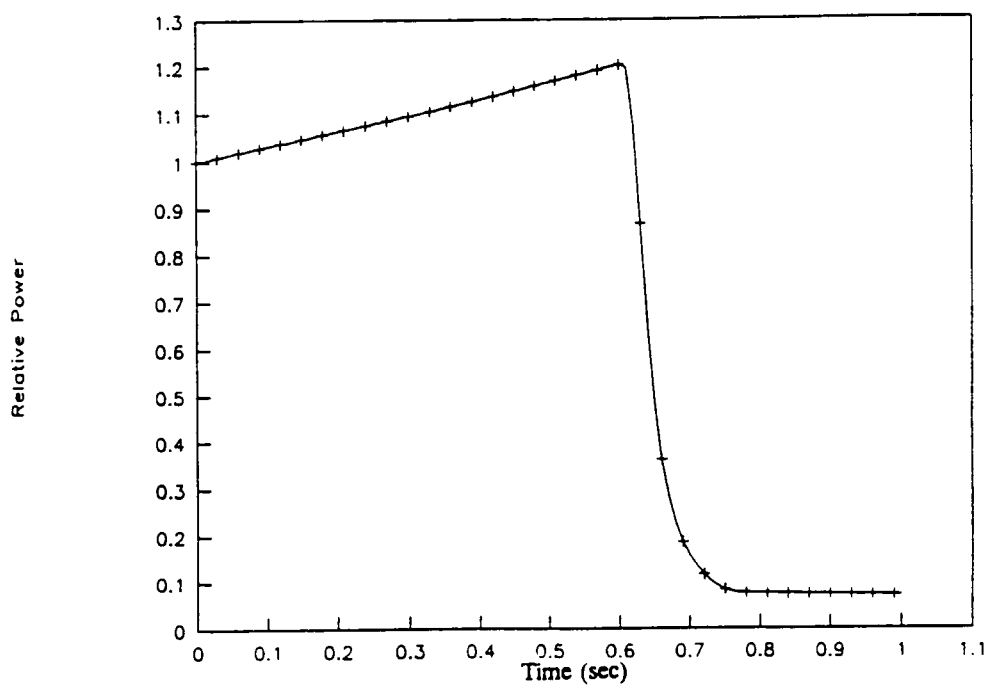


Figure 46 : Power excursion during uncontrolled S-S withdrawal with maximum shim drive speed and S-R withdrawal with shim+regulating drive speed for BOC, Mode 1, full power transient when rate-trip is deactivated.

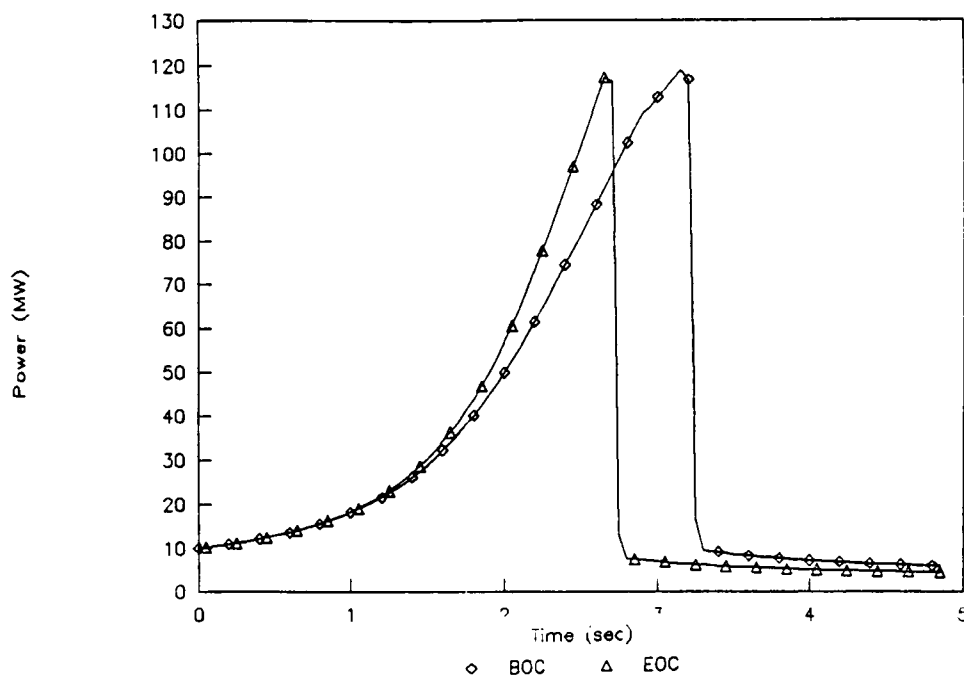


Figure 47 : Power excursion during uncontrolled S-S withdrawal with maximum shim drive speed and S-R withdrawal with shim+regulating drive speed for Mode 1, low-power, full-flow transient when rate-trip is deactivated.

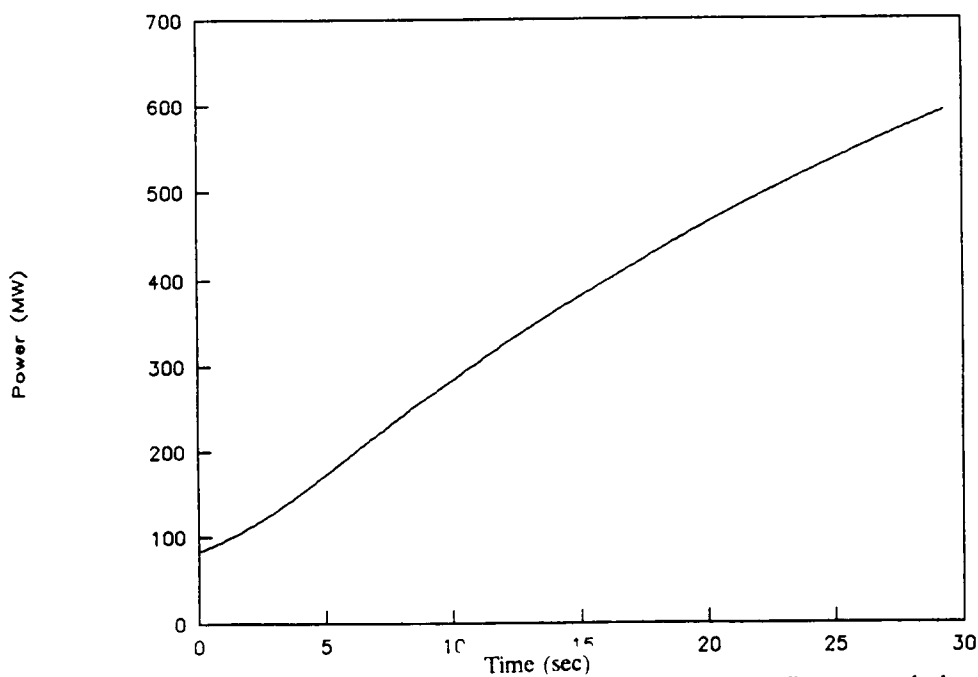


Figure 48 : Power excursion for uncontrolled shim withdrawal of all five control elements at maximum shim drive speed for BOC, Mode 1, full-power transient. Unprotected sequence.

7.1.2. Control Element Ejections

The second class of accidents associated with the reactor control system deals with the structural integrity of the control element drive rods. In the event of a mechanical failure of the drive mechanism, the removal of the control elements out of the core under the forces of gravity and coolant hydrodynamics results in insertion of a substantial amount of positive reactivity. In addition to the subsequent increase in the reactor power level, these control element ejections will also cause increase in axial power peaking due to asymmetry. This effect is not studied in this analysis because the local peaking cannot be adequately modeled with point kinetics.

Since there is no applicable database available regarding the physical failure of the drive mechanisms involved and there is no clear initiator for damage, the frequency of these events is not specified. However, they are estimated to be in the range of limiting faults.¹¹ Unprotected sequences are also considered even though their frequency classification is most likely below the credibility limit.

7.1.2.1. Shim-Regulating Cylinder Ejection

The shim-regulating cylinder is rigidly connected to its associated driving gears. However, a physical failure in the drive mechanism could potentially cause the gear drive to disengage. In such an event, the hydraulic cylinder on the lower end of the drive rod which is normally used to balance the downward forces of gravity and pressure will act as a shock absorber and will limit the reactivity insertion rate. Even when it is depressurized, the cylinder is expected to retain sufficient oil to damp the safety-regulation cylinder ejection.¹⁴ With this damping cylinder in effect, the shim-regulating rod maximum withdrawal speed would be about 0.65 in/sec.¹⁴

The calculated maximum reactivity insertion rate is shown in Figures 49 and 50 for the full-power and low-power transients, respectively. Compared to the uncontrolled shim withdrawal accident, the rate of reactivity insertion for this case is considerably greater as shown in these figures.

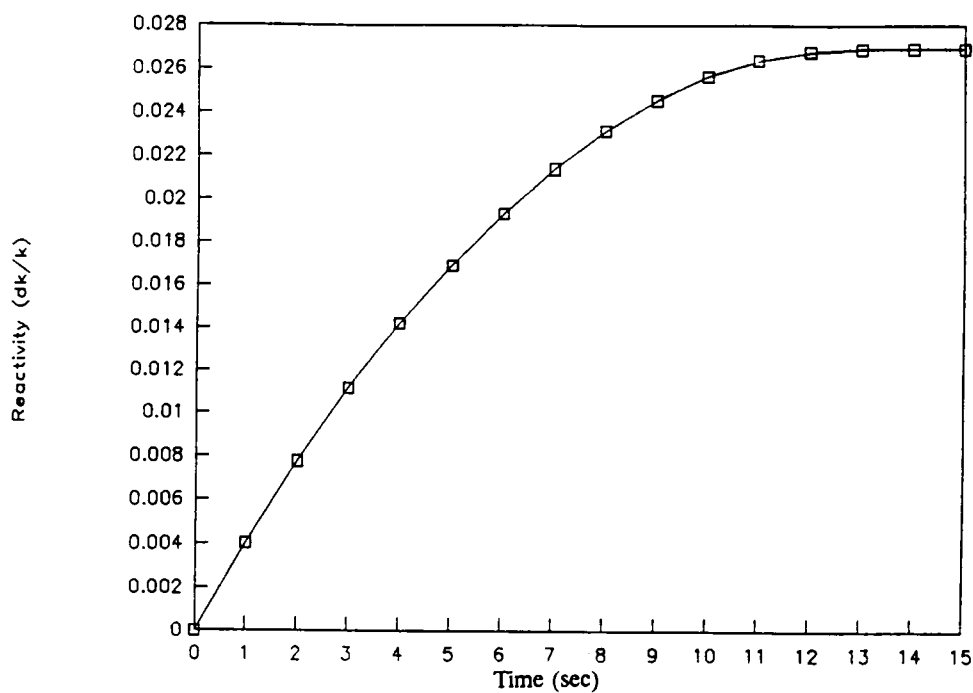


Figure 49 : Reactivity insertion rate during the shim-regulating cylinder ejection accident for Mode 1, full-power case.

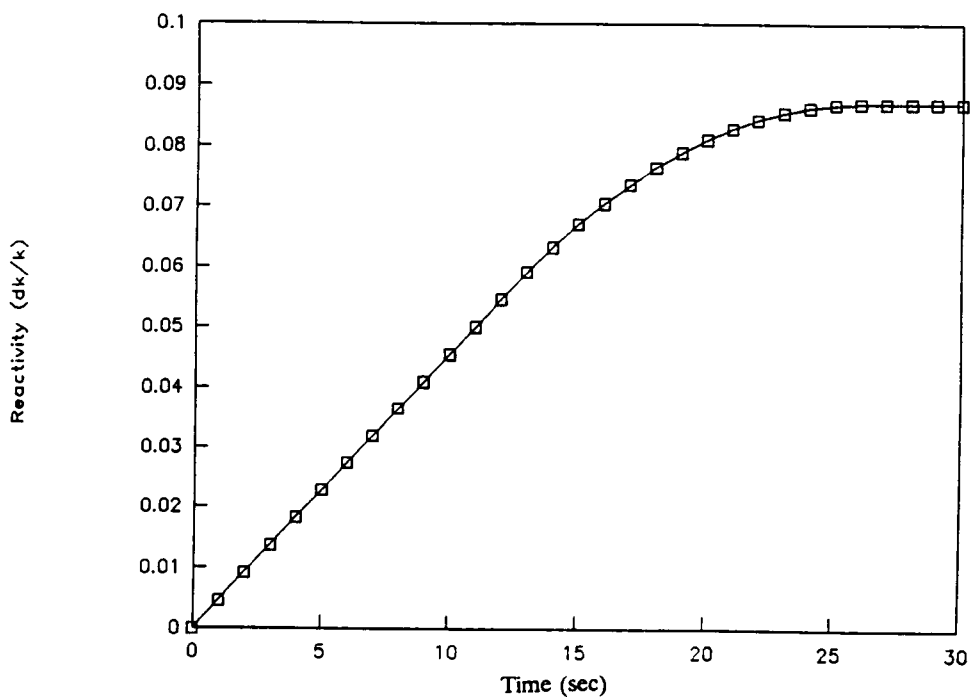


Figure 50 : Reactivity insertion rate during the shim-regulating cylinder ejection accident for Mode 1 - low-power, and Mode 2 cases.

The results for the protected sequences are presented in Tables 27 and 28 for seven different operating states. Since the control elements are assumed fully withdrawn, shim-regulating cylinder ejection is not considered at the end of the fuel cycle conditions in **Mode 1** for full-power. These transients are also very mild and quite similar to those for the uncontrolled shim withdrawal sequences except that the shim-regulating cylinder ejection is terminated by the scram action a lot sooner. This typical transient behavior of the state variables are displayed for the inner-element in Figures 51 to 54 for BOC, Mode 1, full-power case.

An unprotected shim-regulating cylinder ejection sequence is also investigated for BOC, Mode 1, and full-power. For this case, the reactor power level steadily increases to a value of 560 MW and some nominal fuel nodes with high power density reach the aluminum melting point leading to a partial core damage. Since the total amount of reactivity involved is minimal for this case, other sequences are also expected to be "damaging." Therefore, they are not studied in this work.

7.1.2.2. Shim-Safety Quadrant Drop

Another case of interest is associated with the breakage of the attachment between a shim-safety quadrant and its drive rod. After such an event, it is possible for the control plate to drop until the guide bearings hit the bottom of their track. This would leave the upper black-white interface about three inches below the upper active end of the core, thus leaving a three inch "white" window in the control plate. If the bearing attachments also break, the plate would fall another two inches and come to rest at the bottom of the pressure vessel.

Such a shim-safety quadrant drop will actually decrease the core reactivity initially. In fact, a plate drop from symmetric critical conditions is expected to shut the reactor down. However, it is suggested conservatively that the core would be critical again at about the point where the rod guide bearings hit the bottom of their track.¹⁴ Furthermore, if the bearing attachments also break, the reactor would be supercritical with a reactivity addition rate of 0.54 dk/k/sec for a single shim-safety plate. The total amount of positive reactivity that could

Table 27 : Peak powers and energy released during shim-regulating cylinder ejection accident.

OPERATING STATES	Peak Power (MW)	Time Integrated Power (MW-sec)	Differential Time Integrated Power (MW-sec)
BOC, Mode 1, Full-Power, Full-Flow	90.5	13.2	0.44
BOC, Mode 1, Low-Power, Full-Flow	15.1	6.75	1.25
BOC, Mode 1, Low-Power, Low-Flow	12.0	3.47	0.32
BOC, Mode 2	3.35	1.20	0.16
EOC, Mode 1, Low-Power, Full-Flow	15.2	6.75	1.25
EOC, Mode 1, Low-Power, Low-Flow	12.1	3.36	0.31
EOC, Mode 2	3.35	1.19	0.16

Table 28 : Peak temperatures during shim-regulating cylinder ejection accident.

OPERATING STATES		Peak Average Fuel Temperatures (F)		Peak Bulk Coolant Temperatures (F)	
		Nominal Plate	Hot Plate	Nominal Channel	Hot Channel
BOC, Mode 1, Full-Power, Full-Flow	Inner	214.2	252.6	167.5	187.2
	Outer	220.4	261.3	167.6	187.4
BOC, Mode 1, Low-Power, Full-Flow	Inner	137.4	145.3	128.5	132.5
	Outer	138.6	146.9	128.5	132.6
BOC, Mode 1, Low-Power, Low-Flow	Inner	205.0	233.2	176.7	195.1
	Outer	209.1	238.9	176.8	195.2
BOC, Mode 2	Inner	144.3	153.6	135.3	141.0
	Outer	145.7	155.5	135.3	141.0
EOC, Mode 1, Low-Power, Full-Flow	Inner	138.9	147.4	130.0	134.8
	Outer	138.7	147.1	130.0	134.8
EOC, Mode 1, Low-Power, Low-Flow	Inner	214.4	246.4	187.0	209.3
	Outer	214.8	246.6	187.0	209.4
EOC, Mode 2	Inner	147.0	157.5	137.9	144.7
	Outer	146.9	157.2	137.9	144.7

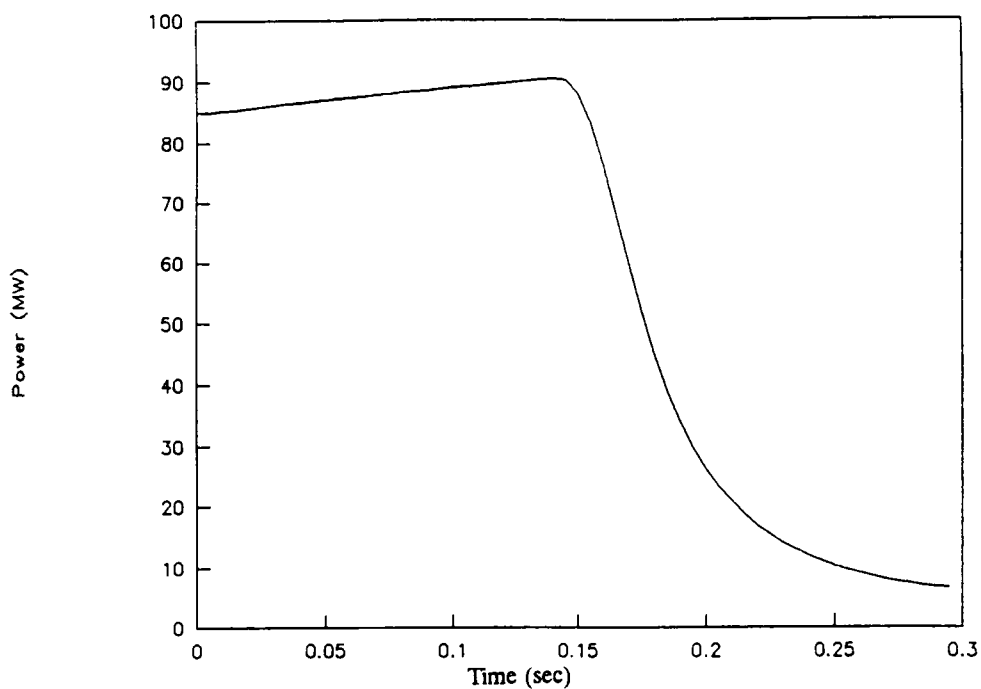


Figure 51 : Power excursion during shim-regulating cylinder ejection for BOC, Mode 1, full-power transient.

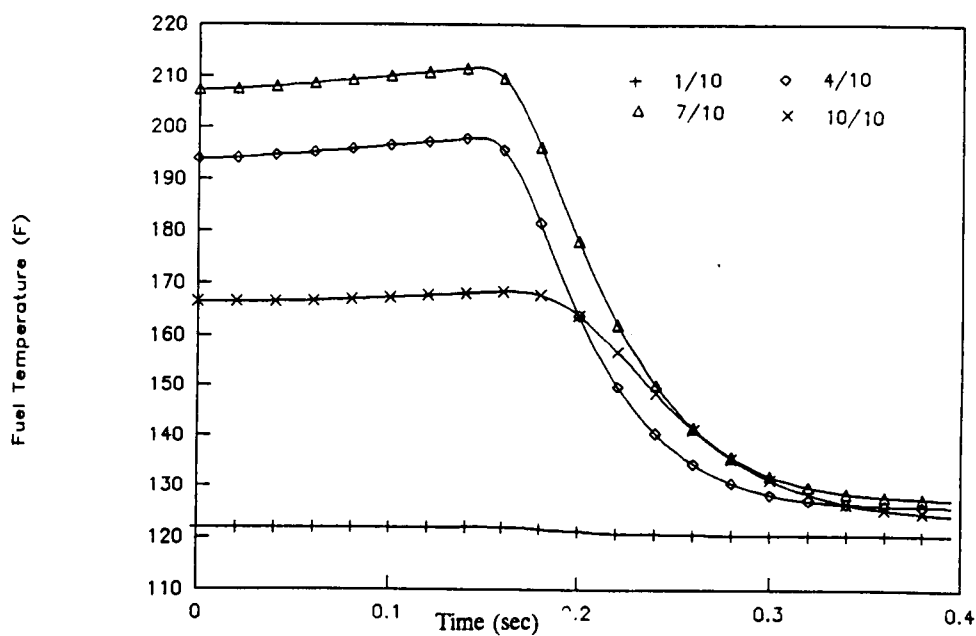


Figure 52 : Inner-element, nominal-plate average fuel temperatures for nodes 1, 4, 7, and 10 during shim-regulating cylinder ejection for the BOC, Mode 1, full-power transient.

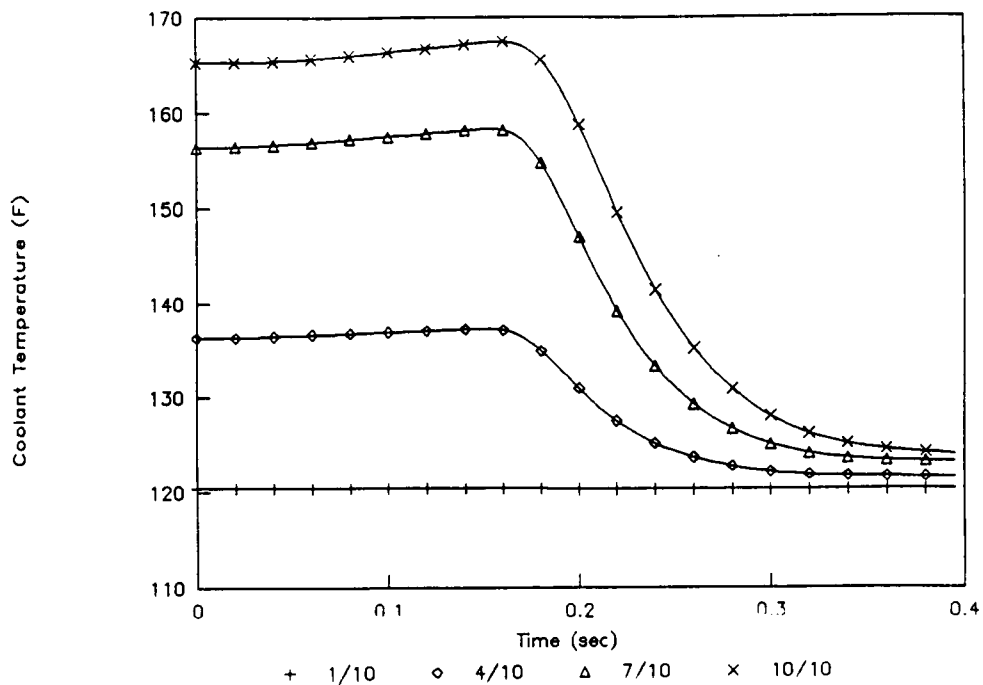


Figure 53 : Inner-element, nominal-channel bulk coolant temperatures for nodes 1, 4, 7, and 10 during shim-regulating cylinder ejection for the BOC, Mode 1, full-power transient.

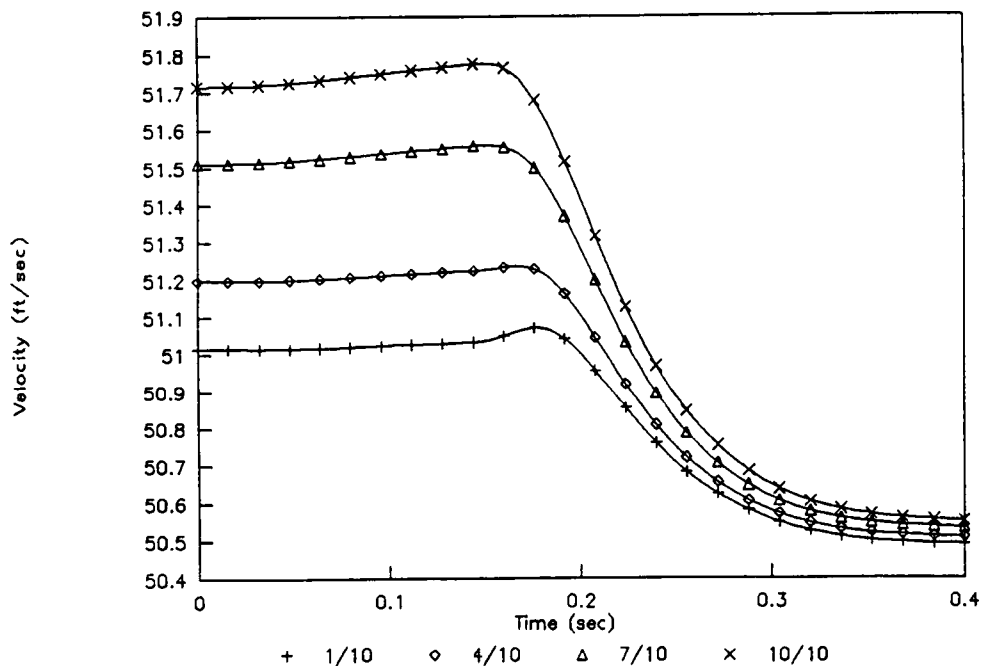


Figure 54 : Inner-element, nominal-channel velocities for nodes 1, 4, 7, and 10 during shim-regulating cylinder ejection for the BOC, Mode 1, full-power transient.

be added would be about 0.0056 dk/k.¹⁴

The result for this scenario are presented in Tables 29 and 30. For these protected sequences, only a three-plate scram is considered since one plate is out of service. The highest peak powers and hot-plate temperatures are obtained in Mode 1 and at full-power. Power excursions for the BOC and EOC conditions are presented in Figure 55. And the behavior of several key state variables for EOC transient is shown in Figures 56 to 58 for the inner-element.

In order to assess the sensitivity of the reactivity insertion rate for the single shim-safety quadrant drop accident, another case is investigated in which step-wise insertion of all available reactivity is considered for an *unprotected* transient. Even when 0.0056 dk/k reactivity is stepped into the reactor for EOC in Mode 1 and at full-power (rather than the 0.54 dk/k/sec ramp insertion), the corresponding peak power is 224.6 MW while the peak average fuel temperatures were 296.6 °F for the nominal plate, and 361.7 °F for the hot-plate in the outer-element. For this case after the initial oscillations, the power achieves a steady-state value of 160 MW as shown in Figure 59. Two low-flow cases are also studied since the thermal-hydraulic conditions in these low-pressure states are less favorable. For the Mode 1, low-flow transients, the peak powers are found to be 36.4 MW and 36.0 MW for BOC and EOC conditions, respectively. The reactor reaches an equilibrium state at 25 MW and 30 MW in BOC and EOC for these low-flow cases. Since the peak temperatures are well below critical values in these three most decisive cases, it is concluded that the protected and unprotected shim-safety quadrant ejections do not pose a serious threat to reactor operation.

In summary, from the analyses presented above it is concluded that the reactivity accidents which would result from runaway control elements or broken drive and support mechanisms are not significant in terms of core damage.

7.2. COLD WATER ACCIDENT

Since the fuel region is under-moderated and has a negative feedback coefficient, the core reactivity can be increased by an increase in density from an overcooling event. There

Table 29 : Peak powers and energy released for a shim-safety quadrant drop scenario.

OPERATING STATES	Peak Power (MW)	Time Integrated Power (MW-sec)	Differential Time Integrated Power (MW-sec)
BOC, Mode 1, Full-Power, Full-Flow	218.7	7.47	3.38
BOC, Mode 1, Low-Power, Full-Flow	32.1	1.35	0.74
BOC, Mode 1, Low-Power, Low-Flow	30.2	1.23	0.62
BOC, Mode 2	10.6	0.69	0.45
EOC, Mode 1, Full-Power, Full-Flow	200.9	11.8	5.12
EOC, Mode 1, Low-Power, Full-Flow	26.3	1.35	0.64
EOC, Mode 1, Low-Power, Low-Flow	24.6	1.20	0.53
EOC, Mode 2	8.87	0.63	0.38

Table 30 : Peak temperatures for a shim-safety quadrant drop scenario.

OPERATING STATES		Peak Average Fuel Temperatures (F)		Peak Bulk Coolant Temperatures (F)	
		Nominal Plate	Hot Plate	Nominal Channel	Hot Channel
BOC, Mode 1, Full-Power, Full-Flow	Inner	272.1	328.5	193.9	220.9
	Outer	282.9	343.0	194.1	221.4
BOC, Mode 1, Low-Power, Full-Flow	Inner	145.7	155.7	131.7	136.3
	Outer	147.6	158.3	131.8	136.4
BOC, Mode 1, Low-Power, Low-Flow	Inner	216.3	247.3	178.0	196.5
	Outer	222.5	255.5	178.0	196.5
BOC, Mode 2	Inner	154.0	165.7	137.2	143.1
	Outer	156.4	168.9	137.2	143.1
EOC, Mode 1, Full-Power, Full-Flow	Inner	289.5	352.7	215.3	251.9
	Outer	285.2	346.9	215.3	251.8
EOC, Mode 1, Low-Power, Full-Flow	Inner	144.5	154.4	132.4	137.6
	Outer	143.8	153.5	132.4	137.6
EOC, Mode 1, Low-Power, Low-Flow	Inner	222.8	256.8	187.8	210.2
	Outer	221.9	255.4	187.9	210.3
EOC, Mode 2	Inner	153.9	166.1	139.7	146.8
	Outer	153.5	165.5	139.8	146.8

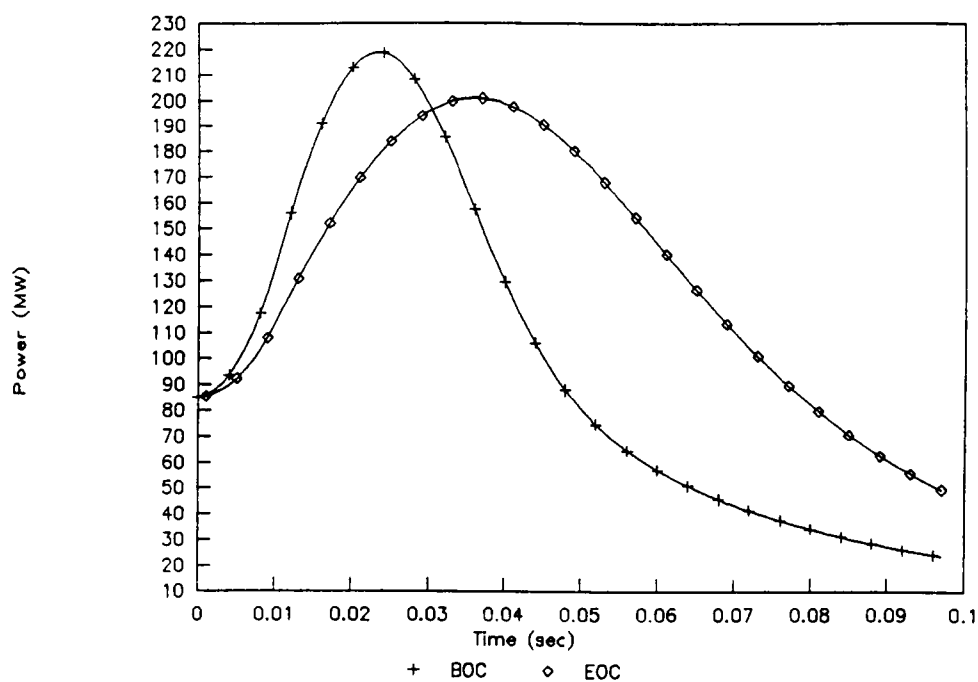


Figure 55 : Power excursion during a shim-safety quadrant drop scenario for Mode 1, full-power transient.

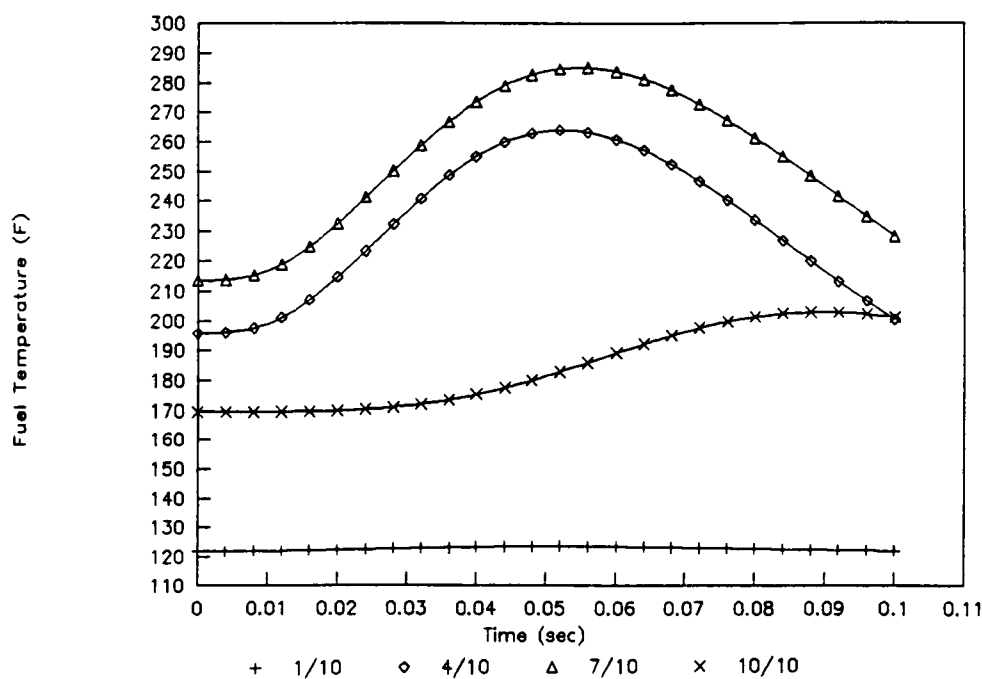


Figure 56 : Inner-element, nominal-plate average fuel temperatures for nodes 1, 4, 7, and 10 during a shim-safety quadrant drop scenario for the EOC, Mode 1, full-power transient.

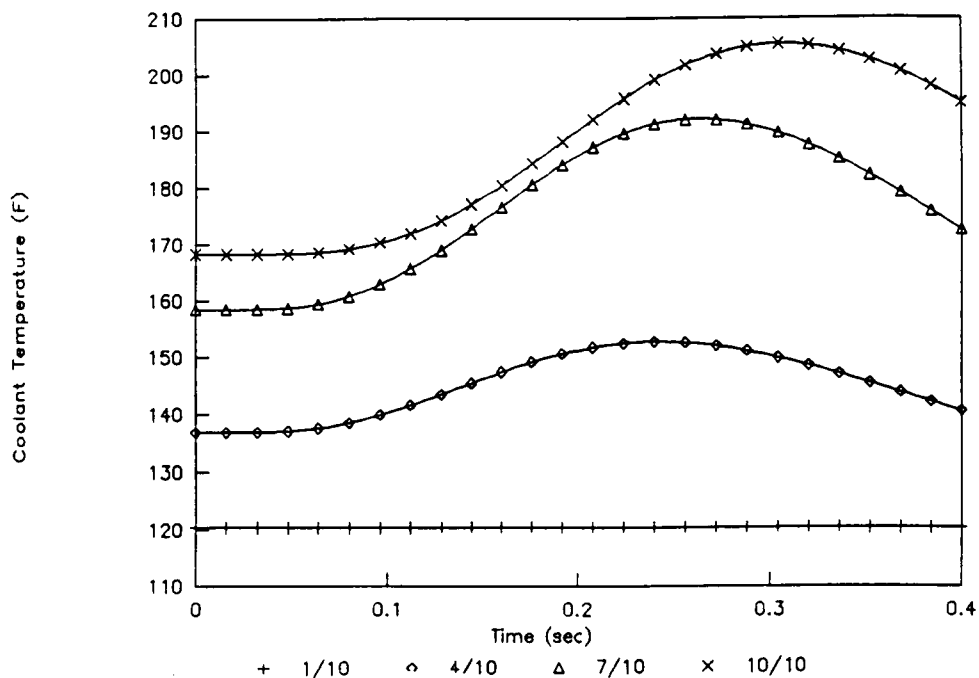


Figure 57 : Inner-element, nominal-plate bulk coolant temperatures for nodes 1, 4, 7, and 10 during a shim-safety quadrant drop scenario for the EOC, Mode 1, full-power transient.

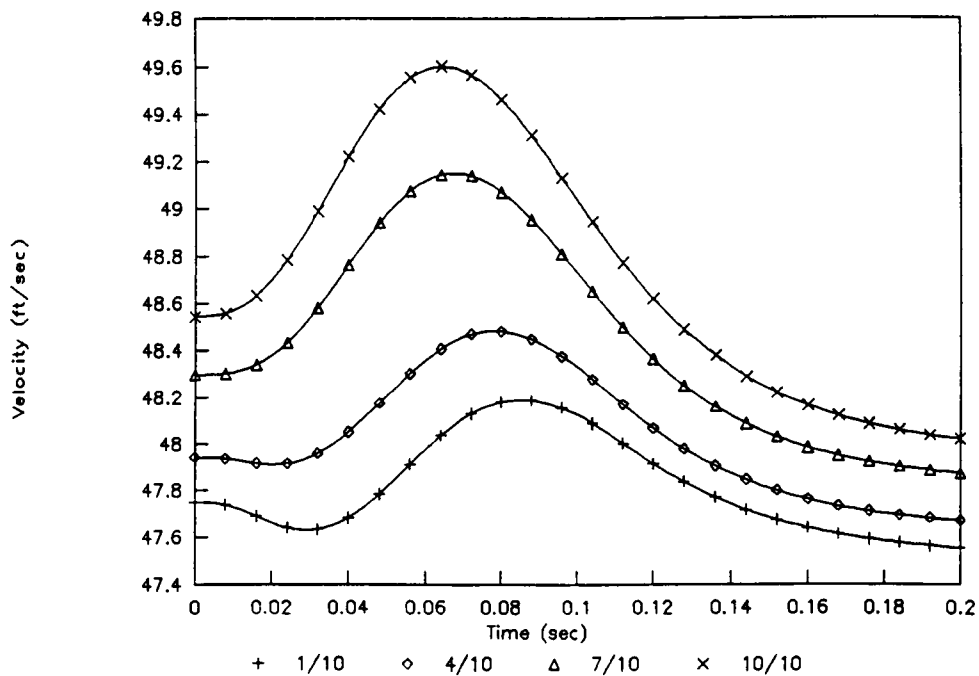


Figure 58 : Inner-element, nominal-channel velocities for nodes 1, 4, 7, and 10 during a shim-safety quadrant drop scenario for the EOC, Mode 1, full-power transient.

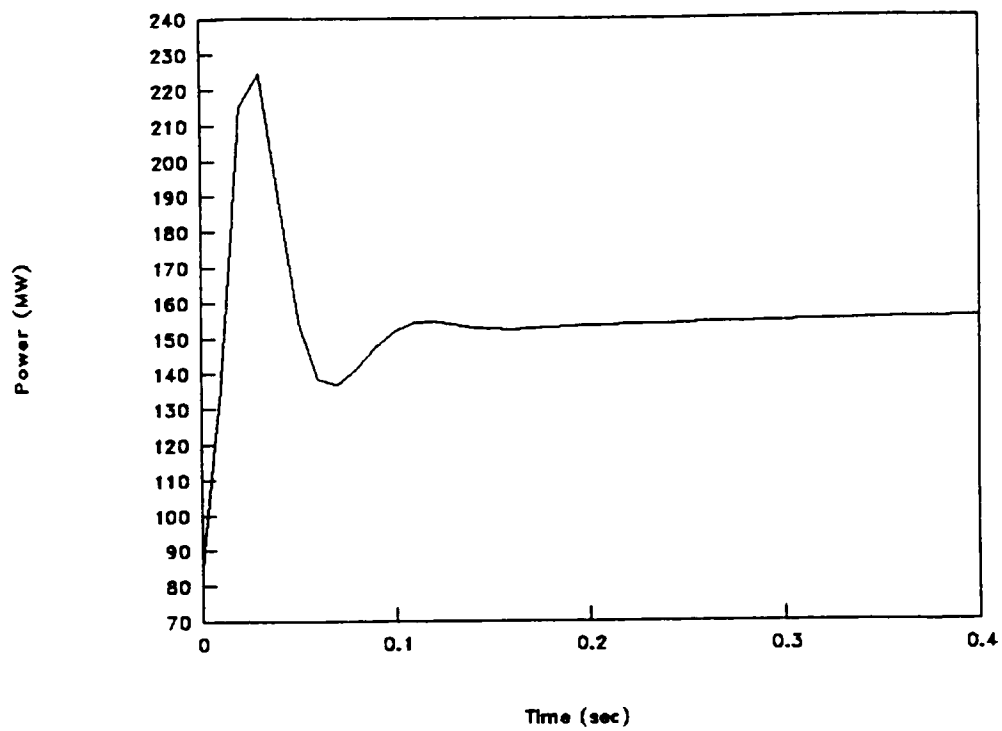


Figure 59 : Unprotected shim-safety quadrant drop accident for EOC, Mode 1, full-power.

are several initiating events which could result in overcooling of the moderator in the HFIR core. These are the events which lead to a decrease in the secondary loop temperature, an increase in the secondary flow rate, and a decrease in the pool temperature. These transients generally cause relatively slow cooldowns which lead to very mild power increases; therefore, they can be ignored as initiating events of reactivity accidents.

One transient, however, initiated by a pump start in a cold idle heat exchanger loop can result in a rapid short term cooldown and a significant reactivity insertion by sending a slug of cold water through the reactor core. The severity of this transient depends strongly on the initial conditions such as the mode of operation, flow rate, core temperatures, and on such factors as how quickly the pumps attain full speed, and how much mixing occurs.¹¹

In **Mode 1**, when the cold loop is started, the coolant will be mixed with the established flow. Therefore, the minimum temperature experienced in the core would depend on the initial flow conditions. The maximum cooldown would occur during a two loop operation at the minimum flow. However, reactor trip power would be at its lower limit for this condition. Starting a cold loop at full-flow would cause less cooldown, but the trip power would be higher. In addition to rate or level trip signals, the cold slug is expected to trip the reactor within two seconds due to low inlet temperature in **Mode 1**.^m

In **Mode 2**, the maximum cooldown will result from a pump start during two-loop operation with the minimum flow. If the pressure is atmospheric, the pump will be started with a pony motor. When the pressure is 250 psia, the pump will be started with the primary motor. In **Mode 3**, all pumps would be idle initially and the coolant outside the pressure vessel would be assumed cold.

Due to uncertainties on several parameters such as how quickly the pumps attain full speed, how much mixing takes place between the cold slug and the primary coolant, and what the final coolant temperature will be after mixing, a "best-estimate" analysis of the cold water

^m Before reaching the core, the cold slug will affect the inlet temperature sensors which provide input to the heat-power calculators in **Mode 1**. A rapid drop in temperature at the inlet will result in a false high power signal causing a reactor trip.

accident is not feasible with the available model. Instead an overly-conservative bounding case is described and analyzed below, and the consequences are interpolated based on the results.

Clearly, operation in **Mode 3** presents the worst case in terms of total reactivity available for insertion. With no flow through the primary system, it is possible for the water in the heat exchangers to be at a rather low temperature while the water in reactor vessel is at a relatively high temperature due to low-power experiments with no forced flow. In this case, if all three main circulating pumps were simultaneously turned on (contrary to normal operating procedures), cold water would be injected to the core. Since the primary pumps are locked in this mode, pump start-up would be by the pony motors. Therefore, the shortest effective ramp conceivable for this cold water injection rate would be much greater than 60 msec.

The greatest increase in reactivity would occur if the cold water enters only the fuel region. Calculations indicate that, for a more probable case in which the cold water enters all parallel regions of the core, similar conditions would create about 1.5 times less positive reactivity. However, the cold slug is assumed to enter the fuel region only and a change in the fuel-region temperature from 212°F to 32°F is considered as an extreme case.

The frequency of an inadvertent pump start in the cold loop is given as 3.2×10^{-1} per year in **Mode 1**, therefore it has a moderate frequency. In **Modes 2 and 3**, this frequency is expected to be somewhat smaller since the loops are usually isolated. In addition, assumptions regarding the startup of all three pumps simultaneously and the cold slug with an extreme temperature of 32 °F entering the fuel region only significantly reduces the credibility of this event.

The results of this conservative scenario are presented in Table 31 and 32. The power excursion for the beginning of cycle (BOC) and end of cycle (EOC) conditions are also presented in Figures 60 and 61, respectively. Even though the power level increases eight-fold for BOC, and two-fold for EOC, the sweep of cold water through the coolant channels actually decreases the core temperatures. Indeed, the peak temperatures reported in Table

Table 31 : Peak powers and energy released during cold water accident.

OPERATING STATES	Peak Power (kW)	Time Integrated Power (kW-sec)	Differential Time Integrated Power (kW-sec)
BOC	780	40	30
EOC	210	20	10

Table 32 : Peak temperatures during cold water accident.

OPERATING STATES		Peak Average Temperatures	
		Nominal Plate	Nominal Channel
BOC	Inner	212.3	212.2
	Outer	212.3	212.2
EOC	Inner	212.4	212.3
	Outer	212.4	212.3

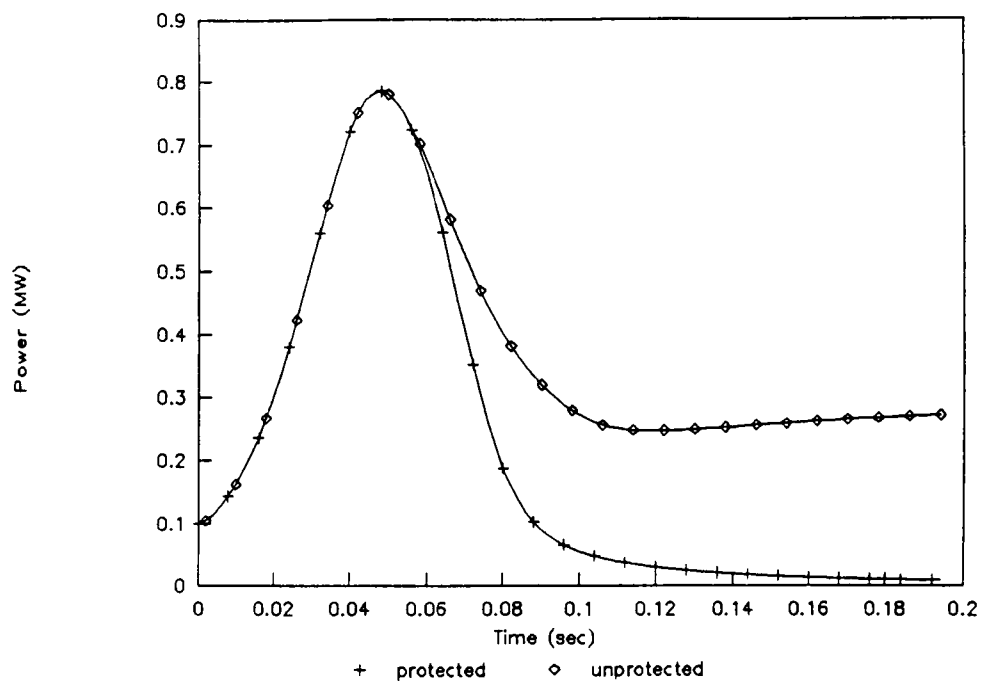


Figure 60 : Power excursion for protected and unprotected cases during the Mode 3 cold-water transient for BOC.

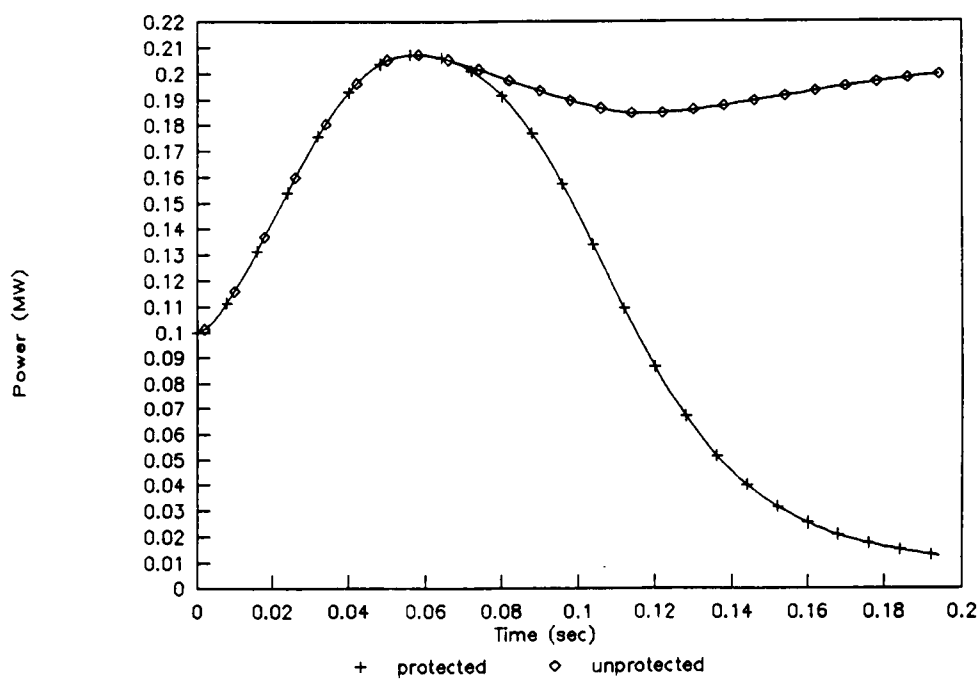


Figure 61 : Power excursion for protected and unprotected cases during the Mode 3 cold-water transient for EOC.

32 are those at the beginning of the transients.

As seen from these tables and figures, the peak values are well below critical limits which allows us to classify this and other cold water sequences as "non-damaging." It should also be pointed out that once the flow in the coolant channels increases, the primary pressure also increases raising the critical heat flux level substantially above the low pressure values in Mode 3.

Even when the scram system fails, cold water accidents are expected to be self limiting since the cold slug will pass through the core in a finite time, and the increase in core power will heat-up the moderator. The results for the unprotected sequences are also included in Figures 60 and 61. Coincidentally, the peak values for the unprotected sequences are the same as for the protected ones indicating that the scram action is not an important factor in terminating these transients. The frequency of this ATWS event is less than 2.0×10^{-5} per year which classifies this sequence as limiting.¹⁴

7.3. ACCIDENTS ASSOCIATED with VOIDS in TARGET REGION

The third class of reactivity accidents are associated with the voids in the target region. The flux trap region is highly over-moderated and, therefore, has a large positive feedback coefficient. As the water density decreases, the decrease in absorption is more effective than the change in moderation. However, the temperature and void reactivity coefficients of the target region are positive only for the initial changes in density.¹⁵ At an optimum void fraction, the effect of moderation and absorption are balanced, and for the larger changes in density, the coefficients become both negative.

Early reactivity accountability studies report that,¹⁵ with a simulated 300 g Pu target, $dk_{\max}$ associated with the island voids is 0.016 with a corresponding void fraction of 42%. At this void fraction, the changes in moderation compensate the changes in absorption, and for larger void fractions the island becomes under-moderated. Without the target, $dk_{\max}$ becomes 0.032 with a corresponding void fraction of 70%. However, it is specified that the reactor will not be operated without the target or its equivalent in the island.¹⁴ Therefore, void accidents

which might occur without the target have not been analyzed.

Three possible ways of formation of voids in the target region are hypothesized.¹⁷ These are:

- i. Formation of a vortex,
- ii. Sweeping in of bubbles with the coolant flow,
- iii. Flow blockage and subsequent steam formation.

The first and second cases seem to be more serious in consequences because the voids can be formed more rapidly resulting in faster reactivity insertion rates. However, they are less likely as suggested by PRA work,^{11,12} operating experience indicates that formation of vortexes or sweeping of any amount of air bubbles with the primary coolant is an incredible event with a frequency less than 1.0×10^{-7} /year. If the flow to the target region is blocked during full-power operation, steam voids could be formed around target rods. The frequency of a flow blockage in the target is calculated as 8.5×10^{-7} per year.^{11,12} If the steam void formed is assumed to be of optimum size and to occur in the exact limiting location, this frequency also drops below the 1.0×10^{-7} /year limit. Nevertheless, these sequences are included in this analysis since they constitute the greatest challenge as far as reactivity transients are concerned.

The effective ramp at full-flow for the void fraction of .42 is estimated as 0.030 seconds.¹⁴ This value is based on the formation of a vortex or the sweeping of air bubbles scenarios. However, since the high velocity region of the flux trap does not have enough volume for that optimum void formation, it was concluded that the maximum change in reactivity will be only 0.013 dk/k, instead of 0.016 dk/k, for the associated 0.030 second ramp. Since this scenario is far more severe than any other void related accidents, other possibilities related to the low-velocity region are not considered. At the 10% flow of **Mode 1 and 2**, the ramp time is conservatively assumed to be 0.300 seconds with a corresponding maximum reactivity of 0.016 dk/k.

The results of these sequences for all possible operating states are presented in Tables 33 and 34. As seen from these tables, the highest power and temperatures are obtained

Table 33 : Peak powers and energy released during transients associated with an optimum amount of voids in the target region.

OPERATING STATES	Peak Power (MW)	Time Integrated Power (MW-sec)	Differential Time Integrated Power (MW-sec)
BOC, Mode 1, Full-Power, Full-Flow	787.7	17.1	12.9
BOC, Mode 1, Low-Power, Full-Flow	486.5	10.5	9.79
BOC, Mode 1, Low-Power, Low-Flow	13.4	0.80	0.12
BOC, Mode 2	5.16	0.45	0.14
EOC, Mode 1, Full-Power, Full-Flow	803.5	27.2	20.8
EOC, Mode 1, Low-Power, Full-Flow	187.5	6.72	5.84
EOC, Mode 1, Low-Power, Low-Flow	13.4	0.91	0.13
EOC, Mode 2	5.11	0.49	0.15

Table 34 : Peak temperatures during transients associated with optimum amount of voids in target region.

OPERATING STATES		Peak Average Fuel Temperatures (F)		Peak Bulk Coolant Temperatures (F)	
		Nominal Plate	Hot Plate	Nominal Channel	Hot Channel
BOC, Mode 1, Full-Power, Full-Flow	Inner	465.7	562.8	280.9	334.4
	Outer	494.7	587.5	282.0	341.8
BOC, Mode 1, Low-Power, Full-Flow	Inner	337.4	403.6	212.8	236.2
	Outer	356.8	428.9	213.4	238.9
BOC, Mode 1, Low-Power, Low-Flow	Inner	202.2	229.7	174.7	192.6
	Outer	205.9	234.8	174.7	192.8
BOC, Mode 2	Inner	145.0	154.4	134.8	140.4
	Outer	146.7	156.7	134.8	140.4
EOC, Mode 1, Full-Power, Full-Flow	Inner	534.6	melts	376.2	373.7
	Outer	527.5	melts	374.5	339.7
EOC, Mode 1, Low-Power, Full-Flow	Inner	239.6	278.1	179.0	196.3
	Outer	236.2	273.6	179.0	196.2
EOC, Mode 1, Low-Power, Low-Flow	Inner	211.8	243.2	184.9	206.9
	Outer	212.5	243.8	185.0	207.0
EOC, Mode 2	Inner	148.0	158.6	137.6	144.3
	Outer	147.6	158.1	137.6	144.3

during Mode 1, full-power transients. The power excursion for these transients are displayed in Figure 62 for both BOC and EOC conditions. And the typical behavior of other state variables are presented in Figures 63 to 65 for the inner-element at the EOC.

Optimum void accident yields the worst results among all reactivity transients considered because a substantial amount of reactivity is involved and it is assumed to be inserted in a very short time. For EOC, Mode 1, full-power, full-flow conditions the protected sequence leads to a partial melting of the hot-plate as indicated in Table 34. As the reactor power reaches its peak value at about 50 msec, extreme surface heat fluxes initiate the subcooled boiling regime even though the bulk of the liquid is highly subcooled. And, as the boiling starts in the hot-channel, immediate film blanketing of the heat transfer surfaces leads to a critical heat flux situation in the subcooled boiling regime.

If the voids are carried into the target region with the primary coolant flow, most likely they will also be swept out of the core at the same rate. This more realistic scenario of passage of a void slug in and out of the target with the primary coolant flow is also investigated for the Mode 1, full-power cases by considering a ramp insertion of 1.3% dk/k in 30 msec followed by a ramp withdrawal of exactly same amount of reactivity in the same amount of time. For the beginning of cycle transient, the peak power is reduced from 787.7 MW to 727.1 MW, and the respective peak hot-plate average fuel temperatures are 527.4 °F and 554.9 °F for the inner and outer elements. In parallel with the reduction in peak power, the total and differential energy released during the transients reduces to 14.1 MW-sec and 10.4 MW-sec, respectively. For the end of fuel cycle transient, more significant drops in peak values are obtained. The peak power is reduced from 803.5 MW to 553.7 MW, and the total energy released during the transients is 14.1 MW-sec from 27.2 MW-sec. More importantly, hot-plate melting is not predicted for this transient and the peak hot-plate average fuel temperatures are 515.9 °F and 507.3 °F for the inner and outer elements, respectively.

Since voids are assumed to be carried out with the primary coolant flow, a 300 msec ramp is assumed for the cases with low-flow. In order to investigate a faster void insertion scenario, two other cases are studied for Mode 1, low-flow in which 0.016 dk/k reactivity is assumed to be ramped into the core over 30 msec instead of 300 msec. These results are

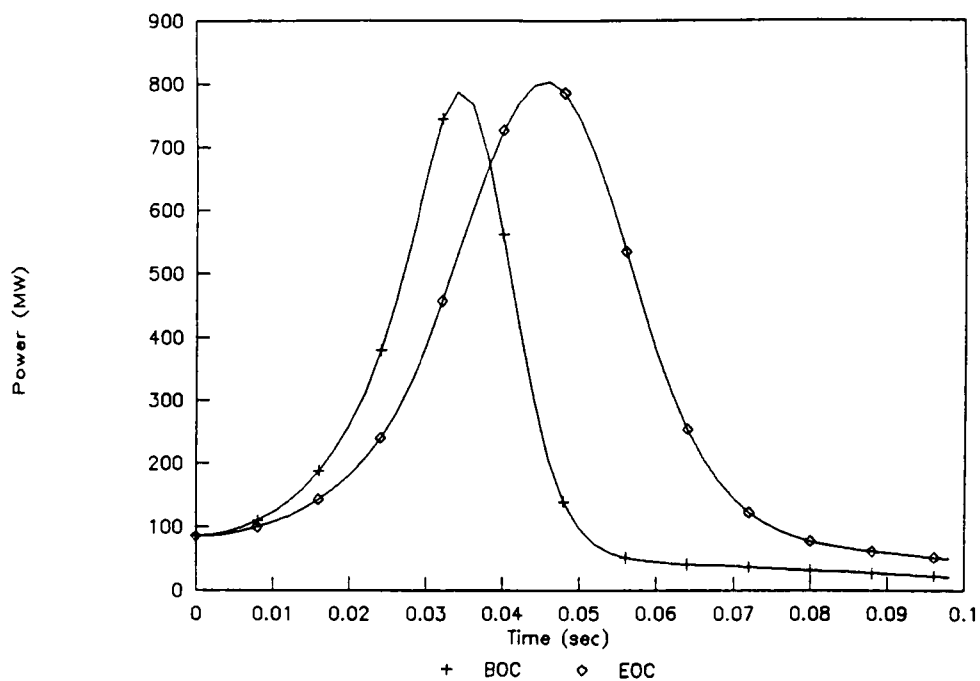


Figure 62 : Power excursion during optimum void accident for the EOC, Mode 1, full-power transient.

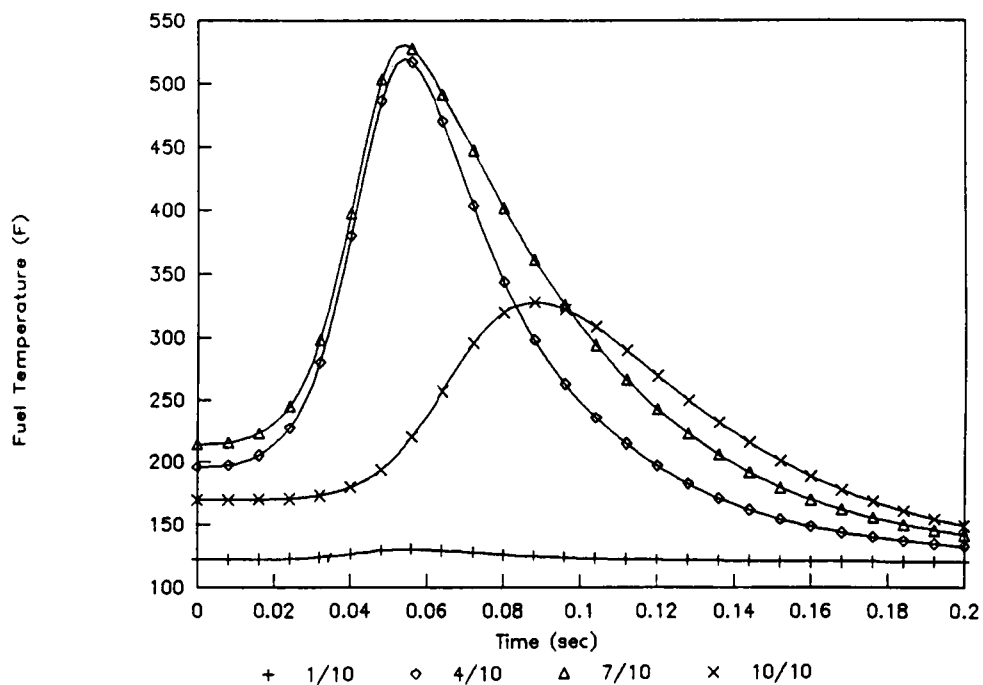


Figure 63 : Inner-element, nominal-plate average fuel temperatures for nodes 1, 4, 7, and 10 during optimum void accident for the EOC, Mode 1, full-power transient.

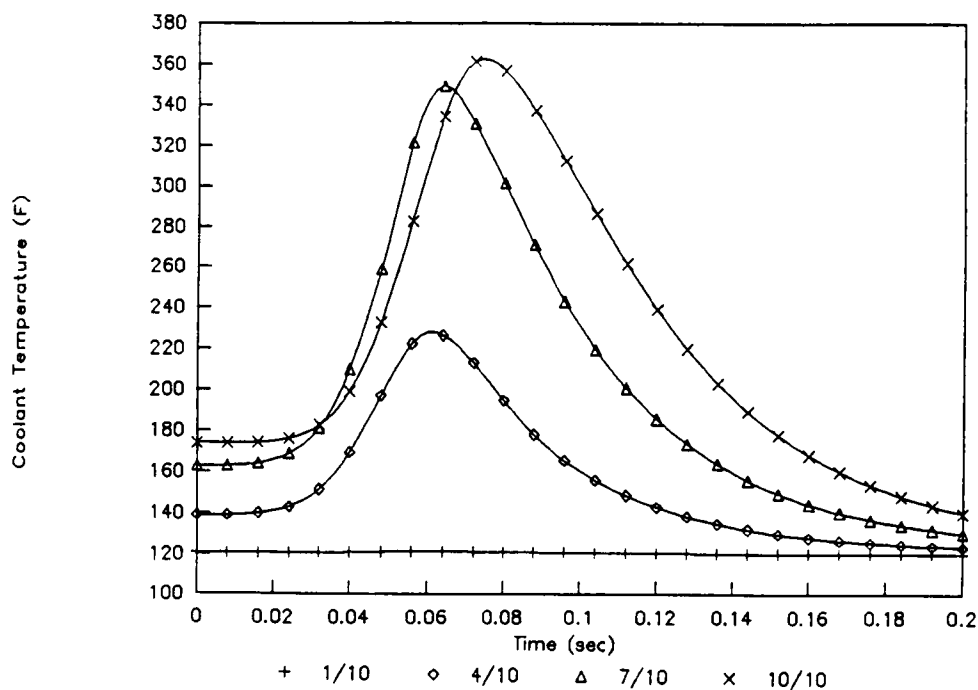


Figure 64 : Inner-element, nominal-channel bulk coolant temperatures for nodes 1, 4, 7, and 10 during optimum void accident for the EOC, Mode 1, full-power transient.

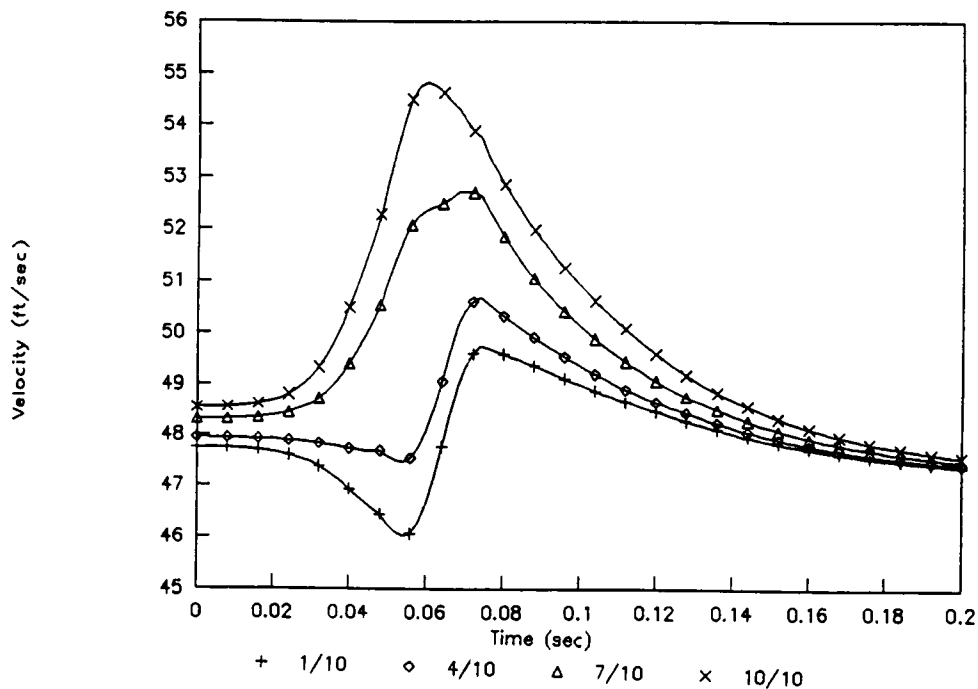


Figure 65 : Inner-element, nominal-channel velocities for nodes 1, 4, 7, and 10 during optimum void accident for the EOC, Mode 1, full-power transient.

shown in Table 35 and 36. Despite the significant increase in peak powers and energy released, both nominal and hot plate/channel temperatures stay below the critical values during these low pressure transients for both BOC and EOC conditions.

If a blockage of coolant flow to the target region takes place, voids, if any, would most likely form at a slower rate even for a rapid boiling situation. To examine the significance of the reactivity insertion rate during target flow blockages, a series of other cases are studied for EOC, Mode 1, full-power sequence with different insertion times. The results of this study indicate that any 1.3% dk/k reactivity insertion time greater than 50 msec eliminates the possibility of hot-spot melting completely.

Some unprotected sequences for the optimum void accident are also investigated even though their combined frequency is far below 1.0×10^{-7} /year limit. For the beginning of the fuel cycle when the thermal-hydraulic conditions of the nominal and hot-channels are typically better, both "*full-power, full-flow*" and "*low-power, low-flow*" cases end up with only partial melting of the hot-plate. For the full-power transient, the peak power is 1126.9 MW while for low-flow transient the peak power is only 67.4 MW. Nominal-plate peak average fuel temperatures are 575.8 and 403.3 °F in the outer-element for "*full-power, full-flow*" and "*low-power, low-flow*" cases, respectively. For the end of fuel cycle conditions, however, both "*full-power, full-flow*" and "*low-power, low-flow*" cases lead to at least partial melting of the core.

7.4. SUMMARY of RESULTS and WORST REACTIVITY ACCIDENTS

A summary of the reactivity transients analyzed are presented in Table 37 in the order of decreased expected frequency. Note that sequences with frequencies less than 1.0×10^{-7} /year are not included in the summary. As shown in the table and discussed in previous sections, two particular transients with frequencies greater than 1.0×10^{-7} /year result in fuel damage. These are the unprotected sequences for the uncontrolled shim withdrawal accident and the accident associated with target voids.

Due to the substantial amount of positive reactivity involved, the unprotected uncontrolled shim withdrawal sequences are expected to result in significant fuel damage and

Table 35 : Peak powers and energy released during transients associated with an optimum amount of voids in the target region. 0.013 dk/k reactivity insertion over 30 msec for low-flow cases.

OPERATING STATES	Peak Power (MW)	Time Integrated Power (MW-sec)	Differential Time Integrated Power (MW-sec)
BOC, Mode 1, Low-Power, Low-Flow	333.2	7.54	6.86
EOC, Mode 1, Low-Power, Low-Flow	135.8	4.90	4.90

Table 36 : Peak temperatures during transients associated with optimum amount of voids in target region. 0.013 dk/k reactivity insertion over 30 msec for low-flow cases.

OPERATING STATES		Peak Average Fuel Temperatures (F)		Peak Bulk Coolant Temperatures (F)	
		Nominal Plate	Hot Plate	Nominal Channel	Hot Channel
BOC, Mode 1, Low-Power, Low-Flow	Inner	411.0	467.2	241.3	273.9
	Outer	431.5	483.4	244.5	279.5
EOC, Mode 1, Low-Power, Low-Flow	Inner	325.9	383.8	225.6	254.1
	Outer	320.3	376.8	225.7	254.3

Table 37 : Summary of the reactivity transients analyzed.

Transient Description	Frequency and Classification ¹²	Results of the Analysis
Protected cold water accident	3.2×10^{-1} (moderate)	Non-damaging
Protected uncontrolled shim withdrawal with shim drive	4.0×10^{-2} (infrequent)	Non-damaging
Protected shim withdrawal with shim and regulating drives	4.0×10^{-2} (infrequent)	Non-damaging
Unprotected cold water accident	2.0×10^{-5} (limiting)	Non-damaging
Unprotected uncontrolled shim withdrawal sequence	2.4×10^{-6} (limiting)	Nominal-plate melt in all Modes
Shim-regulating cylinder ejection	(limiting)	Non-damaging
Single shim-safety quadrant drop	(limiting)	Non-damaging
Accident associated with optimum target voids	$< 8.5 \times 10^{-7}$ (limiting)	Partial hot-plate melt for E1FF

whole-core involvement in all seven operating states considered. Therefore, an assessment for the consequences of this accident requires a severe accident analysis which is beyond the scope of this work.

The optimum void accident, on the other hand, results in only partial melting of the hot-plate for one particular operating state; at the end of fuel cycle, in Mode 1, full-power, full-flow conditions. Since the design requirements and transient acceptance criteria specified for the limiting faults require that nominal fuel structural integrity and a coolable geometry should be maintained, further analysis of this sequence is needed to examine the potential for propagation of hot-plate melting. This analysis is provided in Section 6.3. In this section, the conditions before the initiation of the fuel melting are presented in more detail both for BOC and EOC by providing additional plots of state variables for both the inner and outer elements, and the nominal and hot-channel/plate (Figures 66 to 81). A complete set of results for the optimum void accident for all state variables can be found in Appendix F.

The optimum void accident involve substantial amount of reactivity which is inserted in a very short time. The two most critical transients are for BOC and EOC in Mode 1, full-power, full-flow conditions. Despite the fact that the HFIR core is more reactive for BOC cases (shorter neutron generation time), the safety system action is more effective due to favorable initial rod position, and, with the help of stronger reactivity feedback, the transients are terminated typically more promptly compared to EOC cases as shown in Figure 62. Consequently, the energy released is also higher for EOC cases.

For BOC, in about 40 msec, the power reaches its peak value of 787.7 MW. Despite its promptness, the energy released is only about 9 MW-sec at this point which causes initiation of mild subcooled boiling in the hot-channels of both the inner and outer elements (Figures 66 and 67). Since the subcooled void fraction in the boiling regions is very low, the mixture densities are typically unaffected by the boiling process. However, similar to its rise, the power drops rapidly after this point with the help of scram action, avoiding a CHF situation. A pressure spike accompanies the power excursion causing a decrease in coolant velocity at the channel inlet, and accelerating the coolant out of the core at the outlet as shown in Figures 68 to 70. In parallel with the increase in power, the average fuel and

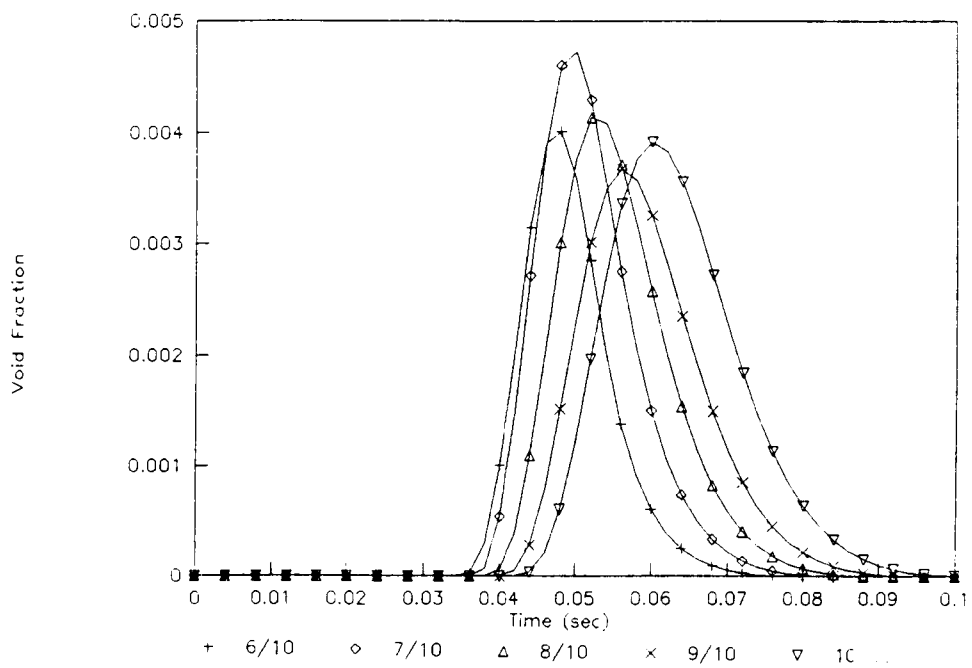


Figure 66 : Inner-element hot-channel void fractions for lower half of the core during optimum void accident for BOC, Mode 1, full-power transient.

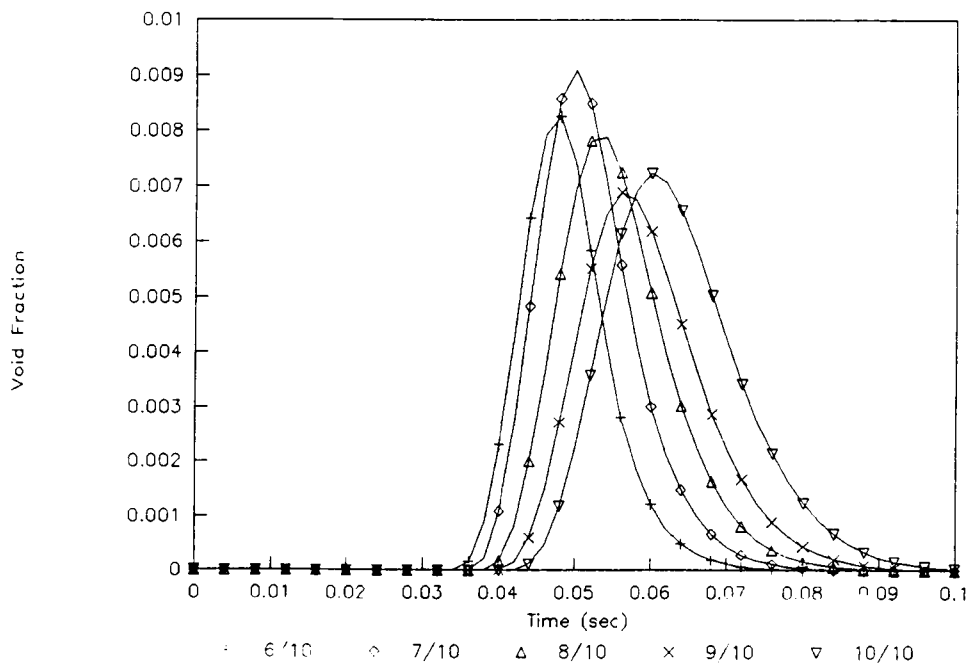


Figure 67 : Outer-element hot-channel void fractions for lower half of the core during optimum void accident for BOC, Mode 1, full-power transient.

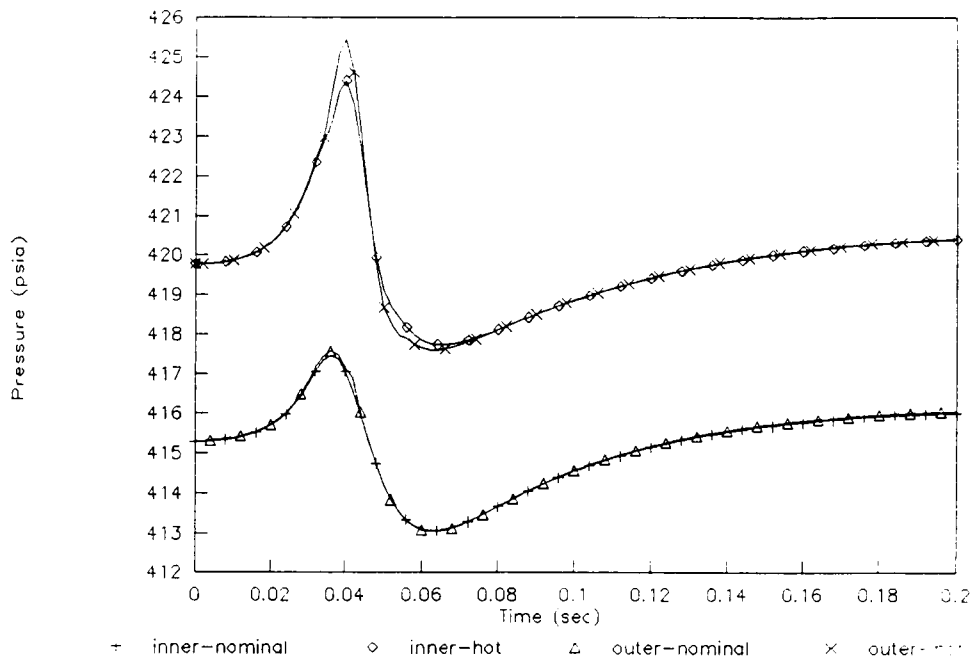


Figure 68 : Fourth node (out of ten) pressures during optimum void accident for BOC, Mode 1, full-power transient.

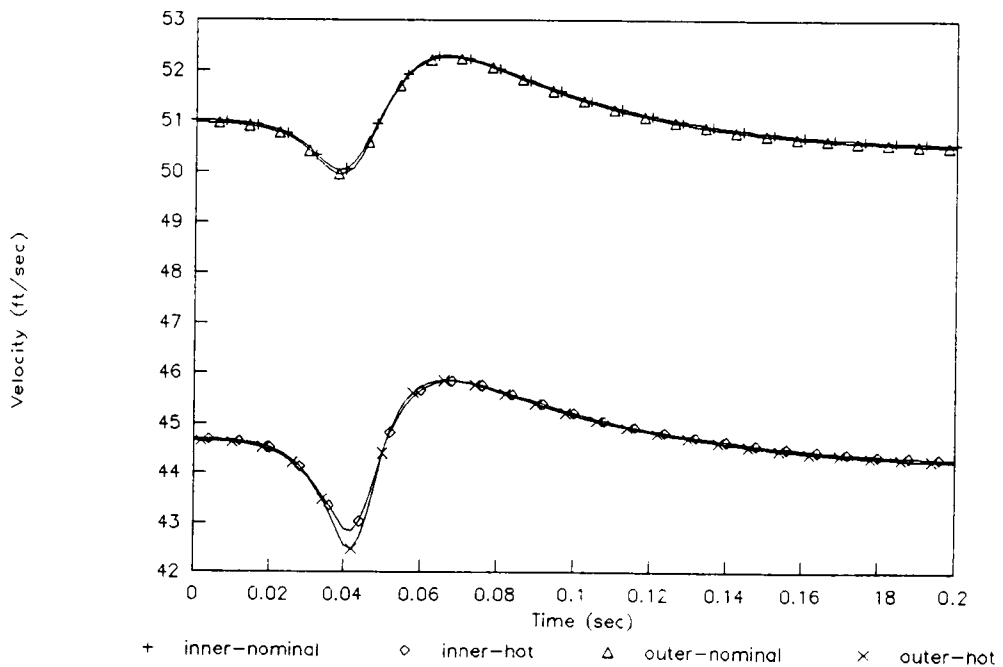


Figure 69 : Inlet node velocities during optimum void accident for BOC, Mode 1, full-power transient.

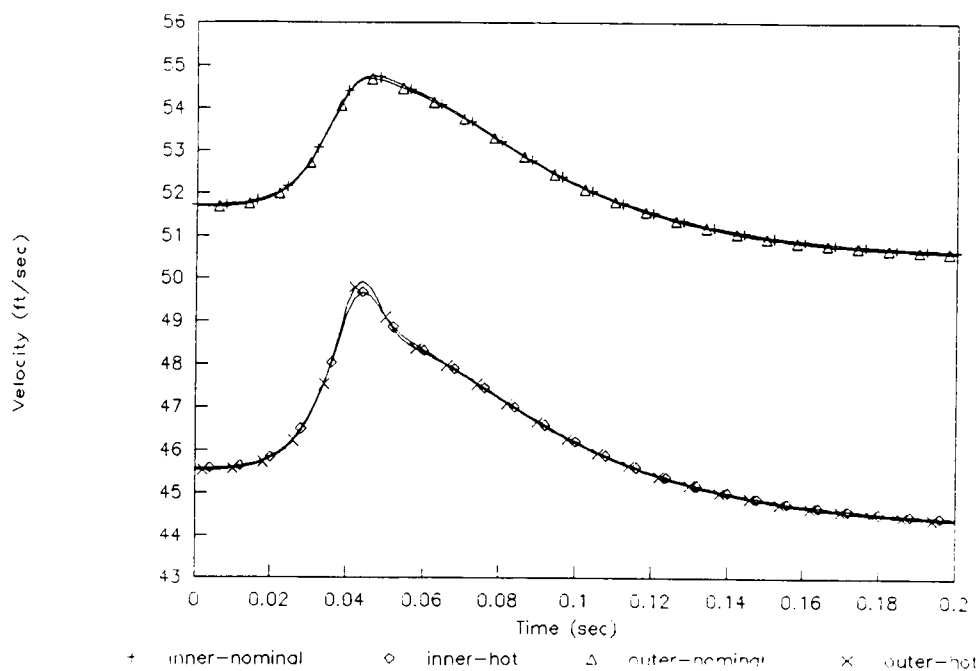


Figure 70 : Outlet node velocities during optimum void accident for BOC, Mode 1, full-power transient.

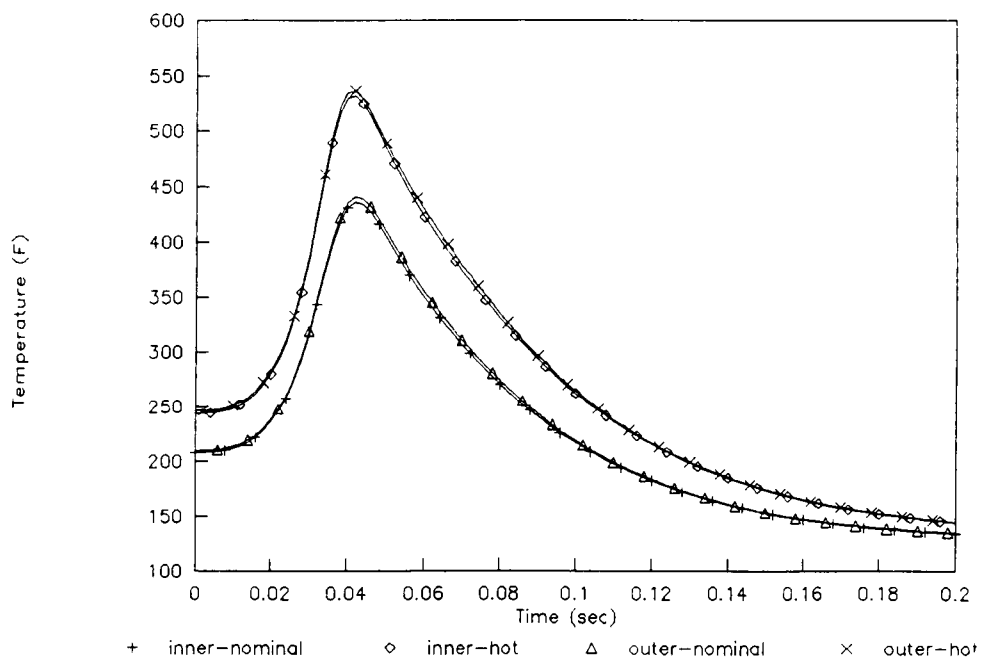


Figure 71 : Seventh node average fuel temperatures during optimum void accident for BOC, Mode 1, full-power transient.

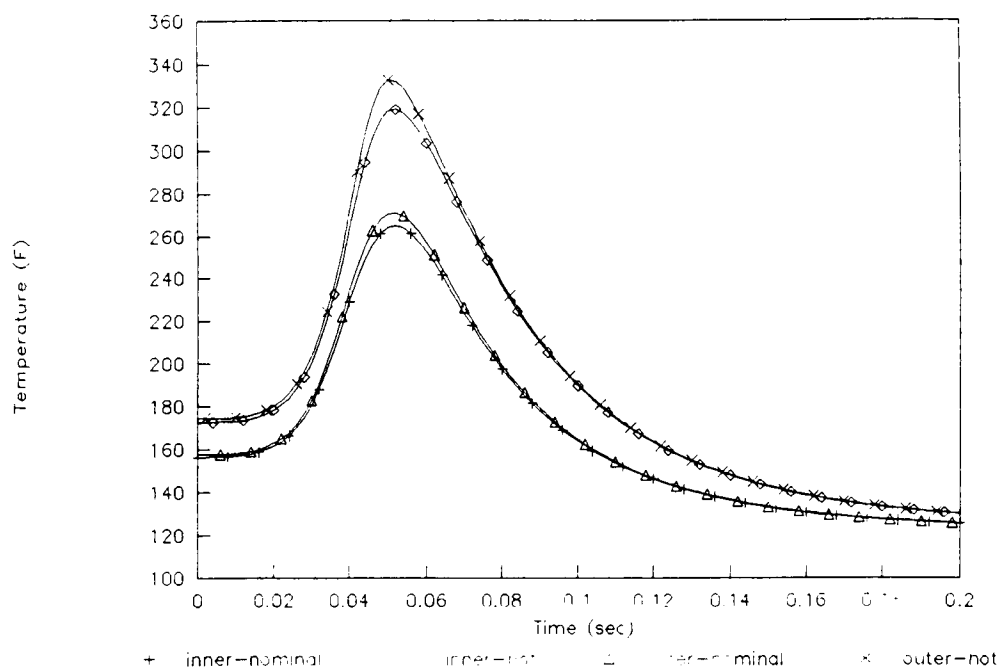


Figure 72 : Seventh node liquid phase temperatures during optimum void accident for BOC, Mode 1, full-power transient.

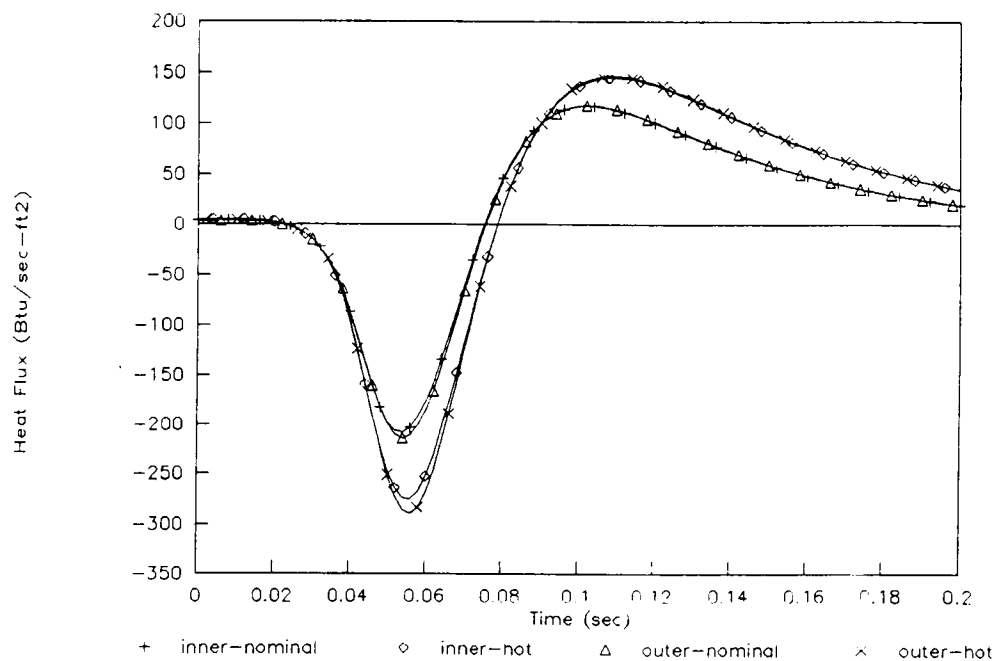


Figure 73 : Outlet node surface heat-fluxes during optimum void accident for BOC, Mode 1, full-power transient.

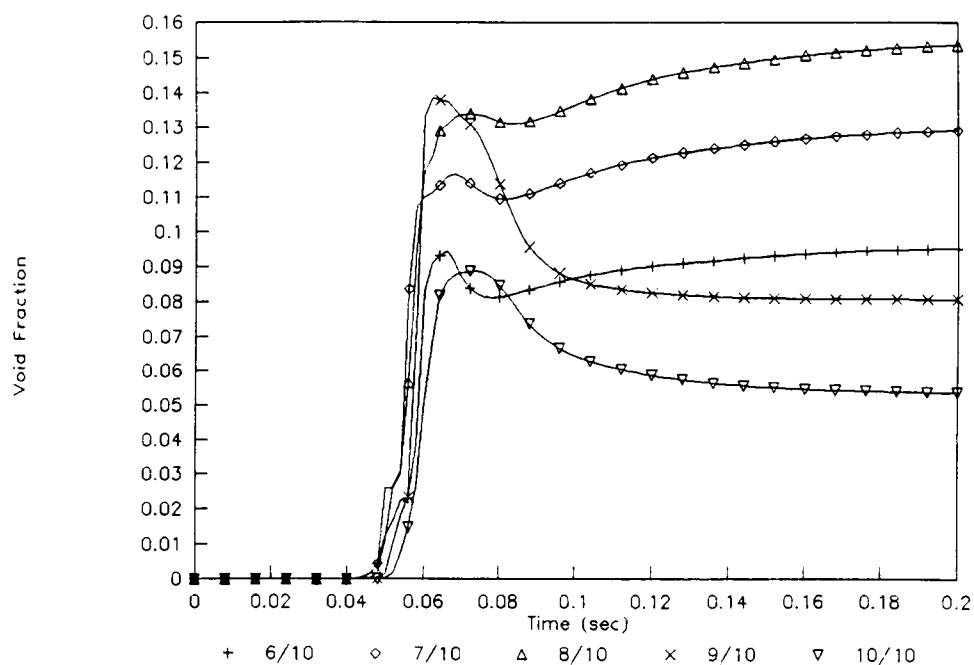


Figure 74 : Inner-element hot-channel void fractions for lower half of the core during optimum void accident for EOC, Mode 1, full-power transient.

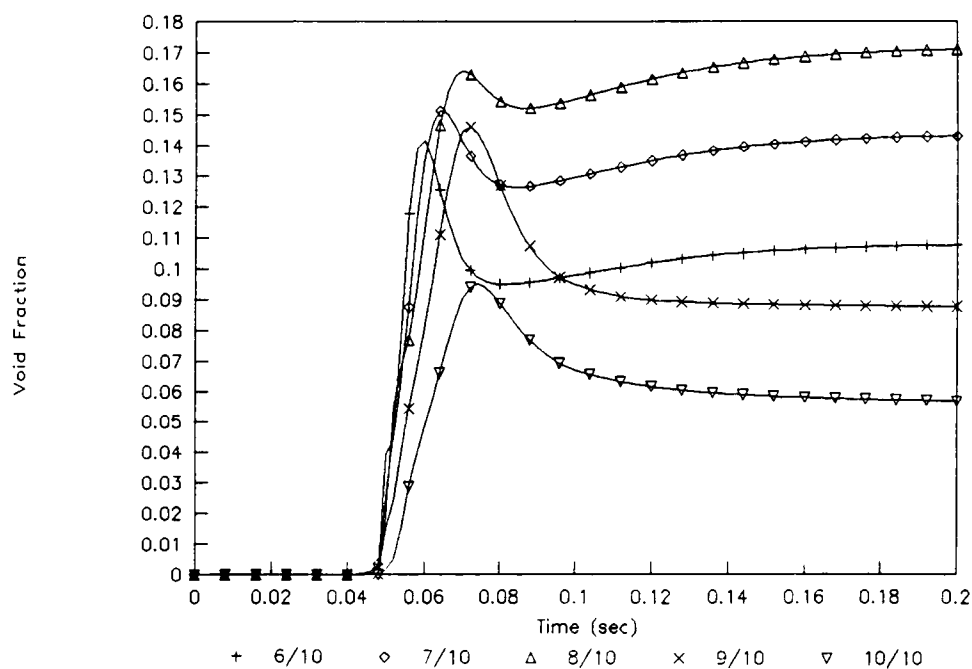


Figure 75 : Outer-element hot-channel void fractions for lower half of the core during optimum void accident for EOC, Mode 1, full-power transient.

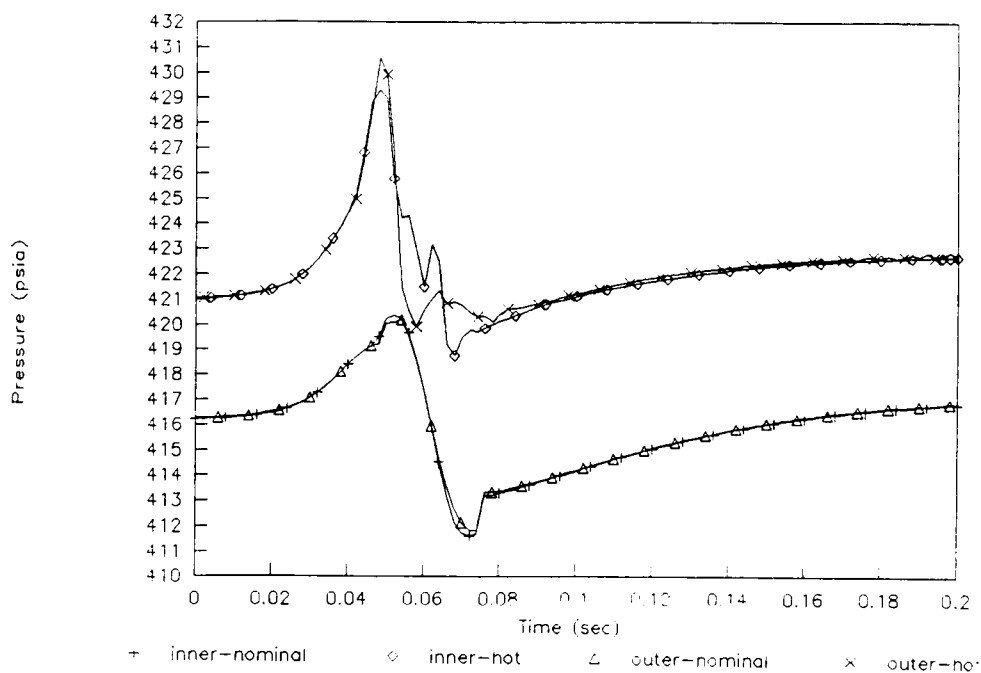


Figure 76 : Fourth node (out of ten) pressures during optimum void accident for EOC, Mode 1, full-power transient.

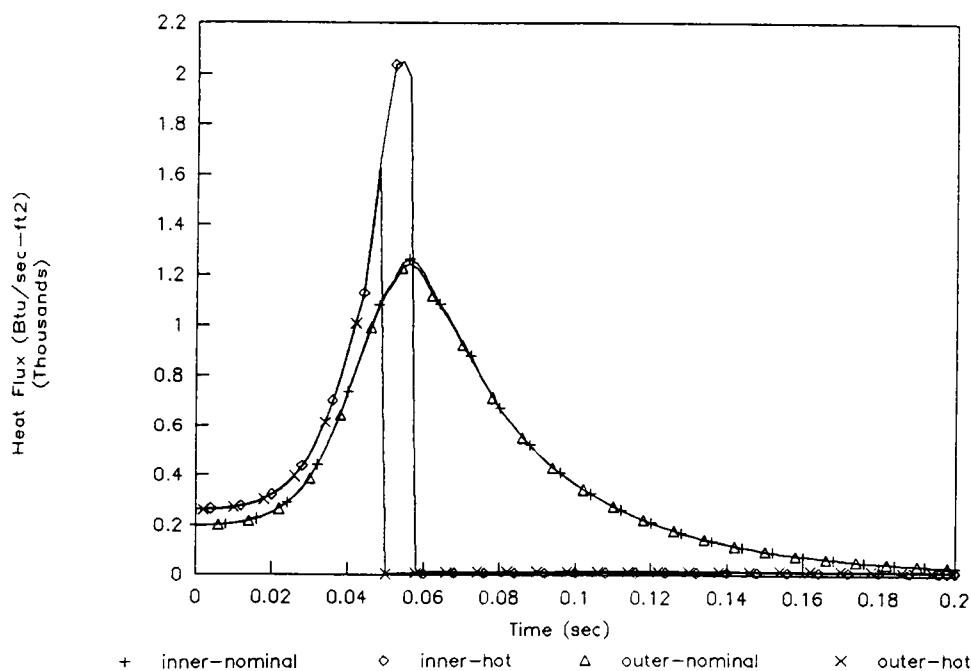


Figure 77 : Fourth node surface heat-fluxes during optimum void accident for EOC, Mode 1, full-power transient.

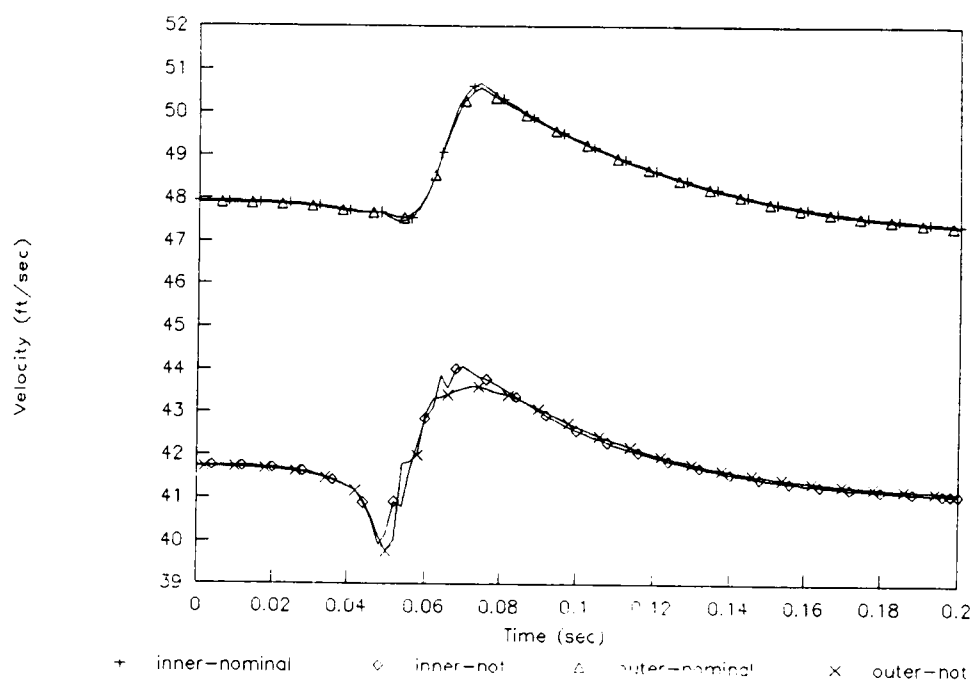


Figure 78 : Fourth node velocities during optimum void accident for EOC, Mode 1, full-power transient.

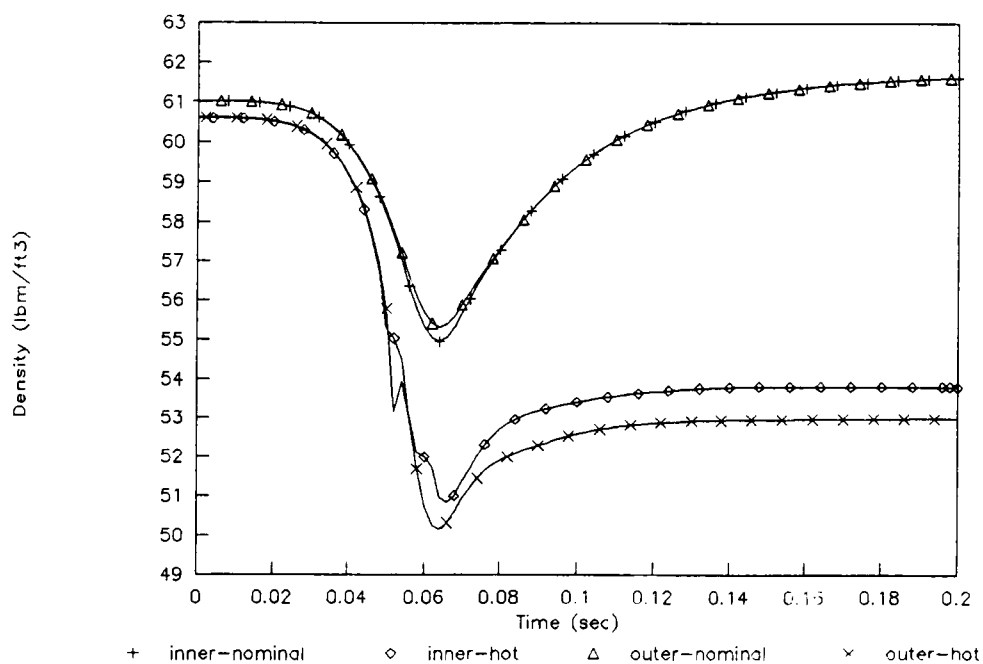


Figure 79 : Seventh node two-phase mixture density during optimum void accident for EOC, Mode 1, full-power transient.

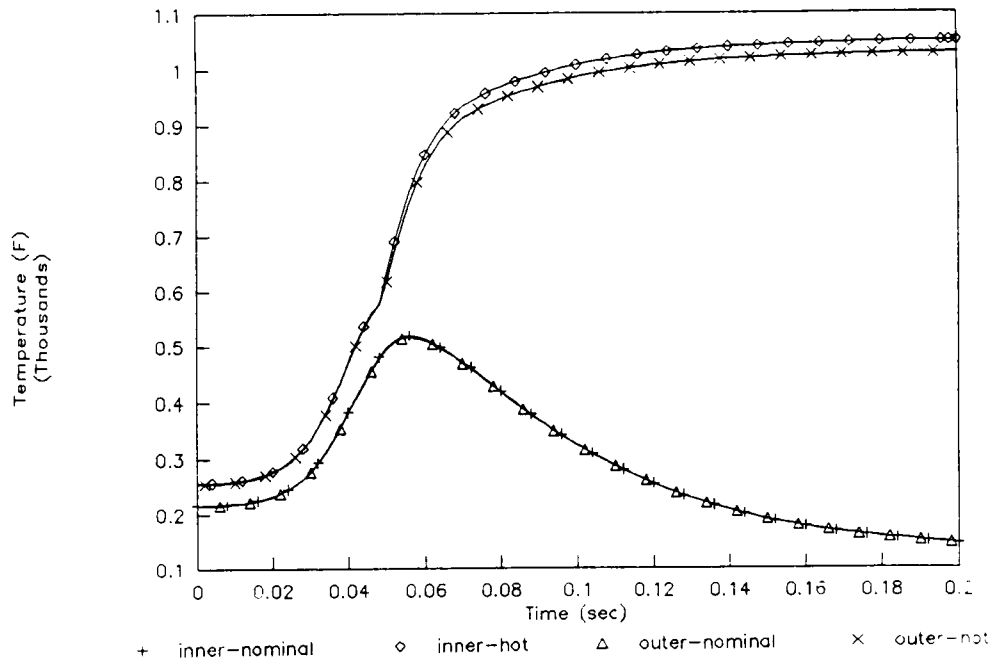


Figure 80 : Seventh node average fuel temperatures during optimum void accident for EOC, Mode 1, full-power transient.

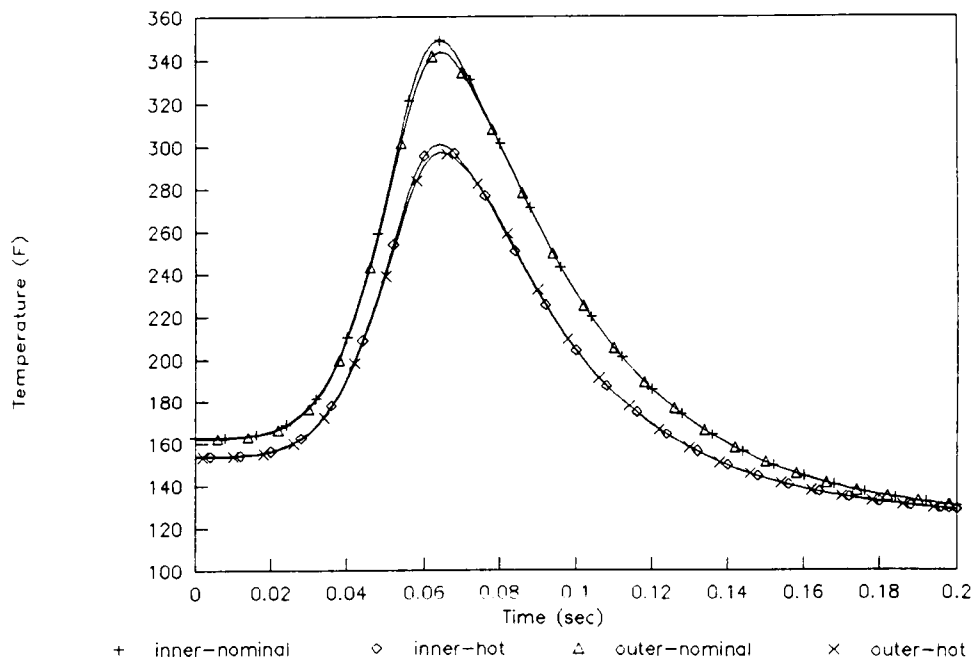


Figure 81 : Seventh node liquid phase temperatures during optimum void accident for EOC, Mode 1, full-power transient.

surface temperature curves follow a similar profile reaching their peak values only a few milliseconds after the power peaks (Figures 71 and 72). Since the inlet and outlet nodes (1/10 and 10/10 plots) reside beyond the active length of the fuel plates, there is very little internal heat generation associated with these nodes. Therefore, as the coolant outlet temperature reaches its peak value, the fuel plate outlet node is typically colder resulting in a negative surface heat flux as shown in Figure 73.^a Complete results for BOC-Mode 1 optimum void accident are presented in Appendix F.

For the EOC transient, not only is the peak power higher (803.5 MW), but it also takes longer to reach this peak value resulting in significant additional energy release (about 15 MW-sec at the peak power) compared to the BOC transient (Figure 62). This leads to initiation of subcooled boiling in both the nominal and hot channels in the inner and outer elements. As shown in Figures 74 and 75, the hot-channel void fraction is four times larger than the BOC void fractions. The time to boiling is about 50 msec which leads to mild pressure spikes in the coolant channels as shown in Figure 76. At this point, the power is still rising and immediate film blanketing of the heat transfer surfaces due to extremely high surface heat fluxes leads to a critical heat flux situation in the hot channels in the subcooled boiling regime.

This transition to film boiling affects the fluid dynamics variables in an unusual way. The sonic velocity decreases significantly by more than one order of magnitude as film boiling begins. This characteristic exists because the sonic wave propagates faster in the single-phase region and slows down considerably in the two-phase region. This sudden change in the sonic velocity at the boiling boundary imposes extra problems on the stability of the numerical scheme.^o In addition, the discontinuities associated with the heat transfer mechanisms (see Figure 77) adversely affect the stability forcing the time-advancement scheme to use smaller time steps. Therefore, for the cases in which either bulk or film boiling is initiated, the time

^a Negative surface heat flux means heat flowing from the coolant to the fuel.

^o In addition to the sonic velocity, the transition to film boiling affects the partial derivatives of the mixture density with respect to enthalpy and pressure (quantities described in Equations (133) and (134)) which result in discontinuities in the solution scheme.

step becomes prohibitively small from a computer time standpoint. Partially due to these numerical problems, some of the fluid-dynamics variables (namely the hot-channel pressure and velocities) exhibit violent oscillations at the film boiling front as seen in Figures 76 and 78. This behavior also reflects on the mixture densities to the extent shown in Figure 79.

The thermal variables, however, are largely unaffected by the numerical oscillations as shown in Figures 80 and 81. With the initiation of film boiling, the hot-plate average fuel temperatures for nodes 4, 5, 6, 7, and 8 keep increasing beyond the typical peak values, and the poor heat transfer in the film boiling regime leads to extreme temperatures in about 200 msec. The sudden decrease in the heat transfer after the critical heat flux is exceeded is displayed in Figure 77. Again, complete results for this EOC sequence are provided in Appendix F.

The results of this analysis are subject to uncertainties in several key safety-system parameters such as initial acceleration of the safety plates, reactivity worth curves, and position of the plates for initial criticality. In addition, since a transient may be limiting for any initial condition set such as initial power, coolant inlet temperature, system pressure, and coolant flow rate, a wider range of parametric studies is needed to determine the bounding initial conditions. In order to address these issues, the optimum void accident for both BOC and EOC is further studied to determine these limiting factors and the degree of conservatism in the model.

For the worst case scenario, the primary temperature is assumed to be at the high alarm limit of $(125+1.2) = 126.2$ °F. Initial reactor power is assumed to be 87.6 MW. The primary pressure is assumed to be at the low alarm setpoint of $(418-12) = 406$ psi. Flow rates at the lowest limit allowed by flux-to-flow ratio of $(1.25*1.03) = 1.2874$ and at the design full-flow of 15840 gpm are considered for these Mode 1 cases. This reduction in total flow is translated to the inner-element nominal coolant channel flow rate as $(0.99*51) = 50.5$ ft/sec for the BOC transient. The flow rates in the inner-element hot-channel, the outer-element nominal channel, and the outer-element hot-channel for both BOC and EOC are determined using inner-element nominal-channel flow rate as the base value. In parallel with these changes in operating parameters, the initial conditions are also adjusted accordingly. With

this initial condition set, the inner-element nominal-channel inlet and outlet pressures are 389.1 psia and 292.4 psia for BOC, respectively. For EOC, the respective pressures are 390.0 psia and 293.5 psia for inner and outer elements. The hot-channel pressure profiles are also calculated using the nominal-channel pressure drop as the base value. The results for this worst initial condition set indicate a partial hot-plate melting only in the outer-element for the BOC transient. The inner element hot-plate average fuel temperature reaches 557.1 °F. For the EOC transient, however, in addition to hot-plate melting, a partial melting of the nominal plates are predicted. It should be noted that, the changes in the inlet temperature and coolant flow rate do not affect the outcome of these transients significantly. The changes in the system pressure is the vital parameter in terms of fuel damage. For both BOC and EOC, the peak power is actually lower (777.0 MW for BOC and 797.7 MW for EOC). Nevertheless, the channel pressure is also considerably lower causing a CHF at even lower power levels and leading to fuel damage.

In order to examine the uncertainties in the safety system set-points, two additional transients are studied. For these transients the reactor trip is assumed to occur at a flux-to-flow ratio of $1.25 \times 1.03 = 1.2875$ or a rate of change of $20 \times 1.1 = 22$ MW/sec in Mode 1. Also, to cover the uncertainties in the scram delay time, the technical specification requirement of 50 msec is used. These changes result in substantial increases in peak powers for both BOC and EOC transients. For BOC, the peak power reaches 1126.9 MW, and for EOC it is 935.8 MW. In parallel with these rises in power, the energy released during each transient also increases to 22.2 MW-sec for BOC, and 39.0 MW-sec for EOC. Despite the larger increases in power and energy released, only hot-plate melting is predicted for BOC transient. The nominal-plate peak average fuel temperatures elevate to 552.4 °F and 575.8 °F for the inner and outer elements, respectively. For the EOC transient, however, the increase in power is sufficient to initiate melting in nominal conditions as well as in hot-plates.

Another uncertainty regarding the safety system action is associated with the safety elements' time of flight during scram. The HFIR Technical Specifications require that the safety system response time be no longer than 400 msec. As discussed in Section 4.4, the motion of the shim-safety quadrants in this model is characterized by a linearly decreasing initial acceleration of these elements. It is assumed that the initial acceleration is equivalent

to 4g for the spring loaded quadrants which results in approximately 250 msec for the time of flight. In order to investigate the effect of a different scram response time, a series of other cases are analyzed with initial accelerations ranging from 5g to g. A change in the initial safety quadrant acceleration makes a greater impact for BOC transients for which the initial plate position is more favorable compared to the EOC cases. The results indicate that a 5g initial acceleration does not eliminate the hot-plate melting at the end of cycle conditions. Lowering the initial acceleration from 4g to 3g does initiate a hot-plate melting for BOC, but does not spread the hot-plate melting to nominal-plates for the EOC transient. And finally, reducing the initial acceleration down to 1g, which results in a 375 msec time of flight, leads to fuel damage for the nominal core conditions for both BOC and EOC transients. It should be recognized, however, that a 1g initial acceleration is an extremely unrealistic situation since it does not consider the effect of spring loading and coolant hydrodynamic forces.

The sensitivity of the results to the initial control element position for the BOC transient is also examined.^P For the beginning of cycle (BOC) conditions, the safety and the regulating control elements are assumed symmetric and the distance the plates are withdrawn from the core for initial criticality is conservatively taken as 18.54 inches as an extreme case.⁸¹ A series of other cases are studied in which the distance the plates are withdrawn changes in the range of $(18.54" \pm 1.0")$. As expected, a decrease in the distance the plates are withdrawn reduces the transient overpower while an increase works in opposite way. However, the results do not seem to be sensitive to the initial plate position in this range ($\pm 1.0"$), and neither extreme leads to fuel damage.

Finally, a series of other cases are studied in which one or more shim-safety quadrants fail to scram as suggested in various earlier HFIR reactivity accident analysis reports.^{4,25,3} Briefly, a 3-plate scram (out of 4) results in hot-plate damage for the BOC transient, and a nominal-plate damage for the EOC transient. Further reducing the number of shim-safety quadrants involved in the scram action does not change the results in terms of fuel damage.

^P For the end of cycle (EOC) conditions, plates are assumed fully withdrawn conservatively.

8. HFIR FUEL ELEMENT FAILURE PROPAGATION

As discussed in previous sections, a partial hot-plate melting is predicted for the optimum void accident in Mode 1, at the end of fuel cycle, at full-power, full-flow conditions. For the limiting faults, some local fuel melting is generally acceptable provided the damage is self-limiting. Since the design requirements and transient acceptance criteria impose that nominal fuel structural integrity and a coolable geometry should be maintained during such an accident, a further analysis of this sequence is needed to examine the potential for additional melting beyond the hot-plate.

Melting of the cladding material begins at about 1205 °F. It should be noted that neither the fuel nor cladding melt at a single temperature. It is reasonable to assume, however, that the fuel disruption will occur before 2060 °F where melting of the fuel plate is complete. Fuel failure consequences varies from loss of fuel (molten fuel ejection) which may cause mild fuel coolant interactions, to damage of adjacent plates, chemical reactions, and blockage formations. The initial fuel disruption modes and the conditions at the onset of fuel melting are discussed in more detail in the next section by providing additional information obtained from a literature review. The hot assembly results are presented in the following Section 6.3.2. The questions concerning whether or not the hot-plate melting is self-limiting and maintaining core coolability are addressed also in Section 6.3.2.

8.1. INITIAL DISRUPTION MODES

As the film boiling regime starts in the hot-channel and the hot-plate approaches the melting temperature, the structural integrity of the fuel elements can be lost through a number of disruption modes.¹⁰¹ These are thermal and mechanical stress induced disruptions due to initial core heat-up and coolant flow, chemical reaction induced disruptions due to U_3O_8 -aluminum and fuel-coolant interactions, and the disruption due to internal fission gas pressure as the fission gas bubbles coalesce and grow with the change of phase. The resulting fuel plate melting and possible coolant channel blockages may have significant implications for the transient. Throughout this section, Reference 101 is used extensively.

8.1.1. Loading Stresses

With the exception of thermal stress induced fragmentation, one possible mechanism for early disruption of the HFIR fuel is buckling of the plate under its own weight and coolant hydrodynamic forces. Creep rates of the aluminum matrix in the fuel meat and the aluminum cladding are sufficiently low at temperatures below 800 °F that the deformation is essentially elastic. In this range, the critical buckling of a plate under its own weight and coolant hydrodynamic forces can be calculated via the well known Euler column formula.¹⁰² Estimated values for HFIR fuel elements yield a critical stress which is at least 100 times greater than the applied maximum stresses during a full-flow reactivity transient. As the temperature of the plates increase above 800 °F, the strength of aluminum continues to decrease. However, design data for aluminum alloys up to 700 °F shows that the yield strength is at least 1000-2000 psi at the upper limit.¹⁰¹ Plastic strain at high temperatures can also occur by "creep" where the deformation is rate-dependent. For low loading stresses, the creep rate is typically proportional to the applied stress, and a catastrophic increase in the deformation is not expected for HFIR plates since the elastic buckling load is not easily exceeded. These analytical findings suggest that classical rate-independent yielding of the fuel plates under maximum applied stresses is unlikely to occur until the temperature is near the melting point.

Experimental tests have been performed on U_3O_8 -aluminum cermet fuels with additional weights attached to simulate compressive stresses near the core midplane of an assembly where the transient temperatures are highest during a severe accident.¹⁰³ The results of these cermet fuel tests show that fuel plate collapse under its own weight only occurs after the aluminum melting point (1220 °F) is exceeded. In addition, measurement of the collapse temperatures of Savannah River Reactor fuel tubes show that the short segments of fuel tubes (18 inches long) does not collapse under their own weight until 1237 °F which is well above the fuel solidus temperature.¹⁰³ These results confirm that loading stresses will not cause early disruption of fuel plates. Thus, for non-equilibrium heating conditions during a reactivity excursion, the most reasonable lower bound for a loading stress induced disruption would appear to be the aluminum solidus temperature where both the cladding and the matrix of the fuel meat melt.

8.1.2. Chemical Reactions

During the initial heating phase of the optimum void accident, only the aluminum cladding is exposed to the steam atmosphere. The measurements of the oxidation rates for aluminum by vapor at temperatures near the melting point indicate that the thickness of the surface oxidation of the polished samples were typical of the films that would form on aluminum at lower temperatures and longer times.¹⁰⁴ Such films are expected to exist on the HFIR fuel plates prior to accident initiation as discussed in Section 4.1.3. Therefore, very little additional oxidation would occur before clad melting. Once the clad starts melting, however, fresh cladding surfaces may be exposed to the high velocity steam which leads to further oxidation. The rate of oxidation of the molten aluminum by steam at temperatures within the range of interest is approximated by¹⁰⁵

$$w_{Al} = \left[4.0 \cdot 10^9 t \exp\left(-\frac{73500}{RT}\right) \right]^{\frac{1}{3}} \quad (258)$$

where w_{Al} is the weight of the aluminum reacted (mg/cm^2),

t is time (min),

R is gas constant ($1.987 \text{ cal}/\text{mole}\cdot\text{K}$), and

T is temperature (K).

The time t over which oxidation may contribute to the disruption of a partially molten fuel plate depends on the heating rate and the difference between the solidus and liquidus temperatures. Conservatively assuming that it takes more than an hour for the fuel plates to reach liquidus temperature ($\sim 1400 \text{ K}$) and all the oxidation occurs at that temperature, the maximum thickness of the oxide layer which would form on these newly-exposed molten aluminum surfaces does not exceed 0.4 mils which is insignificant compared to actual fuel plate and clad thickness.

If the molten cladding drains off the fuel plates prior to disruption, the remaining Al-uranium fuel meat will become exposed to the steam environment. Because the solidus temperature of the fueled region is very close to clad solidus temperature, the fuel meat

would be expected to be partially melted at this stage. Similar measurements for molten aluminum-uranium alloy suggest that the oxidation of the fuel meat would not influence the disruption.¹⁰⁶ In these experiments, the reaction between Al-20 wt% U alloy and steam was determined at 1700-2500 °F by measuring the amount of hydrogen generated during the reaction. For temperatures less than 2000 °F, the thickness of the Al₂O₃ sheath which is formed around molten globules is predicted to be 0.15 mils which is again very small compared to thickness of the fuel meat.

Two other potential chemical reactions for the HFIR fuel involve aluminum and U₃O₈ inside the fuel element, and the aluminum cladding and water at the fuel surface. TREAT tests¹⁰⁷ on chemical reactions involving HFIR type fuel indicate that the uranium-aluminum alloy fuel plates fuse at energies between 350 and 950 Btu/lb_m. However, the fuel samples retain their shape at energies as high as 750 Btu/lb_m. The minimum temperature at which a significant chemical reaction is believed to occur is 1500°F for the Al-U₃O₈ reaction.¹⁰⁸ The energy release for a complete reaction is calculated to be 0.0358 and 0.0438 MWsec per plate, for the inner and outer fuel elements respectively.⁴ These numbers are based on a maximum credible energy release and Al-U₃O₈ composition in the meat. The time scale of the energy release is also an important factor.

Although the minimum temperature for the Al-water reactions is reported to be 2140°F,¹⁰⁹ this reaction usually requires molten aluminum to be dispersed as small droplets or vaporized metal into steam or water for the reaction to proceed to a significant extent. A temperature of 3180°F is required for an essentially complete reaction.¹¹⁰ Due to its explosive nature, a separate analysis is needed for this reaction and the minimum reaction temperature is to be included as an upper limit in this work.

8.1.3. Internal Fission Gas Pressure

A very high percentage of the fission products generated during steady-state irradiation is retained in the HFIR's aluminum-based fuel plates. Of these products, about 0.246 atoms of insoluble krypton and xenon are generated per fission reaction.¹¹¹ Most of this gas is retained in the fuel matrix as single gas atoms or in very small bubbles under high

surface tension restraint and is expected to cause little swelling under normal operating conditions (Section 4.1.2). However, during reactivity accidents that lead to fuel melting, the fission gas in the fuel matrix may coalesce into larger bubbles which would transform the molten fuel into a foam-like structure.¹¹² Formation of such a foam will have significant implications on the subsequent course of the accident because of fuel reactivity feedback effects caused by density changes, uranium segregation, and fuel relocation. Also, depending on the flow characteristics and channel pressure when melting occurs, fuel foam may fill the narrow coolant channels between two neighboring plates resulting in flow blockages.⁹

Fuel foaming is essentially an end of core cycle concern when fuel burnup and fission product accumulation is highest. The swelling potential of the fuel for these conditions can be estimated by comparing the volume into which molten fuel can expand with the maximum volumetric fraction that fission gas can occupy free of its surface tension restraint. By approximating the fission gas behavior with the ideal gas law, it is predicted that the swelling potential (whether or not fuel could expand to fill the maximum volume) is proportional to the fuel burnup and inversely proportional to the channel pressure. Therefore, the reactivity transients in which significant pressurization takes place is expected to have less fuel swelling potential compared to depressurized protected events such as a LOCA. Partial blockages seems more likely for pressurized transients. Nevertheless, the potential for foaming of the HFIR fuel to fill the voided coolant channels cannot be ruled out.

The kinetics of fuel foaming is also important because the time scale between initial fuel melting and fuel mobility may be too rapid to allow the needed gas bubble coalescence or may be too long to maintain the foamed-up state once the full swelling potential is realized. Most of the retained fission gas in the HFIR fuel is expected to be in the form of small bubbles or in solution within the uranium-rich phases of the fuel matrix. Under transient heating, this gas will become mobile giving rise to bubble coalescence from both random and biased bubble migration. However, unlike ceramic fuels, the small temperature gradients in aluminum-based fuels tends to limit the amount of coalescence until melting

⁹ The term "foam" is usually reserved for agglomerations of bubbles where the void fraction exceeds 0.9. Dispersions of gas bubbles in liquids with smaller void fractions are termed "gas emulsions." {5-7}

occurs. At this point the bubble mobility increases by many orders of magnitude.

Earlier analytical work on foaming of molten nuclear fuels by fission gases is based on a similarity solution to a formulation developed for analyzing gas bubble behavior in solids.¹¹³ It is concluded that for mixed oxide LMFBR fuels the "fuel frothing" time depends strongly on the initial average bubble radius and it could be as rapid as 1 msec following fuel melting.¹¹⁴ Also, the governing equations for this formulation¹¹³ are solved numerically to investigate gas bubble behavior in molten ceramic and metallic fast reactor fuels.^{115,116} In these studies, it is shown that bubble distributions resulting from coalescence by bubble migration under buoyancy forces probably cannot be represented by a similarity solution which reduces the time and bubble-size variables to a single independent variable. However, these calculations did show that the evolution of the bubble distributions under buoyancy forces produces rapid swelling behavior at a certain critical time similar to the results given by the similarity solution. The results indicate bifurcation of bubble distribution and rapid swelling after about 3 msec.¹¹⁶

There is also considerable experimental evidence that retained fission gases in metallic fuels can indeed rapidly coalesce into larger bubbles once melting begins even during rapid reactivity transients. One of the most recent examples comes from TREAT tests¹¹² where EBR-II fuel pins were examined under 8-second period transient overpower conditions with heating rates of 100 K/sec at the time of fuel melting. The results of these tests indicate that massive gas bubble coalescence did occur in a time period of less than 300 msec. Also preliminary in-cell tests performed on the proposed HWR-NPR fuel shows clear evidence that extensive foaming of aluminum-based metallic fuels can occur.¹⁰¹

During a reactivity transient, a distribution of bubble sizes will arise in the foam. The bubbles will not grow indefinitely, however, and the maximum bubble radius is expected to be limited by the Taylor instability.¹⁰¹ In overall, swelling of molten fuel will stop once all of the retained fission gas has collected into larger bubbles that are free of their surface tension constraint. Whether or not this foamed-up state is stable and the time scale over which such a swollen state would remain intact in the absence of external forces other than gravity is also important for the analysis of fuel damage propagation. The effects of hydrodynamic forces

or fuel entrainment is considered in the following section.

There is evidence that, once formed, fuel foam will remain stable for a considerable time before it collapses. Analytical studies suggest that the foam will remain stable until the fluid ligaments between the bubbles drain to the point at which they begin to rupture.¹¹⁷ The case of film drainage over a bubble at a liquid-gas interface assuming a balance between gravity and viscous forces have been studied by Hartland.¹¹⁸ Additional studies were performed on film drainage when the film was thin.¹¹⁹ And finally, work performed during the HWR-NPR fuel design studies indicates that, under a heating transient, bubble agglomeration within the foam will initially occur at a very rapid rate followed by a more stable foam period as agglomerated bubbles reach a critical size.¹⁰¹ The calculated foam half-life for proposed Al-based fuel for HWR-NPR reactor is expected to be in the range of 10-25 minutes for different components of the fuel assembly. In summary, analytical studies indicate that if foams form in Al-based fuels, they would be expected to remain stable for a significant time period in the absence of hydrodynamic forces.^f

8.1.4. Coolant Hydrodynamic Forces

An important aspect of the initial fuel disruption mode is the extent to which molten fuel can be torn from the liquid film and entrained by the high velocity coolant during the reactivity transient under consideration. Since the fuel plate surface for nodes at melting temperatures is covered with a vapor film, interaction of molten aluminum alloy with saturated steam should be considered. The steam at given velocities and pressures is expected to flow across the molten metallic fuel film stripping off molten fuel droplets.

Various correlations for gas flowing over liquid layers and films are available in the literature for estimating the onset of entrainment and entrainment rates.¹²⁰⁻¹²⁵ The threshold

^f It should be pointed out, however, an alternative calculation of foam stability in molten uranium-aluminum fuels concludes that film drainage is very rapid ruling out any lasting foamed condition.¹⁰¹ This calculation is based on an equation for ligament thinning derived from a solution of Spefan's equation for liquid squeeze films and considers additional factors such as the effect of ionizing radiation on film rupture.

steam velocity for the onset of entrainment of a liquid film is often given by a relationship of the form¹²⁰⁻¹²²

$$v_{ent} = C \left[\frac{(\rho_f - \rho_g) \sigma g g_c}{\rho_g^2} \right]^{1/4} \quad (259)$$

where v_{ent} is the threshold steam velocity for entrainment,

ρ_f is liquid (metal) film density,

ρ_g is steam density,

σ is the liquid surface tension,

g is gravitational constant.

A value of $C=2.46$ for the correlation coefficient has been shown to provide a good prediction of entrainment inception in most situations.¹²⁰⁻¹²²

Using this value, scoping calculations are performed over a range of potential optimum void accident steam conditions in the HFIR hot-channels. Molten fuel is considered to be pure metallic liquid with no oxides or small grains of unmelted material in it. The steam is assumed to be saturated. The results are summarized in Table 38. As indicated in the table, over the saturated steam pressure range of 300 psia to 500 psia, the entrainment onset steam velocity varies from 31.6 ft/sec to 24.4 ft/sec. During the optimum void accident, when the hot-plate temperatures for nodes with a high power density (nodes 5, 6, 7, and 8) reach the melting temperature, the hot-channel pressure for the corresponding coolant nodes varies from 412 psi to 380 psi. In this range, the equation (259) yields an entrainment inception velocity of 27-28 ft/sec while the hot-channel vapor velocity ranges from 44 ft/sec to 48 ft/sec.

Several other entrainment inception models are also reviewed.¹²³⁻¹²⁵ Of these, only one model yields noticeably different threshold velocities.¹²⁴ This model is developed for low viscosity fluids like water and assumes that shearing-off of roll wave crests is the dominant mechanism of the liquid entrainment into a gas core flow. Application of it to the highly viscous liquid metal system results in overprediction of the entrainment inception by a factor

Table 38 : Threshold steam velocities for onset of molten fuel entrainment.

Saturated Steam Conditions			Entrainment Onset Steam Velocity (ft/s)
Steam Pressure (psia)	Steam Temperature (F)	Steam Density (lb _m /ft ³)	
300	417	0.648	31.6
400	445	0.861	27.4
500	467	1.078	24.4

of 1.75.

Consideration of other correlations suggest that the results given in Table 38 represent a reasonable prediction of the onset of entrainment for the HFIR molten fuel and steam system. It should also be noted that the suggested model is applicable for small, rigid, and spherical droplets. For larger and deformed droplets, a value of 1.4 (instead of 2.46) is suggested for the correlation coefficient C .¹⁰¹ It further reduces the inception velocities to about 18 ft/sec and 14 ft/sec for 300 psia and 500 psia, respectively. Hence the sweepout may actually begin at velocities less than those predicted due to droplet deformation as well as the presence of various size droplets. This further supports the conclusion that the entrainment of the fuel by flowing steam appears to be a possible disruption mode for HFIR fuel plates during the optimum void accident.

Once the entrainment begins, the timescale of the sweepout process becomes an important factor in determining consequences of the fuel element failure. Few suitable

correlations for the rate of entrainment of molten fuel has been proposed in the literature.^{125,126} An evaluation of these correlations can be found in Reference 101. These studies, in general, indicate a strong dependency of entrainment rate on steam velocity and steam pressure. The results demonstrate that entrainment rates are rapid and result in complete fuel film removal in less than a second for high steam velocities. Due to several uncertainties regarding molten fuel film conditions (molten alloy thermal properties, film thickness etc...) a "best estimate" analysis has not been made for the entrainment rates for the HFIR optimum void accident. Based on the reported results, however, it is concluded that when the entrainment inception occurs, the removal of molten HFIR fuel will occur quite rapidly on the order of 100 msec.

8.2. HOT-ASSEMBLY RESULTS and FUEL DAMAGE PROPAGATION

As indicated earlier, the basic concern about the local hot-plate melting is the potential for a loss of coolable geometry within the fuel assembly and the propagation of fuel melting beyond the hot-plate. Fuel damage propagation concerns during the optimum void accident are analyzed using the hot-assembly model of the HFIR which is presented in Section 4.2.1. Here, the hot-assembly results are presented and potential spread of hot-plate melting are analyzed based on the discussion of the initial disruption modes in previous section.

Hot-assembly results are presented for both the inner and outer elements separately. Only the average fuel temperatures for nodes with a high power density (4th, 5th, 6th, 7th, and 8th nodes) are reported. The inner element average fuel temperatures are presented in Figures 82 to 86. As seen in these pictures, the critical heat flux is exceeded in the hot-channels only (channels 4 and 5) which surround the hot-plate (plate 5) for nodes 5, 6, 7, and 8. This also affects the two neighboring fuel plates leading to a critical heat flux situation and excessive temperatures. The neighboring plates (plates 4 and 6) experience the critical heat flux situation for nodes 6 and 7 only.

In the film boiling region, the hot-plate temperatures exceed 1000 °F for only nodes 5, 6, and 7 while the temperatures for plate 4 and 6 all remain below 800 °F. The highest

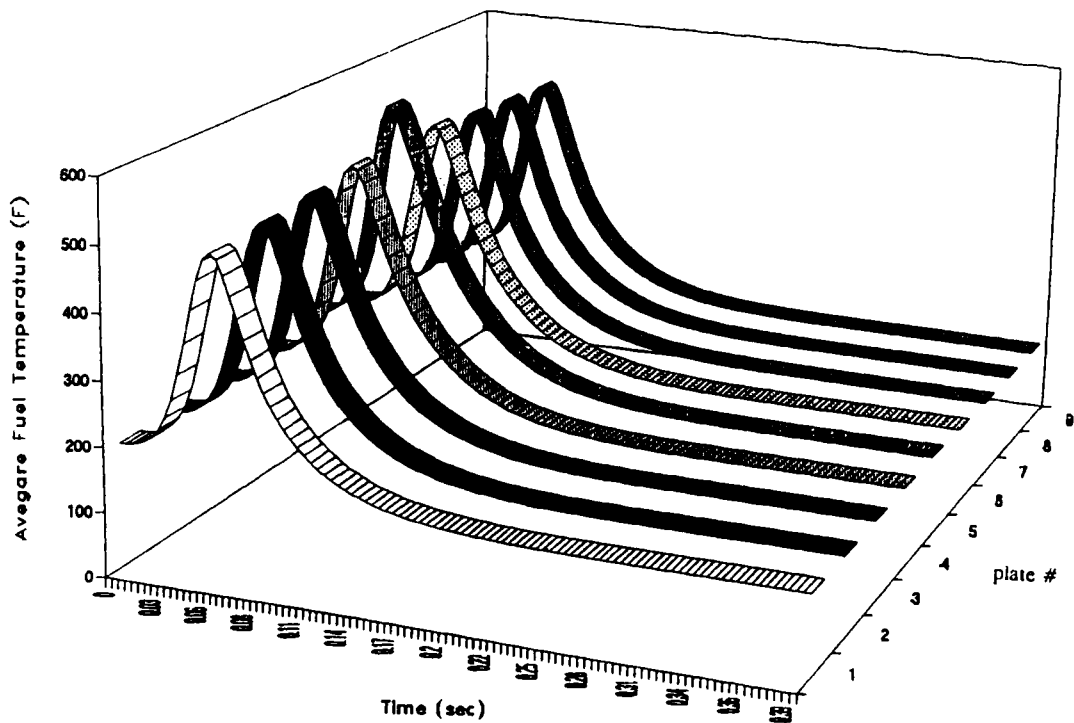


Figure 82 : Inner-elements hot-assembly average fuel temperatures for the 4th node during EOC optimum void accident.

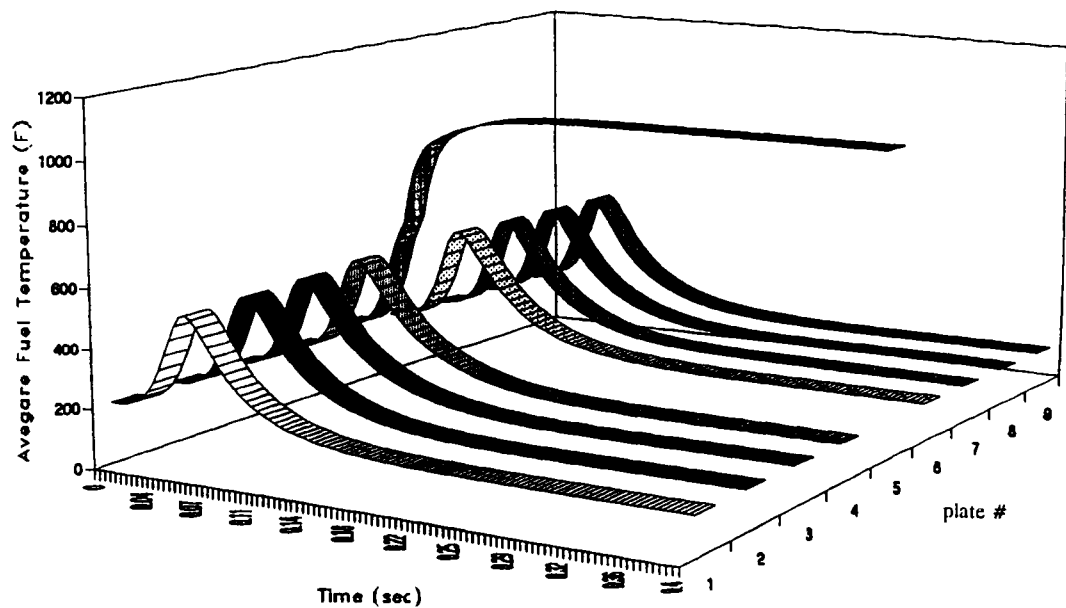


Figure 83 : Inner-elements hot-assembly average fuel temperatures for the 5th node during EOC optimum void accident.

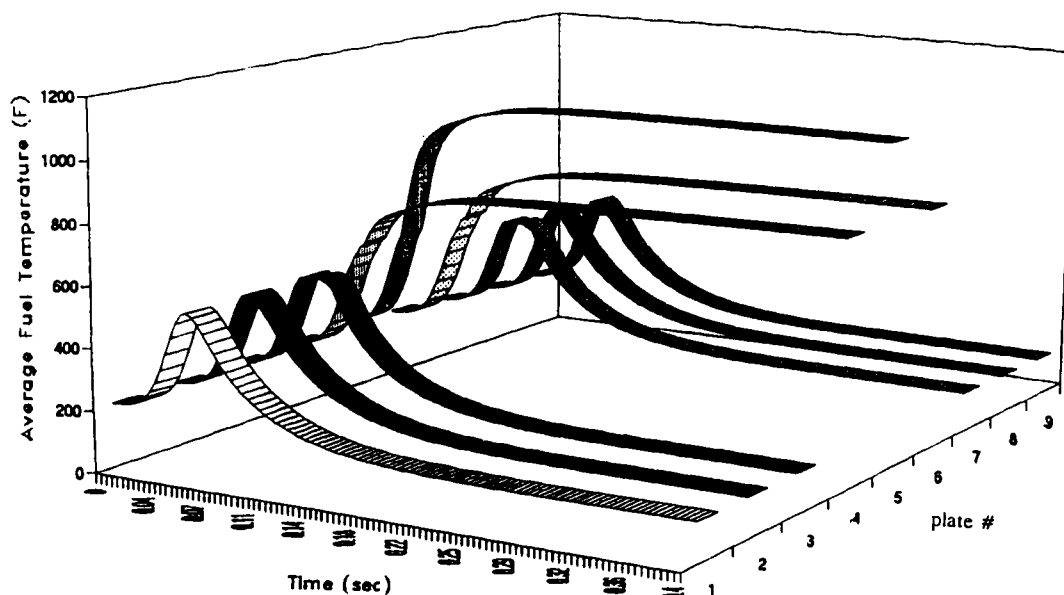


Figure 84 : Inner-elements hot-assembly average fuel temperatures for the 6th node during EOC optimum void accident.

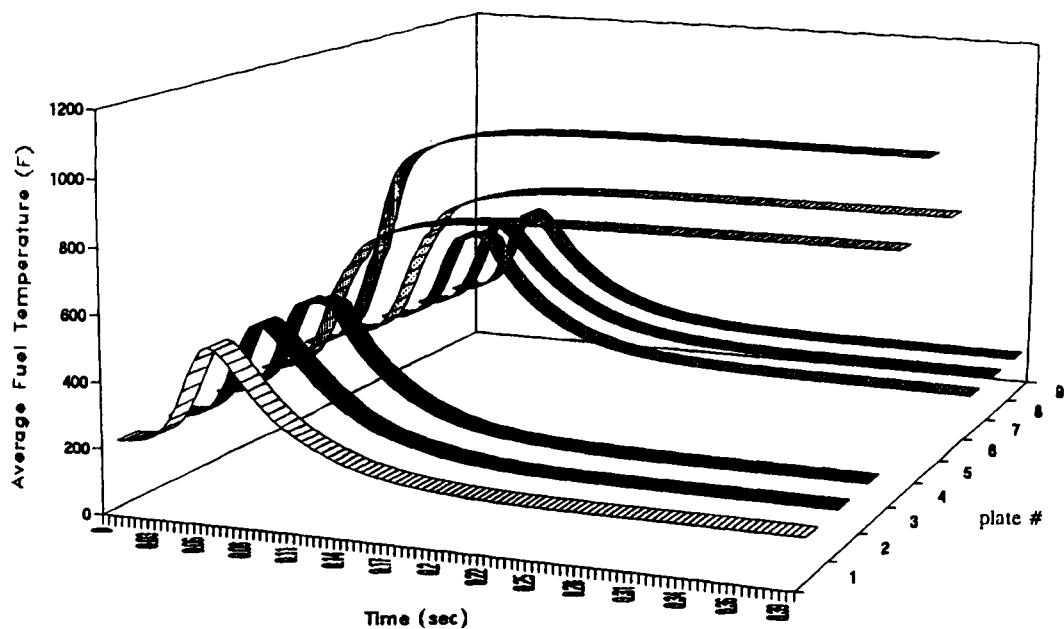


Figure 85 : Inner-elements hot-assembly average fuel temperatures for the 7th node during EOC optimum void accident.

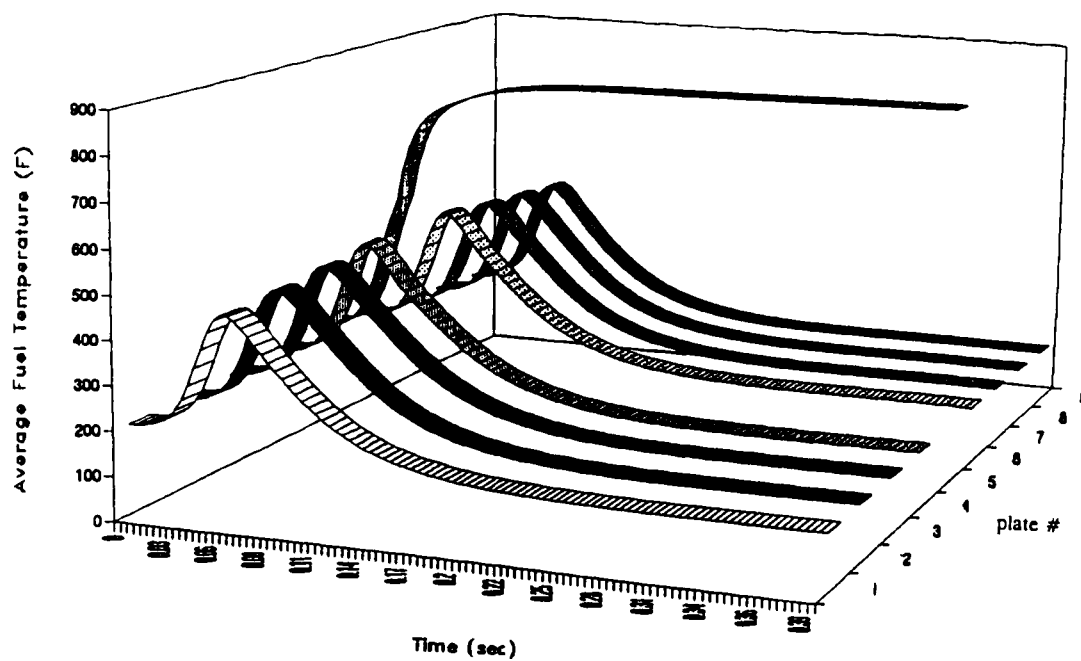


Figure 86 : Inner-elements hot-assembly average fuel temperatures for the 8th node during EOC optimum void accident.

hot-plate temperature attained is 1075 °F. Although aluminum melting is generally accepted to begin at about 1205 °F, melting does not initiate at an exact specified temperature. In fact substantial fuel/cladding liquidification does not occur until the fuel/cladding temperatures approach to 2060 °F where melting of the fuel plate is complete. Nevertheless, since the difference between the hot-plate temperatures and the expected melting inception is marginal, it is conservatively assumed that the hot-plate experiences local melting for the nodes with high power generation. Furthermore, calculations indicate that plates would maintain their high temperature state for a considerable time, on the order of several minutes, during which time-dependent fatigue also becomes an important consideration. This assumption for early fuel melting also covers the uncertainties in the critical heat flux and the film boiling heat transfer correlations which essentially determine the plate temperatures at decay heat power levels long after scram. Temperatures for plate 4 and 6, on the other hand, remain below the melting point by a safe margin (about 400 °F) and a melting inception is not expected for these plates unless disruption in the hot-plate leads to a more adverse condition.

The outer-element hot-assembly results are provided in Figures 87 to 91. As seen in these figures, the outer element assembly experiences a milder transient and only the hot-plate (plate 5) temperatures approach the melting point for nodes 5, 6, 7, and 8. For this case, the neighboring plates (plate 4 and 6) do not seem to be affected at all. Also, the peak hot-plate temperature is lower compared to the inner-element results. Therefore, only the inner-element hot-assembly is studied as the limiting case for the following analysis of damage propagation.

As the hot-plate temperatures approach the melting point, various modes of fuel plate disruptions may be encountered during the rapid optimum void accident as reviewed in the previous section. One of the early disruption mechanisms considered is the buckling of the fuel plate under its own weight and coolant hydrodynamic forces. Analytical and experimental studies indicate that the high temperature strength of the HFIR fuel plates during rapid heating events, along with the constraint provided by the side plates, would prevent early collapse of the plates prior to onset of fuel melting. However, consideration of substantial hydrodynamic forces due to high velocity coolant flow suggests that fuel will probably become mobile before melting is complete to form a continuous liquid phase. A second mechanism

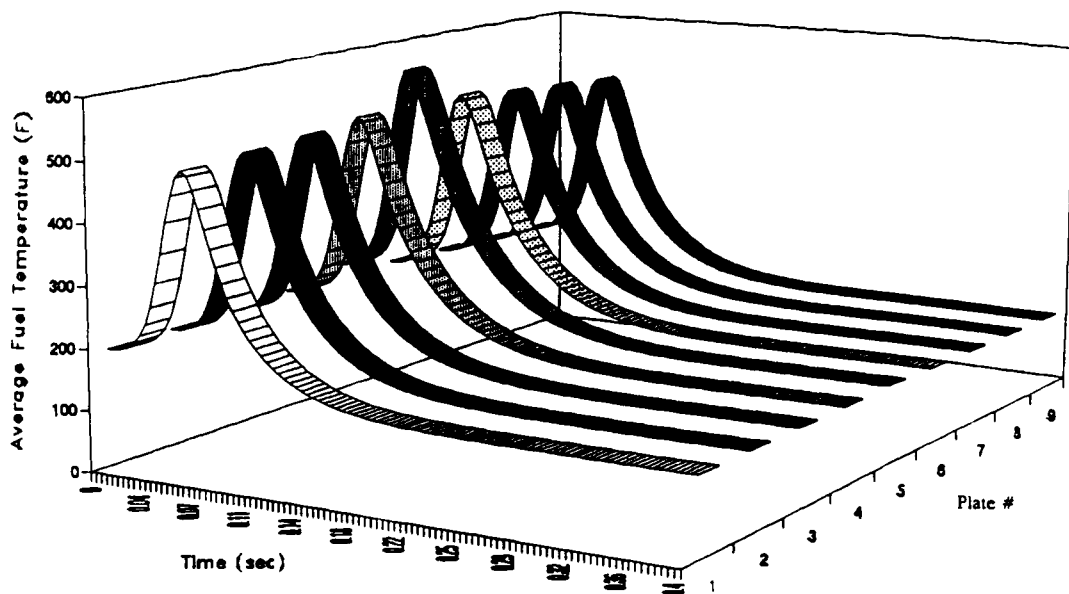


Figure 87 : Outer-elements hot-assembly average fuel temperatures for the 4th node during EOC optimum void accident.

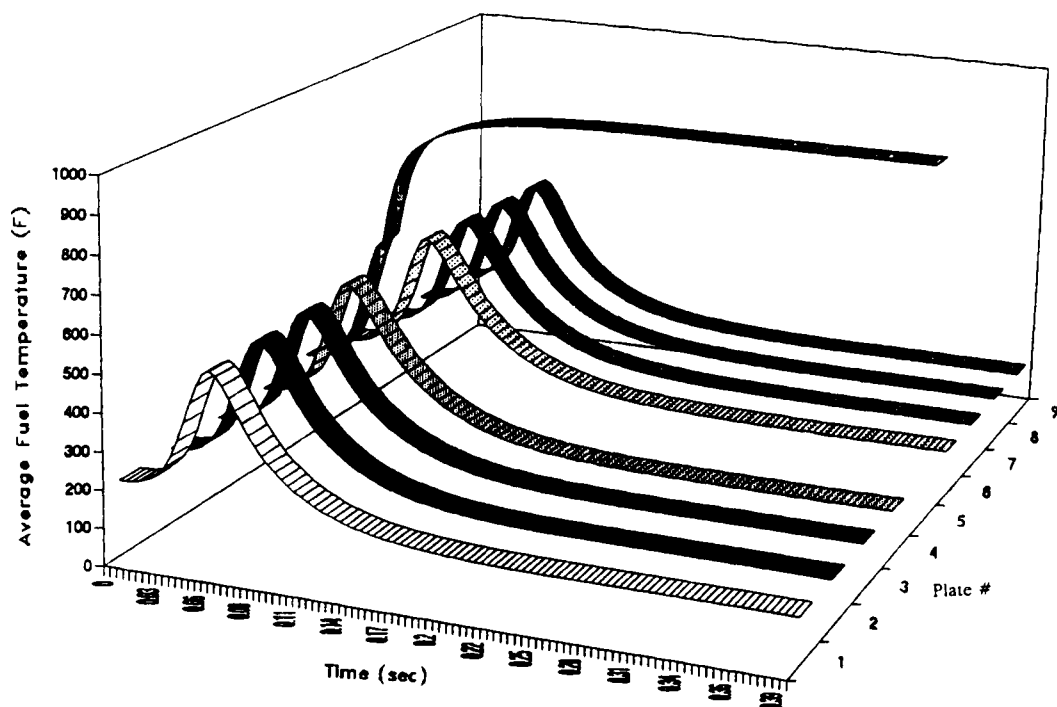


Figure 88 : Outer-elements hot-assembly average fuel temperatures for the 5th node during EOC optimum void accident.

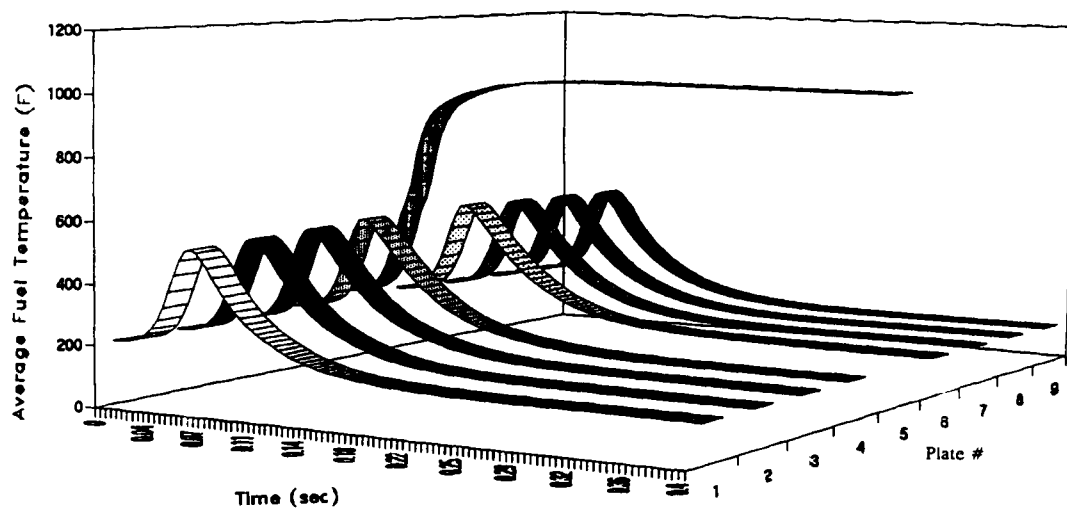


Figure 89 : Outer-elements hot-assembly average fuel temperatures for the 6th node during EOC optimum void accident.

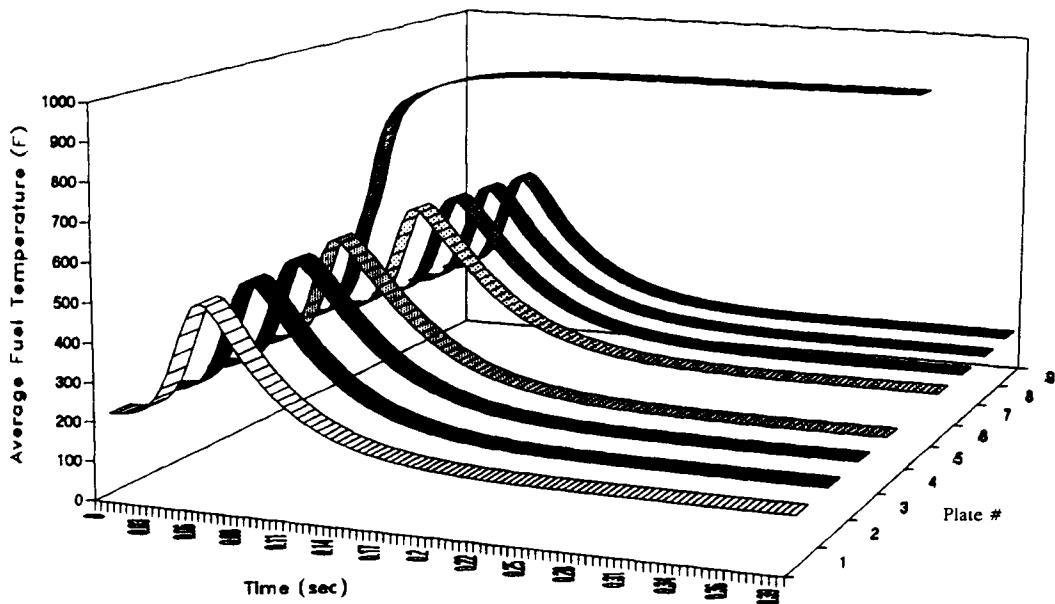


Figure 90 : Outer-elements hot-assembly average fuel temperatures for the 7th node during EOC optimum void accident.

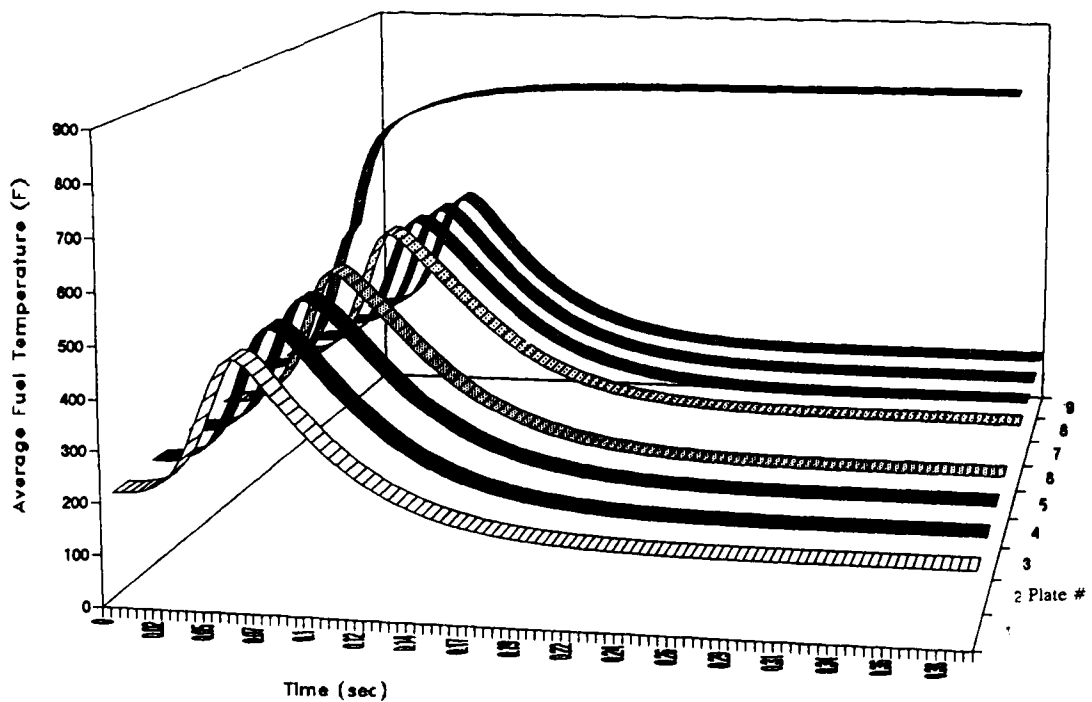


Figure 91 : Outer-elements hot-assembly average fuel temperatures for the 8th node during EOC optimum void accident.

for fuel disruption prior to fuel melting is the oxidation reaction between the fuel and steam. Again, because of the short time scale during the transient and the protective nature of the aluminum oxide layers, only very thin films are expected on the fuel surfaces which would not lead to breakup of the plates. Other chemical reactions such as the $\text{U}_3\text{O}_8\text{-Al}$ thermite reaction simply do not begin until well after fuel melting is initiated (1500 °F); therefore they are not of immediate concern as initial disruption modes.

Two mechanisms for initiating molten fuel motion are fuel foaming and entrainment of fuel by high velocity coolant. A high percentage retention of the noble fission gases in the HFIR plates during steady-state operation generates a considerable potential for swelling of molten fuel once the melting point is reached. Several studies indicate that the time scale for initial foaming of high burnup fuel may be as short as several milliseconds. In addition, once formed, fuel foam is expected to remain stable in absence of external forces for a considerable time (on the order of several minutes) before it collapses due to draining of the liquid fuel between the bubbles. The swelling potential of the molten fuel, however, drops significantly for system pressures above 100 psia. Therefore, during the optimum void accident at the end of the fuel cycle, the fission gas available in the fuel plates is not expected to be sufficient to fill the space between the two neighboring plates with the fuel foam.

Scoping calculations have also been performed to assess the potential for entrainment of molten HFIR fuel by the high velocity coolant flow during the optimum void accident. Results indicate that the velocity of both the steam film and the subcooled liquid in the hot-channels are considerably above the predicted onset of entrainment velocity. When the hot-plate temperatures for nodes with a high power density (nodes 5, 6, 7, and 8) reach the melting temperature, the hot-channel pressure for the corresponding coolant nodes varies from 412 psi to 380 psi. An entrainment inception velocity of 27-28 ft/sec is estimated in this range while the hot-channel vapor velocity ranges from 44 ft/sec to 48 ft/sec and the liquid velocity ranges from 42 ft/sec to 46 ft/sec (Figure 78, also see Figures 35-38 in Appendix F). Therefore, it is concluded that, once melted, ejection of the molten fuel is highly likely resulting in complete removal of the molten fuel on the order of milliseconds.

Ejection of molten fuel out of the core will probably enhance the results as far as flow

blockages are concerned. However, since the melting begins at the heated lengths of the coolant channel, entrainment may create partial flow blockages in the narrow HFIR coolant channels as the molten/foamed mixture accumulates as it moves and cools off with the presence of water at the unheated channel outlet. In this mode, complete blockages within the core are highly unlikely, but not impossible.

To investigate the impact of such flow blockages, a series of cases with varying blockage sizes are analyzed in which a sudden reduction in coolant flow area is formulated in the hot channels (channels 4 and 5). The flow blockages are considered for all coolant nodes in which the critical heat flux is experienced in at least one surrounding surface. It is assumed that all blockages are initiated at 200 msec after transient initiation when the hot-plate temperatures approach their peak values. The abrupt area changes in and out of blocked regions are analyzed with sudden contraction and sudden expansion modeling as described in Section 3.2.6.2. The results of this analysis indicate that the thermal variables are not affected in a notable way and the increase in neighboring plate temperatures are insignificant until complete channel blockages are realized. The time advancement scheme encounters numerical instabilities and does not provide results for sudden flow blockages larger than a 75% reduction in coolant flow area.

The behavior of key fluid dynamics variables are shown in Figures 92 to 94 for a sudden 75% reduction in flow area. With the reactor at a substantial fraction of nominal power, it does not appear that localized fuel melting would trigger a steam explosion. Therefore, the accelerating pressures resulting from water and plate expansion would be limited to those due to channel blockage. Still, the impact of flow blockage is quite rapid causing significant local pressurization of the channel as shown in Figure 92. This results in large flow disturbances throughout the channel as shown in Figure 93. After the channel depressurizes into the upper regions of the core, the coolant flow also readjusts resulting in flow oscillations. Changes in void fraction during blockage is shown in Figure 94. Although the void fraction increases substantially for nodes with a high power density after blockage is realized in the hot-channels, voiding of the entire node is unlikely. This increase in void fraction is primarily due to the decrease in local pressure and flow area which results in reduction in water content.

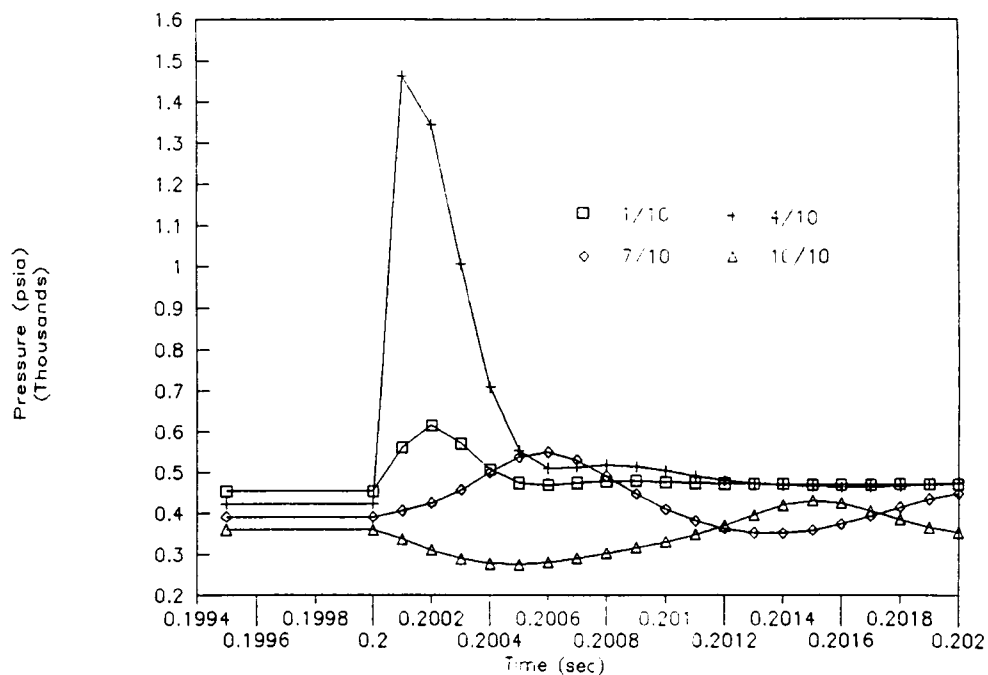


Figure 92 : Inner-element hot-channel pressures for nodes 1, 4, 7, and 10 during optimum void accident after a hypothetical flow blockage in which 75% reduction in coolant flow area is experienced.

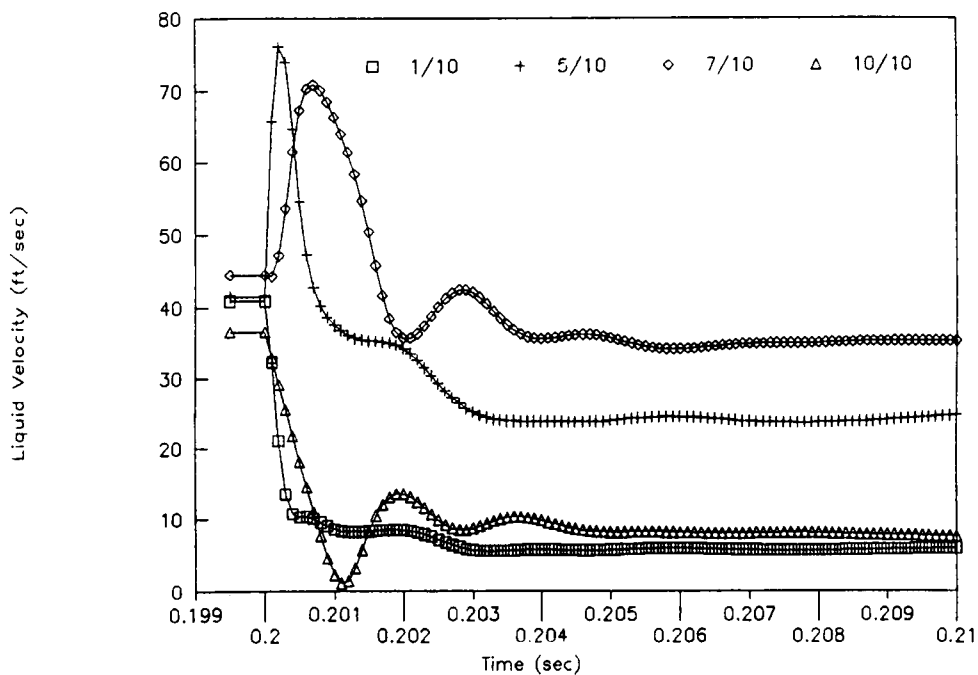


Figure 93 : Inner-element hot-channel liquid velocities for nodes 1, 5, 7, and 10 during optimum void accident after a hypothetical flow blockage in which 75% reduction in coolant flow area is experienced.

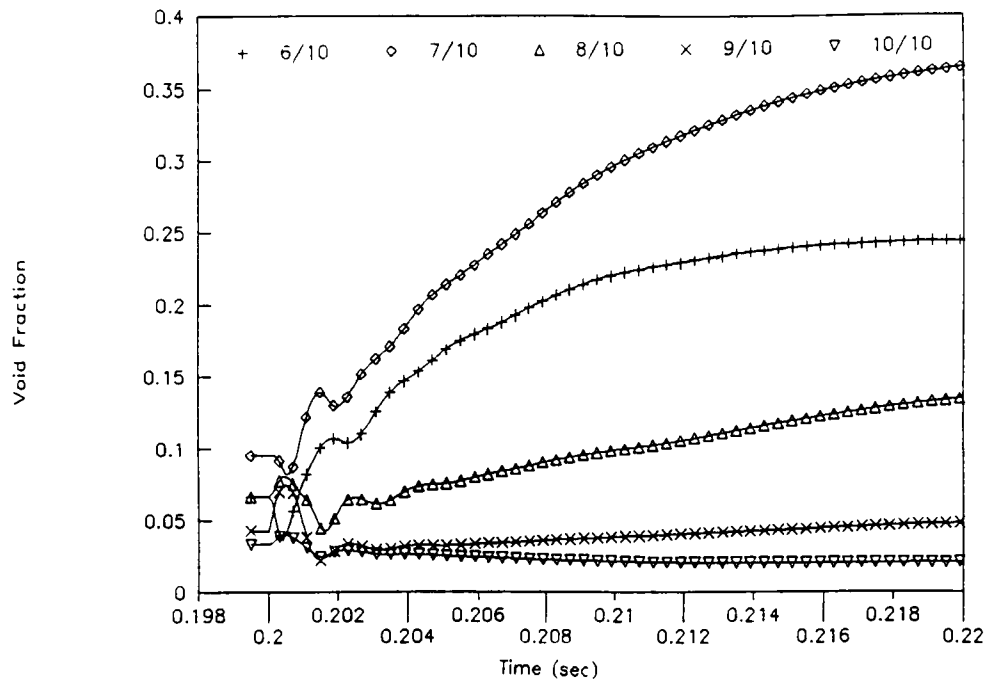


Figure 94 : Inner-element hot-channel void fractions for nodes 1, 4, 7, and 10 during optimum void accident after a hypothetical flow blockage in which 75% reduction in coolant flow area is experienced.

A few other cases are studied for flow blockages with reductions in flow area larger than 75% by considering gradual blockage scenarios rather than sudden obstructions.^s For these cases, a gradual blockage formation is considered up to a 90% reduction in flow area over 30 msec. The analysis of blockages larger than 90% has not been feasible due to numerical instabilities. Similar to the cases with less serious flow blockages, the increase in temperatures for plates surrounding the channels which experience these blockages is rather insignificant, and, when plotted on the same scale, the results are indistinguishable. Strangely, the plate temperatures seem to decrease for some cases with larger blockages. However, these results are not expected to be realistic since numerical instabilities are observed for most of these large blockage cases.

8.3. LIMITATIONS of the EXISTING MODEL

The major limitation observed for this model is the lack of axial heat conduction inside the fuel plates. In the existing model, only one-dimensional heat conduction is considered for fuel plates in which heat is transferred in the azimuthal direction only and is connected to surrounding coolant channels by convection (Figure 16). However, this model results in a highly unrealistic situation for fuel plates which experience CHF in plate surfaces. This behavior is displayed for the hot-plate in Figure 95. As seen in this figure, once the critical heat flux is exceeded in the hot-channels, the poor heat transfer in the film boiling region leads to sudden increases in the hot and surrounding plate temperatures for nodes with surfaces having a film blanket. The temperatures for the other nodes in the same fuel plates, however, drops substantially as the power goes down to decay heat levels due to reactor scram. Since the existing model lacks any axial heat conduction features, no provision has been made for heat transfer between two nodes with a significant temperature difference; namely between nodes 4 and 5, as well as between nodes 8 and 9. Normally, one would expect to see an axial temperature distribution proportional to the power density distribution and similar to the initial temperature profile. This is particularly true for aluminum-based fuel

^s These gradual blockages are still modeled as abrupt area changes. The size of this sudden reduction in coolant flow area is assumed to increase gradually in time.

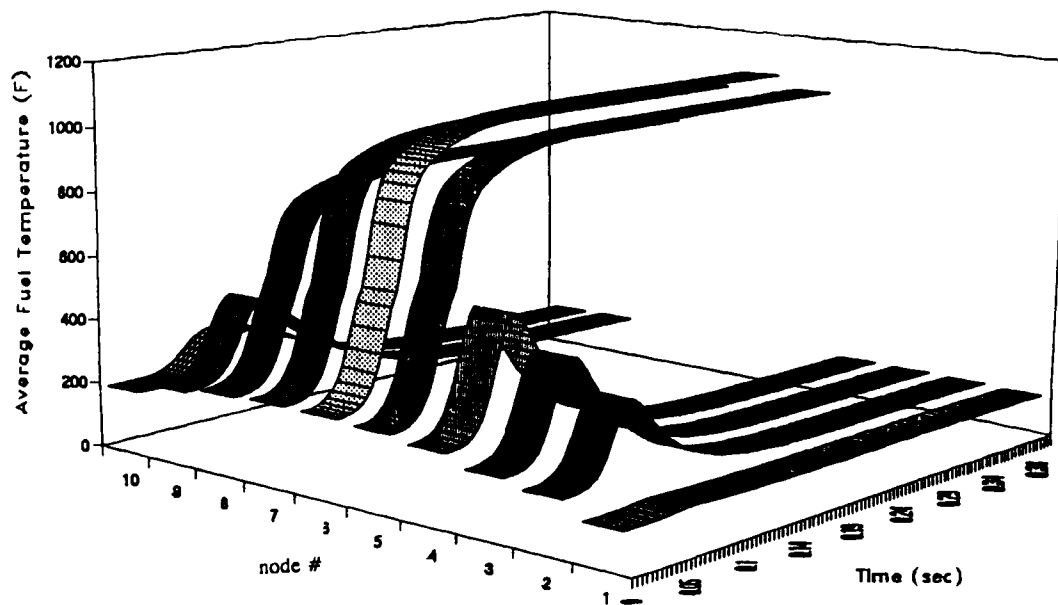


Figure 95 : Inner-element hot-plate average fuel temperatures for all ten nodes during EOC optimum void accident.

which is characterized with a high thermal conductivity compared to ceramic fuels.

It should also be recognized that complex multi-dimensional flow redistribution between the coolant channels is also possible when a local cavity forms in fuel plates and it must be considered for best-estimate evaluations of the hot-spot melt progression. Since the fluid-dynamics model in this work is based on one-dimensional fluid flow with no provision for cross-flow between the neighboring channels, analysis of these multi-dimensional effects has not been feasible.

In addition to thermal and hydraulic impacts, the neutronic effects of such fuel relocation need to be considered as well. Although not expected to be significant, the reactivity excursion potential of the initial melt relocation cannot be ruled out. In general, low homogenized fuel densities, such as is the case for fuel foaming, would lead to a less reactive situation. However, foam expansion of the fuel/clad into the coolant channels may be a positive reactivity change from the voided state. Early calculations for the HFIR demonstrate that uniform removal of fuel plates from the core results in a maximum reactivity increase of .025 dk/k for removal of about 30% of the core material and replacement with moderator.⁴ For relatively smaller amounts the coefficient is about 0.113 (dk/k)/(dv/v).

Studies also indicate that a local cavity might grow in size due to local power-density peaking.⁴ The increase necessary for self-propagation would depend on the magnitude of fuel segregation associated with the melted spot and upon other characteristics of melting. Supposing that hotspot melting is self propagating, the melted area might continue to grow only until an optimum size is formed. The size of the optimum hole is generally a function of the competing reactivity effects of moderation and absorption in the water cavity. The maximum reactivity addition from the formation of such a cavity is reported as 0.0003 dk/k when the size of the optimum cavity is 2.36 inches in diameter.⁴ The reactivity would decrease if the hole grew bigger, which it probably would.

The figures discussed above are not sufficiently clear to implement such a reactivity excursion potential into the existing model. A more detailed analysis is needed based on space-time neutron kinetics. A number of core reconfigurations after initial disruption can

be sketched based on the results of this analysis and arguments presented above. These configurations provide static snapshots of the disrupting core and may be used to provide the basis for the static neutronic reactivity calculations.

In all likelihood, fuel damage relocation will be limited to the hot-plate and surrounding fuel plates. Therefore, significant feedback from molten regions is not expected. Typically, the need to assess the reactivity excursion potential once the core material relocation begins exist for transients with whole core involvement. To support this argument, a separate analysis is performed in which the feedback from hot-assembly is included.^t This analysis indicated that the localized feedback from the hot-channels and the hot-plate is insignificant for the optimum void accident in BOC, full-power case, and the peak power and peak temperatures remain unchanged. Furthermore, since the hot-plate temperatures approach the melting point at scram power levels as shown in Figure 95, all this additional reactivity potential is not expected to make a significant impact; therefore, it is not included in this analysis of the optimum void accident.

^t Normally, the dynamic behavior of the core is conservatively assumed to be dependent on the nominal conditions only.

9. CONCLUSIONS and RECOMMENDATIONS for FUTURE WORK

9.1. CONCLUSIONS

An improved dynamic model for the analysis of rapid reactivity transients of U-Al dispersion fueled research reactors has been developed. The model includes point reactor kinetics for the modeling of the core neutronics. The thermal-hydraulic model includes a non-homogeneous, non-equilibrium (subcooled boiling) two-phase fluid dynamics treatment combined with one-dimensional heat conduction for the average fuel temperatures. The thermal-hydraulics model allows for flow and heat transfer in seven different regimes, separate incipient boiling and critical heat flux formulations, and differing phasic velocities through a slip correlation.

The model is first benchmarked against the RELAP5/Mod2⁸ and REACC² codes for verification of the fluid dynamics and neutronics/feedback behavior, respectively. Partial comparisons with RELAP/Mod2 are performed for both single-phase and two-phase flow by providing power excursion data to the RELAP model as a power table. The agreement between the results of these comparisons for single-phase liquid flow is satisfactory. For the two-phase flow comparisons, however, the RELAP results exhibit a different high frequency oscillatory behavior for the short time frame in which boiling incipience is realized. This behavior is attributed to the two-momentum equation formulation of RELAP. The amplitude of these oscillations is marginal, and other than the transitory region, the results show good agreement in general. Therefore, the two-phase comparisons with RELAP are also deemed satisfactory. The neutronic behavior of the model and the feedback mechanisms are compared to a previous HFIR core transient model, REACC. Close agreement is obtained for the power excursion, and the fuel and surface temperatures. On the other hand, mild discrepancies are observed for the coolant temperatures and surface heat fluxes. Recognizing the inherent differences between the models of the two codes, the agreement between the results is acceptable. And finally, in an attempt to verify the overall coupled neutronic and thermal-hydraulic behavior of the model, a series of comparisons was performed with the SPERT-II experiments. The results of these limited comparisons are also satisfactory.

In the second part of this work, HFIR reactivity accidents are discussed and the transient response of the HFIR for these accidents is investigated. The reactivity transients analyzed can be grouped as control system related accidents, pump starts in a cold loop, and accidents associated with voids in the target region. Each transient under these headings is further categorized according to mode of operation and the initial conditions in each mode. Basically, four distinct reactor operating states are identified and each transient is analyzed for both the beginning of cycle and end of cycle conditions, and for both the inner and the outer elements.

Based on the results of these analyses, it is concluded that the amount of reactivity and its rate of addition during an optimum void insertion to the target region represents the maximum conceivable reactivity accident for the reactor. The sequence in Mode 1, at full-power, full-flow conditions, and at the end of fuel cycle is identified as the bounding case and it is used as the basis for the parametric studies on modeling uncertainties. These cases ensure that the bounding combination of the several uncertainties in the initial conditions such as the power, inlet temperature, and plate reactivity worth curves is considered.

The results indicate that the optimum void accident results in a critical heat flux situation in the hot-assembly hot-channels for both the inner and outer fuel elements which leads to excessive temperatures in the hot-plate for the nodes with a high power density. It should be pointed out that the highest temperatures attained during this accident do not reach the generally accepted aluminum melting point. However, since melting of aluminum does not initiate at a specific temperature and the difference between the hot-plate temperatures and the expected melting inception is marginal, it is assumed that the hot-plate average fuel temperatures for the nodes which experience critical heat flux at the bounding surfaces reach the melting point, and that the plates contain sufficient heat to actually initiate melting. The design requirements and transient acceptance criteria generally require that some local fuel melting is acceptable for limiting faults as long as a coolable geometry is maintained. Therefore, the questions concerning core coolability after initiation of hot-plate melting are also addressed.

Various modes of initial fuel plate disruptions have been reviewed and scoping calculations have been performed to assess the potential for molten fuel entrainment at the expected hot-channel conditions during the optimum void accident. Once the poor heat transfer in the film boiling region initiates fuel melting at the nodes with high power densities, fuel foam is expected to expand and partially fill the coolant channels. Correlations for the entrainment inception velocities for saturated steam conditions and fuel at its liquidus temperature suggest that the high pressure drop across the HFIR core will force this molten/foamed fuel out of the core region under the calculated channel conditions.

Nevertheless, the potential consequences of partial flow blockages are also investigated in the narrow HFIR coolant channels as the molten fuel accumulates as it moves and cools off with the subcooled water at the channel outlet. To examine the impact of such flow blockages, a series of cases with varying blockage sizes is analyzed in which a sudden reduction in the coolant flow area is experienced in hot channels. The results indicate that the thermal variables are not affected in a significant way and the increase in neighboring plate temperatures is also insignificant until complete channel blockages are realized.

Under these conditions, it is believed that the damage will be limited to melting in a portion of the hot-plate accompanied by a minor release of fission products to the primary coolant. Therefore, it is concluded that some local melting can be tolerated without destroying the mechanical integrity of the rest of the element and the HFIR can withstand the maximum credible reactivity accident with only a small amount of melting.

It should be pointed out that the existence of simulated hot-plate conditions requires the maximum credible fuel loading and segregation coinciding with the maximum credible coolant channel narrowing and clad separation, all at the highest flux region of the core. To this has been added the production of an exactly (no more, no less) optimum void from an unknown source, the sweeping of this void at full coolant speed into the flux trap with none of it entering the other regions, and maintaining it in the optimum location until the transient terminates. With all of these adverse conditions occurring simultaneously, only localized hot-plate melting is predicted.

9.2. RECOMMENDATIONS for FUTURE WORK

Although the model developed provides a mechanistic framework for analyzing the energetics of accident sequences considered in this work, there remain modeling limitations and uncertainties. The neutronics behavior of the HFIR core is modeled with the space independent point kinetics approximation in this work. This approximation is based on the assumption that the shape function of the total neutron flux does not change significantly throughout the transients. For accidents involving coolant voiding and possible fuel melting relocation, however, this assumption may not be valid, and a space-time kinetics approach may be needed. A relatively simple and efficient way of adding space dependency to the model is to use an adiabatic method. In this approach, the shape equation (3) as described in Section 3.1.1 is reduced to an eigenvalue problem which can be solved with a steady-state core physics algorithm. In a typical calculation, the steady-state shape equation is solved repeatedly for a set of core configurations as the transient progresses. This shape function and its adjoint function can be used then to compute the integral point kinetics parameters such as core reactivity and prompt neutron lifetime. The obvious advantage of this scheme is that the calculation of the flux shape can be carried out with available steady-state neutronics codes. With the capability of recalculating the flux shape only when necessary, this approach retains the advantages of the point kinetics without being constrained by its limitations.

The existing model contains considerable uncertainties regarding the point kinetics parameters particularly at the end of fuel cycle conditions. The data used in this work are primarily based on early HFIR criticality experiments which considered only fresh core conditions. Several EOC parameters such as neutron generation time and reactivity feedback coefficients are estimated based on extrapolations to the end of cycle conditions. Therefore, a depletion analysis for the EOC conditions, including the calculation of EOC reactivity coefficients and power shapes is strongly recommended. The new nuclear analyses in progress or planned for the SAR are expected to extend the original nuclear design results and provide the necessary data for the analysis.

Another major limitation of this model is that only a one-dimensional heat conduction

is considered for the fuel plates in which the heat is transferred in the azimuthal direction only and connected to surrounding coolant channels by convection. The lack of axial heat conduction inside the fuel plates results in highly unrealistic situations for fuel plates which experience CHF in heat transfer surfaces. Simply adding another space dimension to the heat conduction formulation would result in significant increase in the total number of differential equations to be solved which, in turn, could require prohibitively long computation times. Instead, axial heat conduction can be introduced to the model in such a way that the heat transfer between two neighboring fuel nodes can be calculated through an explicit scheme. In other words, instead of using classical two-dimensional heat conduction, two one-dimensional heat conduction problems can be synthesized. Since this formulation would require only a few additional differential equations, it would retain the advantages of the current heat conduction model with the ability of calculating the effects of axial heat transfer.

And finally, reducing the range of uncertainty on parameters such as fission gas foaming, porous blockage formation and the time scales for molten fuel behavior requires more study and a more extensive literature survey. There is also a strong need for experimental work in order to develop a better understanding of the complex phenomena associated with gradual core disruption.

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APPENDICES

APPENDIX A

A REVIEW OF EXISTING COMPUTER CODES

To solve the core dynamics problem properly, both neutron kinetics and spatial thermal-hydraulics models are needed. A number of methods have been developed for the solution of the time dependent multigroup diffusion equations describing spatial neutron kinetics. These methods range from direct calculation of full energy space-time dependent neutron diffusion equations to point kinetics equations with an appropriate evaluation of nuclear parameters and reactivity. The various methods may be classified into two groups: direct integration methods and factorization methods. The factorization methods category includes three subgroups. These are the quasi-static, adiabatic and point kinetics methods.¹⁸

Because of the sensitivity of feedback to the moderator density, choosing an appropriate thermal-hydraulic model for the HFIR conditions and for the type of transients to be simulated is essential. For the transients in which very rapid pressure and flow changes are encountered, a two-fluid model for the analysis of the thermal-hydraulic behavior is needed. The two-fluid model is based on six equations with an equation for the conservation of mass, momentum and energy written for each phase. In addition, constitutive equations describing the interfacial exchange between phases are required. Although the two-fluid model has a wider range of validity than homogeneous models, the simultaneous solution for these six conservation equations is time consuming and may encounter numerical instability. In addition, determining the parameters defining interfacial exchange is very difficult since little is known about the flow in narrow HFIR coolant channels under high heat fluxes. Instead, by introducing some simplifications, the number of equations can be reduced for the particular transients of concern in this work. Basically, the conservation equations for mass, energy, and axial momentum are derived by considering the transport in and out of a control volume.

A number of computer programs for the transient analysis of the PWR's have been written. A summary of these codes, their major users and principal characteristics are

presented here. Some of these codes are proprietary vendor codes and only limited information about their numerical procedures is available. Those are TWINKLE (Westinghouse), XTRAN (Exxon Nuclear), HERMITE and FIESTA (Combustion Engineering).¹³⁸

The 3D core behavior of both PWR and BWR cores can be modeled by MEKIN.¹³⁹ The thermal-hydraulics model used in COBRA-IIIC¹⁴⁰ has been modified and is embodied in MEKIN. Homogeneous conservation equations are solved iteratively by assuming a uniform inlet and exit pressure distribution. Two heat transfer regimes, forced convective single-phase and nucleate boiling, are available. No critical heat flux and hot channel models are available. It includes an extra lateral momentum equation to calculate cross flow between coolant channels. The neutron kinetics model consists of the 2 group 3-D diffusion equation and six delayed neutron families in cartesian geometry. The equations are written in finite difference form for space and time and solved by an inner and outer iteration procedure. An extra iteration between the kinetics and thermal-hydraulics segments is required. Since a very small time step is used in this finite difference method, very long computational times are needed. Because of the sophistication of its thermal-hydraulics and neutronics, MEKIN is often considered as a benchmark method for core dynamics problems.

The TITAN code¹⁴¹ couples 3D neutronics and thermal hydraulics for use in BWR and PWR safety analysis. The program uses a two-fluid model and can handle some complex fluid dynamics conditions such as natural circulation, blowdown, flow reversal and phase separation. It is reported that the heat transfer package can determine appropriate regimes based on a complete boiling curve. A semi-implicit numerical method is used to solve the conduction and convection portions. Two group diffusion equations are solved by an analytic nodal method. Neutronics and thermal-hydraulics are solved successively. This code is usually considered for special applications such as a LOCA.

The TWIGL and BNL-TWIGL programs use essentially the same modeling and methodology as BEAGL (Brookhaven and EPRI adaptation of TWIGL). The BEAGL⁴³ code is primarily designed to predict LWR core behavior during reactivity transients such as rod drop or rod ejection accidents. Two group neutron diffusion equations with six delayed

neutron precursor groups are solved by a finite difference method in a radial-axial mesh. The thermal hydraulics model has been upgraded from the previous version of TWIGL to allow for subcooled boiling and slip during two phase conditions. A momentum conservation equation is added to the original homogeneous flow equations to calculate the inlet flow distribution for BWR's and cross flow for PWR's. A one dimensional radial conduction model is used to determine average temperatures for the pellet-gap-cladding structure.

RETRAN is a nuclear steam supply system code for BWR's and PWR's.¹⁴² Although an early version of RETRAN used point kinetics, the new version utilizes a factorization technique to solve the one dimensional two-group neutron diffusion equations. With this technique, the total neutron flux is factorized into a time dependent amplitude function and a space-time-energy dependent shape function. A one dimensional, homogeneous equilibrium mixture thermal hydraulic model supplemented with a dynamic slip equation is used for the reactor cooling system. The dynamic slip equation is obtained by algebraic manipulation of the phasic momentum equations. RETRAN-02 has a comprehensive thermal-hydraulic package which includes 16 heat transfer regimes, three options of the two-phase friction multiplier, four options for the critical heat flux calculation, a hot channel analysis, and a flow distribution calculation.

The new version of Transient Reactor Analysis Code, TRAC-PF1, was created to improve on the capabilities of its predecessor, TRAC-PD2, for analyzing slow reactor transients such as small-break loss-of-coolant accidents.⁸⁶ It uses a semi-implicit finite difference method for modeling three-dimensional flows in the reactor vessel. By introducing a stability enhancing two-step method,¹⁴³ the entire reactor system is modeled with one dimensional components. In the method, a separate stabilizing step is introduced to provide information about the transport of fluid density, energy, and momentum across cell boundaries.

And finally, RELAP5 is a well known nuclear steam supply system code for light water reactors.^{8,85} It uses point kinetics for neutronics, and a six-equation model for the two phase thermal-hydraulics. The code modeling capabilities are simulation of loss-of-coolant accidents as well as operational transients such as ATWS, loss of offsite power, loss of feedwater, and

loss of flow. However, this code is not suitable for rapid reactivity insertion accidents as discussed in Section 6.1.1.

These computer codes, in general, are developed for commercial light water power reactors with geometries and operating conditions significantly different from those of HFIR and thus have limitations for HFIR applicability. Furthermore, they are designed for the analysis of system transients with a relatively long period. The behavior of the HFIR during a rapid reactivity transient is quite different from that which occurs in a power reactor. For the type of transients of interest in this work, it is necessary to modify these codes for narrow rectangular channels with high coolant velocities in the fuel element region, and for the highly subcooled core conditions of HFIR. Instead, new computer code is developed in this work specifically for the HFIR core and, more generally for aluminum dispersion plate type fueled research reactors.

APPENDIX B

A REVIEW OF TWO-PHASE FLOW MODELS

The analysis of transient two-phase vapor-liquid flow and heat transfer phenomena is the subject of current research. Considerable interest is directed toward the development of improved forms of the governing equations and improved numerical techniques for their solution. A brief review of these models for vertical flow systems is presented here.

Many transient two-phase flow analyses in current use are based on a single-fluid model (three conservation equations). Among these, probably the simplest model is the *homogeneous-equilibrium model* (HEM) in which the two-phase fluid is viewed as a homogeneous mixture of vapor and liquid, generally with the phases in thermal equilibrium. In Tong and Weisman's description of this model,²⁷ the equations for conservation of mass, momentum, and energy are written as:

$$\rho \frac{\partial v}{\partial z} + \frac{\partial \rho}{\partial t} + v \frac{\partial \rho}{\partial z} = 0$$

$$\frac{\partial v}{\partial t} + v \frac{\partial v}{\partial z} + \frac{1}{\rho} \frac{\partial p}{\partial z} + F = 0$$

$$\frac{\partial}{\partial z} \left[\rho v \left(H + \frac{v^2}{2} \right) \right] + \frac{\partial}{\partial t} \left[\rho \left(U + \frac{v^2}{2} \right) \right] = \dot{q}$$

where v is velocity, ρ is density, p is pressure, F is the frictional head loss per length, H is enthalpy, U is fluid internal energy, and $\dot{q}$ is heat transfer and generation rate. Here the gravitational effects have been ignored in the momentum equation. By introducing the relationship

$$c^2 = \frac{\partial p}{\partial \rho}$$

where c is the velocity of sound, the momentum balance becomes

$$\frac{\partial v}{\partial t} + v \frac{\partial v}{\partial z} + \frac{c^2}{\rho} \frac{\partial \rho}{\partial z} = 0$$

Over a narrow pressure and enthalpy range, we can approximate c^2 by

$$c^2 = \rho^K$$

where K is a constant. We can then transform mass and momentum conservation equations to the following

$$c \frac{\partial v}{\partial z} + \frac{2}{K} \left(\frac{\partial c}{\partial t} + v \frac{\partial c}{\partial z} \right) = 0$$

$$\frac{\partial v}{\partial t} + v \frac{\partial v}{\partial z} + \frac{2c}{K} \frac{\partial c}{\partial z} + F = 0$$

Lahey has written the homogeneous flow equations in a different form.¹²⁷ Lahey's model accounts for the effect of gravitation and can be used in channels with varying cross-sectional area.

$$\frac{\partial}{\partial z}(\rho v A) + A \frac{\partial \rho}{\partial t} = 0$$

$$\frac{\partial}{\partial t}(\rho v) + \frac{1}{A} \frac{\partial}{\partial z}(\rho v^2 A) = - \frac{\partial p}{\partial z} + g \rho \sin \theta - \frac{\tau_w P}{A}$$

$$\rho \left(\frac{\partial e}{\partial t} + v \frac{\partial e}{\partial z} \right) = \dot{q}_s \frac{P}{A} + \dot{q}_v + \frac{1}{J} \frac{\partial p}{\partial t}$$

where

$$e = H + \frac{v^2}{2} + g \sin \theta$$

In Lahey's formulation, the momentum equation is in the standard Eulerian form for the one-dimensional homogeneous two-phase flow. In these equations, A is the flow cross sectional area, τ_w is the wall shear stress, $\dot{q}_s$ is surface heat flux, $\dot{q}_v$ is volumetric heat generation rate, P is the wetted or heated perimeter, and other symbols are as previously defined.

In some flow regions this homogeneous model gives surprisingly good results. But it has become clear that in many important instances, the two phases are not travelling with the same velocities (or may even be flowing in the opposite direction), they are not uniformly distributed, and are not in thermal equilibrium. As a consequence, further modeling sophistications are needed to account for thermal non-equilibrium and non-homogeneity.

Various approaches are being taken to obtain single-fluid models in which the differing phasic velocities are appropriately modelled. Some current single-fluid models are based on the use of the *slip ratio* to model the unequal phasic velocities (*Slip Model*).¹ In Tentner and Weisman's description,¹²⁸ the conservation equations for flow in a constant cross sectional area channel become

$$\frac{\partial}{\partial t}[\alpha \rho_g + (1-\alpha) \rho_l] + \frac{\partial}{\partial z}[\alpha \rho_g S v + (1-\alpha) \rho_l v] = 0$$

$$\frac{\partial}{\partial t}[\alpha \rho_g S v + (1-\alpha) \rho_l v] + \frac{\partial}{\partial z}[\alpha \rho_g S^2 v^2 + (1-\alpha) \rho_l v^2] + \frac{\partial p}{\partial z} = -\frac{\tau_w}{A}$$

$$\frac{\partial}{\partial t}[\alpha \rho_g e_g + (1-\alpha) \rho_l e_l] + \frac{\partial}{\partial z}[\alpha \rho_g S v e_g + (1-\alpha) \rho_l v e_l] - \frac{\partial p}{\partial t} = \frac{\dot{q}_s}{A}$$

where α is the vapor fraction, and S is the slip ratio which is defined as the ratio of the vapor and liquid phase velocities

$$S = \frac{v_g}{v_l}$$

In the above equations, subscripts l and g refer to liquid and vapor phases, respectively.

Other symbols are as previously defined.

In the *Drift Flux Model*, an extra conservation equation for vapor mass is added, and the differing phase velocities are represented in terms of a "drift flux" or "vapor drift velocity."²⁸⁻³¹ In addition, departure from thermodynamic equilibrium is allowed for by having separate mass balance equations and providing constitutive equations for defining mass transfer between phases. Kroger has developed a model with some simplifications for blowdown situations.³¹ In this model, the axial heat conduction and dissipation terms in the energy equation as well as surface tension effects have been neglected. Also, the covariance terms accounting for the non-even disruptions of the phases in a plane normal to the flow direction have been disregarded. With these simplifications, the four conservation equations are as follows. The conservation of mixture mass is

$$\frac{1}{\rho} \frac{\partial \rho}{\partial H_l} \frac{DH_l}{Dt} + \frac{1}{\rho} \frac{\partial \rho}{\partial p} \frac{Dp}{Dt} - \frac{\rho}{\rho_{lv}} \frac{DC}{Dt} + \frac{\partial v}{\partial z} = -v \frac{dA}{dz}$$

the conservation of vapor mass is

$$C(v_{gm} \frac{1}{\rho} \frac{\partial \rho}{\partial H_l} + \frac{\partial v_{gm}}{\partial H_l}) \frac{\partial H_l}{\partial z} - \psi \frac{\partial p}{\partial t} + C(v_{gm} \frac{1}{\rho} \frac{\partial \rho}{\partial p} + \frac{\partial v_{gm}}{\partial p}) \frac{\partial p}{\partial z} + \frac{DC}{Dt} +$$

$$(\frac{\rho}{\rho_l} v_{gm} + C \frac{\partial v_{gm}}{\partial C}) \frac{\partial C}{\partial z} + C \frac{\partial v_{gm}}{\partial v} \frac{\partial v}{\partial z} = \frac{1}{\rho} \Gamma_o - C v_{gm} \frac{dA}{dz}$$

the conservation of mixture momentum is

$$\begin{aligned}
& \gamma(v_{gm}^2 \frac{1}{\rho} \frac{\partial \rho}{\partial H_l} + 2v_{gm} \frac{\partial v_{gm}}{\partial H_l}) \frac{\partial H_l}{\partial z} + [\gamma(v_{gm}^2 \frac{1}{\rho} \frac{\partial \rho}{\partial p} + 2v_{gm} \frac{\partial v_{gm}}{\partial p}) + \frac{1}{\rho}] \frac{\partial p}{\partial z} + \\
& [(\frac{1+C}{1-C} + \gamma^2 \frac{\rho_l}{\rho_g}) \frac{\rho}{\rho_l} v_{gm}^2 + 2\gamma v_{gm} \frac{\partial v_{gm}}{\partial C}] \frac{\partial C}{\partial z} + \frac{Dv}{Dt} + 2\gamma v_{gm} \frac{\partial v_{gm}}{\partial v} = \\
& +g - \frac{1}{\rho} \gamma \frac{\tau_w}{A} - \gamma v_{gm}^2 \frac{dA}{dz}
\end{aligned}$$

and the conservation of mixture energy is

$$\begin{aligned}
(1-C) \frac{Dh_l}{Dt} - C v_{gm} \frac{\partial H_l}{\partial z} - \frac{1}{\rho} (1-C) \rho \frac{\partial H_g}{\partial p} - \rho H_{lg}^* \beta \frac{Dp}{Dt} - \\
\frac{v_{gm}}{\rho} C (\frac{\rho}{\rho_{lg}} - \rho \frac{\partial H_g}{\partial p}) \frac{\partial p}{\partial z} = \frac{1}{\rho} \frac{\dot{q}_s P}{A} - \frac{H_{lg}^*}{\rho} \Gamma_o
\end{aligned}$$

which is obtained by subtracting the vapor continuity equation multiplied by H_{lg} . In these equations, C is the static quality, v_{gm} is vapor drift velocity, and other quantities are given by the following relations:

$$H_{lg}^* = H_{lg} + [U + \frac{1}{2} (\frac{1-2C}{1-C}) v_{gm}] \frac{V_{gm}}{1-C}$$

$$\gamma = \frac{C}{1-C}$$

$$\Gamma_o = \rho \frac{C_E - C}{t}$$

$$v_{gm} = v_g - v$$

$$H_{lg} = H_l - H_g$$

$$\frac{1}{\rho_{lg}} = \frac{1}{\rho_g} - \frac{1}{\rho_l}$$

where C_E is equilibrium static quality, and t is time constant. The constitutive equations needed for this model are those specifying the vapor generation rate and phase velocities. The vapor generation rate is given as

$$\dot{\Gamma} = \rho \psi \frac{Dp}{Dt} + \Gamma_o$$

where

$$\psi = \left(\frac{\partial C}{\partial p} \right)_s \left(1 + \frac{C_E - C}{t} \right)$$

It should be noted that this model assumes that the vapor is at saturation and that the liquid density is constant.

The true two-fluid models which are based on six equations provide a reasonably complete description of the two-phase flow. In these models, equations for the conservation of mass, momentum, and energy are written for each phase and supplemented with the macroscopic constitutive equations and the equations which describe the interfacial transfer conditions for mass, momentum and energy. The basic application of two-fluid models are for transient boiling situations where significant differences between the phasic velocities and temperatures exist. Since the focus of the model is on the interfacial transfer between the phases, emphasis is given to flow regimes simulated. In general, flow structures may be categorized as:

- i. Mixed flow in which one phase is dispersed in another. These flows are

characterized by strong coupling between the phases. Due to rapid interface transfers of mass, momentum, and energy, large velocity and temperature differences cannot be sustained. Bubbly and dispersed flows are two typical examples for mixed flow.

ii. Separated flows in which the liquid and vapor phases are separated by a continuous interface. These flows are characterized by a weaker coupling compared to previous case; therefore, relatively larger temperature and velocity differences can be realized between the phases. Annular and stratified flows fall into this category.

iii. Transition flow which represent states between the cases described above. These flows are characterized by a geometrically complex interface and interface coupling that is intermediate between the mixed and separated flows. Slug or churn flows in which neither phase is continuous are typical examples for this type of flow.

Although the six-equation two-fluid models have a wider range of validity than single-fluid models, the simultaneous solution of these six conservation equations is time consuming and may encounter numerical instability. Furthermore, determining the parameters defining interfacial exchange is often too difficult and requires extensive experiments. A number of approaches that simplify the two-fluid model characteristics in various flow regimes have been proposed in the literature.¹²⁹⁻¹³² These models, however, have not received general acceptance. Lahey has described a set of conservation equations for separated flow as follows:¹²⁷

$$\frac{\partial}{\partial t}[\rho_f(1-\alpha)A] + \frac{\partial}{\partial z}[\rho_f(1-\alpha)v_fA] = -\delta w'$$

$$\frac{\partial}{\partial t}(\rho_g\alpha A) + \frac{\partial}{\partial z}(\rho_g\alpha v_g A) = \delta w'$$

$$-(1-\alpha)\frac{\partial p}{\partial z} - g\rho_f(1-\alpha)\sin\theta - \frac{\tau_w P_f}{A} + \frac{\tau_i P_i}{A} - (1-\eta)\frac{\delta w'}{A}(v_g - v_f) = (1-\alpha)\rho_f \frac{Dv_f}{Dt}$$

$$-\alpha \frac{\partial p}{\partial z} - g \rho_s \alpha \sin \theta - \frac{\tau_i P_i}{A} - \eta \frac{\delta w'}{A} (v_s - v_l) = \alpha \rho_s \frac{Dv_s}{Dt}$$

$$\frac{\partial}{\partial z} (w_l e_l) \Delta z + \frac{\partial}{\partial z} (w_s e_s) \Delta z - q'' P_h \Delta z - q''' A \Delta z$$

$$+ \frac{\partial}{\partial t} \left[\rho_l (1-\alpha) \left(e_l - \frac{p}{J \rho_l} \right) + \rho_s \alpha \left(e_s - \frac{p}{J \rho_s} \right) \right] = 0$$

where $\delta w'$ is the amount of liquid phase evaporated per unit axial length in the differential control volume, τ_w and τ_i are shear stresses at the wall and liquid-vapor interface, η is the evaporative momentum transfer term, w_l and w_s are mass flow rates for liquid and vapor phases, respectively. The magnitude of the evaporation term depends on the thermodynamic conditions which exist. For the special case of bulk boiling, thermodynamic equilibrium is assumed to exist and a simple heat balance yields the following approximation:

$$\delta w' = \frac{q'' P_h}{h_{fg}}$$

Phasic internal energy terms are given by

$$e_j = h_j + \frac{u_j^2}{2J} + g \frac{z \sin \theta}{J}$$

Wendroff has surveyed various models for non-equilibrium two-phase flow and derived a simple, basic two-fluid model.¹³³ His model is the result of assuming that the functions appearing under integral signs in the averaged equations are constant. In this formalism, the equations for each phase are written in identical form:

$$\frac{\partial}{\partial t} (\alpha_i \rho_i) + \frac{\partial}{\partial z} (\alpha_i \rho_i v_i) = \Gamma_i$$

$$\frac{\partial}{\partial t}(\alpha_i \rho_i v_i) + \frac{\partial}{\partial z}(\alpha_i \rho_i v_i^2) + \alpha_i \frac{\partial p}{\partial z} = M_{xi}$$

$$\frac{\partial}{\partial t}(\alpha_i \rho_i e_i) + \frac{\partial}{\partial z}(\alpha_i \rho_i e_i v_i) + p \left(\frac{\partial \alpha_i}{\partial t} + \frac{\partial}{\partial z}(\alpha_i v_i) \right) = G_i$$

where e is the internal energy, Γ_i is the mass transfer to the i^{th} phase, M_{xi} is the rate of axial momentum transfer to the i^{th} phase. When these equations are closed by specifying equations of state, they constitute six partial differential equations in terms of six state variables which provide one of the simplest two-fluid models. They apply to a variety of flow situations provided viscosity and surface tension effects can be neglected as well as virtual mass. Unfortunately, this model is ill-posed in its basic form and introduction of additional effects, such as artificial viscosity, are required to obtain a well-posed system.

Numerical instability and inconsistency in two-phase flow models can arise due to two reasons; ill-posedness of the mathematical model, and the restrictions of the numerical scheme even when the model is well posed.^{83,134} Although an ill-posed problem leads to an inherently unstable numerical scheme, it is reported that these unstable schemes can produce apparently stable, but generally inconsistent results if the growth rate resulting from the complex characteristics remains small throughout the time span of the numerical experiment or if sufficient numerical damping is present for the time step size used.⁸⁹

Many of the two-phase flow models in use possess complex characteristics whenever different phase velocities are taken into account. Models exhibiting these imaginary characteristics are generally known as ill-posed. Complex characteristics, leading to instabilities in the numerical solution can also be exhibited by single-fluid models incorporating inappropriate values of slip between the phases.¹³⁵ Concerning the question of whether or not the complex characteristics describe the real physical instabilities of two-phase flow, Ramshaw and Trapp have shown that although stability and real characteristics are not related in general, they become essentially equivalent at high frequencies.¹³⁶ Their analysis indicate that at short wavelengths, equations having complex characteristics will

predict unphysical instabilities. In other words, the problem is improperly posed. Another related study proposes that, although the initial value problem exhibits ill-posedness, the mixed initial and boundary value problem may exhibit real characteristics.¹³⁷ However, it is emphasized that the existence of a realistic boundary condition set which would assure well-posedness cannot be proven.

For two-fluid models, well-posedness is frequently obtained by introducing higher order viscous dissipative terms to two-phase field equations.^{85,89} However, unless small size control volumes are used, the kinematic viscosities required to secure a well-posed problem may be appreciably greater than the true value for the fluid considered. It has also been shown that the systems with these higher order terms could generate long wave instabilities even when the final system is well-posed.⁹⁰ These long wavelength instabilities can be stabilized by wall shear-like stress terms.

APPENDIX C

HFIR DESIGN PARAMETERS

In this appendix, the HFIR design parameters, nuclear, thermal-hydraulic and operational characteristics related to transient analysis are presented in several tables to support the description of the model developed in this work. The values in these tables are from Reference 1.

Table C1 : HFIR Design Parameters: Reactor power levels.

REACTOR POWER LEVELS (MW)	Original Design	Current Cond.
Steady-state	100	85
Reverse point	110	93.5
Scram point (neutron flux)	130	106.25
Scram point (heat power)	120	102
Steady-state incipient boiling	142	122.7

Table C2 : HFIR Design Parameters: Thermal neutron fluxes at full power.

THERMAL FLUX at FULL POWER (neutrons cm ⁻² sec ⁻¹)		Original Design	Current Cond.
Maximum unperturbed in island		5.5x10 ¹⁵	4.7x10 ¹⁵
Average in typical target loading		2.1x10 ¹⁵	1.8x10 ¹⁵
Maximum unperturbed in Be reflector	BOC	1.1x10 ¹⁵	9.4x10 ¹⁴
	EOC	1.6x10 ¹⁵	1.4x10 ¹⁵
Maximum unperturbed at reflector interface	BOC	1.4x10 ¹⁴	1.2x10 ¹⁴
	EOC	1.7x10 ¹⁴	1.4x10 ¹⁴
Average in fuel region	BOC	3.3x10 ¹⁴	2.8x10 ¹⁴
	EOC	4.5x10 ¹⁴	3.8x10 ¹⁴

Table C3: HFIR Design Parameters: Total nonthermal neutron fluxes at full power.

TOTAL NON-THERMAL FLUX AT FULL POWER (neutrons cm ⁻² sec ⁻¹)	Original Design	Current Cond.
Averaged in island target	2.4x10 ¹⁵	2.0x10 ¹⁵
Maximum in fuel region	4.0x10 ¹⁵	3.4x10 ¹⁵

Table C4 : HFIR Design Parameters: Fuel plate material characteristics.

	FUEL PLATE MATERIAL
Fuel Plate Material	U ₃ O ₈ -Al cermet, Al cladding
Weight of U ²³⁵ per plate in inner fuel elements (g)	15.18±1 %
Weight of U ²³⁵ per plate in outer fuel elements (g)	18.44±1 %
U ²³⁵ enrichment	~ 93 %
Total fuel loading of U ²³⁵ (kg)	9.40
Total burnable poison load in each inner fuel plate (g)	2.8

Table C5 : HFIR Design Parameters: Material characteristics of other reactor core components.

COMPONENT		MATERIAL
Coolant		H ₂ O
Island moderators		H ₂ O
Side reflector	Removable	Be + 5% H ₂ O
	Permanent	Be + 2% H ₂ O
Shim, safety and regulating cylinders	Black region	Eu ₂ O ₃ + Al
	Gray region	Ta + Al
	White region	Al
Target Rods		Actinide Oxide-Al cermet

Table C6 : HFIR Design Parameters: General heat transfer and coolant data.

GENERAL HEAT TRANSFER and COOLANT DATA		Original Design	Current Cond.
System design pressure (psi)		1000	900 (hydrotest)
Normal operating pressure - vessel inlet (psi)		600	468
System pressure drop (psi)		~ 156	~ 156
Design heat loads at full power (MW)	Fuel elements	97.5	82.9
	Target	0.90	0.77
	Control cylind.	4.7	4.0
	Reflector	3.7	3.1
Design coolant flow rates (gpm)	Total	~ 16000	~ 16000
	Fuel channels	~ 13000	~ 13000
	Between fuel elements	133	133
	Island with target	788	788
	Reflector	740	740
	Control cylind.	1080	1080
	Experimental facilities	365	365
Coolant temperatures at full power (F)	Vessel inlet	120	120
	Vessel outlet	164	158
	Maximum bulk water	249	230
	Maximum surface	387	347

Table C7 : HFIR Design Parameters: Fuel region characteristics.

			FUEL REGION CHARACTERISTICS
Geometry			Two concentric cylindrical annuli, involute fuel plates
Fuel element dimensions (in)	Inner fuel element diameters	Inner diam	5.067
		Outer diam	10.590
	Outer fuel element diameters	Inner diam	11.250
		Outer diam	17.134
	Active fuel region diameters (average)	Inner ID	5.623
		Inner OD	9.920
		Outer ID	11.913
		Outer OD	16.483
	Height of active core		20
	Total height		24
	Fuel plate thickness		0.050
	Cladding thickness		0.010
	Fuel-plate meat thick.		0.030
	Coolant channel thick.		0.050
Number of fuel plates	Inner fuel element		171
	Outer fuel element		369
Total heat transfer area (avg) (ft ²)			428.8
Volume of active core (avg) (liters)			50.59

Table C8 : HFIR Design Parameters: Fuel region heat transfer and coolant data.

FUEL REGION DATA			Original Design	Current Cond.
Heat load at full power (MW)			97.5	82.9
Power Density (MW/liter)	Maximum		4.38	4.11
	Average		1.93	1.64
Heat Flux (at steady-state power) (Btu/hr-ft ²)	Hot-spot		1.97x10 ⁶	1.85x10 ⁶
	Average		0.776x10 ⁶	0.658x10 ⁶
	Hot-spot at onset of boiling		3.4x10 ⁶	3.19x10 ⁶
Temperatures (F)	Inlet		120	120
	Outlet		163-196	156-185
	Oxide-water interface	Max.	387	347
		Avg.	146-269	142-247
	Fuel-oxide interface	Max.	589	519
		Avg.	149-303	145-276
	Fuel plate	Max.	620	545
		Avg.	153-325	148-295
	Saturation - hot-spot		465	435
Coolant flow rate (gpm)			~ 13000	~ 13000
Coolant velocity (ft/sec)			51	51
Pressure drop across fuel elements (psi)			~ 108	~ 108

Table C9 : HFIR Design Parameters: Target region characteristics.

		TARGET REGION CHARACTERIST.
Geometry		Cylindrical target rods spaced on triangular pattern
Target region dimensions (in)	Diameter of water island	5.067
	Rod Diameter	0.360
	Height of active portion	20
	Total rod length	35
	Spacing between rods	0.665
Number of target rods		31

Table C10 : HFIR Design Parameters: Target region heat transfer and coolant data.

TARGET REGION DATA		Original Design	Current Cond.
Heat load at full power (MW)		0.9	0.9
Average heat flux (Btu/hr-ft ²)		0.6x10 ⁶	0.5x10 ⁶
Coolant flow rate (gpm)		788	788
Coolant velocity (ft/sec)		40	40
Temperatures (F)	Inlet	120	120
	Outlet	129	128
	Rod surface	255	255
Pressure drop across target rods (psi)		45	45

Table C11 : HFIR Design Parameters: Control region characteristics.

		CONTROL REGION CHARACTERISTICS
Geometry		5 plates which constitute two concentric cylinders
Overall region radii (in)	Inside	8.567
	Outside	9.436
Inner control cylinder radii (in)	Inside	8.671
	Outside	8.921
Outer (safety) quadrants radii (in)	Inside	9.050
	Outside	9.300
Height within the core region (in)	Black region	22
	Gray region	5
	White region	~ 20
Coolant channel thickness (in)		0.104, 0.170, 0.095

Table C12 : HFIR Design Parameters: Control region heat transfer and coolant data.

CONTROL REGION DATA		Original Design	Current Cond.
Heat load at full power (MW)		4.7	4.0
Maximum heat flux (Btu/hr-ft ²)		3.8x10 ⁵	3.23x10 ⁵
Coolant flow rate (gpm)		1080	1080
Coolant velocity (ft/sec)		16	16
Temperatures (F)	Inlet	120	120
	Outlet	137	135
	Cylinder surface	205	192
Pressure drop across target rods (psi)		~ 9	~ 9

Table C13 : Reactivity parameters of the HFIR core.

PARAMETER		REACTIVITIES (dk/k)	
		BOC	EOC
Fuel worth at zero power (without poison, target and reflector)		0.135	
Temperature deficit (zero power, with target)		-0.004	
Boron burnable poison		-0.05	-0.009
$\text{Xe}^{135} + \text{Sm}^{149}$		0	-0.053
All fission products		0	-0.086
Be poison ($\text{Li}^6 + \text{He}^3$)	Time zero	0	
	0.2 years	-0.013	
	5 years	-0.016	
Beam tube flooding		0	
Fuel loading tolerance ($\pm 1\%$)		± 0.0015	
Boron loading tolerance ($\pm 10\%$)		± 0.0038	
Fuel distribution tolerance ($\pm 10\%$)		± 0.0054	
Boron distribution tolerance ($\pm 35\%$)		± 0.0023	
Minimum $k_{\text{eff}} - 1$ (clean core, full power)		0.054	
Maximum $k_{\text{eff}} - 1$ (clean core, zero power)		0.106	
Control cylinder worth	Both cylinders inserted	0.187	
	Inner cylinder withdrawn, 4 safety quadrants in.	0.147	
Shutdown margins for maximum k_{eff}	Both cylinders inserted	0.069	
	Inner cylinder withdrawn	0.030	
Shutdown margins for nominal k_{eff}	Both cylinders inserted	0.081	
	Inner cylinder withdrawn	0.042	

Table C14 : HFIR control elements reactivity characteristics.

PARAMETER		
Control element total worth (dk/k)	Both cylinders inserted	0.187
	Inner cylinder withdrawn, 4 safety quadrants inserted	0.147
Maximum differential worth (dk/k/in)	Inner cylinder	0.011
	Four outer quadrants	0.010
Control element speeds (in/sec)	Outer cylinder shim action	0.0958
	Outer cylinder scram action	1.0
	Inner cylinder shim action	0.0958
	Inner cylinder regulating act.	0.250
Control element maximum reactivity insertion rates (dk/k/sec)	Outer cylinder shim action	± 0.00096
	Outer cylinder scram action	-0.010
	Inner cylinder shim action	± 0.0011
	Inner cylinder regulating act.	± 0.0028

Table C15 : HFIR temperature coefficients of reactivity.

CONDITION		REACTIVITY (dk/k/F)	
		at 79°F	at 155°F
With target	Overall isothermal	-1.16×10^{-5}	-3.1×10^{-5}
	Fuel region only	-7.3×10^{-5}	-10.2×10^{-5}
	Island + control + reflector	6.1×10^{-5}	7.1×10^{-5}
Without target	Overall isothermal	2.3×10^{-5}	-2.1×10^{-5}
	Fuel region only	-6.0×10^{-5}	-11.7×10^{-5}
	Island + control + reflector	8.3×10^{-5}	9.6×10^{-5}

Table C16 : HFIR void reactivity coefficients for uniform voids.

CONDITION			REACTIVITY [(dk/k)(dV/V)]
With target	Island	< 20 % voids	0.05
		~ 42 % voids (optimum)	0
		Maximum positive change in k_{eff}	0.016 dk
	Fuel region	Inner fuel element	-0.188
		Outer fuel element	-0.384
		Annulus between fuel elements	-0.046
Without target	Island	< 20 % voids	0.06
		~ 70 % voids (optimum)	0
		Maximum positive change in k_{eff}	0.032 dk
	Fuel region	Inner fuel element	-0.188
		Outer fuel element	-0.384
		Annulus between fuel elements	-0.046

APPENDIX D

COMPARISONS WITH RELAP5/MOD2

The RELAP comparisons are originally discussed in Chapter 6. To improve readability, however, additional comparison figures which support the discussions in Section 6.1.1 are presented here. The nodalization diagram of the RELAP model for which the comparisons are made is shown in Figure 17 and a description of each of the hydrodynamic components and heat structures shown in the diagram is included in Table 19. In this model nine parallel coolant channels and nine fuel plates are modeled to represent the flow between the inlet and outlet plena using periodic boundary conditions. The pressure vessel upper and lower plena are modeled as "time-dependent volumes" to specify fluid conditions (specifically temperature and pressure) at a pressure boundary. Coolant channels are modeled as a series of 10 volumes and 9 junctions describing "pipes." Fuel plates are modeled as "heat structures" with six mesh intervals. The model used for comparison purposes does not include the reactor kinetics module. Instead, the power excursion data are taken from new model, FDP, and introduced to the RELAP model as a "*power table*." Therefore, only the verification of fluid dynamics variables has been feasible.

The two-pressure boundary condition formulation described above is mainly used for verification of boiling cases. Since the coolant channels are connected to the upper and lower plena through normal junctions in this model, inflow and outflow depend on the pressure difference between the upper and lower plena and the channel friction losses. Therefore, by adjusting the channel surface roughness, the channel flow is adjusted to match the value determined with FDP calculations. In order to verify the effect of different boundary conditions on results as discussed in Section 3.2.6.1, the inlet flow rate and outlet plenum pressure combination is also studied for the single-channel single-phase comparisons. For these transients in which the inlet flow rate is specified, the channel inlet junctions which connect the upper plenum to coolant channels are converted from "single junctions" to "time dependent junctions."

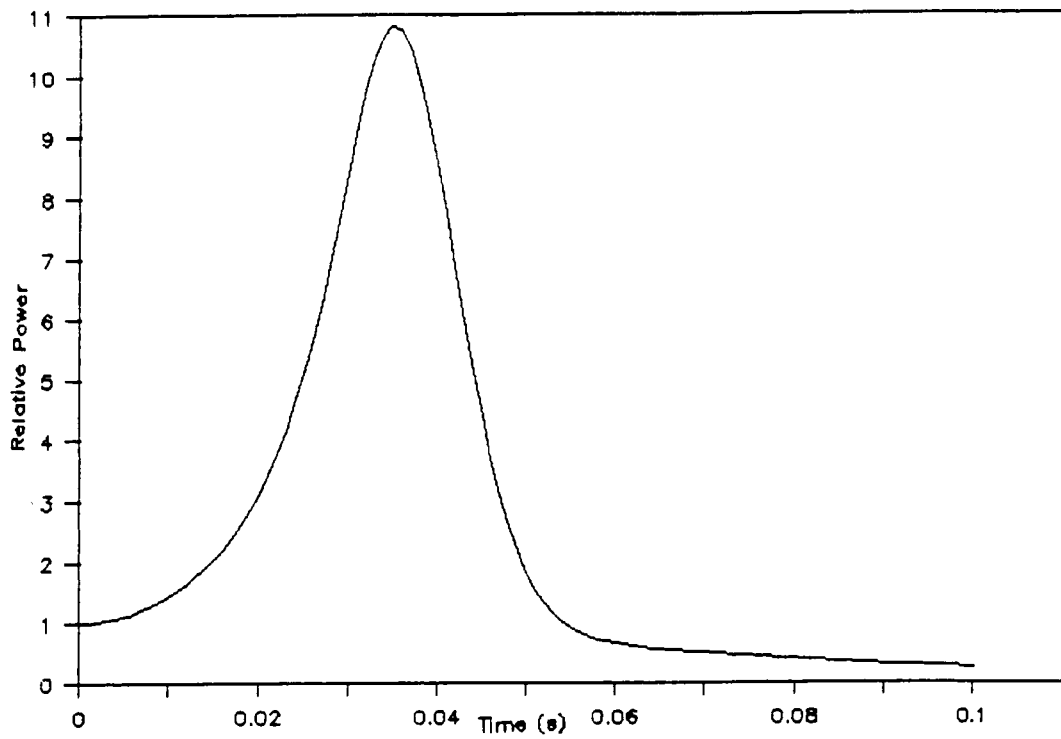


Figure D1 : Power excursion for RELAP5/Mod2 single-phase flow comparisons.

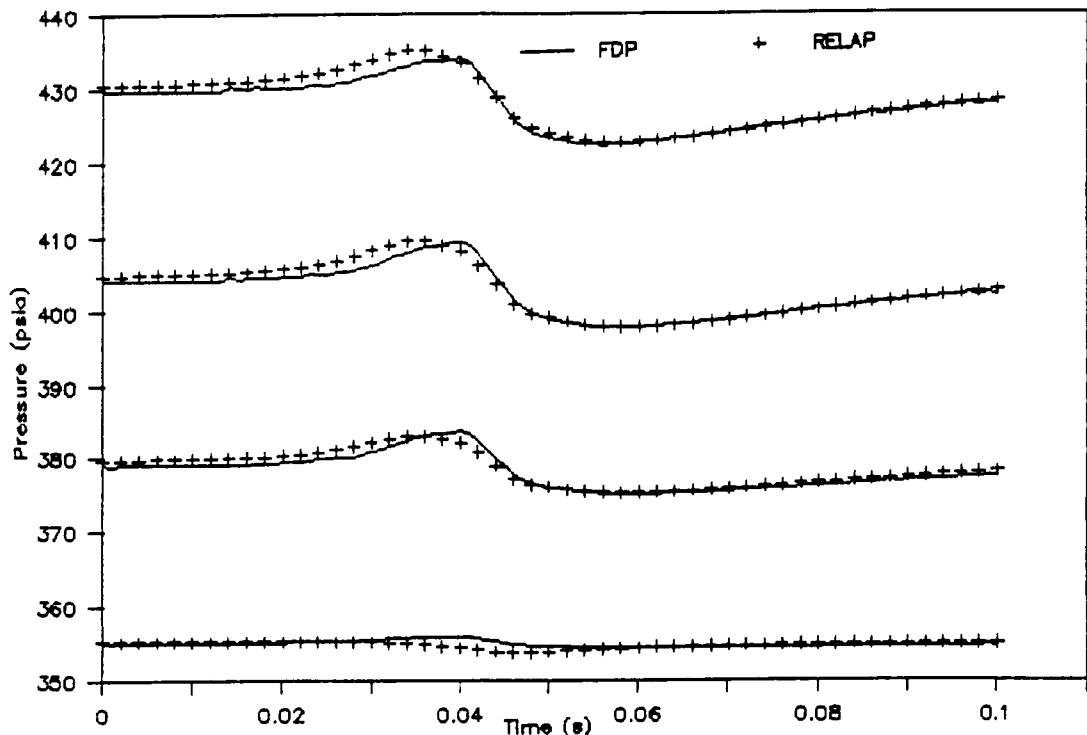


Figure D2 : Channel pressure comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for single-phase flow.

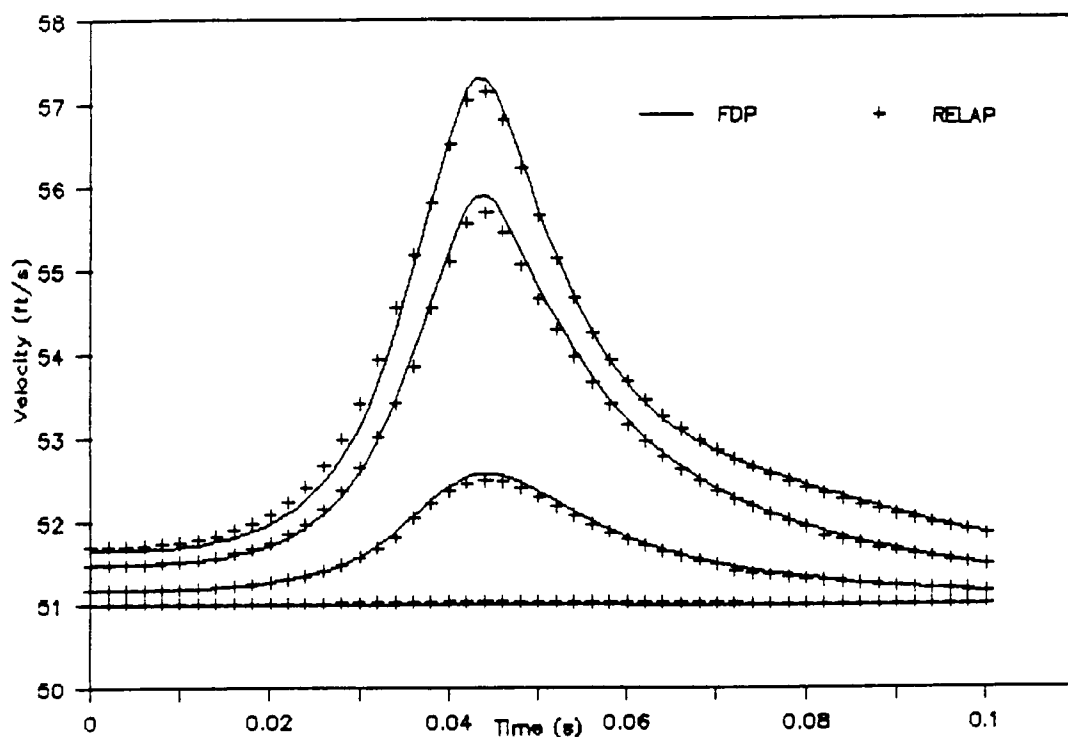


Figure D3 : Coolant velocity comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for single-phase flow.

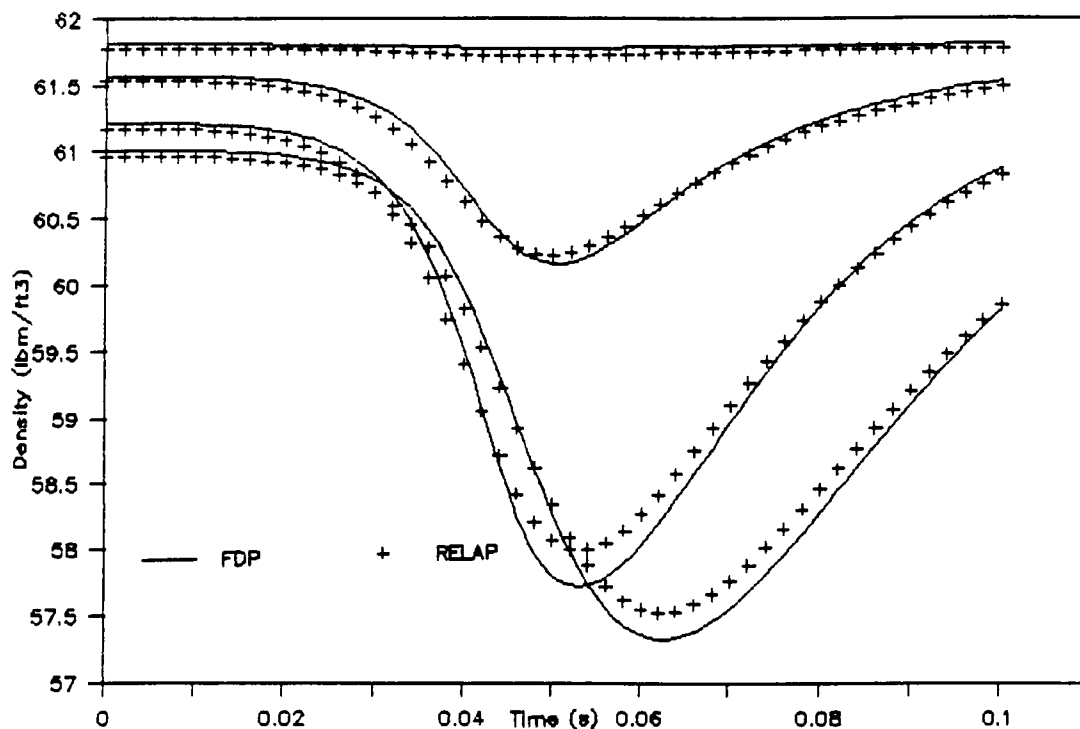


Figure D4 : Coolant density comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for single-phase flow.

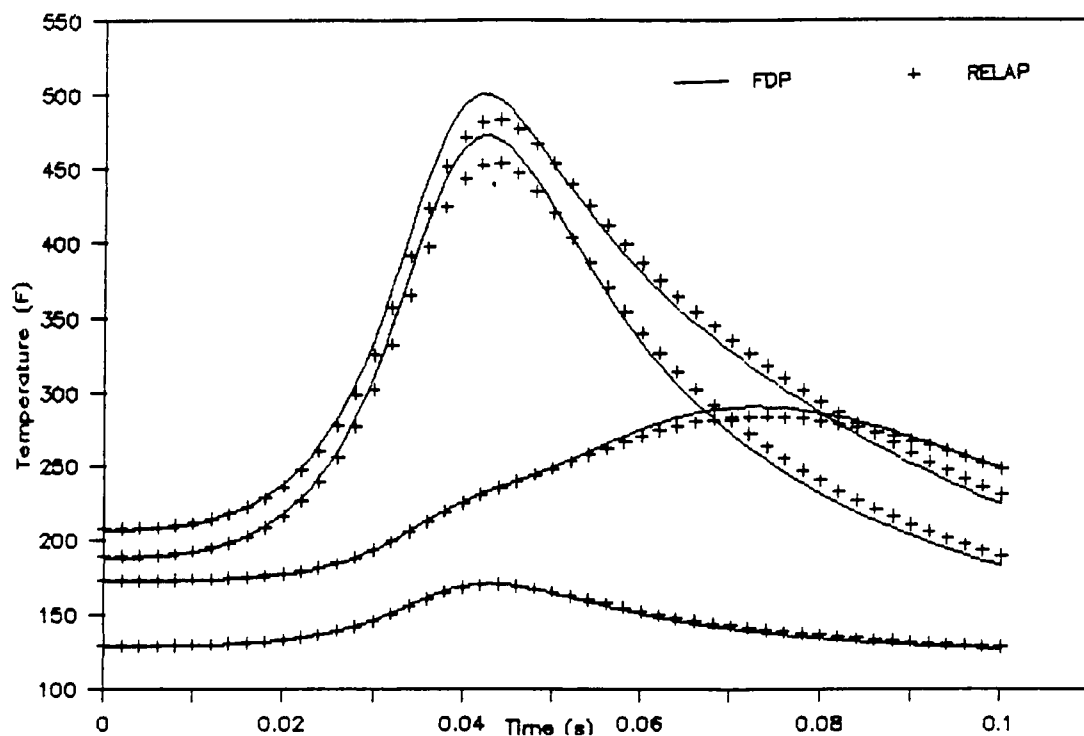


Figure D5 : Fuel temperature comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for single-phase flow.

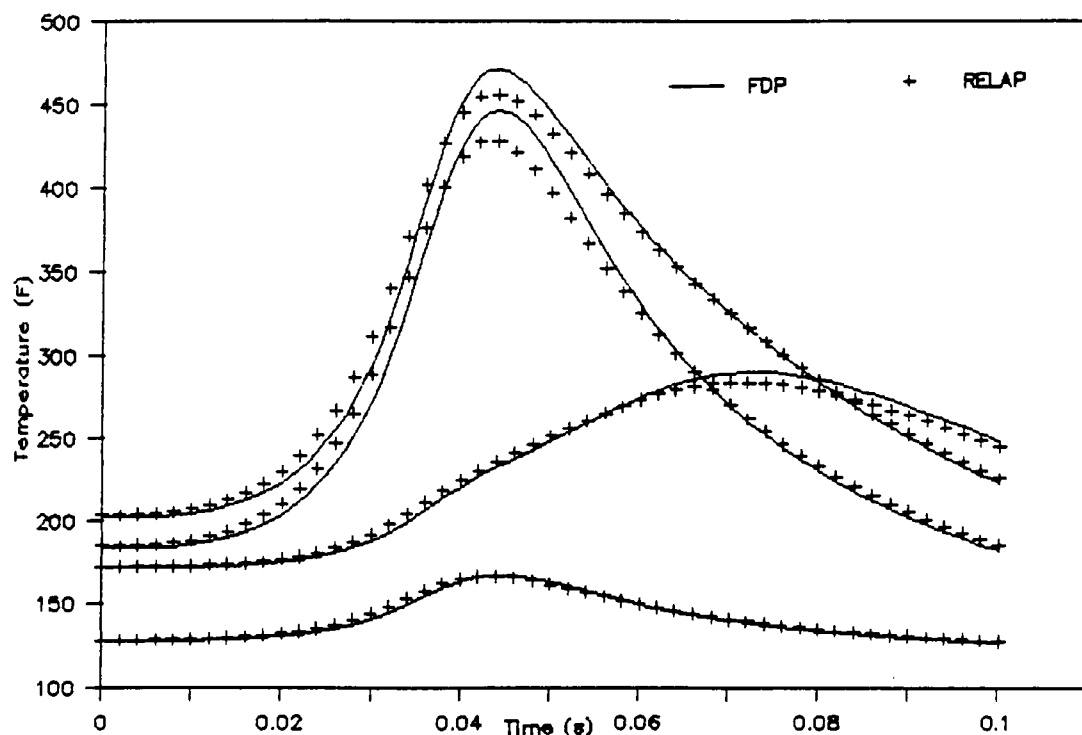


Figure D6 : Surface temperature comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for single-phase flow.

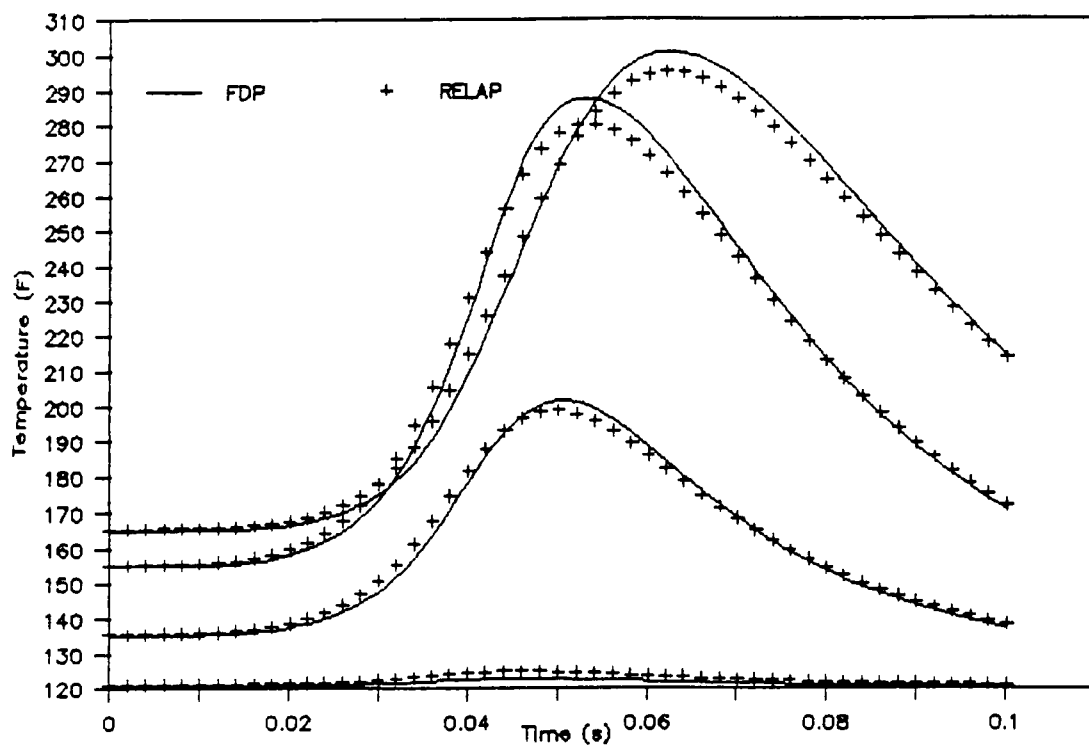


Figure D7 : Coolant temperature comparisons with RELAP5/Mod2 for 1, 4, 7, 10th nodes for single-phase flow.

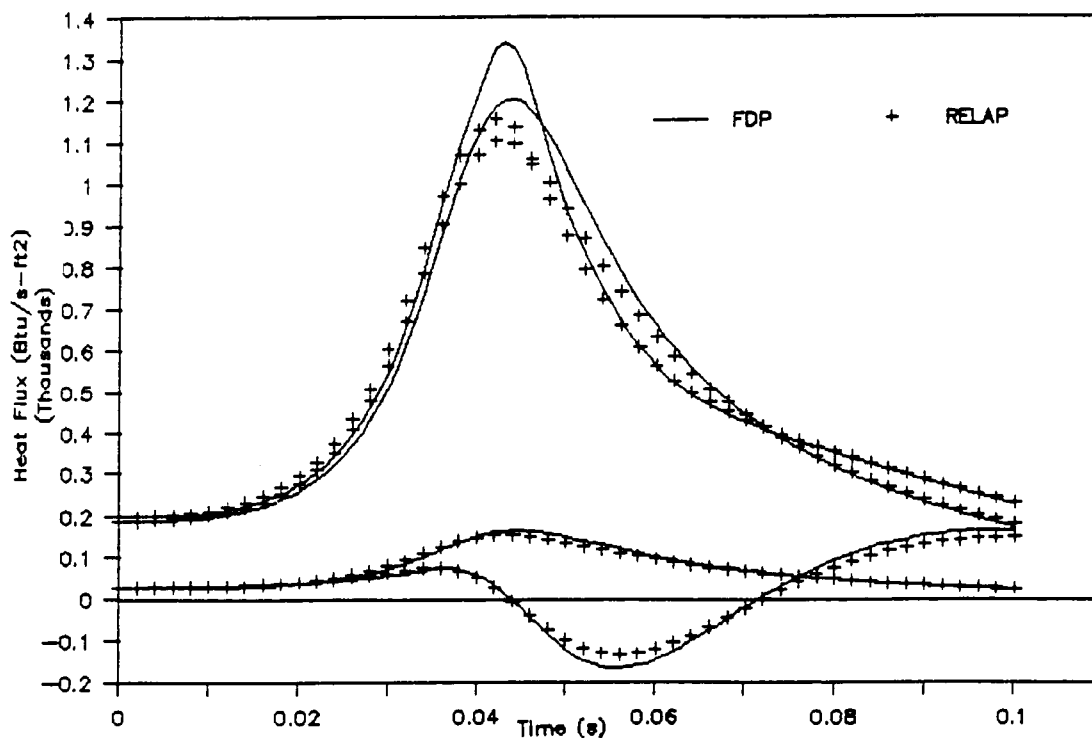


Figure D8 : Surface heat flux comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for single-phase flow.

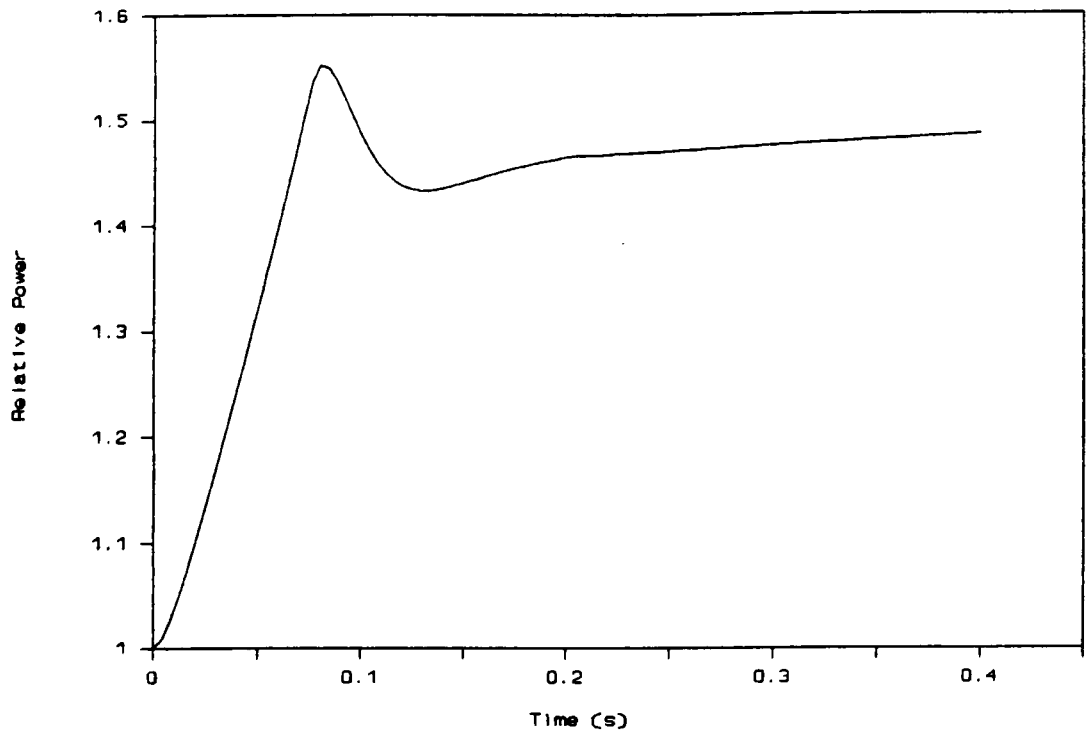


Figure D9 : Power excursion for RELAP5/Mod2 subcooled boiling flow comparisons.

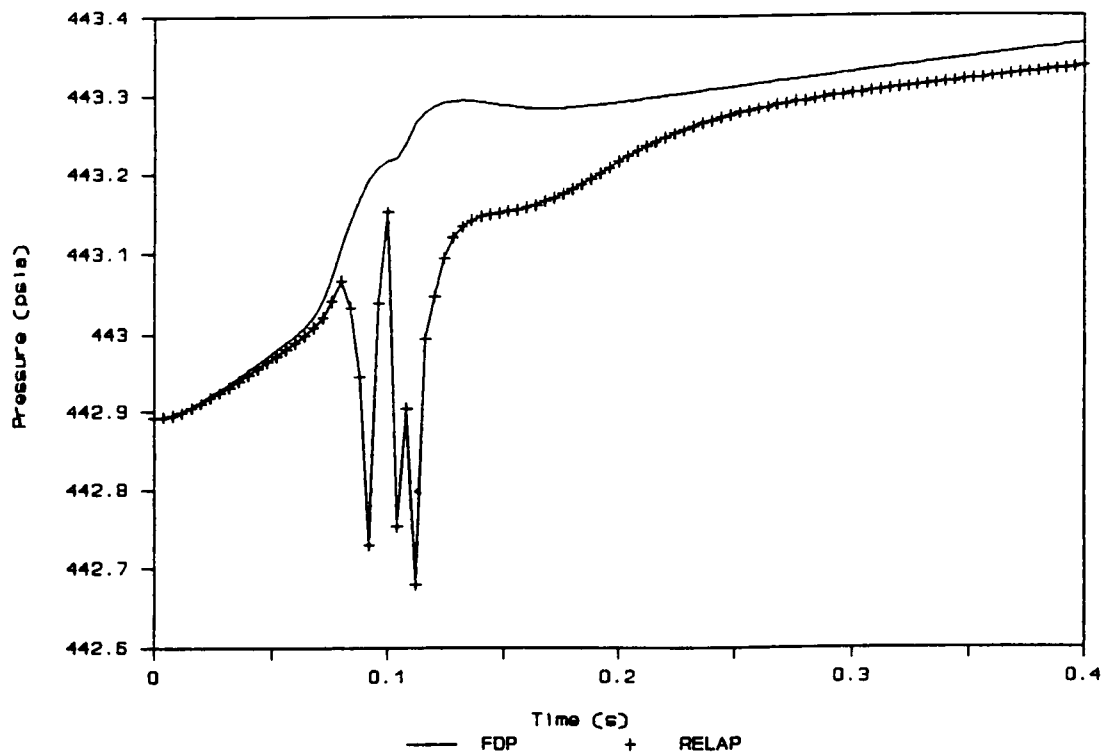


Figure D10 : Inlet (1st) node pressure comparisons with RELAP5/Mod2 for subcooled boiling flow.

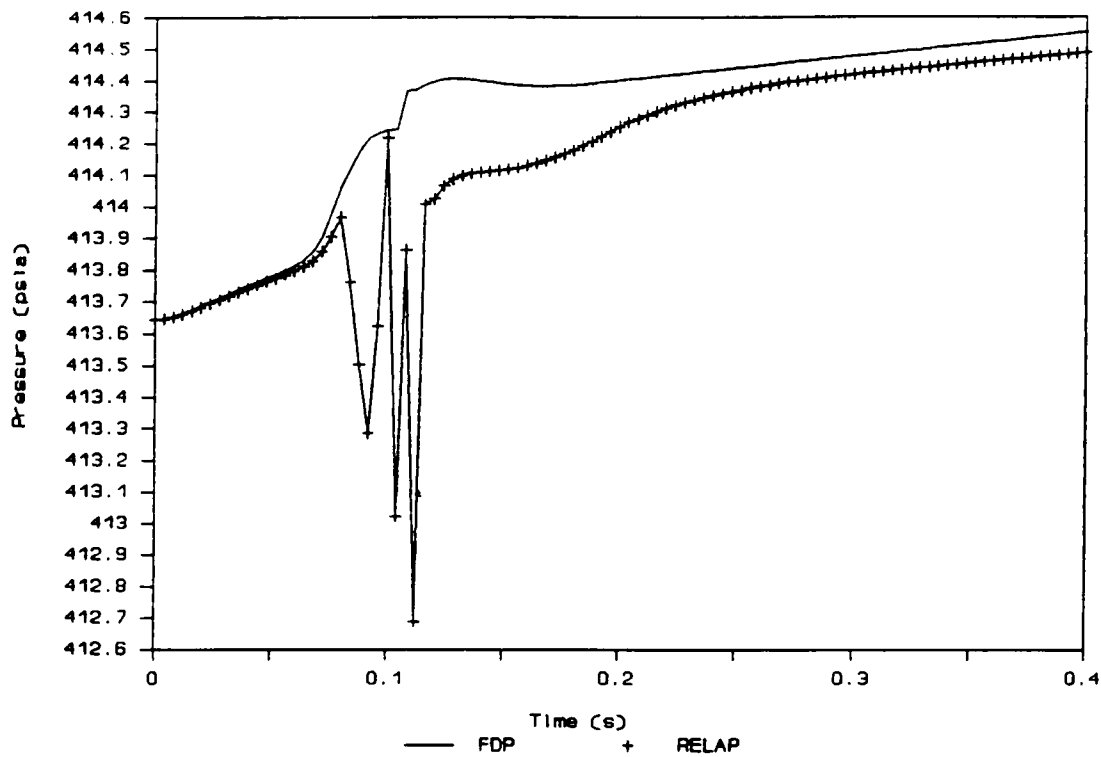


Figure D11 : 4th node (out of 10) pressure comparisons with RELAP5/Mod2 for subcooled boiling flow.

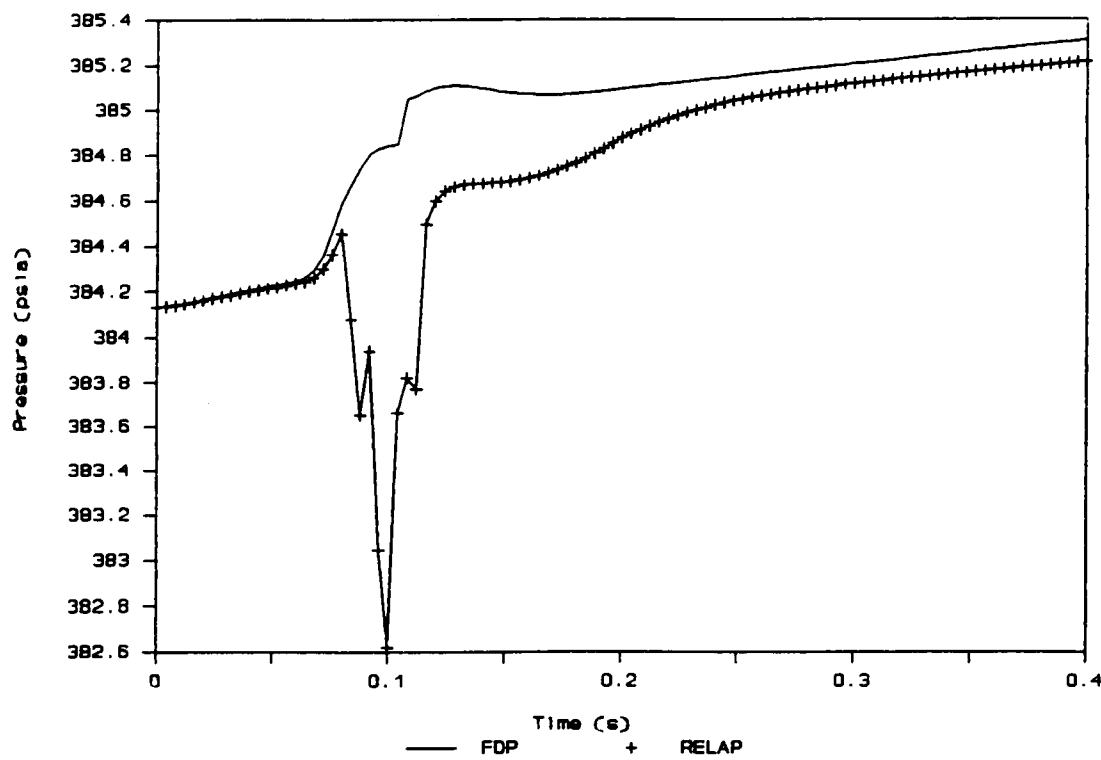


Figure D12 : 7th node pressure comparisons with RELAP5/Mod2 for subcooled boiling flow.

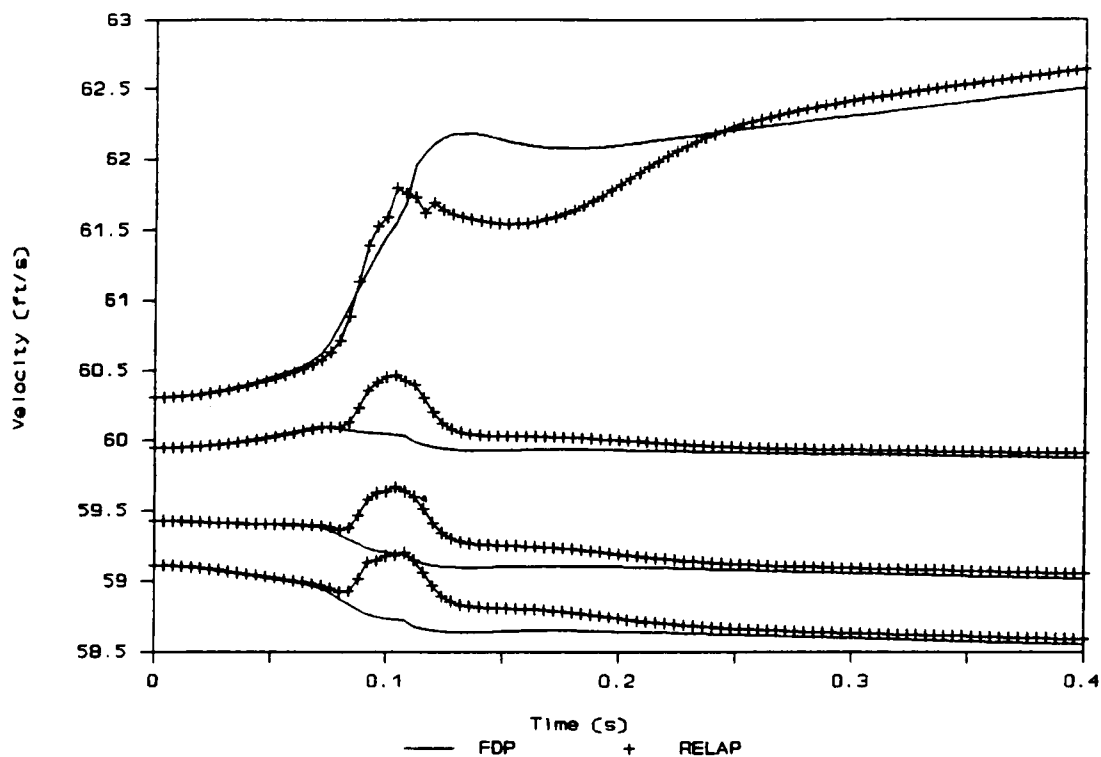


Figure D13 : Liquid velocity comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for subcooled boiling.

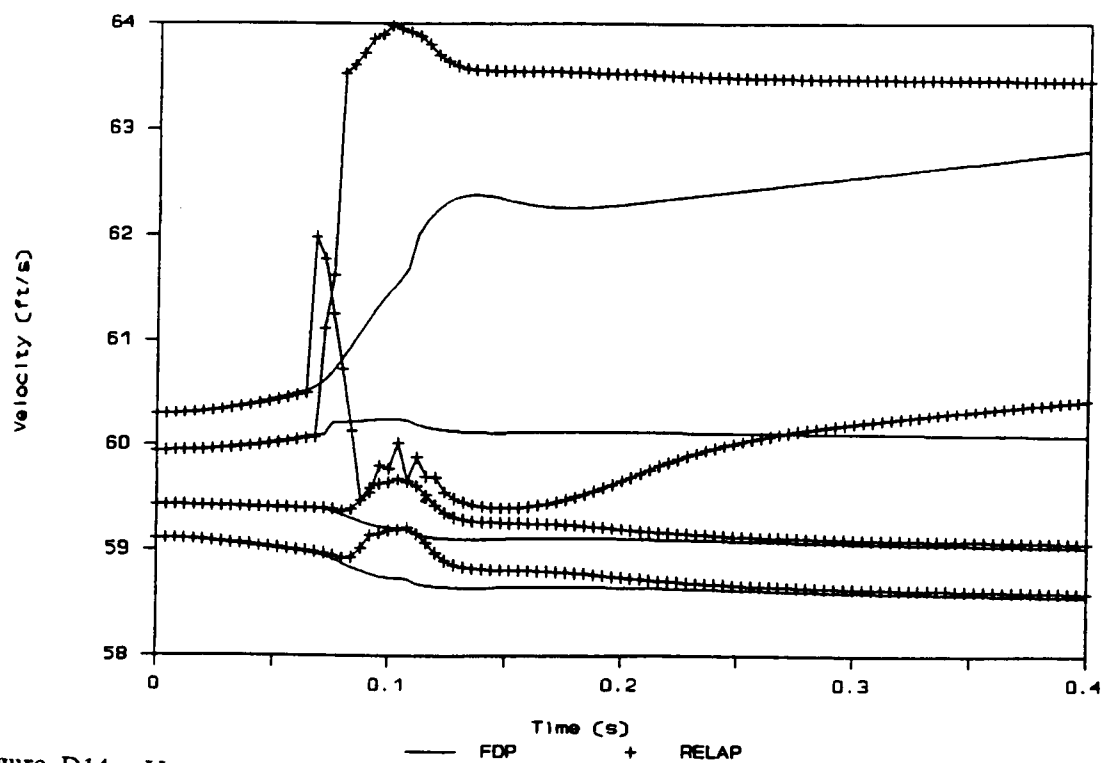


Figure D14 : Vapor velocity comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for subcooled boiling.

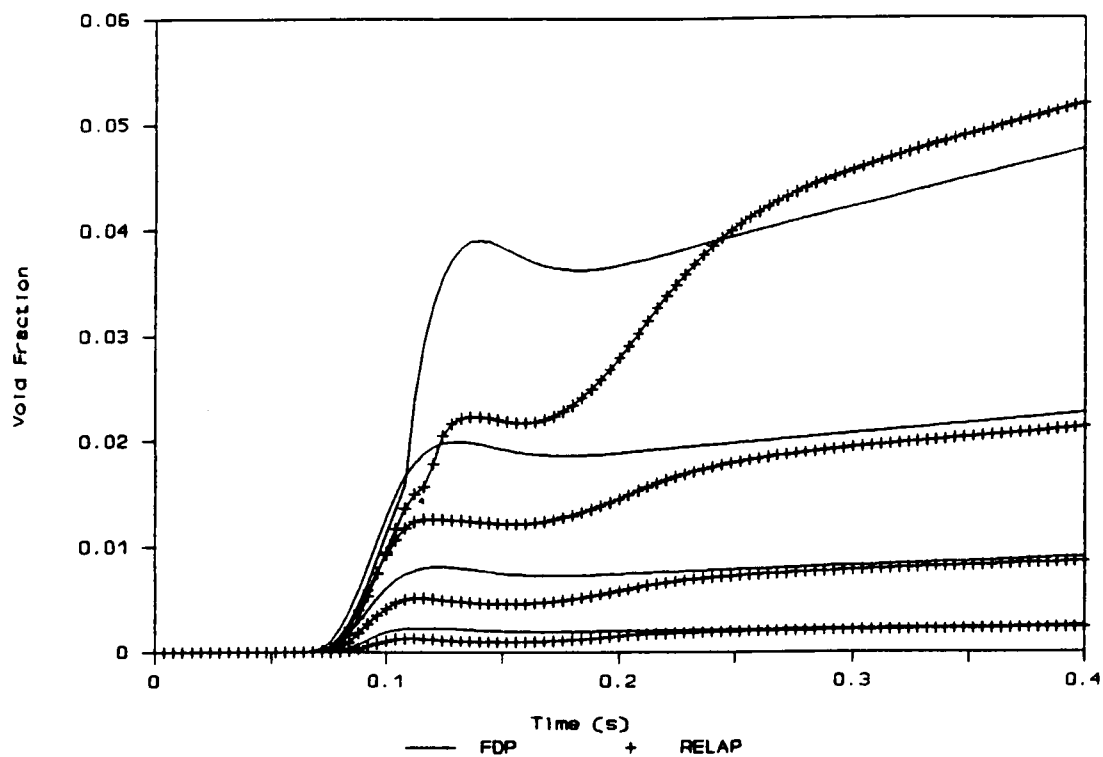


Figure D15 : Void fraction comparisons with RELAP5/Mod2 for last four nodes for subcooled boiling flow.

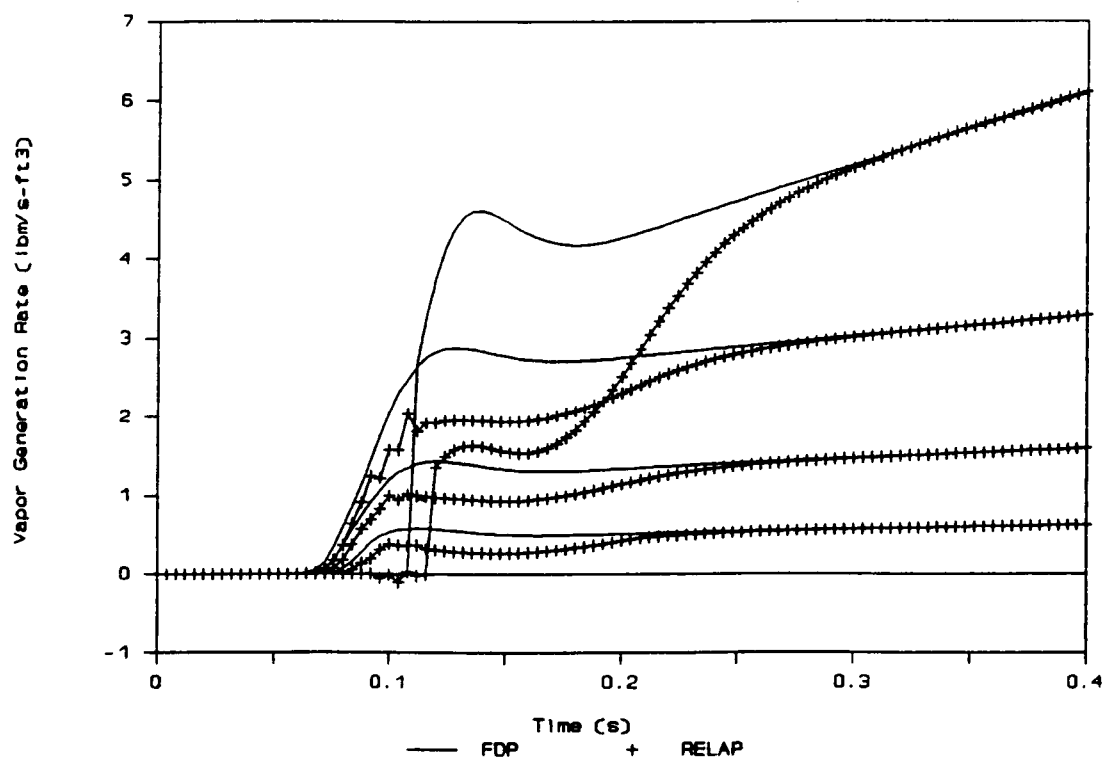


Figure D16 : Vapor generation term comparisons with RELAP5/Mod2 for last four nodes for subcooled boiling.

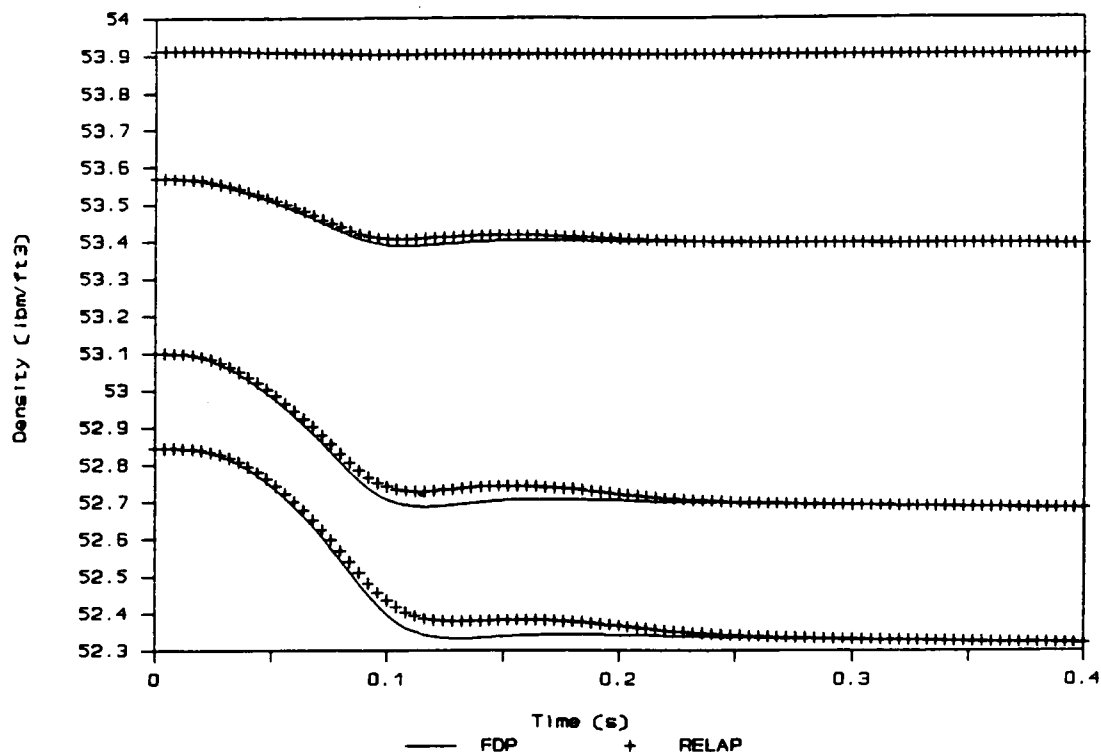


Figure D17 : Liquid density comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for subcooled boiling flow.

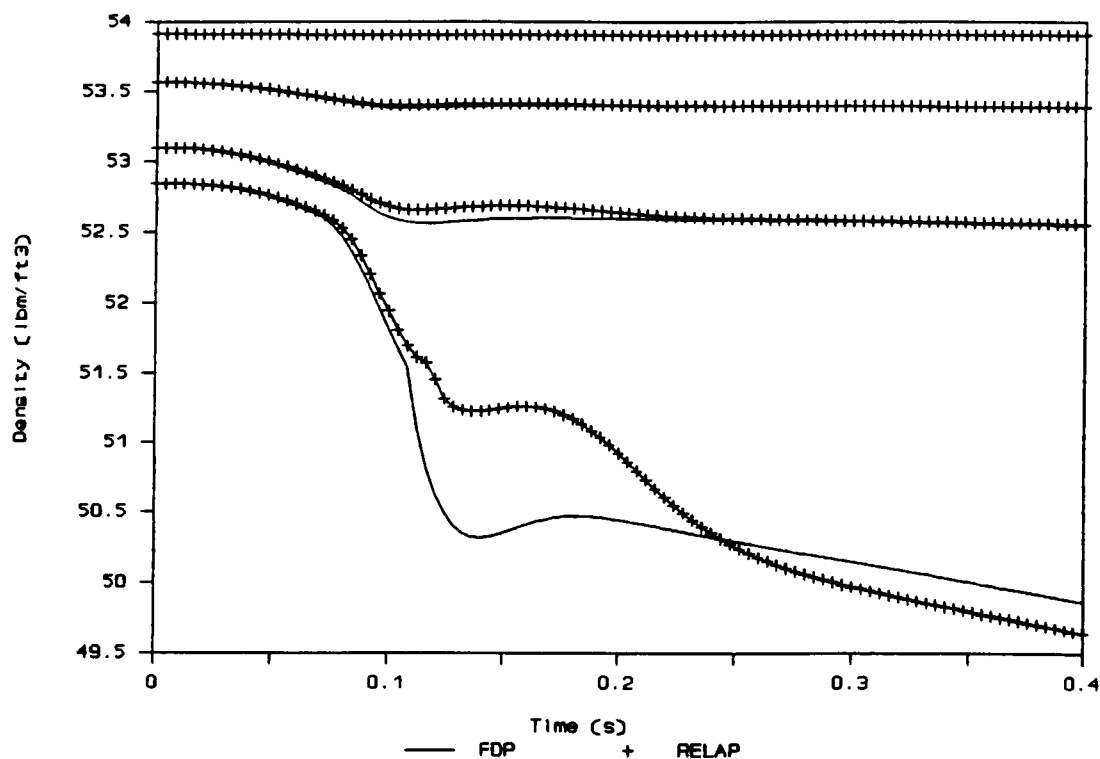


Figure D18 : Mixture density comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for subcooled boiling.

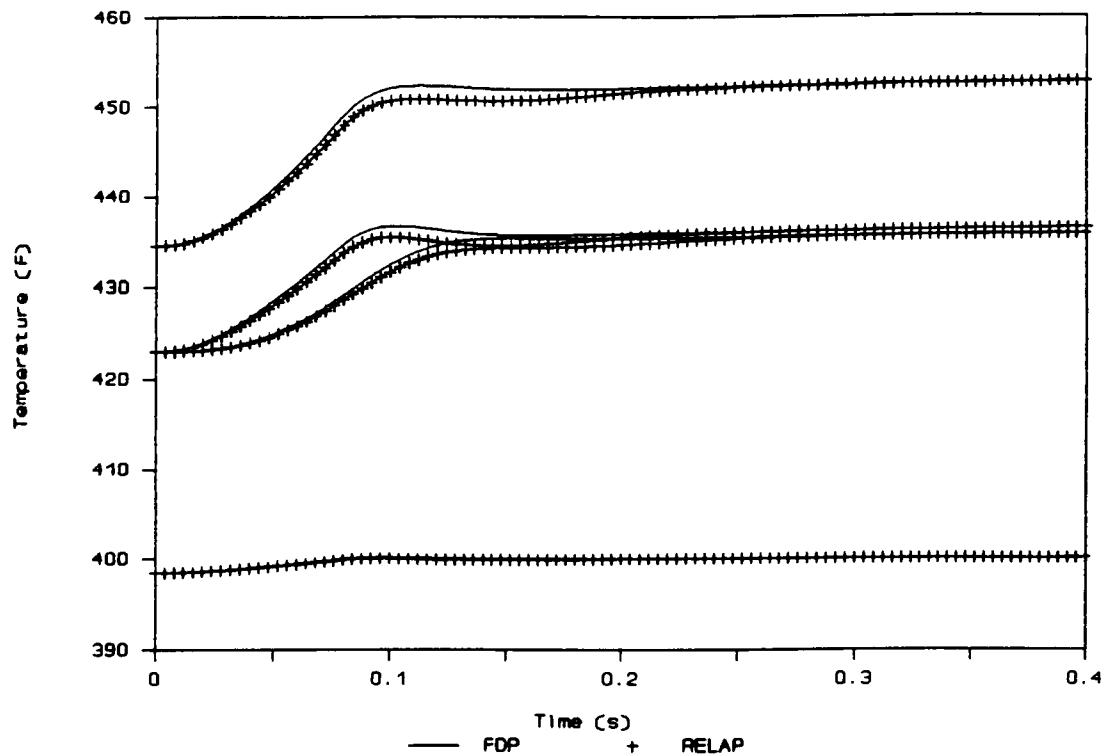


Figure D19 : Fuel temperature comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for subcooled boiling.

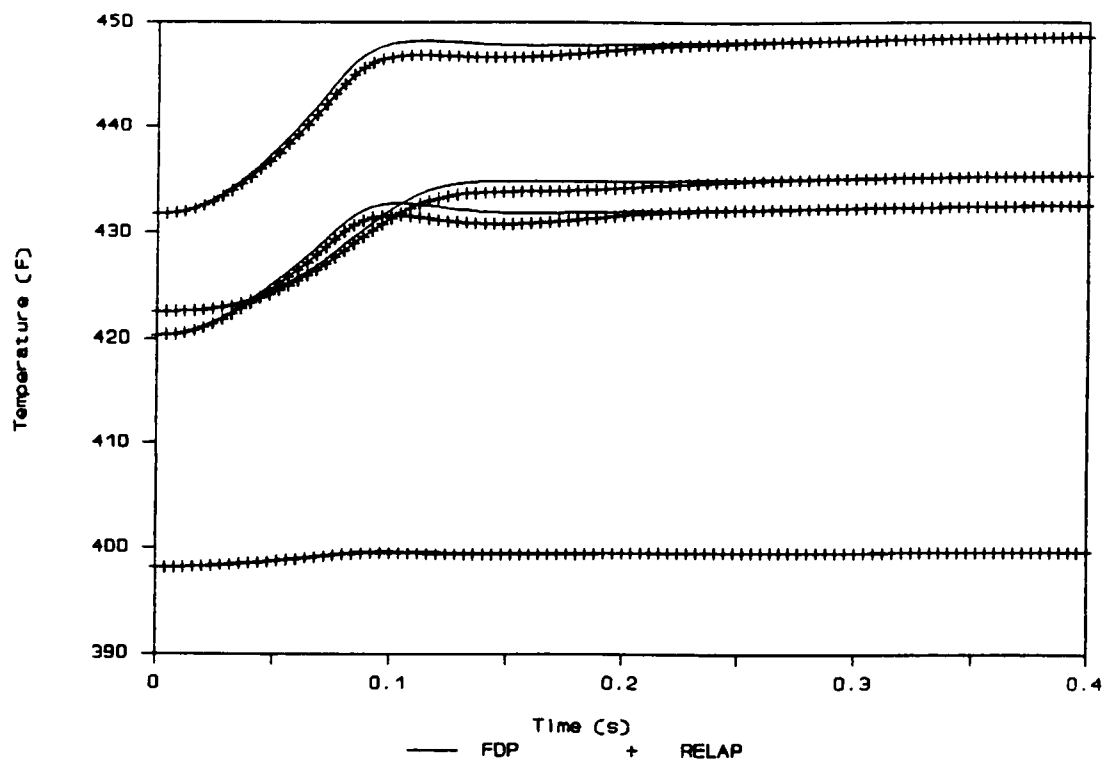


Figure D20 : Surface temperature comparisons with RELAP5/Mod2 for 1, 4, 7, 10th nodes for subcooled boiling.

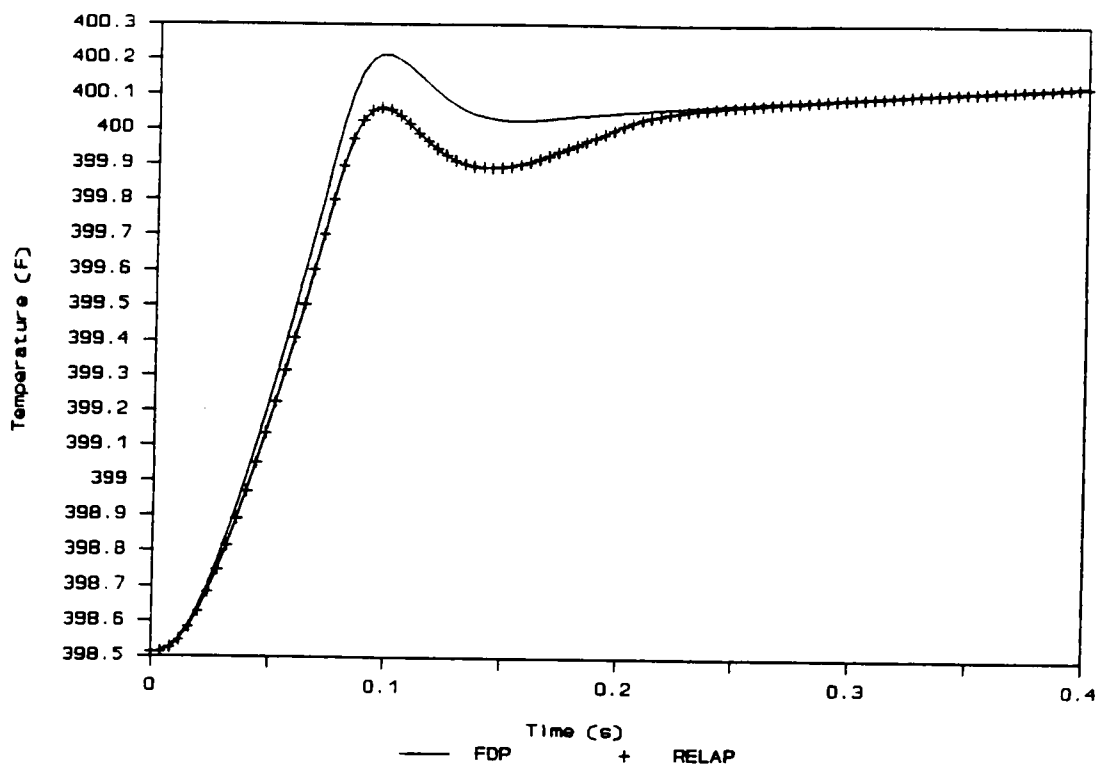


Figure D21 : Inlet node average fuel temperature comparisons with RELAP5/Mod2 for subcooled boiling flow.

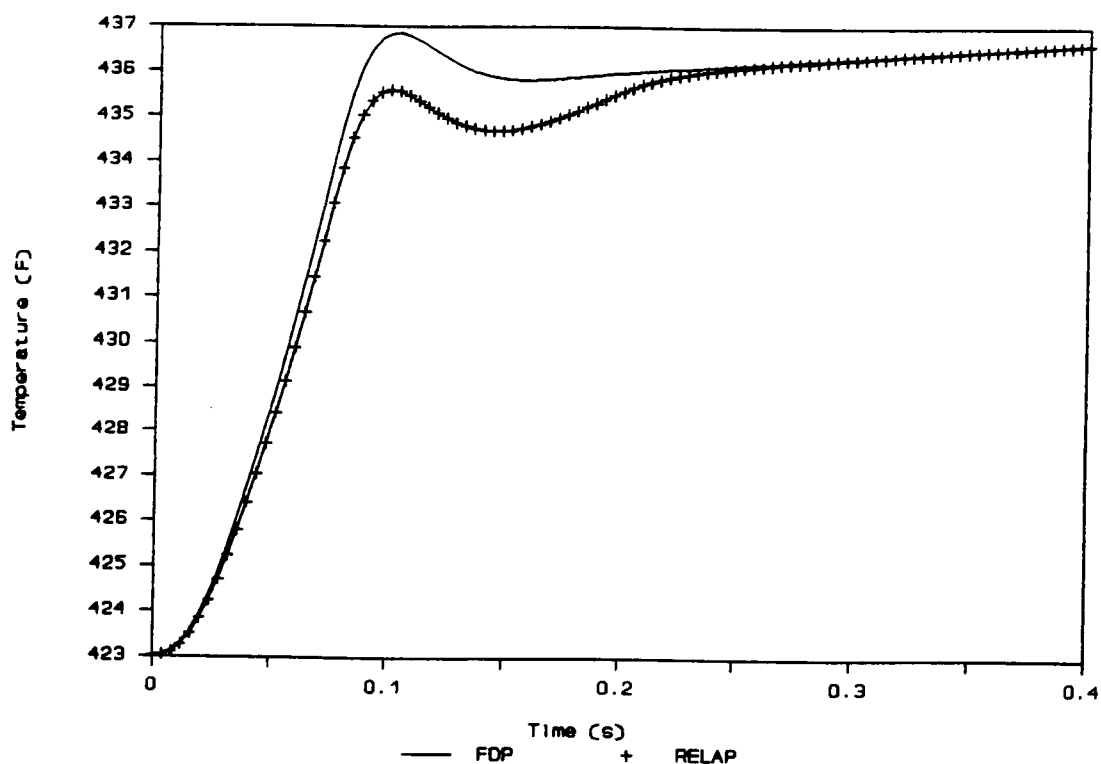


Figure D22 : 4th node average fuel temperature comparisons with RELAP5/Mod2 for subcooled boiling flow.

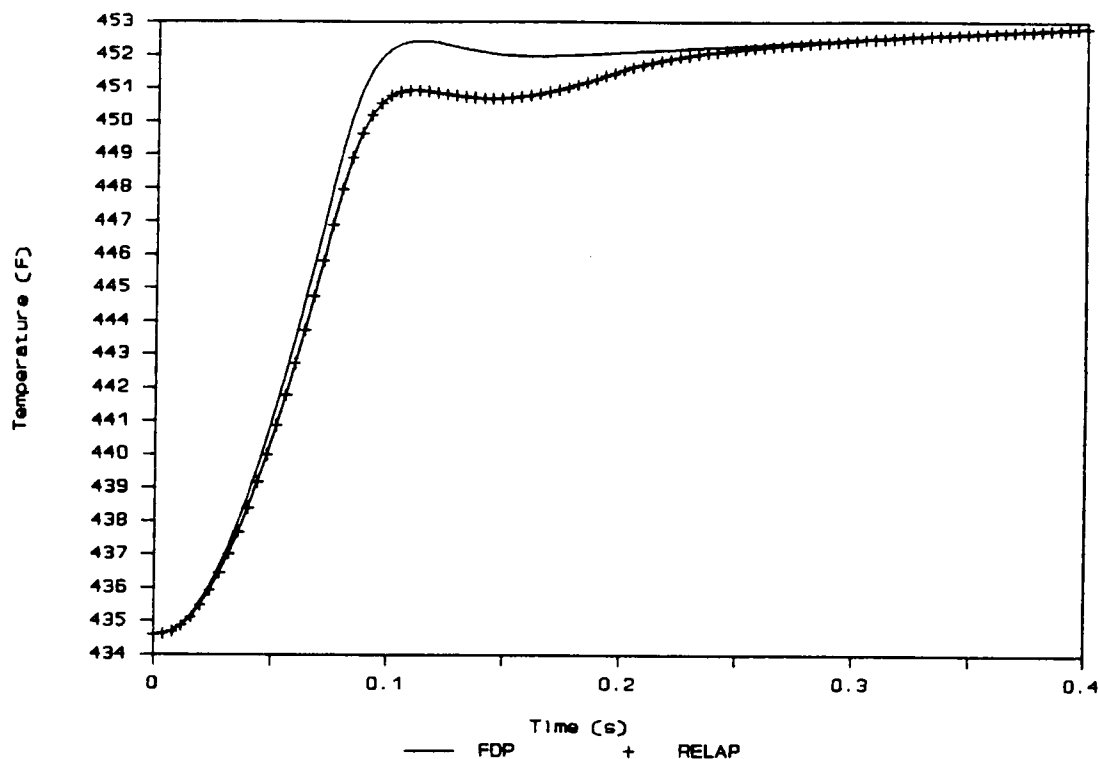


Figure D23 : 7th node average fuel temperature comparisons with RELAP5/Mod2 for subcooled boiling flow.

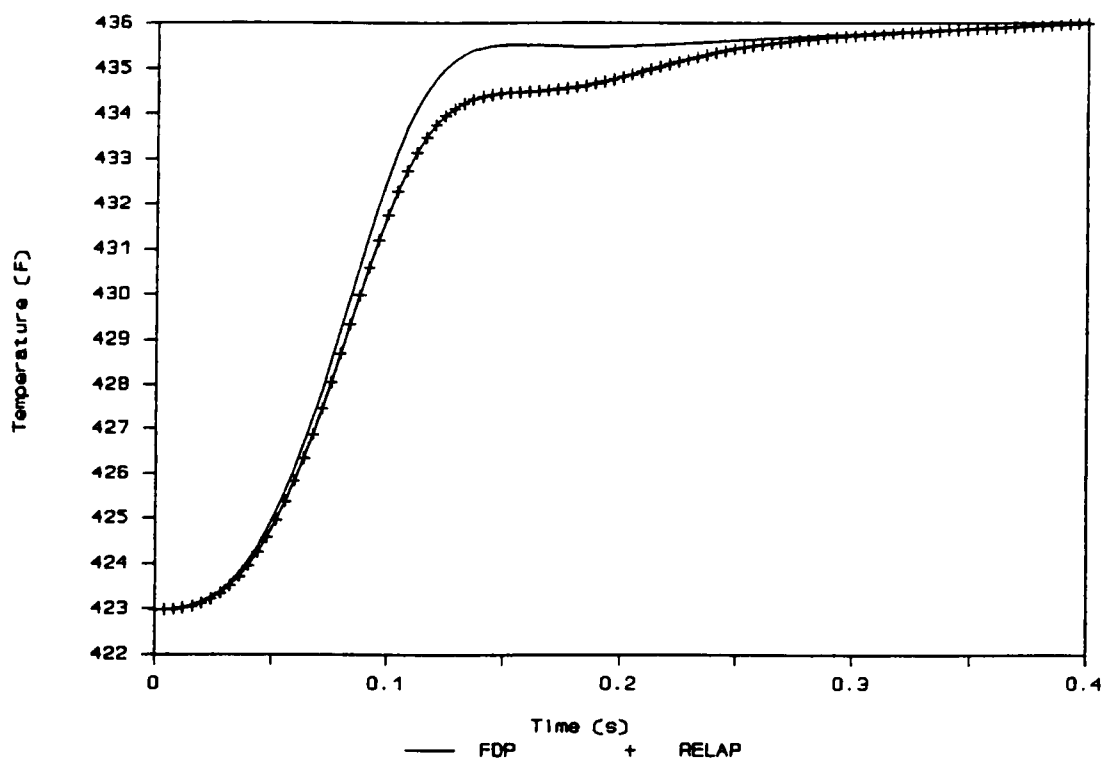


Figure D24 : Outlet node average fuel temperature comparisons with RELAP5/Mod2 for subcooled boiling flow.

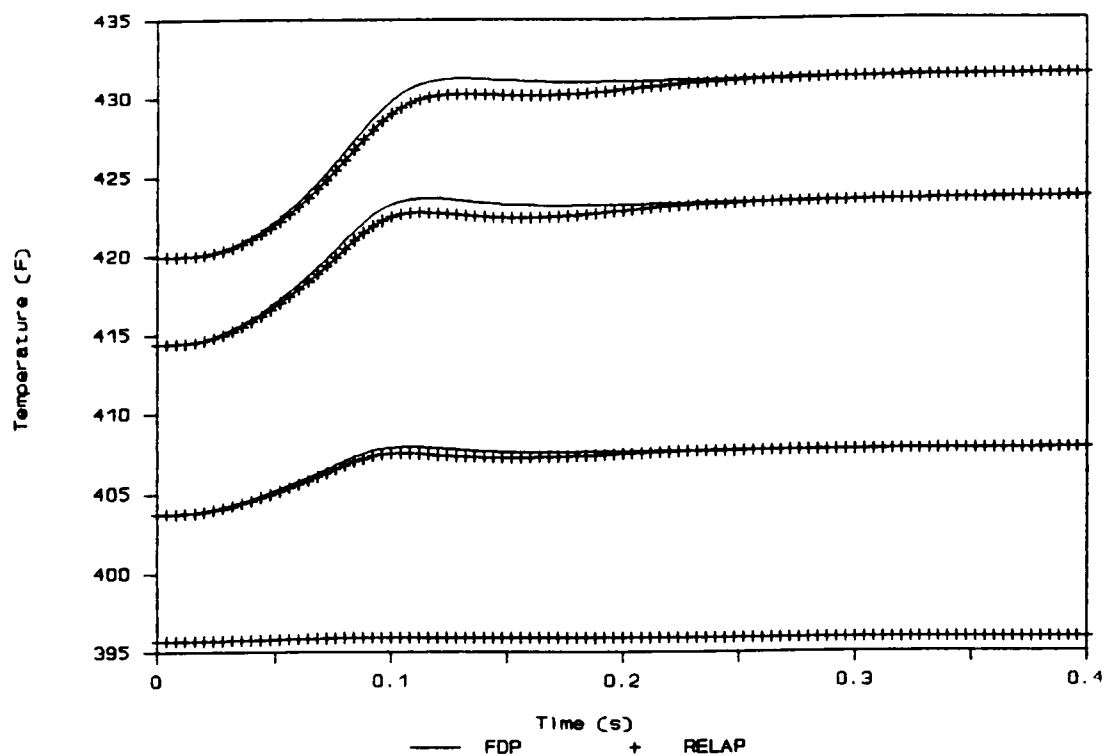


Figure D25 : Coolant temperature comparisons with RELAP5/Mod2 for 1, 4, 7, 10th nodes for subcooled boiling.

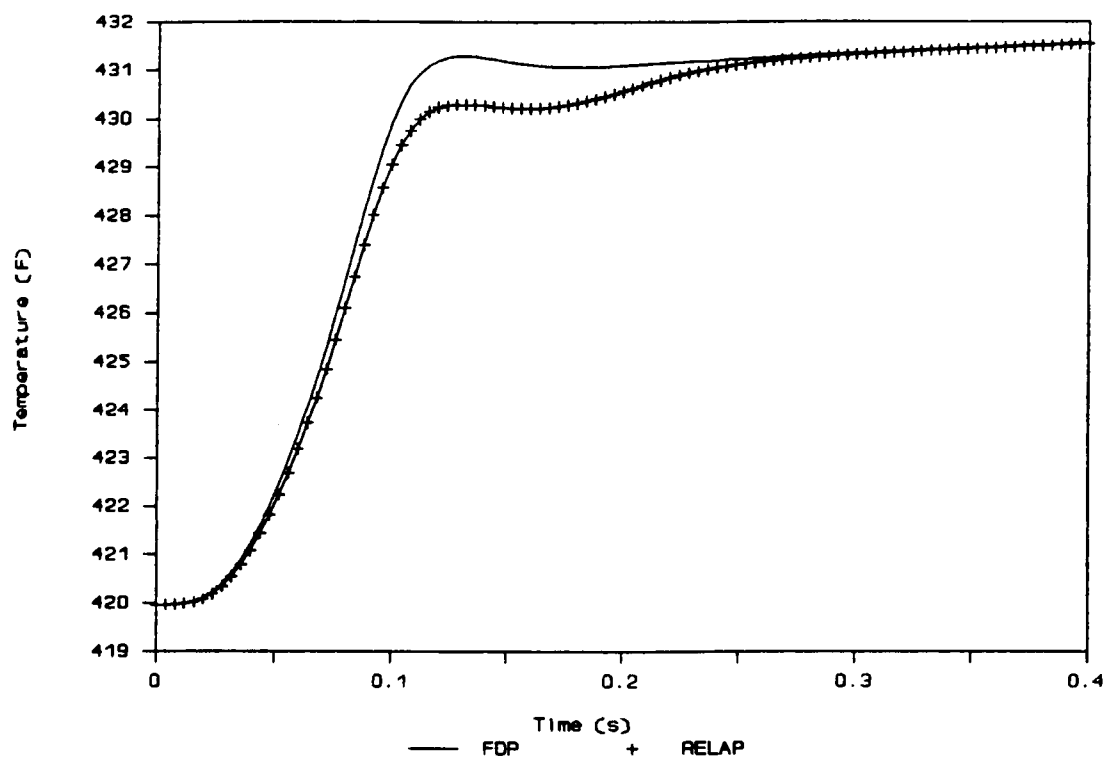


Figure D26 : Outlet node bulk coolant temperature comparisons with RELAP5/Mod2 for subcooled boiling flow.

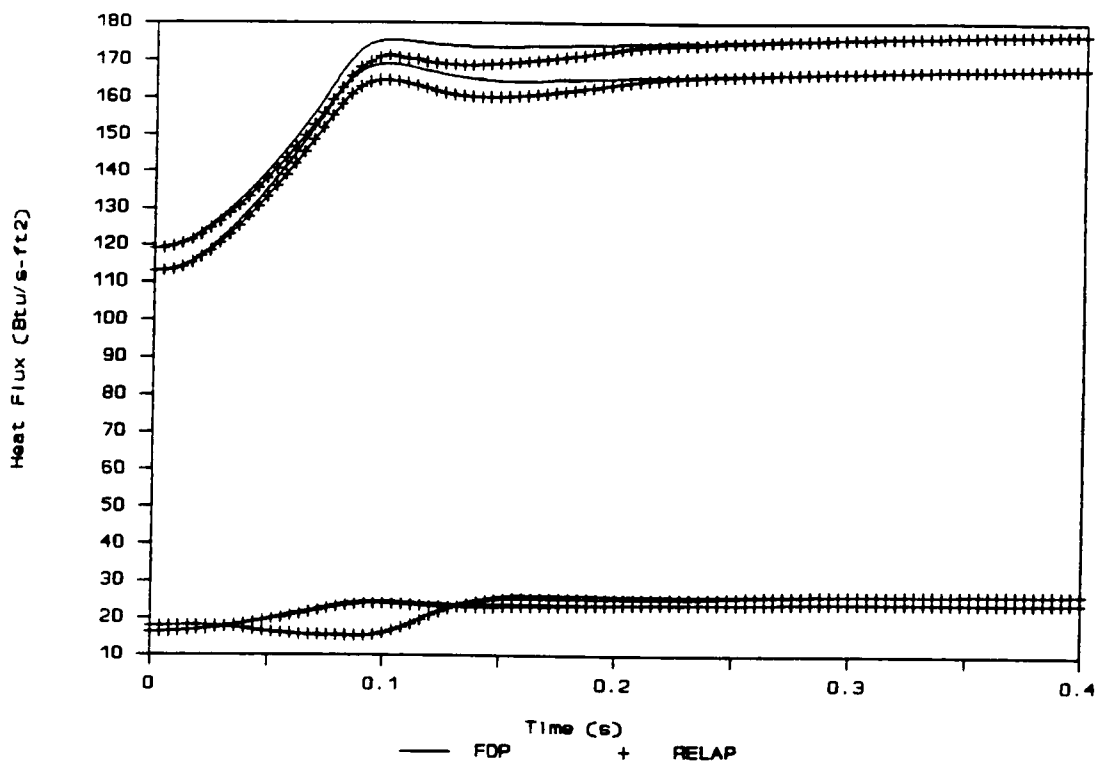


Figure D27 : Surface heat flux comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for subcooled boiling.

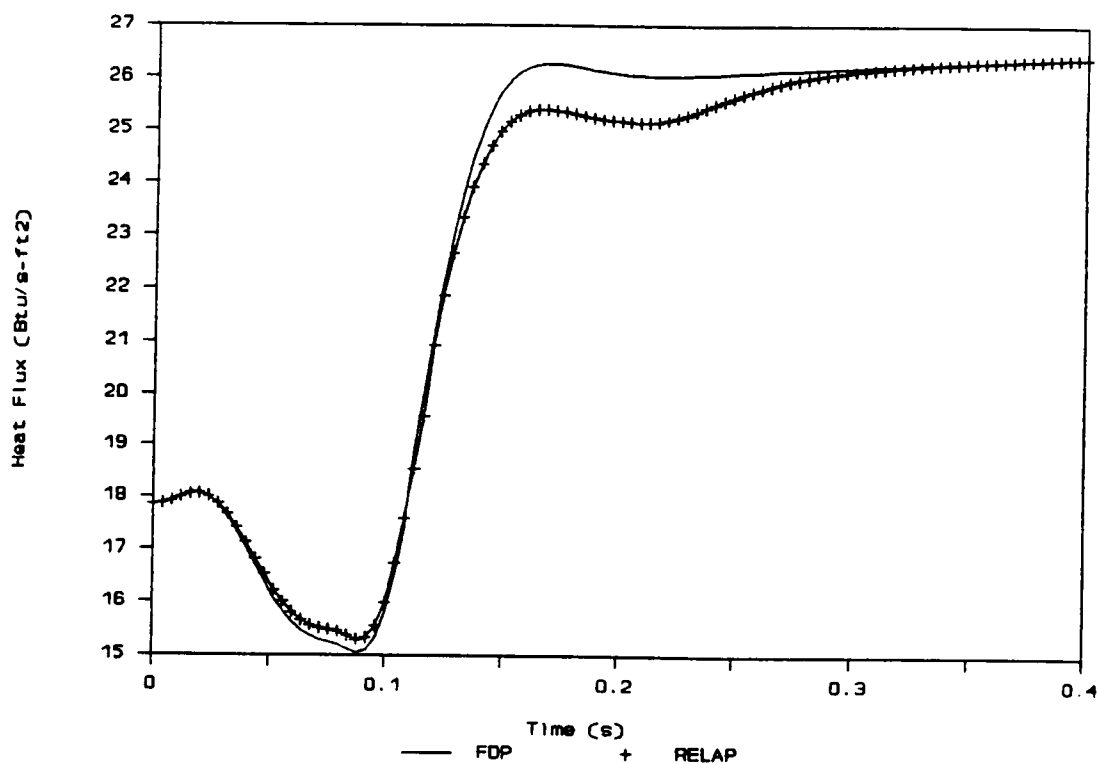


Figure D28 : Outlet node surface heat flux comparisons with RELAP5/Mod2 for subcooled boiling flow.

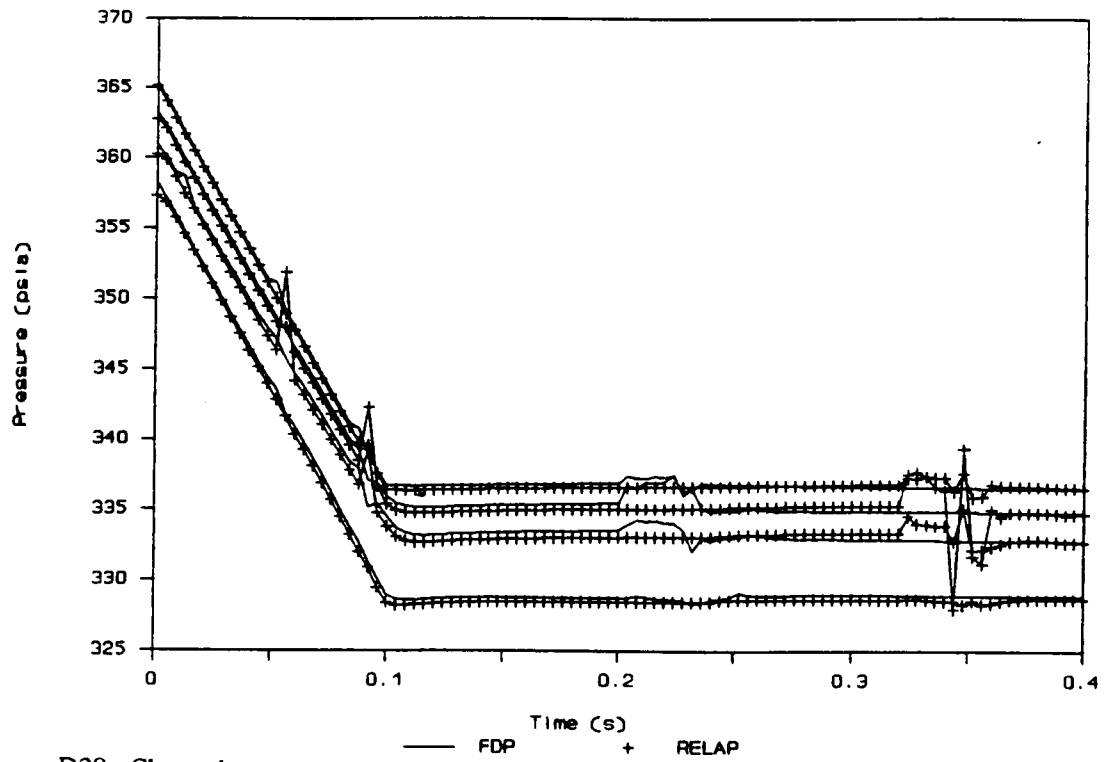


Figure D29 : Channel pressure comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for bulk boiling.

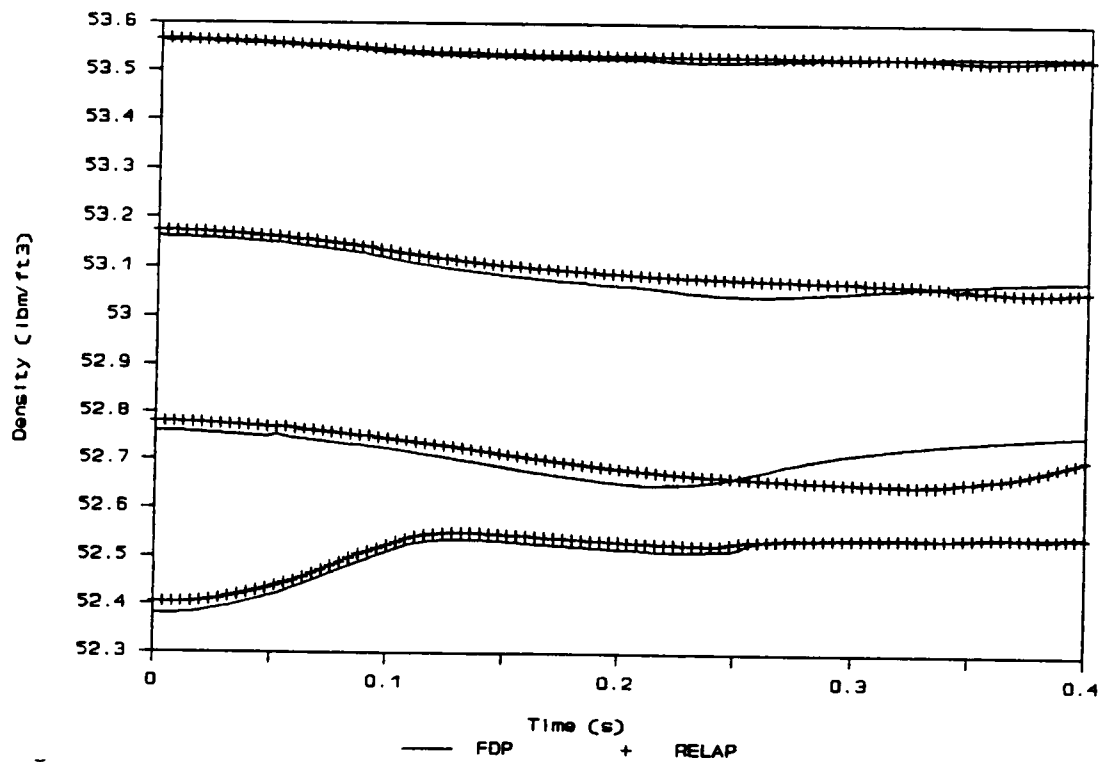


Figure D30 : Liquid density comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for bulk boiling.

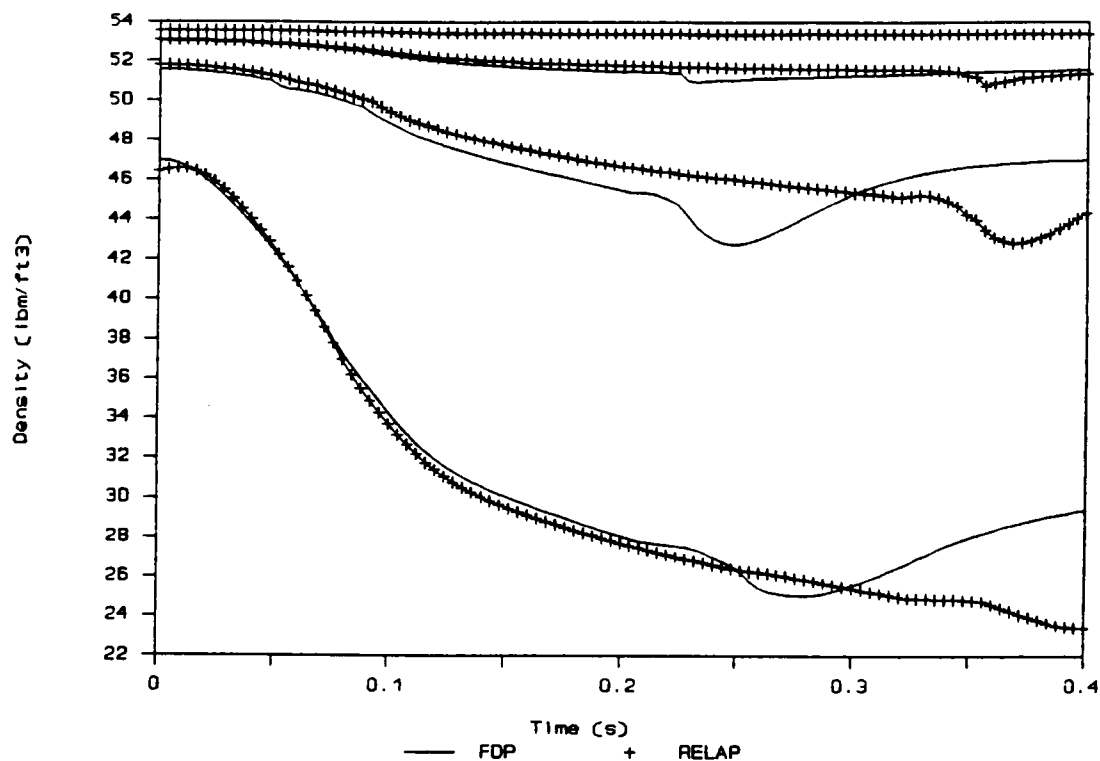


Figure D31 : Mixture density comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for bulk boiling.

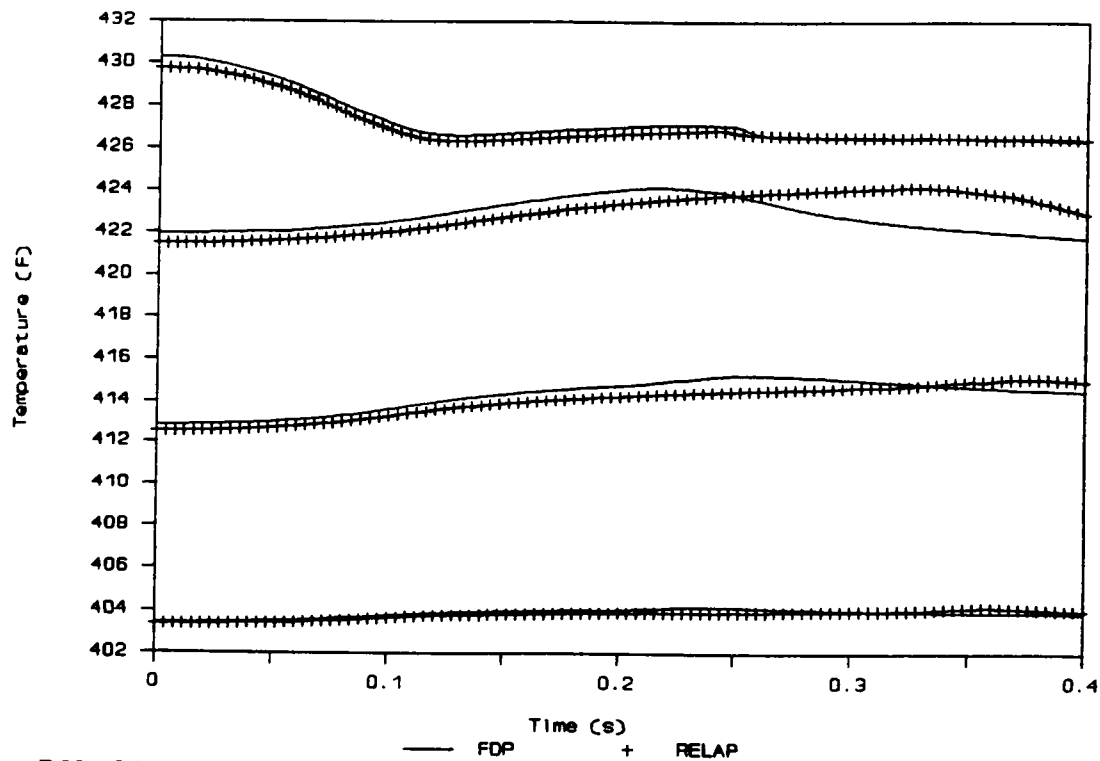


Figure D32 : Liquid temperature comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for bulk boiling.

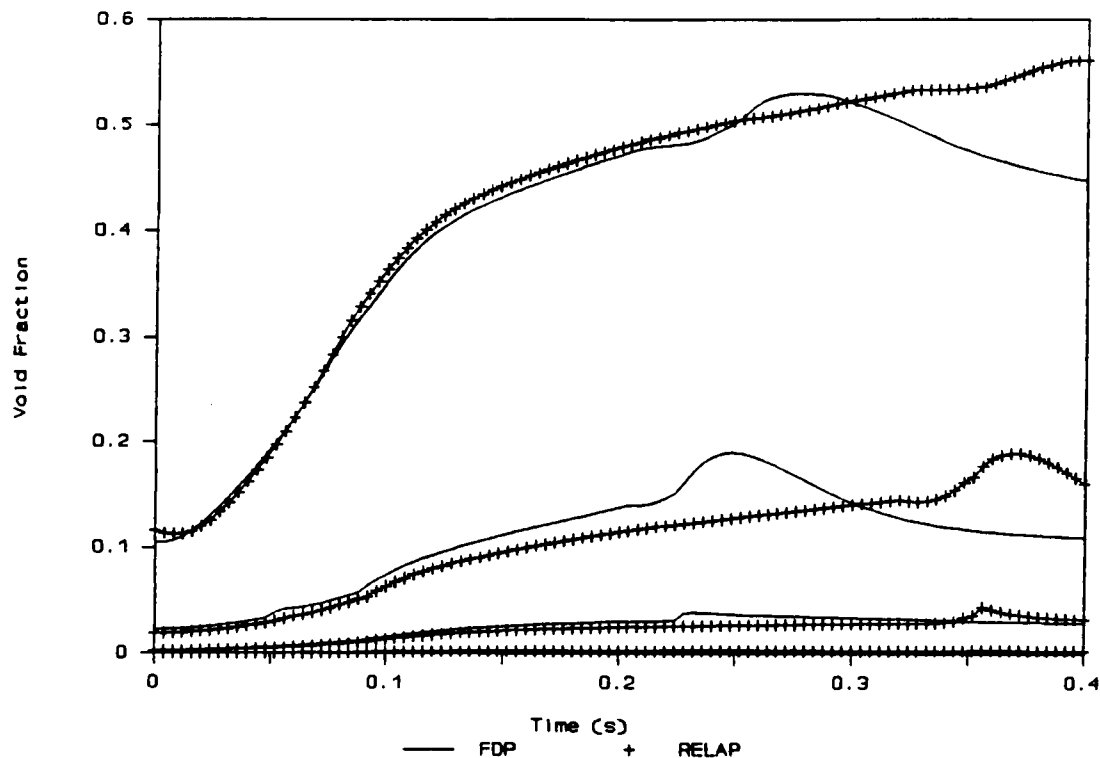


Figure D33 : Void fraction comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for bulk boiling.

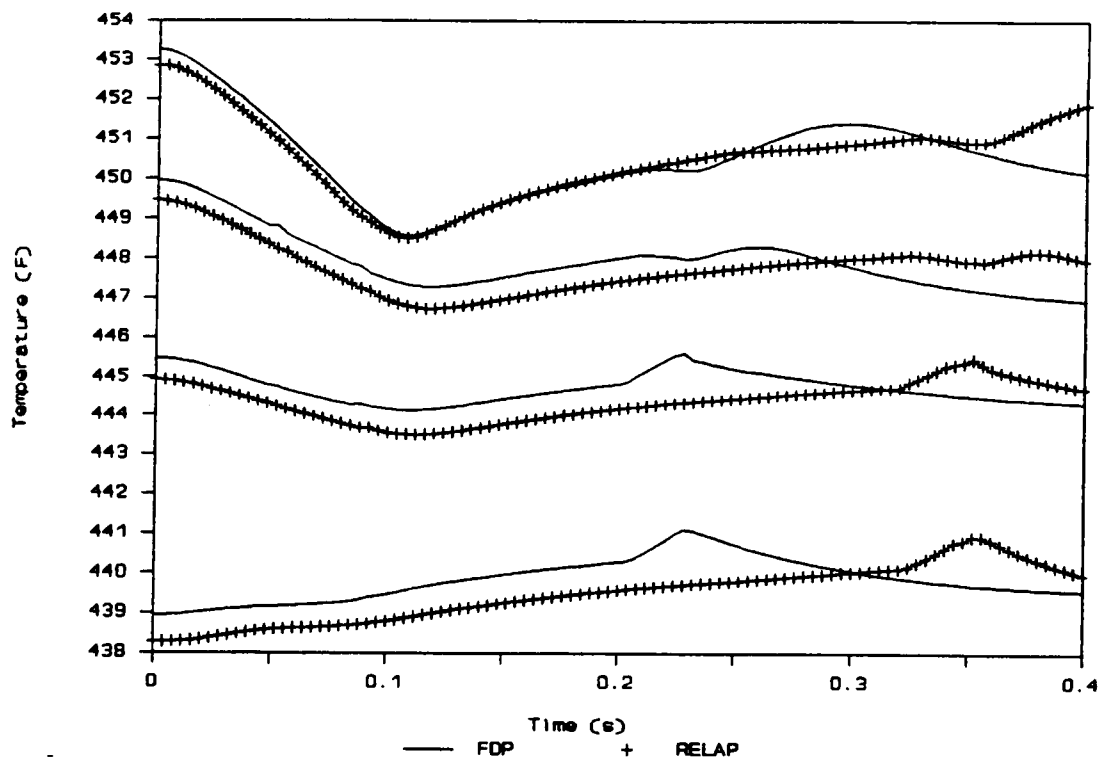


Figure D34 : Fuel temperature comparisons with RELAP5/Mod2 for 1, 4, 7, and 10th nodes for bulk boiling.

APPENDIX E

INITIAL STEADY-STATE CONDITIONS

The typical initial steady-state values for some key variables in each operating state are presented in Figures E1 to E17. In these figures and subsequent sections, the following terminology is used extensively:

- B1FF; Beginning of Fuel Cycle, Mode 1, Full-Power, Full-Flow;
- E1FF; End of Fuel Cycle, Mode 1, Full-Power, Full-Flow;
- B1LF; Beginning of Fuel Cycle, Mode 1, Low-Power, Full-Flow;
- E1LF; End of Fuel Cycle, Mode 1, Low-Power, Full-Flow;
- B1LL; Beginning of Fuel Cycle, Mode 1, Low-Power, Low-Flow;
- E1LL; End of Fuel Cycle, Mode 1, Low-Power, Low-Flow;
- B2; Beginning of Fuel Cycle, Mode 2;
- E2; End of Fuel Cycle, Mode 2;

The initial steady-state pressure drops in a nominal channel are presented in Figures E1 and E2 for full-flow and low-flow conditions, respectively. The pressures in inner and outer fuel elements are slightly different; however, the difference is not observable in these figures. The velocities, on the other hand, are uniquely different for inner and outer assembly, at the beginning and at the end of fuel cycle, for full-power and low-power conditions, for full-flow and low-flow situations. The nominal initial velocities for all eight cases are presented in Figures E3 to E10. The nominal channel initial bulk coolant temperatures are presented in Figures E11 to E13. Similar to the case with pressures, the coolant temperatures are slightly different for inner and outer fuel elements. And finally, the nominal plate initial average temperatures are presented in Figures E14 to E17 for all eight cases, and for inner and outer fuel elements. It should be noted that the hot plate, wide and narrow channel results are somewhat different from those presented above.

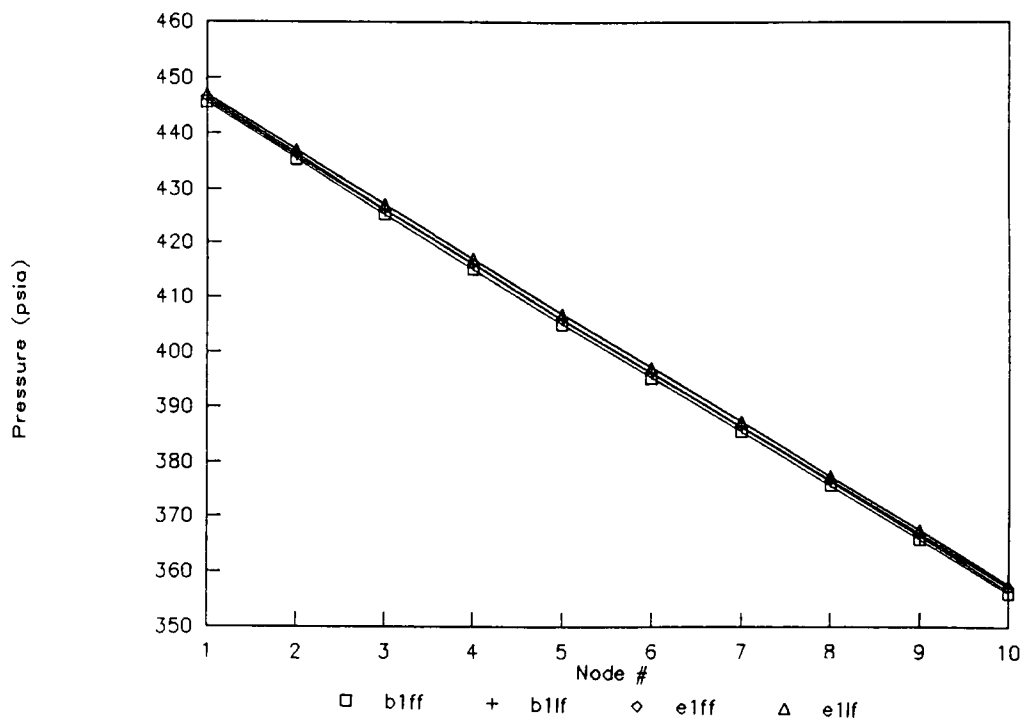


Figure E1 : The typical initial steady-state pressures in a nominal-channel for full-flow situations.

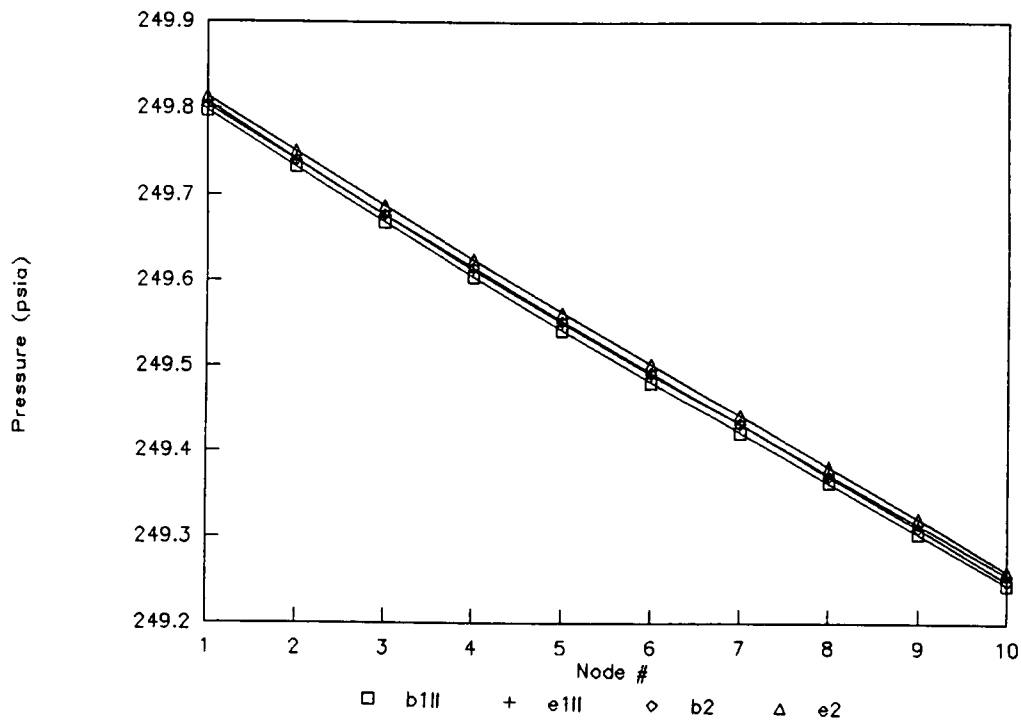


Figure E2 : The typical initial steady-state pressures in a nominal-channel for low-flow situations.

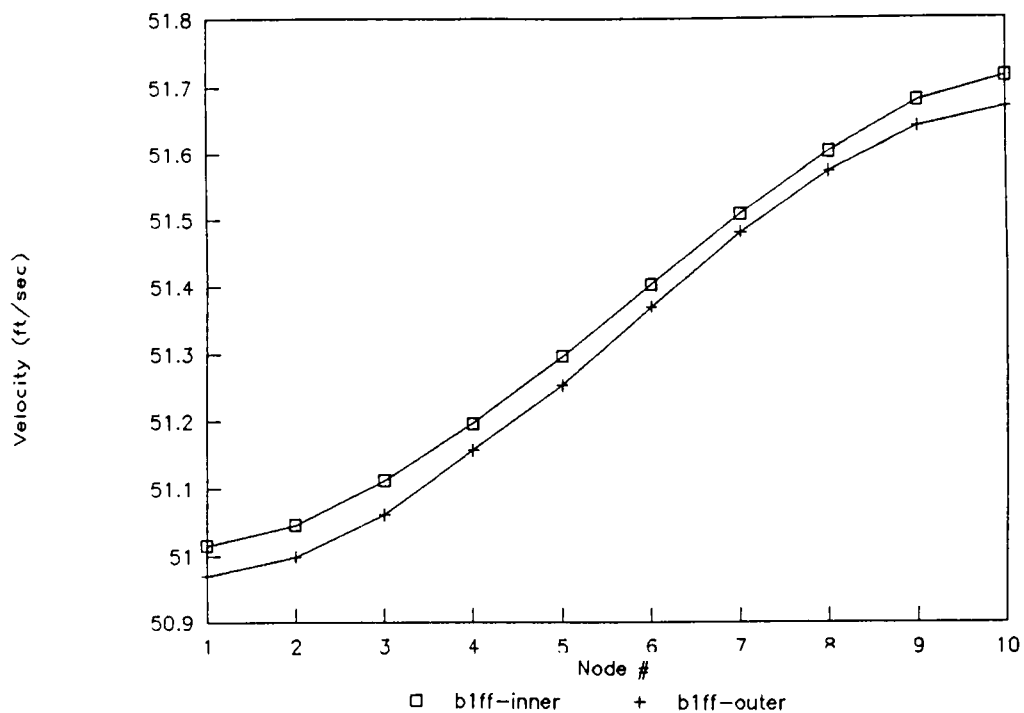


Figure E3 : Nominal initial velocities for the beginning of cycle, Mode 1, full-power transients.

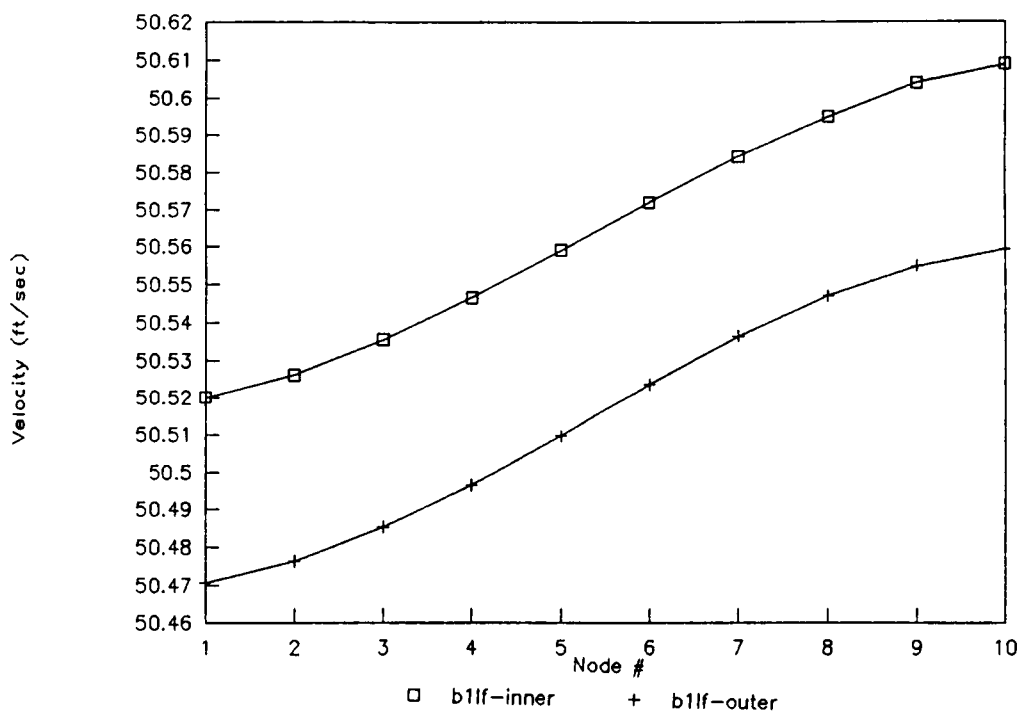


Figure E4 : Nominal initial velocities for the beginning of cycle, Mode 1, low-power, full-flow transients.

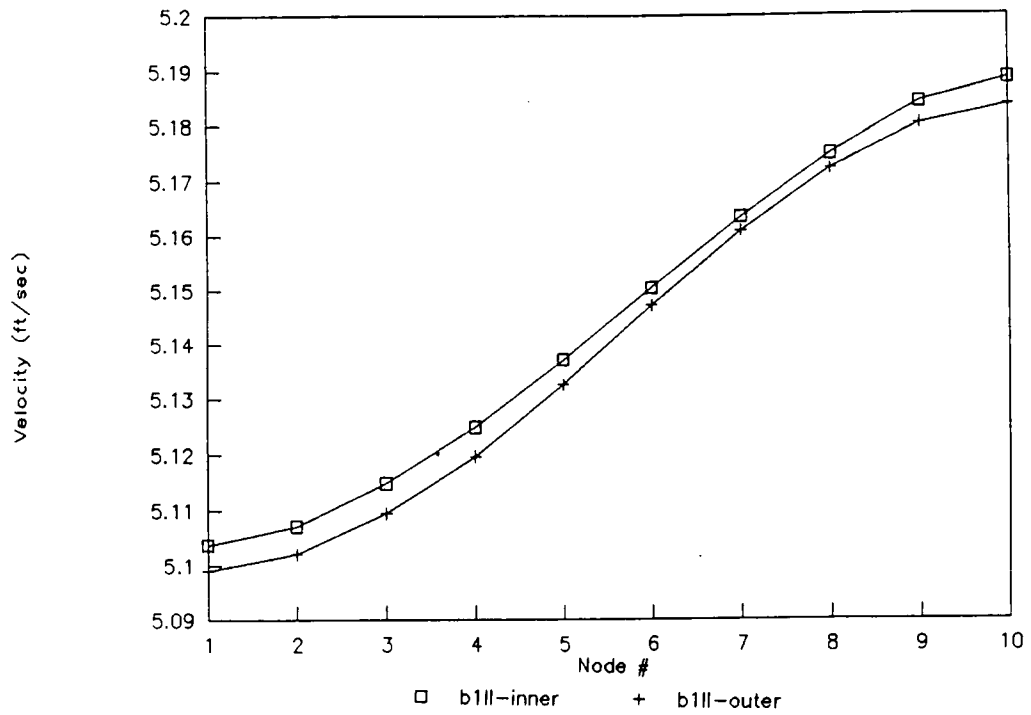


Figure E5 : Nominal initial velocities for the beginning of cycle, Mode 1, low-power, low-flow transients.

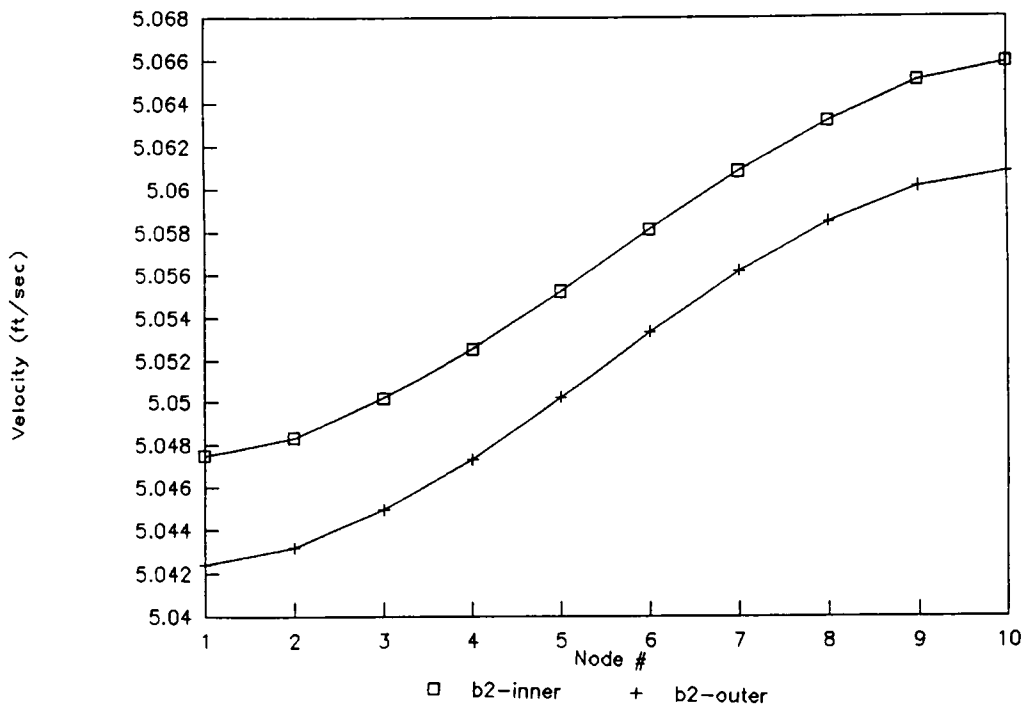


Figure E6 : Nominal initial velocities for the beginning of cycle, Mode 2, transients.

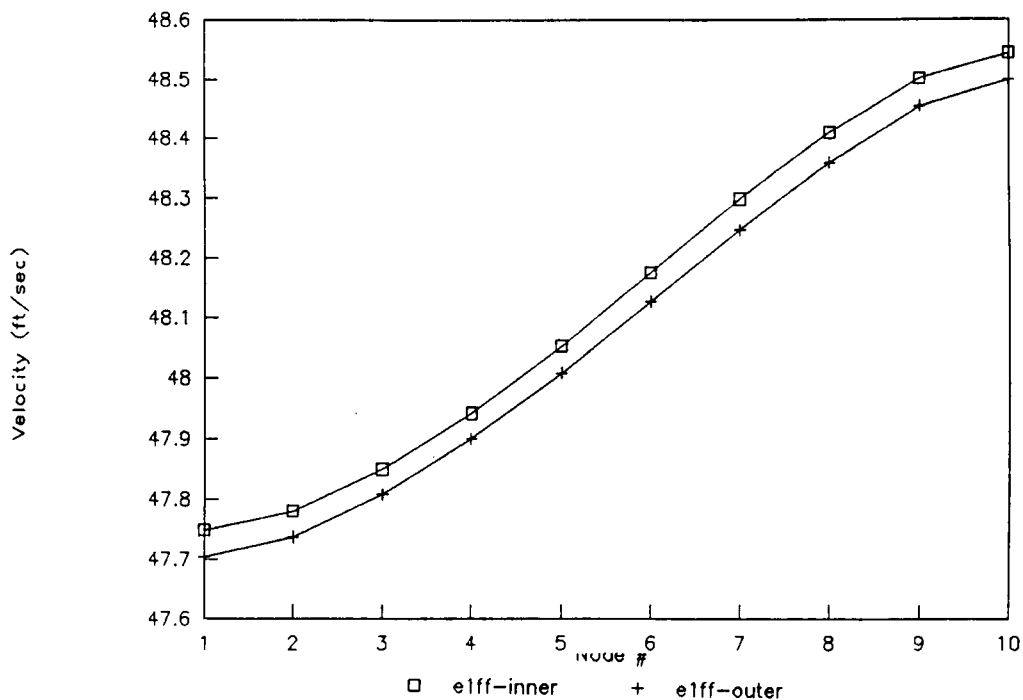


Figure E7 : Nominal initial velocities for the end of cycle, Mode 1, full-power transients.

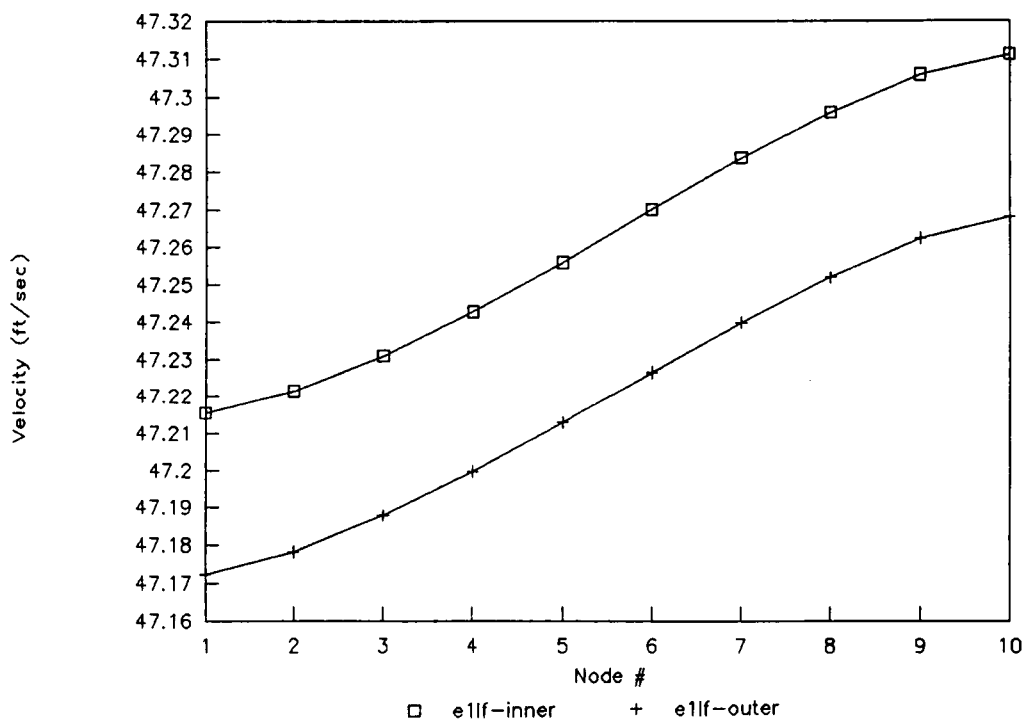


Figure E8 : Nominal initial velocities for the end of cycle, Mode 1, low-power, full-flow transients.

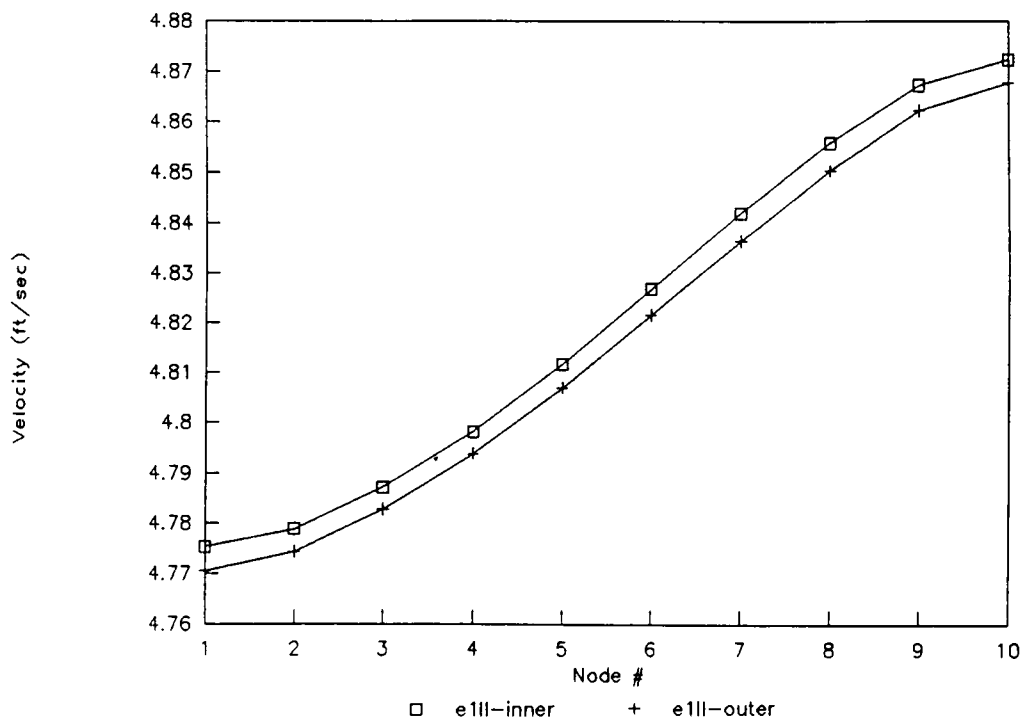


Figure E9 : Nominal initial velocities for the end of cycle, Mode 1, low-power, low-flow transients.

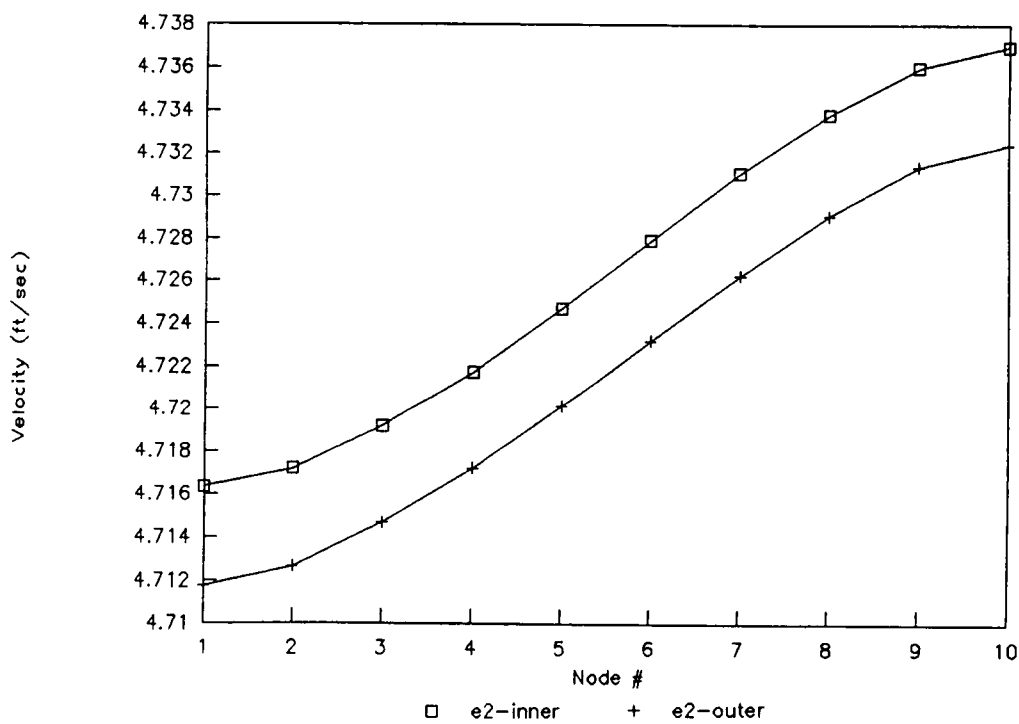


Figure E10 : Nominal initial velocities for the end of cycle, Mode 2 transients.

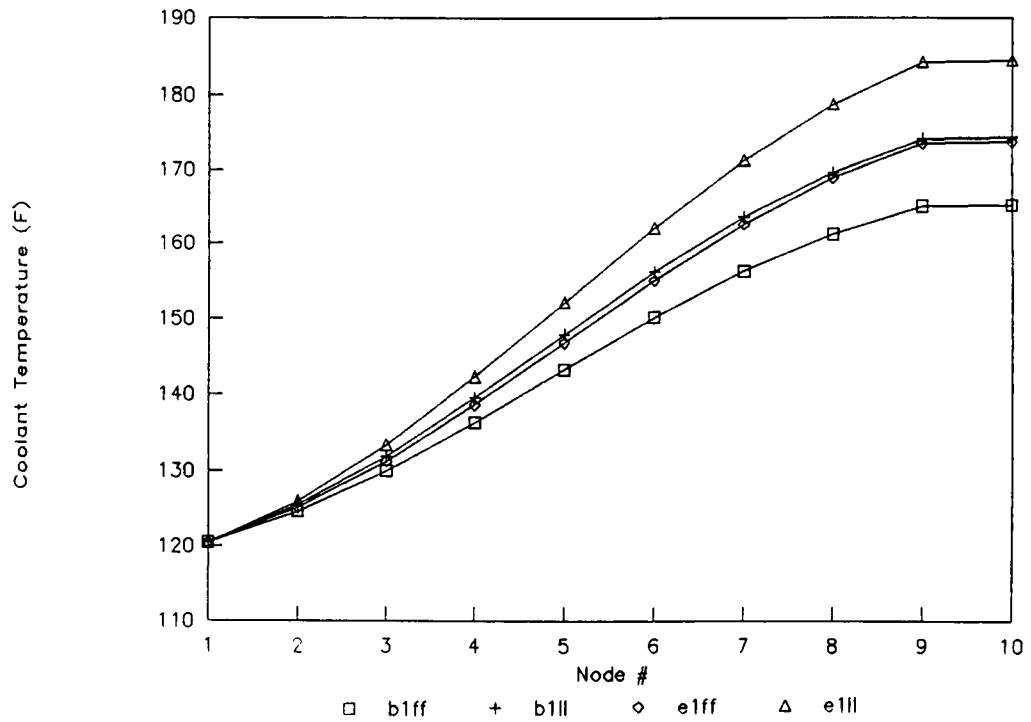


Figure E11 : Typical initial steady-state bulk coolant temperatures.

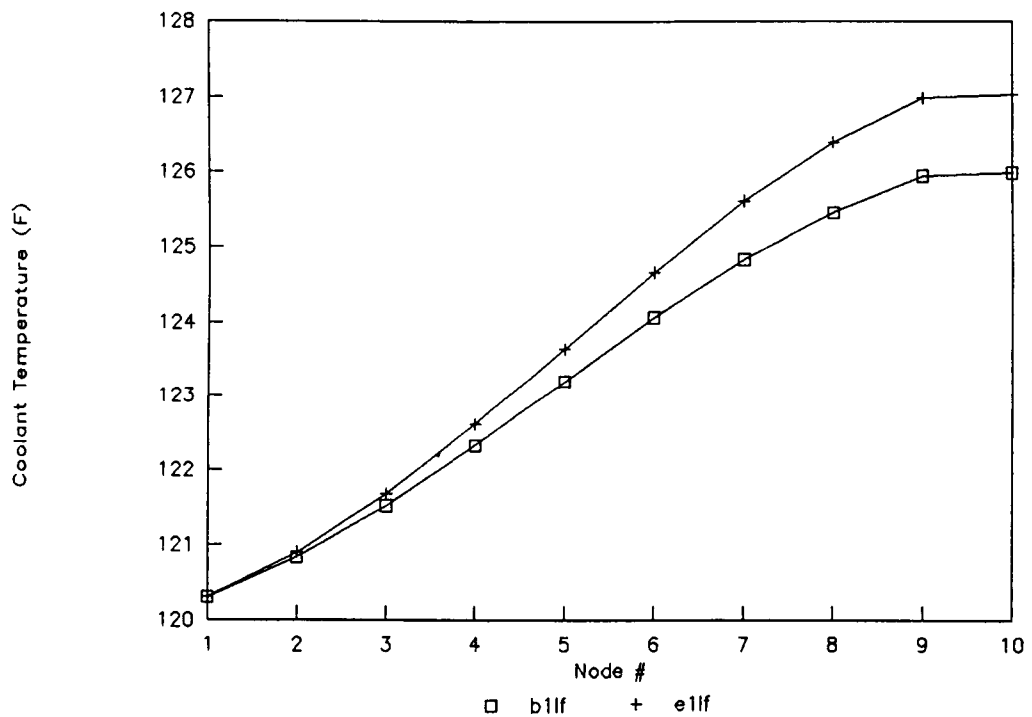


Figure E12 : Typical initial steady-state bulk coolant temperatures for Mode 1, low-power, full-flow transients.

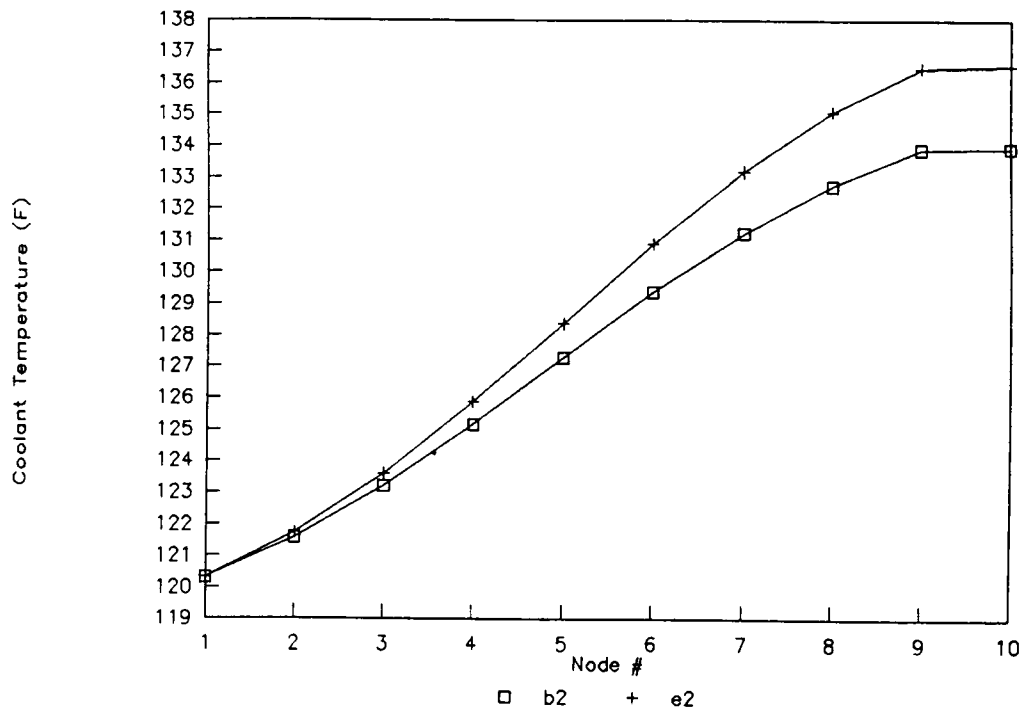


Figure E13 : Typical initial steady-state bulk coolant temperatures for Mode 2 transients.

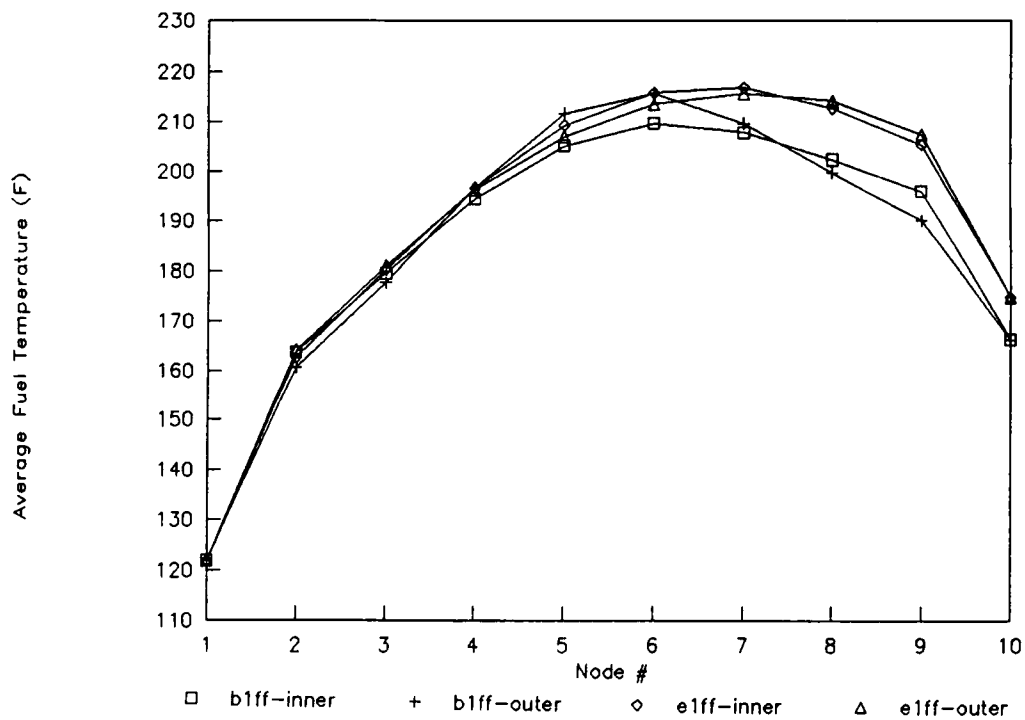


Figure E14 : Nominal-plate initial average fuel temperatures for Mode 1, full-power, full-flow transients.

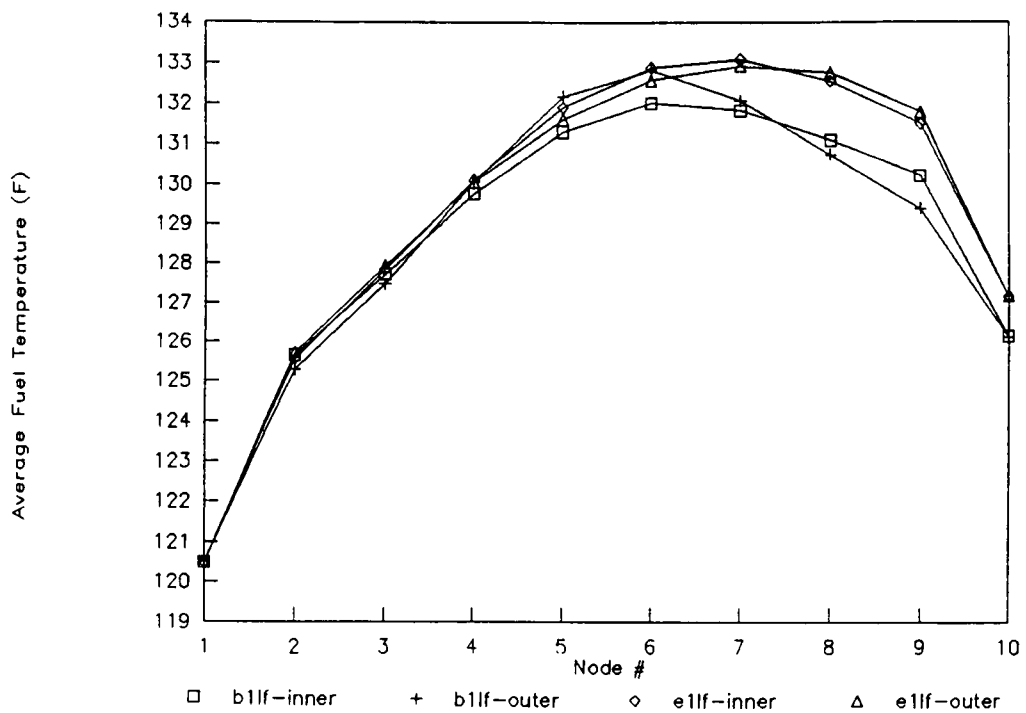


Figure E15 : Nominal-plate initial average fuel temperatures for Mode 1, low-power, full-flow transients.

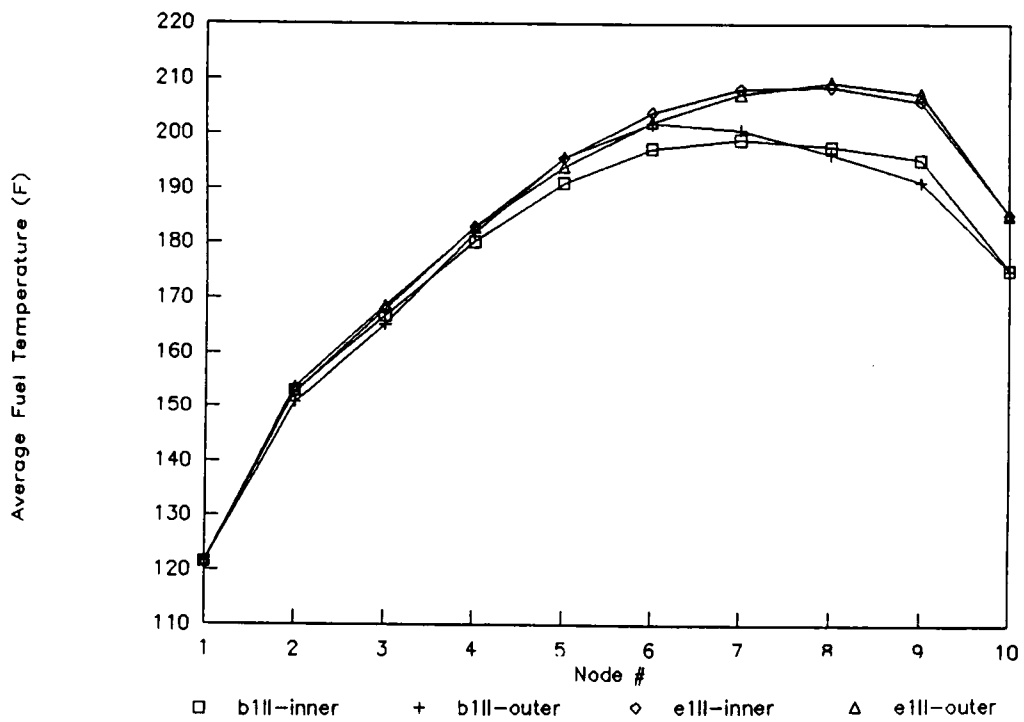


Figure E16 : Nominal-plate initial average fuel temperatures for Mode 1, low-power, low-flow transients.

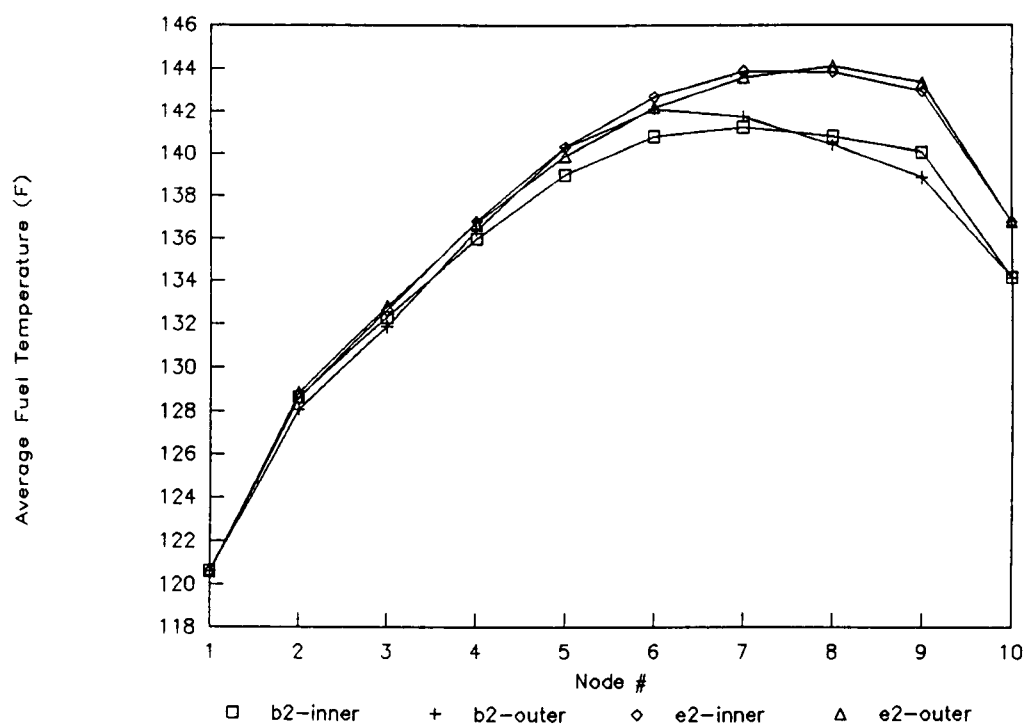


Figure E17 : Nominal-plate initial average fuel temperatures for Mode 2 transients.

APPENDIX F

COMPLETE RESULTS FOR OPTIMUM VOID ACCIDENTS

In this appendix, the complete results are presented for the optimum void accident in Mode 1, at full-power, full-flow for both beginning and end of cycle conditions. The figures provided here basically support the discussions in Section 6.2.4. Both inner and outer element results are presented for both nominal channel/plate and hot channel/plate. Unlike the plots presented in Section 6.2.3, a slightly different format is preferred here. Instead of providing the time dependent behavior of a state variable for inlet, fourth, seventh and outlet nodes all in one plot, nominal and hot-channel/plate values for both inner and outer elements are provided for each four nodes separately. This allows a more detailed display since scale for each four nodes are narrowed accordingly.

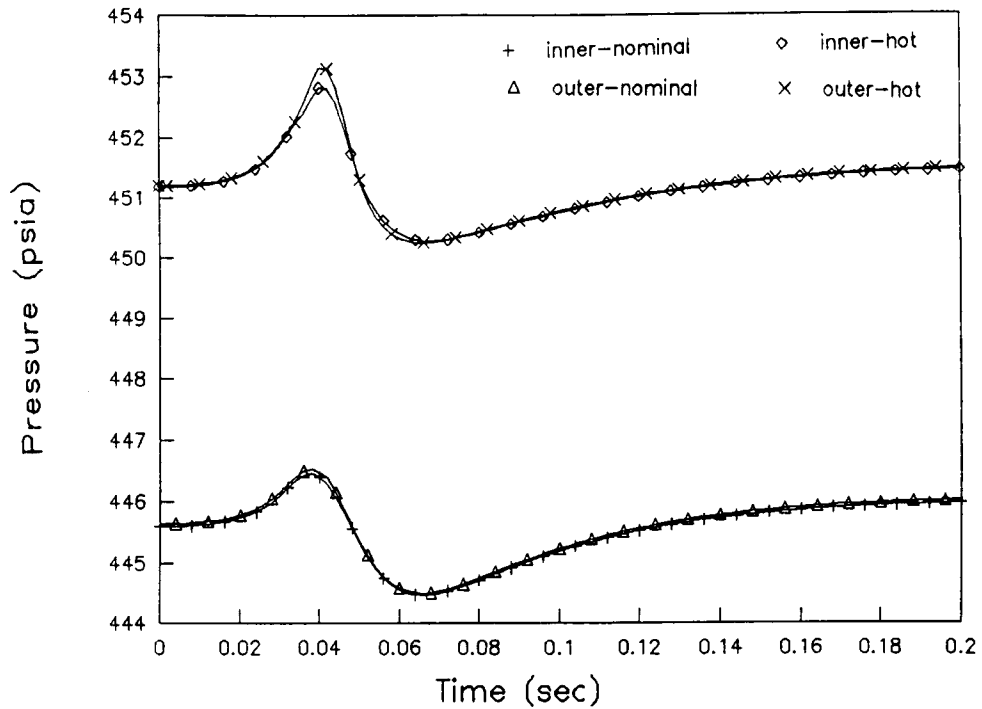


Figure F1 : Inlet node pressures during optimum void accident for BOC, Mode 1, full-power transient.

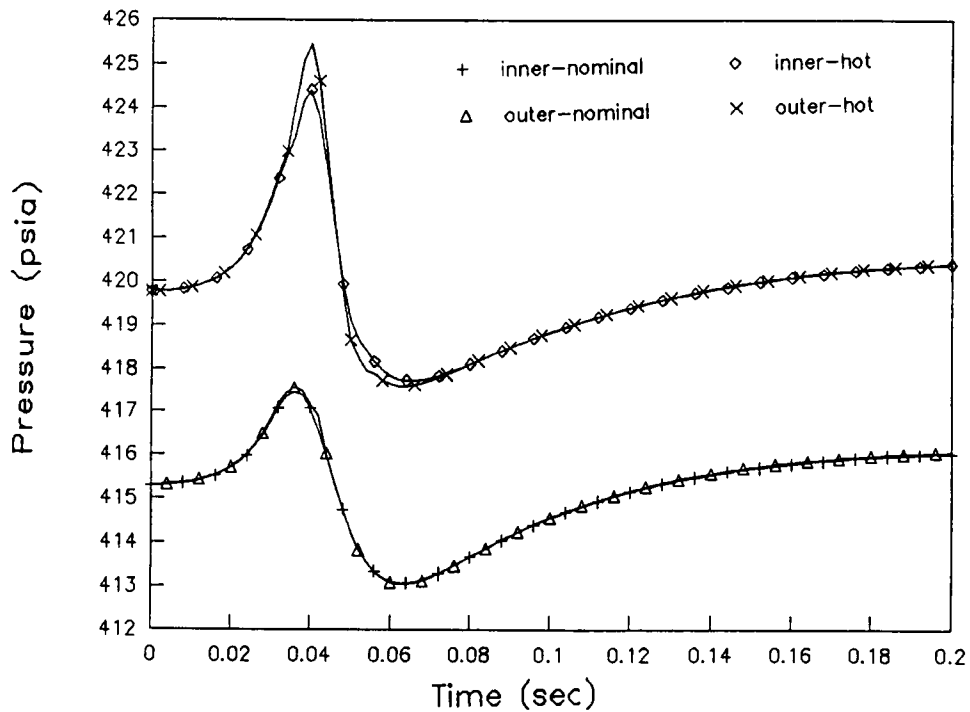


Figure F2 : Fourth node (out of ten) pressures during optimum void accident for BOC, Mode 1, full-power transient.

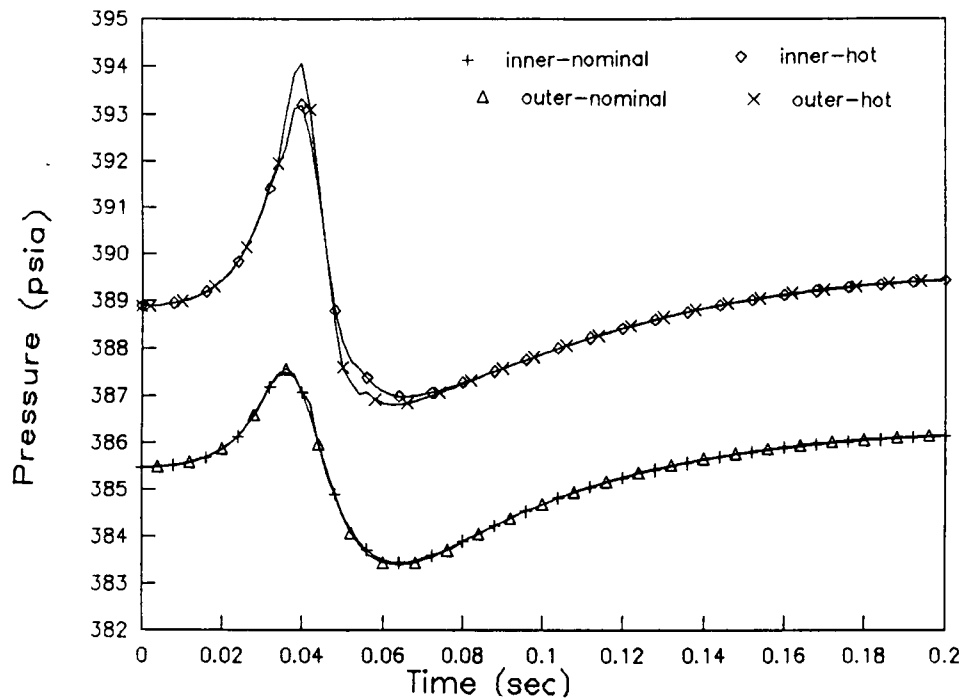


Figure F3 : Seventh node (out of ten) pressures during optimum void accident for BOC, Mode 1, full-power transient.

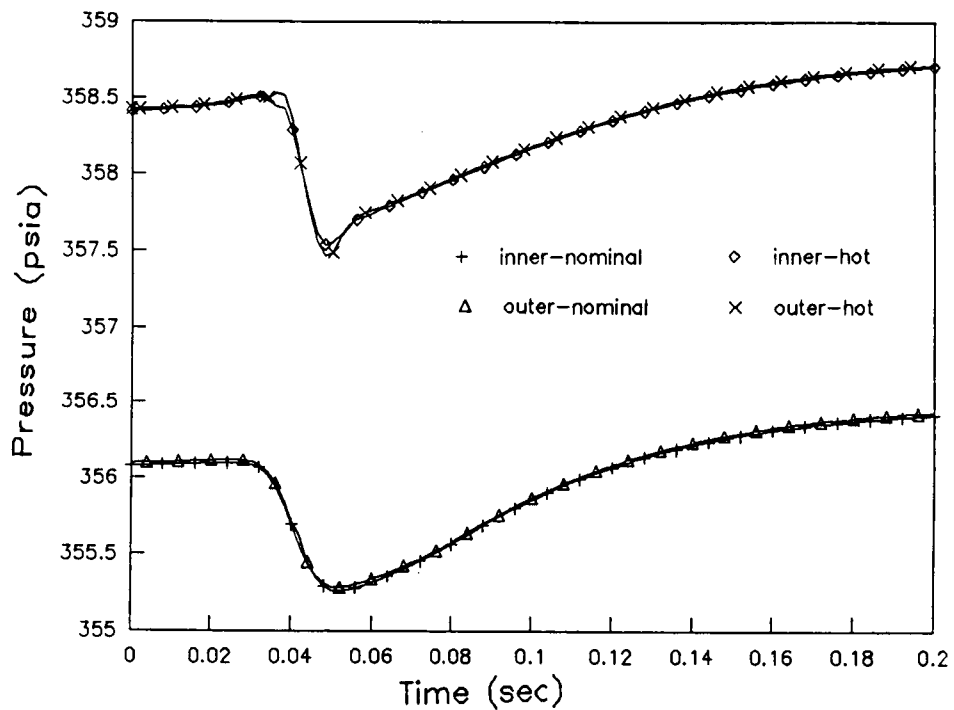


Figure F4 : Outlet node pressures during optimum void accident for BOC, Mode 1, full-power transient.

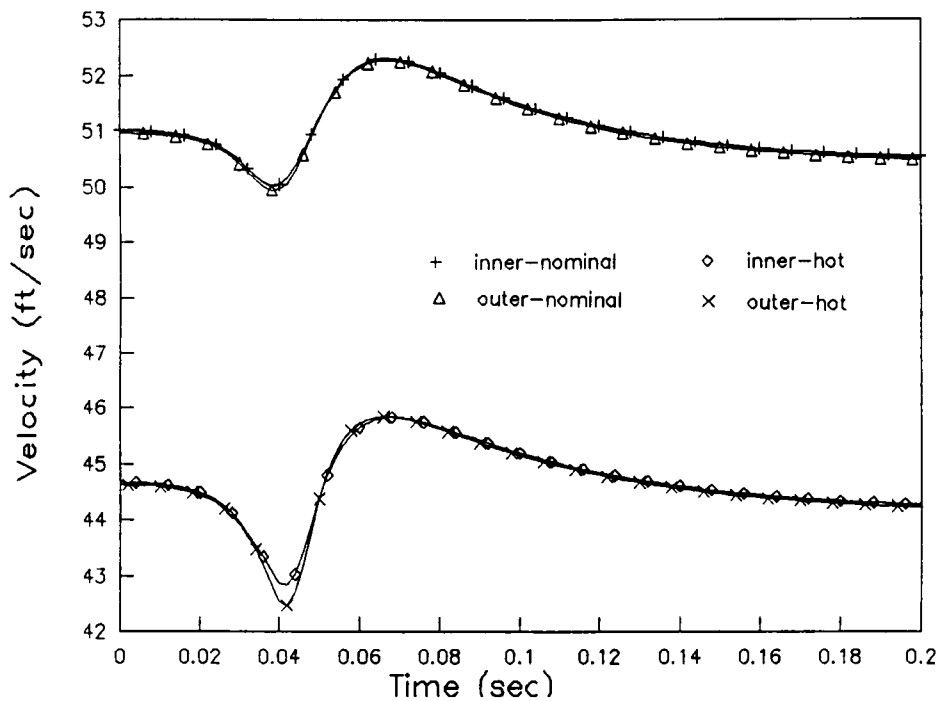


Figure F5 : Inlet node velocities during optimum void accident for BOC, Mode 1, full-power transient.

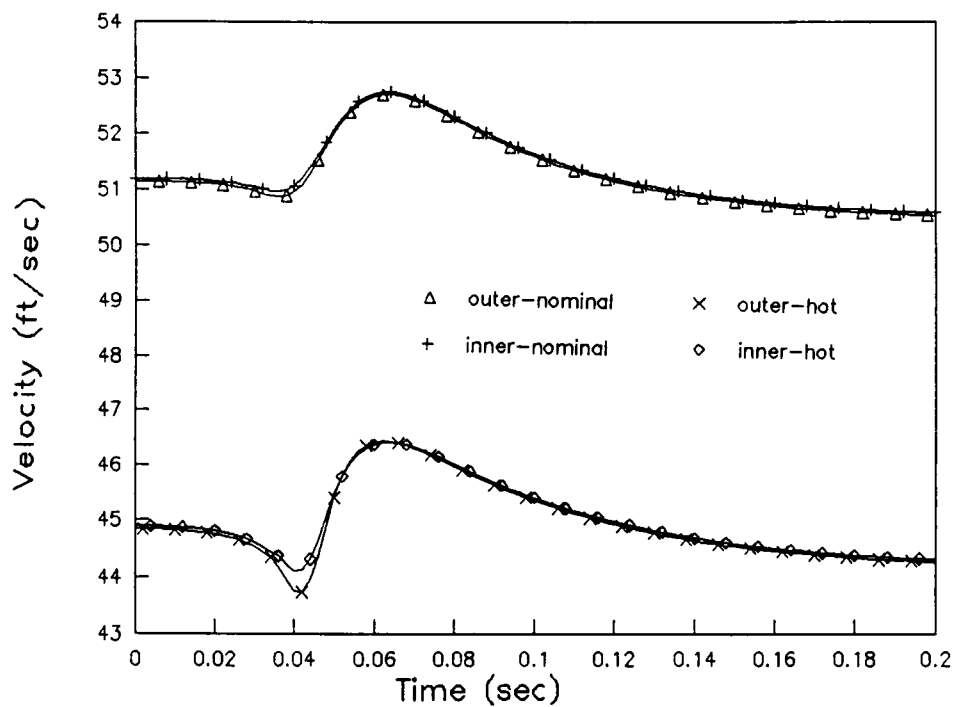


Figure F6 : Fourth node velocities during optimum void accident for BOC, Mode 1, full-power transient.

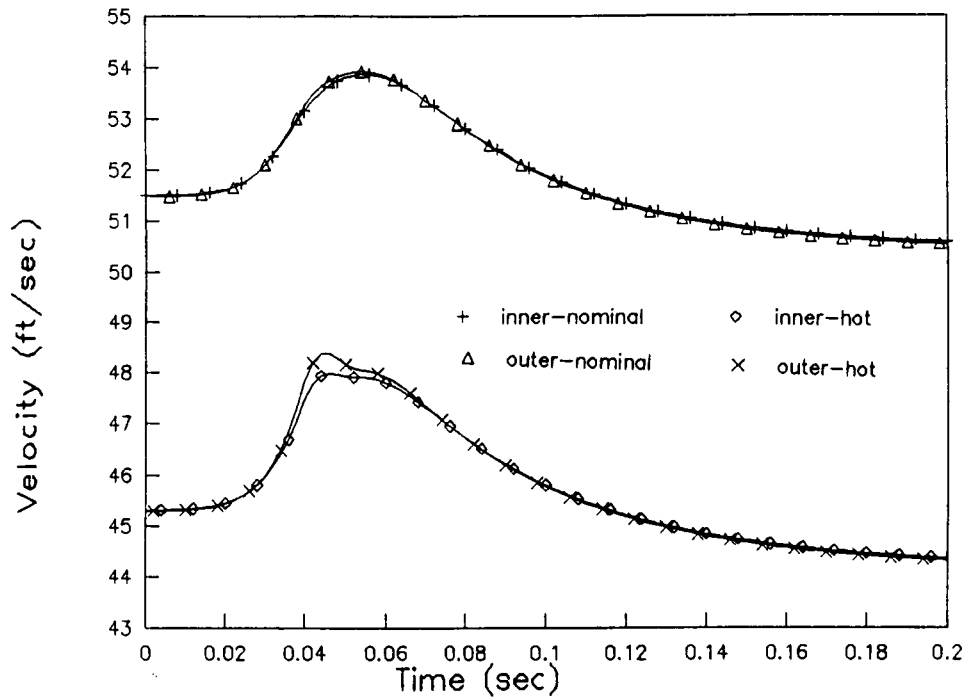


Figure F7 : Seventh node velocities during optimum void accident for BOC, Mode 1, full-power transient.

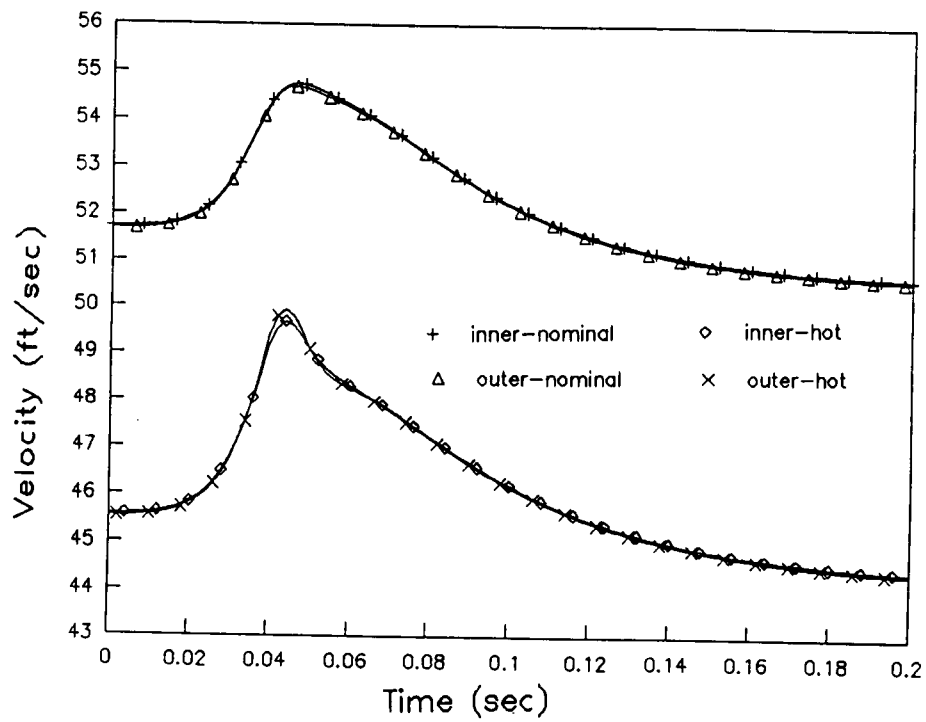


Figure F8 : Outlet node velocities during optimum void accident for BOC, Mode 1, full-power transient.

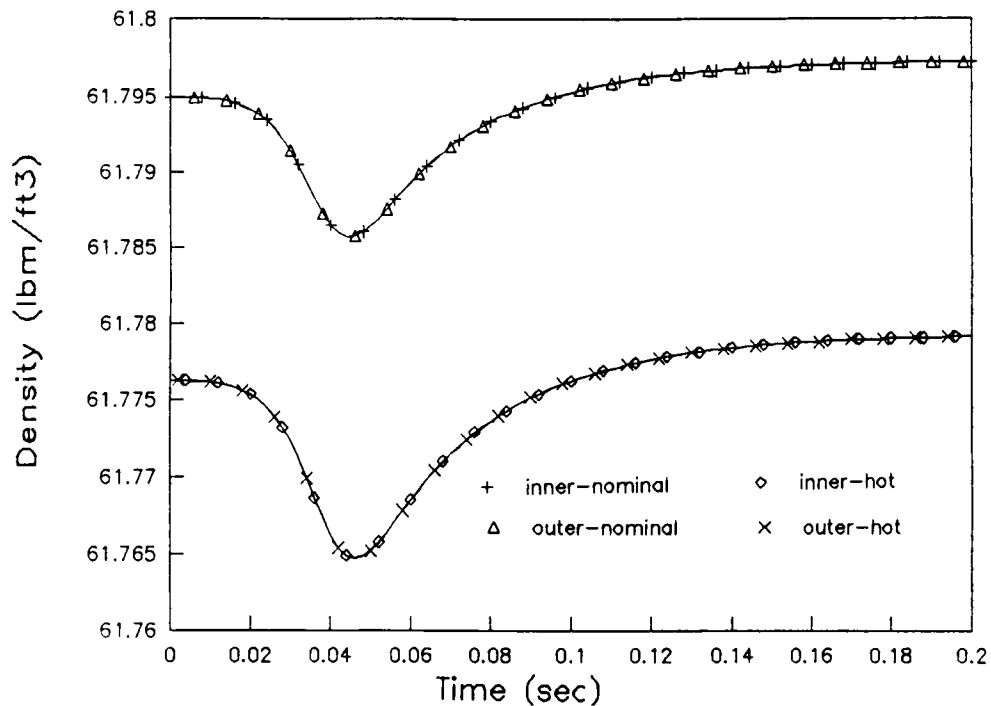


Figure F9 : Inlet node two-phase mixture density during optimum void accident for BOC, Mode 1, full-power transient.

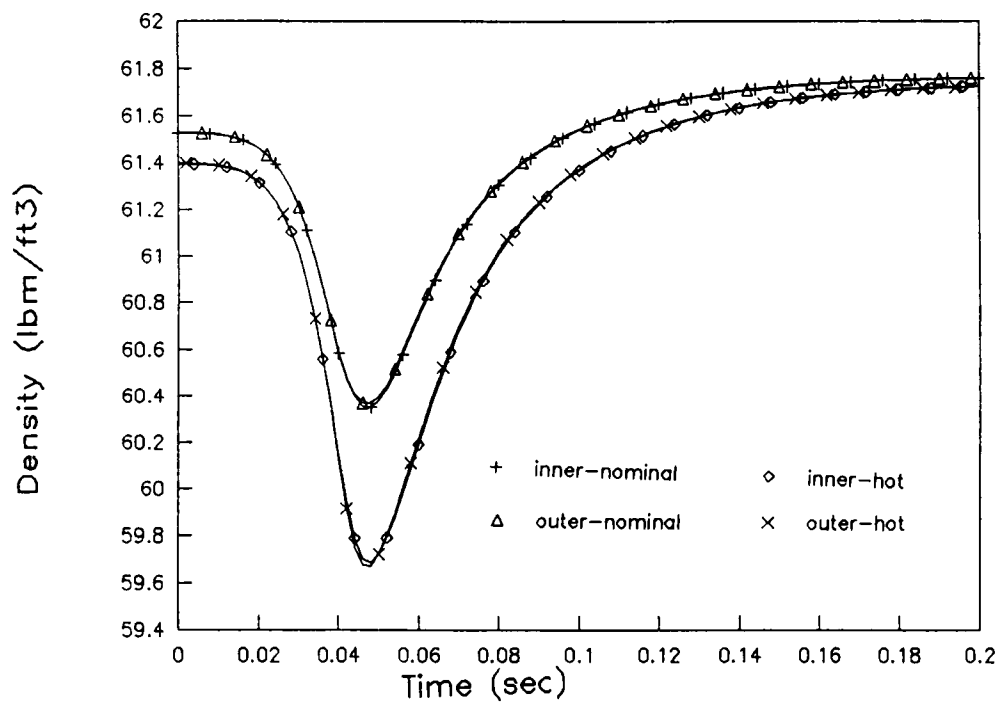


Figure F10 : Fourth node two-phase mixture density during optimum void accident for BOC, Mode 1, full-power transient.

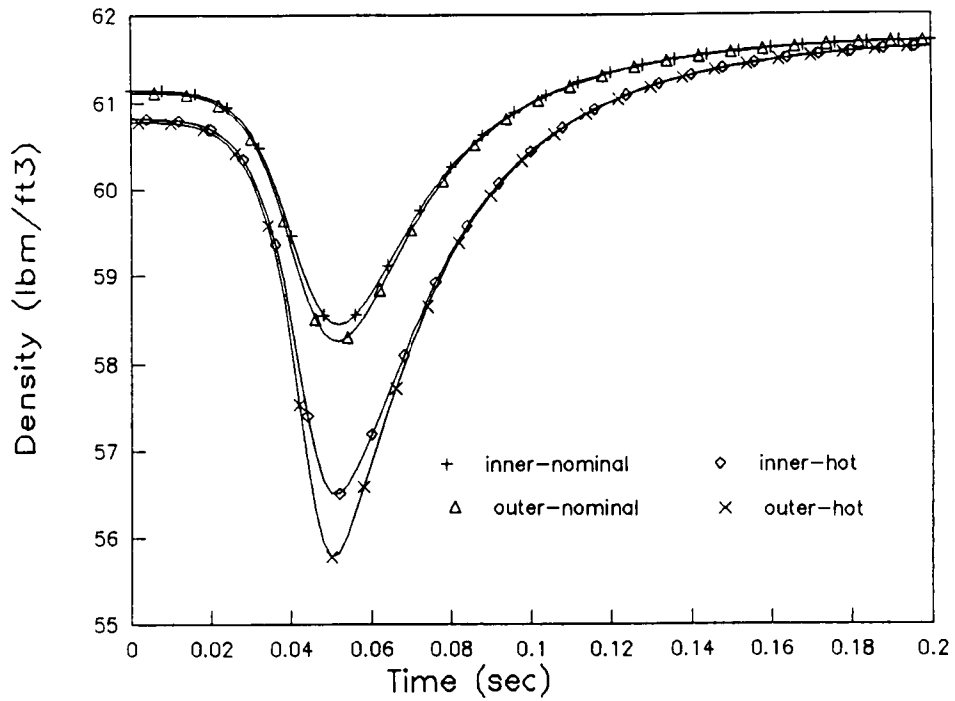


Figure F11 : Seventh node two-phase mixture density during optimum void accident for BOC, Mode 1, full-power transient.

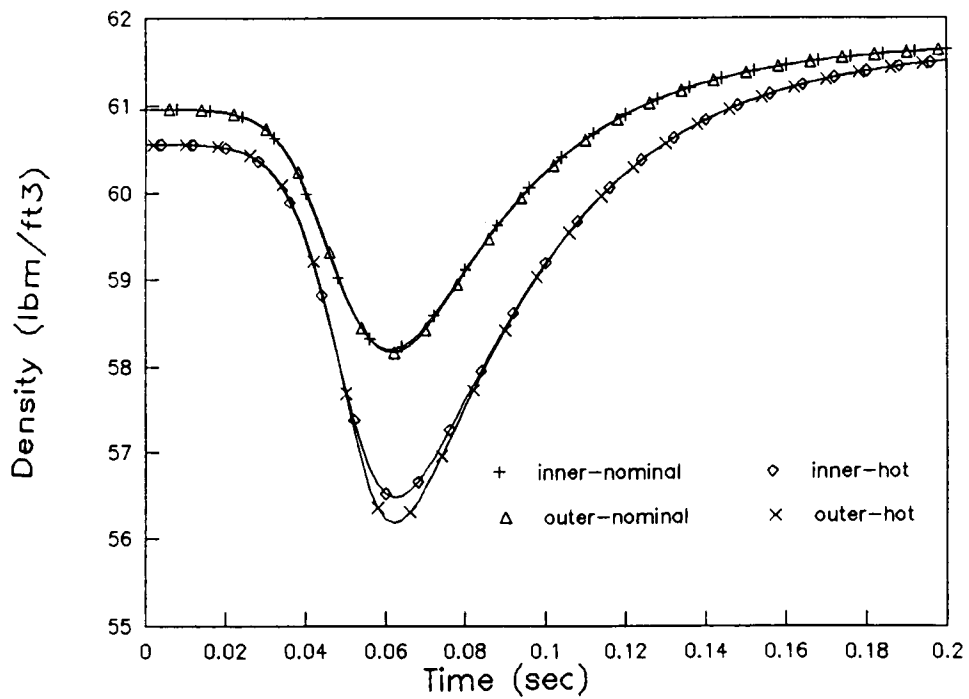


Figure F12 : Outlet node two-phase mixture density during optimum void accident for BOC, Mode 1, full-power transient.

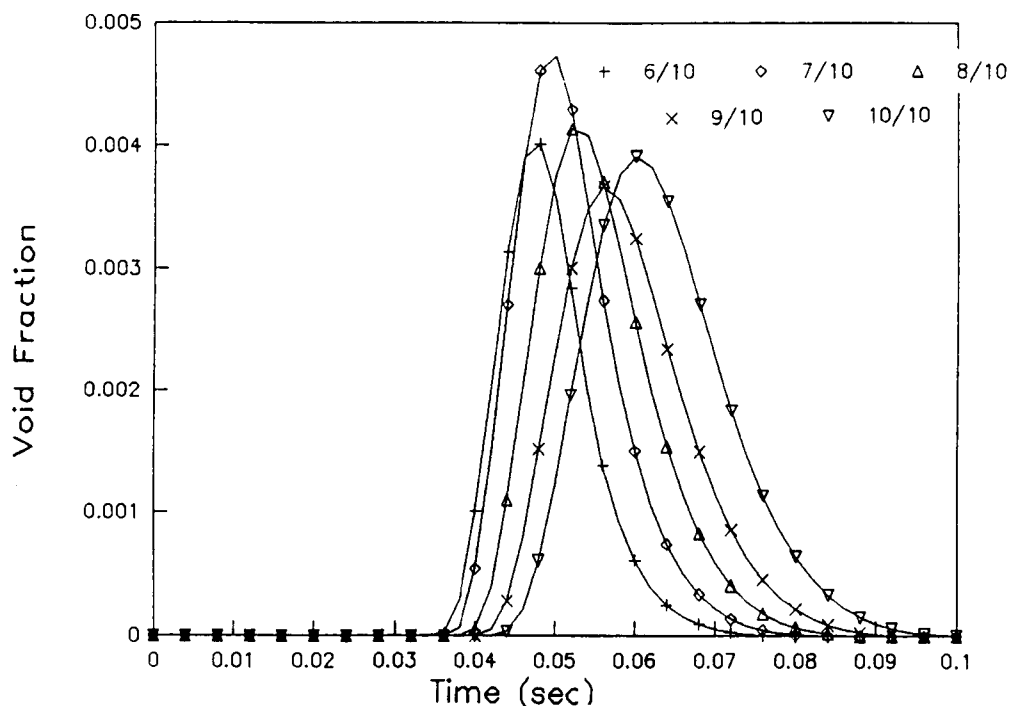


Figure F13 : Inner-element hot-channel void fractions for lower half of the core during optimum void accident for BOC, Mode 1, full-power transient.

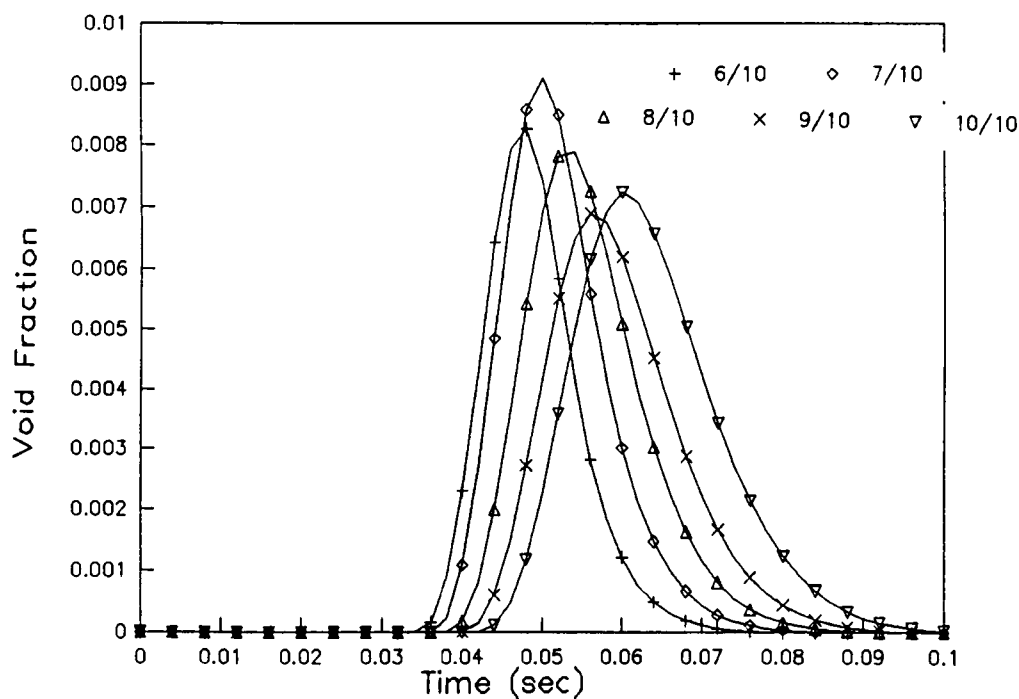


Figure F14 : Outer-element hot-channel void fractions for lower half of the core during optimum void accident for BOC, Mode 1, full-power transient.

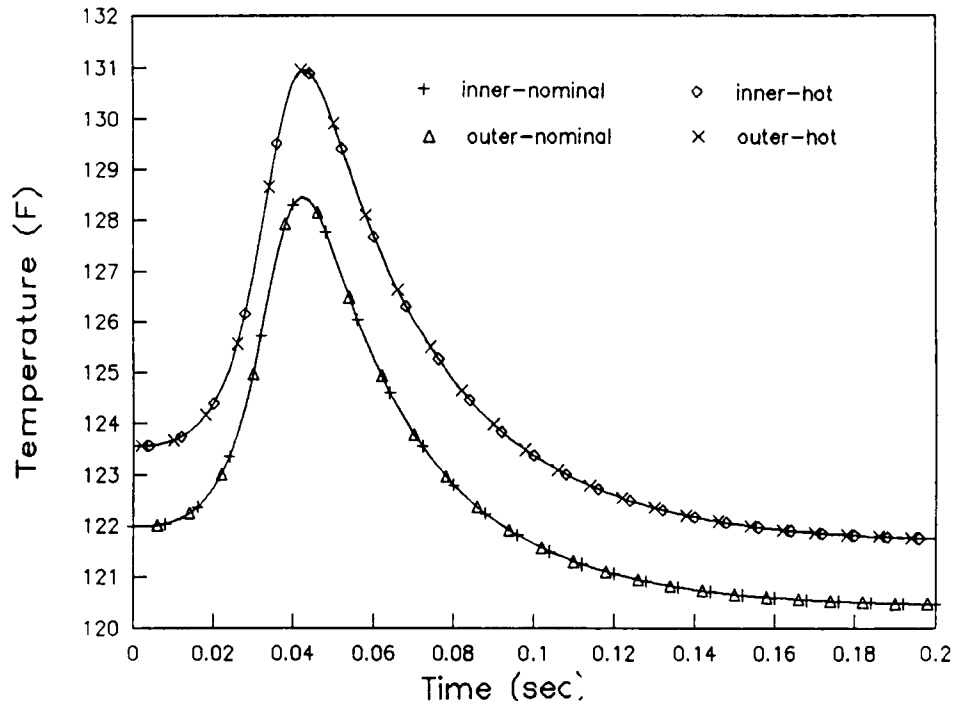


Figure F15 : Inlet node average fuel temperatures during optimum void accident for BOC, Mode 1, full-power transient.

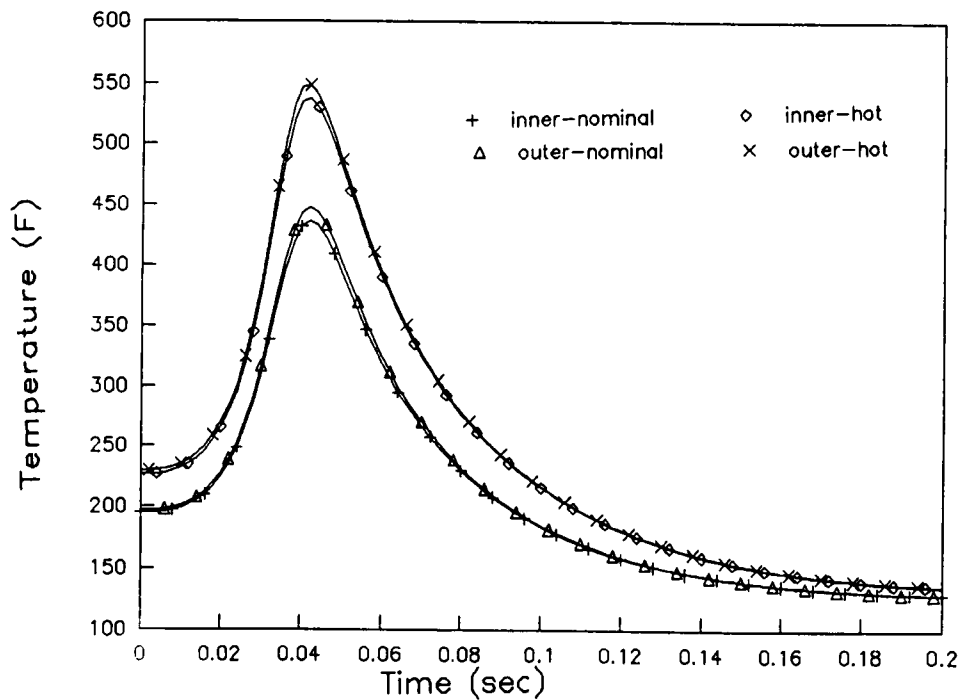


Figure F16 : Fourth node average fuel temperatures during optimum void accident for BOC, Mode 1, full-power transient.

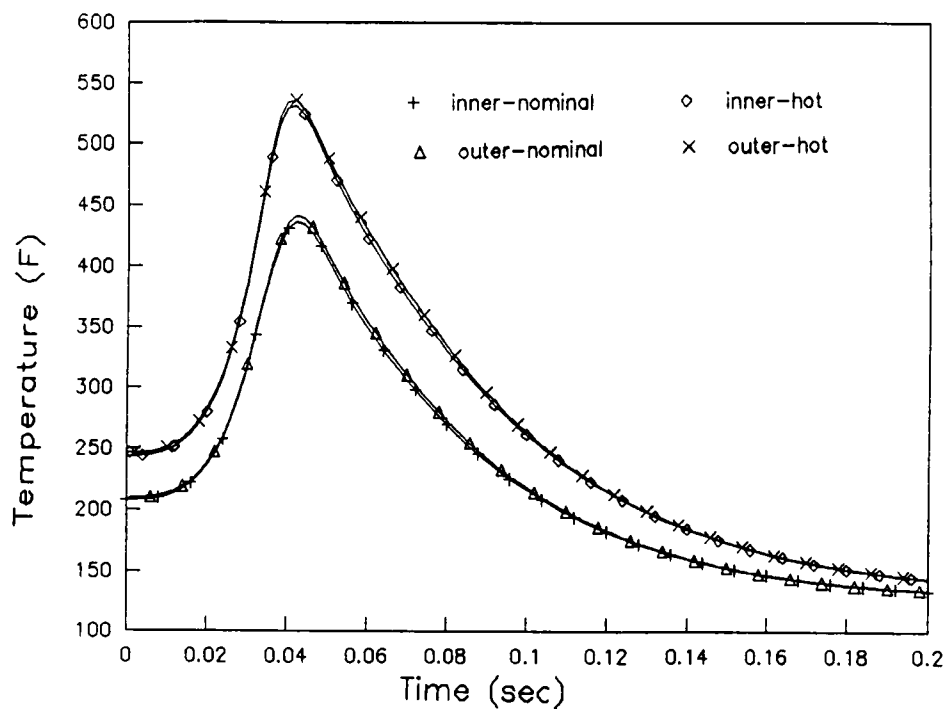


Figure F17 : Seventh node average fuel temperatures during optimum void accident for BOC, Mode 1, full-power transient.

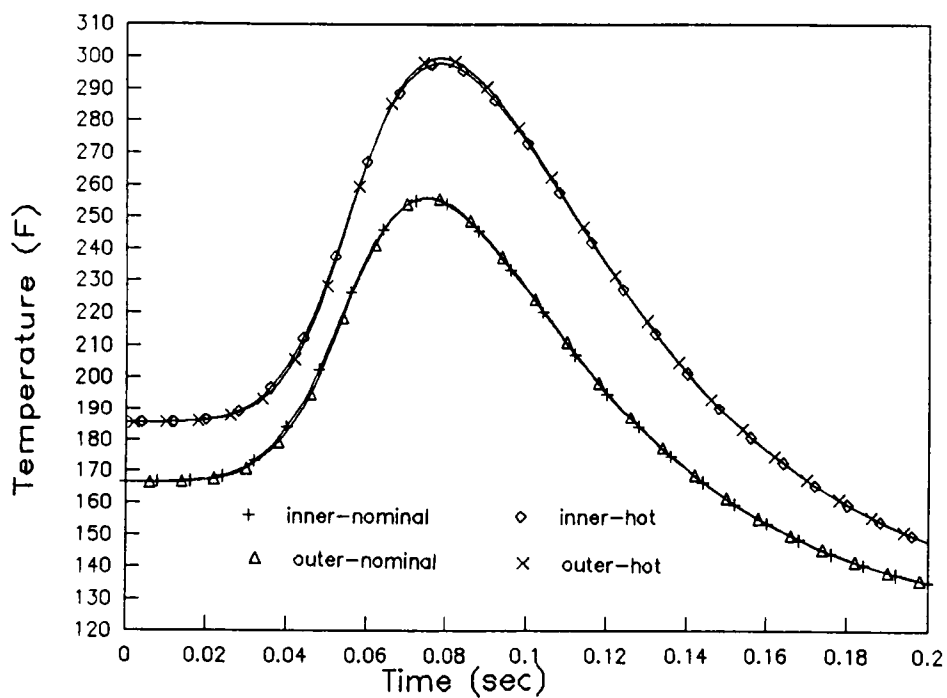


Figure F18 : Outlet node average fuel temperatures during optimum void accident for BOC, Mode 1, full-power transient.

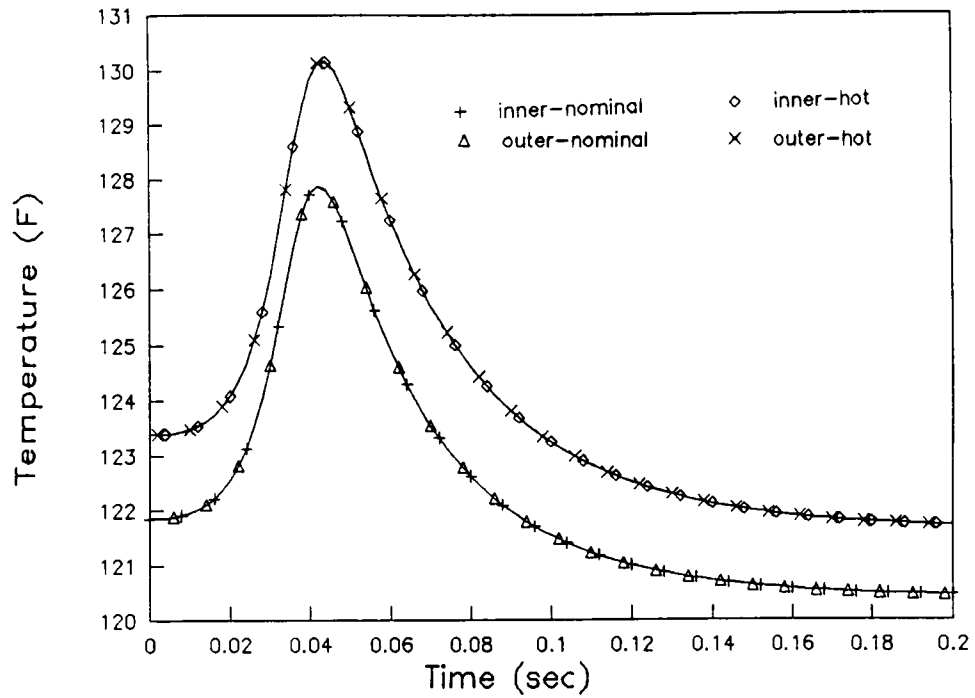


Figure F19 : Inlet node surface temperatures during optimum void accident for BOC, Mode 1, full-power transient.

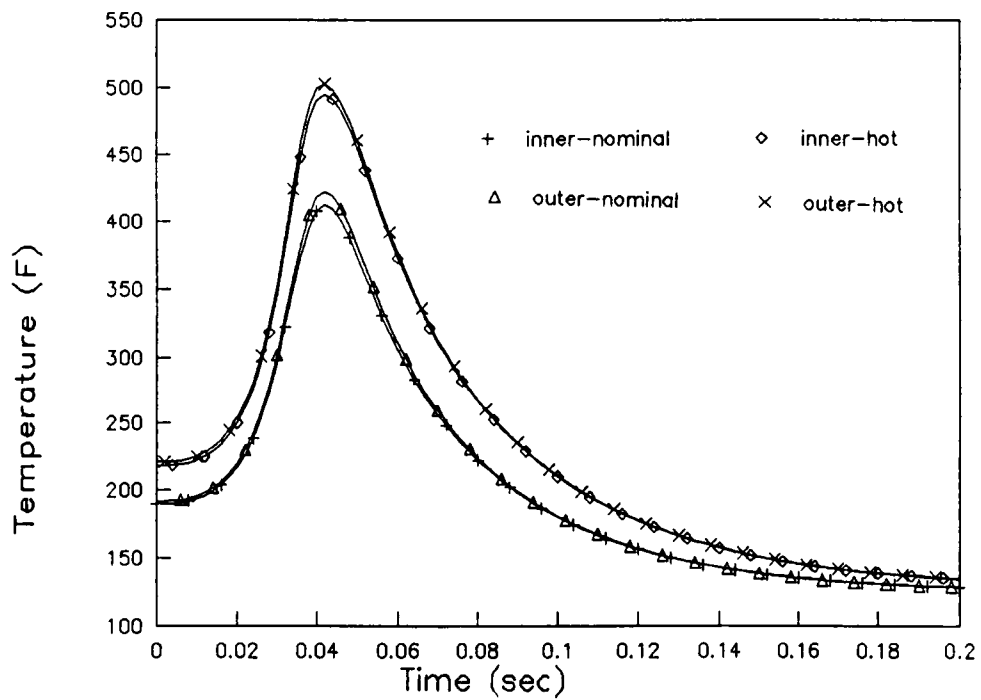


Figure F20 : Fourth node surface temperatures during optimum void accident for BOC, Mode 1, full-power transient.

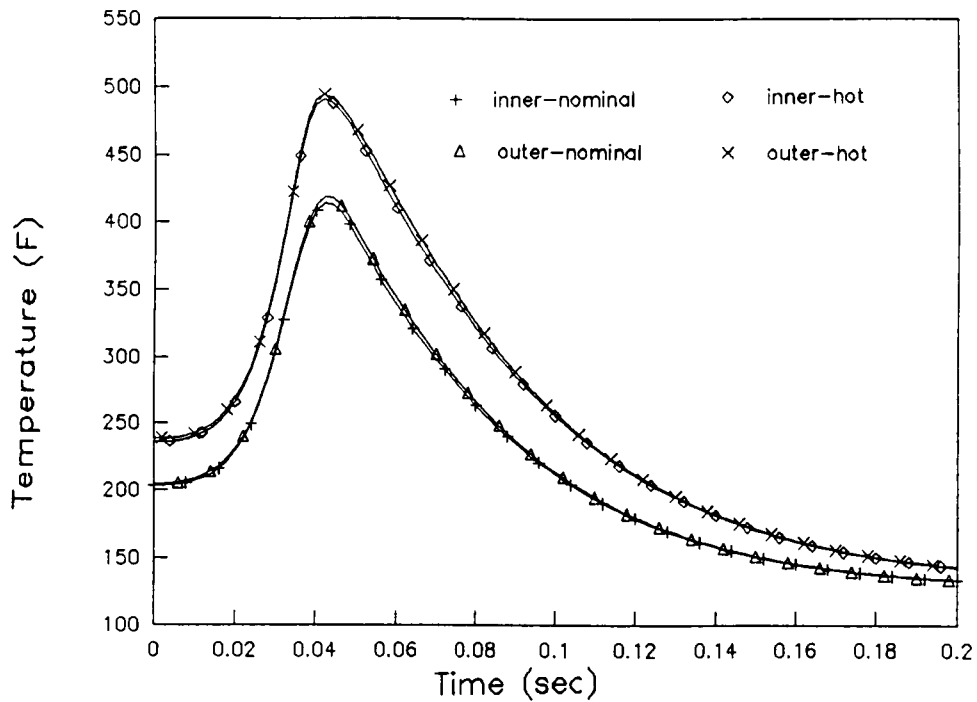


Figure F21 : Seventh node surface temperatures during optimum void accident for BOC, Mode 1, full-power transient.

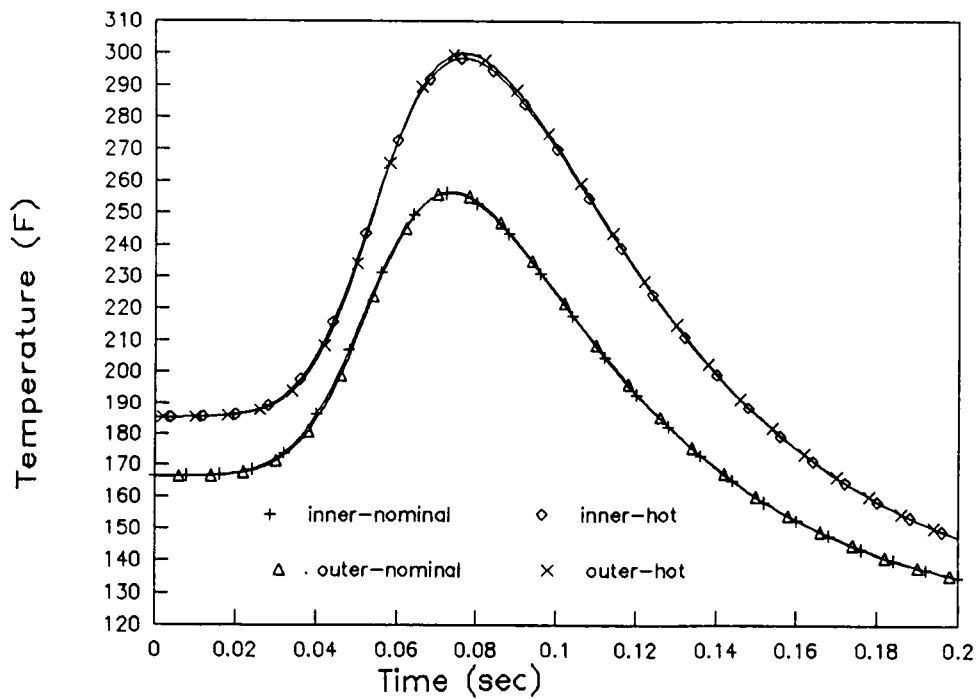


Figure F22 : Outlet node surface temperatures during optimum void accident for BOC, Mode 1, full-power transient.

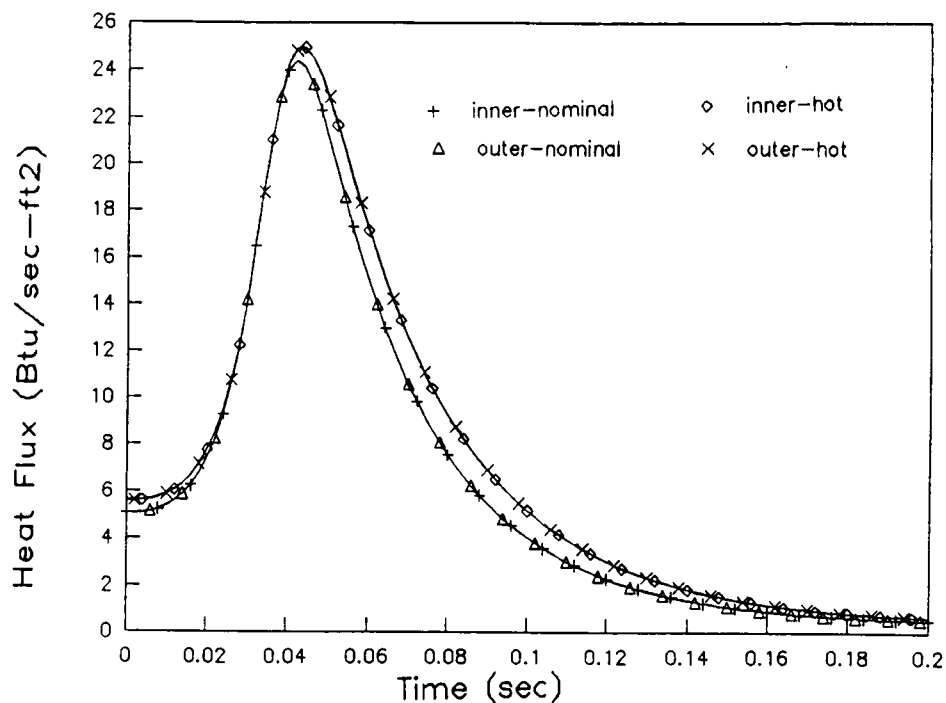


Figure F23 : Inlet node surface heat-fluxes during optimum void accident for BOC, Mode 1, full-power transient.

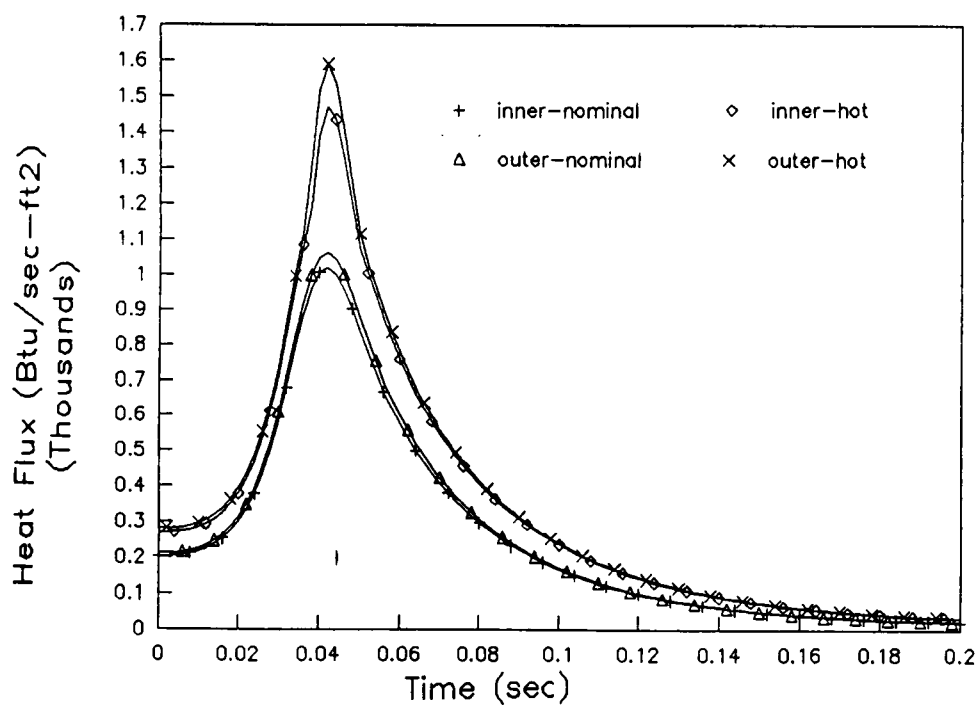


Figure F24 : Fourth node surface heat-fluxes during optimum void accident for BOC, Mode 1, full-power transient.

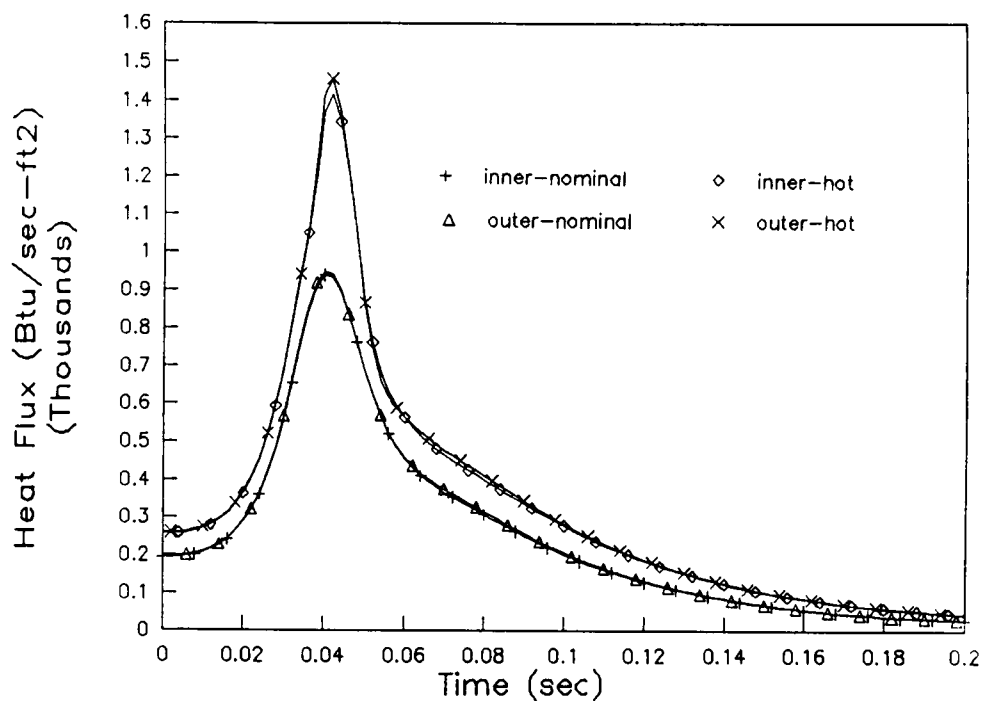


Figure F25 : Seventh node surface heat-fluxes during optimum void accident for BOC, Mode 1, full-power transient.

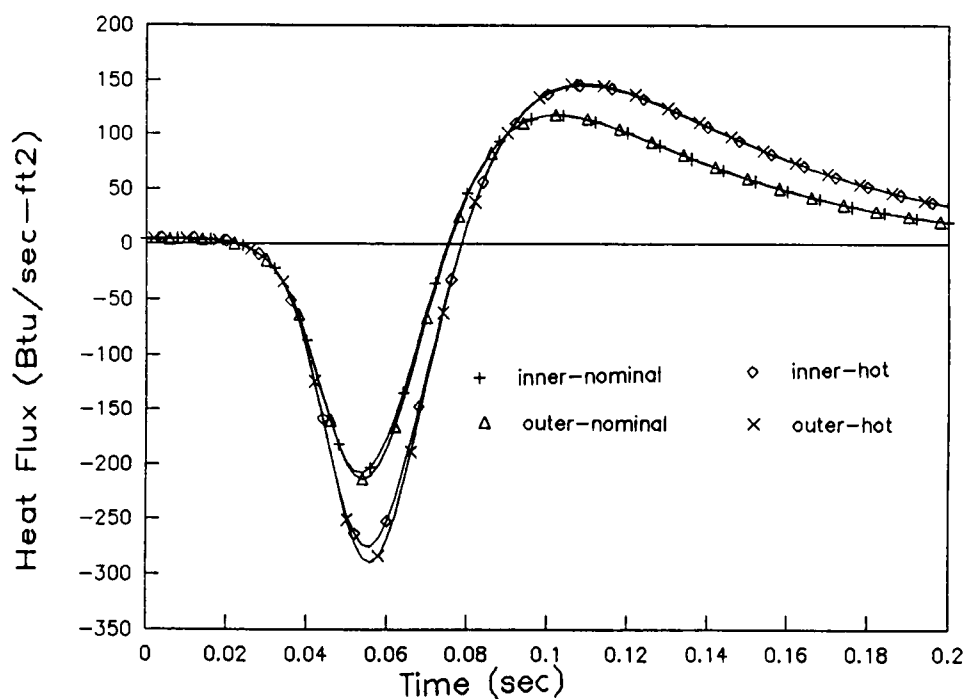


Figure F26 : Outlet node surface heat-fluxes during optimum void accident for BOC, Mode 1, full-power transient.

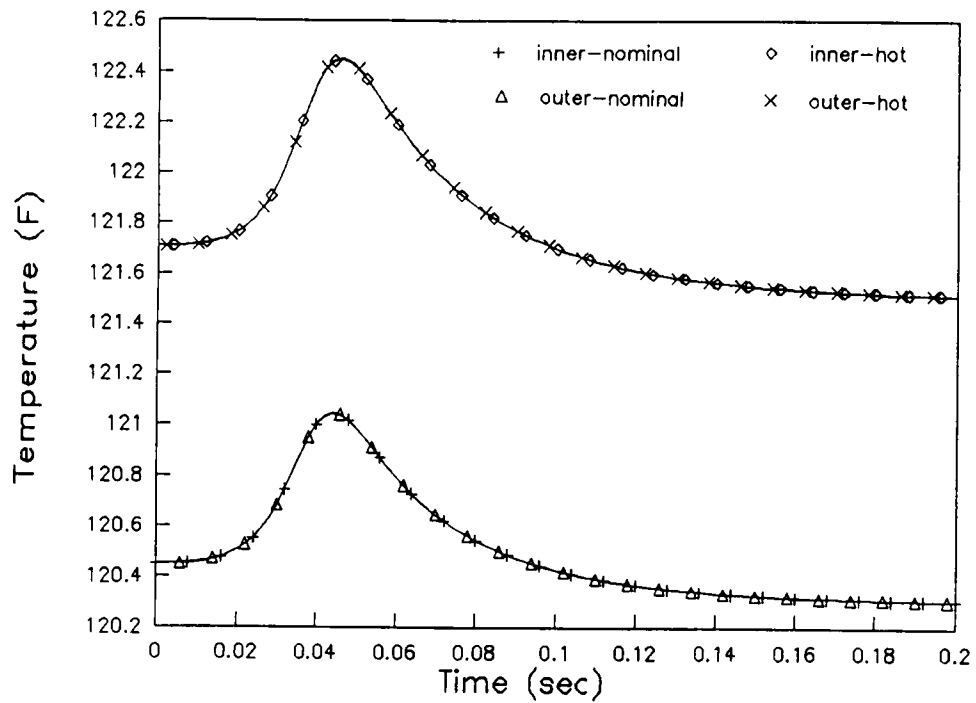


Figure F27 : Inlet node liquid phase temperatures during optimum void accident for BOC, Mode 1, full-power transient.

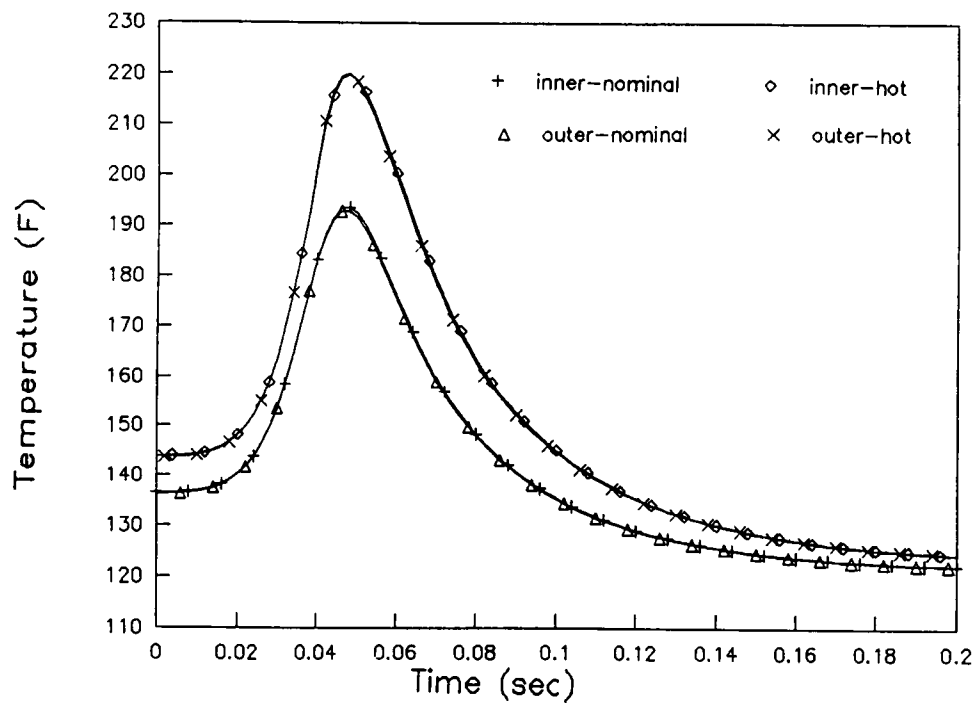


Figure F28 : Fourth node liquid phase temperatures during optimum void accident for BOC, Mode 1, full-power transient.

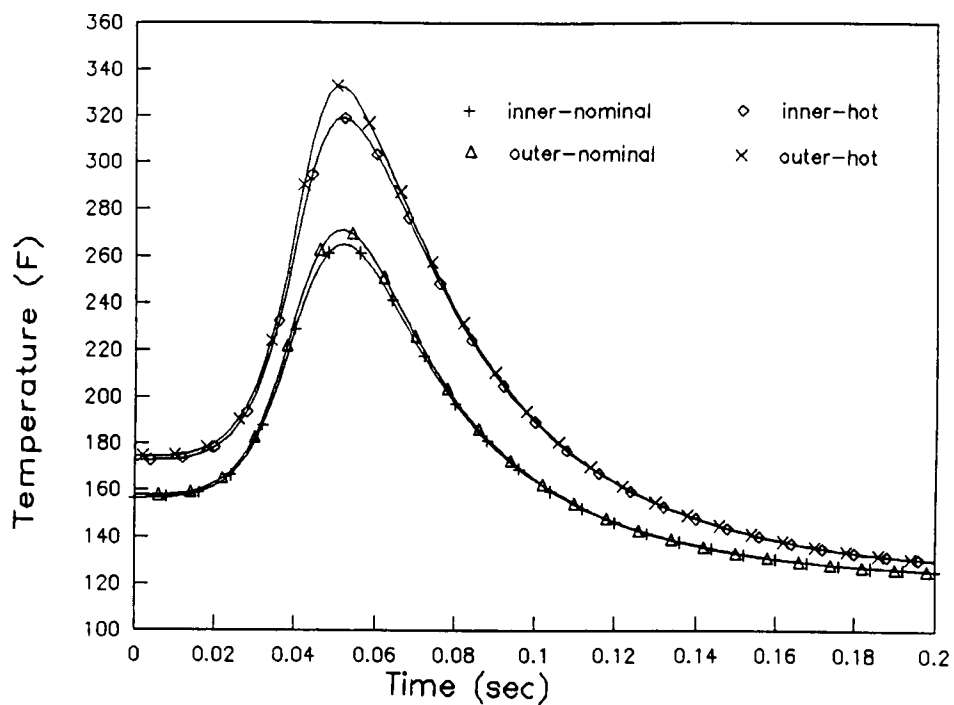


Figure F29 : Seventh node liquid phase temperatures during optimum void accident for BOC, Mode 1, full-power transient.

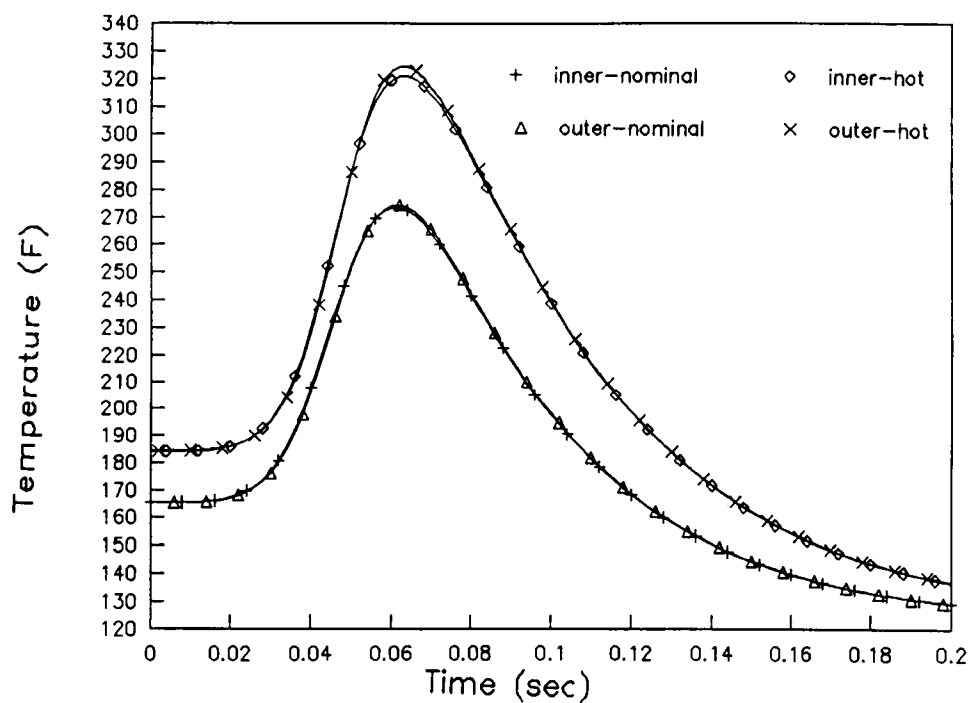


Figure F30 : Outlet node liquid phase temperatures during optimum void accident for BOC, Mode 1, full-power transient.

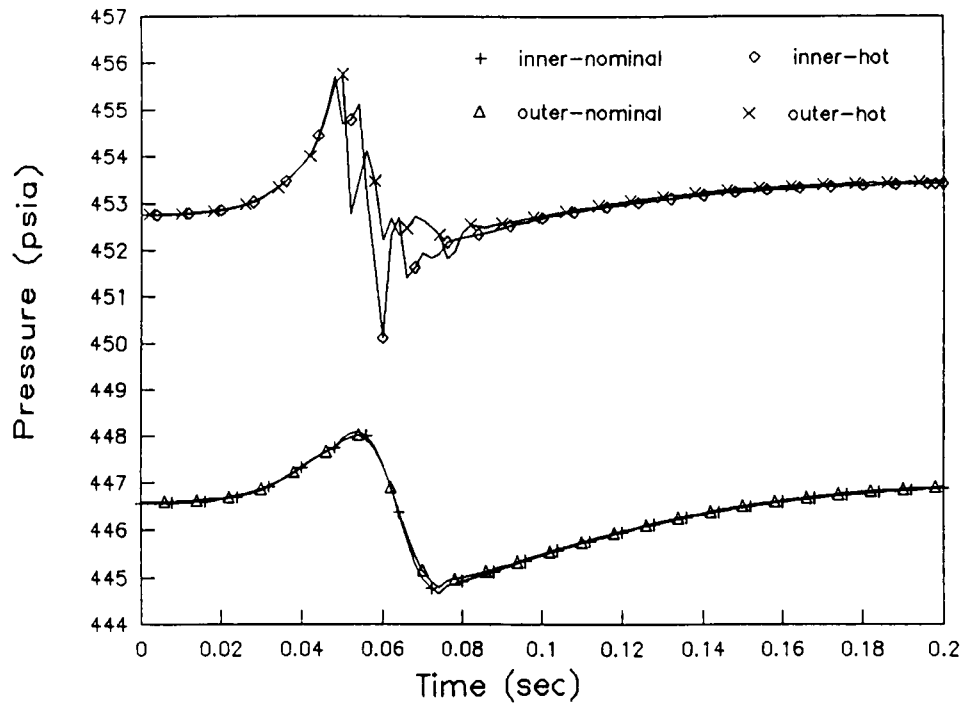


Figure F31 : Inlet node pressures during optimum void accident for EOC, Mode 1, full-power transient.

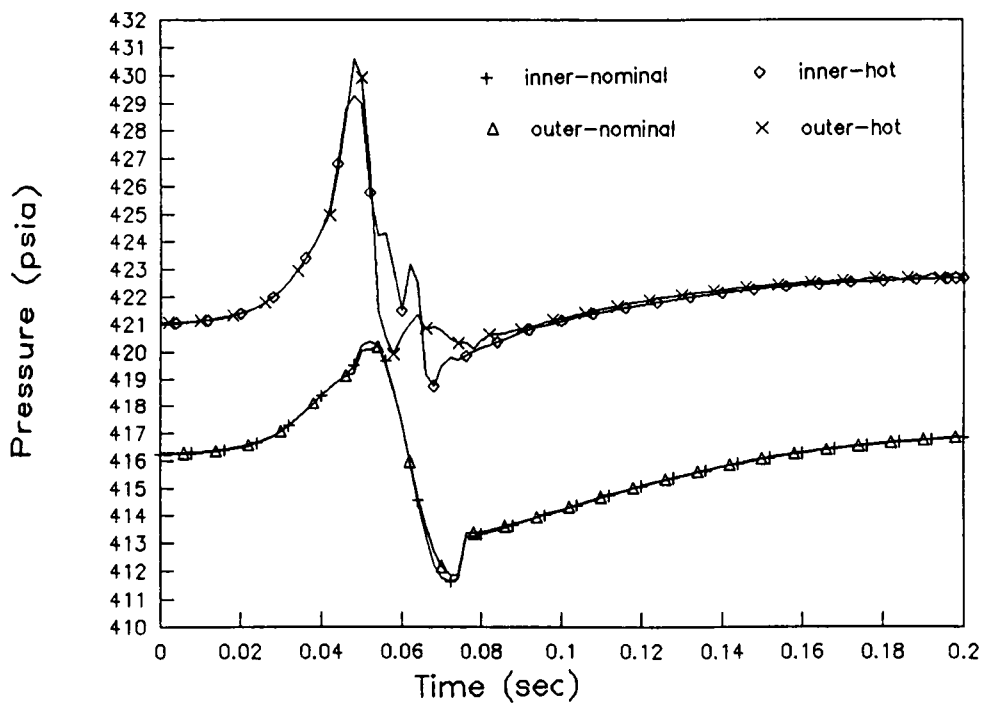


Figure F32 : Fourth node (out of ten) pressures during optimum void accident for EOC, Mode 1, full-power transient.

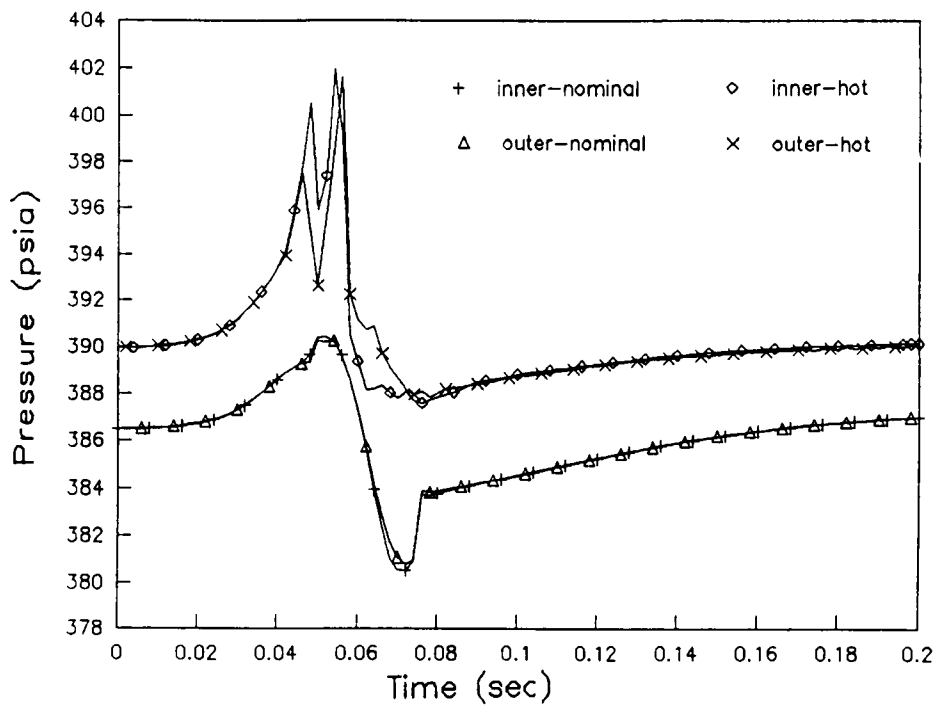


Figure F33 : Seventh node (out of ten) pressures during optimum void accident for EOC, Mode 1, full-power transient.

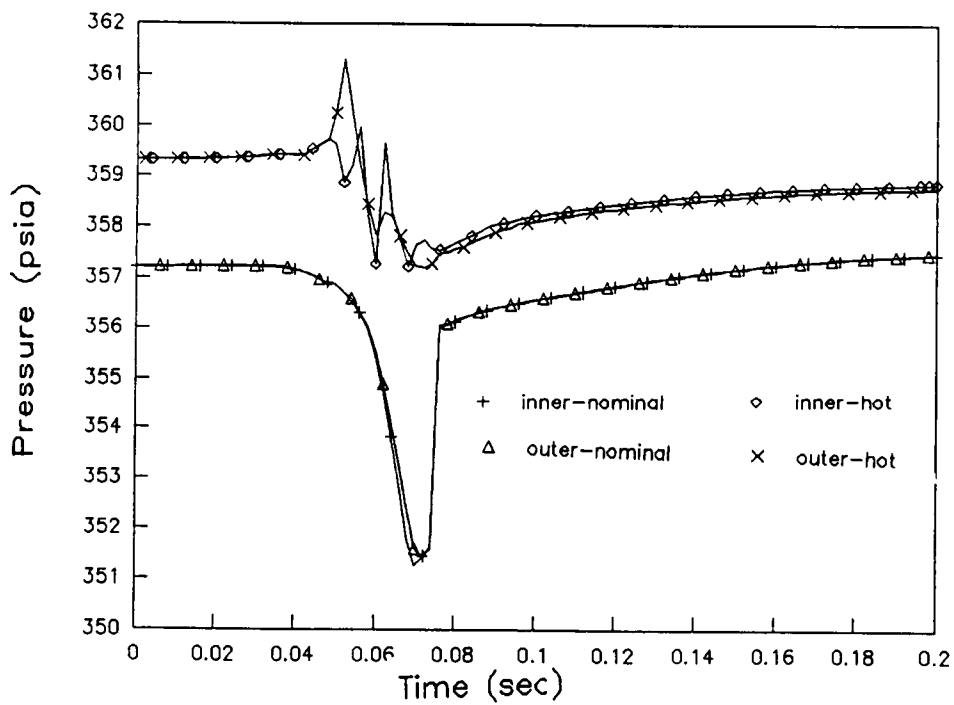


Figure F34 : Outlet node pressures during optimum void accident for EOC, Mode 1, full-power transient.

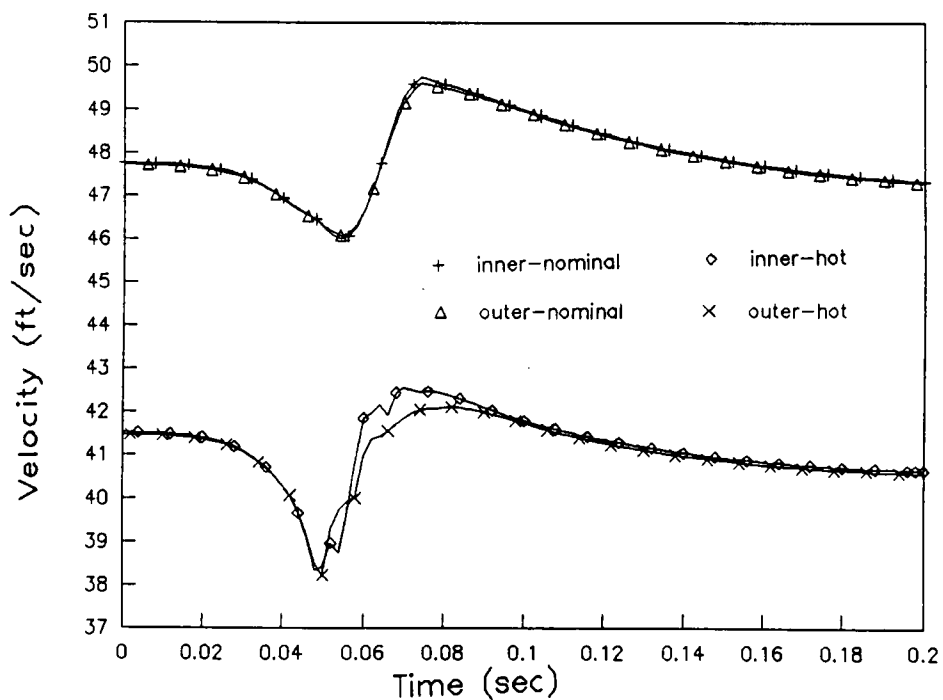


Figure F35 : Inlet node velocities during optimum void accident for EOC, Mode 1, full-power transient.

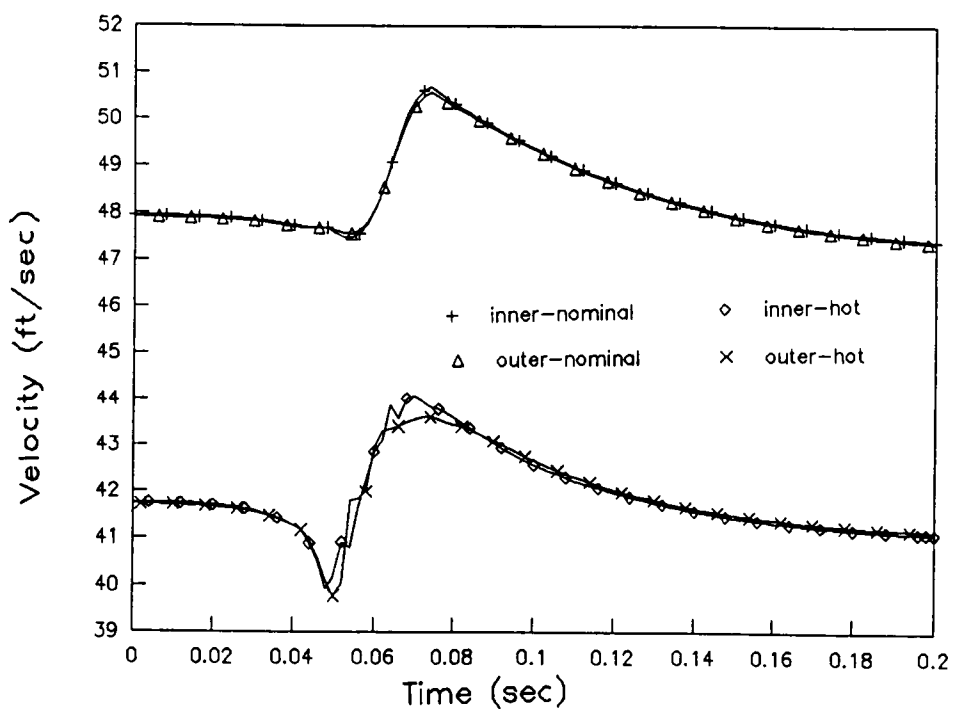


Figure F36 : Fourth node velocities during optimum void accident for EOC, Mode 1, full-power transient.

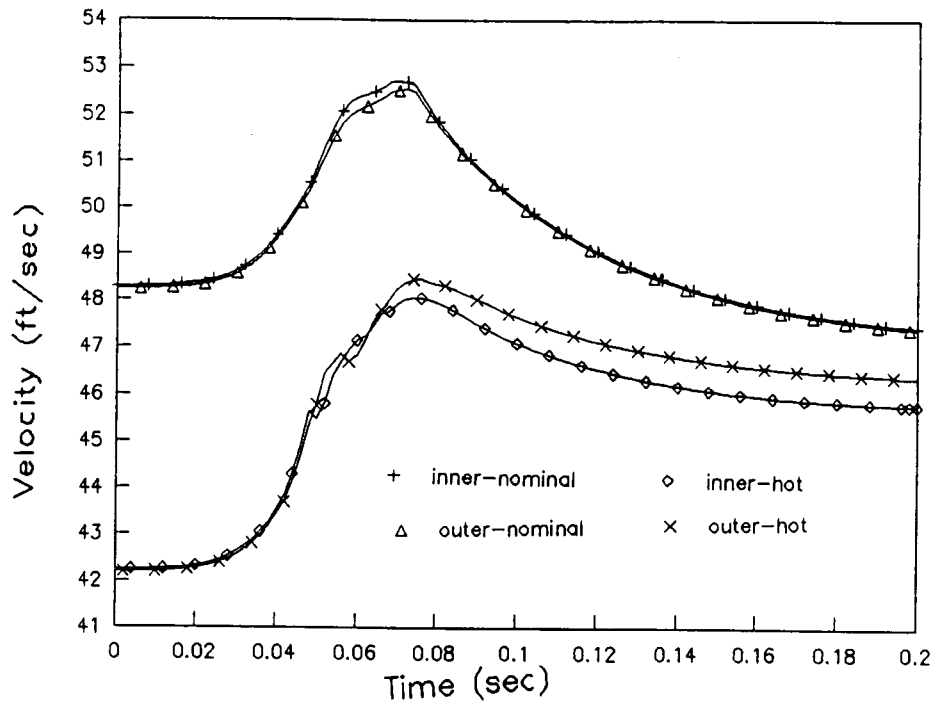


Figure F37 : Seventh node velocities during optimum void accident for EOC, Mode 1, full-power transient.

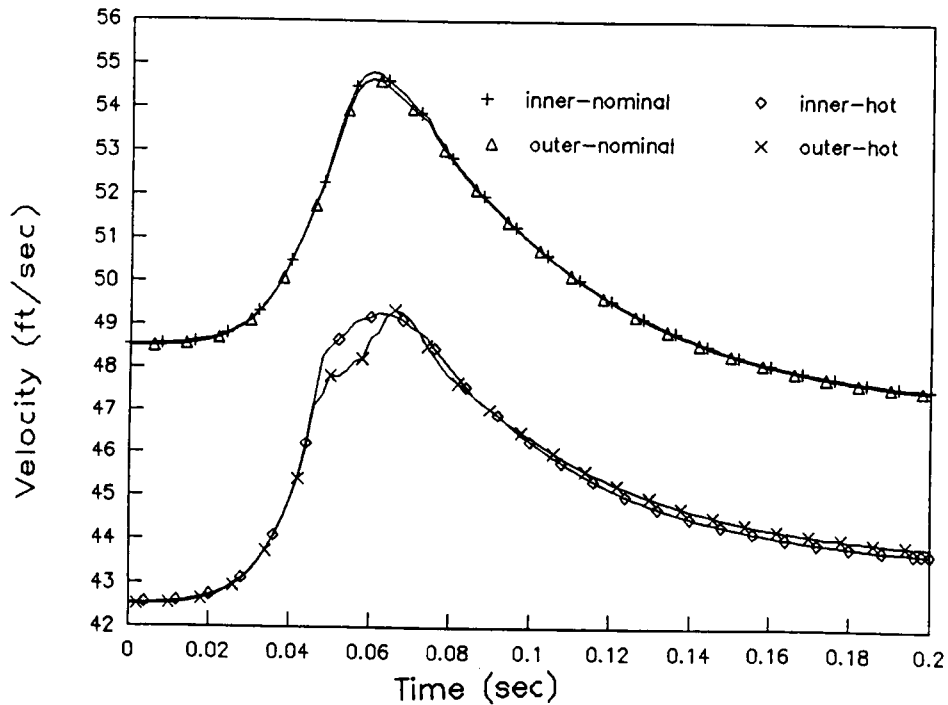


Figure F38 : Outlet node velocities during optimum void accident for EOC, Mode 1, full-power transient.

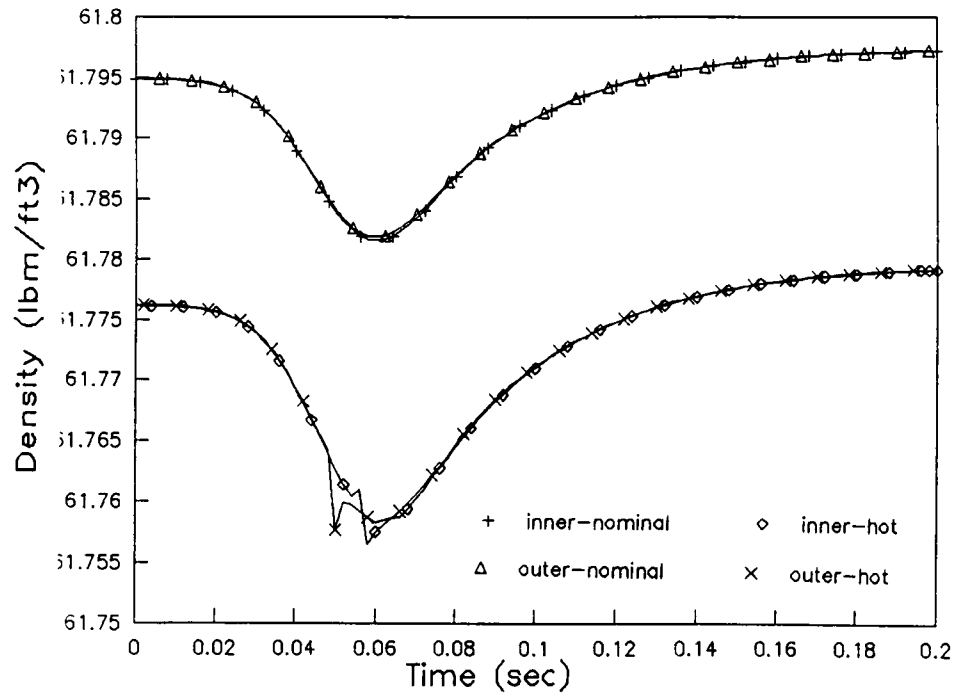


Figure F39 : Inlet node two-phase mixture density during optimum void accident for EOC, Mode 1, full-power transient.

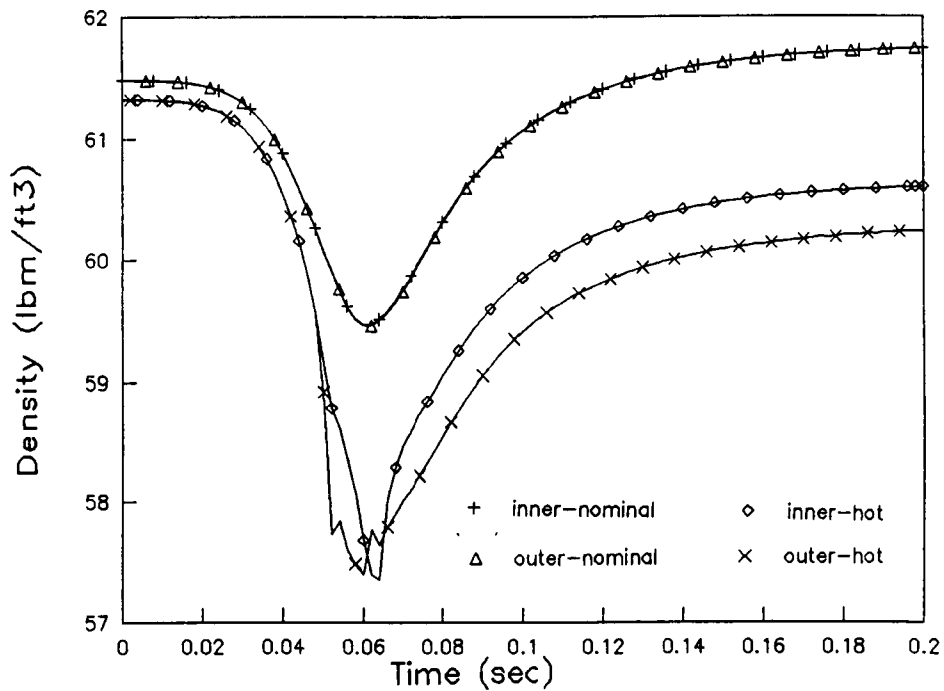


Figure F40 : Fourth node two-phase mixture density during optimum void accident for EOC, Mode 1, full-power transient.

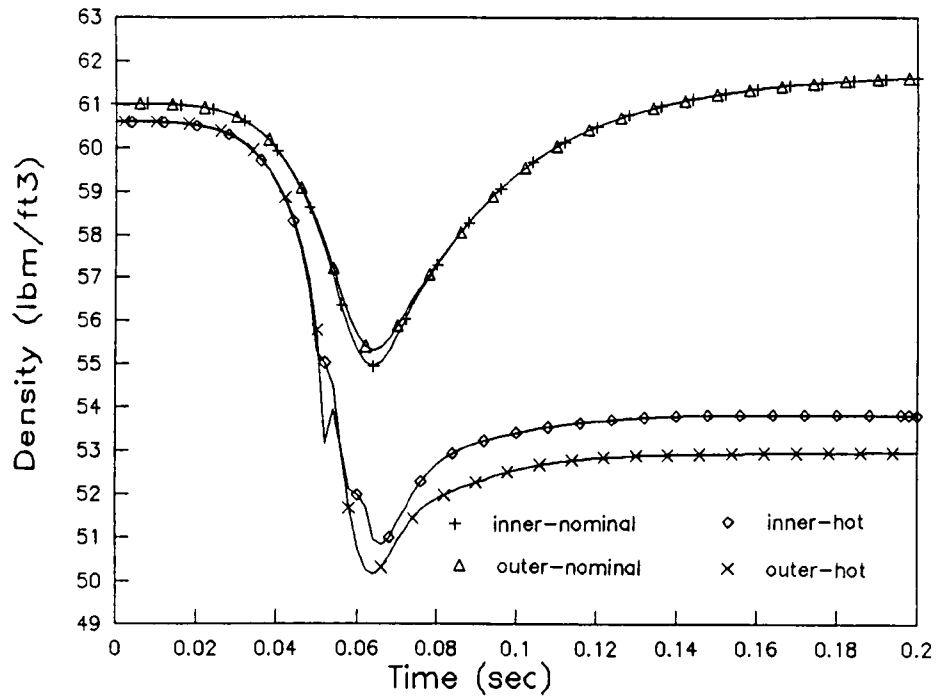


Figure F41 : Seventh node two-phase mixture density during optimum void accident for EOC, Mode 1, full-power transient.

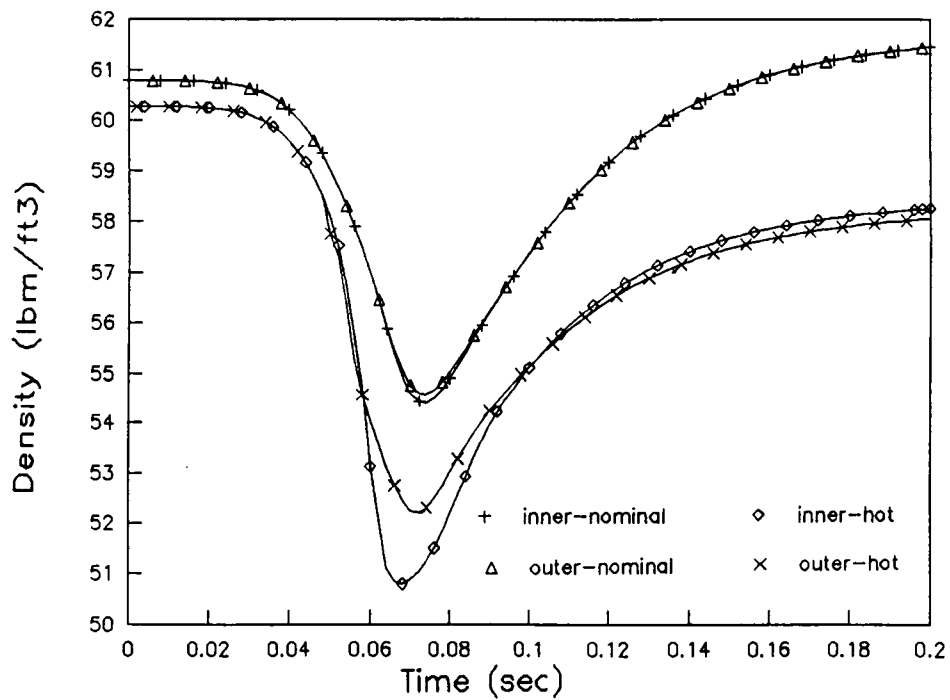


Figure F42 : Outlet node two-phase mixture density during optimum void accident for EOC, Mode 1, full-power transient.

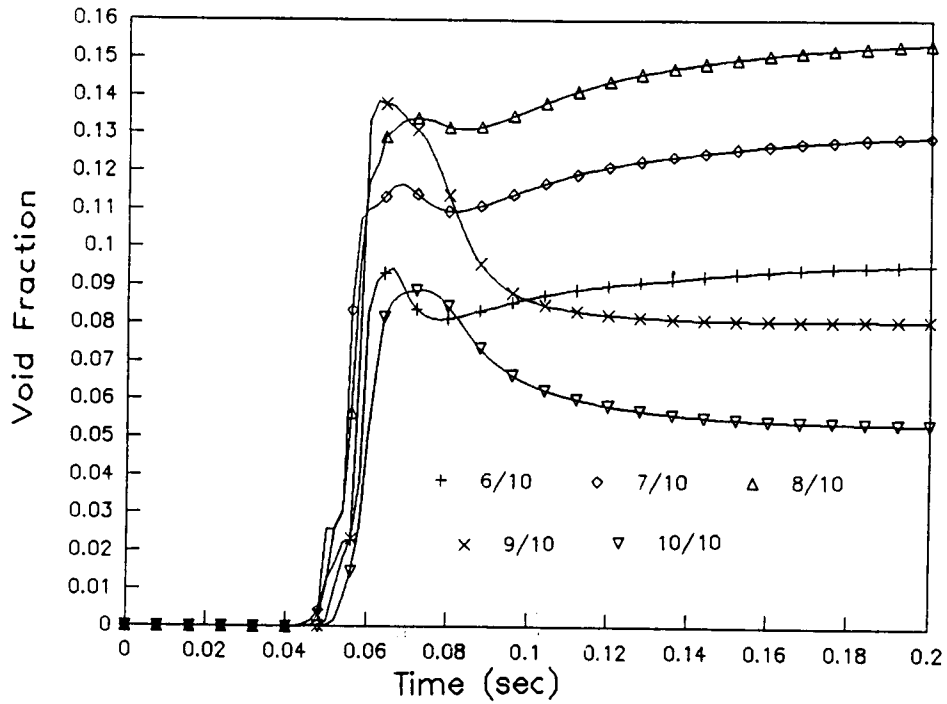


Figure F43 : Inner-element hot-channel void fractions for lower half of the core during optimum void accident for EOC, Mode 1, full-power transient.

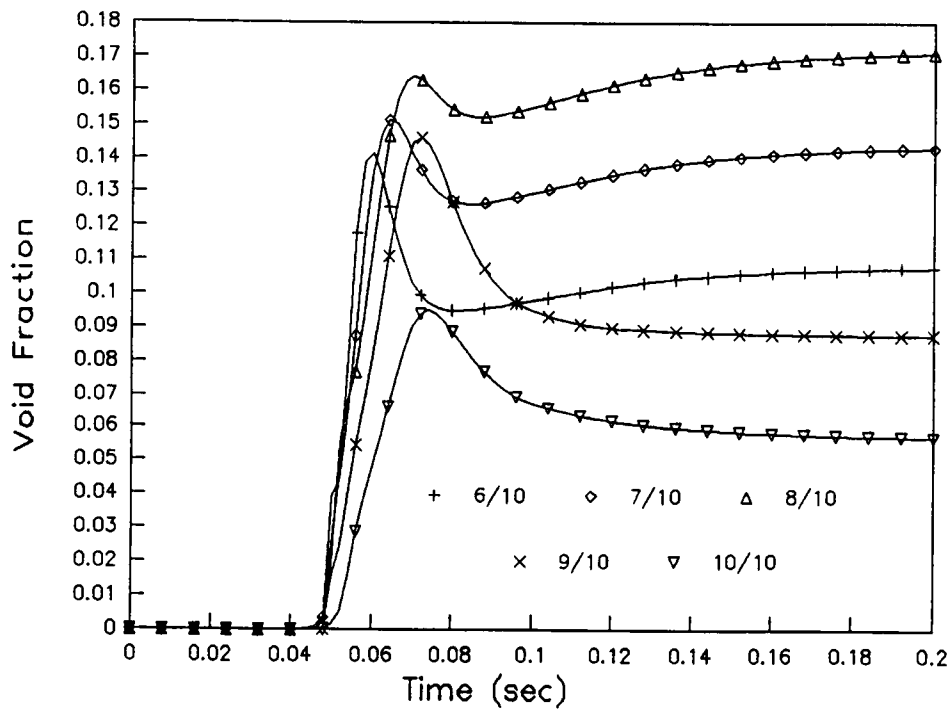


Figure F44 : Outer-element hot-channel void fractions for lower half of the core during optimum void accident for EOC, Mode 1, full-power transient.

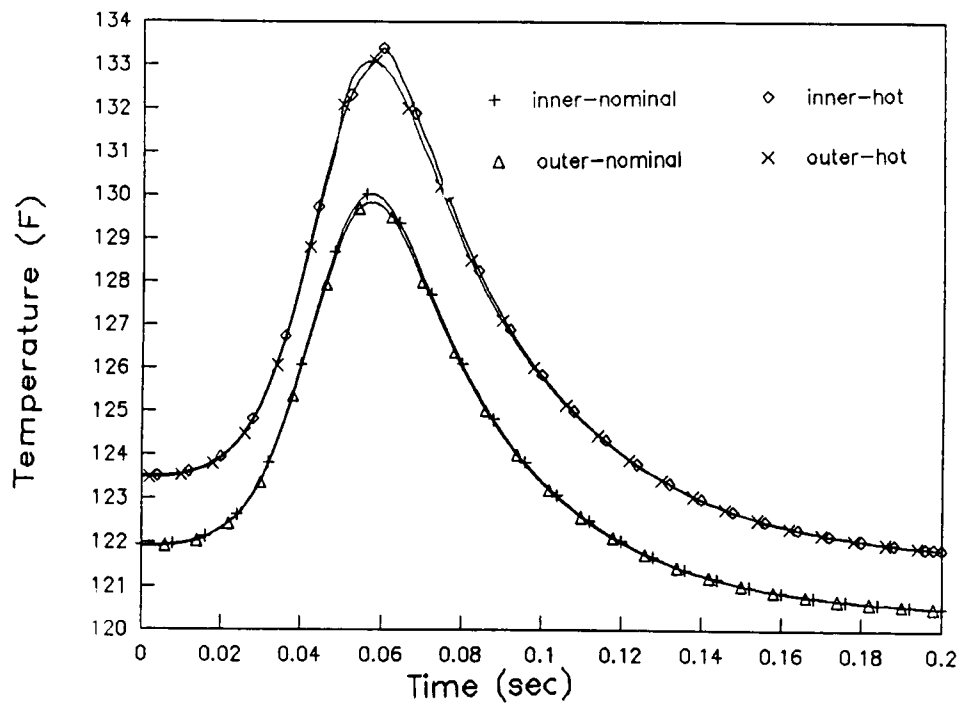


Figure F45 : Inlet node average fuel temperatures during optimum void accident for EOC, Mode 1, full-power transient.

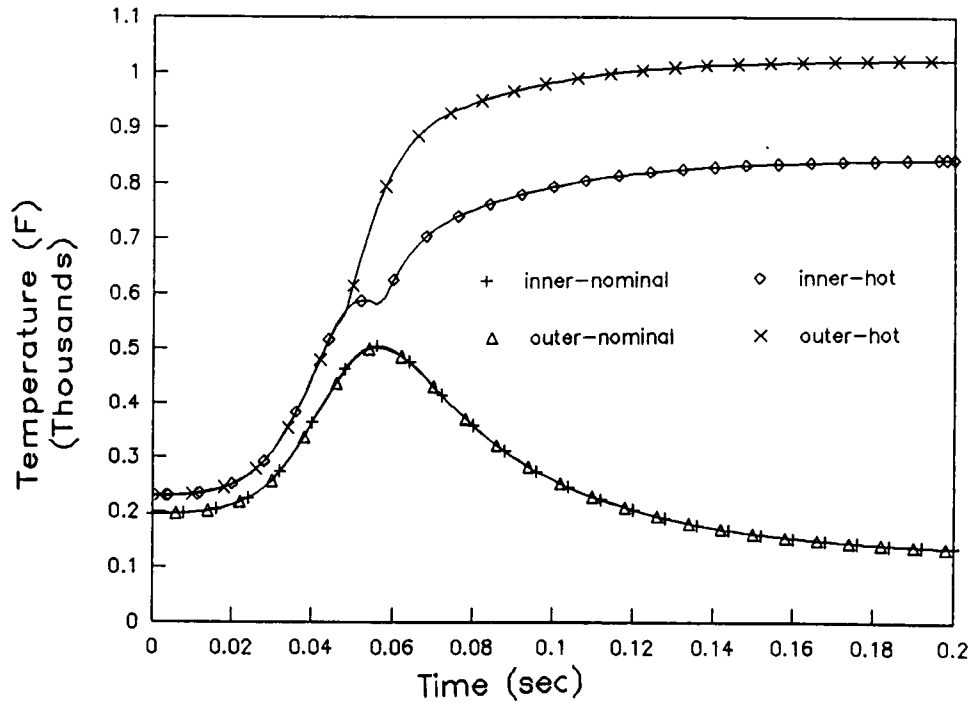


Figure F46 : Fourth node average fuel temperatures during optimum void accident for EOC, Mode 1, full-power transient.

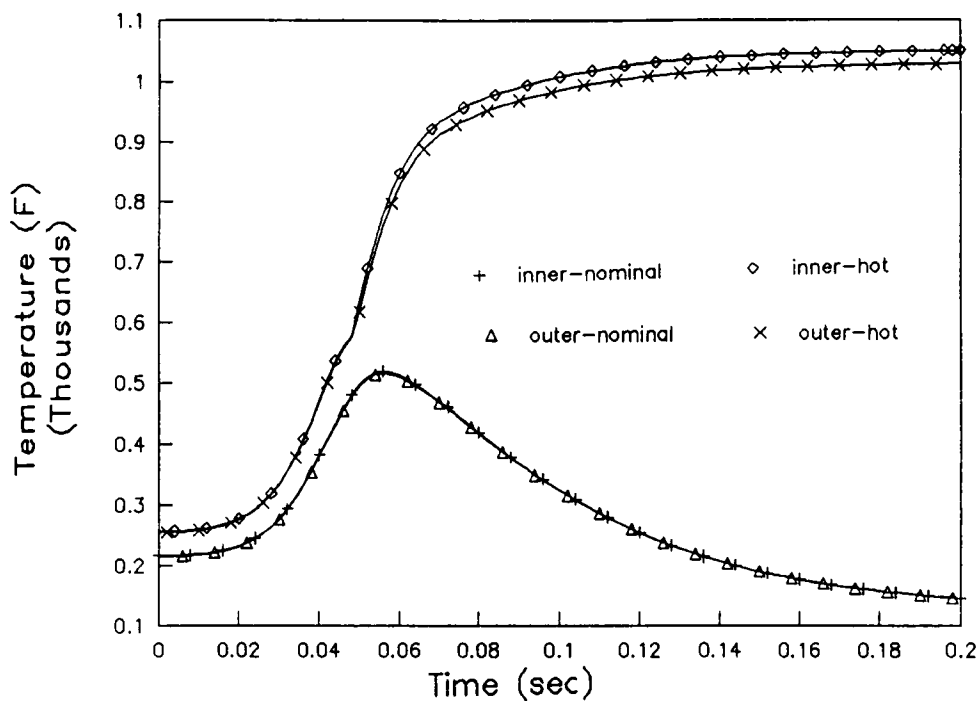


Figure F47 : Seventh node average fuel temperatures during optimum void accident for EOC, Mode 1, full-power transient.

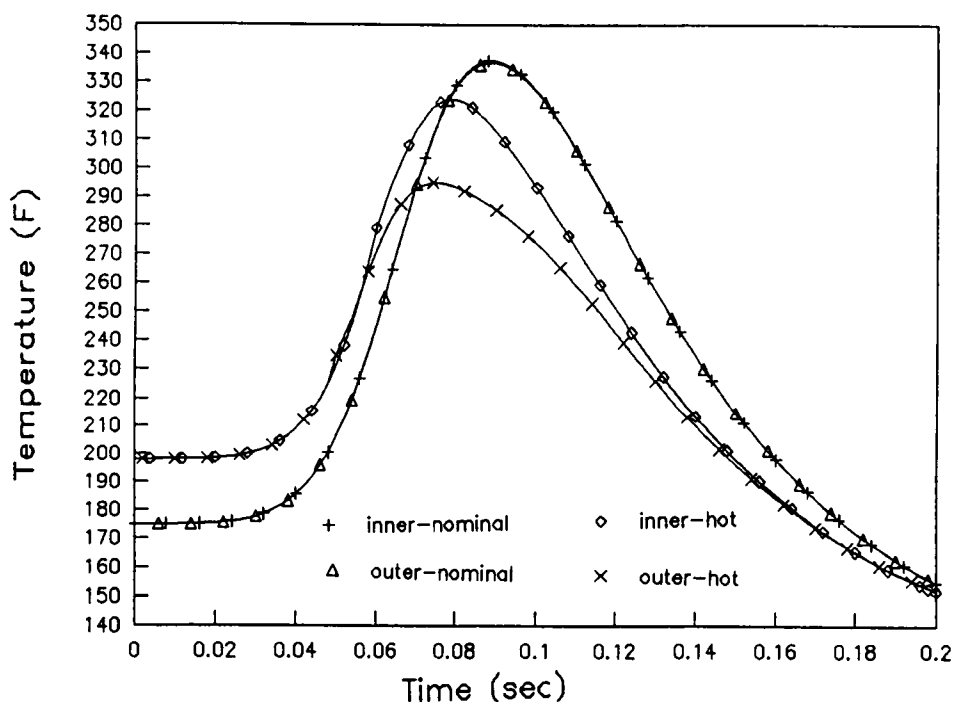


Figure F48 : Outlet node average fuel temperatures during optimum void accident for EOC, Mode 1, full-power transient.

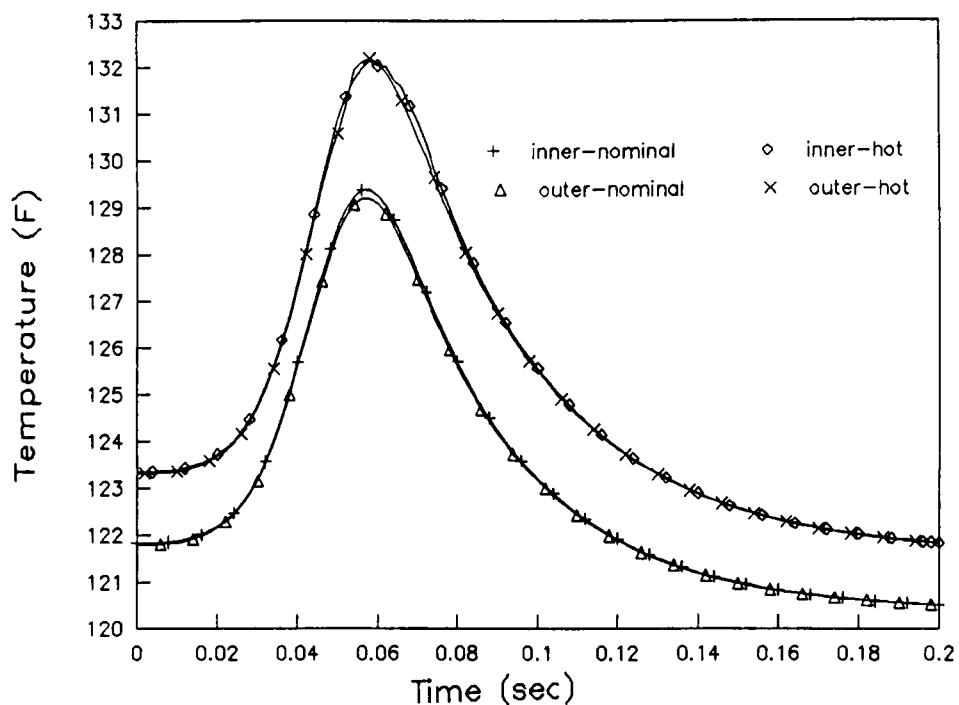


Figure F49 : Inlet node surface temperatures during optimum void accident for EOC, Mode 1, full-power transient.

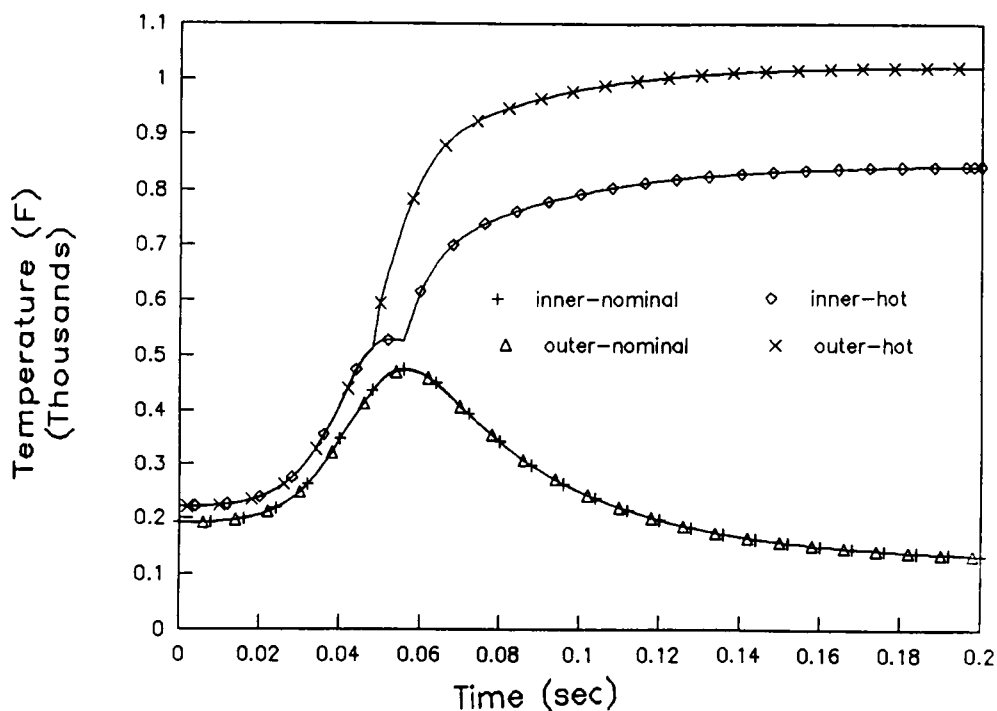


Figure F50 : Fourth node surface temperatures during optimum void accident for EOC, Mode 1, full-power transient.

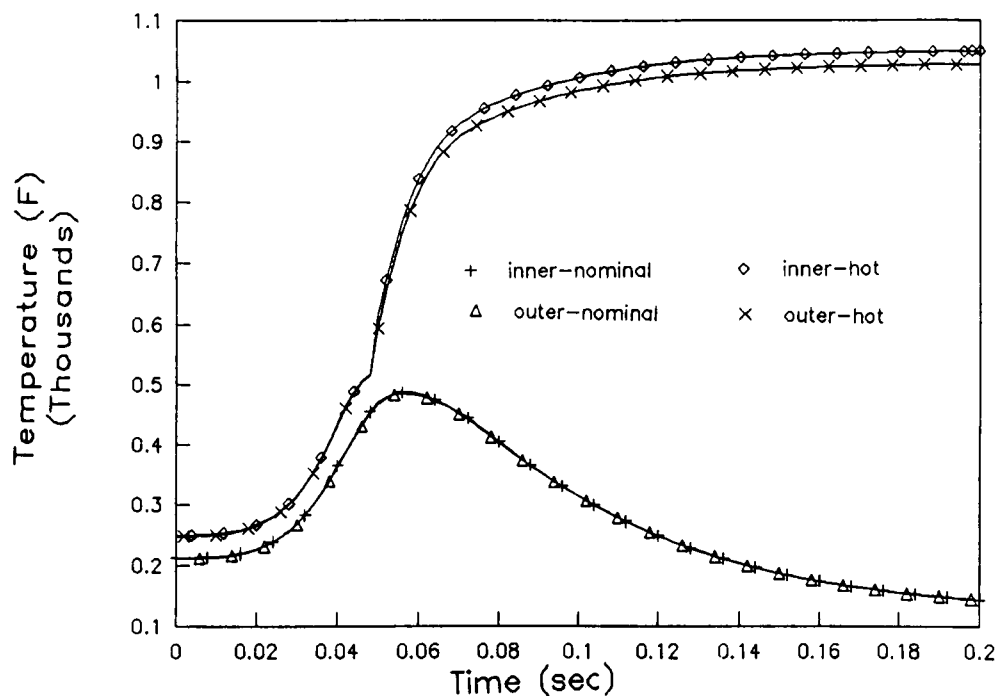


Figure F51 : Seventh node surface temperatures during optimum void accident for EOC, Mode 1, full-power transient.

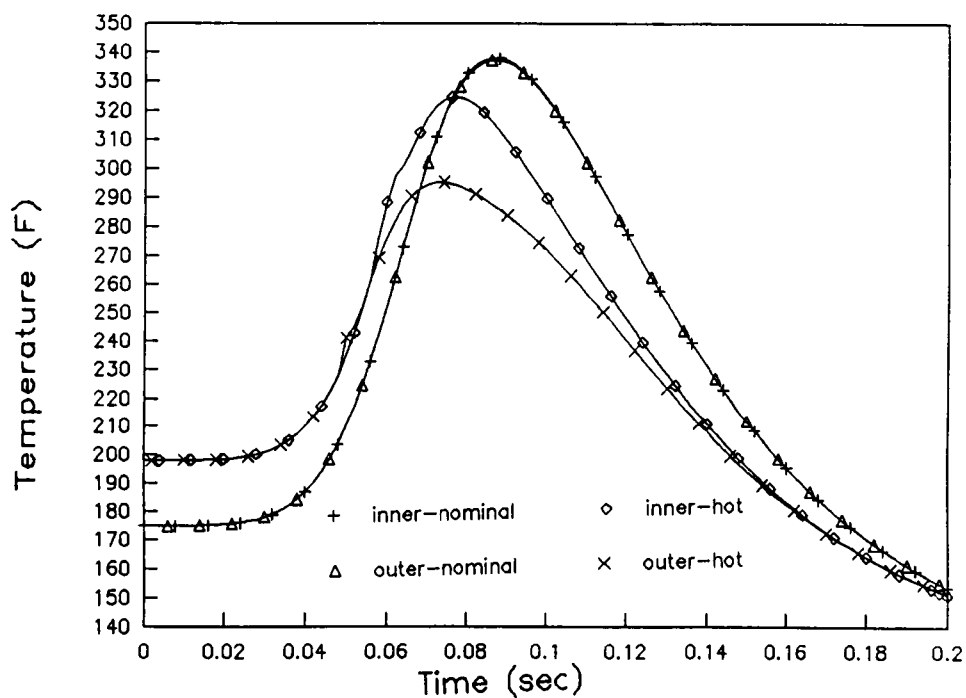


Figure F52 : Outlet node surface temperatures during optimum void accident for EOC, Mode 1, full-power transient.

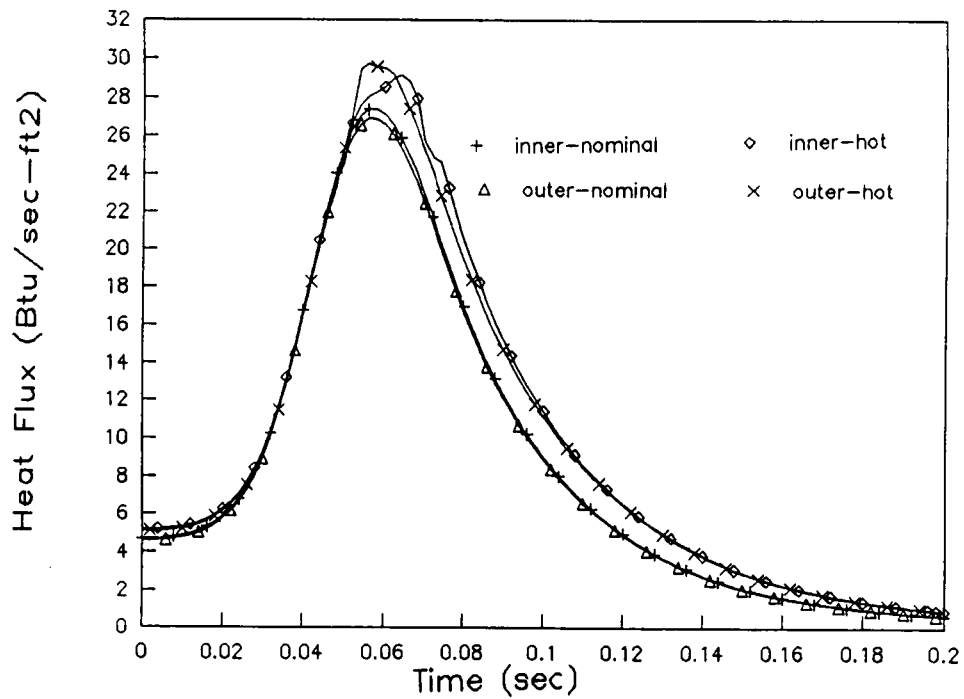


Figure F53 : Inlet node surface heat-fluxes during optimum void accident for EOC, Mode 1, full-power transient.

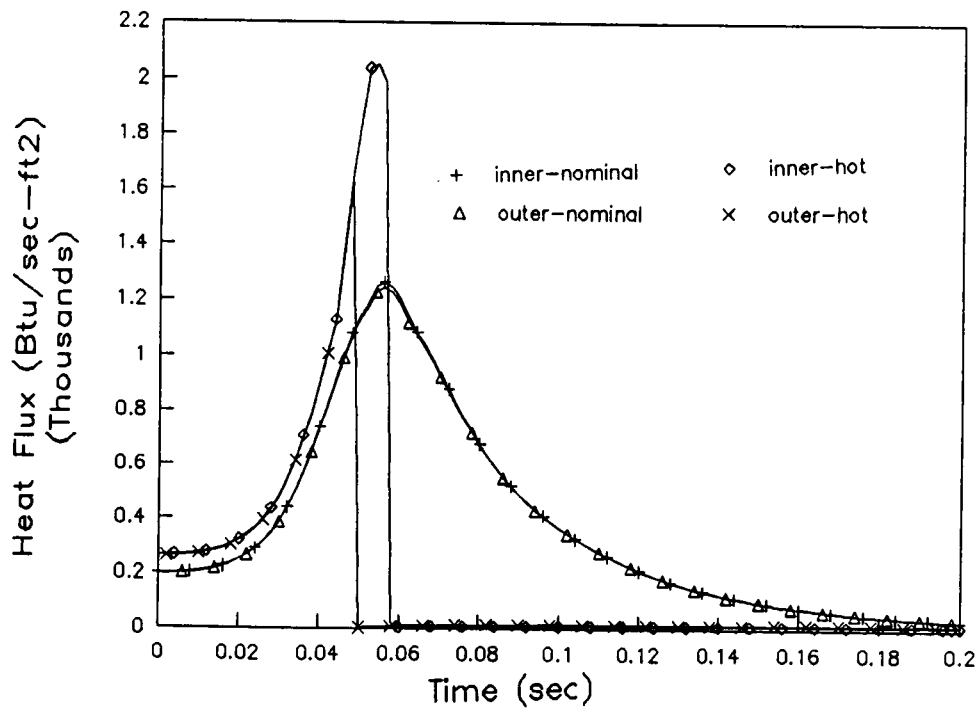


Figure F54 : Fourth node surface heat-fluxes during optimum void accident for EOC, Mode 1, full-power transient.

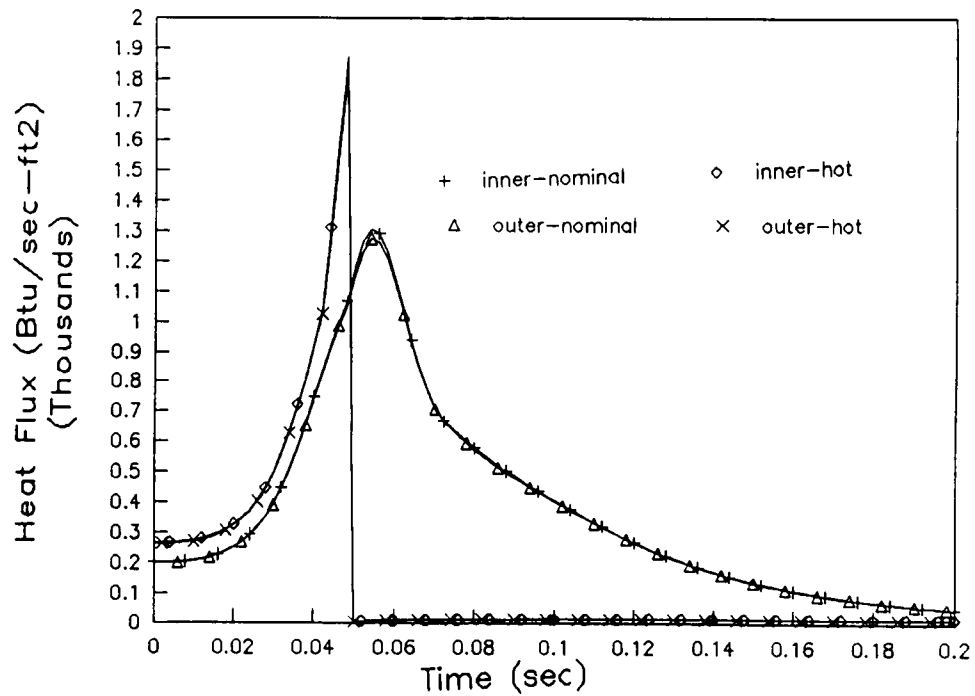


Figure F55 : Seventh node surface heat-fluxes during optimum void accident for EOC, Mode 1, full-power transient.

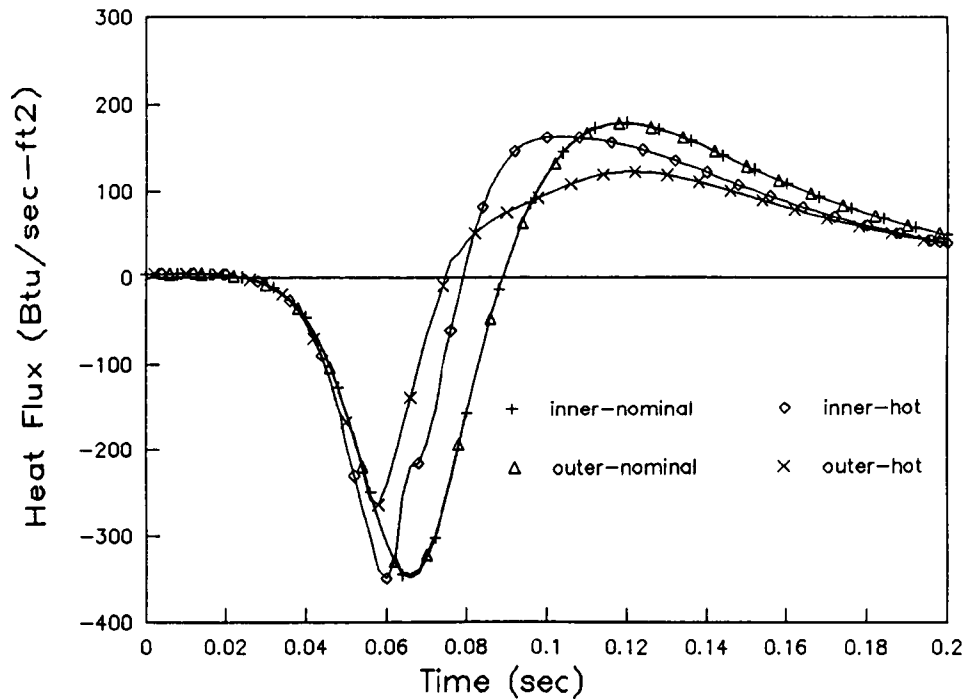


Figure F56 : Outlet node surface heat-fluxes during optimum void accident for EOC, Mode 1, full-power transient.

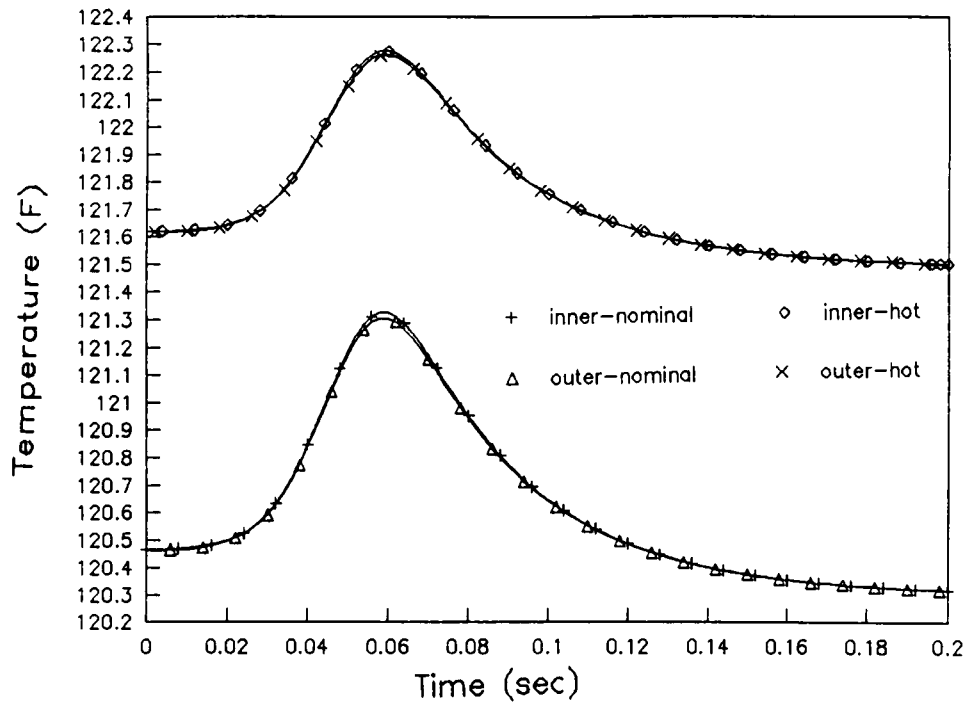


Figure F57 : Inlet node liquid phase temperatures during optimum void accident for EOC, Mode 1, full-power transient.

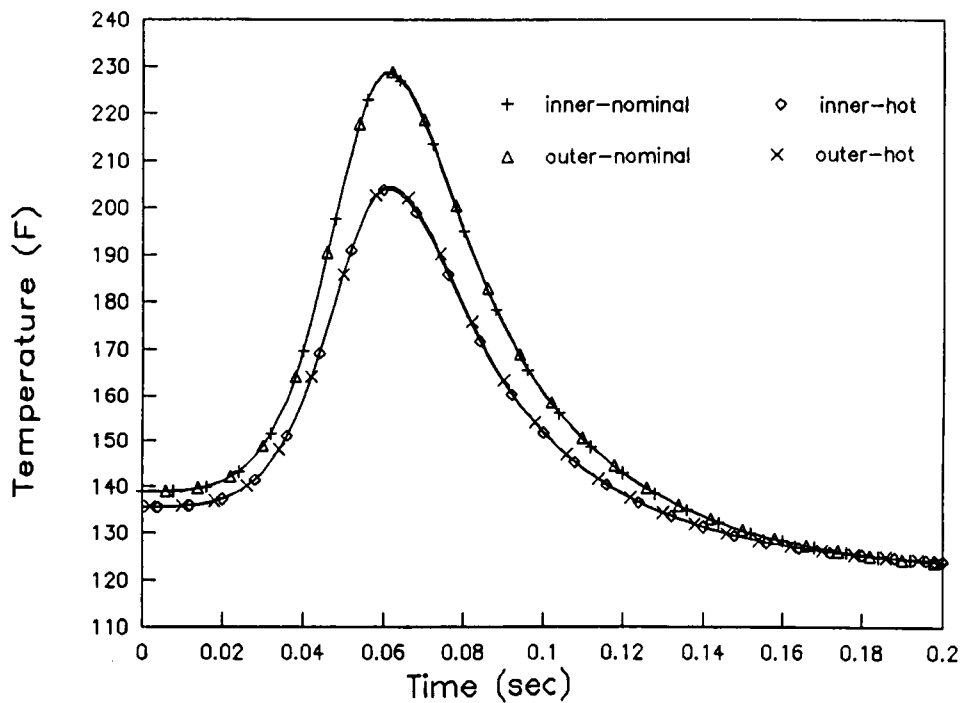


Figure F58 : Fourth node liquid phase temperatures during optimum void accident for EOC, Mode 1, full-power transient.

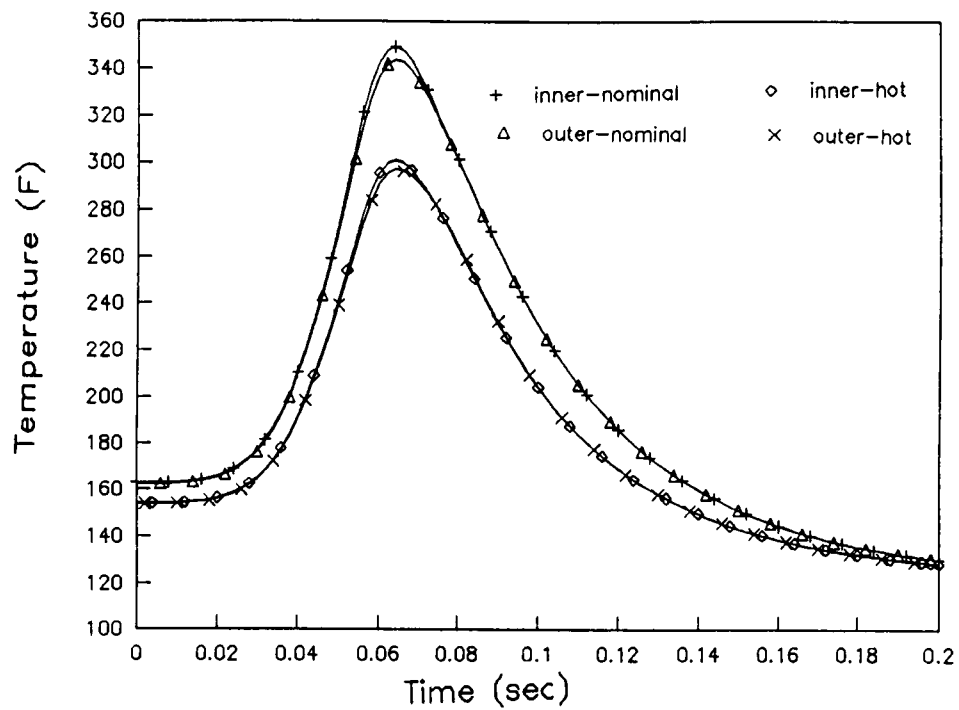


Figure F59 : Seventh node liquid phase temperatures during optimum void accident for EOC, Mode 1, full-power transient.

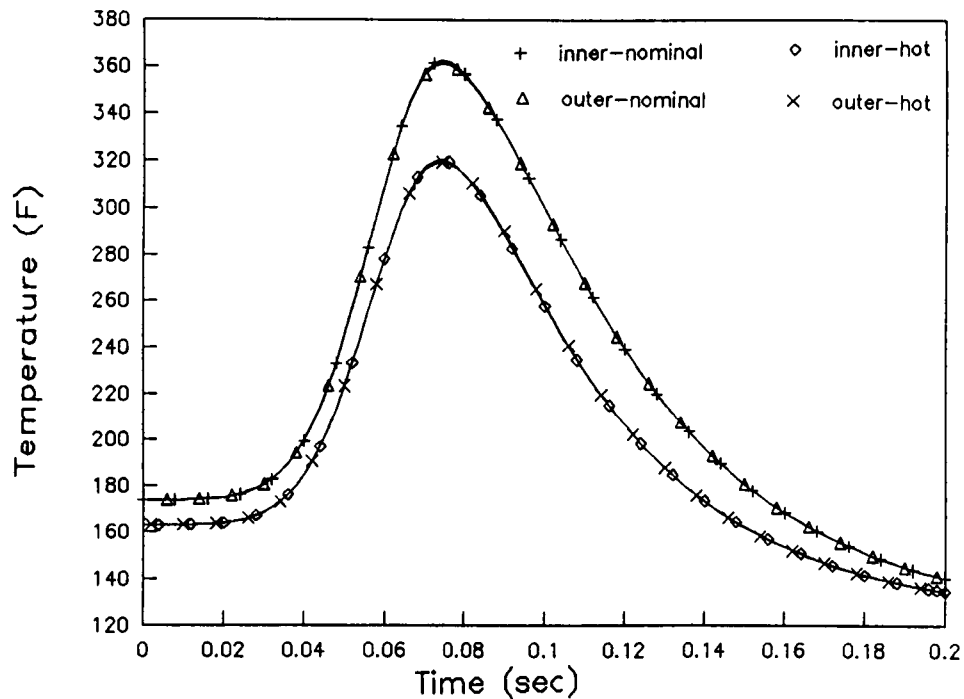


Figure F60 : Outlet node liquid phase temperatures during optimum void accident for EOC, Mode 1, full-power transient.

VITA

Tanju Sofu was born in Ankara, Turkey on June 23, 1959. He attended several elementary and secondary level schools in Istanbul, Ankara, Erzurum, and graduated from Polatli High School, Ankara, Turkey in June 1976. He received his B.S. degree in Physics in June 1982, and the M.S. degree in Nuclear Engineering in June 1986 both from Hacettepe University, Ankara, Turkey. After attending four quarters at the University of Cincinnati, Cincinnati, Ohio, he transferred to the University of Tennessee, Knoxville, Tennessee where he received his Ph.D. in Nuclear Engineering in December 1992. During his doctoral studies he held a research assistantship with the Oak Ridge National Laboratory which is operated by Martin Marietta Energy Systems. He accepted a position with the Argonne National Laboratory, Reactor Analysis Division, in Argonne, Illinois.