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Generic BWR-4 Degraded Core In-Vessel Study ----- Status Report -----

Project Staff:

R. P. Anderson
L. L. Briggs
F. B. Cheung
J. M. Kramer
K. R. Merckx†
J. H. Tessier
T. Y. C. Wei
R. A. Wigeland

Light Water Reactor Systems Analysis Section
Reactor Analysis and Safety Division
ARGONNE NATIONAL LABORATORY
9700 South Cass Avenue
Argonne, Illinois 60439

†Currently at Exxon Nuclear Company, Richland, Washington.

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Abstract

The original intent of this project was to produce a phenomenological study of the in-vessel degradation which occurs during the TQUX and TQUV sequences for a generic BWR-4 from the initiation of the FSAR Chapter 15 operational transient through core debris bed formation to the failure of the primary pressure boundary. Bounding calculations were to be performed for the two high pressure and low pressure non-LOCA scenarios to assess the uncertainties in the current state of knowledge regarding the source terms for containment integrity studies. Source terms as such were defined in terms of hydrogen generation, unreacted metal, and coolant inventory, and in terms of the form, sequencing and mode of dispersal through the primary vessel boundary. Fission product release was not to be considered as part of this study.

Premature termination of the project, however, led to the discontinuation of work on an "as is" basis. Work on the in-core phase from the point of scram to core debris bed formation was largely completed. A preliminary scoping calculation on the debris bed phase had been initiated. This report documents the status of the study at termination. The results show that:

- The in-core/intact pin phase for the TQUX and TQUV sequences is of the order of $3-4 \times 10^3$ seconds.
- The differences between the various FSAR Chapter 15 transients with the possible exception of the control rod drop transient which was not examined, should be treatable in the context of a small variation in initial coolant inventory.
- The core heat up is essentially adiabatic.
- The hydrogen generation is dominated by steam starvation and that an upper bound would be $\sim 5-10\%$ of the metal content oxidized with the production of a few hundred lbs of hydrogen.
- Early core disruption due to the failure of vessel upper internals is unlikely. Core disruption and debris bed formation upon the lower tie plate due to clad thermal degradation is more likely and occurs in $3-4 \times 10^3$ seconds after scram.

- The possibility of energetic fuel coolant interaction in the high/average power subassemblies will require a more detailed fuel motion model than the time constraints allowed in this study.
- A significant fraction of the core, the low power subassemblies, has boiled dry to the lower fuel tie plate (if there are no interactive effects from disrupted higher power subassemblies) prior to fuel pin disruption in those subassemblies thus reducing the probability of fuel coolant interaction during this phase (however a more comprehensive examination of the axial heat losses should be performed).
- Primary boundary failure is not likely to occur during the in-core phase.
- The scoping calculation for the debris bed phase indicated a time of $\sim 5 \times 10^3$ seconds for the boil dry of the coolant inventory in the lower plenum.

While the effects of the different operational transients, of varying heat transfer coefficients, of treating two dimensional radial incoherency effects, and of axial power shapes were considered in this study, the project was prematurely terminated so extrapolation to generalized conclusions should be treated with caution.

1.0 Introduction

The original intent of this project was to perform a phenomenological study of the progression of core degradation in the TQV and TQX events for a generic BWR-4 from the initiation of the FSAR Chapter 15 operational transient event to the dispersal of core material through the pressure boundary. The study was to identify and evaluate the controlling phenomena, assess uncertainties in the current state of phenomenological modeling and to examine their impact upon the accuracy of conclusions drawn from computations utilizing these models [1.1-1.7] through the accident phases of coolant boil off, core uncover and meltdown, failure of internal structures and loss of integrity of the primary pressure boundary. These results would have been interpreted in the sense of source terms which would determine the loading, both thermal and hydraulic, for the containment structures. Quantity, mode and form of dispersion into the containment coupled with the timing sequence was the formulation with which the source terms from this assessment of the in-vessel degradation phase would have interfaced with the various containment loading investigations. Where possible within the uncertainties of current knowledge an initial objective of a range of $\pm 50\%$ with a confidence limit of one σ was set for the source terms of; corium and water ejection, hydrogen production and missile generation. The final source term, fission product release [1-15] is only mentioned here as a matter of completeness as it is outside the scope of this study. In addition to the relative timing sequences of the various sources, issues which need to be explored are:

- 1) Corium
 - (a) The composition and in particular the amount of unreacted metal.
 - (b) The ejection rate and mode of dispersal from the primary system: droplet aerosolization dispersal vs. liquid stream pour vs. massive liquid/debris bed drop.
 - (c) Presence of water in the mixture.
- 2) Hydrogen
 - (a) Amount of hydrogen.

- (b) Other hazardous material generated à la the hydrogen via chemical reactions.

3) Missiles

- (a) The role of steam explosions.
- (b) In-vessel hydrogen reactions.
- (c) Any remote possibility of neutronic bursts.

And finally as a matter of comprehensiveness

4) Fission Products

- (a) Partial bypass of the suppression pool.
- (b) Amount tied up in primary system at point of corium ejection.
- (c) Solution in water/fuel matrix retention.

As initially envisioned, with the exception of (4), this would be the scope and perspective of the generic BWR4 degraded core in-vessel study.

Premature termination of project funding however has significantly reduced the range of the work documented in this report. Work was terminated in an "as is" condition. Consequently general conclusions to be drawn are limited. Although this was the case it was determined that sufficient effort had been expended in performing scoping calculations, in evaluating the available experimental database and in assessing the current understanding of the various incore phenomena that it would be of value, as an internal ANL matter, to document the current status of the project. For both the TQUV and TQUX sequences [1.18] analytical calculations and standalone computations have been performed from the transient initiation to the point of debris bed formation on the lower tie plate within intact subassemblies. RETRAN code calculations were performed for different initiating transients ranging from turbine trip without bypass to seized recirculation pumps to investigate differences in initial conditions. Extensive, but not comprehensive, parametric computations have been carried out with a simple thermal code, specifically written for the purpose, to examine the effects of uncertainties in a number of physical parameters on variables such as total hydrogen generation. Some exploratory efforts were made with regard to core slumping rates and the

consequent implications for steam explosions. Finally a scoping calculation was made of debris bed dryout and dispersion into the lower plenum region. This report documents that work.

Chapter 2 discusses the range of FSAR Chapter 15 transients which initiate the TQUV and TQUX sequences. Differences in thermal-hydraulic conditions after scram for the spectrum of transients are detailed as they are used for initial conditions for the remainder of the sequences. Neutronic heating is examined in Chapter 3 with an emphasis on radial and axial incoherencies in power shapes and the behavior of decay heat with time as the core degrades. The coolant thermal/hydraulics model which provide the forcing functions for the computation of the pin chemical/thermal behavior is documented in Chapter 4. Analytical expressions are derived to bound the long time quasi-static boil off and core uncover phase and to investigate possible two-dimensional effects. Chapter 5 examines the chemical reactions thermodynamically possible, in particular those leading to the generation of hydrogen, and details the thermal stand alone pin/structure code specifically written to compute the chemical and thermal degradation of the core while still in pin geometry. An extensive survey was made of the current experimental data base in order to determine the justification for postulating certain fuel failure modes given the pin and coolant thermal conditions computed by the models discussed in Chapters 4 and 5. The conclusions from this survey and the physical fuel/clad disruption and motion scenario postulated based upon these conclusions are detailed in Chapter 6. With the generation of superheated steam as the fuel pins degrade and with the attendant possibility of energetic fuel coolant interactions the implications for mechanical loss of integrity of both internal structures and the primary pressure boundary need to be considered as such failures would impact branch points in the sequence of events. Chapter 7 is an assessment of the high temperature material failure data and a compilation of techniques required to analyze these beyond design limit failures. Chapter 8 discusses the possibility of steam explosions as the fuel pins slump into the remaining coolant while Chapter 9 integrates the entire spectrum of physical phenomena, discussed in the preceeding chapters, and takes the accident from the FSAR Chapter 15 operational transient, through coolant boiloff, structural heat-up, hydrogen production as pin degradation occurs to the point where the fuel

slumps and forms a debris bed on the lower tie plate in subassemblies which still have intact walls. The parametrics performed for this study are detailed in this chapter. Chapter 10 discusses the scoping analysis performed for debris bed behavior, core support plate thermal failure and debris accumulation on the lower head. As funding was terminated at this point all work was halted at this juncture in the accident sequence. What general conclusions that could be formulated at this stage are drawn in Chapter 11.

The remainder of this chapter is divided into two subsections, the first of which, §1.1, describes the BWR-4 plant in terms of in-vessel geometry and design conditions and relevant plant systems. Subsection 1.2 give an overall view of TQUV and TQUX.

1.1 BWR-4 Design

The BWR is a direct cycle machine and Fig. 1-1 shows the in-vessel geometry [1.9] with the important internal components. The vessel has several penetrations particularly in the bottom head where the control rod drives enter the vessel. The location and timing of primary boundary failure is impacted by the presence of these penetrations. Superheated steam flowing out off the safety/relief (S/R) valves could contribute to failure at steam line nozzles. The presence of the numerous control rod drive penetrations could lead to localized failures instead of massive lower head fracture when fuel debris beds/pools form on the bottom head. The upper internal structures are comprised of the dryer assembly, which is attached to the vessel wall, and the steam separator unit which form part of the upper shroud head which is in turn bolted to the core shroud. These units appear as substantial heat sinks to the flow of superheated steam but therefore are also susceptible to early failure and collapse upon the core. The seal between the dryer and the steam region in the upper downcomer is formed by the liquid level.

Figures 1-2 to 1-4 show the typical 8 x 8R fuel subassembly and fuel rod design. The lower tie plate provides a support for an intrasubassembly debris bed. The intricate arrangement for supporting the subassemblies is shown in Fig. 1-5 [1-8]. Each group of four subassemblies rests on a fuel support piece which contains the orifice and is in turn mainly supported by the control rod guide tubes. The core support plate primarily affords lateral

Figure 1-1. Reactor Vessel Internals

DIM IDENT	A	B	C	D	E	F	G	H
DIM INCHES	12.0	5.278	0.366	0.317	0.108	0.157	0.1575	0.648
DIM IDENT	I	J	K	L	M	N	O	P
DIM INCHES	0.832	0.410	4.875	0.167	1.982	0.668	0.188	0.025
DIM IDENT	Q	R	S	T	U	V		
DIM INCHES	0.483		0.388	0.961	0.531	0.720		

Figure 1-2. D-Lattice Dimensions

Figure 1-4. Fuel Assembly

Figure 1-5. BWR Core Support Structures

Figure 1-6. Fuel Bundle Geometry and Various Leakage Flow Paths [1-16]

restraint. Mechanical drawings obtained from Oak Ridge [1.10] show this in more detail. This support design has a number of implications for post incore degradation fuel debris motion. Figure 1-6 shows the orifice design. Major component dimensions are documented in Appendix A. Core dimensional information are shown in Figs. 1-2 and Table 1-1 [1-14]. Additional information was provided by TVA [1-11].

Figure 1-7 shows the relevant systems [1-12, 1-14] involved in the TQUV and TQUX sequences. The safety/relief valves relieve directly into the suppression pool thus providing a direct pathway for fission product filtering by the water in the pool. These valves also form part of the automatic depressurization system (ADS) which could be used to decrease vessel pressure to the low pressure ECCS (emergency core cooling system) design range. The diagram does not show the condensate booster and feedwater pumps in the feedwater line nor does it show the isolation valves on the steam line inboard and outboard of the drywell. It does show the ECCS systems and the drywell.

Thermal hydraulic conditions at nominal operation for a typical BWR-4 are shown in Fig. 1-8. These are the starting conditions for the analyses performed in this report.

1.2 Accident Sequences

The TQUV and TQUX sequences [1-18], chosen for the scope of this study, are sequences initiated by the non LOCA operational transients with scram demand and actuation. The transients range from turbine trip without bypass to simultaneous seized recirculation pumps and would normally bound the plant response in terms of thermal margin, overpressurization criteria and release rates. However, feedwater delivery is postulated to fail and as the boildown proceeds the various level trips signal in turn for MSIV closure and the initiation of the high pressure ECCS system. This in turn is assumed to fail at which point operator intervention will be required in order to prevent core uncover. There are a number of alternative means of coolant delivery but an investigation of those alternatives is not within the scope of this project [1-6, 1-17]. It will be assumed that for the TQUX sequence at this time in the accident, the operator fails to depressurize and thereby loses

Table 1-1. Fuel Data

Core	
Number of Fuel Assemblies	764
Fuel Cell Spacing (Control Rod Pitch), in.	12
Total Number of Fueled Rods	47368 ^a
Core Power Density (Rated Power) kW/l	49.15
Total Core Heat Transfer Area, ft ²	74871
Fuel Assembly Data	
Overall Length, in.	176.16
Nominal Active Fuel Length, in.	150 ^b
Fuel Rod Pitch, in.	0.640
Fuel Rod Spacing, in.	0.157
Fuel Bundle Heat Transfer Area, ft ²	98
Fuel Channel Wall Thickness, in.	0.100
Channel Width (Inside), in.	5.278
Fuel Rod Data	
Outside Diameter, in.	0.483
Cladding Inside Diameter, in.	0.419
Cladding Thickness, in.	0.032
Fission Gas Plenum Length, in.	9.48
Pellet Immersion Density, %TD	95
Pellet Outside Diameter, in.	0.410
Pellet Length, in.	0.410
Water Rod Data	
Outside Diameter, in.	0.591
Inside Diameter, in.	0.531

^aDoes not include two water rods in each assembly.

^bIncludes six inches of Natural U at the top and bottom of the fuel column.

1 CONTAINMENT	6 LOW PRESSURE COOLING INJECTION MODE OF RESIDUAL HEAT REMOVAL SYSTEM
2 DRYWELL	7 CONDENSATE STORAGE TANK
3 RPV	8 SERVICE WATER
4 SAFETY/RELIEF VALVE DEPRESSURIZATION	9 HIGH PRESSURE CORE SPRAY
5 LOW PRESSURE CORE SPRAY	

Figure 1-7. BWR Systems

Figure 1-8. Typical BWR Heat Balance (at rated power)

all means of coolant delivery to the primary system which then continues to boiloff and eventually reaches core degradation conditions. For the TQUV sequence, it will be presumed that the operator manually depressurizes, since the ADS will not actuate under the postulated conditions, to the design pressure range for the low pressure ECCS systems. These systems are then postulated to fail which results in no coolant delivery to the primary system and the TQUV sequence then proceeds along lines similar to the TQUX sequence, coolant boiloff and ultimate core degradation. TQUX and TQUV are therefore respectively high pressure and low pressure nonLOCA core degradation scenarios which can be divided into five or six phases; an operational transient period of a few tens of seconds during which scram occurs; a coolant boildown period; core uncover and degradation with essentially intact pin geometry; disruption of the pin geometry with either the formation of a intrasubassembly debris bed within intact can walls or an energetic fuel interaction disassembly; ultimate mobility of the debris bed through failure of the core support plate or the lower tie plate and the formation of a corium bed/pool on the lower vessel head; and finally dispersal of in-vessel material into the containment. Table 1-2 shows approximate times to reach specified thermodynamic states of the in-vessel material based on decay heat. These times do not consider chemical reaction heat and are arbitrary specifications for the internal enthalpic distributions. The work presented in this report sharpens those bounds and provides technical justification for the various enthalpic states.

There are a number of ambiguities or options involved with both the TQUV and TQUX sequences which have to be explored. The spectrum of operational transients and the effect of the choice of a particular "T" on initial post scram conditions for the remainder of the accident needs to be evaluated. The timing of loss of feedwater relative to the initiating transient is not definitive. For the purposes of this project it will be assumed that feedwater delivery is lost instantaneously at the initiation of the transient. This minimizes the coolant inventory. For TQUV the sequencing of the operator response has to be specified. It will be assumed that the operator responds in five minutes and depressurizes the system to 350 psi. This would be in the range of not only the low pressure ECCS but also possibly the condensate booster pump which would open up more alternatives. Five minutes would appear to be on the short end of the spectrum but as the results discussed later in

Table 1-2. Postulated Event Sequence

<u>Thermodynamic State</u>	<u>Time (seconds)[‡]</u>
1. Boil off all water (no superheat) except for lower downcomer water which cannot be utilized because of jet pump geometry. [†]	4500
2. (1) + Zr clad and UO ₂ at Zr melting temp (~ 2000°C).	6500
3. (2) + melt Zr clad*.	6500
4. (3) + support plate and bottom head at steel melting temp. (~ 1400°C). ^{††}	8000
5. (3) + Zr clad and UO ₂ at UO ₂ melting point (~ 2800°C).	8000
6. (5) + melt UO ₂ .	9500
7. (6) + melt bottom head.	11000

[‡]Time origin is point of scram.

[†]No superheat of steam.

*Zr clad melt is negligible.

^{††}To bring support plate to melt adds negligibly.

this report will show, to delay significantly longer could have adverse effects on core uncover. With the assumption of five minutes the core is still covered after depressurization to 350 psi and TQUV then proceeds along lines similar to TQUX.

Given the post scram conditions resulting from the specific operational transient chosen the accident evolves into a slow quasi-static boiloff. This evolution occurs within tens of seconds after scram when temperature profiles within the pins adjust to the decay heat levels, the recirculation pumps coastdown to a natural recirculation mode and the water level in the downcomer and the core is determined by hydrostatics. This is discussed in Chapters 2 and 4. During the quasi-static boiloff period when the core is still covered nothing of significance occurs as the system is in thermodynamic equilibrium. The safety/relief valves lift and the setpoint determines the pressure and thereby the temperature. Alternative scenarios are considered with automatic lifts and operator determined manual lifts of longer duration at lower frequencies. As the set point pressure is only ~100 psi higher than nominal operating pressure and the operational transients last only ~ few tens of seconds temperatures outside of the fuel pins do not change significantly from the nominal operating conditions. All the decay heat generated is removed via the valves. The flow patterns however do alter as the natural circulation mode is a low flow mode and there are two-dimensional intersubassembly effects due to the power incoherencies discussed in Chapter 3. The two-dimensional coolant hydraulics effects are detailed in Chapter 4.

Once core uncover occurs then superheating of the steam commences, with concomitant production of hydrogen, different levels in the subassemblies and interstitial regions are established and heat transfer to internal structures and nozzle regions through both radiation and convection become important. Radiative losses from the top of the core to the shroud head decreases the hydrogen production rate as the bottom of the core continues the boildown of the liquid level. Convective transfer to the steam line nozzle weakens the nozzle region and introduces the possibility of pressure boundary failure prior to fuel mobility. This would not only transform the TQUX scenario into a TQUV scenario at the drywell pressure but could also lead to a reduction in the fission product filtering efficiency of the suppression

pool. Fuel mobility at drywell pressures would delete the high pressure aerosolization fuel dispersal picture. Convective heat transfer to the internal structures, dryer and separator, would help protect the nozzle region from the superheated steam. Figure 1-9 shows the main steam flow paths [1-15]. The deterioration of mechanical strength of the upper internal structures and the subsequent collapse could be a mechanism for producing a debris bed out of a now much deteriorated core. Chapters 4, 5 and 7 detail these aspects of the accident during this phase.

The core mechanical condition is governed largely by the cladding behavior as irradiated BWR fuel at nominal burnup is widely fragmented. Unlike LOCA events the shattering of cladding through thermal stresses induced by the cycling of the level with cold ECCS injection does not appear to be the probable scenario for TQUV and TQUX. While there is a general heatup of the fuel pins and a cycling of the core water level with the cycling of the S/R valves inducing level swell, the level swell and the temperatures involved do not appear to be significant enough for thermal stress shattering of the clad. Rather it appears that the chemical reactions and the phase changes determine the mechanical strength of the cladding and consequently the criterion for the onset of both clad and fuel mobility. Chapter 6 examines these questions in detail.

With the mass flow rates and composition of the mobile fuel/clad mixture determined by the core slumping picture detailed in Chapter 6 there is a potential for energetic fuel coolant interactions as the pin mixture slumps into the coolant pool in the non power producing bottoms of the subassemblies. A preliminary assessment of that potential was made using mass flux rates which do not incorporate delays due to refreezing and remelting and jamming possibilities as the pin slumps. Results are presented in Chapter 8.

If the fuel coolant interaction is not sufficiently energetic enough to disrupt the geometry further an intrasubassembly debris bed is formed upon the lower tie plate bounded by intact can walls. The formation of the bed disrupts the generation of hydrogen as the superheating of the steam is reduced with the decrease in the active region height. Competing heat transfer processes, which lead to failure of subassembly walls, melt of the

Figure 1-9. Flowpaths for Steam [1-15]

lower tie plate and motion through the orifices, and mechanical failure of the core support plate determine the enthalpic and material composition as well as the mode of dispersal of the bed into the lower plenum region. Chapter 10 is a scoping analysis performed to resolve these questions. No further work was carried out for the remaining phases of the accident as funding was terminated at this stage.

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2.0 Initiation Transient

The TQUX and TQUV sequences are the "with scram" sequences and reactor scram is the logical reference point to use in these event sequences. Initial conditions required for analyzing the remainder of the accident consists of temperatures, pressure, flow rates and distribution of water inventory at scram. Each FSAR Chapter 15 operational transient will generate a unique set of such initial conditions but since the plant system is designed to meet thermal margin, pressurization and dose rate criteria all such sets are within a certain design envelope. Matters are complicated by the postulate of simultaneous loss of feedwater which is not normally considered. As most of these transients are over in ~ few tens of seconds an additional decrease in inventory of $\sim 50 \times 10^3$ lbs of water is possible with the assumption of loss of feedwater at the initiation of the transient. This is, however, compensated for by the inventory in the feedwater lines which is $\sim 50 \times 10^3$ lbs. It will be assumed that this inventory is available to the primary system.

To delineate the design envelope of initial conditions, then, the only requirement is to consider the normal category of Chapter 15 transients and select the limiting classes [2-1]. These categories of transients are:

- Pressure increase transients.
- Reactor vessel water temperature decrease.
- Reactor vessel coolant inventory decrease.
- Core coolant flow decrease.
- Core coolant flow increase.
- Positive reactivity insertion.

For the pressure initial condition the pressurization transients generator load rejection without bypass (GLRWOB) and MSIV closure are limiting. This then determines the bulk coolant temperature as the system is essentially in thermodynamic equilibrium. The limiting fuel pin temperatures should be determined by the power transients which could either be the GLRWOB or the positive reactivity insertion transients; in specific, control rod ejection. Coolant total inventory and distribution should be determined by the loss of feedwater and simultaneous seized recirculation pumps transients. The simul-

Figure 2-1. RETRAN Model Noding

taneous seized recirculation pumps transient has characteristics of a LOCA with flow to the core starved, and consequently has the possibility for core uncovering within the initial few seconds of the transient.

Once scram occurs all the transients evolve into a slow quasi-static boiloff state. The temperature profiles in the fuel pins decay to a quasi-static decay heat profile in several seconds; the recirculation pumps coast down to a hydrostatic natural recirculation flow in ten to twelve seconds and the pressure levels out at the relief valve set point thus also setting the bulk coolant temperature. As the transients are generally over in few tens of seconds structural temperatures are either at nominal operating temperatures or at the new bulk coolant temperature given the range of conduction time constants present. The only variable is the coolant inventory which then depends upon the transient considered. To provide this inventory a number of RETRAN02/MOD02 [2-2] computations were performed and an analytical calculation was made to draw bounding conclusions. The analytical calculation is discussed in Chapter 5 while the next subsection details the RETRAN results.

2.1 RETRAN02/MOD02 Model

The nodalization used in the RETRAN [2-3] calculation is shown in Fig. 2-1. The plant model utilizes the one-dimensional kinetics, the algebraic slip option and the non-equilibrium pressurizer model of RETRAN02/MOD02 and was used in licensing calculations in the best estimate mode. It has therefore been qualified against benchmark tests and has undergone extensive examination. Four cases have been run with RETRAN02/MOD02. These consist of three transients with the single failure of loss of feedwater and are GLRWOB, loss of feed and simultaneous seized recirculation pumps. The fourth case was performed for comparison purposes and this is the GLRWOB with normal feed. Premature project termination did not allow for a control rod ejection case.

Figures 2-2 to 2-5 show the pressure response. The loss of feed case has a number of differences with published GE results available in the open literature performed with either REDY or ODYN. While the calculation appears to be internally consistent there was insufficient time to reconcile the differences with GE results. The case is characterized by significant

Figure 2-2. Pressure Response GLRWOB Without Feedwater

BWR DEGRADED CORE FWFCF

Figure 2-3. Pressure Response Loss of Feedwater

Figure 2-4. Pressure Response Locked Rotor Without Feedwater

Figure 2-5. Pressure Response GLRWOB With Feedwater

bulk boiling throughout the primary system which could be the superficial reason for the differences. As the figures show the asymptotic pressure behavior for all the cases tend towards the relief valve set point.

Tables 2-1 to 2-4 are summaries of the coolant inventory at various times in the different transients. The times are essentially chosen to sample at three power levels. The first column is at $t=0$, the 100% power time, the second column is at $\sim 20\%$ power and the final column is at decay heat levels $\sim 6\%$ power where it is clear that scram has occurred. Reference should be made to Fig. 2-1 for the physical significance of the volume numbers.

The tables show that the number of prescram full power seconds, the decay heat and the fuel pin stored energy for this range of transients is worth from 20×10^3 to 50×10^3 lbs of water ($\sim 15\%$ of total inventory). The analytical estimates in Chapter 5 for water inventory after scram accounting for stored energy is in this range. As the decay power is worth ~ 200 lbs/sec at scram it is within the range of the estimate. If, therefore, the analytical estimate is used, a range of $\pm 10\%$ of the total inventory should be used for parametrics. While this may appear to be on the narrow side, Fig. 2-6 [2-4] will illustrate the reason for it being so. The trip systems are so designed that the plant scrams, on a level deviation of $+22"/-12"$ in order to protect the turbine from moisture damage. There is an order of ~ 20 ft of water about the core so the level set points translate into $+10\%/-5\%$. The system is designed such that inventory does not vary significantly. The simultaneous loss of feed complicates the issue but the RETRAN computations show that the range is still narrow. Differences in subcooling of $\sim 20^\circ\text{F}$ are only equivalent to $\sim$ few % of the latent heat of vaporization and should therefore be negligible.

The analytical estimate discussed in Chapter 5 will therefore be used to provide the initial conditions for the remainder of the accident for both the TQUV and TQUX sequences. Parametrics with a range of $\pm 10\%$ in the initial coolant inventory will be considered.

Table 2-1. GLRMOB No Feedwater

Volume		t=0 secs	t=2.5 secs	t=9.0 secs
Water	UDC	70	58	56
Inventory	#160	548 [†]	549.2	549
10 ³ lbs		2 ϕ	2 ϕ	2 ϕ
/Temperature	MDC	95	93	80
°F	#180	528	542.6	558
/Quality %		0	0	0.7
	UP + SP	19	19	20
	#125	549	567.3	558
		10	10	10
	Core Nodes	31	34.5	37.5
	Lower Plenum	106	106	104
	#100	529	531	542.5
		0	0	0

Max. fuel		1900	1900	1000
centerline				
temp. °F				
Power %		100	16	5.7
Pressure (#190)		1057	1200	1123
psi				

[†]2 ϕ = two phase.

Table 2-2. Loss of Feedwater

Volume		t=0 secs	t=2.5 secs	t=9.0 secs
Water	UDC	refer	92.7	99
Inventory	#160	to	544	544.8
10 ³ lbs		Table 2-1	2 ϕ [†]	2 ϕ
/Temperature	MDC		34.4	30
°F	#180		544	545
/Quality %			7.5	10
	UP + SP		17.5	20
	#125		545.1	545.9
			10.9	8.7
	Core Nodes		30	28
	Lower Plenum		100	83.5
	#100		545.4	548.6
			0	1.1
<hr/>				
Max. fuel			1690	1270
centerline				
temp. °F				
Power %			21	6
Pressure (#1)			1017	1024
psi				

[†]2 ϕ = two phase.

Table 2-3. Seized Recirculation Pump Rotors No Feedwater

Volume		t=0 secs	t=1.75 secs
Water	UDC	Refer to	87.7
Inventory	#160	Table 2-1	548
10 ³ lbs			24
/Temperature	MDC		94
°F	#180		533.8
/Quality %			
	UP + SP		12.4
	#125		548.5
			16
	Core Nodes		18
	Lower Plenum		106
	#100		529
			0
<hr/>			
Max. fuel			1790
centerline			
temp. °F			
Power %			17
Pressure (#1)			1040
psi			

†24 = 2 phase.

Table 2-4. GLRNOB With Normal Feedwater

Volume		t=0 secs	t=2.5 secs	t=9.0 secs
Water	UDC	Refer to	62	58
Inventory	#160	Table 2-1	549.2	548.6
10 ³ lbs			2†	2†
/Temperature	MDC		94	93.7
°F	#180		537.3	539
/Quality %			0	0
	UP + SP		19.8	19.9
	#125		568.3	557.7
			10	10
	Core Nodes		34	39
	Lower Plenum		106	105
	#100		530.4	536.8
			0	0

Max. fuel			1956	
centerline				
temp. °F				
Power %			16	5.8
Pressure (#1)			1221	1121
psf				

†2† = 2 phase.

Figure 2-6. Vessel Level Monitoring Systems Referenced to Active Fuel for a Typical BMR/4

References

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- 2-3 S. L. Forkner et al., "BWR Transient Analysis Model Utilizing the RETRAN Program," Tennessee Valley Authority Report, TVA-TR81-01 (December, 1981).
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3.0 NEUTRONICS

3.1 Introduction

The initial driving force of the degraded core accident is the heat generated by the decay of fission products after reactor scram. It is the decay heat which boils off the coolant inventory and which heats up the fuel, and causes further heatup through exothermic oxidation of the exposed cladding by steam thereby driving the pins toward disruption. Therefore, an accurate representation of the dependence of the decay heat on both space and time is essential for making realistic estimates of how the accident will progress. At the same time, the decay heat representation must be smooth enough to be used easily in computations.

The basic assumption made in this study in modeling the decay heat is separation of space and time. Making this assumption allows the decay heat expressions to be much simpler and easier to use than would otherwise be the case. Since no large changes would be expected to take place in the decay heat spatial profile until large-scale disruption occurs, the assumption should not compromise the accuracy of the calculation. Therefore, this assumption satisfies both of the requirements mentioned above.

3.2 Time Dependence of the Decay Heat

The best available information on the variation of the core-wide spatially averaged decay heat with time was found in Ref. 3-1. The decay heat data in Ref. 3-1 were generated for typical light water reactor conditions using the isotope generation and depletion code ORIGEN [Ref. 3-2]. These decay heat values were felt to be the best because they reflect the contribution of not only the fission products but also the actinides and their daughters and the light and structural elements. The decay heat data are presented in Table 3-1 over the time of interest in this degraded core study. As can be seen from the table, the light and structural elements make very little contribution to the total decay heat, but the actinides and their daughter products are quite important, accounting for about 10% of the decay heat over most of the time period.

TABLE 3-1 Decay Heat Values for a Typical LMR^a [Ref. 3-1]

Decay Heat As Percent of Full Power					
Time After Shutdown (sec)	Fission Products	Actinides	Light Elements and Structural	Total	Fission Products as % of Total
0.0	5.63	0.246	0.00160	5.88	95.8
1	5.23	0.246	0.00160	5.48	95.5
5	4.63	0.246	0.00157	4.88	94.9
10	4.26	0.245	0.00155	4.51	94.5
30	3.63	0.244	0.00153	3.88	93.7
60 (1 min)	3.21	0.242	0.00152	3.45	92.9
10 ²	2.89	0.239	0.00152	3.13	92.3
500	2.07	0.213	0.00152	2.28	90.6
10 ³	1.77	0.188	0.00151	1.96	90.3
3.6x10 ³ (1 hr)	1.20	0.121	0.00151	1.32	90.7
10 ⁴	0.852	0.0933	0.00150	0.947	90.0
2.16x10 ⁴ (6 hrs)	0.676	0.0881	0.00149	0.766	88.3

^aBurnup = 33,000 MWd/MT, power = 38 MW/MT, $\phi = 4.78 \times 10^{13} \text{ n/cm}^2\text{-s}$,
loading = 1 MTHM ²³⁵U/²³⁸U with ²³⁵U at 3.2 wt.%.

An alternative source of decay heat data is the current ANS decay heat standard [Ref. 3-3]. However, there are two disadvantages to using the ANS standard data: 1) the data are for the limiting case of infinite operating time prior to scram and 2) the data do not include the actinide contribution, and the effect of the actinides must be added on in an approximate manner. On the other hand, ORIGEN can directly calculate the actinide contribution, and it can generate data for finite burn times. In addition, ORIGEN has been benchmarked against the ANS standard data for the case of infinite burn time and consideration of fission product contribution only. Therefore, it seems clear that the ORIGEN calculation provides more accurate information than would be the case using the ANS standard directly.

There are some differences between the parameters of the "typical" LWR described in Ref. 3-1 and those of the reactor considered in this study, and therefore it is necessary to ensure that these differences do not prevent the decay heat data in Ref. 3-1 from being applicable to the study. The differences in question are the burn times (33,000 MWd/MT in Ref. 3-1, 19,300 MWd/MT in the study) and the isotopic compositions (3.2% U^{235} with no plutonium present initially in Ref. 3-1, 2.19% U^{235} with plutonium present in the study). According to Ref. 3-1, a 20% increase in burn length makes less than a 3% change in the decay heat values, and so the difference in burn times between Ref. 3-1 and the study case probably does not make more than a 5% difference in the decay heat values. In the case of the isotopic compositions, the uranium-plutonium mixture in the study would produce somewhat lower decay heat numbers than shown in Ref. 3-1, but again, the difference should be only a few percent. Thus, the ORIGEN data represent a conservative and fairly accurate estimate of the decay heat values which would be expected for the study core.

The discrete data points obtained from Ref. 3-1 need to be used to generate a representation of the decay heat as a continuous function of time, since the thermal-hydraulics calculations which must use the decay heat as input require a continuous form rather than a discrete one. An expression is presented in Ref. 3-4 which is attributed to Untermeyer and Weills [Ref. 3-5] which represents the data fairly well at early times but deviates considerably from the Ref. 3-1 data for times greater than about 100 s. However, by using

the same functional form as the expression from Ref. 3-5 but modifying the constants used in the expression, a function was developed which reproduces the data in Ref. 3-1 to within 10% over the time range need for this study (0 - 6 hours). The constants were determined largely by trial and error. This function is

$$\frac{P}{P_0} = 0.1 [(t + 8.5)^{-0.2} - 0.9612 (t + 7.76 \times 10^5)^{-0.2}],$$

where t is the time in seconds since reactor shutdown, P is the power at time t , and P_0 is the power at the time of shutdown. The good agreement between this function and the data from Ref. 3-1 can be easily seen in Fig. 3-1, in which the two are plotted.

Since the total heat generated from the start of the transient is also needed as a function of time by some of the study calculations, an integrated form of the decay heat data must be created. It is, of course, quite straightforward to integrate the functional fit presented above, but, as can be seen from Fig. 3-1, the function overpredicts the values from Ref. 3-1 for the first hundred seconds or so, then tends to underpredict them over the remainder of the range, so the integrated function needs to be checked against a numerical integration of the discrete data to be sure that the integrated function accurately represents the integral of the data points. The data points were integrated using a third-degree interpolation algorithm from Ref. 3-6. The accuracy of this algorithm was verified by applying it to discrete points taken directly from the functional fit and comparing the results to the analytically determined integral of the function. These calculations demonstrated that: 1) the interpolation algorithm is quite accurate if given a sufficient number of data points, 2) the integration of the data points using the algorithm gives a value which is about 10% higher than the analytical integral of the functional fit over most of the time period, and 3) more than half of this 10% error can be attributed to the relatively small number of data points available to the algorithm. Therefore, it was concluded that the analytical integral of the functional fit to the data points represents the integral of the data points quite well (within the $\pm 2\%$ accuracy of the data values themselves) and should be used to represent the integrated heat generation.

Figure 3-1. Comparison of ORIGEN-Generated Decay Heat Data and the Functional Fit to These Data Points.

These functional dependencies do not account for fission product migration, as the fission product source term was not within the scope of this study.

3.3 Spatial Dependence of the Decay Heat

The decay heat spatial shape is assumed identical to that of the reactor power just prior to scram. This assumption neglects any migration of fission products which may have taken place over the time of reactor operation and any movement of fission products during the course of the degraded core accident. However, the migration of fission products prior to reactor scram should be a fairly small effect, and no significant release of nonvolatile/nongaseous fission products should occur prior to fuel disruption, so the assumed spatial shape should be fairly accurate in this respect. This, however, neglects the issue of volatile/gaseous fission products (private communications have indicated that this component may be around 30% of the decay heat) which should be examined in future iterations.

A further approximation is made by assuming that the spatial shape can be represented in cylindrical geometry as a function of r and z only (no angular dependence) and that the function is separable in r and z . These assumptions have been tested against the power shape data available for the core used in the study (Ref. 3-7). These data are for a core average exposure of 19,300 MWd/MT at the end of cycle 6. Plots were made of the axial power shape taken at different radial locations, and these plots were compared to the radially-averaged axial power curve. These plots showed that the axial power shape does not vary significantly with radial location. Similar plots of the radial power shapes at various axial locations indicated that the radial power shape is fairly constant along the axial direction. Therefore, it is quite reasonable to separate the axial and radial dependence of the decay heat. In addition, the power shape data show that the power is only a weak function of angle, and so it is also valid to assume r - z geometry.

The power shape data available are in the form of discrete values at specific spatial locations. These can be used to create continuous, normalized functions of r and z which represent the decay heat shape. This was

done by applying least squares polynomial fits to the data. In the case of the radial function, a single fourth-order polynomial was found which fit the data to better than 4%; this function is

$$P_r(r) = 0.944 + 0.402r - 0.191 r^2 + 3.57 \times 10^{-2} r^3 - 2.36 \times 10^{-3} r^4,$$

$$0 \leq r \leq 7.899 \text{ ft.}$$

The radial shape function and the radial average power data are plotted in Fig. 3-2. Note from the figure that the data points follow a nearly flat line through most of the core, then drop sharply at the periphery of the core (due to flux losses out of the core) and also drop somewhat at the core center (since the core is designed to operate with a reduced flux at the center in order to achieve a more even distribution of burnup). Therefore, it is feasible to separate the data points into three zones and use a low-order polynomial fit over each zone as follows:

$$P_r(r) = 0.955 + 0.281 r - 2.83 \times 10^{-2} r^2, \quad 0 \leq r \leq 1.1284 \text{ ft.},$$

$$P_r(r) = 1.244 - 1.55 \times 10^{-2} r, \quad 1.1284 \leq r \leq 5.642 \text{ ft.},$$

$$P_r(r) = -1.872 + 1.101r - 9.97 \times 10^{-2} r^2, \quad 5.642 \leq r \leq 7.8988 \text{ ft.}$$

Each function gives a more accurate representation of the shape of the data in the region in which it is applicable than does the fourth-order polynomial given above, and so these functions are used to represent the radial variation of decay heat in the calculations described in Chapter 5. Zone 1 is ~ 2% of the core while zones 2 and 3 are each ~ 49% of the subassemblies. The minimum power level for a low power subassembly is ~ one-half of the average power. A maximum power for the high power subassemblies is ~ 115% of average power.

The axial power distribution shows the same type of shape as does the radial one, but with much sharper drops at both ends. The flux drop at the top of the core is due to the reduction in the amount of moderator present as a result of the voiding which takes place near the top of a boiling water

Figure 3-2. Comparison of the Measured Radial Power Data to the Fourth-Order Polynomial Fit to These Data.

Figure 3-3. Comparison of Measured Axial Power Data to the Three Polynomial Fits to These Data.

reactor core, and the drop at the bottom is caused by the presence of the control rods. The power drops would be steeper still were it not for the fact that the core design includes reflector pellets at the top and bottom of the active region. Therefore, the axial decay heat data are also divided into three zones, and polynomial fits are found to the data in each zone. This approach gives the following three functions:

$$P_z(z) = -3.542 + 4.786z - 1.653 z^2 + 0.191 z^3, 1 \leq z \leq 3.08 \text{ ft.},$$

$$P_z(z) = 1.075 + 3.574 \times 10^{-2}z - 3.427 \times 10^{-3} z^2, 3.08 \leq z \leq 10.90 \text{ ft.},$$

$$P_z(z) = 44.47 - 11.95z + 1.106 z^2 - 3.442 \times 10^{-2} z^3, 10.90 \leq z \leq 13.5 \text{ ft.}$$

Each function fits the data in the appropriate region to better than 1%. These functions are plotted along with the axial power shape data in Fig. 3-3. Note that, because all three functions are low-order polynomials, they fit the data smoothly; by contrast, a single function which would fit the axial data would have to be a high-order polynomial which would introduce anomalous "wiggles" in between the discrete data points. Therefore, it is more satisfactory to use the three low-order functions than to try to find a single function to fit all the data.

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4. Coolant Thermal/Hydraulics

This chapter describes the transient thermal and hydraulic behavior of the water and steam in the reactor vessel during the loss of feedwater accident sequences. Both the high pressure TQUX (1100 psia) and low pressure TQUV (350 psia) cases are discussed. The initial conditions are obtained from steady-state full power operating conditions, and the calculations continue until there is no water remaining in the active core. Since the early stages of the transient are identical for both TQUX and TQUV, those events will be covered in one section. In this section, the cooling of the core from steady-state full power temperature levels and estimates of the water flow rate into the core are discussed.

Separate sections for TQUX and TQUV are then used for describing the later events in each sequence. For the high vessel pressure case, TQUX, there is a discussion of the estimates which were made of the time required for boiling enough water for the water level to drop below the top of active core. There is also a description of the models used for determining the void fraction in the core. The resulting void fraction profiles not only affect the time at which the top of active core is exposed, but they also influence the transient water level in the core as it dries out. Several other features of TQUX are discussed, including the effects of relief valve cycling, the relief valve pressure range, and the axial and radial power distributions. Finally, the heat transfer coefficients and some heat transfer calculations are reviewed.

For the low pressure case, TQUV, the initial estimates for the time required to depressurize the vessel are given. This is followed by a discussion of the estimates of the time required to expose the top of active core and of the transient water level in the core as the core dries out. There is also a brief review of the important heat transfer coefficients.

4.1 Early Stages of the Transient

This section describes the coolant behavior in the vessel during the transition from steady-state full power operation to quasistatic decay heat

level boiloff in the core. As mentioned above, the initial conditions are obtained from the steady-state results. This information is available from the output of the RETRAM calculations described in detail in Chapter 2. In the following descriptions, the geometry and volume designations used for the RETRAM calculations will also be used. Due to time constraints, the variation in initial coolant inventory discussed in Chapter 2 was not examined, although it is probably only a second order effect.

4.1.1 Initial conditions

At steady-state operation, there are 106,108 lbs. of water in the lower plenum, 98,573 lbs. on the core side (core, upper plenum, standpipes, and steam separators), and 176,440 lbs. on the return side (upper downcomer, middle downcomer, and jet pump volumes). Once the recirculation pumps have stopped the water in the lower downcomer (71,742 lbs.) and in the recirculation pump circuit would not be available until much later in the accident sequence, so these volumes are not included in the initial water inventory.

The water temperature on the core side is between 531 F and 551 F at steady-state, with an average of 546 F. The lower plenum water temperature is 529 F, while the average temperature on the return side is 537 F. The average temperature of the fuel pins in the core is 1244 F at full power and steady-state operation. For all of the calculations, the steam properties were taken from the ASME Steam Tables (4.1), while the material properties were taken from MATPRO-II (4.2).

4.1.2 Removal of the Steady-State Stored Energy

In both TQUX and TQUV accident sequences, it is assumed in this study that the feedwater is lost, the reactor is scrammed, the recirculation pumps are tripped, and the main steamline isolation valves are closed all at the same time. In the transition from steady-state operation to the slow boiling of the cooling water at decay heat levels, the most prominent event is the removal of the heat stored in the core, as the fuel pin temperature rapidly equilibrates with the water temperature.

Once the main steamline isolation valves have closed, the vessel pressurizes to at least 1131 psia, the set-point cited in the RETRAN input for the first group of relief valves. Depending on the details of the steam generation rate, it is possible that the pressure rises temporarily to open more relief valves for handling the volume of steam generated, although the maximum pressure is only 1151 psia. However, for the purposes of this study, the details of this process were not needed, as the only information required was the amount of water lost in this initial phase. Ignoring the detailed behavior during this stage should only introduce a minor error due mostly to the variation of thermodynamic variables with pressure, with the error in timing the accident sequence being much less than the accuracy of some of the later calculations.

The problem is then reduced to one of determining the amount of heat available for boiling water in the range of 1097-1131 psia when the fuel pin temperature is reduced from 1244 F to 556-560 F, the corresponding saturation temperature range. Using an average heat capacity for the fuel pins, averaged over both the temperature range and accounting for the different materials, the heat that needs to be removed to reduce the fuel temperature is 2.065×10^7 Btu. The use of average properties for this calculation resulted in an error of less than 5%.

This heat is transferred to the water in the core with part of the energy being used to raise the water temperature to the saturation temperature, and the rest used to provide the heat of vaporization. The estimate of the water boiled during this process is 32,285 lbs., again with an accuracy of 5%. It was assumed in this case that all of the water boiled came from the core side, where the average water temperature was higher, resulting in the largest quantity of water vaporized. It was subsequently shown that this will not have any impact on the overall result. All of the water boiled during the accident sequence has to be heated to the saturation temperature, so while the timing of the steam generation may be affected, there is no effect on the total quantity of steam generated in the first few minutes. However, this does raise the question of whether there is sufficient water in reality on the core side or not, or if the core will remain covered during this stage of the accident. For this reason, the flow rate into the core during this event was estimated.

4.1.3 Initial Water Flow Rate Into the Core

As described above, there are 98,573 lbs of water on the core side when the accident begins. However, 44,226 lbs are in the core bypass volume, and would not be expected to be available during a rapid event. This leaves 54,347 lbs, of which 32,285 lbs have boiled away, resulting in only 22,062 lbs remaining to cover the core. Even with the high void fractions that exist in the core at steady-state (over 70% near the top), there would not be enough water to keep the core covered. Since it is expected that the heat stored in the fuel pins will be removed in the first 20-30 seconds, based on the small time constant for the fuel pins, the question is how rapidly the water from the lower plenum and the downcomers can get into the core to make up the difference.

The flow rate into the core during these initial stages is controlled by the coastdown of the recirculation pumps and the difference in static pressure between the downcomer side and the core side. The effect of the difference in pressure will be discussed first, and is equivalent to the case where the pump shafts have seized and no coastdown occurs. In order to determine the flow rate due to this pressure difference, the major and minor losses for all of the components on the flow path between the downcomer regions and active core need to be evaluated. Loss coefficients for all of these components are available from the RETRAM input or are calculated in the RETRAM code. Although these coefficients were derived for steady-state conditions, they should give a good estimate of the initial flow rate into the core and determine whether or not the recirculation pumps are needed to keep the core from uncovering during these initial stages.

The minimum difference in water level between the downcomer and the core is about 10.5 ft. at steady-state operation. Considering only this effect and using all of the loss coefficients, this pressure difference translates into a flow rate of approximately $200 \text{ ft}^3/\text{sec}$, or 9200 lb/sec . Since only an additional 28,000 lbs of water, at most, is needed to keep the core covered (when there are no voids in the core), it would appear that there would be sufficient flow into the core to keep the core covered.

However, there is an additional complication of having higher pressure in the steam separator than in the steam dome. This pressure difference is more than sufficient to offset the difference in water level. The recirculating water causes a large pressure loss through the steam separator at steady-state operating conditions. This would be rapidly eliminated as the recirculating flow is stopped, although the timing is difficult to estimate. However, based on the amount of water in the standpipes and steam separators, and comparing this to the recirculating water flow rate at steady-state conditions, only a fraction of a second would be needed to remove the water from these regions. The pressures should also be equalizing on the same time scale, which is very small when compared to the other events. This result is used as the basis for many of the subsequent calculations, as it allows the water level in the downcomers to be directly related to the water level in the core by balancing the hydrostatic pressure between these two regions.

If these estimates are correct, then the coastdown of the recirculation pumps becomes relatively unimportant. Its only effect will be to raise the temperature of the water in the downcomer regions slightly. This would result in less water being boiled during the removal of the steady-state stored heat, as discussed previously, but the amount is small, only about 3000 lb when the time constant for the coastdown is 5-10 seconds. Assuming a coastdown of the recirculation pumps, heat is used to raise the water temperature in the middle downcomer region instead of boiling water in the core. Later in the accident sequence the water in the middle downcomer would be heated anyway by conduction through the shroud, standpipes and steam separators. Therefore, the overall steam generation and timing is the same using either approach, and is accurately estimated as a result.

The insensitivity of the amount of steam generated to the behavior of the recirculation pumps is explained by the initially uniform water temperature throughout the middle downcomer, lower downcomer and recirculation circuit. Then, since only the total amount of water available to the core is of interest, it does not matter whether the water comes out of the recirculation circuit or the middle downcomer or from the core side. However, the coastdown of the recirculation pumps provides additional assurances that the core will remain adequately covered during the initial stages of the accident.

As a result, during this initial stage, the core temperature is brought into equilibrium with the steam saturation temperature, the water in the downcomers has partially relocated so that there is no pressure difference driving more water into the core, and some water has boiled away and been vented, leaving 348,836 lb in the reactor vessel. Afterwards, the heat input to the water is supplied only by decay heat, and the process is one of boiling water at the saturation temperature. The steam generated is vented by cyclic operation of the pressure relief valves, so that a slow loss of water occurs. The timing of these subsequent events and the uncovering of the core are dependent on which scenario is being considered, either TQUX or TQUV. The details of these two cases are covered in the following sections.

4.2 TQUX: High Vessel Pressure

The high pressure case considers the events when the vessel is allowed to pressurize up to the relief valve settings. The first part of this sequence involves the boiling off of the water in the vessel, which is then vented periodically to maintain the vessel pressure within the cycling limits of the relief valves. For the present calculations, it is assumed that all of the power generated by decay heating goes into vaporization of water in the core, heating of water in the downcomer regions, and heating of the internal structures up to the steam saturation temperatures. As long as the core is covered, the fuel pins are kept at the saturation temperature, and these are the only areas where the decay heat can be absorbed. An estimate of the time at which the core would start to be uncovered is then obtained by a simple balance between the total amount of heat generated, the amount of water that has to be heated and vaporized and the amount of heat absorbed by the structures. The amount of water that needs to be vaporized can be estimated for all of the regions as a function of average void fraction in the core. A range of average void fraction in the core was used to obtain approximate times for exposing the top of the core. Accurate void distributions were then calculated using the decay heat levels at those times to obtain a better estimate of the time when the core starts to uncover. The additional effects of relief valve cycling and the radial power distribution were then added to this first estimate.

4.2.1 Time At Which Top Of Active Core Is Exposed

From the calculations discussed above, an estimate was obtained of the amount of water remaining in the vessel after removal of the steady-state stored energy. The method used in this calculation was to take the total amount of decay heat generated up to a particular time and balance this with the heat required for removing enough water to uncover the core and for heating the structure. The appropriate decay heat correlation was discussed in Chapter 3. At this stage of the calculation, an average power is used and the results are obtained by assuming that all subassemblies are identical.

At the time when the water level in the core will be at the top of active core, there will be different amounts of water in the core depending on the void fraction. The amount of water on the downcomer side is determined by balancing the pressure between the core and the return side so that there is no flow between them caused by a difference in static pressure, as discussed in Section 4.1.3. Therefore, the amount remaining in the downcomers and jet pumps will depend on the void fraction in the core, with the amounts still based on having no static pressure differential between the two regions.

As a result of the calculations, it was shown that most of the decay heat was used for the vaporization of water, as expected. Significant amounts were also used for raising the temperature of the water coming into the core from the lower plenum, heating the upper internal structures, and heating the water in the upper and middle downcomers. This last quantity would account for the recirculation pump coastdown effect of heating the downcomer water while boiling less initially, as discussed earlier. The total amount of heat absorbed by the water would be the same in any case. Minor amounts of heat were used for pressurizing the steam in the vessel and raising the temperature of the reactor vessel.

The results were that 8.53×10^7 Btu had to be supplied to start uncovering the core for the case where it was assumed that there was no void fraction in the core at all. This was taken as a limiting case, provid-

ing the earliest uncovering of the core. In obtaining this value, an average heat of vaporization was used, since the pressure varies over the range of 1097-1131 psia, based on the relief valve settings used for the RETRAN calculations. Once the steady-state stored heat had been removed, it was found that the steam generation rate was low enough so that only one relief valve was required to maintain vessel pressure, limiting the pressure range as a result.

Using the decay heat correlation described in Chapter 3, approximately 1070 seconds (17 min 50 sec) would be needed for this much heat to be generated. As mentioned above, this is a minimum time since it was assumed that there was no vapor in the core. After performing a detailed void fraction distribution calculation at 1070 seconds, using uniform axial and radial power distributions, it was determined that the average void fraction was 27%. With this average void fraction, the time to the start of core uncovering was extended to 1505 seconds (25 min 5 sec), and this time was used as the initial best estimate of the time required to start exposing the active core. Further improvements were made as more of the additional effects were included, such as the radial and axial power distributions, and the results of those calculations are included in a separate section.

4.2.2 Two-phase Flow Models For Void Fraction Calculations

As shown above, the void fraction in the core has a large influence on the overall timing of the accident. For this reason, a detailed two-phase flow model was used for determining the local void fraction. The model is mostly based on the unified approach discussed by Ishii and Zuber(4.3). However, some of the correlations were modified to facilitate computing, particularly with respect to the flow regimes.

Basically, the bubble shapes fall into three categories, depending on their size. Different correlations are used for each of these bubble shapes, either spherical, distorted, or cap. A terminal velocity for the isolated bubble is determined, which is then modified to account for the presence of other bubbles. The result is then further modified to account for the flow regime, either bubbly, churn-turbulent, slug, or drop flow. The

choice of flow regime is dependent on the local void fraction. This method was shown to give excellent agreement with experimental data on bubbling column experiments using air and water, such as those by Orth, et al. (4.4).

Due to the high pressure, it is expected that the bubbles will be fairly small. For bubbles in the range of 0.04-0.25 inches, their sizes indicate that they will be in the distorted particle regime. This regime is characterized by having the bubble rise velocity independent of the bubble size. Thus, the bubble size need not be known precisely for accurate calculations of the void fraction distribution. This size range also makes the bubbles smaller than the pin-to-pin spacing in the subassembly so that the flow regimes will more likely be either bubbly or churn-turbulent flow, rather than slug flow. For these reasons these are the bubble sizes considered for the present analyses.

For these flow regimes, the effect of having multiple-bubbles is accounted for by reducing the terminal velocity as the void fraction increases in the bubbly flow regime. For simplicity in these initial calculations, the churn-turbulent flow regime was approximated by using a maximum local void fraction for the bubbly flow regime of 50%. This value was derived from the experiments of Orth (4.4) for a problem of similar geometry, although a lower maximum value could be used. In the churn-turbulent flow regime, bubble agglomeration and coalescence result in much higher terminal velocities, which has the effect of limiting the maximum local void fraction in stationary boiling pools.

With these results, the models reduced to a very simple set of expressions which were readily solved. The terminal rise velocity is only a function of the water and steam properties, and the correction for increasing void fraction is a functional form dependent only on the void fraction. A plot of the calculated void fraction as a function of height in the core is shown in Figure 4.1. The heat generated in the first 10 inches is used to heat the incoming water to the saturation temperature. This profile is used for many of the subsequent calculations, as there was not sufficient time to incorporate a later, more accurate version.

Figure 4.1. Initial Estimate of the Void Fraction Distribution for an Average Power Subassembly at 1505 Seconds After Scram.

4.2.3 Transient Water Level in the Core

Once the void profile had been determined, it was possible to continue the calculation as the water in the core was boiled away. For these calculations, several assumptions were used. The fuel pin temperature below the water level is maintained at the saturation temperature since the fuel pin time constant is short, as described above. Decay heat is used to heat the fuel pin only after the fuel pin is exposed. With all of the decay heat in the covered part of the core being used for heating the incoming water and for boiling, the water level as a function of time is independent of the events occurring in the uncovered part of the core. Axial heat conduction is not considered in the present study, except for an approximation made for completely emptying the core; see Chapter 5. As mentioned above, the void profile is also held constant as the core uncovers since it is slowly changing and the greatest effects are in the upper part of the core which is rapidly voided. The present calculation will lead to faster emptying of the core since the void fraction decreases with time, and a higher void fraction results in a larger fraction of the core power being used to boil water.

A simple, explicit finite-difference technique is used to calculate the water level in the core. The power and water level at the present time step are used to determine the steam generation rate. Given the void profile and the water level in the downcomer and core bypass regions, this is converted into a rate for lowering the water level in the core. This rate is then used to calculate the water level in the core at the next time step, and the process is repeated. The results of the calculation for an average power subassembly with uniform axial and radial power is shown in Figure 4.2. This result was used as input to the computer code described in Chapter 5, where the details of fuel pin heatup and cladding oxidation during core dryout are discussed. The slight break in the curve occurs when the water on the return side empties the middle downcomer and moves into the jet pump where the cross-sectional area is much less. Once that happens, less water has to be evaporated for a given change in water level in the core. These results were also used to evaluate the effects of relief valve cycling, since the periodic variation in pressure was not included in the initial estimates.

Figure 4.2. Two-phase Mixture Level, Steam Generation Rate and Relief Valve Cycling Time for an Average Power Subassembly.

4.2.4 Relief Valve Cycling Effects

The cycling of the relief valve causes the pressure in the vessel to vary between 1097 psia, where the valve is set to close, and 1131 psia, where it is set to open. The different saturation conditions at these two pressures introduce additional effects into the accident sequence. Starting at 1131 psia, the relief valve opens and starts to vent steam. This causes the vessel pressure to drop to 1097 psia, where the time required depends on the vapor volume in the vessel and piping, with typical values in the range of 5 to 10 seconds.

While the valve is open and the pressure in the vessel is dropping, flashing of the water in the core will occur. Not only does this expand the water volume in the core, but it also lowers the temperature of the water in the core. For the present calculation, it is assumed that the system will always be in an equilibrium state. That is, when the pressure has dropped to 1097 psia, the water will be at the saturation temperature corresponding to 1097 psia, and the available heat has all been converted to steam. Since the water in the core is already boiling, and the pressure is dropping rather slowly, the water and steam should be very close to equilibrium. It is also assumed that the flashed steam will remain in the water while the valve is open, since most of it would not have had enough time to separate out. This has the overall effect of venting slightly more steam while the valve is open, due to the decreased volume available to the steam. This extends the cycling time slightly as well. Once the valve closes, the steam and water will separate to the extent that the normal void fraction profile is restored. While the steam that flashed during this process resulted in larger steam generation rates for the short period of time when the relief valve is open, the flashing also reduced the water temperature in the core. Once the valve has shut, the decay heat is divided between vaporizing more water and raising the water temperature back to the saturation temperature at 1131 psia. The overall result is that the increased steam generation rate while the vessel is depressurizing is balanced by a slightly reduced rate while the vessel repressurizes, and there is no net difference over the cycle.

The length of time that the valve is closed depends strongly on the steam generation rate. Initially, at 1505 seconds, the valve is closed for only about 18 seconds, with a total cycle time of 27 seconds. Later in the scenario, at 2505 seconds, the valve will be closed for 62 seconds, with a total cycle time of 68 seconds. As the steam generation rate falls off further, the cycle time becomes even larger, although the core is rapidly running out of water. The approximate cycling times are also indicated on Figure 4.2. These are only intended to indicate the relative timing of relief valve operation while the core is being uncovered, and do not represent the actual times when the valve would be open.

During the expansion of the water pool in the core as the water is flashing, there was a possibility that the expanding pool may be able to quench the rapidly heating cladding and cause premature failure of the fuel pins. As a result of the above calculations, it was shown that the pool expansion is only about 10% when momentum effects are ignored. Since the pressure drop is not that great, only 34 psi, and the time required for the drop is around 5-10 seconds, the momentum effect is not expected to be large. As the calculations discussed in Chapter 5 demonstrate, the cladding temperature even near the top of the core is not high enough for this sudden fracture to occur until there is only 1 or 2 feet of water left in the core. It does not seem conceivable that flashing at that stage would enable the water to be relocated to the top of active core. For this reason, the expansion of the pool is not considered to be a method for early cladding failure. The only effect of the additional expansion provided by momentum effects would be to lengthen the cycle time slightly, and as such does not need to be evaluated.

4.2.4.1 Effect of Varying the Pressure Range

The above analysis was carried out using the nominal values for the relief valve pressure settings and automatic operation. Other pressure ranges were investigated to determine their effect on the relief valve cycling process.

A range of 50 psia (1081-1131 psia) was selected as an upper bound for the automatic operation of the relief valves. At 1505

seconds, increasing the pressure range increases the relief valve cycling time to 41 seconds. The relative expansion of the water pool in the core is also increased from 10% to 21%. This increased expansion is not expected to have any impact on the early fracture of the fuel pin cladding, since the depressurization time is also increased to about 15 seconds which should make momentum effects small. The results at later times show corresponding increases in the relief valve cycling time.

There is also the possibility that the operator may intervene and manually operate the relief valves. In this case, a 200 psi range (931-1131 psia) was used. In this case, at 1505 seconds, the relief valve cycling time is 143 seconds, with the valve being open for 56 seconds. The amount of steam generated by flashing is correspondingly large, giving a pool expansion of about 80% or more, if none of the steam separated from the water. However, with the relief valve being open for such an extended period of time, the assumption that none of the steam created by flashing would separate from the pool is certainly incorrect, and the pool expansion would not be nearly as large. However, even with such a large expansion of the pool, it is not expected that the water would be able to quench cladding that was hot enough to fail. It should be mentioned that for this case, the pressure range is large enough to cause some minor changes in the overall timing of the accident sequence, but there was not sufficient time to calculate the results.

4.2.5 Effect of Axial and Radial Power Distributions

As discussed above, all of the previous calculations were done assuming a uniform power throughout the core and taking the average sub-assembly conditions. In this section, the effects of nonuniform power distribution are examined. First, the influence of the axial distribution is described.

4.2.5.1 Axial Power Distribution Effects

The effect of the axial power distribution is examined for the same conditions as used for the calculations in Section

Figure 4.3. Effect of Axial and Radial Power Distributions on Void Fraction Profiles at 1505 Seconds After Scram.

Figure 4.4. Two-phase Mixture Level and Steam Generation Rate for a Low Power Subassembly.

Figure 4.5. Two-phase Mixture Level and Steam Generation Rate for a High Power Subassembly.

4.2.2. The axial power distribution is available from the RETRAN calculations, as discussed in Chapter 3. For the present study, it is assumed that the power shape remains the same, regardless of the decay heat level. It is also assumed that the power is radially uniform at any axial height for the calculations described in this section.

The results are shown on Figure 4.3, and as a comparison with Fig. 4.1 indicates, there is not much difference between using an average power or using the detailed power shape. This is caused by the power shape being relatively constant in the lower part of the core, while it drops off in the upper part. In the lower part of the core, the void fraction distribution is mostly a function of the steam generation rate. In the upper part, the local void fraction is a function of both the local steam generation rate and the void fraction in the nodes below, which for this case has not changed much. As a result, the transient water level in the core is not significantly different from that calculated for the case shown in Figure 4.2.

4.2.5.2 Effects of the Radial Power Distribution

There are several effects of the radial power distribution. In this section, only the influence on the transient water level in the core will be discussed. As before, the axial power shape is assumed to be the same, regardless of the relative power of the subassembly.

Starting with the low power subassembly, a minimum value for the decay heat level is approximately one-half of the average power, as seen from the radial power distribution given in Chapter 3. This results in an average void fraction in the core of 12%, as compared to 27% for the average subassembly. The void fraction profile is also shown on Figure 4.3. The effect on the transient water level in the core and the steam generation rate is shown in Figure 4.4. The rapid drop at the start of uncovering the core is caused by the lower void fraction in the low power assemblies. The lower void fraction results in a higher pressure at the bottom of the subassembly as compared to the average subassembly. Water will flow out of the subassembly until the static pressure at the bottom of the subassembly is equal to that in the average subassembly.

For the high power subassembly, the situation is reversed. A maximum power for the high power subassemblies is about 115% of average power, again based on the radial distribution given in Chapter 3. The average void fraction in the high power subassembly is 29.3%, as compared to 27% in the average subassembly. This void fraction profile is also shown on Figure 4.3. The effect on the transient water level and the steam generation rate is shown in Figure 4.5. The slight delay in exposing the fuel pins is caused by the lower pressure at the bottom of the subassembly due to the higher void fraction. This lower pressure causes water from other lower power subassemblies to flow into the high power ones, resulting in a higher water level and a delay in uncovering the fuel pins in those subassemblies.

These results were also used as input to the computer code discussed in Chapter 5. The influence of the radial power distribution on the heatup of the core was demonstrated. In reality, because of the mixing in the upper plenum, the radial variation in power does not affect the upper structures very much.

4.2.5.3 Influence of Improved Void Profile Calculation

As discussed above, the void fraction profile used for the calculations was the initial best estimate. With the availability of more accurate physical properties, an improved estimate of the void fraction profile was made. The results are shown on Figure 4.6 for the average subassembly (with axial power variation but using a uniform radial power distribution), and for both high and low power subassemblies. The results are quite similar to the earlier cases, although the void fraction is uniformly higher due to a decrease in the terminal bubble rise velocity. Using these results, another aspect of the radial power distribution was investigated.

While the core is still covered, there would be downward flow in the low power subassemblies, and upward flow in the high power subassemblies due to the variation in void fraction between assemblies. Taking these velocities into account uniformizes the void fraction in the core, with the average void fraction just under 30%. The average power case, where the power was radially uniform, resulted in an average void fraction of 33%.

Figure 4.6. Axial Void Fraction Profiles Using Improved Calculation at 1580 Seconds.

Due to the nonlinearities in the models for the void fraction calculations, a higher value is calculated when the core is assumed to be radially uniform than when the radial variation is taken into account. Once the core is uncovered and the connection between the subassemblies is lost, though, the void fraction will differ greatly between the low and high power subassemblies, as discussed above. This additional 3% in the average void fraction would add about 60-70 seconds to the initial core uncover time, however.

4.2.6 Heat Transfer Coefficients and Calculations

In the process of performing the voiding core calculations, several heat transfer coefficients are required. After the core has started to uncover, the convective heat transfer coefficient between the steam and the rapidly heating fuel pins becomes important. As the steam becomes superheated, the heat transfer to the upper structures and the main steamlines also becomes important. These processes have been modeled by the computer code described in Chapter 5. In order to verify the computer modeling of these events, some simplified analytical results will also be described.

4.2.6.1 Heat Transfer Coefficients

The heat transfer coefficients were all derived from experimental correlations which have the following form, and which is sometimes identified as a Dittus-Boelter form;

$$\bar{h}_c = 0.023 \text{ Re}_D^{0.8} \text{ Pr}^{0.33} k/D$$

where $\bar{h}_c$ = average convective heat transfer coefficient, Btu/hrft²F
 Re_D = Reynolds number based on the diameter D
Pr = Prandtl number
k = thermal conductivity of the fluid, Btu/hrftF
D = diameter (approximate for interpin flow areas), ft

The values of Reynolds number depend on the particular flow situation, although the flow rates are generally very low. For this reason, the following laminar flow correlation is also used (Kreith 4-5):

$$\bar{h}_c = \frac{k}{D} \cdot \frac{RePrD}{4L} \ln \left[\frac{1}{1 - \frac{2.654}{Pr^{3/4} (RePrD/L)^{1/2}}} \right]$$

where L = length over which heat transfer coefficient is to be evaluated.

The value for the length was arbitrarily set at 0.5 ft, as the temperature is rapidly increasing in the axial direction, and there is a much greater effect on the heat transfer coefficient through the changes in thermophysical properties.

At the start of uncovering the core, the steam velocity in the subassembly is only 0.4 ft/sec, resulting in a Reynolds number of about 3500. Since this is probably turbulent, the above correlation was used to estimate $\bar{h}_c = 12$ Btu/hr ft² F. Even if the flow were laminar, the correlation above would give $\bar{h}_c = 10$ Btu/hr ft² F. Later in the accident, at 2505 seconds, the Reynolds number is only about 900. The laminar flow correlation then gives a value for $\bar{h}_c$ in the range of 4-5 Btu/hr ft² F. This drops even further as the steam generation rate continues to decrease, with a minimum value of around 2 Btu/hr ft² F expected.

In order to evaluate the overall heat transfer between the steam and the fuel pins, the conductive resistance must also be included, in general. For the present study, however, estimates showed that the resistance from the thermal conductivity of the cladding, gap, and fuel is very low compared to that for convection. Using a convective heat transfer coefficient of 10 Btu/hr ft² F, more than 92% of the overall heat transfer coefficient is due to convection. For this reason, considering the inaccuracies already introduced in evaluating the convective heat transfer coefficient, the convective heat transfer coefficient alone was used as the estimate for the overall heat transfer coefficient. These values were then used in the computer code results discussed in Chapter 5.

Similar calculations were performed for the heat transfer in the standpipes and steam separators. The Reynolds numbers are much higher in these regions, due to the relatively large diameter of the

pipes. Using the turbulent flow correlation given above, the heat transfer coefficient is about 13.5 Btu/hr ft² F when the core first starts to be uncovered. At 2505 seconds, this has dropped to about 3.7 Btu/hr ft² F. It is interesting to note that these are almost the same as those calculated for the core, indicating that the heat transfer coefficients seem to be proportional to the mass flux. This observation resulted in the heat transfer coefficients for the standpipes and steam separators being used for the steam dryers also. No detailed geometry was available for a more accurate determination in the steam dryer region, but in light of the above observations, it is believed that the error in making this assumption should be small.

For the vessel walls, it was assumed that there was no heat transfer between the steam and the walls. Over most of the vessel surface, the velocity of the steam would be negligible in comparison to the steam flowing out the relief valve, or even compared to the velocity in the core. The problem is controlled by conduction only, at least until buoyancy effects could take over. Any large scale instability would be interrupted by the periodic cycling of the relief valve. The 30-60 second period which occurs during this stage of the accident is rapid when compared to the movements expected from natural circulation. Once the core has been dried out, however, and there is little or no steam generation, this possibility would be considered again, but there was not enough time to pursue this stage of the accident.

The main steamline nozzle has to be treated differently due to the periodic nature of the flow in that region. In order to estimate the heat transfer coefficient for this region, an average coefficient over the relief valve cycle was calculated. Based on the relief valve capacity, the velocity in the steamline and in the vicinity of the nozzle was estimated to be about 30 ft/sec. Using turbulent boundary layer correlations for the nozzle, the average convective heat transfer coefficient for a one-half foot length is 395 Btu/hr ft² F. Assuming that there is no heat transfer during the time when the valve is closed, the overall heat transfer coefficient is estimated by taking this maximum value and multiplying it by the fraction of the cycle time that the valve is open. This results in the average heat transfer coefficient being about 130 Btu/hr ft² F at 1505 seconds,

when the core starts to uncover. The value drops to 35 Btu/hr ft² F at 2505 seconds since the relief valve cycle time is increasing, although the valve is open for about the same amount of time.

4.2.6.2 Heat Transfer Analysis

In this section, some simplified calculations are described which were used primarily for ensuring that the computer code discussed in Chapter 5 was giving reliable answers. Some of the calculations use simple finite difference techniques to obtain approximate answers.

4.2.6.2.1 Adiabatic Fuel Pin Temperature Rise

These calculations were performed to provide a lower limit for the time required to reach the cladding failure temperature. It also provided a maximum rate of temperature rise for the fuel pin, as long as decay heat was the only heat source. It was assumed that all of the decay heat went into heating of the fuel pin. The variation of heat capacity with temperature was also included. Two cases were done; one where just the fuel pins were heated, and the other where the subassembly wall material was also included with the fuel pins. These results were obtained using average power with no axial or radial profile.

The results of these calculations are shown on Figure 4.7. Extrapolating these results, the cladding temperature does not reach melting until about 1765 seconds after the uncovering of the core starts. If the subassembly walls are also included, melting of the cladding is delayed until about 2075 seconds after the start of uncovering the core. For the first few hundred seconds after the top of the core is exposed, these results give a good estimate of the upper limit for heating the fuel pins. As the cladding temperature increases, though, the reaction between steam and cladding will provide a significant quantity of heat. At that point, the computer results cannot be compared with these limiting calculations.

A quasi-static calculation was also performed, where all of the heat generated in the region where the core was exposed was assumed to be absorbed by the steam. Using the transient water level shown in Fig.

Figure 4.7. Adiabatic Fuel Pin Temperature Rise and Initial Estimate of Fuel Pin and Steam Temperature Rise at the Top of the Core.

4.2, the results of this calculation showed a steam temperature rising faster than the adiabatic fuel pin temperature. For such a rapid transient, the heat absorbed by the fuel is not negligible. Clearly, this indicated that the quasi-static assumption is not valid for this case, and that the proper limiting case is where adiabatic conditions are assumed for the fuel pins.

4.2.6.2.2 Initial Fuel Pin Temperature Rise

These calculations provided an estimate of the initial steam and fuel pin temperature rise at the top of the core. A finite-difference scheme was used to evaluate the temperatures. The problem was solved on a node-by-node basis, using RETRAN nodes. The time step was determined by the time required to void a given node. A balance was performed between the amount of heat generated, the amount of steam convected in that time, and the heat capacity of the fuel pins. The steam generation rate was used for estimating the amount of heat available to heat the fuel while the node was voiding. The convective heat transfer coefficient was $10 \text{ Btu/hr ft}^2 \text{ F}$, and was used to apportion the heat generated in a voided node between the fuel and the steam passing through.

The results of these calculations are also shown on Figure 4.7. These results were shown to agree very well with the computer model for the outlet steam temperature, although the fuel temperature estimate was too high. This was caused by the use of a uniform axial power for these calculations, while the computer code used the axial power profile. The power falls off fairly rapidly in the top node, resulting in a slower heatup of the fuel pin. The steam temperature is more of an integrated effect, and is not as sensitive to the uniform power assumption.

4.2.6.2.3 Heating of the Upper Internal Structure

This section describes the results for the analysis of the heating of the upper internal structures by superheated steam. The steam temperature for the steam exiting the core is obtained from the computer results in Chapter 5. The transient behavior is approximated by a ramp function, which is accurate up to the time when the cladding-steam reaction

becomes important. There is some error near the start as well, but by delaying the onset of the ramp function, this can be greatly reduced.

The rise in the structure temperature is dependent only on the difference between the steam and structure temperatures. The amount of heat transferred to the structure is related to the energy lost by the steam passing over the structure, and since the energy of the steam leaving the core is known, it was possible to solve a simple first order linear differential equation for the structure temperature as a function of time. The two equations for the first structure region are as follows:

$$m_1 C_{p1} \frac{dT_1}{dt} = hA(T_{s1} - T_1)$$

$$hA(T_{s1} - T_1) = wC_p(T_{sc0} - T_{s10})$$

where m_1 = mass of region 1
 C_{p1} = heat capacity of region 1
 T_1 = temperature of region 1
 h = heat transfer coefficient
 T_{s1} = temperature of the steam in region 1
 w = steam mass flow rate
 T_{sc0} = steam temperature at core exit
 C_p = steam heat capacity
 T_{s10} = steam outlet temperature from region 1.

These two equations are combined and solved analytically. The temperature rise calculated by the solution of these equations is summarized in Table 4.1. The numerical results are obtained by averaging all of the nodes in the first upper structure region from the computer code output. As can be seen, the agreement is quite good.

Table 4.1 Temperature rise in the upper structure

<u>Time, sec</u>	<u>Analytical result</u>	<u>Computer model</u>
0	0	0
100	5.1 F	7.4 F
200	18.5 F	19.7 F
300	38.6 F	38.4 F
400	64.4 F	62.6 F

Similar results were also obtained for the second upper internal structure, the steam dryer. The results are summarized in Table 5.2.

Table 4.2 Temperature rise in the steam dryer

<u>Time, sec</u>	<u>Analytical result</u>	<u>Computer model</u>
0	0	0
100	7.8 F	10.9 F
200	28.4 F	28.2 F
300	52.1 F	50.2 F

Considering all of the nonlinearities associated with the material properties, and the large volumes used for these calculations, the agreement is excellent. This provides assurance that the modeling in the computer code is reliable.

The steam dryer, steam separator, and the stand-pipes were assumed to have uniform temperature through the walls of the material due to the thinness of these components. For the shroud head, however, there was some question about the accuracy of this approach. An analytical solution was then used to estimate the error.

Using the 2 inch thickness of the shroud head, it was modeled as a uniform slab with the same thickness. The resulting area in

contact with the steam was about the same since the reel shroud head is perforated with large holes, and the sides of those holes contribute significant heat transfer area. Using an analytical solution for such a slab subjected to a linear increase in steam temperature, with constant properties and constant heat transfer coefficient (Carslaw and Jaeger(4.6)), the estimated temperature rise was obtained. In comparison with the numerical results, the agreement on the temperature rise was within 10%. This demonstrated that treating the shroud head as having a single uniform temperature was adequate for the present study, and the details of the temperature gradient across the shroud head were not needed for estimating the temperature rise of these upper structures in the vessel.

4.3 TQUV: Low Vessel Pressure

This section describes the results on the low pressure scenario. For this situation, the following sequence of events was used. Up to 5 minutes after scram, the events are identical to those of the high pressure case. At 5 minutes, the operator decides to depressurize the vessel. For this study, the final pressure is assumed to be 350 psia. After the vessel has been depressurized, the pressure is maintained at 350 psia, and the core is dried out in the same manner as in the high pressure case.

4.3.1 Depressurizing the Vessel

In order to depressurize the vessel, it was decided that four of the relief valves would be opened by the operator at 5 minutes into the accident. This provides one relief valve open in each of the four main steamlines leaving the vessel. The capacity of these four valves is sufficient to finish the depressurization of the vessel while there is still enough water in the vessel to keep the core covered after they are shut. As a first approximation the steam vented from the system is estimated by performing a series of isentropic expansions. Since the losses associated with these expansions are low, the isentropic assumption should work reasonably well. After each expansion, the properties of the steam left in the core is averaged based on the relative masses, and a new thermodynamic starting point is established for the next expansion. These expansions were done in steps of

100 psia beginning at 1100 psia. The last step to 350 psia was only 50 psi. In this way, some of the irreversibility associated with the steam absorbing heat from the internal structures was included. It should be emphasized that this was only the initial estimate for TQUV, and time was not available for refining the vessel depressurization calculation.

The water inventory in the reactor vessel at 5 minutes after scram was estimated at 380,907 lb. including the water in the lower downcomers. All of this water will be involved in flashing as the vessel depressurizes, since the saturation temperature will drop to only 431 F at 350 psia. The energy available for flashing water is evaluated by taking the difference in saturation enthalpy for the water as the pressure is reduced. The mass of water flashed is then added to the initial steam inventory. The final steam inventory at the lower pressure of each step is determined, and the difference is the amount vented by the relief valves. At the same time the reactor decay heat is still boiling water, adding more steam to the system which was vented by the relief valves. The relief valve capacity is evaluated at the midpoint pressure of each isentropic expansion.

The results for depressurizing the vessel are shown on Figure 4.8. Using information provided in the RETRAN output, and the results of Murdock and Bauman(4.7), the relief valve capacity as a function of pressure was determined. Approximating the capacity as a linear function of pressure was found to be accurate to within 1% of the available data, so a linear dependence was used. As shown on the graphs, the pressure drops most rapidly at first, mostly because the relief valve capacity is highest for those pressures. It was found that 3 min 45 sec was required for depressurizing the vessel.

At this time the operator shuts the relief valves, causing the flashing to stop. Steam generation is only caused by decay heat in the core from this point on. While the vessel was depressurizing, the large amount of water being vaporized most likely expanded the water pool to some extent, although it was assumed that none of the water was carried out of the vessel by the escaping steam. Once the valves are closed, the pool will collapse to the void profile that can be sustained by the boiling in the core. The void

Figure 4.8. Vessel Depressurization Using Four Relief Valves Starting at 300 Seconds After Scram.

profile was calculated using the same model described in Section 4.2.2. At the end of the depressurization, it was found that the core was still covered, but that the top would be uncovered about 200 seconds after the pressure reached 350 psia.

4.3.2 Transient Water Level in the Core

It was determined that the top of the core would be uncovered at approximately 725 seconds after scram. The void fraction profile in the core at this time is shown in Figure 4.9. These results were obtained for an average radial power, although the axial power distribution was included. As shown on the graph, the maximum of 50% is reached very rapidly. This is caused by the much larger change in volume at 350 psia as compared to 1100 psia. It is not known at this time whether or not the maximum of 50% is reasonable in this case. There was not time enough to investigate the effect of this maximum on the overall results, but for comparison with TQUX, it was kept at 50%. The resulting core average void fraction is almost 44%, and this was used to determine that the core would start to be uncovered at 725 seconds.

The drop in water level and the steam generation rate in the core is shown in Figure 4.10. The results are quite similar to those obtained for TQUX. These results were used for the initial calculations of the core heatup at low pressure using the computer code described in Chapter 5. The curves are very smooth in this case, since the water level in the return side is in the jet pump for the entire sequence. The striking similarity between TQUX and TQUV results is fortuitous. The core is emptying at an earlier time in TQUV, which corresponds to higher decay heat levels. The heat of vaporization is also higher, partially compensating for the increased power. The void fraction in the core is much higher as well, and less water needs to be vaporized for a given change in water level. The overall result looks very similar. If the depressurization had been started at 10 minutes instead of 5 minutes, the results would probably be quite different, but there was no time to investigate this further.

Figure 4.9. Initial Estimate of the Void Fraction Distribution for an Average Power Subassembly in the Low Pressure Case TQV at 525 Seconds After Scram.

Figure 4.10. Two-phase Mixture Level and Steam Generation Rate for an Average Power Subassembly in the Low Pressure Case TQW.

4.3.3 Heat Transfer Coefficients

Since the physical properties are different at 350 psia as compared to 1100 psia, new heat transfer coefficients were determined. The equations used in Section 4.2.6.1 were also used here. Due to the increased volume change when the water is converted to steam, the velocity in the core is significantly higher for TQUV as compared to TQUX. This assures that the flow will be turbulent. The results of these calculations showed that the convective heat transfer coefficients are approximately one-half as large as those used for TQUX. Since only an initial analysis was to be done, this was considered sufficient for the computer code calculations, as discussed in Chapter 5.

References:

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5.0 Chemical Reactions and Thermal Models

5.1 Chemical Reactions

Many chemical reactions between a variety of interacting materials may occur during a hypothetical severe accident in an operating reactor. This was recognized early in the reactor program and concerns that certain energetic reactions might occur led to a rather vigorous series of experiments and analyses by several investigators during the 1950's and 1960's. Both laboratory tests and in-reactor experiments were conducted, leading to a considerable base of understanding and associated data for those reactions having potentially significant consequences. An excellent summary of the state of knowledge gained up to early 1969 can be found in Chapter 17 and its Addendum of Technology of Nuclear Reactor Safety, Vol. 2 [5-1].

More recent studies were prompted by concern for understanding core response to postulated large-break loss of coolant accidents. Since emergency core cooling systems were assumed to perform as designed, the emphasis was placed on the potential for steam oxidation of cladding in the 1100 to 1580°C range with the large quantities of hydrogen that might be generated. Finally, the need for knowledge was broadened and accelerated by the TMI-2 accident in 1979 leading to large programs established by both government (NRC) and industry (IDCOR) to determine the consequences of postulated accidents that could lead to core degradation including meltdown. A plethora of related reports and papers (e.g. Ref. 5-2) have been published in recent years covering calculated accident sequences for a variety of initiators that lead to severe core damage, relocation of core materials and penetration of the reactor vessel leading to mechanical and thermal loads on the containment building, in some cases considered sufficient to cause its failure. In these studies, significant chemical effects i.e. those that significantly add to degradation of materials or potentially add to containment loads are generally treated at each stage of the accident progression under study.

It was the original intent of this study to include such an assessment of relevant chemical effects for each major phase of the in-vessel scenario. Table 5.1 is a comprehensive list of all in-vessel material which

Table 5.1 In-Vessel Materials

SAS33 GR B CC 1339

304 SS

Inconel 600 SIB66

Inconel 600 SBI68

SA508 CA CC1332

SA106 Grade B

SA802 GR B

Zircaloy-2

Zircaloy-4

Inconel-X

Uranium-oxide

Gadolinia

Fission Products

Water

Boron Carbide

Helium

Table 5.2 Core Heat-up Event Sequence

Event	T, °C (Spencer)	T, °C† (Other)
Threshold for steam-zircaloy reaction	800	
Cladding becomes plastic and either balloons (external underpressure) or collapses (external overpressure)	850	
Threshold for stainless steel (SS) steam reaction	1100	
Threshold for solid-solid UO ₂ -Zry reaction*	1400	<1000 (KFK 4-20)
Stainless steel-steam reaction becomes very rapid	1400	
Steam-zircaloy reaction becomes very rapid	1400	1580 (Baker 4-4)
304-SS melts		1400
Zircaloy (Beta and α-Zr(O)) melts	1850-1975	

†The values in this column override.

*Spencer [5-23] claims limited solubility potential as the reason for the high threshold temperature. Hofmann [5-19] does not mention this effect, but does point out the need for sufficient external overpressure to collapse the cladding onto the fuel for good solid-solid contact below 1500°C.

should therefore be considered as candidates for chemical reactions. However, constraints of funding have limited this treatment only to the point of initial core disruption and hence the in-core heatup phase following the beginning of uncover is addressed. Table 5.2 shows the possible chemical reactions and phase changes and important temperatures relevant to those phenomena. Boron Carbide and Gadolina reactions have not been considered. During this phase it is considered that only two chemical reactions occur that substantially affect the scenario, viz., the reaction between zircaloy and steam and between zircaloy and UO_2 .

5.1.1 Zircaloy-Steam Reaction

5.1.1.1 General Considerations

The core of a typical BWR contains a large amount of zircaloy metal used for structural purposes as fuel rod cladding and sub-assembly wrapper cans. The cladding alone amounts to more than 76,000 pounds. If all this cladding were to be oxidized by steam, more than 3,300 pounds of H_2 would be liberated and the highly exothermic reaction would generate more than 2×10^5 MW-sec of heat. The destructive potential of such large amounts of hydrogen and heat could considerably exacerbate an already severe accident condition. For certain accident scenarios, conditions could be further worsened by oxidation of the zircaloy subassembly wrapper. However, physical constraints exist to limit the extent of the reaction that actually occurs. Phenomena such as steam starvation and hydrogen blanketing have to be considered.

5.1.1.2 Thermodynamics and Rate Laws

Zirconium, as well as several other elements used in nuclear components, are relatively high in the electromotive series and hence react with oxidizing agents such as water. The complete reaction with steam is:

There is a large negative Gibbs free energy associated with this reaction indicating a strong tendency to go to completion. The reaction is exothermic releasing 6300 to 6530 J/g of Zr oxidized.

As stated earlier, because of concerns that such a reaction could become violent, especially at the elevated temperatures associated with severe nuclear accidents, many studies have been conducted to determine the reaction kinetics. Two documents have been published recently that provide excellent summaries of work conducted to date with some recommendations regarding preferred correlations and needs for added information; references 5-3 and 5-4. Early isothermal experiments led to the derivation of a parabolic rate equation as representation of the data. This equation can be expressed as:

$$W^2 = A \cdot t \cdot \exp(-B/RT)$$

where ; with a consistent set of units:

w = mass of Zr oxidized per unit area of surface - (mg-Zr/cm²)

A = experimentally determined pre-exponential factor - (mg-Zr/cm²)²/s

t = reaction time - s

B = activation energy - J/mole

R = universal gas constant - J/K-mole

This parabolic law is plausible for the bulk of the experiments performed which were designed to ensure an adequate supply of steam at the surface of the oxide during the tests. It is then postulated that oxidation rate is limited by solid-state diffusion which as the parabolic law asserts is inversely proportional to the thickness of the oxide layer.

Reference 5-4 summarizes a number of studies which, along with the early ANL analyses [5-6] provide the results of Table 5-3.

Table 5-3. Parabolic Rate Law Constants for Zircaloy

Investigator	Rate Constant (mg Zr/cm ²) ² /s at			Pre-exp. Factor x 10 ⁻⁶	Activation Energy kJ/mole
	1852°C	1580°C	1000°C		
Baker-Just (5-6)	655	(143)	--	33.3	190.4
Suzuki (5-7)	--	60.2	0.39	3.80	170.3
Leistikow (5-7)	--	58.5	0.31	5.86	177.4
Brown (5-7)	--	58.3	0.38	3.64	170.1
Cathcart (5-8)	--	57.2	0.41	2.94	167.1
Lemmon (5-9)	--	45.9	0.68	0.47	142.2
Westerman (5-10)	--	42.2	0.58	0.52	145.2
Klepfer (5-11)	--	40.0	0.64	0.36	140.2
Ballinger (5-12)	--	35.9	0.58	0.31	139.6
Urbanic (5-13)	--	33.9	0.54	0.30	139.8

The value of the rate constant at 1580°C cited for the Baker-Just investigation is not reflective of a measured value but is calculated using their correlation. Note that it predicts a much larger value than the others. Recent studies have shown that a sharp transition in the oxidation rate occurs at 1580°C [5-13]. This temperature is the stability limit for the tetragonal oxide phase; beyond this temperature the cubic oxide phase becomes stable. Because, as stated earlier, the oxidation rate is believed to be controlled by solid-state diffusion of oxygen through the oxide layer, the change in crystalline structure is assumed to cause the sharp increase in rate observed at 1580°C. These recent results have led Baker to conclude that the Baker-Just correlation over predicts the oxidation rate below 1580°C and is somewhat conservative above that temperature. He also points out that the Cathcart correlation is more conservative than others and was obtained with great care and precision. Baker, therefore, recommends the use of the Cathcart correlation in the temperature range up to 1580°C and the Baker-Just correlation for higher temperatures.

Evidence of other rate limiting mechanisms have been the subject of several investigations. Limitation of the gaseous diffusion of steam through the evolving hydrogen was postulated early [5-6] especially for

high temperatures where the solid-state reaction rate becomes large. However, in the relatively high temperatures tests of Baker-Just, the generated hydrogen rapidly became the predominant gas constituent causing a hydrogen-rich atmosphere but without apparent deviation from the parabolic rate law. Also, other tests have shown no dependence on total pressure at temperatures above 1000°C [5-14]. Thus, for a period of time it was concluded that the reaction does not depend on the steam-hydrogen ratio except at very high temperatures where gaseous diffusion could be important.

Quite recently, tests performed at ANL indicate rate reductions caused by excess hydrogen introduced into the experimental system [5-15, 5-16, 5-17]. Low temperature tests showed a small but definite effect which was verified as due to inhibition by hydrogen by conducting companion tests with excess helium. However, some of the high temperature tests which showed very large rate reductions were not similarly verified and as Baker points out [5-5] the effect could be due to a gaseous diffusion limitation. Thus, the magnitude of rate inhibition by excess hydrogen blanketing is controversial and requires added experiments, especially at high temperatures, to resolve the issue.

Finally, Figure 5-1 graphically depicts the correlations recommended by Baker over the temperature range of interest. The large discontinuity in rate at 1580°C is clearly shown; these temperature dependent constants are used in the fuel pin thermal model and code described below.

5.1.2 Zircaloy-UO₂ Reaction

The definitive work to date in the study of the UO₂-Zry reaction has been done at the Kernforschungszentrum Karlsruhe (KfK). Several papers (Refs. 5-18,19,20) have resulted from this work, and it is from these that the information in this section has been taken, unless otherwise noted.

Figure 5-1. Zircaloy Rate Constants for Cathcart and Baker-Just Correlations.

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Zircaloy and UO_2 are known to be thermodynamically unstable with respect to each other, even at low temperatures. The reaction rate increases with temperature, however, so that, while the rate is low enough at normal reactor operating temperatures to make the reaction negligible, it is rapid enough at high temperatures to be of significance in accident analyses. Zircaloy can react with solid UO_2 as either a solid or a liquid; however, it is the solid-solid reaction which is of interest under degraded core accident conditions.

At low temperatures, and in the absence of any oxygen, zircaloy is in what is called the α -phase, or α -Zry. At about 800°C , the zircaloy begins to change phase and enters a condition in which both α -Zry and a second phase, β -Zry, coexist. Above about 950°C , the phase change is complete, and only β -Zry is present. However, if oxygen is added to either α -Zry or β -Zry, the result is a stabilized form of the α -phase, denoted α -Zr(O), which can exist at higher temperatures; and the higher the oxygen content, the higher the temperature at which α -Zr(O) is stable.

The UO_2 -Zry reaction creates α -Zr(O) when the zircaloy reduces the UO_2 and liberates the necessary oxygen. Metallic uranium is also produced and reacts with zirconium to form a (U,Zr) alloy. These materials separate out into three layers as follows: an α -Zr(O) layer in which some of the (U,Zr) alloy is present (this layer is next to the unreacted UO_2), a layer of (U,Zr) alloy, and finally, a layer of α -Zr(O) which contains no (U,Zr) and which borders the unreacted zircaloy region. The (U,Zr) layer forms when the elemental U formed by reduction of UO_2 by zircaloy flows as a liquid along the α -Zr(O) grain boundaries and interacts with the zirconium to create a (U,Zr) alloy which is liquid above about 1150°C . It is not immediately obvious that this process would form a structure of two α -Zr(O) layers sandwiching a (U,Zr) layer; rather, a two-layer configuration of an α -Zr(O) layer followed by a (U,Zr) layer might seem more plausible. However, the interfacial energies of these two structures are such that the three-layer sequence is energetically more stable than is the two-layer one, and so the three-layer sequence is the one which is experimentally observed.

An important additional result of the UO_2 -Zry reaction is extensive formation of uranium on the UO_2 grain boundaries. The formation of uranium completely destroys the UO_2 grain bond, so that, once the cladding melts, the molten material can penetrate not only the cracks in the fuel pellets but also the grain boundaries on which the bond has been destroyed, making dispersal of the fuel in the molten cladding easier. This effect is of greatest importance near the UO_2 surface.

A prerequisite for the UO_2 -Zry reaction to take place is that there be good contact between the two solids. If there is not, the only path by which oxygen can pass between the UO_2 and the zircaloy is through the gas between the two solids, and this takes place at such a slow rate as to be negligible in the time frame of the degraded core accident.

The UO_2 -Zry reaction is important for two reasons: 1) the uptake of oxygen by the zirconium embrittles the cladding and 2) the melting point of $\alpha\text{-Zr(O)}$ is significantly higher than that of $\beta\text{-Zr}$ (1850°C for $\beta\text{-Zry}$, versus up to 1975°C for $\alpha\text{-Zr(O)}$), and so the creation of substantial amounts of $\alpha\text{-Zr(O)}$ in the cladding increases the cladding melting point and impacts the disruption scenario. Unlike the steam-Zry reaction, however, the UO_2 -Zry reaction does not release any significant amount of heat.

Just as in the case of the steam-zircaloy reaction, the rate at which the zircaloy reduces the fuel is determined by the diffusion of oxygen in the zircaloy. Therefore, the UO_2 -Zry reaction also follows a parabolic rate law, and, above about 1100°C , the two reactions proceed at about the same pace. The UO_2 -Zry reaction does not, however, experience any sudden increase in reaction rate such as that characteristic of the steam-Zry reaction around 1580°C . This is because the sharp increase in the steam-Zry reaction rate is due entirely to an increase in the rate of formation of ZrO_2 ; the rate at which the $\alpha\text{-Zr(O)}$ layer associated with the steam-Zry reaction forms has no such discontinuities [Ref. 5-21]. Since there is no ZrO_2 formation in the UO_2 -Zry reaction, the reaction rate does not take any sudden jumps.

Figure 5-2. $\text{UO}_2/\text{Zry-4}$ Reaction Zone Growth as a Function of Reciprocal Temperature (Ref. KfK).

The parabolic rate laws governing the growth of each of the reaction zones are presented graphically and in equation form in Fig. 5-2, which is taken from ref. 5-18. These formulae can be easily manipulated to give the rate of growth of any of the three layers.

5.2 Thermal Models

Early in this study it was concluded that a simple, stand-alone, thermal model and computer code would need to be developed in order to carry out the assigned work. Initial attention was given to heatup of the core during uncovering as caused by fission product decay heat and the zircaloy-steam exothermic reaction. The hoped for result was a computer code that adequately treats the physics of the problem in a highly understandable manner and was also efficient in terms of flexibility and computer time so that parametric studies could be made with reasonable expenditure of these resources.

5.2.1 Model and Code

The program language chosen was the IBM product, Continuous System Modeling Program (CSMP) [5-22] because of considerations of programming ease, availability of unique library routines and a variety of integration algorithms of varying degree of sophistication.

5.2.1.1 Core Fuel Pin Model

Initial modeling was of the core region for which a single pin and associated coolant channel was represented. The fuel pin and channel may be subdivided into a user selected number of axial segments. At the axial mid-plane of each axial segment, a single node represents the average temperature of the fuel, cladding and coolant. Change of phase is not simulated. Table 1.2 and the results using this code presented in §5.3 show that Zircalloy clad melting takes insignificant time (~ 5 minutes). The hydrogen generation will be overestimated slightly. Furthermore, Section 6.0 shows that the slumping rates are negligibly affected. Heat transport along the coolant channel is represented by a linearly extrapolated coolant convection term. Also, at each fuel and cladding node a heat source is provided,

representing nuclear decay and chemical heat generation respectively. In the latter case both the Cathcart and the Baker-Just zircaloy oxidation correlations are included in the program. Cathcart is used for cladding temperatures less than 1580°C and a switch provides changeover to Baker-Just at higher temperatures. For the nuclear decay heat an axial power shape was incorporated in the code (see Chapter 3, above). Values for the specific heat of fuel, cladding and steam and liquid water are temperature dependent as are the conductivities of fuel and cladding and the steam density. A constant value of the fuel to cladding gap conductance was input. The film heat transfer coefficient was input with a value calculated as described in Chapter 4. Also, input as forcing functions and described in Chapter 4 are the time dependent values of water level in the core and attendant steam generation rates. Since the radial power shape of the core is not flat, three such pairs of forcing functions are required to perform independent calculations of the effects of radial incoherencies. This was done for the TQUX scenario but only the average power case was calculated for TOUY.

For each case, the code calculates the total amount of zircaloy oxidized and mass of hydrogen generated. The mass rate of steam used to oxidize zircaloy is calculated for each axial segment; if this rate exceeds the inlet steam mass flow rate (i.e. the exit flow goes to zero), computation of the oxidation rate is switched from the aforementioned rate correlation and set equal to the inlet steam mass flow rate. When the inlet flow goes to zero, the oxidation process is terminated for that node. This is a model simplification for the phenomenon of steam starvation as a detailed nodal steam mass balance is not performed. The other extreme of triggering the shutoff on outlet flow was also programmed. This accounting of the steam is for control of the oxidation process only; for purpose of heat transfer the depleted steam should be replaced with an equal number of moles of hydrogen. However, hydrogen tracking has not yet been incorporated, so for the purposes of heat transfer, continued cooling by flowing steam at the input generation rate is assumed. This also means that the code does not provide for any treatment of oxidation rate limiting by hydrogen as discussed above. This had been planned as a feature to be added with project continuation. In its present form, therefore, it is assumed that the code overpredicts the oxidation rate and extent although the magnitude of overprediction is controversial.

For the accidents studied herein, low water inventories limit the total in-core zircaloy cladding oxidation that can occur. Our scoping calculations (see Section 9.0) indicate that only ~12% of the cladding reacts because the water inventory and attendant steam supply becomes exhausted. Because of this depletion, we also infer that for this accident there would be little added heat or hydrogen generated by in-core oxidation of the zircaloy subassembly wrapper cans. The can walls would become hot enough to participate chemically by heat transferred to them directly from the steam and by radiation from the fuel pins. However, by the time this occurs over any significant area of the can wall, the steam reaction with the cladding is vigorous enough to deplete the steam and hence, any oxidation of can wall metal would be essentially compensated by a reduction in the amount of cladding oxidized.

5.2.1.2 Upper Structures Models

Initial calculations with the above core model showed that very hot steam was generated. To escape the vessel, this steam must flow through relief valves located in the main steam lines. This escape path causes the steam to flow past upper internal structures viz., the upper shroud head, standpipes, separators, dryers and finally the steam line nozzle at vessel exit. Heating of these structures by the hot steam raised the possibility of loss-of-strength sufficient to cause their failure, including early breaching of the reactor vessel. Therefore, the thermal model was expanded to compute the temperatures of these structures as required by the stress analyses presented in Chapter 7.

The total model is depicted schematically in Figure 5-3 which shows its major elements. The equations employed are standard for such a lumped parameter approach and it was not felt warranted to present them here, especially considering the state of the project. The equations are contained in the computer program and source code listings provided in Appendix C for TQUX and Appendix D for TQUV conditions.

Figure 5-3. Major Element Schematic of Thermal Model

5.2.1.3 Model Qualification and Verification

Certain aspects of the total model described above were checked by both companion independent computer and analytical calculations and by sensitivity calculations with the code. The radial division of the fuel pin was suspected to be too coarse; however, a small, independent computer program that provided a detailed treatment of radial heat transfer in the pin showed very flat temperature profiles during similar transients and therefore confirmed the adequacy of radial nodalization used. The pin model was further confirmed by analytical solution for limiting conditions. Similarly, analytical calculations of the temperature response of upper structures to the passage of hot steam over their surfaces confirmed the modeling of those regions (see Chapter 4).

With regard to the axial nodalization of the fuel pin, sensitivity calculations were made varying the number of nodes from five to twenty. The results of these studies are shown in Fig. 5-4 which shows close agreement in the amount of hydrogen generated for all cases. These results are for steam starvation triggered on node inlet flow. Corresponding results were obtained for triggering on node outlet flow. As to be expected those results approached the asymptotic limit from the lower bound and indicated that 10 axial nodes provided reasonable values. With this nodalization the oxidation front propagation is slow enough that the nodal steam inventory is of lesser importance. The mass of hydrogen calculated is a sensitive barometer of integrated code performance.

Because the ten node case shows some conservatism above that for twenty and ran much more efficiently, it was decided to use ten axial nodes for all subsequent calculations.

To summarize, the code provides standalone multi-axial node thermal calculations for the fuel pin, the upper structure and the steam line nozzle with convection heat transfer from the flowing steam coolant exiting the nozzles. Axial conduction is neglected and the radial conduction is treated by a lumped parameter approach. The transients are long compared to the conduction time constants so the lumped approach is reasonable. The

analytical code calculations in Chapters 4 and 6 and Appendix E verify this. Those analytical/code calculations also show that the axial conduction in the pin cladding is only important when the core water level drops to ~ 0.5 ft. Hydraulic conditions are forced upon the code from the results obtained in Chapter 4 with water level and steam flow rate histories and convective heat transfer coefficients being input. Simplifications are made for the steam mass balance and the sensitivity calculations show reasonable agreement. The calculations also show that 10 axial nodes should suffice. Variations in axial power distribution can be considered.

It is concluded that this model should provide reasonable estimates of the in-core heatup phases of the TQUX and TQUV BWR scenarios. The code runs at a reasonably efficient rate with a typical CPU/transient-time ratio of ~ 0.15 . This is for a variable time step routine using a fourth order Runge-Kutta algorithm coupled with Simpson's method for a estimator. Parametric variations are readily accomplished. A chief deficiency in the code currently is the lack of treatment of the effects of hydrogen generation and its inhibiting effects on the steam-zircaloy reaction.

5.3 TQUX/TQUV Results

The computer code described above was used to calculate several core and upper structure heatup cases for the TQUX scenario and one calculation for TQUV. Comparative results from these calculations are summarized in this subsection. Table 5-4 shows the matrix of parametrics performed for this study to obtain the effects on the amount of hydrogen generated, the quantity of zircaloy left unreacted, the mass flux rate of disrupted core material slumping into the incore coolant pool, and the temperatures of the shroud head and steam line nozzle. The high pressure cases are the TQUX cases (~ 1130 psi) while the low pressure case is the TQUV case (~ 350 psi) discussed in Section 4. Axial power shapes are either flat or the nominal shape detailed in Section 3 with partial control rod insertion. The differentiation between average, low and high power subassemblies, utilized to introduce radial incoherencies into the study, is also detailed in Sections 3.3 and 4.2.5.2. Sections 4.2.6.1 and 4.3.3 discuss the variation in heat transfer coefficients. Table 5.5 summarizes that variation.

Figure 5-4. Parametrics Matrix

Pressure	Axial Power Shape	Subassembly Total Power	Heat Transfer Coefficients	Case #
High (TQUX)	Nominal	Average	High	1
			Low	2
			Average	3
		High	High core [†]	4
			Low core	5
			Average core	6
		Low	High core	7
			Low core	8
			Average core	9
	Flat	Average	Average	
<hr/>				
Low (TQUV)	Nominal	Average	Average	10

[†]Mixing in the core outlet plenum implies that parametrics not involving the average subassembly should only vary core heat transfer coefficients. Varying heat transfer coefficients in the upper internal is only consistent for the average core behavior.

Table 5-4. Heat Transfer Coefficient Range TQUX

Location/(Btu/hrft ² *F)	Maximum	Nominal	Minimum
Core	12	10	2
Standpipes and steam separators	13.5	10	3.7
Nozzle and steam outlet pipe	130	100	35.2

This section presents results obtained from the average subassembly power level TQUX "base case" (#3) to illustrate the general core behavior and other phenomena. The nominal axial power shape detailed in Chapter 3 is used as well as the nominal heat transfer coefficients discussed in Chapter 4. The following six figures depict typical results.

Figure 5-5 shows the fuel (TF) and cladding (TC) temperatures at the axial midplane of the uppermost node in the core and the steam temperature at core exit (TSOUT). The time origin for all code results is the onset of core uncover. The shape of these temperature curves are typical for the condition of initial heatup due only to decay heat, a large increase in slope as the steam-zircaloy reaction becomes vigorous and then an abrupt reduction when the region becomes depleted of steam. The two sources of heat at that location, decay heat (QF) and chemical (QC) are shown in Fig. 5-6. This figure clearly shows the rapid increase in oxidation as the cladding becomes hot with the heat of reaction becoming the dominant source temporarily. The abrupt cutoff is due to steam depletion.

Figure 5-7 and 5-8 show the core-averaged mass of cladding (WGLBS) oxidized for this transient and the amount of hydrogen (WH2LBS) generated thereby. As mentioned earlier the total in-core reaction involves ~ 12% of the cladding mass in the active fuel region. The reaction stops when the water level in the core drops to the bottom of active fuel and steam generation ceases.

The final two figures for this case, 5-9 and 5-10, show the temperature response of the upper shroud head (TUH) and steam line exit nozzle (TUP3) respectively. As mentioned earlier, these structures are heated by the passage of hot steam flowing over their surfaces as it escapes the vessel through safety relief valves. As the decay heat decreases the rate of temperature increase diminishes as it compensates for the drop in steam flow rate. At ~ 1600 seconds the oxidation becomes significant and the rate of temperature change increases. This change then propagates through the downstream heat sinks with an appropriate delay. The final flat top of these curves is due to the eventual depletion of the core water inventory.

Figure 5-5. TQX Average Power--Steam, Clad and Fuel Temperatures at Top of Core.

Figure 5-6. TQUX Average Power--Fuel and Clad Heat Sources at Top of Core.

Figure 5-7. TQUX Average Power--Mass of Cladding Reacted by Steam.

Figure 5-8. TQUX Average Power--Mass of Hydrogen Generated by Steam-Zircaloy Reaction.

Figure 5-9. TQUX Average Power--Upper Shroud Head Temperature.

Figure 5-10. TQUX Average Power--Steam Line Exit Nozzle Temperature.

It should be noted that values of the two-phase mixture level and steam generation rate vs. time are presented in Ch. 4 above for the period of 2000 s after beginning of core uncover. To calculate the core heatup beyond this time the associated curves were extrapolated by hand in lieu of additional calculations. This was considered appropriate because the low water levels would tend to make two dimensional heat transfer effects significant and the complication of adding axial heat conduction in the fuel pins was deemed unwarranted. Reference should be made to Appendix B where it can be seen that for boiloff two dimensional pin heat transfer effects can be neglected upto levels of 0.5 ft.

Thus, the curves for water level and steam generation were curve fit by hand out to 3000 s; beyond ~1400 s the level curves are coincident for all cases in this study. The slope of the water level curve at 3000 s was used to finally extrapolate the level to zero which was achieved at 3350 s. For each case, a constant value of the steam generation rate equal to the extrapolated value at 3000 s was used up to 3350 s at which time it was set equal to zero. Beyond this time calculated values of inlet or exit steam temperatures for any region are not meaningful. The calculation becomes one of adiabatic heatup whereas there would be radiative and convective heat losses from the top and bottom of the core.

The high power case with the average heat transfer coefficient (Case #6) is illustrated in Figs. 5-11 to 5-14. With the exception of the upper shroud head and steam line nozzle temperatures, for which there are no plots, these graphs are the equivalents of Figs. 5-5 to 5-10. It can be seen that the ~15% increase in power does not lead to drastic differences in the results either in temperatures or in hydrogen generation rates. The time scales are similar. The nodal temperature rates increase from ~1°F/sec to 10°F/sec once the transition in the oxidation correlation is reached and steam starvation occurs soon thereafter. The upturn in the hydrogen production appears to occur earlier.

The low power parametric with the average heat transfer coefficient (Case #9) while exhibiting the same general characteristics does introduce a delay in the time scales. Figures 5-15 to 5-18 are the corresponding graphs

Figure 5-11. TQUX High Power--Steam, Clad and Fuel Temperatures at Top of Core.

Figure 5-12: TQX High Power--Fuel and Clad Heat Sources at Top of Core.

Figure 5-13. TQUX High Power--Mass of Cladding Reacted by Steam.

Figure 5-14. TQUX High Power--Mass of Hydrogen Generated by Steam-Zircaloy Reaction.

Figure 5-15. TQUX Low Power--Steam, Clad and Fuel Temperatures at Top of Core.

Figure 5-16. TQIX Low Power---Fuel and Clad Heat Sources at Top of Core.

Figure 5-17. TQUX Low Power--Mass of Cladding Reacted by Steam.

Figure 5-18. TQUX Low Power--Mass of Hydrogen Generated by Steam-Zircaloy Reaction.

for the low power subassembly. It can be inferred from these figures that the water level has dropped to the lower fuel tie plate even before the pin temperature has reached 2400°F. As will be discussed in Chapter 6 this result has implications for the problem of fuel coolant interactions. However, as noted earlier the modelling has deficiencies as the boil dry condition is reached. It is recommended that pin two dimensional heat transfer and top and bottom losses be reexamined in future studies.

The parametrics in the heat transfer coefficient for TQUX show one complication in the pin thermal response. While the heatup is basically adiabatic, the outlet positions are essentially being heated by the steam from the central hotter sections of the pin as the axial power shape plays a role. It is difficult to draw conclusions on temperatures when the core heat transfer coefficients are varied as this inversion exists. Chapter 6 which discusses the pin disruption picture will show this in greater detail. The effect upon hydrogen generation rate does not appear to be significant.

The corresponding figures for the corresponding TQUV (11) case are Figs. 5.19-5.24. While the water level history and steam generation rate, as discussed in Chapter 4, are quite similar to the TQUX sequence there are differences due to earlier core uncover and the low pressure effect on heat transfer coefficients. Core outlet steam temperature, the shroud head temperature and the steam line nozzle temperature are lower. The hydrogen generation rate appears to be somewhat higher and the fuel runs somewhat hotter. However, in general the two sequences have the same characteristics and about the same time scale.

Table 5.5 shows the total hydrogen generated for each of the parametrics listed in Table 5-4. A few cautionary notes are in order here. While the calculations model the steam starvation effect, the effect of hydrogen blanketing, whatever it may be, is not simulated. Furthermore, the values listed in column (2) are at the time the water inventory is depleted in the active core region. This is in general beyond the time of fuel pin disruption which would render the code invalid as the modeling assumes intact pin geometry. It however will not affect the conclusions drawn later regarding the

Figure 5-19. TQV Average Power--Steam, Clad and Fuel Temperatures at Top of Core.

Figure 5-20. TQUV Average Power--Fuel and Clad Heat Sources at Top of Core.

Figure 5-21. TQV Average Power--Mass of Cladding Reacted by Steam.

Figure 5-22: TQV Average Power--Mass of Hydrogen Generated by Steam-Zircaloy Reaction.

Figure 5-23. TQV Average Power--Upper Shroud Head Temperature.

Figure 5-24. TQV Average Power--Steam Line Exit Nozzle Temperature.

cladding oxidation. Column (1) is at the time of fuel pin disruption and shows that most of the hydrogen generated is produced prior to pin disruption. Fuel pin disruption will be addressed in Chapter 6. The results shown in Table 5-5 should be regarded as bounding limits. It can be concluded from the table that about the only parameter considered which has a significant effect on hydrogen generation is power level and that on the average during the incore phase ~ 10% of the zircaloy clad is oxidized. The hydrogen production is dominated by the phenomenon of steam starvation as all the steam boiled off, once accelerated oxidation commences, is consumed in the oxidation reaction.

Table 5-5. Hydrogen Generated and Zircaloy Oxidized by Steam*

Transient	Axial Shape	Subassembly Power	Heat Transfer Coefficients	(1)	Hydrogen Generated LBS (2)	(1)	Zircaloy Oxidized LBS (2)
TQUX	Nominal	Ave.	Ave.	300	388	6400	8827
TQUX	Nominal	Ave.	Low	320	386	7100	8782
TQUX	Nominal	Ave.	High	300	390	7200	8872
TQUX	Nominal	Low	Ave.	180	180	4095	4095
TQUX	Nominal	Low	Low	189	189	4300	4300
TQUX	Nominal	Low	High	179	179	4072	4072
TQUX	Nominal	High	Ave.	300	422	6400	9600
TQUX	Nominal	High	Low	310	444	7500	10100
TQUX	Nominal	High	High	290	424	6500	9646
TQUX	Flat	Ave.	Ave.	280†	370	6700	8300
TQUV	Nominal	Ave.	Ave.	275	350	6200	7692

*Note: Amounts calculated are for 47,368 active fuel pins which is the entire core (total clad + can wall zircaloy inventory ~ 60×10^3 kgm).

†Underestimated as node outlet steam flow trigger was used for steam starvation.

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6.0 FUEL/CLADDING MECHANICAL DISRUPTION AND MOTION

6.1 Postulated Scenario

In the early stages of the transient, the cladding serves to contain the fuel in a pin configuration. Because of the long period of time for which the fuel was irradiated prior to the start of the transient, the fuel is no longer in the form of smooth pellets; rather, the fuel has swollen, cracked, and fragmented due to the formation of fission products, so that, without the cladding to contain it, the fuel would simply collapse into a pile of rubble. Therefore, once the cladding weakens sufficiently at a given axial location, the pin geometry will be disrupted at that location, and the fuel and cladding will fall away under the influence of gravity. The critical issue, then, is to establish a criterion for deciding when the cladding has become too weak to maintain the pin geometry. In this study, such a criterion was derived from information gathered in a literature survey of experiments designed to simulate degraded core conditions.

The experiments surveyed included both in-pile tests which attempted to simulate the integrated progression of events in a degraded core transient and out-of-pile tests which focused on specific aspects of the degraded core conditions. The ideal source of information would have been an in-pile experiment which simulated degraded core conditions and used irradiated fuel rods. However, the only experiment which approaches this ideal is the Severe Fuel Damage Test 1-3 (ref. 6-1), which was run in July, 1984, and data are not yet available from this test. Therefore, results from in-pile tests with fresh fuel were evaluated in conjunction with data from out-of-pile tests which investigated the mechanical behavior of unirradiated fuel and cladding under thermal-hydraulic conditions similar to those expected during a degraded core transient. Also, in order to formulate at least a qualitative idea of how a degraded core test might progress differently if performed with irradiated pins rather than fresh ones, an examination was made of results from a test which did not simulate a degraded core but which did use both irradiated and fresh fuel pins. All of these experiments will be discussed in detail below.

The experimental results indicate the following sequence of events once core uncover has begun:

- 1) The zircaloy cladding begins to react with both the UO_2 and the steam, taking up oxygen from both. The UO_2 -zircaloy reaction is a reaction between two solids and hence depends on good contact between the solids; the contact should be quite good in the present situation due both to the swelling of the fuel from prior irradiation and the fact that the internal pin pressure is lower than the external steam pressure, causing the cladding to collapse onto the fuel once the cladding is hot enough to be in the plastic range.
- 2) The uptake of oxygen by the zircaloy from the fuel and steam results in the coexistence of several different phases of zircaloy as the temperature increases. In addition, the zircaloy-steam reaction creates a zirconium oxide layer along the outer edge of the cladding.
- 3) Eventually, the cladding becomes hot enough to begin melting. Because several different phases of zircaloy are now present in the cladding, the cladding will melt over a range of temperatures, since the different phases have different melting temperatures. The center of the cladding will melt first, with the edges melting last. The ZrO_2 layer has a much higher melting point than do any of the zircaloy phases, and so it will remain intact as the zircaloy melts.
- 4) Because the ZrO_2 layer remains solid as the remainder of the cladding melts, the molten material initially has no flow path and so does not move. However, once the oxygen-rich zircaloy layer next to the fuel melts, the molten cladding flows along the cracks in the fuel, creating a slurry of fuel fragments and molten zircaloy. The sharp-edged fuel fragments are now free to move around within the slurry and jam against the ZrO_2 layer. Since the ZrO_2 is quite brittle, it will shatter from the mechanical loading by the fuel. The fuel fragment-molten cladding slurry will then simply drop under the force of gravity into the water pool at the bottom of the core.

The criterion for predicting disruption, then, is that all phases of zircaloy shall have become molten; once this occurs, shattering of the ZrO_2 layer and subsequent pin disruption are imminent. This is equivalent to requiring that the cladding temperature have reached or exceeded the melting point of all phases of zircaloy present in the cladding. This maximum melting temperature is 1975°C.

Prior to the mobilization of the fuel fragments, no mechanism exists during the transient by which to subject the ZrO_2 layer to either a thermal or mechanical stress, and so the layer remains intact up until this point. This is quite different from the more widely studied loss-of-coolant accident (LOCA), in which the coolant inventory is lost very quickly, followed by heatup of the uncovered core (with the attendant formation of a ZrO_2 layer on the cladding) and then a rapid bottom reflooding which thermally shocks and shatters the (mostly oxidized) cladding in the lower regions of the core. As discussed in Chapter 4, some oscillation of the steam-water interface does occur during core uncover (see Fig. 4-2), but the oscillations are not large (the interface moves about ± 1 foot), and the small layer of oxide which builds up between peaks in the oscillations is not a big enough fraction of the cladding for the cladding to shatter.

At this point, a physical picture of how the accident progresses has been developed, as has a quantitative criterion for predicting the onset of fuel disruption. What is missing is a quantitative description of the accident progression, i.e., the rate at which the phenomena described above in items 1 through 4 occur. This description is provided by calculations performed with the simple thermal-hydraulics code discussed in Chapter 5. The details of these calculations are presented in Chapter 9. This code uses a lumped parameter approach which assumes a flat radial temperature profile in both the fuel and the cladding. In addition, it does not monitor the development of the α -Zr(O) layers in the cladding, although it does compute the buildup of the oxide layer. Therefore, the disruption criterion as applied to the results from this code is simply that the cladding temperature (which, because of the lumped parameter assumption, is actually the radially-averaged cladding temperature) at the hottest axial location reaches 1975°C, the maximum zircaloy melting temperature.

The validity of this simple approach to the radial temperature profile and the disruption criterion has been verified through the use of a second code which was developed expressly to validate some of the assumptions made in the main code. This second code does not treat axial temperature dependence but instead focuses on a specific axial location and computes the radial temperature profiles in the fuel and cladding. It also uses the rate laws discussed in Chapter 5 to compute the thicknesses of the ZrO_2 and $\alpha-Zr(O)$ layers as functions of time and temperature. More detailed information about this code, including a listing, is given in Appendix E. The results from this code show that the radial temperature profile is indeed nearly flat (less than $1^\circ F$ difference across the cladding) and that the fuel and cladding temperatures are nearly the same throughout the transient. The code predicts the creation of thin layers of $\alpha-Zr(O)$ and ZrO_2 along the cladding surfaces, with a central region of $\beta-Zry$ remaining. Due to the nearly flat radial temperature profile, the code indicates that the center of the cladding will melt first, with the melt fronts spreading out toward both surfaces. Therefore, cladding melting will not be complete (and therefore disruption will not occur) until the oxygen-saturated $\alpha-Zr(O)$ next to the fuel and to the ZrO_2 melts at $1975^\circ C$. This is equivalent to stating that disruption occurs when the average cladding temperature reaches $1975^\circ C$, which is just the criterion used in the main thermal-hydraulics code. Thus, the second code has confirmed the modeling of the radial temperature profile and the disruption criterion made in the main code calculations.

One phenomenon not considered by either code is the effect of the latent heat of fusion of the cladding on the determination of the disruption time. Zircaloy has a fairly large latent heat of fusion (2.25×10^5 J/kg according to ref. 6-2), and so it might seem initially that neglecting this effect would substantially underpredict the time at which disruption starts. However, the mass of the zircaloy is a small portion (about 15%) of the total pin mass. In addition, as seen from the radial temperature calculations just discussed, the fuel and the cladding are fairly tightly coupled thermally. Therefore, once the cladding reaches the melting temperature, nearly all the energy which had been used to heat up both the fuel and the cladding (mostly the much more massive fuel) goes into performing the solid-to-liquid cladding phase transformation. Because energy is supplied at a fairly rapid rate, the

phase change takes only about three to five minutes, and so neglecting the latent heat of fusion introduces an error of at most 10% into the determination of the start of disruption, well within the uncertainties of the experimental information on which the calculations are based.

As melting of the zircaloy progresses axially outward from the point of initial pin disruption, a flow of the fuel/cladding slurry down to the water pool is established. The mass flow rate of both the liquid and the solid components is then needed to determine whether or not a steam explosion will occur. A good estimate of these mass flow rates can be made from a simple calculation using data from the main thermal-hydraulics code runs discussed in Chapters 5 and 9. This code solves the energy equations along a ten-node axial grid, and so it provides the times at which the average temperature in each node reaches the melting point of the entire zircaloy mixture in that node. These can be used to compute the slurry mass flow rate as follows: the mass flow rate $\dot{m}$ is given by

$$\dot{m} = \rho A v,$$

where ρA is assumed time-independent and is a weighted average between the fuel and the cladding of the product of the density and the cross-sectional area and v is the velocity at which the slurry is falling. This velocity is estimated as

$$v = \frac{\Delta z}{\Delta t},$$

where Δz is the distance between node midpoints and Δt is the difference between the code-calculated time of complete zircaloy melting in a node and the calculated time of melting in the adjacent node. The velocity can then be estimated as a function of time from the melting times provided by the code and used to give the mass flow rate as a function of time.

This method of estimating the slurry velocity assumes that the melt fronts move at a constant speed between midpoints of adjacent nodes and neglects any freezing and remelting which might occur; it thus provides a conservative limiting case for the actual accident. Also neglected is the

time required for the slurry to drop from its original position in the pin into the water (a good assumption in this case, because the melt fronts move slowly relative to the rate at which the material drops into the pool if jamming and refreezing mechanisms are neglected; in other words, the process is melt-rate limited rather than motion limited). Note that, since the hottest point in the cladding is not at the top of the core, the pin originally disrupts somewhat below the top of the core, and so there are two melt fronts, one above and one below the original point of disruption. The upper front is in a much lower power region and so moves more slowly than the lower one, but it still makes a significant contribution to the mass flow rate of the disrupted material.

Slurry mass flux rates have been computed for a series of parametrics studies using this method. These are discussed in §6.4 and Chapter 9.

6.2 In-Pile Experimental Work

Of the experiments about which information is currently available in the open literature, the one which most closely simulates the degraded core conditions of interest in this study is the Severe Fuel Damage Test 1-1 (SFD 1-1) (ref. 6-3). This test simulates the slow uncover of a reactor core due to decay heat boiloff of the water following scram. The pressure in the core is 1000 psia, exactly the same as in the present study. The total core power is about 1.5% of full power, similar to that in the study, although the experiment maintains a constant power as nearly as possible, whereas the decay heat power declines with time in the study. The experiment bundle has a prolonged exposure to steam generated by boiloff of the water pool. The radial temperature distribution across the bundle is quite flat; this is consistent with the assumption made in the study that the temperature is constant radially across any given subassembly. The experiment bundle is not too different in size from the 64-pin bundle considered in the study; the test bundle contains 32 pins which are about 20% smaller than those in the study, arranged in a square lattice similar to the eight-by-eight lattice used in the study.

Unlike many of the in-pile and out-of-pile experiments, SFD 1-1 is not terminated by a rapid quench; rather, it is allowed to cool slowly, making it more prototypic of the true accident situation. This also allows more accurate conclusions to be drawn about fuel pin disruption from posttest examination of the bundle, since no rapid quench which would shatter intact, brittle cladding is used.

There are several features which are different between the experiment and the study. Perhaps the most significant of these is that the fuel used in the experiment is fresh, not irradiated as in the study, and so the extensive cracking and fragmentation which play a major role in the postulated disruption scenario of the study are not present in the experiment. In addition, the fuel pins are PHR pins, not BWR pins as in the study. This means that the internal pressure of the experiment pins is higher than the external steam pressure, and so the pins will balloon and rupture rather than collapsing onto the fuel, as is the case in the study. Also, the flow of steam is maintained at a constant rate (the equivalent of 4 kg/s over the entire full-size core) in the experiment; in the study, the flow decreases with time from an initial peak of about 40 kg/s down to around 2 kg/s. Finally, the axial power shape in the experiment is very close to a cosine shape, whereas that in the study is quite flat over most of the core, with a sharp drop-off at the top and the bottom; therefore, the location at which pin disruption will occur will be different in the experiment and in the study.

The experiment begins with a slow uncover of the test bundle due to boiloff of the water by the (simulated) decay heat. The uncovered pins heat up, and the cladding begins to react with the steam to form a zirconium oxide layer on the outside of the cladding. At about 1100 K, the cladding becomes plastic and, because of the high internal pin pressure, balloons and ruptures, exposing the fuel to the steam. The axial center of the bundle is the hottest point in the bundle and so reaches the temperature threshold of rapid reaction with the steam first. This rapid oxidation consumes all the steam in the lower half of the bundle, resulting in steam starvation in the upper half. Since the steam-cladding reaction is highly exothermic at this point, the bundle heats up rapidly in the vicinity of the bundle center, causing the region in which rapid oxidation is taking place to spread outward radially and downward axially.

Eventually, much of the zircaloy in the lower half of the bundle has been oxidized and so no longer consumes steam. This makes steam available again to the upper bundle region, and this area begins to heat up rapidly due to the energy released in the steam-zircaloy reaction. Thus, the center of rapid cladding oxidation moves from the midpoint of the bundle up into the upper half of the bundle. Meanwhile, the cladding at the center of the bundle has reached the melting point, begins to penetrate the oxide layer on the cladding surface, and eventually slumps, taking with it about 4% of the fuel; this is fuel which has liquified in the reaction between the zircaloy and the UO_2 . Some of the molten cladding and liquified fuel appears to fall into the water at the bottom of the bundle, causing a mild (0.2 MPa) pressure pulse. By now, a power decrease has been initiated, and the transient is essentially over.

Certainly the accident postulated for the degraded core study should not be expected to follow exactly the same path as SFD 1-1 due to the design differences described above. The ruptured cladding provides escape paths for the molten material which would not exist in the postulated accident, and the fresh fuel used does not provide sharp-edged fragments to shatter the brittle ZrO_2 layer and allow large-scale pin disruption. However, the progression of the cladding oxidation and the overall core heatup should be similar to what might be expected in the postulated accident. Since the conditions prescribed for the study should be more favorable to pin disruption than are those of SFD 1-1, the fact that some molten fuel and cladding do appear to fall into the water pool during the experiment would seem to indicate that significantly more molten material might be expected to drop into the pool in the postulated accident.

Some further insight into the difference in behavior between fresh and irradiated fuel can be gleaned from the Reactivity Initiated Accident Test RIA 1-1 (ref. 6-4). Although this test has little in common with degraded core accidents (it is a power burst experiment), it is of interest here because it was run with a combination of two fresh fuel pins and two pins which had undergone extensive irradiation prior to the experiment. Since the two types of pins were exposed to identical conditions during the test, any differences in their behavior must be attributed to the differences in their irradiation histories.

The RIA 1-1 results indicate that the previously irradiated rods produced more extensive disruption and produced it at an earlier time than did the fresh rods. The irradiated pins appeared to fragment during the course of the test, while most fragmentation of the fresh pins occurred during the quenching which took place at the end of the transient. The indication, then, is that if a test such as SFD 1-1 had been run with irradiated fuel rather than fresh fuel, more extensive pin disruption would probably have taken place. Therefore, it seems reasonable to predict that the postulated degraded core accident would lead to significantly more pin disruption than was demonstrated in SFD 1-1.

6.3 Out-of-Pile Experimental Work

Because the UO_2 -Zry and steam-zircaloy reactions play such an important role in degraded core accidents, they have both been thoroughly investigated through out-of-pile tests. These tests allow the reactions to be observed in isolation, rather than in the midst of the various phenomena occurring in a reactor test, and so provide much more detailed information about the reaction kinetics than can be obtained from in-pile tests.

6.3.1 UO_2 -Zry Reaction

In section 5.1.2, a detailed description of how UO_2 and zircaloy interact is presented. The existence of such detailed information can be attributed to an extensive series of out-of-pile tests performed at the Kernforschungszentrum Karlsruhe in Germany. Excellent discussions of these experiments are to be found in references 6-5 through 6-7. What follows is a brief summary of that material.

The goal of these tests is to expose fuel rod segments to a range of temperature and pressure conditions over a period of time, then perform metallographic examination of cross-sectional slices taken from the rods to determine what chemical and structural changes have taken place as a result of the UO_2 -Zry reaction. The segments used consist of high-density, stoichiometric UO_2 pellets contained in zircaloy-4 cladding. The segments are 10 cm long and are filled with helium at 1 bar. Tests were performed on a

number of samples, with the temperature ranging from 1000°C to 1700°C and the external pressure on the sample from 1 to 200 bar.

The experiments were conducted by first inductively heating the test section at atmospheric pressure up to the desired temperature, then applying an external pressure. Although the external pressure went as high as 200 bar in some tests, in most it was held at 40 bar. The sample was then held at steady state for anywhere from 60 to 9000 seconds. Because the UO_2 -Zry reaction embrittles the specimen, a slow cooling process was performed in order to minimize cracking of the sample. All tests were performed in an argon environment so as to focus exclusively on the UO_2 -Zry reaction and eliminate any effects from reaction between the zircaloy and an atmosphere containing oxygen.

After cooling, the samples were sliced into sections and examined by several different techniques. These include scanning electron microscope, electron microprobe, and Auger electron spectroscopy. Optical photomicrographs were also taken in both bright field and polarized light. Analysis of the large quantity of data thus generated has produced the picture of the three-layer structure discussed in section 5.1.2 and has determined the reaction rate formulae as functions of time and temperature which are presented at the end of section 5.1.2.

6.3.2 Steam-zircaloy reaction.

The steam-zircaloy reaction plays two roles in the degraded core accident scenario: 1) as a source of heat, as discussed in Chapter 5, and 2) as a cause of cladding embrittlement through the formation of ZrO_2 and oxygen-stabilized α -zircaloy. This process of cladding embrittlement has been studied at length in a series of experiments performed by Chung and Kassner (ref. 6-8). These tests were performed to establish a criterion by which to predict when cladding exposed to steam becomes sufficiently brittle to shatter under thermal or mechanical shock.

The Chung and Kassner experiments are out-of-pile tests designed to allow isolated investigation of the steam-zircaloy reaction. They

are conducted with 15.3 cm long pieces of Zry-4 cladding filled with simulant fuel (high-purity recrystallized alumina pellets). The specimens are intended to simulate sections of PWR fuel rods, and so they are filled with helium gas to a pressure of 1000 psia at room temperature. All tests are conducted in a steam environment, with an unlimited supply of steam provided.

The experiments begin with the sample at room temperature. The temperature of the sample is then raised at a linear rate (about 10 K/s) by directly heating the cladding by an ac power supply. Since the steam is kept at a pressure of 8.7 to 11.6 psia, the high internal pin pressure causes the cladding to burst once it heats up enough to become plastic. Once the desired temperature is reached, the pin is held at steady state for a period of time, after which the sample is quenched by bottom flooding of the test chamber. Many such tests were run, with exposure temperatures varying between 1027 and 1427°C and exposure times between 20 and 10,000 seconds. Upon quenching, samples with a sufficient buildup of ZrO_2 and α -Zr(O) shattered from thermal shock; the rest did not.

After cooling, mechanical shock tests were performed on each sample prior to removal from the test chamber. The pins were then sectioned and examined by scanning-electron and scanning-Auger microscopy, as well as optically. Evaluation of the resulting data led to the conclusion that if the thickness of the β -Zry is greater than 0.1 mm and the oxygen content of the β -Zry is less than 0.9 wt %, the cladding will not shatter under thermal shock, regardless of the total specimen thickness, total specimen oxygen content, or oxidation temperature. The cladding can withstand mechanical shock with an impact energy up to 0.3 J at 300 K if the β -Zry contains less than 0.7 wt % oxygen and is at least 0.3 mm thick. These two specifications provide the required criterion for predicting when zircaloy cladding is sufficiently brittle to shatter if stressed.

This embrittlement criterion forms the basis for selecting the conditions for disruption discussed in section 6.1. Several possibilities for disruption exist; 1) prior to melting of the zircaloy, the fuel pin swelling could strain the cladding to the point of rupture if the cladding were sufficiently brittle, 2) the embrittled cladding could buckle under its own

weight and crack, 3) the ZrO_2 could crack due to loading from fuel fragments once the zircaloy has melted. However, the calculations discussed in Chapter 5 indicate that somewhat more than 80% of the cladding remains in the β -phase. This information, combined with the fact that the cladding is about 0.8 mm thick, means that, by Chung and Kassner's embrittlement criterion, too much β -Zry (more than 0.3 mm) is present for either of the first two possibilities to seem likely. However, in the third situation, the only solid material remaining in the cladding is the ZrO_2 , which is certainly brittle enough to be cracked by fuel fragments in a slurry. Therefore, the Chung-Kassner criterion leads to acceptance of the third possibility as the probable path of the accident.

6.4 TQUX/TQUV Results

Based on the pin disruption picture postulated and discussed in Section 6.1, which essentially translates into a fuel disruption thermal criterion of 1975°C, and on the results of the standalone thermal pin code detailed in Chapter 5, mass flux slumping rates were obtained around the fuel pin disruption time. These rates are summarized in Table 6.1. To reiterate the physical picture is one where the irradiated fuel pellet fragments disrupt the thin zircaloy oxide shell remaining after all the various zircaloy phases melt (~1975°C) and slump into the coolant pool at the bottom of the core.

To simplify matters and obtain an upper bound on the slumping rates free fall of the fragments into the pool is assumed. This gives the upper limit since phenomena such as refreezing, remelting and jamming, all mechanisms which would be expected to delay the slumping process and decrease velocities and mass flux rates, are not considered. The main assumption is then one of a zircaloy clad melt rate controlled dispersion, and the simple equation discussed in Section 6.1 for the clad melt front axial velocity up and down the pin is used to compute the mass flux rate into the bottom coolant pool. As Figs. 5-5 and 5-19 show that the cladding and fuel pellet temperatures are closely coupled, the composition of the dispersing debris is of zircaloy cladding (~25% volume fraction) mostly molten at 1975°C and solid fuel pellet fragments (~75% volume fraction) at 1975°C.

With the limitations in nodalization it was felt desirable to round off the slumping rates presented in Table 6.1. Given this constraint it can be seen that clear patterns do not emerge in the matrix of parametrics. The complication of nodalization and a time dependent slumping rate as the melt front propagates up and down the pin travelling down the temperature gradient from high power regions to low power regions, renders it difficult to draw trends. However, within each transient it can be seen that there is initially a relatively large slumping rate which then decreases asymptotically to a slumping rate which is of $\sim$ few tenths of a lb/sec for each subassembly for all the parametrics with the exception of one. The exception is the TQUX case for a flat axial profile where the slumping rate is 0.4 lbs/sec throughout. Axial power shape has made a significant difference in the slumping rate at the onset of pin disruption. The cases where the nominal axial power is used are cases where the initial pin disruption is not at the core outlet. The clad temperature profile is peaked with the result that the initial disruption occurs in a region with a zero temperature gradient leading to the large initial slumping rate. The axial power shaping has led to a difference of $\sim$ factor of 10 in the initial slumping rate. Axial power shape effects need to be investigated further.

Table 6.1 also shows that there is a significant delay in the onset of fuel/clad disruption in the case of the low power subassemblies as opposed to the high and average power subassemblies. The onset of fuel pin disruption in the low power subassembly occurs after boil dry (2" water at 3200 seconds) in those assemblies. The water level is therefore below the lower fuel tie plate when slumping occurs and fuel coolant interactions upon debris bed formation would therefore not be expected for these subassemblies. However, as indicated in Chapter 5 the algorithm for predicting dryout used in this study needs to be refined to take better account of top and bottom losses.

Table 6.1. TQUX/TQUV Parametrics Slumping Rates[†]

	Axial Power Shape	Subassembly Power	Heat Transfer Coefficient	Disruption* times, sec	Δt sec	Node #	$\dot{M}_{UO_2}$ lbs/sec	$\dot{M}_{Zr}$ lbs/sec	$\dot{M}_{Total}$ lbs/sec
	Nominal	Average	High						
T Q U X				2472	18	9	3.0	0.6	3.5
				2490	132	8	0.5	0.1	0.6
				2622	192	7	0.4	0.1	0.5
				2814		6			
				2871		10			
			Low	2424	87	8	0.9	0.2	1.1
				2511		7			
				2575	151	9	0.75	0.15	0.9
				2661	213	6	0.3	0.05	0.35
				2874		5			
				3157		4			
				3570		10			
			Ave.	2473	12	9	4.0	1.0	5.0
				2485	130	8	0.5	0.1	0.6
				2615	190	7			
				2805		6	0.4	0.1	0.5
				2900		10			
		High	High	2208	43	9	1.3	0.3	1.6
				2251	154	8	0.5	0.1	0.6
				2405		7			
				2529	185	10	0.4	0.1	0.5
				2590		6			
			Low	2158	80	8	0.8	0.2	1
				2238		7			
				2355	149	9	0.6	0.1	0.7
				2387	236	6	0.3	0.05	0.35
				2623		5			
				2913		4			
				3135		10			
			Ave.	2211	35	9	1.6	0.4	2.0
				2246	150	8	0.5	0.1	0.6
				2396		7			
				2557	185	10	0.4	0.1	0.5
				2581		6			
		Low	High	4710	127	8	0.5	0.1	0.6
				4837	146	7	0.4	0.1	0.5
				4983		6			
				5203	280	9	0.3	0.05	0.35
				5263		5			

Table 6.1 (cont'd)

Axial Power Shape	Subassembly Power	Heat Transfer Coefficient	Disruption* times, sec	At sec	Node #	$\dot{M}_{UO_2}$ lbs/sec	$\dot{M}_{Zr}$ lbs/sec	$\dot{M}_{Total}$ lbs/sec
		Low	4666	8	8	6	1.5	7.5
			4674	188	7	0.35	0.05	0.4
			4862	308	6	0.25	0.05	0.3
			5170		5			
			5297		9			
		Ave.	4706	116	8	0.5	0.1	0.6
			4822	153	7	0.4	0.1	0.5
			4975		6			
			5202	280	9	0.3	0.05	0.35
			5255		5			
Flat	Ave.	Ave.	2530	150	10	0.35	0.05	0.4
			2680	160	9	0.35	0.05	0.4
			2840	170	8	0.35	0.05	0.4
			3010		7			
<hr/>								
T	Nominal	Ave.	2355	7	8	7	1.5	9.5
Q			2362	107	9	0.5	0.1	0.6
U			2462	152	7	0.4	0.1	0.5
V			2614	194	6	0.35	0.05	0.4
			2808		5			
			3003		10			

[†]Per subassembly.

*Time after core uncover.

References:

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- 6-4. Stephen L. Seiffert et al., Reactivity Initiated Accident Test Series Test RIA 1-1 Fuel Behavior Report, NUREG/CR-1465 (EGG-2040), September, 1980.
- 6-5. P. Hofmann and D. K. Kerwin-Peck, UO_2 /Zircaloy-4 Chemical Interactions and Reaction Kinetics from 1000 to 1700°C under Isothermal Conditions (Final Report), KFK 3552, November, 1983.
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- 6-7. W. Dienst et al., "Chemical Interactions between UO_2 and Zircaloy-4 from 1000 to 2000°C," Nuclear Technology, 65, p. 109 (April, 1984).
- 6-8. H. M. Chung and T. F. Kassner, Embrittlement Criteria for Zircaloy Fuel Cladding Applicable to Accident Situations in Light-Water Reactors: Summary Report, NUREG/CR-1344 (ANL-79-48), January, 1980.

7.0 Structural Failure

Methods used in the mechanical evaluation of a boiling water reactor under degraded core conditions is described in this section. During the accident sequences being considered the reactor internals, reactor vessel and the immediately attached penetrations are calculated to reach temperatures that significantly exceed the operational design conditions. Since such an event has small probability of occurring, the philosophy proposed in the ASME Boiler and Pressure Vessel Code, Appendix F,⁷⁻¹ under Faulted Conditions or Level D Service Limits is adopted for these evaluations. For such evaluations, only primary loads are considered. Thus, thermal stresses are neglected and failure criteria based on peak localized elastic stresses and cyclic loadings are not considered. The primary loads are assumed to be induced by the internal pressure and the weight of components supported by the structure under evaluation.

7.1 Pressure Containment

This particular section covers the pressure containment by the reactor vessel, top reactor vessel head, flange bolts and steam outlet piping. These components are constructed of carbon and low alloy steels which are subjected to temperatures significantly exceeding their normal design applications. Appendix A is a listing of the materials and dimensions of components considered in this mechanical evaluation. These data are for a General Electric BWR-4 unit. The degraded core conditions are calculated to persist for hours. Creep rupture of the pressure containment components subjected to the calculated high temperatures was therefore selected as their most probable failure mechanism.

7.1.1 Evaluation Procedure

The failure mechanism proposed for the reactor vessel, top vessel head, flange bolts and steam outlet piping under degraded core conditions is creep rupture. As the temperature is increased, the local stresses are relaxed by creep, and the reinforced nozzles are geometrically strengthened⁷⁻² by the creep deformations. The reduction in load carrying capacity of

the vessel, head, pipe and bolts under the highly deformed conditons associated with creep rupture is evaluated in terms of the following stress intensities:

Flange Bolts

$$S_b = PD_i^2 / nd_b^2$$

where

- S_b = stress intensity in bolt - [psi]
- P = internal pressure in the vessel - [psig]
- D_i = internal diameter of vessel
- d_b = diameter of the bolt
- n = number of bolts

Reactor Vessel

$$S_v = PD_m / 2h$$

where

- S_v = stress intensity⁷⁻³ in vessel - [psig]
- D_m = mean diameter of the reactor vessel - [in]
- h = thickness of the vessel - [in]

Reactor Vessel Head

$$S_H = PR_H / 2h$$

where

- S_H = stress intensity in the reactor vessel head - [psi]
- R_H = mean radius of the spherical vessel head - [in]
- h = thickness of the spherical vessel head - [in]

Steam Outlet Piping

$$S_p = PD_m / 2h$$

where

$$\begin{aligned} S_p &= \text{stress intensity}^{7-3} \text{ in the steam outlet piping - [psi]} \\ D_m &= \text{mean diameter of the outlet steam piping - [in]} \\ h &= \text{thickness of the outlet steam piping - [in]} \end{aligned}$$

7.1.2 Material Failure Criteria

The stress rupture behavior of a material is based on the time to rupture at constant stress and temperature. Various parametric methods⁷⁻⁴ such as Larson-Miller, Dorn, Manson-Haferd, etc., are used to correlate creep rupture data. One such correlation is required for each material to be considered. These correlations can be functionally expressed as

$$t_R = f(S, T)$$

where

$$\begin{aligned} t_R &= \text{time to rupture} \\ S &= \text{stress intensity} \\ T &= \text{temperature} \end{aligned}$$

The BWR structural components are subjected to a varying temperature environment during the postulated degraded core accident. A linear damage accumulation based on time to rupture is assumed. The damage accumulation fraction is evaluated as

$$A = \sum \Delta t_i / f(S_i, T_i)$$

where

$$A = \text{damage accumulation fraction}$$

Δt_i = time increment at conditions S_i and T_i
 S_i = average stress intensity during Δt_i
 T_i = average temperature during Δt_i

Failure is assumed to occur when $A=1$ as the most probable failure predictor.

7.1.3 Discussion of Evaluation Procedure

An extensive literature exists concerning failure of reactor penetrations, selection of stress intensity criteria, damage accumulation under high temperature creep conditions and correlation of creep rupture conditions. The references reviewed during this study were cataloged on a computer data file and provide an entry into the literature. A listing of the references is given in Appendix F. The variability of material lots and the extrapolation of creep rupture times to conditions for which test conditions may not be available should compensate for the simplifications that have been made in the evaluation procedure. Thus, the remainder of this section will concentrate on the data base used to evaluate the creep rupture relations, how these creep rupture correlations were evaluated and the checks used to confirm the resultant correlations.

7.1.4 Evaluation of the Creep Rupture Correlations

The available data for each material considered for this study varied in format and in content. To determine the critical components, the initial evaluations have been made in the most expedient manner. Should some aspect of this evaluation become critical to the general degraded core analyses, the data sources found by this study should provide a start for more detailed evaluations. Further work can then be done on expanding the data base and refining the correlations, damage criteria and mechanical evaluation of the various component responses. The write-ups that follow describe the approach that was taken and the rationale behind the approximations.

7.1.4.1 Stress Rupture of the Carbon Steel

Creep rupture data on carbon steels were found in the literature^{7-5,7-6} for test temperatures up to 1050°F. Other creep rupture data on carbon steels^{7-7,7-8} was found to be consistent with those reported by Smith.^{7-5,7-6} Also the tensile data reported by Smith⁷⁻⁶ was in agreement with the ultimate stress values reported in the ASME code⁷⁻¹ for the carbon steel SA106 GRB. The tensile data of Smith⁷⁻⁶ is shown in Fig. 7.1 with the solid line representing the ASME code values between 0 and 1000°F. Since the degraded core analysis should use the most probable failure values, the dashed line in Fig. 7.1 is recommended for use at temperatures below 750°F.

The creep rupture values used to obtain the recommended correlation are those from Smith⁷⁻⁶ shown in Figs. 7.2, 7.3 and 7.4. The data were analyzed in the original work^{7-5,7-6} to obtain creep rupture strengths at 10,000 and 100,000 hours. For the degraded core analyses high temperatures will be maintained for only a few hours, but even for such short times a significant reduction in creep rupture strengths can occur. For the steam outlet piping remaining near system pressure, ~1050 psi, the stress intensity, section 7.1.1, will be approximately 14,000 psi. If the ultimate strengths were used, failure would occur at 1300°F (ref. Fig. 7.1). If the temperatures were maintained for ~100 hours, failure would occur at 1000°F as a creep rupture failure (ref. Fig. 7.4). Thus the extrapolation in temperature of the creep rupture response is required.

Smith^{7-5,7-6} had plotted the creep rupture stress versus the Larson-Miller parameter, $P = T(20 + \log t_r) \times 10^{-3}$. T is the absolute temperature, °R, and t_r is rupture time, hr. Fitting a curve to this data presentation and re-estimating the 1050°F creep rupture data did not yield a good representation of these data over the time range of interest. Since the temperature range of interest is greater than that for the available creep data and the tensile data spans the calculational range, a correlation method was sought that used the tensile data.

Fig. 7-1. Ultimate Tensile Strength for Carbon Steel.

Fig. 7-2. Stress vs. time for rupture at 800 and 850 F for Carbon Steel.

Fig. 7-3. Stress vs time for rupture at 900 and 950 F for carbon steel.

Fig. 7-4. Stress vs time for rupture at 1000 and 1050 F for carbon steel.

The creep rupture correlation developed for the low carbon steel SA106 is empirical and based on a linear relation between the log of the stress and the log of the creep rupture time. Linear approximations of the graphical results in Figs. 7.2 to 7.4 were extended to the ultimate stress values presented in Fig. 7.1. The intersections of these curves were approximately at a rupture time of 0.25 hours. The rupture stresses at this time were assumed to be the ultimate tensile strength. The slope of each curve was determined such that it would pass through the creep rupture time at 100 hr. These slopes could be approximated as a linear function of $\log(1/T)$ where T is the test temperature. The resultant correlation was:

$$\log_e(4t_r) = 1.498 \log_e(S_u/S) / (\log_e(T) - 7.05)$$

where

t_r	=	rupture time - [hr]
S_u	=	ultimate stress - [psi]
S	=	test stress - [psi]
T	=	test temperature - [°R]

If T is less than 1160°R then the failure process is assumed to be independent of temperature. If the stress intensity exceeds S_{IJ} , the failure is assumed to be instantaneous. Appendix G is the listing of an interactive program that evaluates the creep rupture time. The creep rupture times calculated with this program are plotted on the graphs with the test data, Figs. 7.2-7.4.

7.1.4.2 Stress Rupture of the Low Alloy Steels

The vessel and its top and bottom heads are made of SA533GRB while the nozzles are made of SA508C1. These materials are low alloy derivatives of C-0.5 Mo steel which have similar responses.⁷⁻¹ Creep test results were found for test temperatures as high as 1200°F.⁷⁻⁹ Additional creep test results were found for test temperatures up to 1400°F⁷⁻¹⁰ for materials of similar composition. Some short time creep rupture graphical correlations⁷⁻¹¹ for SA508C2 were found for temperatures as high as 1750°F. Comparative creep rupture⁷⁻¹² results for SA533GRA and SA533GRB were also

found. Tensile results were also available up to 1400°F⁷⁻¹⁰ for the general class of this alloy which is correspondent to those results for the SA533^{7-13,7-14} materials. Creep rupture correlations of the Larson-Miller and Manson type were compared⁷⁻¹⁴ with the creep rupture data and each other. Both of these correlation procedures yielded similar results.

The tensile strength tables for low alloy steels used in the interactive program (Appendix G) developed for this study are based on Smith's⁷⁻¹⁴ curve for the tensile strength ratio of A533 steel. The curve was extended to higher temperatures using data for the base C-Mn alloy. At temperatures below 600°F the tensile strength was chosen equal to an average strength of 80,000 psi from data for different product forms. The correlation is compared with data and a correlation for C-0.5 Mo steel⁷⁻¹⁰ in Fig. 7.5.

The creep-rupture correlation for low alloy steels is based on Larson-Miller parameter fit to the 100 hour rupture data⁷⁻¹⁰ for C-0.5 Mo steel. Data from other references for SA508, A533 and C-Mn steel are shown in Fig. 7.6 along with the C-0.5 Mo data and the curve that was used to develop the correlation. Again a table look-up procedure is used in the interactive program to determine the Larson-Miller parameter P given a stress and temperature. The rupture time is evaluated by the relation:

$$t_r = 10^{(-20 + 1000P/T)}$$

For the typical BWR vessel (see Appendix A), with an internal pressure of 1050 psi, the stress intensity is ~ 21,400 psi. The tensile failure⁷⁻¹⁰ would occur at less than 1300°F (see Fig. 7.5) and the creep response would be within the range of the Larson-Miller parameter values⁷⁻¹⁰ (see Fig. 7.6) used to develop the creep rupture correlation.

7.1.4.3 Stress Rupture of the Flange Bolts

The flange bolts are structural members that must resist the pressure loads. They are manufactured from a material that differs from the other primary pressure containment components. An AISI4140 or SA193B7 steel is used for the bolts in the representative BWR selected for

Fig. 7-5. Tensile Strength of Low Alloy Steel

Fig. 7-6. Creep Data Used to Develop Larson-Miller Parameter for Low Alloy Steel.

this study (Appendix A). The same type of data, tensile, creep rupture and Larson-Miller plots, are presented⁷⁻¹⁰ for a steel of AISI4140 composition as was presented for the low alloy steel. This material data covers creep rupture up to 1400°F. The Aerospace Structural Metals Handbook⁷⁻¹⁵ (Code 1209, page 9) indicates that the ultimate tensile strength decreases with increasing bar stock size. The normalized high carbon ultimate tensile strength values of ASTM-STP No. 151⁷⁻¹⁰ are similar to those values given for the bar stock of AISI4140 tempered at 1265°F (Code 1203, page 13).⁷⁻¹⁵ Various sources of tensile strength values^{7-10,7-13,7-15,7-16} are in agreement, thus the values given in ASTM⁷⁻¹⁰ publication (Fig. 7.7) were used to represent the short time tensile properties.

Heat treatment does influence the creep behavior (Code 1203 page 16)⁷⁻¹⁵ even up to 1200°F. The influence of notches⁷⁻¹⁷ at higher temperatures (1000°F) in normal material (not embrittled) is not significant, thus regular creep rupture data can be used to represent the high temperature response of the bolts.

The Larson-Miller plots are for the creep rupture behavior of various data sources.^{7-10,7-17} The creep rupture data in the ASTM⁷⁻¹⁰ and NRI⁷⁻¹⁶ were in good agreement (Fig. 7.8), thus the Larson-Miller representation of the ASTM⁷⁻¹⁰ data (Fig. 7.9) was used to develop the creep rupture correlation. Since the data presentation used to develop the correlation is the same as for the low alloy steel, the same methodology is used to develop the correlation.

The interactive program (Appendix G) for determining the creep rupture times evaluates the ultimate tensile stress with a table look-up based on Fig. 7.7. If the stress is higher than this value, rupture is assumed to occur. A decrease of the rupture stress due to creep is not assumed to occur below 700°F. The Larson-Miller curve of Fig. 7.9 is approximated with the quadratic expression

$$P = -3.1562 + 11.01866 \ln(S) - 0.72674 [\ln(S)]^2$$

where

Fig. 7-7. Tensile Strength of Bolt Material.

Fig. 7-8. Rupture in 100 Hours of Bolt Material.

Fig. 7-9. Larson-Miller Parameter versus Creep Rupture Stress for the Bolt Material.

P	=	$T(20 + \log(t_r)) \times 10^{-3}$	= Larson-Miller parameter
T	=	test temperature [°R]	
t_r	=	rupture time [hr]	
S	=	test stress [psi]	

The rupture time is evaluated with the relation:

$$t_r = 10^{(-20 + 1000P/T)}$$

7.2 Reactor Internal Structures

The critical BMR internal structures (excluding the fuel assemblies, discussed in Section 6) are fabricated of either annealed AISI type 304 stainless steel or Inconel alloy 600 (ASTM SB166 and SB168). The dimensions, weights and materials are given in Appendix A. Stainless steel structures include the 1) steam dryers, 2) steam separators, 3) shroud head, 4) core shroud, 5) jet pumps, 6) top fuel guide, 7) core support plate, 8) control rod guide tubes and 9) control rod drive housings. Inconel structures include the control rod drive tube stubs and the shroud support columns.

Under degraded core conditions the reactor internal structures are subjected to temperatures ranging from the design temperature of 575°F to temperatures exceeding their melting point (approximately 2500°F). The primary mechanical loading on the internals is due to the weight they support. Pressure differentials inside the reactor from the low steam flow-rate are negligible. Again, because of the high temperatures, moderate loads and long times involved in degraded core analysis, the primary modes of failure are assumed to be those associated with creep deformation and creep rupture.

7.2.1 Evaluation Procedure

The reactor internals may carry their loads in tension, compression or bending. Depending on the loading and on the geometry, the appropriate failure criteria used here is based on stress rupture or on creep collapse.

Within the limitations of the present study, only the shroud head was considered. The shroud head is the first internal structure that is expected to fail as the core uncovers and the temperature of the steam at the core outlet increases.

Shroud Head

The shroud head structure consists of a shallow spherical cap with the steam separators welded into circular penetrations. The total weight of the integral structure is 139,600 lbs. The simplified analysis presented here considers the shroud head as a spherical shell subjected to a dead load equal to the above weight. The hoop stress S_θ and meridional stress S_ϕ are given by (see, for instance, Table 6.1 of Reference 56)

$$S_\theta = -\frac{F R}{A h} \left(\cos\phi - \frac{1}{1 + \cos\phi} \right)$$

$$S_\phi = -\frac{FR}{Ah} \left(\frac{1}{1 + \cos\phi} \right)$$

where

- F = dead weight, 139,600 lbs
- A = middle surface area, 39,274 in²
- R = radius of curvature, 216 in
- h = shell thickness, 2 in
- ϕ = polar angle, $0 < \phi < 30^\circ$

For buckling considerations the dead load is replaced by a uniform pressure loading P where

$$P = F/A$$

The resulting uniform stress state

$$S_\theta = S_\phi = -\frac{FR}{2h}$$

is equal to the dead-load stress state for small ϕ .

7.2.2 Material Failure Criteria

Stress rupture calculations for the internal structures are based on the same life fraction method outlined for the pressure containment in Section 7.1.2. The maximum tensile stress is used in material correlations to calculate the life-time at a given temperature. For a variable stress and temperature history, damage is assumed to accumulate in proportion to the fraction of the rupture time spent at a given stress and temperature. Failure is assumed to occur when the life fraction reaches 1.0. In addition, the maximum stress is continuously compared with the ultimate strength at the current temperature. If the ultimate strength is exceeded, failure is assumed to occur instantaneously by plastic instability.

Buckling calculations are based on approximate methods⁷⁻¹⁸ using elastic formulas with Young's modulus replaced by the tangent modulus. The creep strain is calculated from

$$\epsilon_c = \int_0^t \dot{\epsilon}_c(T, S) dt$$

where $\dot{\epsilon}_c$ is the material creep rate at constant stress and temperature. The tangent modulus E_t is then calculated from

$$1/E_t = 1/E + \frac{\partial \epsilon_c}{\partial S}$$

An example of the buckling calculations is the creep collapse of the shroud head. The critical stress S_{cr} for a spherical cap is calculated from (see Eq. 6.35 of Ref. 56)

$$S_{cr} = 0.125 \frac{E_t h}{R}.$$

Collapse of the head is calculated to occur when $S > S_{cr}$.

7.2.3 Evaluation of Creep and Creep Rupture Correlations

Data for high temperature creep deformation and creep rupture were reviewed for the reactor internal materials. An extensive search of applicable data for 304 stainless steel was completed before the present study was terminated. The survey Inconel data is much less complete.

7.2.3.1 Deformation and Stress Rupture of Type 304 Stainless Steel

Austenitic type 304 stainless steel is widely used as an elevated-temperature structural material. Consequently, there is an abundance of data available on its high temperature properties. Much of the data has been collected for application to nuclear fast breeder reactors^{7-19,7-20,7-21,7-22,7-23,7-24,7-25} and is reported in the Nuclear Systems Materials Handbook (NSMH).⁷⁻²⁶

The NSMH data for the ultimate strength of type 304 stainless steel is based on G. V. Smith's evaluation⁷⁻²⁷ of the elevated temperature data compiled by Simmons and Van Echo.⁷⁻²⁸ Smith reduced this data in terms of the ratio of the ultimate strength at temperature to the ultimate strength at room temperature. The table look-up procedure adopted for the present study is based on Smith's stress ratio table (Table 2 of Ref. 54) extended to 2200°F using data⁷⁻²⁷ for type 316 stainless steel. The average room temperature ultimate strength of annealed type 304 stainless steel is 75,000 psi.⁷⁻²⁷

Since both creep deformation and creep rupture are important in determining failure of reactor internals under degraded core conditions, it is desirable to use consistent correlations for the two that are based on the same data. The correlations selected here are based on the average results of data from 26 lots of material.⁷⁻²⁵ The creep rupture and minimum creep rate are given by

$$\log t_r = C_h - 0.01258T - 1.232 \times 10^{-5} TS - 4.770 \times 10^{-3} T \log S ,$$

and

$$\log \dot{\epsilon}_c = C'_h + 0.02113T + 1.754 \times 10^{-5}TS + 5.218 \times 10^{-3}T \log S$$

respectively. Here

- t_r = rupture life (h),
- $\dot{\epsilon}_c$ = minimum creep rate (%/h),
- S = stress (Mpa; 1 ksi = 6.895 MPa), and
- T = temperature (K).

The constants $C_h = 25.458$ and $C'_h = -33.890$ are not constants which are chosen as the reported average values. It is noted that the above equation for $\dot{\epsilon}_c$ is the recommended equation for minimum creep rate given in the latest version of the NSMH.⁷⁻²⁶

Fast neutron flux is known to embrittle stainless steel so that the rupture life t_r of BWR internals may be less than that given by the above equation, which is based on data from unirradiated material. The shroud is the closest component to the core and by the end of plant life the most irradiated point on the inner surface of the shroud is subjected to a total integrated fast neutron flux of 2.7×10^{20} Mvt ($E > 1$ Mev).

Little data was found for the high temperature ($T > 1600^\circ\text{F}$) failure of irradiated type 304 stainless steel. However, transient burst tests on irradiated fast reactor 20% C.W. type 316 stainless steel cladding tubes⁷⁻²⁹ suggests that the above level of fluence would not greatly reduce the high temperature strength of the BWR core internal structures. Fluence effects were therefore ignored here.

7.2.3.2 Deformation and Stress Rupture of Inconel Alloy 600

Curves for the ultimate strength, secondary creep rate and rupture time of Inconel alloy 600 are given in the Nuclear Systems Material Handbook.⁷⁻³⁰ Inconel data is not included in the materials property routine given in Appendix G because the Inconel structures would not be

expected to fail until after core disruption, which is beyond the scope of the present report.

7.3 TQUX/TQUV Results

The mechanical response of the pressure containment and reactor internal structures under degraded core conditions were calculated for the postulated scenarios using the above methodology. Temperatures and pressures were supplied from calculations given in other Chapters of this report. Failure predictions and their influences on the accident sequences are discussed below.

During the incore degradation phase where intact pin geometry is still retained the collapse of upper internals upon the thermally weakened core or the failure of the primary pressure boundary at locations such as in the region of the steamline nozzle could lead to branch points in the scenario. Two calculations are presented here involving the possibility of structural failure of the upper internals and the steam line piping for the TQUX sequence using the results of Chapter 5 for an average power subassembly.

The first calculation examines the failure of the steam line piping just downstream of the nozzle which should be the weak point in the primary boundary for the incore/intact pin geometry degradation phase.

During steady-state operation the steam piping is subjected to the reactor pressure of 1050 psi at the corresponding saturation temperature of 547°F. Under the high pressure TQUX scenario the pressure remains nearly constant, but the temperatures slowly rise as the core uncovers. Figure 7-10 shows the hottest temperature in the steam line near the reactor outlet nozzle for two scenarios (average heat transfer coefficient).[†] The first scenario is one where it is presumed that the separator and dryer are bypassed by the steam flow. This gives the upper bound on the steam line temperatures. Nonbypass is postulated for the second scenario. The lower temperature curve is typical of outlet piping temperatures for nonbypass.

[†]Refer to Chapter 5.

Fig. 7-10. Temperature and Life Fraction to Failure for Steam Lines at the Reactor Outlet Nozzle. See the text for the two different scenarios shown.

Fig. 7-11. Shroud Head Temperature and Fraction of Critical Buckling Stress S_{cr} .

In the previous sections of this chapter an evaluation procedure is presented for determining the failure of structures under degraded core conditions. The procedure for analyzing steam pipe failures is based on high temperature creep rupture. Stress rupture and ultimate strength correlations are given for the carbon steel steam line material. The stress rupture correlations are incorporated into a life fraction analysis to determine the creep rupture time (life fraction = 1) under transient loading and temperature conditions. If at any time the stress is calculated to exceed the ultimate strength at the current temperature, instantaneous failure is assumed to occur by plastic instability and the life fraction is set equal to 1.

The second set of curves in Fig. 7-10 show the calculated life fraction of the steam lines as a function of time for the given temperature histories and for a loop stress of 14,000 psi corresponding to the 1050 psi steam pressure. Under these conditions the life fractions are seen to accumulate rapidly at temperatures above 1100°F. However, in both cases the ultimate strength is reached at 1300°F before the life fraction reaches 1 as noted by the breaks in the slope of the curves. The corresponding failure times are 2080 and 4160 seconds after core uncover.

Figure 7-11 shows the calculated shroud head temperature for the TQUX sequence (maximum heat transfer coefficient analytical calculation)[†]. This curve should give an upper bound. The shroud head structure consists of a shallow spherical cap which supports the steam separators. During the period when the temperatures are increasing, the pressure drop across the head is small because of the low steam flow rate. The only significant load on the head is therefore compression from the dead weight of the steam separators and the head itself.

The nominal dimension, weights and material properties for the Type 304 stainless steel head structure are given in the preceding sections along with a procedure for evaluating failure based on creep collapse. The weight of the head plus the separators is 139,600 lbs and the area of the middle

[†]Refer to §4.2.6.2.3 and Chapter 5.

surface is 39,300 in² giving a distributed load of 3.55 psi. The ratio of the hoop stress S to the critical hoop stress S_{cr} for the Se conditions is only 0.008 at low temperatures where the head responds elastically. At higher temperatures, however, stainless steel begins to creep rapidly. The procedure for calculating creep buckling under these conditions is based on approximate methods using elastic buckling formulas with Young's modulus replaced by a calculated tangent modulus, as outlined previously.

The ratio of the applied stress to the critical stress for collapse of the shroud head under the calculated temperature history is shown in Fig. 7-11. Failure is predicted at 3120 s after core uncover at a temperature of 2350°F. The collapse would occur very rapidly with the weight of the separators and shroud head dropping down on the core.

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8.0 Fuel Coolant Interaction

This section considers the interactions that may occur when the top section of the fuel elements overheat enough to fragment and fall into the water still present near the original core bottom.

Analysis using the computer code described in Chapter 5 and the fuel disruption criterion described in Chapter 6 presents the following picture. Late in the accident sequence (about 2470 sec after beginning of core uncover) a sizable section of the fuel elements near the core exit will fragment (as indicated by overheating of one calculational node which is 15 inches long). Fragmentation will occur over a 10 second period giving mass flow rates of 4.3 $\#_m/s$ of UO_2 and .86 $\#_m/s$ of Zircaloy. The code also predicts the mean temperature of these particles will be slightly less than 2000°C, thus the UO_2 will be in the form of hot solid particles and the Zircaloy will be liquid. The fragmented material will fall through the length of the core filled with steam and hydrogen (~10.5 feet) and impact on the remaining water near the original core bottom (a 12 inch deep layer above the core support plate containing slightly less than 6 $\#_m$ of water). How will this incoming material react with the water? We considered three types of reactions:

1. As the particles impact on the upper water surface violent boiling may splash the particles and some water backup into the dry part of the core.
2. The particles may penetrate into the water and boil the water dry in a relatively short period of time.
3. The particles may penetrate into the water and cause a vapor explosion.

Each type of reaction will be discussed in more detail below.

8.1 Surface Boiling

The behavior of hot particles (either liquid or solid) as they fall through gas and/or vapor layer and impact on a much colder liquid surface is determined by a complex interaction between the particles, the gas and the liquid. A gas/vapor envelope develops around the falling particles. As the particles approach the liquid surface four things occur. The gas envelope is squeezed and pressurized, the cold liquid surface distorts, the particle decelerates (if it's liquid, it also distorts), and vapor is formed at the cold liquid surface. Depending on the size, shape, and velocity of the particle and the properties of the three materials (particle, gas/vapor, liquid) the particle may squeeze completely through the gas/vapor envelope and impact directly on the liquid surface. The resultant sudden vaporization blows the particles back up into the gas space along with a splash of water. Even for a completely defined system (particle size, shape, etc.) the conditions that will lead to a sudden surface boiling reaction are not well understood. For the conditions in the reactor where the size, shape, and velocity of the particles are unknown we can only say the reaction may occur.

8.2 Subsurface Boiling

If surface boiling does not occur, the hot particles will settle through the water surrounded by a gas/vapor film. The water will film boil off the hot particles. The mean specific heat for both the UO_2 and the Zircaloy is about $370 \text{ J/KG}^\circ\text{K}$ in the temperature range between 600°K and 2400°K . If all of the heat in the hot particles above the boiling temperature of the liquid at the ambient pressure is converted into latent heat of vaporization, the water layer would boil dry in slightly less than three seconds. Even if it is assumed as much as 50% of the heat remains in the hot particles to account for temperature gradients across the hot particles and through the vapor film the residual water will still boil dry in only six seconds. Unless a vapor explosion occurs.

8.3 Vapor Explosions

Vapor explosions occur when a hot liquid suddenly fragments and intimately mixes with a cold liquid causing explosively rapid boiling and the generation of high pressure vapor. Current theory says that during an explosion the system goes through four sequential steps:

1. The liquids coarsely intermix. During this first step an insulating vapor layer between the liquids prevents a violent reaction.
2. A trigger of some sort causes a local collapse of the vapor film. Liquid-liquid contact at very high temperature generates a local region of high pressure vapor.
3. This local pressurization causes collapse of the vapor film in the surrounding region. The area of high pressure grows and the boundary between high and low pressure areas steepens into a shock wave which passes through the rest of the mixture making a coherent explosion.
4. The high pressure steam expands thus converting thermal energy to destructive mechanical energy.

Triggering is a poorly understood phenomena. It is known that increases in ambient pressure tend to stabilize the vapor film and make triggering more difficult. The high pressure in the reactor (1100 PSI) greatly reduces the probability of an explosion. However the possibility of an explosion cannot be positively precluded. If an explosion does occur, how much energy would it release and how high a pressure would it generate? The explosive energy release is dependent on the mass of the hot material M_h , the maximum theoretical energy E_m per mass and the efficiency of the actual explosion defined as $E_x/E_m(M_h) \equiv EFF \equiv \epsilon$. The pressure time history would depend on both the energy release and the accelerated reaction mass. If it assumed that an explosion occurring at the bottom of a subassembly produces a one dimensional acceleration of a liquid slug upward (a gun barrel type reaction) the impulse, defined as the time integral of the vertical force across the subassembly, will take the following form.

$$I \equiv \int \text{Force } d(\text{time}) = \int P A dt = A \int P dt = A \bar{P} t \quad (8.1)$$

where I is impulse, A is the flow area of the subassembly, P is pressure $\bar{P}$ is average pressure and t is time. Impulse and energy are related by

$$E_x = M_H E_m(c) = \frac{I^2}{2M_a} = \frac{\bar{P}^2 t^2 A}{2 L \rho} \quad (8.2)$$

where M_a is the mass of the accelerated water slug and is equal to length of the slug L times the flow area A times the density ρ . (See, for example, the derivation in Ref. 8.1.)

Again, the lack of knowledge about both vapor explosions and the particular system under study does not allow the various terms in Eq. (8.2) to be specified. However, we can make some arbitrary but reasonable assumptions about various values and see the effect on predicted pressure-time history. Maximum theoretical energy release per mass for a 2000°C Zircaloy - 1100 PSI water system would be about 250 Joules/gm.^{8.1} Assume that the amount of molten Zircaloy flowing into the water bath in one second reacts in the bottom half of the water pool and blows the top half of the pool upward. Assume a 50% efficient explosion (about as high as ever observed) and a mean pressure of 1000 PSI (typical of pressure generation in reactions of this type). These assumptions predict a pressure impulse lasting for nearly 4 ms. Even if a more typical efficiency of 5% is assumed the pressure impulse would last over 1 ms. In either case, a vapor explosion, if it occurred, would produce a destructively large pressure wave.

8.4 Conclusions

We have considered three types of reactions between hot fuel element fragments and water. We lack the detailed knowledge of the specific reactor geometry, and the understanding of the basic phenomena necessary to specify which of these reactions or which combination of reactions would actually occur in a reactor accident scenario.

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9.0 Incore/Intact Pin Scenario Summary

This chapter summarizes the significant results obtained in the preceding chapters pertaining to those phases of the TQUX and TQUV sequences when the system undergoes the FSAR Chapter 15 operational transient to the point where the core pin begins to form a debris bed on top of the subassembly lower tie plates within intact can walls.

Table 9-1 shows the important events in the TQUX and TQUV sequences. It should be compared to Table 1-2 which was derived for prescribed thermodynamic end states. Given the spectrum of operational transients, it can be concluded that in general, with the possible exception of the control rod drop transient (insufficient time prevented an examination of this transient), the system reaches a quasistatic boiloff state in a few tens of seconds with the completion of flow coastdown and the equilibration of pin temperature profiles to decay heat levels. Variations in the spectrum can be considered as variations of $\pm 10\%$ in the nominal coolant inventory. While it proved impossible to examine this variation parameterically, it is probably a second order effect.

Boiloff in a quasistatic mode leads to a choice of automatic relief or less frequent but more substantial operator initiated depressurization. Valve cycling causes level cycling and the attendant temperature cycling. It appears that, at least for the TQUX sequence, the level swell duration and extent is such that early cladding failure due to the cycling is not probable. It is not clear at this stage whether this is significant for the hydrogen generation rate as most of that occurs when there is only 2-3 ft. of water left in the active core region for both the TQUX and TQUV sequences. The effect of automatic versus manual cycling at this time can only be speculated on but axial power shape effects are negligible. Radial two dimensional intersubassembly effects are more substantial. Intrasubassembly effects, if any, are not examined. The two dimensional intersubassembly flow patterns do not add significantly to core uncover time in this slow quasistatic boiloff but once core uncover occurs, the radial incoherence leads to initial sharp level differences between the various power subassemblies.

Table 9-1. Sequence of Events

t Seconds	TQUX	TQUV
Initiation of FSAR Chapter 15 Operational Transient	0	0
Quasistatic Steady State	few tens of seconds	few tens of seconds
Depressurization		300
Core Uncovery	1505	725
Rapid Hydrogen Generation	3230	2200
Cladding Melt	~3700	~2800
Pin Disruption/Formation of Debris Bed	~4000	~3000
Active Region Water Level at 1' (Average Subassembly)	~4000	~3200

These radial effects have their largest significance in the hydrogen generation rate. Table 9-2 shows the parametrics performed during the course of this study for the hydrogen generation rate. Also included are the effect on pin disruption, the mass flux rate of slumping core material and temperatures of internal structures and at the primary boundary. While the calculations performed in Chapters 4 and 5 show that at these flow rates typical of TQUX and TQUV the core heatup is basically adiabatic, the degree of nonadiabaticity does matter for certain parameters.

Hydrogen generation for both TQUX and TQUV is dominated by the phenomenon of steam starvation. All the steam generated is consumed in the cladding oxidation reaction once accelerated oxidation commences. Figure 9-1 illustrates the physics of the hydrogen production problem. The left hand axis is the rate of hydrogen production assuming that all the steam boiled off from the coolant pool is consumed by the oxidation reaction. The parametric computations detailed in Chapter 5 using the simple stand alone pin thermal code have shown that the propagation of the oxidation front axially is slow enough that the steam inventory need not be considered for most of the in core phase. The hydrogen generation curve is then just based on the steam production curve of Fig. 4.2. The dashed curves referring to the right hand axis are total hydrogen accumulation as a function of time assuming various time delays in the initiation of the oxidation reactions. This then simulates the fact that there is a threshold temperature before the rapid increase in the oxidation reaction, as noted in Chapter 5, occurs. Table 9-1 indicates that accelerated oxidation did not take place until ~1700 seconds after core uncover. Figure 9.1 shows that the $t = 1700$ curve gives about ~few hundred lbs. of hydrogen accumulation at clad melting. Most of the water is boiled off before enough superheating occurs for the accelerated oxidation. This implies that any mechanism which shifts the accelerated oxidation to the right while retaining the same boiloff rate will decrease the hydrogen production. Heat loss by radiation and perhaps natural convection from the top of the core to the shroud and the shroud head would be such a mechanism. Insufficient time prevented the examination of these possibilities.

Table 9-2. Parametric Results*

	Axial Power Shape	Subassembly Power	Heat Transfer Coefficient	Time to** Disruption/Node secs	Hydrogen Generation lbs	Mass ⁺ Flux lbs/sec		T °F Shroud	T °F Nozzle
						Peak	~200 sec Average		
T Q U X	Nominal	Average	High	2500/9 or 7	300	3.5	0.5	1450	800
			Low	2500/9 or 8	320		0.7	870	870
			Average	2500/9 or 8	300	5	0.5	1300	800
	High	High		2200/9	290		0.5		
			Low	2200/8	310		0.7		
			Average	2200/8 or 9	300		0.5		
	Low	High		4700/8	179		0.5		
			Low	4700/7	189	7.5	0.4		
			Average	4700/8	180		0.5		
	Flat	Average	Average	2600/10	280*	0.4	0.4		
T Q U V	Nominal	Average	Average	2300/8	275	8.6	0.5	830	700

* At point of fuel disruption.

+ Per subassembly.

** Time from core uncover.

* Uses outlet steam starvation trigger.

Figure 9-1. TQUX Average Power Case Hydrogen Production

The absence of heat transfer coefficient effects can be attributed to the fact that the pin is basically heating up adiabatically so the timing of the accelerated oxidation is not altered significantly when the heat transfer coefficient is altered. The explanation for the dependence upon the subassembly power is more involved as the level and steam flow histories differ according to the radial incoherency. In addition, the low power subassembly active region boils dry before pin disruption occurs. However, the basic element still is that rapid oxidation is shifted to the right for the low power case as compared to the high power case thus resulting in less steam production and therefore decreased hydrogen generation. A different oxidation correlation, as those discussed in Chapter 5, would only have an effect if it decreased the hydrogen production rate. In addition, the phenomenon of hydrogen blanketing, which would again only decrease the production, was not considered.

The two dimensional intersubassembly thermal hydraulics effects during the boil dry phase in the individual subassemblies primarily influence the steam flow rate once the water levels decrease to 3-4 ft. The levels themselves are not strongly effected by the difference in void profiles and power. The profiles merge together, the magnitudes become small and there is significant subcooling. As a significant portion of the hydrogen generation occurs during this period the two dimensional intersubassembly flow effects are essentially restricted to steam flow effects.

All the factors imply that the numbers generated in this study are upper bounds. In general, it can be concluded that steam starvation dominates and that ~ 5-10% of the core zircaloy content is oxidized in a period of several hundred seconds with the production of a few hundred lbs. of hydrogen within a few thousand seconds of scram for both TQUX and TQUY.

Table 9-2 also shows that fuel pin disruption timing is highly dependent upon the subassembly power level which is an indication of the radial incoherence present but, once again, as the heat up is basically adiabatic, the heat transfer coefficients are not a factor. Axial location of initial disruption does not vary significantly in the set of parametrics presented as the nodes are only 1 ft. in length in a twelve foot core. Thus variations in drop

height should not affect impact velocity whether in a free fall mode or a refreezing/remelting slumping mode and, therefore, should not influence the resulting fuel coolant interaction. The resulting fuel coolant interaction does appear to be influenced by the axial power shape. The flat power shape case results in an initial mass flux rate which is a factor of ~10 lower than the nominal axial power shape. However, while the nominal shape with the average subassembly power does lead to a relatively energetic fuel coolant interaction, it would definitely be premature to extrapolate to definitive conclusions. The fuel pin disruption model considered in Chapter 6 leads to upper bounds on the mass flux rates as it does not consider the delaying and decelerating effects of fuel motion due to refreezing/remelting and jamming. By assuming that the motion was cladding melt rate controlled, the model leads to upper bounds. This is an area which deserves further examination. Furthermore, the low power subassemblies boil dry to the lower fuel tie plate, if there are no interactive effects from disrupted higher power subassemblies, thus deleting the possibility of fuel coolant interaction in those subassemblies. The effect of axial heat losses should be examined in order to increase the confidence in this conclusion.

The parameters for which the heat transfer coefficients are important are indicated by Table 9-2 to be the shroud head and steamline temperatures. It should be noted that when "nozzle" is used, it actually refers to the steam line at the nozzle region. As the line is much thinner than the actual nozzle, it is more vulnerable to the thermal hydraulic environment. The shroud head which supports the steam separators and standpipes in particular, has a swing of a few hundred degrees Fahrenheit for the TQUX sequence depending upon the heat transfer coefficient. The nozzle appears to be well protected by the separators and the dryer regardless of the variation in the heat transfer coefficient. If significant bypass of the dryer occurs with the decrease in level and the disappearance of the liquid seal, as depicted in Fig. 1-9, the temperature of the nozzle could increase by ~200°F, which is the temperature drop in the dryer for the nominal case. The calculations presented in Chapter 7 on structural failure demonstrate that this is still insufficient to cause nozzle failure on the few thousand second timescale characteristic of the incore/intact pin phase. This means that for both the TQUX and TQUV sequences primary boundary loss of integrity is not likely to occur prior to fuel pin

disruption and debris bed formation. This incore pin phase will occur at the initial relatively high pressure levels. In addition, the structural failure computations in Chapter 7 also indicate that in spite of the seemingly higher temperatures of the shroud head, the shroud head will not fail and collapse on the core before thermal induced disruption of the fuel pins occur. Early incore debris bed formation will not occur with the collapse of the steam separators on the thermally weakened core structure. Debris bed formation and the end of the incore scenario occurs with the thermal degradation of the clad.

10. In-Vessel Cooling of Core Debris

This section describes the transient behavior of core debris in the stage following the completion of the incore scenario. One major issue is to determine the conditions for debris bed dryout at high pressures (~1100 psi) commonly encountered in BWRs. In the following subsections, previous work on the critical heat flux relevant to the present study is reviewed and a simple model describing the effect of system pressure is presented. The phenomena of boiling and dryout as a core debris bed forms within the individual fuel subassembly are studied. A scoping calculation is performed to determine the transient water level and the steam generation rate. Melting of the core debris (mainly Zircaloy) and the can wall during the depletion of water in the fuel subassemblies is then considered. Molten debris flow into the interstitial volume following melt-through of a portion of the can wall located above the water level is assumed. Also assumed is that there is no melting of the lower tie plate in this period since it remains covered by water. This leads to the formation of a debris layer consisting of solid fuel and molten Zircaloy on top of the core support plate. Conductive heating of the plate by the debris layer and downfacing boiling of water in the lower plenum are modeled and the time for local failure of the core support plate is estimated. Also estimated are the jet velocity during debris flow into the lower plenum and the total drainage time. Cooling of the core debris by heating and boiling of water in the lower plenum is studied so as to calculate the rate of steam generation and the time at which all the water in the lower plenum is depleted by boiling. Finally, modeling of the two-dimensional, transient behavior of the dry debris layer inside the dome-shaped vessel head is discussed.

10.1 Literature Review on Dryout Studies

The critical heat flux for a debris bed is the maximum heat flux attainable without dryout in a portion of the bed. The term "dryout" refers to the situation in which decay heating exceeds heat transfer from a debris bed to the coolant, resulting in boil-dry of liquid from a sizable area of the bed and an abrupt rise of bed temperature to cause melting. Early studies have been performed almost exclusively to simulate the debris conditions for

sodium-cooled fast reactors. The dryout of a debris bed under postulated LWR accident conditions has received considerable attention only in recent years owing to the increasing interest in possible degraded core cooling sequences. Besides the properties of the coolant (sodium versus water) and the decay power level, there are two main differences between the LMFBR and LWR cases. These are the particle size and the system pressure, both of which are much larger in the case of LWRs.

Numerous studies have been performed to determine the conditions for debris bed dryout. Dryout heat flux data have been obtained for both volume-heated and bottom-heated particulate beds consisting of a variety of solid materials including UO_2 , stainless steel, nickel, lead, copper, and glass [1-14]. The liquid media that have been employed in these experiments include water, sodium, methanol, acetone, Freon 113, and isopropanol. Wide ranges of particle sizes and bed depths have been explored. There are, however, large discrepancies not only between the data taken by various apparatus, but also between data taken with different materials in the same equipment. Besides, most experiments have been conducted under the atmospheric conditions. It appears that [14] is the only study that has explored the higher pressure conditions (up to 0.5 MPa).

Many models have been developed for predicting the conditions required to induce dryout in a heated debris bed. For laminar flow in the interstitial volume of the bed usually occurring in small particles that have diameters less than one millimeter, dryout models have been set forth by Hardee and Nilson [15], Dhir and Catton [16], Jones et al. [17], and Gabor et al. [18]. For turbulent flow encountered in beds of larger particles (>1 mm), models and correlations for predicting dryout heat fluxes include those of Lipinski [19], Ostensen and Lipinski [20], Henry and Fauske [21], and Dhir and Barleon [22]. Time-dependent debris bed boiling and dryout has also been modeled by Turland and Moore [23] and Gorham-Bergeron [24]. Most recently, Lipinski [25] has extended his previous models to include both laminar and turbulent flows. Owing to the immense scatter in the available data, it is extremely difficult to provide a definitive appraisal of any one of these models.

There are many parameters that affect the conditions for debris bed dryout. These include the size and the shape of the particles, the bed depth, the intrinsic properties of the solid and the liquid phases, the boundary and the heating conditions, the height of the liquid column above the bed and its degree of subcooling, the system pressure, the configuration (two-dimensional) of the bed, etc. Physically, it is the type of flow of the vapor phase (bubbling versus flooding for example) that dictates the critical conditions for dryout. The formation of channels is responsible for the much higher dryout heat fluxes observed in shallow beds than those in deep beds.

10.2 Determination of the Critical Heat Flux

To assess the conditions for debris bed dryout at high pressures (~7.6 MPa) encountered in BWRs, extrapolation of the low-pressure data is required. This is achieved by employing the formula advanced by Gabor et al. [26]

$$Q_d(P_1) = \frac{\{h_v[(\rho_l - \rho_v)\rho_v]^{1/2}\}_{P_1}}{\{h_v[(\rho_l - \rho_v)\rho_v]^{1/2}\}_{P_0}} Q_d(P_0) \quad , \quad (1)$$

where $Q_d(P_1)$ and $Q_d(P_0)$ are the critical heat fluxes at pressures P_1 and P_0 , respectively, h_v the latent heat of vaporization, ρ_l the liquid density, and ρ_v the vapor density. Since the liquid phase is a condensed phase and since $\rho_l \gg \rho_v$, we have

$$[(\rho_l - \rho_v)^{1/2}]_{P_1} \sim [(\rho_l - \rho_v)^{1/2}]_{P_0} \quad . \quad (2)$$

From thermodynamic considerations [27], it can be shown that

$$\frac{1}{h_v} \int_{P_0}^{P_1} \left(\frac{\partial h_v}{\partial P} \right)_T dP \ll 1 \quad \text{or} \quad h_v(P_1) \sim h_v(P_0) \quad . \quad (3)$$

The above relationship holds even though $(P_1 - P_0)$ is of the order of 100 atm. Finally, assuming that ideal gas law applies, we have

$$\frac{(\rho_v)_{P_1}}{(\rho_v)_{P_0}} \sim \frac{P_1}{P_0} \quad (4)$$

Equation (1) thus reduces to

$$Q_d(P_1) = \left(\frac{P_1}{P_0}\right)^{1/2} Q_d(P_0) \quad (5)$$

The above result has been compared favorably with the experimental data presented in [14] over the pressure range of 0.02 to 0.5 MPa.

Our next step is to obtain the value of $Q_d(P_0)$. Previous studies [1-13] indicate that there is a sharp increase in the dryout heat flux as the bed depth falls below the channel depth, h , and thus enters the shallow-bed regime. On the other hand, the dryout heat flux is independent of the bed depth in the deep-bed regime when the bed depth exceeds the value of h . A model for predicting channel depth in particulate beds has been proposed by Jones et al. [28]. This is

$$h = \frac{6\sigma}{(\rho_s - \rho_l)g\epsilon D} \quad (6)$$

where σ is the interfacial surface tension, ρ_s the solid density, ρ_l the liquid density, g the gravitation acceleration, ϵ the porosity of the bed, and D the particle diameter. For fuel debris in water, h is of the order of 20 mm. Thus we are facing a deep-bed regime in BWRs. The data of [14], obtained for the bed depth of 80 mm, may be applied. For particle diameter of 3 mm or larger, the dryout heat flux at 0.5 MPa is approximately

$$Q_d(0.5) \sim 2.5 \times 10^6 \text{ W/m}^2 . \quad (7)$$

From Eq. (5), we have, at 7.6 MPa (~1100 psi)

$$Q_d(7.6) = \left(\frac{7.6}{0.5}\right)^{1/2} (2.5 \times 10^6) \sim 1 \times 10^7 \text{ W/m}^2 . \quad (8)$$

Accordingly, dryout is not likely to occur in BWRs unless the rate of decay heating per unit cross-sectional area is of the order of $1 \times 10^7 \text{ W/m}^2$.

10.3 Debris Bed Behavior within the Subassemblies

Results of the incore studies presented in previous sections for the TQUX sequence indicate that at 3905 seconds after scram, pin debris start to flow and relocate downward within each subassembly. The mass flow rates of UO_2 and Zircaloy on the average, are on the order of $\dot{m}_{\text{UO}_2} = 0.152 \text{ kg/s}$ and $\dot{m}_{\text{Zirc}} = 3.03 \times 10^{-2} \text{ kg/s}$, respectively. The volumetric flow rate, V , of the core debris as a whole is given by

$$V = \frac{\dot{m}_{\text{UO}_2}}{\rho_{\text{UO}_2}} + \frac{\dot{m}_{\text{Zirc}}}{\rho_{\text{Zirc}}} = 1.85 \times 10^{-5} \text{ m}^3/\text{s} . \quad (9)$$

Referring to the surface of the core support plate, the height of the debris bed is given by

$$d = 0.203 + \frac{V(t-3905)}{(1-\epsilon)A_s} , \quad (10)$$

where ϵ is the bed porosity and A_s is the flow area in each subassembly. The latter is $A_s = (7.804)/(764) = 1.0 \times 10^{-2} \text{ m}^2$. The value of ϵ is taken to be 0.4 based on the measured data of [14], which is found to be almost independent of particle diameters. This gives

$$d = 0.203 + 3.02 \times 10^{-3} (t-3905) \quad . \quad (11)$$

The above expression is valid during the period $3905 \text{ s} < t < t_w$, where t_w is the time at which the water level starts to drop below the debris bed height. For $t > t_w$, we have

$$d = 0.203 + 3.02 \times 10^{-3} (t_w-3905) + 1.81 \times 10^{-3} (t-t_w) \quad . \quad (12)$$

To determine the transient water level, l , referring to the surface of the core support plate, heat transfer between the core debris and water needs to be considered. At 3905 s, the decay power level is about 1.25%. Based on the nominal core power of 3382.5 MWt, the rate of decay heating per unit weight of fuel is approximately $2.67 \times 10^2 \text{ W/kg}$. The water level at 3905 s is 0.406 m above the bottom of the active core or 0.558 m above the lower tie plate. Heat removal from a debris bed of this height requires a surface heat flux of

$$Q_s = (2.67 \times 10^2) (0.558) (1-\epsilon) (\dot{m}_{\text{UO}_2} / V) = 7.34 \times 10^5 \text{ W/m}^2 \quad , \quad (13)$$

which is much less than the value of $Q_d(7.6)$ given by Eq. (8). Thus dryout will not occur in the bed. Boiling is the only mechanism to deplete the water causing remelting of the core debris. Assuming the rate of boiling of water or the steam generation rate to be $\dot{m}_{\text{H}_2\text{O}}$, the transient water level becomes

$$l = 0.761 + [1 - (1-\alpha)\epsilon] (d-0.203) - \dot{m}_{\text{H}_2\text{O}} (t-3905) \quad , \quad (14)$$

where α is the liquid-vapor void fraction and the value 0.761 refers to the water level in meter above the core support plate at $t = 3905 \text{ s}$. A value of $\alpha = 0.2$ will be used. Equation (14) gives the water level within the subassemblies. In the interstitial volume, the water level is

$$l_i = 0.758 - \dot{m}_{\text{H}_2\text{O}} (t-3905) \quad . \quad (15)$$

From Eqs. (11) and (14), the time t_w at which the water level is the same as the debris bed height is given by

$$t_w = 3905 + [\dot{m}_{H_2O} + 3.02 \times 10^{-3}]^{-1} \{0.558 + [1 - (1-\alpha)\epsilon](d_w - 0.203)\} \quad (16)$$

where d_w is the value of d at time $t = t_w$.

During the transient formation of the core debris bed within the individual subassembly, energy is transferred from the hot debris to the coolant via boiling. The initial temperature of the debris consisting of molten Zircaloy and solid fuel is taken to be 1975°C whereas the initial temperature of water is at 295°C . The steam generation rate is given by

$$\dot{m}_{H_2O} = \frac{\dot{q}_1 + \dot{q}_2 + \dot{q}_3}{A L_{H_2O}} \quad , \quad (17)$$

where A is the flow area, L_{H_2O} the latent heat of vaporization of water, $\dot{q}_1$ the heat transfer rate required to cool the debris down to the boiling point of water, $\dot{q}_2$ the decay heating rate in the portion of the active core covered by water, and $\dot{q}_3$ the additional rate of decay heating due to the debris bed itself. The values of A and L_{H_2O} are taken to be $1.84 \times 10^{-2} \text{ m}^2$ and $1.46 \times 10^6 \text{ J/kg}$, respectively. The value of $\dot{q}_1$ may be estimated as

$$\dot{q}_1 = (C_{UO_2} \Delta T) \dot{m}_{UO_2} + (C_{Zirc} \Delta T + L_{Zirc}) \dot{m}_{Zirc} \quad , \quad (18)$$

where C_{UO_2} and C_{Zirc} are the heat capacities of fuel and Zircaloy, respectively, L_{Zirc} the latent heat of fusion of Zircaloy, and $\Delta T = (1975-295) = 1680^\circ\text{C}$. Using the values of $C_{UO_2} = 320 \text{ J/kg-K}$, $C_{Zirc} = 330 \text{ J/kg-K}$, $L_{Zirc} = 2.25 \times 10^5 \text{ J/kg}$, we have

$$\dot{q}_1 = 1.053 \times 10^5 \text{ W} \quad . \quad (19)$$

The values of $\dot{q}_2$ and $\dot{q}_3$ are given respectively by

$$\dot{q}_2 = (4.427 \times 10^6 F) \left(\frac{\lambda - 0.356}{3.81} \right) , \quad (20)$$

$$\dot{q}_3 = (4.427 \times 10^6 F) \left[\frac{0.152 (t-3905)}{207.6} \right] , \quad (21)$$

where F is the time-dependent decay power level given in Section 3. Equations (14) to (21) may be solved simultaneously to give the transient water level and steam generation rate. In doing so, the value of t_w is also determined. This is $t_w = 4015$ s, at which time we have $d = \lambda = 0.53$ m.

For $t > 4015$ s, the water level drops below the height of the core debris and Eqs. (14) to (21) are no longer valid. Downward conduction from the water-free debris layer to the core debris bed needs to be considered. As the thickness of the water-free debris layer increases, melting of the can wall and the flow of debris will proceed. Using a quasi-steady approach, it can be shown that

$$\dot{q}_d = \left[k_d \left(\frac{1680}{d - \lambda} \right) + \frac{S}{2} (d - \lambda) \right] A_s , \quad (22)$$

where the debris conductivity k_d and the volumetric rate of heating S are given respectively by

$$k_d = \frac{k_{UO_2} (\dot{m}_{UO_2} / \rho_{UO_2}) + k_{Zirc} (\dot{m}_{Zirc} / \rho_{Zirc})}{\dot{m}_{UO_2} / \rho_{UO_2} + \dot{m}_{Zirc} / \rho_{Zirc}} , \quad (23)$$

$$S = (4.427 \times 10^6 \text{ F}) \left[\frac{\dot{m}_{\text{UO}_2} / 207.6}{\dot{m}_{\text{UO}_2} / \rho_{\text{UO}_2} + \dot{m}_{\text{Zirc}} / \rho_{\text{Zirc}}} \right] . \quad (24)$$

With $k_{\text{UO}_2} = 2.5 \text{ W/m-K}$, $k_{\text{Zirc}} = 52 \text{ W/m-K}$, $\rho_{\text{UO}_2} = 10.98 \times 10^3 \text{ kg/m}^3$, and $\rho_{\text{Zirc}} = 6.4 \times 10^3 \text{ kg/m}^3$, we have

$$k_d = 15.08 \text{ W/m-K} \quad \text{and} \quad S = 1.75 \times 10^8 \text{ F} . \quad (25)$$

The steam generation rate for $4015 \text{ s} < t < t_d$, where t_d is the time at which all the water above the core support plate has been depleted, is given by

$$\dot{m}_{\text{H}_2\text{O}} = \frac{\dot{q}_d + (4.427 \times 10^6 \text{ F}) [0.7(\ell - 0.203)]}{A L_{\text{H}_2\text{O}}} , \quad (26)$$

where for $\ell < 0.203 \text{ m}$, the decay heating term is set equal to zero in the above equation. The water levels become

$$\ell = 0.54 - \dot{m}_{\text{H}_2\text{O}} (t - 4015) / [(1 - \alpha) \epsilon] , \quad (27)$$

$$\ell_i = 0.31 - \dot{m}_{\text{H}_2\text{O}} (t - 4015) , \quad (28)$$

where the value 0.31 refers to the water level in the interstitial volume at $t = 4015 \text{ s}$. Equations (22) to (28) may be solved simultaneously to obtain the transient values of $\dot{m}_{\text{H}_2\text{O}}$, ℓ , and ℓ_i . In doing so, the value of t_d is found to be 4685 s. Results are presented in Figs. 10.1 to 10.3 for the bed depth, water level, and steam generation rate over the period $3905 \text{ s} < t < 4685 \text{ s}$.

10.4 Debris Bed Behavior Above the Core Support Plate

After the water is depleted in the subassemblies, melting of the can walls and Zircaloy debris will continue to proceed, leading to the formation

Figure 10.1. Debris bed formation within the subassemblies: debris bed height versus time.

Figure 10.2. Water levels within the subassemblies and in the interstitial volume above the core support plate.

Figure 10.3. Transient steam generation rate during boiling of water by the core debris in the subassemblies.

of a layer of solid fuel debris in molten Zircaloy above the core support plate. The depth, δ , of the debris layer may be estimated as

$$\delta = [B_{UO_2} / \rho_{UO_2} + B_{Zirc} / \rho_{Zirc}] / A_c , \quad (29)$$

where B_{UO_2} and B_{Zirc} are the amounts of fuel and Zircaloy involved and A_c is the cross-sectional area of the core support plate. With $B_{UO_2} = 1.59 \times 10^5$ kg, $B_{Zirc} = 3.81 \times 10^4$ kg, and $A = 19.71$ m², we have $\delta = 1.037$ m. The core support plate, initially at the boiling point of water, is heated from above by the debris layer and cooled from below by downfacing boiling of water. The energy required to raise the plate temperature to its melting point ($\sim 1410^\circ\text{C}$) is

$$E_c = \rho_{ss} C_{ss} V \Delta T = 5.75 \times 10^6 \text{ kJ} , \quad (30)$$

where the values of $\rho_{ss} = 7.5 \times 10^3$ kg/m³, $C_{ss} = 600$ J/kg-K, $V = 1.145$ m³, and $\Delta T = 1115^\circ\text{C}$ have been employed. Using a quasi-steady approach, the downward conductive heat flux from the debris layer can be shown to be

$$Q_{\text{down}} = \frac{k_d (1975 - T_c)}{\delta} + \frac{S}{2} \delta , \quad (31)$$

where T_c is the time-averaged plate temperature and k_d , S are given by Eq. (25). According to the downfacing boiling experiments of Ishigai et al. [29], Anderson and Bova [30], and Sowa and Pavlik [31], the rate of downfacing boiling is estimated to be 4×10^4 W/m². The time required to raise the plate temperature to its melting point is, therefore,

$$\Delta t = E_c / [Q_{\text{down}} - 4 \times 10^4] A_c = 265 \text{ s} . \quad (32)$$

It follows that at approximately $t = 4950$ s, the core support plate is expected to fail (either mechanically or by melt-through). Note that the total decay energy generated within this time interval is about 1×10^7 kJ. This is 4.25×10^6 kJ in excess of the amount given by Eq. (30). A portion of the excess amount may be radiated upward and the remaining portion may be used up in

melting the Zircaloy and over-heating the core debris. The steam generation rate averaged over the time period, $4685 \text{ s} < t < 4950 \text{ s}$, is

$$\dot{m}_{\text{H}_2\text{O}} = \frac{4 \times 10^4}{1.46 \times 10^6} = 0.027 \text{ kg/m}^2 \cdot \text{s} , \quad (33)$$

which is an order of magnitude smaller than the values of $\dot{m}_{\text{H}_2\text{O}}$ shown in Fig. 10.3.

10.5 Debris Flow into the Lower Plenum and the Drainage Time

When the core support plate fails, one or more cracks or drainage holes may develop for debris flow. The driving force is the pressure head of the debris layer. Based on Torricelli's law, the velocity of the jet consisting of solid fuel debris and molten Zircaloy is given by

$$v_j = C_v (2 g \delta_{av})^{1/2} , \quad (34)$$

where C_v is the coefficient of vena contraction, g the gravitational acceleration, and δ_{av} the time-averaged value of δ . Using the experimental value of $C_v = 0.64$ and taking $\delta_{av} \sim 0.5 \text{ m}$, we have $v_j \sim 2.0 \text{ m/s}$. The volumetric flow rate is

$$V_j = \frac{1}{4} \pi D_j^2 v_j = 0.48 \text{ m}^3/\text{s} , \quad (35)$$

from which the mass flow rates of solid fuel debris and molten Zircaloy may be calculated respectively as

$$\begin{aligned} \dot{m}_{\text{UO}_2} &= \left(\frac{207.6/10.98 \times 10^3}{207.6/10.98 \times 10^3 + 49.9/6.4 \times 10^3} \right) (0.48) (10.98 \times 10^3) \\ &= 3.73 \times 10^3 \text{ kg/s} , \end{aligned} \quad (36)$$

$$\begin{aligned}\dot{m}_{Zirc} &= \left(\frac{49.9/6.4 \times 10^3}{207.6/10.98 \times 10^3 + 49.9/6.4 \times 10^3} \right) (0.48) (6.4 \times 10^3) \\ &= 0.9 \times 10^3 \text{ kg/s} .\end{aligned}\quad (37)$$

Assuming the diameter of the effective drainage hole to be the same as the diameter of the four lobed orifice holes, i.e., $D_j = 0.276$ m, the total drainage time is

$$\tau = \frac{\delta A_c}{\frac{1}{4} \pi D_j^2 v_j} = 42.5 \text{ s} .\quad (38)$$

To estimate the steam generation rate within this time interval, we assume the jet of core debris to be cooled from its initial temperature of $\sim 2000^\circ\text{C}$ to the boiling point of water upon quenching. The rate of heat transfer is

$$\dot{q}_j = (C_{UO_2} \Delta T) \dot{m}_{UO_2} + (C_{Zirc} \Delta T + L_{Zirc}) \dot{m}_{Zirc} = 2.75 \times 10^9 \text{ W} .\quad (39)$$

This will be used up in heating and boiling of the subcooled water in the lower plenum. At $t = 4950$ s, the total amount of water in the plenum is 6.3×10^4 kg. To raise this amount of water from approximately 278°C to 295°C requires an energy of the amount $E_1 = 4.5 \times 10^6$ kJ. To completely boil dry this amount of water requires an additional amount of $E_2 = 9.2 \times 10^7$ kJ. All the water in the lower plenum will be depleted within the time interval of

$$\Delta t_w = (E_1 + E_2)/\dot{q}_j = 35 \text{ s} .\quad (40)$$

Thus boil-dry occurs at $t = 4985$ s whereas the debris drainage process is completed at $t = 4992.5$ s. The steam generation averaged over the heat transfer area during the period $4950 \text{ s} < t < 4985 \text{ s}$ is

$$\dot{m}_{H_2O} = \frac{6.3 \times 10^4}{35 A_c} = 91.9 \text{ kg/m}^2\text{-s} . \quad (41)$$

The various in-vessel events occurring after the completion of the incore scenario is summarized in Fig. 10.4 along with the steam generation rate.

10.6 Behavior of the Core Debris Layer on the Vessel Head

After all the water in the lower plenum is depleted by boiling, a layer of dry solid debris consisting of approximately 80% by weight of fuel and 20% by weight of Zircaloy starts to form inside the dome-shaped vessel head. Initially, the system may be considered to be at a uniform temperature equal to the boiling point of water. It gradually warms up owing to decay heating of the fuel. To determine the transient temperature of the system, two-dimensional heat conduction in the heat-generating debris layer and in the vessel head need to be studied. The upper boundary condition for the debris layer may be written as

$$k_d \frac{\partial T_d}{\partial y} = \epsilon \sigma (T_d^4 - T_0^4) , \quad (42)$$

where ϵ is an interchange factor, σ the Stefan-Boltzmann constant, and T_0 is the temperature of the surrounding structure such as the core support plate. The lower boundary condition for the vessel head is

$$k_s \frac{\partial T_s}{\partial n} = h (T_s - T_\infty) , \quad (43)$$

where $n = n(x,y)$ is the contour of the lower surface of the vessel head, h the effective heat transfer coefficient, and T_∞ the temperature of the ex-vessel cavity. To be conservative, a value of $h = 0$ is recommended. This would lead to the highest rate of heating of the vessel head by the heat-generating core debris. At the interface (curved boundary) between the debris layer and the vessel head, the continuity conditions for temperature and heat flux may be used. The temperature distribution in the vessel head may then be determined and incorporated into the mechanical failure criteria such as those described

Figure 10.4. Various in-vessel events and the steam generation rate after the completion of the in-core scenario.

in Section 7 to predict the time and the mode of failure of the vessel head. Meanwhile, the core debris may start to remelt and relocate downward into the ex-vessel cavity. This marks the end of the in-vessel scenario.

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11.0 Conclusions

It could be concluded from the work detailed in Chapters 2-9 on the TQUX and TQUV sequences that for the incore/intact pin phase, namely, from scram to the formation of the core debris bed on the fuel lower tie plate while intact pin geometry is still retained;

- The incore phase is on the order of $3-4 \times 10^3$ seconds for the two sequences.
- Differences between the various FSAR Chapter 15 operational transients, with the possible exception of the control rod drop transient, could be accommodated in the context of a small variation in initial coolant inventory.
- The core heatup is essentially adiabatic rendering heat transfer coefficients largely unimportant.
- Hydrogen generation is dominated by steam starvation. An upper bound would be ~ few hundred lbs. of hydrogen produced and ~ 5-10% of core metal content oxidized. Core bottom or top heat losses, whether through radiation or natural convection, incore heat sinks such as control rods and axial heat conduction should be further examined.
- Two dimensional intersubassembly thermal hydraulic effects are largely restricted to effects on steam flowrate during the latter portion of the boil dry phase where the hydrogen production is most significant. Level and void profile effects are less significant in this period. Intra-subassembly effects, if any, were not examined.
- Early core disruption due to the failure of vessel upper internals is unlikely. Pin disruption and debris bed formation upon the lower fuel tie plate due to clad thermal degradation is the more likely mode of core disruption and occurs in $3-4 \times 10^3$ seconds after scram. Significant radial incoherencies exist with a mixture of intact and disrupted assemblies and delays of possibly several hundred to a thousand seconds.

- Axial power shape significantly influences slumping core mass flux rates into the coolant pool. The possibility of an energetic fuel coolant interaction will require a more detailed fuel motion model.
- The low power subassemblies which form a significant portion of the core appear to boil dry to the lower fuel tie plate, if there are no interactive effects from disrupted higher power subassemblies, before pin disruption. However, axial heat losses will have to be examined before it can be definitely concluded that there are no fuel coolant interactions in these subassemblies during this phase of the accident.
- Pressure boundary failure is not likely during the incore phase.

The preliminary scoping work documented in Chapter 10 on the debris bed phase which follows the incore phase with intact geometry indicates that

- Can wall failure should occur earlier than fuel lower tie plate failure which would then lead to debris bed formation upon the core support plate.
- Depletion of the coolant above the core support plate occurs at ~ 4700 seconds.
- Thermal failure of the core support plate and the boil dry of the coolant inventory in the lower plenum occurs at ~ 5000 seconds.

Although a comprehensive effort, particularly in the incore intact geometry phase, was made in this study to evaluate the effects of various factors, such as heat transfer coefficients, two dimensional incoherencies, and axial power shapes, it should be reemphasized that this study was prematurely terminated. Extrapolation of the results presented to generalized conclusions, particularly for the debris bed phase, should be treated with caution.

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APPENDIX A

DATA FOR A BWR-4 PLANT

REFERENCES

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REF.3 REALISTIC SIMULATION OF SEVERE ACCIDENTS IN BWRS -
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REF.4 DOCKET NO.50-259,OCT 19,1983.
INSPECTION AND REPAIR OF PIPE CRACKS ON BFNP.
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REF.5 CONTACT OF T.WEI WITH TVA-AUG 1984.

PENETRATIONS NOT BEING CONSIDERED

HEAD SPRAY NOZZLE	ELEV=875.5(IN)
LEVEL INSTRUMENTATION AND VENT NOZZLE	ELEV=875.5(IN)
SPARE NOZZLE	ELEV=875.5(IN)
CONTROL ROD DRIVE HYDRAULIC RETURN	ELEV=448.5(IN)
JET PUMP INSTRUMENT NOZZLE	ELEV=100(IN)
CORE DIF. PRESSURE & STANDBY LIQUID CONTROL NOZZLE	
FLUX MONITOR PENETRATION	

DATA CONFLICTS

TOP HEAD THICKNESS REF.2 AND REF.3
LOW PRESSURE COOLANT INJECTION REF.1 AND REF.3
FEEDWATER PIPE DOES NOT HAVE O.D. OR THICKNESS OF
USUAL SCHEDULE PIPES.
DIMENSIONS OF REF.1 AND REF.5 FOR RECIR. OUTLET PIPE
STEAM PIPE THICKNESS REF.1, P.4.6-1 AND REF. 5

DATA ON BOUNARIES BEING CONSIDERED

COMP	BOTTOM VESSEL HEAD			
MAT	SA533 GR B CC 1339		REF.1-TAB4.2-1	KRM
WGT		207500. (LB)	REF.1-TA34.2-2	KRM
DIM	INNER RADIUS	125.5 (IN)	REF.1-FIG4.3-1	KRM
DIM	THICKNESS (TOP)	6.3125 (IN)	REF.2-REF.3	KRM
DIM	THICKNESS (BOTTOM)	8.0 (IN)	REF.4-P8	KRM
END				
COMP	CONTROL ROD GUIDE TUBES			
NO.		185	REF.1-TAB3.3-1	KRM
MAT	304 SS		REF.1-P3.3-1	KRM
WGT		46250 (LB)	REF.1-TAB3.3-1	KRM
END				

COMP	CONTROL ROD GUIDE TUBE STUBS				
NO		185		REF.1-TAB3.3-1	
MAT	INCCONEL 600 SB166			REF.1-TAB4.2-1	
END					
COMP	CORE SHROUD				
MAT	304 SS			REF.1-P3.3-1	KRM
WGT		116900	(LB)	REF.1-TAB3.3-1	KRM
DIM	UPPER O.D.	220.	(IN)	REF.1-TAB3.3-1	KRM
DIM	CENTRAL O.D.	207.125	(IN)	REF.1-TAB3.3-1	KRM
DIM	CENTRAL THICKNESS	2.	(IN)	REF.1-TAB3.3-1, REF.4	KRM
DIM	LOWER O.D.	201.	(IN)	REF.1-TAB3.3-1	KRM
END					
COMP	CORE SHROUD HEAD				
MAT	304 SS			REF.1-P3.3-1	
WGT	WITH STEAM SEPARATOR	139600.	(LB)	REF.1-TAB3.3-1	
DIM	THICKNESS	2.	(IN)	REF.1-TAB3.3-1	
DIM	O.D.	220.	(IN)	REF.1-TAB3.3-1	
DIM ***	RADIUS OF CURVATURE	216.	(IN)	SCALE FROM REF.1, FIG.4.2-1	
DIM	POLAR ANGLE	30.	(DEG)	CALCULATED	
ELEV	TOP	442.63	(IN)	REF.1-FIG4.2-1	
END					
COMP	CORE SHROUD SUPPORT COLUMNS				
MAT	INCONEL 600 SB168			REF.1-TAB4.2-1	
END					
COMP	CORE SPRAY INLET NOZZLE				
NO.		2		REF.1-TAB4.2-2	KRM
MAT	SA508 C1 CC1332			REF.1-TAB4.2-1	KRM
SIZE		10.	(IN)	REF.1-TAB4.2-2	KRM
ELV	CENTERLINE	484.5	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP	CORE SPRAY PIPE				
NO.		2		REF.1-TAB4.2-2	KRM
MAT ***					
SIZE	NOMINAL DIAM.	10.	(IN)	REF.1-TAB4.2-2	KRM
DIM ***	INNER DIAM.				
DIM ***	THICKNESS				
ELV	CENTERLINE	484.5	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP	CORE SUPPORT				
MAT	304 SS			REF.1-P3.3-1	KRM
WGT		20500.	(LB)	REF.1-TAB3.3-1	KRM
DIM ***	PLATE THICKNESS	1.0	(IN)	REF.2	KRM
ELV	TOP	211.3125	(IN)	REF.1-FIG4.2-1	KRM
END					

COMP NO.	FEED WATER INLET NOZZLE	6		REF.1-TAB4.2-2	KRM
MAT	SA508 C1 CC1332			REF.1-TAB4.2-1	KRM
SIZE		12.	(IN)	REF.1-TAB4.2-2	KRM
ELV	CENTERLINE	498.5	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP NO.	FEED WATER PIPE	6		REF.1-TAB4.2-2	KRM
MAT ***					
SIZE	NOMINAL DIAM.	12.	(IN)	REF.1-TAB4.2-2	KRM
DIM ***	DIAM. I.D.	10.78	(IN)	REF.2	KRM
DIM ***	THICKNESS	1.219	(IN)	REF.2	KRM
ELV	CENTERLINE	498.5	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP	FLANGE				
MAT	SA508 CL2 CC1332			REF.1-TAB4.2-1	KRM
WGT		106000.	(LB)	REF.1-TAB4.2-2	KRM
ELV	BETWEEN	745.	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP NO.	FLANGE BOLTS	92			KRM
MAT ***					KRM
SIZE ***	AISI-4140 STEEL	6	(IN)		KRM
DIM ***	NOMINAL DIAM.				
END	I.D. THREAD ROOT				
COMP NO.	FUEL ASSEMBLY	764		REF.1-TAB3.2-1	KRM
MAT	ZIRCALOY-2		CLADDING	REF.1-P3.2-1	KRM
MAT	ZIRCALOY-4		CHANNEL	REF.1 P3.2-2	KRM
ELV	CORE MIDPLANE	288.3125	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP NO.	JET PUMP	20		REF.1-TAB4.3-1	KRM
MAT	304 SS			REF.1-P3.3-1	KRM
WGT		22700.	(LB)	REF.1-TAB3.3-1	KRM
END					
COMP	REACTOR VESSEL				
MAT	SA533 GR B CC 1339			REF.1-TAB4.2-1	KRM
WGT		842000	(LB)	REF.1-TAB4.2-2	KRM
DIM	I.D.	251.	(IN)	REF.1-TAB4.2-2	KRM
DIM	LENGTH	875.125	(IN)	REF.1-TAB4.2-2	KRM
DIM	THICKNESS	6.3125	(IN)	REF.2,REF.4-P9	KRM
END					
COMP	REACTOR VESSEL INTERNALS				
WGT		47700.	(LB)	REF.1-TAB3.3-1	KRM
END					

COMP NO.	RECIR.WATER INLET NOZZLE	10		REF.1-TAB4.2-2	KRM
MAT	SA508 C1 CC1332			REF.1-TAB4.2-1	KRM
SIZE		12.	(IN)	REF.1-TAB4.2-2	KRM
ELV	CENTERLINE	181.	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP NO.	RECIR.WATER INLET PIPE	10		REF.1-TAB4.2-2	KRM
MAT	304 SS			REF.1-P4.3-3	KRM
SIZE	NOMINAL DIAM.	12.	(IN)	REF.1-TAB4.3-1	KRM
DIM ***	DIAM. O.D.	12.75	(IN)	REF.4	KRM
DIM ***	THICKNESS	0.789	(IN)	REF.4	KRM
ELV	CENTERLINE	181.	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP NO.	RECIR.WATER OUTLET NOZZLE	2		REF.1-TAB4.2-2	KRM
MAT	SA508 C1 CC1332			REF.1-TAB4.2-1	KRM
SIZE		36. TO 28.	(IN)	REF.1-TAB4.2-2	KRM
ELV	CENTERLINE	161.5	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP NO.	RECIR.WATER OUTLET PIPE	2		REF.1-TAB4.2-2	KRM
MAT	304 SS			REF.1-P4.3-3	KRM
SIZE	NOMIAL DIAM.	28.	(IN)	REF.1-TAB4.2-2	KRM
DIM ***	DIAM. O.D.	28.146	(IN)	REF.4	KRM
DIM ***	THICKNESS	1.138	(IN)	REF.4	KRM
ELV	CENTERLINE	161.5	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP MAT	STEAM DRYER			REF.1-P3.3-1	KRM
WGT	304 SS	90000.	(LB)	REF.1-3.3-1	KRM
END					
COMP NO.	STEAM OUTLET NOZZLE	4		REF.1-TAB4.2-2	KRM
MAT	SA508 C1 CC1332			REF.1-TAB4.2-1	KRM
SIZE		26	(IN)	REF.1-TAB4.2-2	KRM
ELV	CENTERLINE	658.5	(IN)	REF.1-FIG4.2-1	KRM
END					
COMP NO.	STEAM OUTLET PIPE	4		REF.1-TAB4.2-2	KRM
MAT	SA106 GRADE B				KRM
SIZE	NOMINAL DIAM.	26.	(IN)	REF.1-TAB4.2-2	KRM
SIZE	SCHEDULE 80			REF.1-P4.6-1	KRM
DIM ***	INNER DIAM.	23.647	(IN)	REF.1-P4.6-1	KRM
DIM ***	DIA.O.D.	26.0	(IN)	REF.5	KRM
DIM ***	THICKNESS	0.95	(IN)	REF.5	KRM
ELV	CENTERLINE	658.5	(IN)	REF.1-FIG4.2-1	KRM
DIM ***					
END					

COMP	STEAM SEPARATER				
MAT	304 SS			REF.1-P3.3-1	KRM
WGT		139600.	(LB)	REF.1-TAB3.3-1	KRM
END					
COMP	SUPPORT SKIRT				
MAT	SA-802 GR B			REF.1-TAB4.2-1	KRM
WGT		28000.	(LB)	REF.1-TAB4.2-2	KRM
END					
COMP	TOP FUEL GUIDE				
MAT	304 SS			REF.1-P3.3-1	KRM
WGT		15200.	(LB)	REF.1-TAB3.3-1	KRM
END					
COMP	TOP VESSEL HEAD				
MAT	SA533 GR B CC 1339			REF.1-TAB4.2-1	KRM
WGT		252000.	(LB)	REF.1-TAB4.2-2	KRM
DIM	INNER RADIUS	125.6875	(IN)	REF.1-FIG4.3-1	KRM
DIM ***	THICKNESS	6.3125	(IN)	REF.2	KRM
DIM ***	THICKNESS (TOP)	3.375	(IN)	REF.3-P9	KRM
DIM ***	THICKNESS (BOTTOM)	4.875	(IN)	REF.3-P9	KRM
END					

Appendix B: Clad Axial Conduction

To examine the assumption of negligible clad axial heat conduction in the boiloff of the coolant inventory, a simple model was constructed. Reference should be made to Fig. B-1.

The clad axial heat flux at the coolant interface is approximated by

$$q_{axial} = k_{cl} \frac{\Delta T_{cl}}{(l_0 - l)} 2\pi r \delta r. \quad (1)$$

This assumes a straight line cladding temperature the validity of which will be discussed later. We also have

$$\Delta T_{cl} = T_{cl}(l_0) - T_{cl}(l) = \Delta T_{cool} + \delta \Delta T_{inter}. \quad (2)$$

Making the assumption of quasistatics which should give the largest gradient in the cladding gives

$$\Delta T_{cool} = \frac{Q}{W_{st} C_{st}} = \int_l^{l_0} p''' \frac{\pi r^2}{W_{st} C_{st}} dx \quad (3)$$

$$\delta \Delta T_{inter} = \frac{r}{2} \delta \left(\frac{p'''}{h_{st}} \right). \quad (4)$$

This translates into

$$\frac{q_{axial}}{q_{pool}} = \frac{2\pi r \delta r}{l} \frac{k_{cl}}{W_{st} C_{st}}. \quad (5)$$

From quasistatics we also have

$$W_{st} = P''' \frac{\pi r_{st}^2 L}{L_{st}} \quad (6)$$

Combining (5) and (6) gives

$$\theta = \frac{q_{axial}}{q_{pool}} = \frac{2\delta r}{r_{st}^2 P'''} \left(\frac{L_{st} k_{cl}}{C_{st}} \right) \quad (7)$$

Using typical #s at 2% decay heat leads to

Table B-1

z ft.	$\theta\%$
1	1
0.5	4
0.25	16

This means that cladding heat transfer becomes important to pool boiloff at $\sim 1/4$ ft. pool depth.

If the assumption of a straight line temperature is reexamined what is needed is

$$\alpha = \frac{\Delta T_{cl}/(z_0 - z)}{\frac{dT_{cl}}{dr}} = \frac{P'''_{av}}{P'''(z)} \quad (8)$$

which is typically $\sim 25\%-50\%$ off so Table B-1 is fairly representative. Furthermore, the results for the TQUX and TQUV cases for the average, high and

low subassemblies discussed in Chapter 5 were also examined. As the stand-alone pin code used to generate these results does not include axial conduction, the temperature gradients from those calculations should be overestimates. At the point of pin disruption the gradients are $\sim 500^\circ\text{F}/\text{ft}$, which leads to heat transfer rate of $\sim 1.5\text{--}2$ watts into the pool. The pool level is ~ 1 ft. and the decay heat level is ~ 25 watt/ft so the error in neglecting clad heat transfer is $\sim 5\text{--}10\%$ which is consistent with Table B-1. Fuel axial conduction is even less than cladding heat conduction as the poor conductivity ($\sim$ factor of 10) dominates over the larger heat transfer ($\sim$ factor of 3).

Nomenclature

C	specific heat
h	heat transfer coefficient
k	conductivity
L	latent heat
P'''	power density
Q	total power
r	radius
T	temperature
W	mass flow rate

Subscripts

cl	clad
cool	coolant
inter	interface
st	steam

Figure B.1. Geometry.

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1. 1. 2. 3. 4. 5. 6. 7. 8. 9. 10. 11. 12. 13. 14. 15. 16. 17. 18. 19. 20. 21. 22. 23. 24. 25. 26. 27. 28. 29. 30. 31. 32. 33. 34. 35. 36. 37. 38. 39. 40. 41. 42. 43. 44. 45. 46. 47. 48. 49. 50. 51. 52. 53. 54. 55. 56. 57. 58. 59. 60. 61. 62. 63. 64. 65. 66. 67. 68. 69. 70. 71. 72. 73. 74. 75. 76. 77. 78. 79. 80. 81. 82. 83. 84. 85. 86. 87. 88. 89. 90. 91. 92. 93. 94. 95. 96. 97. 98. 99. 100. 101. 102. 103. 104. 105. 106. 107. 108. 109. 110. 111. 112. 113. 114. 115. 116. 117. 118. 119. 120. 121. 122. 123. 124. 125. 126. 127. 128. 129. 130. 131. 132. 133. 134. 135. 136. 137. 138. 139. 140. 141. 142. 143. 144. 145. 146. 147. 148. 149. 150. 151. 152. 153. 154. 155. 156. 157. 158. 159. 160. 161. 162. 163. 164. 165. 166. 167. 168. 169. 170. 171. 172. 173. 174. 175. 176. 177. 178. 179. 180. 181. 182. 183. 184. 185. 186. 187. 188. 189. 190. 191. 192. 193. 194. 195. 196. 197. 198. 199. 200. 201. 202. 203. 204. 205. 206. 207. 208. 209. 210. 211. 212. 213. 214. 215. 216. 217. 218. 219. 220. 221. 222. 223. 224. 225. 226. 227. 228. 229. 230. 231. 232. 233. 234. 235. 236. 237. 238. 239. 240. 241. 242. 243. 244. 245. 246. 247. 248. 249. 250. 251. 252. 253. 254. 255. 256. 257. 258. 259. 260. 261. 262. 263. 264. 265. 266. 267. 268. 269. 270. 271. 272. 273. 274. 275. 276. 277. 278. 279. 280. 281. 282. 283. 284. 285. 286. 287. 288. 289. 290. 291. 292. 293. 294. 295. 296. 297. 298. 299. 300. 301. 302. 303. 304. 305. 306. 307. 308. 309. 310. 311. 312. 313. 314. 315. 316. 317. 318. 319. 320. 321. 322. 323. 324. 325. 326. 327. 328. 329. 330. 331. 332. 333. 334. 335. 336. 337. 338. 339. 340. 341. 342. 343. 344. 345. 346. 347. 348. 349. 350. 351. 352. 353. 354. 355. 356. 357. 358. 359. 360. 361. 362. 363. 364. 365. 366. 367. 368. 369. 370. 371. 372. 373. 374. 375. 376. 377. 378. 379. 380. 381. 382. 383. 384. 385. 386. 387. 388. 389. 390. 391. 392. 393. 394. 395. 396. 397. 398. 399. 400. 401. 402. 403. 404. 405. 406. 407. 408. 409. 410. 411. 412. 413. 414. 415. 416. 417. 418. 419. 420. 421. 422. 423. 424. 425. 426. 427. 428. 429. 430. 431. 432. 433. 434. 435. 436. 437. 438. 439. 440. 441. 442. 443. 444. 445. 446. 447. 448. 449. 450. 451. 452. 453. 454. 455. 456. 457. 458. 459. 460. 461. 462. 463. 464. 465. 466. 467. 468. 469. 470. 471. 472. 473. 474. 475. 476. 477. 478. 479. 480. 481. 482. 483. 484. 485. 486. 487. 488. 489. 490. 491. 492. 493. 494. 495. 496. 497. 498. 499. 500. 501. 502. 503. 504. 505. 506. 507. 508. 509. 510. 511. 512. 513. 514. 515. 516. 517. 518. 519. 520. 521. 522. 523. 524. 525. 526. 527. 528. 529. 530. 531. 532. 533. 534. 535. 536. 537. 538. 539. 540. 541. 542. 543. 544. 545. 546. 547. 548. 549. 550. 551. 552. 553. 554. 555. 556. 557. 558. 559. 560. 561. 562. 563. 564. 565. 566. 567. 568. 569. 570. 571. 572. 573. 574. 575. 576. 577. 578. 579. 580. 581. 582. 583. 584. 585. 586. 587. 588. 589. 590. 591. 592. 593. 594. 595. 596. 597. 598. 599. 600. 601. 602. 603. 604. 605. 606. 607. 608. 609. 610. 611. 612. 613. 614. 615. 616. 617. 618. 619. 620. 621. 622. 623. 624. 625. 626. 627. 628. 629. 630. 631. 632. 633. 634. 635. 636. 637. 638. 639. 640. 641. 642. 643. 644. 645. 646. 647. 648. 649. 650. 651. 652. 653. 654. 655. 656. 657. 658. 659. 660. 661. 662. 663. 664. 665. 666. 667. 668. 669. 670. 671. 672. 673. 674. 675. 676. 677. 678. 679. 680. 681. 682. 683. 684. 685. 686. 687. 688. 689. 690. 691. 692. 693. 694. 695. 696. 697. 698. 699. 700. 701. 702. 703. 704. 705. 706. 707. 708. 709. 710. 711. 712. 713. 714. 715. 716. 717. 718. 719. 720. 721. 722. 723. 724. 725. 726. 727. 728. 729. 730. 731. 732. 733. 734. 735. 736. 737. 738. 739. 740. 741. 742. 743. 744. 745. 746. 747. 748. 749. 750. 751. 752. 753. 754. 755. 756. 757. 758. 759. 760. 761. 762. 763. 764. 765. 766. 767. 768. 769. 770. 771. 772. 773. 774. 775. 776. 777. 778. 779. 780. 781. 782. 783. 784. 785. 786. 787. 788. 789. 790. 791. 792. 793. 794. 795. 796. 797. 798. 799. 800. 801. 802. 803. 804. 805. 806. 807. 808. 809. 810. 811. 812. 813. 814. 815. 816. 817. 818. 819. 820. 821. 822. 823. 824. 825. 826. 827. 828. 829. 830. 831. 832. 833. 834. 835. 836. 837. 838. 839. 840
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60.      (3350.,0.0),(1.E6,0.0)
61. FUNCTION HSGEN=(0.0,85.0),(100.,75.0),(200.,61.5),(300.,51.5),...
62.      (400.,44.0),(500.,38.5),(600.,34.0),(800.,26.5),...
63.      (1000.,21.5),(1200.,17.5),(1400.,14.0),(1600.,12.0),...
64.      (1800.,10.0),(2000.,8.5),(2650.,5.0),(3000.,2.5),...
65.      (3350.,2.5),(3351.,0.0),(1.E6,0.0)
66. FUNCTION HTCAPP=(80.,5.73E-2),(620.,7.05E-2),(1160.,7.52E-2),...
67.      (1700.,7.76E-2),(2240.,7.88E-2),(2780.,8.48E-2),(3320.,...
68.      9.47E-2),(3860.,1.1E-1),(4400.,1.35E-1),(4940.,1.55E-1),...
69.      (5188.4,1.56E-1),(5192.,1.2E-1),(5480.,1.2E-1)
70. FUNCTION CONDF=(440.,9.79E-4),(1340.,5.62E-4),(2240.,4.33E-4),...
71.      (3140.,3.53E-4),(4040.,4.17E-4),(4922.,5.3E-4),(4940.,...
72.      4.01E-4),(4958.,2.73E-4),(5840.,4.49E-4)
73. FUNCTION HTCAFC=(80.,6.71E-2),(260.,7.21E-2),(692.,7.91E-2),...
74.      (1502.,8.96E-2),(1507.4,.12),(1543.,0.141),(1579.4,0.147),...
75.      (1615.4,0.172),(1651.4,0.195),(1687.4,0.184),(1723.4,.143),...
76.      (1759.4,0.112),(1786.4,8.5E-2),(3318.2,8.5E-2),(1.E6,8.5E-2)
77. FUNCTION CONDC=(80.,2.01E-3),(800.,2.81E-3),(1520.,3.69E-3),...
78.      (2240.,5.14E-3),(2960.,7.7E-3),(3320.,9.31E-3),(3322.,...
79.      5.78E-3),(3680.,5.78E-3),(1.E6,5.78E-3)
80. FUNCTION HTCAPS=(80.,1.27),...
81.      (556.20,1.27),(600.,0.979),(650.,0.806),(700.,.711),...
82.      (800.,0.621),(900.,0.594),(1000.,0.574),(1100.,0.575),...
83.      (1200.,0.577),(1300.,0.581),(2000.,0.581),(4000.,0.581),...
84.      (1.E6,0.581)
85. FUNCTION DENS=(80.0,2.5),...
86.      (556.20,2.5),(600.,2.21),(650.,2.00),(700.,1.84),...
87.      (800.,1.62),(900.,1.46),(1000.,1.33),(1200.,1.15),...
88.      (1500.,0.956),(1750.,0.848),(2000.,0.762),(2500.,0.633),...
89.      (3000.,0.542),(3500.,0.473),(4000.,0.420),(1.E6,0.0)
90. FUNCTION HTCPSS=(80.,9.79E-2),(530.,.110),(930.,.1210),...
91.      (1430.,.127),(1880.,.131),(2330.,.133),(1.E6,.133)
92. FUNCTION CONHSS=(80.,2.25E-3),(2548.,6.26E-3),(2649.,3.21E-3),...
93.      (1.E6,3.21E-3)
94. INIT
95.     LAX=LFUEL/NSECT
96.     LUP1=LSEP/NSECT
97.     RGAP=ALOG(RCA/RF0)/(TPI*KGAP)
98.     HCPL=RHOVL*CFL
99.     QF0=3382.E6/(2.929E-1*3600.*NPINS*12.5)
100.    VOLBS=(83.96/NPINS)
101.    WGLBS=0.0
102.    ICTUH=TSAT
103.    ITSTUH=TSAT
104.    ICTUP2=TSAT
105.    ICTUP3=TSAT
106.    ITSUP2=TSAT
107.    ITSUP3=TSAT
108.    RHNOZ=1./(AHUP3*HU3)
109.    RHUH=1./(AHUH*HUH)
110. NOSORT
111. DO 5 I=1,NSECT
112.     ICTF(I)=561.77
113.     ICTC(I)=556.47
114.     ICTUP1(I)=TSAT
115.     ITSUP1(I)=TSAT
116.     TCK(I)=(ICTC(I)+460.)/1.8
117.     ICTS(I)=TSAT
118.     ICKG(I)=1.0
119.     TS(I)=TSAT

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120.      WS=(1./NPINS)*AFGEN(HSGEN,TIME)
121.      WSRIN(I)=WS
122.      WSROUT(I)=WS
123.      WSRZR(I)=0.0
124.      ZZZ = (I-1)*LAX + LAX*0.5 + 1.0
125.      IF (ZZZ.GT.3.083) GO TO 2
126.      SH(I) = -3.5424 + 4.7856*ZZZ - 1.653*ZZZ*ZZZ + ...
127.           0.1911*ZZZ**3
128.      GO TO 5
129. 2      IF (ZZZ.GT.10.896) GO TO 3
130.      SH(I) = 1.075 + 3.574E-2*ZZZ - 3.427E-3*ZZZ*ZZZ
131.      GO TO 5
132. 3      SH(I) = 44.47 - 11.95*ZZZ + 1.106*ZZZ*ZZZ - 3.442E-2*ZZZ**3
133. 5      CONTINUE
134.      WRITE(6,6)
135. 6      FORMAT(1X,'$$$$$$$$$ THE AXIAL SHAPE FACTORS ARE:',/)
136.      WRITE(6,7) (SH(I),I=1,NSECT)
137. 7      FORMAT(1X,10(F8.6,4X)/)
138.      DYNAM
139.      TSCRM=TIME+1505.
140.      QF=0.1*QF0*((TSCRM+8.5)**(-0.2)-0.9612*(TSCRM+7.76E5)**(-0.2))
141. *      QF=0.1*QF0*((1505.+8.5)**(-0.2)-0.9612*(1505.+7.76E5)**(-0.2))
142.      TF=INTGRL(ICTF,STF,10)
143.      TC=INTGRL(ICTC,STC,10)
144.      TS =INTGRL(ICTS,STS ,10)
145.      WS=INTGRL(ICIG,SIIG,10)
146.      TUP1=INTGRL(ICTUP1,STUP1,10)
147.      TSUP1=INTGRL(ITSUP1,STSUP1,10)
148.      YH20=AFGEN(H2OH,TIME)
149. *      YH20=12.5+1.E-4
150.      YFLOAT=(YH20/LAX)+1.E-4
151.      ICOVER=YFLOAT
152.      WSINTR=AFGEN(HSGEN,TIME)
153. *      WSINTR=85.0
154.      WS=WSINTR/NPINS
155.      FCOVER=ICOVER
156.      BETA=YFLOAT-ICOVER
157.      MCPSS2=MUP2*AFGEN(HTCPSS,TUP2)
158.      MCPSS3=MUP3*AFGEN(HTCPSS,TUP3)
159.      NOSORT
160.      DO 10 I=1,NSECT
161.      MCPF(I)=RHOVF*AFGEN(HTCAPF,TF(I))
162.      RF(I)=ALOG(1.4142)/(TPI*AFGEN(CONDF,TF(I)))
163.      RC1(I)=ALOG(RCC/RCA)/(TPI*AFGEN(CONDC,TC(I)))
164.      UAF(I)=1.0/(RF(I)+RGAP+RC1(I))
165.      MCPC(I)=RHOVC*AFGEN(HTCAPC,TC(I))
166.      UAC1(I)=(TPI/ALOG(RC0/RCC))*AFGEN(CGNDC,TC(I))
167.      UAC2(I)=TPI/(ALOG(RC0/RCC)*(1.0/AFGEN(CONDC,TC(I)))+1.0/(HCS*RC0))
168. 10      STF(I)=(1.0/MCPF(I))*(-UAF(I)*(TF(I)-TC(I))+QF*SH(I))
169.      IF(ICOVER.EQ.0) GO TO 21
170.      DO 20 I=1,ICOVER
171.      STC(I)=(1.0/MCPC(I))*(UAF(I)*(TF(I)-TC(I))-UAC1(I)*(TC(I)-TSAT))
172.      STS (I)=0.0
173.      TSIN(I)=TSAT
174. 20      TSOUT(I)=TSAT
175.      IF(ICOVER.GE.NSECT) GO TO 30
176. 21      J=ICOVER+1
177.      UACT=UAC2(J)
178.      CPS(J)=AFGEN(HTCAPS,TS(J))
179.      MCPS(J)=VOLS*CPS(J)*AFGEN(DENS,TS(J))
180.

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181. TCK(J)=(TC(J)+460.)/1.8
182. IF(TCK(J).LE.1853.) KP(J)=COX2*EXP(-COX3/(R*TCK(J)))
183. IF(TCK(J).GT.1853.) KP(J)=COX4*EXP(-COX5/(R*TCK(J)))
184. SHG(J)=117.45*COX1*KP(J)/HG(J)
185. WSRZR(J)=(36./91.)*SHG(J)*(1./4.5359E5)*LAX
186. WSRIN(J)=WS
187. WSROUT(J)=WSRIN(J)-WSRZR(J)
188. IF(WSROUT(J).LE.0.0) SHG(J)=KH2*WSRIN(J)
189. IF(HG(J).GE.HZRMX) SHG(J)=0.0
190. QC(J)=5.972E-3*SHG(J)
191. STC(J)=(1.0/MCPC(J))*(UAF(J)*(TF(J)-TC(J))-UAC2(J)*(TC(J)-TS(J))+...
192. QC(J))
193. MCPT=BETA*MCPL+(1.0-BETA)*MCPS(J)
194. TERM1=1.0/MCPT
195. TERM2=UAC2*(TC(J)-TS(J))
196. TERM3=2.*(1.0-BETA)*WS*CPJ(J)*(TS(J)-TSAT)/LAX
197. STS(J)=TERM1*(TERM2-TERM3)
198. TSOUT(J)=2.*TS(J)-TSAT
199. J=ICOVER+2
200. IF(J.GT.NSECT) GO TO 30
201. DO 30 I=J,NSECT
202. TSIN(I)=TSOUT(I-1)
203. CPJ(I)=AFGEN(HTCAPS,TS(I))
204. MCPS(I)=VOLS*CPJ(I)*AFGEN(DENS,TS(I))
205. TCK(I)=(TC(I)+460.)/1.8
206. IF(TCK(I).LE.1853.) KP(I)=COX2*EXP(-COX3/(R*TCK(I)))
207. IF(TCK(I).GT.1853.) KP(I)=COX4*EXP(-COX5/(R*TCK(I)))
208. KP10=KP(10)
209. SHG(I)=117.45*COX1*KP(I)/HG(I)
210. WSRZR(I)=(36./91.)*SHG(I)*(1./4.5359E5)*LAX
211. WSRIN(I)=WSROUT(I-1)
212. IF(WSRIN(I).LT.0.0) WSRIN(I)=0.0
213. WSROUT(I)=WSRIN(I)-WSRZR(I)
214. IF(WSROUT(I).LE.0.0) SHG(I)=KH2*WSRIN(I)
215. IF(HG(I).GE.HZRMX) SHG(I)=0.0
216. QC(I)=5.972E-3*SHG(I)
217. STC(I)=(1.0/MCPC(I))*(UAF(I)*(TF(I)-TC(I))-UAC2(I)*(TC(I)-TS(I))+...
218. QC(I))
219. TERM4=1.0/MCPS(I)
220. TERM5=UAC2(I)*(TC(I)-TS(I))
221. TERM6=2.*WS*CPJ(I)*(TS(I)-TSIN(I))/LAX
222. STS(I)=TERM4*(TERM5-TERM6)
223. 30 TSOUT(I)=2.*TS(I)-TSIN(I)
224. HGLBS=0.0
225. DO 35 I=1,NSECT
226. HGLBS=HPINS*(HG(I))*LAX*2.205E-6+HGLBS
227. H12LBS=(4./91.)*HGLBS
228. 35 CONTINUE
229. TREMN=TLEFT(KP10)
230. TSTOP=TREMN-2000.
231. TSTIUM=TSOUT(10)
232. SORT
233. TUH=INTGRL(ICTUH,STUH)
234. TSTIUM=INTGRL(ITSTUH,STSMUH)
235. RMUH=(THUH/2.)/(AMUH*AFGEN(CONDSS,TUH))
236. UAUH=1./(RIUH+RMUH)
237. MCPUH=RMUH*AFGEN(HTCPS,THUH)
238. STUH=(1./MCPUH)*(UAUH*(-TUH+TSTIUM))
239. CPSUH=AFGEN(HTCAPS,TSTIUM)
240. MCPSUH=AFUH*CPSUH*AFGEN(DENS,TSTIUM)

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241. STSMUH=(1./MCPSUH)*(-UAUH*(TSTMUH-TUH)-...
242. 2.*HSINTR*CPSPUH*(TSTMUH-TSTIUH)/LUH)
243. TSTOUH=2.*TSTIUH-TSTIUH
244. NDSORT
245. DO 40 I=1,NSECT
246. TSUPI1(I)=TSUP01(I-1)
247. IF(I.EQ.1) TSUPI1(I)=TSTOUH
248. CPSUP1(I)=AFGEN(HTCAPS,TSUP1(I))
249. MCPSS1(I)=HUP1*AFGEN(HTCPSS,TUP1(I))
250. MCPSU1(I)=AFUP1*CPSPUP1(I)*AFGEN(DENS,TSUP1(I))
251. STUP1(I)=(1./MCPSS1(I))*(HUP*AHUP1*(TSUP1(I)-TUP1(I)))
252. STSUP1(I)=(1./MCPSU1(I))*(-HUP*AHUP1*(TSUP1(I)-TUP1(I))...
253. -2.*HSINTR*CPSPUP1(I)*(TSUP1(I)-TSUPI1(I))/LUP1)
254. 40 TSUP01(I)=2.*TSUP1(I)-TSUPI1(I)
255. TSUPI2=TSUP01(10)
256. SORT
257. TUP2=INTGRL(1CTUP2,STUP2)
258. TUP3=INTGRL(1CTUP3,STUP3)
259. TSUP2=INTGRL(ITSUP2,STSUP2)
260. TSUP3=INTGRL(ITSUP3,STSUP3)
261. TSUPI3=TSUP02
262. CPSUP2=AFGEN(HTCAPS,TSUP2)
263. CPSPUP2=AFGEN(HTCAPS,TSUP3)
264. MCPSU2=AFUP2*CPSPUP2*AFGEN(DENS,TSUP2)
265. MCPSU3=AFUP3*CPSPUP3*AFGEN(DENS,TSUP3)
266. STUP2=(1./MCPSS2)*(HUP*AHUP2*(TSUP2-TUP2))
267. RESH=ALOG(RH0/RHCH)/(TPI*AFGEN(CONDSS,TUP3))
268. UAHQZ=1./(RESH+RHQZ)
269. STUP3=(1./MCPSS3)*(UAHQZ*(TSUP3-TUP3))
270. STSUP2=(1./MCPSU2)*(-HUP*AHUP2*(TSUP2-TUP2)-...
271. 2.*HSINTR*CPSPUP2*(TSUP2-TSUPI2)/LUP2)
272. STSUP3=(1./MCPSU3)*(-HUP*AHUP3*(TSUP3-TUP3)-...
273. 2.*HSINTR*CPSPUP3*(TSUP3-TSUPI3)/LUP3)
274. TSUP02=2.*TSUP2-TSUPI2
275. TSUP03=2.*TSUP3-TSUPI3
276. TIMER FINISH=5000.,PRDEL=10.
277. FINISH TSTOP=0.0
278. PRINT TC(1-10),TS(1-10),HG(1-10),TUP1(1-10),...
279. TSUP1(1),TSUP01(1),TUP2,TSUP2,TUP3,TSUP3,...
280. MH2LBS,YH20,TUH,TSTMUH,...
281. HSINTR,DELT,TSUPI2,TSUP02,TSOUT(10)
282. LABEL ZIRC OXIDE MOD 7
283. OUTPUT TIME,TF(10)=(500.,4500.),TC(10)=(500.,4500.),...
284. TSOUT(10)=(500.,4500.)
285. PAGE XYPLOT
286. OUTPUT TIME,QC(10)=(0.0,3.2),QF(0.0,3.2)
287. PAGE XYPLOT
288. OUTPUT TIME,TC(9-10)=(500.,4500.)
289. PAGE XYPLOT
290. OUTPUT TIME,TC(7-8)=(500.,4500.)
291. PAGE XYPLOT
292. OUTPUT TIME,TC(5-6)=(500.,4500.)
293. PAGE XYPLOT
294. OUTPUT TIME,TC(3-4)=(500.,4500.)
295. PAGE XYPLOT
296. OUTPUT TIME,TC(1-2)=(500.,4500.)
297. PAGE XYPLOT
298. OUTPUT TIME,TUP1(1-2)
299. PAGE XYPLOT
300. OUTPUT TIME,TUP1(3-4)

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301. PAGE XYPLOT
302. OUTPUT TIME,TUP1(5-6)
303. PAGE XYPLOT
304. OUTPUT TIME,TUP1(7-8)
305. PAGE XYPLOT
306. OUTPUT TIME,TUP1(9-10)
307. PAGE XYPLOT
308. OUTPUT TIME,TSUPI1(1),TSUP01(1)
309. PAGE XYPLOT
310. OUTPUT TIME,TSUPI1(2),TSUP01(2)
311. PAGE XYPLOT
312. OUTPUT TIME,TSUPI1(3),TSUP01(3)
313. PAGE XYPLOT
314. OUTPUT TIME,TSUPI1(4),TSUP01(4)
315. PAGE XYPLOT
316. OUTPUT TIME,TSUPI1(5),TSUP01(5)
317. PAGE XYPLOT
318. OUTPUT TIME,TSUPI1(6),TSUP01(6)
319. PAGE XYPLOT
320. OUTPUT TIME,TSUPI1(7),TSUP01(7)
321. PAGE XYPLOT
322. OUTPUT TIME,TSUPI1(8),TSUP01(8)
323. PAGE XYPLOT
324. OUTPUT TIME,TSUPI1(9),TSUP01(9)
325. PAGE XYPLOT
326. OUTPUT TIME,TSUPI1(10),TSUP01(10)
327. PAGE XYPLOT
328. OUTPUT TIME,HGLBS
329. PAGE XYPLOT
330. OUTPUT TIME,H2LBS
331. PAGE XYPLOT
332. OUTPUT TIME,TSUPI2,TSUP02
333. PAGE XYPLOT
334. OUTPUT TIME,TSUPI3,TSUP03
335. PAGE XYPLOT
336. OUTPUT TIME,TSTIUH,TSTOUH
337. PAGE XYPLOT
338. OUTPUT TIME,TUH
339. PAGE XYPLOT
340. OUTPUT TIME,TSTMUH
341. PAGE XYPLOT
342. OUTPUT TIME,TUP2
343. PAGE XYPLOT
344. OUTPUT TIME,TUP3
345. PAGE XYPLOT
346. METHOD RKS
347. END
348. STOP
349. ENDJOB
350. // EXEC POSTPLOT,PLOTTER=VERS11

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60. FUNCTION WSGEN=(0.0,82.1),(100.,70.0),(200.,60.0),(300.,51.0),...
61. (400.,44.0),(500.,38.5),(600.,33.5),(700.,29.5),...
62. (800.,26.0),(1000.,20.0),(1200.,15.5),(1400.,12.5),...
63. (1600.,10.0),(1800.,8.5),(2000.,7.25),(2200.,6.0),...
64. (2650.,3.20),(3000.,1.00),(3350.,1.00),(3351.,0.0),...
65. (1.E6,0.0)
66. FUNCTION HTCAPF=(80.,5.73E-2),(620.,7.05E-2),(1160.,7.52E-2),...
67. (1700.,7.76E-2),(2240.,7.83E-2),(2780.,8.43E-2),(3320.,...
68. 9.47E-2),(3360.,1.1E-1),(4400.,1.35E-1),(4940.,1.55E-1),...
69. (5188.4,1.56E-1),(5192.,1.2E-1),(5480.,1.2E-1),(1.E6,1.2E-1)
70. FUNCTION CONDF=(80.,9.79E-4),...
71. (440.,9.79E-4),(1340.,5.62E-4),(2240.,4.33E-4),...
72. (3140.,3.53E-4),(4040.,4.17E-4),(4922.,5.3E-4),(4940.,...
73. 4.01E-4),(4958.,2.73E-4),(5840.,4.49E-4),(1.E6,4.49E-4)
74. FUNCTION HTCAPC=(80.,6.71E-2),(260.,7.21E-2),(692.,7.91E-2),...
75. (1502.,8.96E-2),(1507.4,.12),(1543.,0.141),(1579.4,0.147),...
76. (1615.4,0.172),(1651.4,0.195),(1637.4,0.184),(1723.4,.143),...
77. (1759.4,0.112),(1786.4,8.5E-2),(3318.2,8.5E-2),(1.E6,8.5E-2)
78. FUNCTION CONDC=(80.,2.01E-3),(800.,2.81E-3),(1520.,3.69E-3),...
79. (2240.,5.14E-3),(2960.,7.7E-3),(3320.,9.31E-3),(3322.,...
80. 5.78E-3),(3680.,5.78E-3),(1.E6,5.78E-3)
81. FUNCTION HTCAPS=(80.0,.761),(431.73,.761),(475.,.679),...
82. (500.,.645),(550.,.596),(600.,.565),(700.,.536),...
83. (750.,.529),(800.,.527),(900.,.529),(1000.,.532),...
84. (1100.,.539),(1200.,.546),(1400.,.559),(1600.,.564),...
85. (4000.,.564),(1.E6,.564)
86. FUNCTION DENS=(80.0,.754),(431.73,.754),(475.,.698),...
87. (500.,.671),(550.,.625),(600.,.588),(700.,.527),...
88. (750.,.502),(800.,.480),(900.,.442),(1000.,.409),...
89. (1100.,.381),(1200.,.357),(1400.,.318),(1600.,.287),...
90. (1800.,.261),(2000.,.240),(2500.,.199),...
91. (3000.,.171),(3500.,.149),(4000.,.132),(1.E6,.132)
92. FUNCTION HTCPS=(80.,9.79E-2),(530.,.110),(980.,.1210),...
93. (1430.,.127),(1880.,.131),(2330.,.133),(1.E6,.133)
94. FUNCTION CONDSS=(80.,2.25E-3),(2548.,6.26E-3),(2649.,3.21E-3),...
95. (1.E6,3.21E-3)
96. INIT
97. LAX=L FUEL/NSECT
98. LUP1=LSEP/NSECT
99. RGAP=A LOG(RCA/RFO)/(TPI*KGAP)
100. HCPL=RHOVL*CPL
101. QF0=3332.E6/(2.929E-1*3600.*NPINS*12.5)
102. VOLS=(83.96/NPINS)
103. NGLBS=0.0
104. ICTUH=TSAT
105. ITSTUH=TSAT
106. ICTUP2=TSAT
107. ICTUP3=TSAT
108. ITSUP2=TSAT
109. ITSUP3=TSAT
110. RHIOZ=1./(AHUP3*HU3)
111. RHUH=1./(AHUH*HUH)
112. NOSORT
113. DO 5 I=1,NSECT
114. ICTF(I)=433.24
115. ICTC(I)=431.93
116. ICTUP(I)=TSAT
117. ITSUP(I)=TSAT
118. TCK(I)=(ICTC(I)+460.)/1.8
119. ICTS(I)=TSAT

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120.      ICNG(I)=1.0
121.      TS(I)=TSAT
122.      HS=(1./NPINS)*AFGEN(HSGEN,TIME)
123.      WSRIN(I)=HS
124.      WSROUT(I)=HS
125.      WSRZR(I)=0.0
126.      ZZZ = (I-1)*LAX + LAX*0.5 + 1.0
127.      IF (ZZZ.GT.3.083) GO TO 2
128.      SH(I) = -3.5424 + 4.7856*ZZZ - 1.653*ZZZ*ZZZ + ...
129.             0.1911*ZZZ**3
130.      GO TO 5
131. 2      IF (ZZZ.GT.10.896) GO TO 3
132.      SH(I) = 1.075 + 3.574E-2*ZZZ - 3.427E-3*ZZZ*ZZZ
133.      GO TO 5
134. 3      SH(I) = 44.47 - 11.95*ZZZ + 1.106*ZZZ*ZZZ - 3.442E-2*ZZZ**3
135. 5      CONTINUE
136.      WRITE(6,6)
137. 6      FORMAT(1X,'$$$$$$$$$ THE AXIAL SHAPE FACTORS ARE:',/)
138.      WRITE(6,7) (SH(I),I=1,NSECT)
139. 7      FORHAT(1X,10(F8.6,4X)/)
140.      DYNAM
141.      TSCRM=TIME+725
142.      QF=0.1*QF0*((TSCRM+8.5)**(-0.2)-0.9612*(TSCRM+7.76E5)**(-0.2))
143. *      QF=0.1*QF0*(( 725.+8.5)**(-0.2)-0.9612*( 725.+7.76E5)**(-0.2))
144.      TF=INTGRL(ICTF,STF,10)
145.      TC=INTGRL(ICTC,STC,10)
146.      TS =INTGRL(ICTS,STS ,10)
147.      NG=INTGRL(ICNG,SI:G,10)
148.      TUP1=INTGRL(ICTUP1,STUP1,10)
149.      TSUP1=INTGRL(ITSUP1,STSUP1,10)
150.      YH20=AFGEN(H20H,TIME)
151. *      YH20=12.5+1.E-4
152.      YFLOAT=(YH20/LAX)+1.E-4
153.      ICOVER=YFLOAT
154.      HSIHTR=AFGEN(HSGEN,TIME)
155. *      HSIHTR=82.1
156.      HS=HSIHTR/HPINS
157.      FCOVER=ICOVER
158.      BETA=YFLOAT-ICOVER
159.      MCPSS2=HUP2*AFGEN(HTCPSS,TUP2)
160.      MCPSS3=HUP3*AFGEN(HTCPSS,TUP3)
161.      NOSORT
162.      DO 10 I=1,NSECT
163.      MCPF(I)=RHOVF*AFGEN(HTCAPF,TF(I))
164.      RF(I)=ALOG(1.4142)/(TPI*AFGEN(CONDF,TF(I)))
165.      RC1(I)=ALOG(RCC/RCA)/(TPI*AFGEN(CONDC,TC(I)))
166.      UAF(I)=1.0/(RF(I)+RGAP+RC1(I))
167.      MCPC(I)=RHOVC*AFGEN(HTCAPC,TC(I))
168.      UAC1(I)=(TPI/ALOG(RC0/RCC))*AFGEN(CONDC,TC(I))
169.      UAC2(I)=TPI/(ALOG(RC0/RCC))*(1.0/AFGEN(CONDC,TC(I)))+1.0/(HCS*RC0))
170. 10      STF(I)=(1.0/MCPF(I))*(-UAF(I)*(TF(I)-TC(I))+QF*SH(I))
171.      IF(ICOVER.EQ.0) GO TO 21
172.      DO 20 I=1,ICOVER
173.      STC(I)=(1.0/MCPC(I))*(UAF(I)*(TF(I)-TC(I))-UAC1(I)*(TC(I)-TSAT))
174.      STS (I)=0.0
175.      TSIN(I)=TSAT
176. 20      TSOUT(I)=TSAT
177.      IF(ICOVER.GE.NSECT) GO TO 30
178. 21      J=ICOVER+1
179.      UACT=UAC2(J)
180.

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181.      CPS(J)=AFGEN(HTCAPS,TS(J))
182.      MCPS(J)=VOLS*CPS(J)*AFGEN(DENS,TS(J))
183.      TCK(I)=(TC(I)+460.)/1.8
184.      IF(TCK(I).LE.1853.) KP(I)=COX2*EXP(-COX3/(R*TCK(I)))
185.      IF(TCK(I).GT.1853.) KP(I)=COX4*EXP(-COX5/(R*TCK(I)))
186.      SHG(I)=117.45*COX1*KP(I)/WG(I)
187.      HSRZR(I)=(36./91.)*SHG(I)*(1./4.5359E5)*LAX
188.      HSRIN(I)=HS
189.      HSROUT(I)=HSRIN(I)-HSRZR(I)
190.      IF(HSROUT(I).LE.0.0) SHG(I)=KH2*HSRIN(I)
191.      IF(WG(I).GE.HZRMAX) SHG(I)=0.0
192.      QC(I)=5.972E-3*SHG(I)
193.      STC(I)=(1.0/MCPC(I))*(UAF(I)*(TF(I)-TC(I))-UAC2(I)*(TC(I)-TS(I))+...
194.      QC(I))
195.      MCPT=BETA*MCPL+(1.0-BETA)*MCPS(J)
196.      TERM1=1.0/MCPT
197.      TERM2=UAC2(I)*(TC(I)-TS(I))
198.      TERM3=2.*(1.0-BETA)*HS*CPS(I)*(TS(I)-TSAT)/LAX
199.      STS(I)=TERM1*(TERM2-TERM3)
200.      TSOUT(I)=2.*TS(I)-TSAT
201.      J=ICOVER+2
202.      IF(J.GT.NSECT) GO TO 30
203.      DO 30 I=J,NSECT
204.          TSIN(I)=TSOUT(I-1)
205.          CPS(I)=AFGEN(HTCAPS,TS(I))
206.          MCPS(I)=VOLS*CPS(I)*AFGEN(DENS,TS(I))
207.          TCK(I)=(TC(I)+460.)/1.8
208.          IF(TCK(I).LE.1853.) KP(I)=COX2*EXP(-COX3/(R*TCK(I)))
209.          IF(TCK(I).GT.1853.) KP(I)=COX4*EXP(-COX5/(R*TCK(I)))
210.          KP10=KP(10)
211.          SHG(I)=117.45*COX1*KP(I)/WG(I)
212.          HSRZR(I)=(36./91.)*SHG(I)*(1./4.5359E5)*LAX
213.          HSRIN(I)=HSROUT(I-1)
214.          IF(HSRIN(I).LT.0.0) HSRIN(I)=0.0
215.          HSROUT(I)=HSRIN(I)-HSRZR(I)
216.          IF(HSROUT(I).LE.0.0) SHG(I)=KH2*HSRIN(I)
217.          IF(WG(I).GE.HZRMAX) SHG(I)=0.0
218.          QC(I)=5.972E-3*SHG(I)
219.          STC(I)=(1.0/MCPC(I))*(UAF(I)*(TF(I)-TC(I))-UAC2(I)*(TC(I)-TS(I))+...
220.          QC(I))
221.          TERM4=1.0/MCPS(I)
222.          TERM5=UAC2(I)*(TC(I)-TS(I))
223.          TERM6=2.*HS*CPS(I)*(TS(I)-TSIN(I))/LAX
224.          STS(I)=TERM4*(TERM5-TERM6)
225.      30 TSOUT(I)=2.*TS(I)-TSIN(I)
226.      HGLBS=0.0
227.      DO 35 I=1,NSECT
228.          HGLS=HPINS*(WG(I))*LAX*2.205E-6+HGLBS
229.          H2LBS=(4./91.)*HGLBS
230.      35 CONTINUE
231.      TREIN=TLEFT(KP10)
232.      TSTOP=TREIN-2000.
233.      TSTIUN=TSOUT(10)
234.      SORT
235.          TUN=INTERL(ICTUN,STUN)
236.          TSTIUN=INTERL(ITSTIUN,STSIUN)
237.          RIUN=(TUN/2.)/(AUN*AFGEN(CONDSS,TUN))
238.          UAUH=1./(RIUN+TSTIUN)
239.          MCPUH=HUB*AFGEN(HTCPSS,TUN)
240.          STUH=(1./MCPUH)*(UAUH*(-TUN+TSTIUN))

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241. CPSUH=AFGEN(HTCAPS,TSTHUI)
242. MCPSUH=AFUH*CPSPUH*AFGEN(DENS,TSTHUI)
243. STSHUI=(1./MCPSUH)*(-UAUH*(TSTHUI-TUH)-...
244. 2.*HSINTR*CPSPUH*(TSTHUI-TSTIUI)/LUI)
245. TSTOUI=2.*TSTHUI-TSTIUI
246. NOSORT
247. DO 40 I=1,HSECT
248. TSUPI(I)=TSUP0(I-1)
249. IF(I.EQ.1) TSUPI(I)=TSTOUI
250. CPSPU(I)=AFGEN(HTCAPS,TSUPI(I))
251. MCPSS(I)=HUP1*AFGEN(HTCPSS,TUP1(I))
252. MCPSU(I)=AFUP1*CPSPU(I)*AFGEN(DENS,TSUPI(I))
253. STUP1(I)=(1./MCPSS(I))*(HUP*AHUP1*(TSUPI(I)-TUP1(I)))
254. STSUP1(I)=(1./MCPSU(I))*(-HUP*AHUP1*(TSUPI(I)-TUP1(I)))...
255. -2.*HSINTR*CPSPU(I)*(TSUPI(I)-TSUPI1(I))/LUP1)
256. 40 TSUP0(I)=2.*TSUPI(I)-TSUPI1(I)
257. TSUPI2=TSUP0(10)
258. SORT
259. TUP2=INTGRL(ICTUP2,STUP2)
260. TUP3=INTGRL(ICTUP3,STUP3)
261. TSUP2=INTGRL(ITSUP2,STSUP2)
262. TSUP3=INTGRL(ITSUP3,STSUP3)
263. TSUPI3=TSUP02
264. CPSPU2=AFGEN(HTCAPS,TSUP2)
265. CPSPU3=AFGEN(HTCAPS,TSUP3)
266. MCPSU2=AFUP2*CPSPU2*AFGEN(DENS,TSUP2)
267. MCPSU3=AFUP3*CPSPU3*AFGEN(DENS,TSUP3)
268. STUP2=(1./MCPSS2)*(HUP*AHUP2*(TSUP2-TUP2))
269. RESN=ALOG(RNO/RNCH)/(TPI*AFGEN(COMDSS,TUP3))
270. UANOZ=1./(RESN*RHNOZ)
271. STUP3=(1./MCPSS3)*(UANOZ*(TSUP3-TUP3))
272. STSUP2=(1./MCPSU2)*(-HUP*AHUP2*(TSUP2-TUP2)-...
273. 2.*HSINTR*CPSPU2*(TSUP2-TSUPI2)/LUP2)
274. STSUP3=(1./MCPSU3)*(-HU3*AHUP3*(TSUP3-TUP3)-...
275. 2.*HSINTR*CPSPU3*(TSUP3-TSUPI3)/LUP3)
276. TSUP02=2.*TSUP2-TSUPI2
277. TSUP03=2.*TSUP3-TSUPI3
278. TIMER FINTIH=5000.,FRDEL=10.
279. FINISH TSTOP=0.0
280. PRINT TC(1-10),TS(1-10),H(1-10),TUP1(1-10),...
281. TSUP1(1),TSUP01(1),TUP2,TSUP2,TUP3,TSUP3,...
282. RH2LES,YH20,TUH,TSTHUI,...
283. HSINTR,DELT,TSUPI2,TSUP02,TSOUT(10)
284. LABEL ZIRC OXIDE MOD 7
285. OUTPUT TIME,TF(10)=(500.,4500.),TC(10)=(500.,4500.),...
286. TSOUT(10)=(500.,4500.)
287. PAGE XYPLT
288. OUTPUT TIME,QC(10)=(0.0,3.2),QF(0.0,3.2)
289. PAGE XYPLT
290. OUTPUT TIME,TC(9-10)=(500.,4500.)
291. PAGE XYPLT
292. OUTPUT TIME,TC(7-8)=(500.,4500.)
293. PAGE XYPLT
294. OUTPUT TIME,TC(5-6)=(500.,4500.)
295. PAGE XYPLT
296. OUTPUT TIME,TC(3-4)=(500.,4500.)
297. PAGE XYPLT
298. OUTPUT TIME,TC(1-2)=(500.,4500.)
299. PAGE XYPLT
300. OUTPUT TIME,TUP1(1-2)

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301. PAGE XYPLOT
302. OUTPUT TIME,TUP1(3-4)
303. PAGE XYPLOT
304. OUTPUT TIME,TUP1(5-6)
305. PAGE XYPLOT
306. OUTPUT TIME,TUP1(7-8)
307. PAGE XYPLOT
308. OUTPUT TIME,TUP1(9-10)
309. PAGE XYPLOT
310. OUTPUT TIME,TSUPI1(1),TSUP01(1)
311. PAGE XYPLOT
312. OUTPUT TIME,TSUPI1(2),TSUP01(2)
313. PAGE XYPLOT
314. OUTPUT TIME,TSUPI1(3),TSUP01(3)
315. PAGE XYPLOT
316. OUTPUT TIME,TSUPI1(4),TSUP01(4)
317. PAGE XYPLOT
318. OUTPUT TIME,TSUPI1(5),TSUP01(5)
319. PAGE XYPLOT
320. OUTPUT TIME,TSUPI1(6),TSUP01(6)
321. PAGE XYPLOT
322. OUTPUT TIME,TSUPI1(7),TSUP01(7)
323. PAGE XYPLOT
324. OUTPUT TIME,TSUPI1(8),TSUP01(8)
325. PAGE XYPLOT
326. OUTPUT TIME,TSUPI1(9),TSUP01(9)
327. PAGE XYPLOT
328. OUTPUT TIME,TSUPI1(10),TSUP01(10)
329. PAGE XYPLOT
330. OUTPUT TIME,H6LBS
331. PAGE XYPLOT
332. OUTPUT TIME,H6LBS
333. PAGE XYPLOT
334. OUTPUT TIME,TSUPI2,TSUP02
335. PAGE XYPLOT
336. OUTPUT TIME,TSUPI3,TSUP03
337. PAGE XYPLOT
338. OUTPUT TIME,TSTIUH,TSTOUH
339. PAGE XYPLOT
340. OUTPUT TIME,TUH
341. PAGE XYPLOT
342. OUTPUT TIME,TSTIUH
343. PAGE XYPLOT
344. OUTPUT TIME,TUP2
345. PAGE XYPLOT
346. OUTPUT TIME,TUP3
347. PAGE XYPLOT
348. METHOD RKS
349. END
350. STOP
351. ENDJOB
352. // EXEC POSTPLOT,PLOTTER=VERS11

```

APPENDIX E

RADIAL TEMPERATURE PROFILE CODE

As mentioned in section 6.1, some of the modeling assumptions made in developing the main thermal-hydraulics code used in this study were checked through a second code which was created specifically for that purpose. The primary focus of this second code was a radial temperature profile calculation across the fuel and cladding. In addition, the code also computed the growth of the α -Zr(O) and ZrO_2 layers formed by the interaction of the zircaloy with steam and UO_2 .

The code performed temperature calculations by discretizing the cladding into a user-specified number of mesh cells, then solving the standard finite difference form of the energy equation for the temperatures at each mesh cell interface. The initial version of the code did not perform a similar discretization over the fuel pin but instead calculated a single, lumped fuel temperature. The calculations performed with this version showed the fuel temperatures to be within a few degrees of the cladding temperatures (in addition to showing that the cladding temperature profile is nearly flat, as discussed in section 6.1), and so it was clear that the fuel temperature profile had to be nearly constant. Therefore, there seemed to be nothing to gain by doing a more refined temperature calculation in the fuel, and so no changes were made to the original formulation of the temperature calculation.

To the greatest extent possible, the secondary code modeled the physics of the transient in the same way as did the main code. All the same materials properties for UO_2 , zircaloy, and steam were used in the two codes, and both codes used the same decay heat levels. Because the secondary code did not model any axial variations, however, there were several aspects of the transient for which it could not properly account. There was no way, for example, for the code to model variations in the flow rate of the steam or the phenomenon of steam starvation. However, these modeling inadequacies did not affect the ability of the code to verify that the pin temperature profile is quite flat nor to determine the growth of the α -Zr(O) layers resulting from the UO_2 -Zry reaction, and so they did not detract from the usefulness of the code.

A listing of the code, the input data, and the JCL required to run the code on an IBM system follows.

```

//HEATUP JOB (,FXXXXX),REGION=250K,CLASS=W
//*MAIN LINES=7
//*MAIN ORG=RADS12
//STEP1 EXEC FTXCLG
//FTX.SYSIN DD *
    IMPLICIT REAL*8(A-H,O-Z)
    DIMENSION TC(20,2), TW(2), TF(2), R(20), RBAR(19), XZONE(4),
1          DENS(20), SPHEAT(20), TDEN(20), TSPH(20),
2          SPHW(13), TSPHW(13), TJT(100), TCJT(100)
10 FORMAT (12I6)
1000 FORMAT (6F12.5)
CC
CC  INITIALIZATION OF PARAMETERS
CC
    PI = 3.14159D0
    DT = 1.D0
    NPRINT = 10
    READ 10, N
    NM1 = N - 1
    XOUT = 0.D0
    XOX = 0.D0
    READ 1000, R(1), R(N)
    RINT = R(N)
    ROX = R(N)
    DELR = (R(N) - R(1))/DFLOAT(NM1)
CC
CC  DETERMINATION OF RADIAL MESH NODES
CC
    DO 25 I=2,NM1
    R(I) = R(I-1) + DELR
25 CONTINUE
    DO 15 I = 1,4
    XZONE(I) = 0.D0
15 CONTINUE
    PRINT 1200, (R(I), I=1,N)
1200 FORMAT (5X,'RADIAL MESH',5D13.5/(16X,5D13.5))
    PRINT 100
100 FORMAT (5X,'STEP NO.',5X,'TIME',8X,'RINT',10X,'STEAM TEMP.',10X,
1 'FUEL TEMP.',10X,'CLADDING TEMP.'/)
    DO 30 I=1,NM1
    RBAR(I) = 0.5D0*(R(I) + R(I+1))
30 CONTINUE
    Q = 6.3D+06
    T = 0.D0
    READ 10, IFLAG
    IF (IFLAG.EQ.0) GO TO 19
    II = 0
11 II = II + 1
CC
CC  CODING TO READ IN INITIAL TEMPERATURE HISTORIES FROM MAIN CODE.
CC  ONLY THE CLADDING TEMPERATURE HISTORY MUST BE STORED FOR USE IN
CC  CALCULATING THE BUILDUP OF THE OXIDE AND OXYGEN-STABILIZED ALPHA
CC  ZIRCALOY LAYERS FROM THE START OF CORE UNCOVERY TO THE TIME AT
CC  WHICH THE MAIN THERMAL-HYDRAULICS CODE ACCESSES THIS CODE.

```

```
CC CLADDING TEMPERATURES ARE CONVERTED FROM FARENHEIT TO KELVIN.
CC
      READ (7,1000,END=12) TJT(II), TCJT(II), TF(1), TW(1)
      TCJT(II) = (TCJT(II) - 32.00)*5.00/9.00 + 273.1500
      IF (II.LT.10000) GO TO 11
CC
CC COMPUTE REACTION ZONE THICKNESSES FROM CORE UNCOVERY UP TO
CC THE END OF THE MAIN CODE CALCULATION.
CC
      12 ILIM = II - 1
      DO 13 IK=1,ILIM
      DELT = TJT(IK + 1) - TJT(IK)
      IF (TCJT(IK).LT.1073.00) GO TO 805
      DENOM = 2.00*1.98700*TCJT(IK)
      IF (TCJT(IK).GE.1853.00) GO TO 804
CC
CC XOUT IS THE THICKNESS OF ZIRCALOY WHICH HAS BEEN CONVERTED BY STEAM
CC TO ALPHA-ZR AND ZIRC OXIDE. THE FIRST EXPRESSION WITHIN THE PARENTHESES
CC IS THE THICKNESS OF THE ZIRC OXIDE; THE FACTOR OF 1.56 ACCOUNTS FOR
CC THE FACT THAT THE VOLUME OF ZIRC OXIDE IS 1.56 TIMES THAT OF ZIRCALOY.
CC THESE EXPRESSIONS WERE TAKEN FROM URBANIC (J. NUCL MATLS., VOL 75,
CC P. 251 (1978)).
CC
      XOUT = XOUT + (3.6D-04*DEXP(-2.6996D+04/DENOM)/1.56D0 +
      1          3.9D-03*DEXP(-3.9402D+04/DENOM))*(DSQRT(T+DELT) -
      2          DSQRT(T))
      RINT = R(N) - XOUT
CC
CC ROX IS THE RADIUS AT WHICH THE OXIDE LAYER BEGINS.
CC
      XOX = XOX + 3.6D-04*DEXP(-2.6996D+04/DENOM)/1.56D0*
      1          (DSQRT(T+DELT) - DSQRT(T))
      ROX = R(N) - XOX
      GO TO 805
      804 XOUT = XOUT + (1.44D-03*DEXP(-3.182D+04/DENOM)/1.56D0 +
      1          3.9D-03*DEXP(-3.9402D+04/DENOM))*(DSQRT(T+DELT) -
      2          DSQRT(T))
      RINT = R(N) - XOUT
      XOX = XOX + 1.44D-03*DEXP(-3.182D+04/DENOM)/1.56D0*
      1          (DSQRT(T+DELT) - DSQRT(T))
      ROX = R(N) - XOX
      NPRINT = 100
      IF (TCJT(IK).GT.2123.00) Q = 6.35D+06
      805 CONTINUE
      I = I + 1
CC
CC CALCULATION OF THICKNESSES OF LAYERS FROM ZRY-UO(2) REACTION
CC
      FT = DSQRT(T + DELT) - DSQRT(T)
      DENOM = 2.00*1.98700*TCJT(IK)
      XZONE(1) = XZONE(1) + 2.35D-02*FT*DEXP(-5.77D+04/DENOM)
      XZONE(2) = XZONE(2) + 8.41D-03*FT*DEXP(-5.01D+04/DENOM)
      XZONE(3) = XZONE(3) + 1.27D-02*FT*DEXP(-4.52D+04/DENOM)
      XZONE(4) = XZONE(4) + 5.09D-03*FT*DEXP(-4.13D+04/DENOM)
```

```
      T = T + DELT
13  CONTINUE
      DO 14 IK=1,N
        TC(IK,1) = TCJT(IK)
14  CONTINUE
CC
CC  CONVERSION OF FUEL AND STEAM TEMPERATURES FROM FARENHEIT TO KELVIN.
CC
      TF(1) = (TF(1) - 32.D0)*5.D0/9.D0 + 273.15D0
      TW(1) = (TW(1) - 32.D0)*5.D0/9.D0 + 273.15D0
      GO TO 21
CC
CC  INITIALIZATION OF TEMPERATURES TO THE START OF CORE UNCOVERY
CC  IF TEMPERATURES ARE NOT BEING READ IN FROM THE MAIN CODE.
CC
19  DO 20 I = 1,N
      TC(I,1) = 564.82D0
20  CONTINUE
      TW(1) = 564.6
      TF(1) = 570.9
CC
CC  CLADDING PROPERTIES AND GEOMETRY
CC
21  PC = 3.854D-02
      RHOC = 6.4D+03
      READ 1000, (TSPH(II), II=1,15)
      READ 1000, (SPHEAT(II), II=1,15)
      AC = 2.93D-05
      HC = 10.D0*5.678263D0
CC
CC  STEAM PROPERTIES AND FLOW AREA
CC
      READ 1000, (TSPHW(II), II=1,13)
      READ 1000, (SPHW(II), II=1,13)
      DO 16 II=1,13
        TSPHW(II) = (TSPHW(II) - 32.D0)*5.D0/9.D0 + 273.15D0
        SPHW(II) = SPHW(II)*4.1868D+03
16  CONTINUE
      READ 1000, (TDEN(II), II=1,16)
      READ 1000, (DENS(II), II=1,16)
      DO 17 II=1,16
        TDEN(II) = (TDEN(II) - 32.D0)*5.D0/9.D0 + 273.15D0
        DENS(II) = DENS(II)*16.01846D0
17  CONTINUE
      AW = 1.59D-04
CC  FUEL PROPERTIES AND GEOMETRY
CC
      RHOF = 10.98D+03
CC
      PF = 3.34D-02
      AF = 8.53D-05
      QDOTF = 2.13D+06
      I = 0
      J = N
```



```

CC
CC  CALCULATION OF HEAT TRANSFER COEF. XH BETWEEN THE STEAM
CC  AND THE CLADDING. STEAM DENSITY AND SPECIFIC HEAT
CC  FOUND BY INTERPOLATION FROM INPUT TABLES.
CC
120 CALL INTIRP(DENS(1), TDEN(1), 16, TC(N,1), RHOW)
    CALL INTIRP(SPHW(1), TSPHW(1), 13, TC(N,1), CPW)
    XH = HC*PC/(RHOW*CPW*AW)
CC
CC  CALCULATION OF FUEL SPECIFIC HEAT AND CONDUCTIVITY USING
CC  EXPRESSIONS TAKEN FROM MATPRO-11. ALSO CLADDING SPECIFIC
CC  HEAT CONDC.
CC
    CPF = 296.7D0*535.285D0*535.285D0*DEXP(535.285D0/TF(1))/(TF(1)*
1      TF(1)*(DEXP(535.285D0/TF(1))-1.D0)**2) + 2.43D-02*TF(1) +
2      8.745D+07*1.577D+05*DEXP(-1.577D+05/(8.3143D0*TF(1)))/
3      (8.3143D0*TF(1)*TF(1))
    CONDF = 1.D+02*(40.4D0/(464.D0+TF(1)) + 1.216D-04*DEXP(1.867D-03*
1      TF(1)))
1    IF (TF(1).GT.1923.D0) CONDF = 1.D+02*(1.91D-02 + 1.216D-04*DEXP(
1      1.867D-03*TF(1)))
    CONDC = 36.D0
    IF (TC(1,1).LT.2248.D0) CONDC = 7.511D0 + TC(1,1)*(2.088D-02 +
1      TC(1,1)*(-1.45D-05 + TC(1,1)*
2      7.668D-09))
    IF (ROX.EQ.R(1)) CONDC = 0.835D0 + 1.81D-04*TC(1,1)
    HF = 1.D0/(R(1)/(4.D0*CONDF) + (R(2) - R(1))/(2.D0*CONDC))
    XKF = HF*PF/(RHOF*CPF*AF)
    QF = QDOTF/(RHOF*CPF)
CC
CC  CALCULATION OF ZRY-STEAM REACTION HEAT SOURCE TERM
CC
    IF (TC(J,1).LT.1073.D0) GO TO 105
    DENOM = 2.D0*1.987D0*TC(N,1)
    IF (TC(J,1).GE.1853.D0) GO TO 104
    DWDT = 8.58D0*DEXP(-1.005D+04/TC(J,1))/DSQRT(T+DT*0.5D0)
CC
CC  XOUT IS THE THICKNESS OF ZIRCALOY WHICH HAS BEEN CONVERTED BY STEAM
CC  TO ALPHA-ZR AND ZIRC OXIDE. THE FIRST EXPRESSION WITHIN THE PARENTHESES
CC  IS THE THICKNESS OF THE ZIRC OXIDE; THE FACTOR OF 1.56 ACCOUNTS FOR
CC  THE FACT THAT THE VOLUME OF ZIRC OXIDE IS 1.56 TIMES THAT OF ZIRCALOY.
CC  THESE EXPRESSIONS WERE TAKEN FROM URBANIC (J. NUCL MATLS., VOL 75,
CC  P. 251 (1978)).
CC
    XOUT = XOUT + (3.6D-04*DEXP(-2.6996D+04/DENOM)/1.56D0 +
1      3.9D-03*DEXP(-3.9402D+04/DENOM))*(DSQRT(T+DT) -
2      DSQRT(T))
    RINT = R(N) - XOUT
CC
CC  ROX IS THE RADIUS AT WHICH THE OXIDE LAYER BEGINS.
CC
    XOX = XOX + 3.6D-04*DEXP(-2.6996D+04/DENOM)/1.56D0*
1      (DSQRT(T+DT) - DSQRT(T))
    ROX = R(N) - XOX

```

```

      GO TO 106
104 XK2 = DSQRT(33.3D+02)*0.5D0
      XK3 = 4.55D+04*0.5D0/1.987D0
      DT = 0.1D0
      DWDT = XK2*DEXP(-XK3/TC(J,1))/DSQRT(T+DT*0.5D0)
      XOUT = XOUT + (1.44D-03*DEXP(-3.182D+04/DENOM)/1.56D0 +
1          3.9D-03*DEXP(-3.9402D+04/DENOM))*(DSQRT(T+DT) -
2          DSQRT(T))
      RINT = R(N) - XOUT
      XOX = XOX + 1.44D-03*DEXP(-3.182D+04/DENOM)/1.56D0*
1          (DSQRT(T+DT) - DSQRT(T))
      ROX = R(N) - XOX
      NPRINT = 100
      IF (TC(J,1).GT.2123.D0) Q = 6.35D+06
      GO TO 106
105 DWDT = 0.D0
106 CONTINUE
      I = I + 1

CC
CC  CALCULATION OF THICKNESSES OF LAYERS FROM ZRY-UO(2) REACTION
CC
      FT = DSQRT(T + DT) - DSQRT(T)
      DENOM = 2.D0*1.987D0*TC(1,1)
      XZONE(1) = XZONE(1) + 2.35D-02*FT*DEXP(-5.77D+04/DENOM)
      XZONE(2) = XZONE(2) + 8.41D-03*FT*DEXP(-5.01D+04/DENOM)
      XZONE(3) = XZONE(3) + 1.27D-02*FT*DEXP(-4.52D+04/DENOM)
      XZONE(4) = XZONE(4) + 5.09D-03*FT*DEXP(-4.13D+04/DENOM)

CC
CC  FUEL TEMPERATURE CALCULATION.
CC
      TF(2) = (TF(1) + XKF*DT*TC(1,1) + QF*DT)/(1.D0 + XKF*DT)
      TF(1) = TF(2)

CC
CC  INNER CLADDING NODE TEMPERATURE CALCULATION
CC
      CONST = 0.D0
      IF (J.EQ.1) CONST = 1.D0
      CALL INTIRP(SPHEAT(1), TSPH(1), 15, TC(1,1), CPC)
      C1 = 1.D0/(CPC*RHOC*(RBAR(1)*RBAR(1) - R(1)*R(1)))
      C2 = HF*PF*C1/PI
C  PRINT 3000,I,C1,C2,HC,DT,CONDC,DWDT,Q,CONST,R(1),R(2),RBAR(1),
C  1 TF(2),TC(1,1),TC(2,1)
      TC(1,2) = (TC(1,1) + C2*TF(2)*DT + (TC(2,1) - TC(1,1))*2.D0*
1          CONDC*RBAR(1)*DT*C1/(R(2)-R(1)) + CONST*Q*DWDT*C1*DT*
2          2.D0*R(N))/(1.D0 + C2*DT)
      IF (N.LT.3) GO TO 131

CC
CC  INTERIOR CLADDING NODE TEMPERATURE CALCULATIONS
CC
      DO 130 K=2,NM1
      CONST = 0.D0
      IF (K.EQ.J) CONST = 1.D0
      CALL INTIRP(SPHEAT(1), TSPH(1), 15, TC(K,1), CPC)
      CONDC = 36.D0

```

```

      IF (TC(K,1).LT.2248.D0) CONDC = 7.511D0 + TC(K,1)*(2.088D-02 +
1      TC(K,1)*(-1.45D-05 + TC(K,1)*
2      7.668D-09))
      IF (ROX.LT.RBAR(K-1)) CONDC = 0.835D0 + 1.81D-04*TC(K,1)
      C2 = RBAR(K)*RBAR(K) - RBAR(K-1)*RBAR(K-1)
      C1 = 1.D0/(C2*CPC*RHOC)
C      PRINT 3000,K,C1,C2,HC,DT,CONDC,DWDT,Q,CONST,R(K),RBAR(K-1),R(K-1),
C      1 TC(K,1),TC(K-1,2),TC(K+1,1),RBAR(K)
      TC(K,2) = (TC(K,1) + DT*C1*((TC(K+1,1) - TC(K,1))*2.D0*CONDC*
1      RBAR(K)/(R(K+1) - R(K)) + TC(K-1,2)*2.D0*CONDC*
2      RBAR(K-1)/(R(K) - R(K-1)) + Q*DWDT*CONST*2.D0*R(N)))/
3      (1.D0 + 2.D0*C1*DT*CONDC*RBAR(K-1)/(R(K) - R(K-1)))
130 CONTINUE
131 IF (N.LT.2) GO TO 132
CC
CC OUTER CLADDING NODE TEMPERATURE CALCULATION
CC
      CONST = 0.D0
      IF (J.EQ.N) CONST = 1.D0
      CALL INTIRP(SPHEAT(1), TSPH(1), 15, TC(N,1), CPC)
      CONDC1 = 36.D0
      IF (TC(N,1).LT.2248.D0) CONDC1 = 7.511D0 + TC(N,1)*(2.088D-02 +
1      TC(N,1)*(-1.45D-05 + TC(N,1)*
2      7.668D-09))
      CONDC2 = 0.835D0 + 1.81D-04*TC(N,1)
      CONDC = CONDC1
      IF (ROX.EQ.R(N)) GO TO 315
      IF (ROX.LE.R(NM1)) GO TO 313
      CONDC = (CONDC1*(ROX-R(NM1)) + CONDC2*(R(N) - ROX))/DELR
      GO TO 315
313 CONDC = CONDC2
315 CONTINUE
      C2 = R(N)*R(N) - RBAR(NM1)*RBAR(NM1)
      C1 = 1.D0/(C2*CPC*RHOC)
C      PRINT 3000,N,C1,C2,HC,DT,CONDC,DWDT,Q,CONST,R(N),RBAR(N-1),R(N-1),
C      1 TC(N,1),TW(1),TC(N-1,2)
3000 FORMAT(3X,15,6D13.5/(8X,6D13.5))
      TC(N,2) = (TC(N,1) + C1*DT*(2.D0*R(N)*HC*TW(1) + 2.D0*CONDC*
1      RBAR(NM1)*TC(NM1,2)/(R(N) - R(N-1)) + Q*DWDT*2.D0*R(N)*
2      CONST)/(1.D0 + 2.D0*HC*R(N)*DT*C1 + 2.D0*CONDC*C1*DT*
3      RBAR(N-1)/(R(N) - R(N-1)))
C      PRINT 3000, I, CONDC, TC(N,1), TC(N,2)
C      PRINT 3010, TC(N,2)
C3010 FORMAT(5X,'TC(N,2) =',1PD13.5)
132 IF (TC(J,1).LT.1073.D0) GO TO 50
CC
CC CALCULATION OF ZR-STEAM REACTION INTERFACE
CC
      IF (ROX.LT.RBAR(NM1)) GO TO 40
      J = N
      GO TO 50
40 DO 45 K = 1,NM1
      KK = N - K
      IF (KK.EQ.1) GO TO 48

```

```

        IF (ROX.LT.RBAR(KK-1)) GO TO 45
        J = KK
        GO TO 50
    45 CONTINUE
    48 J = 1
    50 CONTINUE

CC
CC  RESET CLADDING TEMPERATURES AT START OF TIME STEP
CC  (TC(I,1)) FOR NEW TIME STEP.
CC
        DO 60 II=1,N
            TC(II,1) = TC(II,2)
    60 CONTINUE

CC
CC  STEAM TEMPERATURE CALCULATION.
CC
        TW(2) = (TW(1) + XH*DT*TC(N,2))/(1.DO + XH*DT)
        TW(1) = TW(2)
        IF (MOD(I,NPRINT).NE.0) GO TO 150
        PRINT 250, I, T, RINT, TW(2), TF(2), (TC(II,2), II=1,N)
        PRINT 255, (XZONE(II), II=1,4),ROX,XOUT,XOX
    255 FORMAT (10X,'ZONE THICKNESSES',1P7D13.5//)
    150 IF (I.GT.20000) GO TO 200
        T = T + DT
        GO TO 120
    200 CONTINUE
    250 FORMAT(3X,I6,5X,1PD10.3,6(5X,D12.5)/(75X,3(5X,D12.5)))
        STOP
        END

CC
CC
CC
        SUBROUTINE INTIRP (Y,X,NTOT,X1,Y1)
        IMPLICIT REAL*8 (A-H,O-Z)
        DIMENSION Y(1),X(1)

CC
CC  INTERPOLATION ROUTINE FOR SINGLY SUBSCRIPTED VARIABLES.
CC  FOR VALUES OF X1 LT X(1), Y1=Y(1)
CC  FOR VALUES OF X1 GT X(NTOT), Y1=Y(NTOT)
CC  IT IS ASSUMED THAT X IS A MONOTONICALLY INCREASING FUNCTION OF Y
CC
        Y1 = Y(1)
        IF(NTOT.LE.1 ) GO TO 140
        IF (X1.GT.X(1)) GO TO 100
        GO TO 140
    100 CONTINUE
        LAST=NTOT
        JJ=2
    110 CONTINUE
        IF (X1.GT.X(JJ)) GO TO 120
        LAST=-1
        IF (X1.EQ.X(JJ)) LAST=0
    120 CONTINUE
        JJ=JJ+1

```

```

      IF (JJ.LE.LAST) GO TO 110
      M=JJ-2
      IF (LAST.LT.0) GO TO 130
      Y1=Y(M+1)
      GO TO 140
130  CONTINUE
      Y1=Y(M)+(Y(M+1)-Y(M))/(X(M+1)-X(M))*(X1-X(M))
140  CONTINUE
      RETURN
      END
//GO.SYSIN DD *
5
5.32130E-03 6.13400E-03
0
400.0      640.0      1090.0      1093.0      1113.0      1133.0
1153.0      1173.0      1193.0      1213.0      1233.0      1248.0
2098.0      2099.0      3000.0
302.0      331.0      375.0      502.0      590.0      615.0
719.0      816.0      770.0      619.0      469.0      356.0
356.0      356.0      356.0
556.20      600.0      650.0      700.0      800.0      900.0
1000.0      1100.0      1200.0      1300.0      2000.0      4000.0
1.D+06
1.27      0.979      0.806      0.711      0.621      0.594
0.574      0.575      0.577      0.581      0.581      0.581
0.581
556.2      600.0      650.0      700.0      800.0      900.0
1000.0      1200.0      1500.0      1750.0      2000.0      2500.0
3000.0      3500.0      4000.0      100000.0
2.5      2.21      2.0      1.84      1.62      1.46
1.33      1.15      0.956      0.848      0.762      0.633
0.542      0.473      0.42      0.0
/*
//GO.FT07F001 DD DUMMY

```

APPENDIX F

Structural Failure Bibliography

REF.NO.: - 1

GERDEEN, J.C.

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APPENDIX G

```

C      *** XTRCAL PROGRAM ***
C      *****
C
C      INTERACTIVE PROGRAM TO CALCULATE THE CREEP RUPTURE TIME.
C
C      * NOMENCLATURE *
C      ASL      = LARSON MILLER PARAMETER TABLE LOW ALLOY STEEL
C      ARSU     = REF.ULT. STRESS LOW ALLOY STEEL (PSI)
C      ASU      = ULTIMATE STRESS RATIO LOW ALLOY STEEL
C      ATMAX    = MAX. TABLE TEMP. LOW ALLOY STEEL (F)
C      ATMIN    = MIN.TABLE TEMP. LOW ALLOY STEEL (F)
C      BSU      = ULTIMATE STRESS FOR BOLT STEEL (PSI)
C      BTMAX    = MAX. TABLE TEMP. FOR BOLT STEEL (F)
C      BTMIN    = MIN.TABLE TEMP. FOR BOLT STEEL (F)
C      CH       = COEF. IN 304SS CREEP RUPTURE MODEL
C      CSU      = ULTIMATE STRESS FOR CARBON STEEL (PSI)
C      CTMAX    = MAX. TABLE TEMP. FOR CARBON STEEL (F)
C      CTMIN    = MIN.TABLE TEMP. FOR CARBON STEEL (F)
C      DAT      = TEMP. INCREMENT LOW ALLOY STEEL (F)
C      DBT      = TEMP. INCREMENT FOR BOLT STEEL (F)
C      DCT      = TEMP. INCREMENT FOR CARBON STEEL (F)
C      DST      = TEMP. INCREMENT STAINLESS STEEL (F)
C      MAT      = MATERIAL TYPE INDICATOR
C      PLM      = LARSON-MILLER PARAMETER
C      S        = TEST STRESS (PSI)
C      SRSU     = REFERENCE ULTIMATE STRESS 304SS (PSI)
C      SSU      = ULTIMATE STRESS RATIO STAINLESS STEEL
C      STMAX    = MAX. TABLE TEMP. STAINLESS STEEL (F)
C      STMIN    = MIN.TABLE TEMP. STAINLESS STEEL (F)
C      SU       = CALCULATED ULTIMATE STRESS (PSI)
C      TEMA     = ABSOLUTED TEMP. (R)
C      TEMF     = TEST TEMPERATURE (F)
C      TR       = TIME TO RUPTURE (HR)
C
C      DIMENSION CSU(39),ASU(13),BSU(13),SSU(23),ASL(17)
C      CHARACTER*1 RY,TY
C      DATA TY/'Y'/
C      * DATA FOR CARON STEEL TABLE LOOK UP *
C      DATA CTMIN,CTMAX,DCT/100.,2000.,50./
C      DATA CSU /
C      X 74000., 73500., 73000., 72500., 72000.
C      X,71500., 71000., 70000., 69000., 68000.
C      X,66400., 64200., 62100., 60000., 55100.
C      X,50200., 44600., 39600., 34500., 29300.
C      X,25500., 21800., 18000., 15500., 13200.
C      X,12000., 10200., 9800., 9400., 9000.
C      X, 8600., 8200., 7800., 7400., 7000.
C      X, 6600., 6200., 5800., 5400. /
C      * DATA FOR LOW ALLOY STEEL TABLE LOOK UP *
C      DATA ATMIN,ATMAX,DAT/600.,1800.,100./
C      DATA ARSU/ 84200. /
C      DATA ASU /
C      X 0.950,0.905,0.843,0.755,0.655,0.550,0.430
C      X,0.293,0.174,0.130,0.100,0.090,0.085 /

```

XTR00010
 XTR00020
 XTR00030
 XTR00040
 XTR00050
 XTR00060
 XTR00070
 XTR00080
 XTR00090
 XTR00100
 XTR00110
 XTR00120
 XTR00130
 XTR00140
 XTR00150
 XTR00160
 XTR00170
 XTR00180
 XTR00190
 XTR00200
 XTR00210
 XTR00220
 XTR00230
 XTR00240
 XTR00250
 XTR00260
 XTR00270
 XTR00280
 XTR00290
 XTR00300
 XTR00310
 XTR00320
 XTR00330
 XTR00340
 XTR00350
 XTR00360
 XTR00370
 XTR00380
 XTR00390
 XTR00400
 XTR00410
 XTR00420
 XTR00430
 XTR00440
 XTR00450
 XTR00460
 XTR00470
 XTR00480
 XTR00490
 XTR00500
 XTR00510
 XTR00520
 XTR00530
 XTR00540

```
DATA ASLMIN,ASLMAX,DASL/ 7.25,11.25,0.25 /
DATA ASL /
X 58.0,50.3,46.0,43.0,40.7,39.1
X,38.0,37.1,36.3,35.5,34.7,34.0
X,33.2,32.2,30.6,27.9,22.0 /
C * DATA FOR BOLT STEEL TABLE LOOK UP *
DATA BTMIN,BTMAX,DBT/400.,1600.,100./
DATA BSU /
X 122500.,119000.,115200.,109400.,102200.
X, 92000., 79600., 62000., 40800., 24200.
X, 13000., 8800., 6500. /
C * DATA FOR STAINLESS STEEL TABLE LOOK UP *
DATA STMIN,STMAX,DST/0.0,2200.,100./
DATA SSU /
X 1.00, .97, .86, .80, .78, .77
X, .77, .77, .76, .74, .70, .63
X, .55, .46, .35, .25, .22, .15
X, .094, .062, .037, .025, .0125 /
DATA SRSU,CH / 75000., 25.458 /
C
C * INTERACTIVE INPUT *
10 WRITE(6,11)
11 FORMAT(' MATERIAL - MAT=' /
X ' 1 = CARBON STEEL SA106' /
X ' 2 = LOW ALLOY STEEL SA533' /
X ' 3 = BOLTS AISI4140' /
X ' 4 = 304SS' )
READ(5,*) MAT
IF(MAT.LT.1.OR.MAT.GT.4) GO TO 900
20 WRITE(6,21)
21 FORMAT(' TEST TEMPERATURE (F) - TEMF=' )
READ(5,*) TEMF
WRITE(6,22)
22 FORMAT(' TEST STRESS (PSI) - S=' )
READ(5,*) S
GO TO(100,200,300,400) MAT
C
C * CARBON STEEL *
100 CONTINUE
IF(TEMF.GT.CTMIN) GO TO 105
SU=CSU(1)
TR=0.0
IF(S.LT.SU) TR=1.E10
GO TO 180
105 IF(TEMF.GT.CTMAX) GO TO 910
XXX=(TEMF-CTMIN)/DCT
IX=INT(XXX)
XXX=XXX-REAL(IX)
IX=IX+1
IXP=IX+1
SU=CSU(IX)+XXX*(CSU(IXP)-CSU(IX))
TR=0.0
IF(S.GE.SU) GO TO 180
TR=1.E10
```

XTR00550
XTR00560
XTR00570
XTR00580
XTR00590
XTR00600
XTR00610
XTR00620
XTR00630
XTR00640
XTR00650
XTR00660
XTR00670
XTR00680
XTR00690
XTR00700
XTR00710
XTR00720
XTR00730
XTR00740
XTR00750
XTR00760
XTR00770
XTR00780
XTR00790
XTR00800
XTR00810
XTR00820
XTR00830
XTR00840
XTR00850
XTR00860
XTR00870
XTR00880
XTR00890
XTR00900
XTR00910
XTR00920
XTR00930
XTR00940
XTR00950
XTR00960
XTR00970
XTR00980
XTR00990
XTR01000
XTR01010
XTR01020
XTR01030
XTR01040
XTR01050
XTR01060
XTR01070
XTR01080

IF(TEMP.LT.700.) GO TO 180	XTR01090
TEMA=TEMP+459.4	XTR01100
TR=0.25*EXP(1.498*ALOG(SU/S)/(ALOG(TEMA)-7.05))	XTR01110
180 WRITE(6,181) TR	XTR01120
181 FORMAT(' TIME TO RUPTURE =',E11.4, '(HR)')	XTR01130
190 CONTINUE	XTR01140
WRITE(6,191)	XTR01150
191 FORMAT(' TYPE Y TO CONTINUE N TO STOP')	XTR01160
READ(5,192) RY	XTR01170
192 FORMAT(A1)	XTR01180
IF(RY.EQ.TY) GO TO 20	XTR01190
STOP	XTR01200
C	XTR01210
C * LOW ALLOY STEEL *	XTR01220
200 CONTINUE	XTR01230
IF(TEMP.GT.ATMIN) GO TO 205	XTR01240
SU=ASU(1)*ARSU	XTR01250
TR=0.0	XTR01260
IF(S.LT.SU) TR=1.E10	XTR01270
GO TO 180	XTR01280
205 IF(TEMP.GT.ATMAX) GO TO 910	XTR01290
XXX=(TEMP-ATMIN)/DAT	XTR01300
IX=INT(XXX)	XTR01310
XXX=XXX-REAL(IX)	XTR01320
IX=IX+1	XTR01330
IXP=IX+1	XTR01340
SU=ASU(IX)+XXX*(ASU(IXP)-ASU(IX))	XTR01350
SU=SU*ARSU	XTR01360
TR=0.0	XTR01370
IF(S.GE.SU) GO TO 180	XTR01380
TR=1.E10	XTR01390
IF(TEMP.LT.800.) GO TO 180	XTR01400
TEMA=TEMP+459.4	XTR01410
SL=ALOG(S)	XTR01420
IF(SL.GT.ASLMIN) GO TO 215	XTR01430
PLM=ASL(1)-'SLMIN-SL)*(ASL(2)-ASL(1))/DASL	XTR01440
GO TO 225	XTR01450
215 IF(SL.LE.ASLMAX) GO TO 220	XTR01460
TR=0.0	XTR01470
GO TO 180	XTR01480
220 XXX=(SL-ASLMIN)/DASL	XTR01490
IX=INT(XXX)	XTR01500
XXX=XXX-REAL(IX)	XTR01510
IX=IX+1	XTR01520
IXP=IX+1	XTR01530
PLM=ASL(IX)+XXX*(ASL(IXP)-ASL(IX))	XTR01540
225 TR=10.**((1000.*PLM/TEMA-20.)	XTR01550
GO TO 180	XTR01560
C	XTR01570
C * BOLTS *	XTR01580
300 CONTINUE	XTR01590
IF(TEMP.GT.BTMIN) GO TO 305	XTR01600
SU=BSU(1)	XTR01610
TR=0.0	XTR01620

	IF(S.LT.SU) TR=1.E10	XTR01630
	GO TO 180	XTR01640
305	IF(TEMP.GT.BTMAX) GO TO 910	XTR01650
	XXX=(TEMP-BTMIN)/DBT	XTR01660
	IX=INT(XXX)	XTR01670
	XXX=XXX-REAL(IX)	XTR01680
	IX=IX+1	XTR01690
	IXP=IX+1	XTR01700
	SU=BSU(IX)+XXX*(BSU(IXP)-BSU(IX))	XTR01710
	TR=0.0	XTR01720
	IF(S.GE.SU) GO TO 180	XTR01730
	TR=1.E10	XTR01740
	IF(TEMP.LT.700.) GO TO 180	XTR01750
	TEMA=TEMP+459.4	XTR01760
	SL=ALOG(S)	XTR01770
	PLM=-3.1562+11.01866*SL-0.72674*SL**2	XTR01780
	TR=10.**((1000.*PLM/TEMA-20.))	XTR01790
	GO TO 180	XTR01800
C		XTR01810
C	* 304SS *	XTR01820
400	CONTINUE	XTR01830
	IF(TEMP.GT.STMIN) GO TO 405	XTR01840
	SU=SSU(1)*SRSU	XTR01850
	TR=0.0	XTR01860
	IF(S.LT.SU) TR=1.E10	XTR01870
	GO TO 180	XTR01880
405	IF(TEMP.GT.STMAX) GO TO 910	XTR01890
	XXX=(TEMP-STMIN)/DST	XTR01900
	IX=INT(XXX)	XTR01910
	XXX=XXX-REAL(IX)	XTR01920
	IX=IX+1	XTR01930
	IXP=IX+1	XTR01940
	SU=SSU(IX)+XXX*(SSU(IXP)-SSU(IX))	XTR01950
	SU=SU*SRSU	XTR01960
	TR=0.0	XTR01970
	IF(S.GE.SU) GO TO 180	XTR01980
	TR=1.E10	XTR01990
	IF(TEMP.LT.600.) GO TO 180	XTR02000
	TEMK=(TEMP+459.4)/1.8	XTR02010
	SMPA=S*.006895	XTR02020
	TR=10.**((CH-0.01258*TEMK-1.232E-5*TEMK*SMPA	XTR02030
X	-4.77E-3*TEMK*ALOG10(SMPA))	XTR02040
	GO TO 180	XTR02050
C		XTR02060
C	* ERROR MESSAGES *	XTR02070
900	WRITE(6,901) MAT	XTR02080
901	FORMAT(' MATERIAL MAT=',I2,' NOT DEFINED TRY AGAIN')	XTR02090
	GO TO 10	XTR02100
910	WRITE(6,911) TEMP	XTR02110
911	FORMAT(' TEMPERATURE TEMP=',F7.0,' BEYOND RANGE TRY AGAIN')	XTR02120
	GO TO 20	XTR02130
	END	XTR02140