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RBMK Thermohydraulic Safety Assessments Using RELAP5/MOD3 Codes

G. V. Tsiklauri
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June 1995

Prepared for the U.S. Department of Energy
under Contract DE-AC06-76RLO 1830

Pacific Northwest Laboratory
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Summary

In 1994 and 1995, the Pacific Northwest Laboratory (PNL) performed three thermohydraulic safety analyses of the Soviet-designed, graphite-moderated reactors (RBMKs) using the RELAP5/MOD3 computer code. The analyses were completed at the request of the U.S. Department of Energy (DOE) for its International Nuclear Safety Program (INSP), which is intended to achieve improvements at Soviet-designed nuclear power plants in the areas of safety culture, power plant operation and physical condition, and safety infrastructures.

This report presents three papers that record the results of the following analyses performed using the code:

1) a loss-of-coolant accident at the core pressure tube inlet, the blockage of a pressure tube, and the pressure response of the core cavity to in-core pressure tube ruptures

2) a partial rupture in a group distribution header that results in stagnated (low) flow to up to 40 pressure tubes

3) thermally induced, two-phase instabilities in nonuniformly heated boiling channels in RBMK-1000 reactors.

Scientists and engineers can use this information to further study and support other safety-related work on Soviet-designed RBMK reactors. The results of future analyses will be published in subsequent reports.

The findings of the three thermohydraulic safety assessment analyses using the RELAP5/MOD3 computer code are summarized below:

1) The RBMK models of the RELAP5/MOD3 code used to validate various thermohydraulic transients in RBMK-reactor postulated accidents were able to successfully predict major phenomena during accidents in the RBMK systems.

2) For the three accident scenarios (inlet tube ruptures, tube blockages, and overpressures in the reactor core cavity), the calculations were compared and verified with existing transient data and experimental calculations.

3) For inlet pressure tubes ruptures, the fuel and cladding heatup did not occur. Steam drum inventory and primary coolant flow control could be a concern for long-term core cooling ($\tau \sim 1000$ s).

4) For the tube blockage scenario, which simulated the Leningrad Nuclear Power Plant (NPP) 1992 accident, the minimum time to pressure tube rupture was calculated to be 42 seconds after blockage. This estimate compared well to the real accident time of 40 to 45 seconds.

5) The calculated pressure in the core cavity also compared well to the Leningrad NPP accident measurements. However, the local pressure in the graphite tube annular gap and graphite stack could exceed 20 bar. This pressure could potentially cause the graphite stack to move and rupture. The possibility of a single fuel channel rupture in the core cavity will require further analysis.

6) Thermally induced, two-phase instabilities in nonuniformly heated channels in RBMK-1000 reactor were analyzed for low flow in a distribution group header (DGH) supplying 44 fuel pressure tubes. The model DGH for RBMK was evaluated against experimental data.

7) Modeling sensitivity studies indicated that instability analysis results are sensitive to the nodalization scheme and time step used.

8) The calculations provided the density wave-type oscillation for the high power channels with period 3.1-2.6 s. The amplitude of the flow oscillation varied from 100% to -150% of the tube average flow, which means a reverse flow occurs in high-powered tubes. A reverse flow did not occur in the lower-power tubes. An instability of the flow is more severe in the subcooled region at the inlet to the fuel channels, although the flow oscillations are dissipated in the upper fuel region and outlet connectors.

9) The flow instability threshold for an RBMK reactor was established and compared to Japanese data. The threshold appeared to be in good agreement with the Japanese data.

Additional details are provided in the attachments. Specifically, Attachment A is titled *RELAP5/MOD3 Code Assessment for Pressure Tube, Graphite-moderated Boiling Water Reactors*, Attachment B is called the *RBMK Pressure Tube Rupture Assessment*, and Attachment C is titled *Thermal-hydraulic Instabilities in Pressure Tube Graphite-moderated Boiling Water Reactors*.

Attachment A

"RELAP5/MOD3 Code Assessment for Pressure Tube, Graphite-moderated Boiling Water Reactors"

RELAP5/MOD3 CODE ASSESSMENT FOR PRESSURE TUBE GRAPHITE-MODERATED BOILING WATER REACTORS

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ABSTRACT

The capability of the RELAP5/MOD3 code to validate various transients encountered in RBMK reactor postulated accidents has been assessed. The assessment results include a loss of coolant accident at the inlet of the core pressure tube, the blockage of a pressure tube, and the pressure response of the core cavity to in core pressure tube ruptures. These assessments show that the RELAP5/MOD3 code can predict major phenomena during postulated accidents in the RBMK reactors.

I. INTRODUCTION

The RELAP5 computer code is a one-dimensional non-equilibrium (with respect to the interface momentum and energy exchange) two-phase thermal-hydraulic systems code, developed at the Idaho National Engineering Laboratory, and has been successfully applied to PWR and BWR types of reactors. The U.S. Nuclear Regulatory Commission has accepted the use of RELAP5 for licensing audit calculations, evaluation of operator guidelines and Emergency Operational Procedure (EOP) for PWR. However, RELAP5/MOD3 has limited use for the pressure tube graphite-moderated boiling water reactor such as the FSU RBMK reactors. Considering the uniqueness of the thermal-hydraulic systems of RBMK type reactors, assessment studies are required to adapt the RELAP5/MOD3 code and assure its applicability to RBMK reactors.

The purpose of this paper is to verify that RELAP5/MOD3 can be adapted to accident analysis of RBMK reactors. The Leningrad accident [2] and cavity pressure [3] were chosen as benchmark cases. In addition, comparisons were made with some experimental data. The results obtained also provide insights into other potential concerns.

The general characteristics of the RBMK type reactor are as follows:

- Thermal core power 3200 MW.
- 1661 fuel tubes, 7 m active core, average linear heat flux 153 W/cm.
- Operating pressure 7 MPa.
- 37,600 tonne/hr total loop flow, an average of 6.288 kg/s per tube.

The reactor has four steam drum separators, two hydraulic loops common at the steam header and 8 main circulation pumps (PCP), (6 operating, and 2 reserved).

Our study considers three LOCA events:

1. Inlet pressure tube rupture (one or several).
2. Blockage of coolant at the inlet of a pressure tube (similar to the Leningrad NPP accident in 1992).
3. Pressure response of the core cavity for pressure tube ruptures in the core.

In case (3) two-phase flow at the stagnation pressure ~7 MPa is discharged from the ruptured tubes into the graphite stack with the subsequent overpressure in the core cavity. The Chernobyl accident in 1986 revealed that due to overpressure the steel plate at the top of the reactor was lifted and the core was exposed. In the accident at Leningrad NPP in 1992, an increase in cavity pressure was observed as well.

In case (1) the rupture of a single pressure tube was investigated. This was the design basis accident for the first generation of RBMK. For the Smolensk and Ignalina NPPs, the pressure in the graphite cavity reaches $P_{cav} \sim 1.4$ bar for this accident [3]. That corresponds to a considerable margin of safety (for the RBMK design the core cavity limiting pressure is determined by the top plate uplifting pressure, $P_{top} = 3.1$ bar)

However, there is a possibility that several pressure tubes could rupture simultaneously. The probability of independent rupture of tubes is too low to be of practical consequence. The propagation of multichannel ruptures is potentially a greater risk. Previous calculations of the RBMK core cavity pressure were presented at the RDIPE-PNL Workshop on N-Reactor Lessons [3]. With the conservative assumptions of simultaneous multichannel ruptures, these calculations showed that the RBMK-1000 and RBMK-1500 unmodified core cavity pressure protection system assures protection from only 3 pressure tube ruptures. Four pressure tube ruptures would result in an overpressure above the margin value $P_{top} = 3.1$ bar and possible loss of the core integrity. For the modified upgrade piping system of RBMK-1000 (Smolensk NPP), the core integrity is assured for 9 pressure tube ruptures and for RBMK-1500 (Ignalina NPP), for 11 pressure tube ruptures.

It is important to note that the results obtained in [3] include the assumption that the nearby stack does not contain the graphite rings, or other graphite fragments, around the broken pressure tube. This means that two-phase flow through the graphite stack and rings is not considered. This assumption is conservative for the cavity pressure because it maximizes the overpressure at the top plate. However it is not conservative with respect to the local pressure near the rupture which would determine the possibility of propagation of multichannel ruptures.

II. MODEL DESCRIPTION

The RELAP5/MOD3 thermal hydraulic model is based on a one-dimensional, two-velocity, two-temperature model of two-phase flow. For engineering applications, two-phase flows are represented by mathematical models consisting of quasi-linear sets of partial equations. The equations are space-averaged and time-averaged (statistically averaged) and may be written in the following form:

$$A_t \frac{\delta X}{\delta t} + A_z \frac{\delta X}{\delta z} = B \qquad (1)$$

Where t and z are the independent time and space variables; X is the vector of the n dependent variables used to describe the flow; A_t and A_z being square (nxn) matrixes; and B is a column-vector of n elements. A_t, A_z, and B are functions of X, t, z. In the RELAP5/MOD3 code six dependent variables are used:

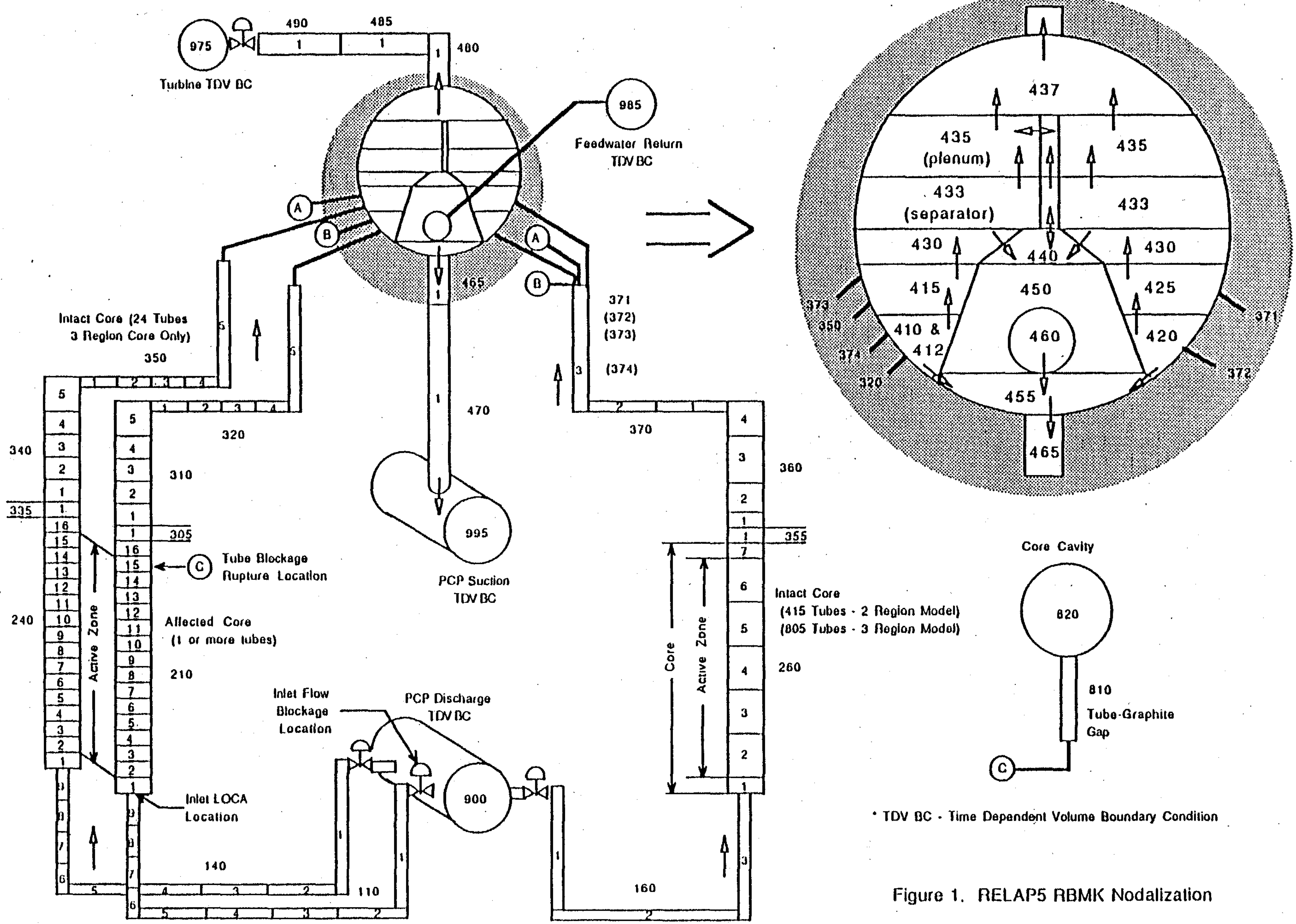
$$X = f\,[\alpha,\, p,\, W_f,\, W_g,\, h_f,\, h_g] \qquad (2)$$

Mass, momentum and energy balances are written for each phase. Empirical constitutive correlations are required to close the set of equations. In transient processes, models for non-equilibrium interphase mass-transfer and slip between liquid and gas velocities are essential. In RELAP5/MOD3 improved non-equilibrium transient experimental correlations are used, as compared to previous code versions.

Model Nodalization

Two base RELAP5 models were developed that represent a 1/4 core and 1/2 core of an RBMK reactor. With these two models, minor modifications are made specific to the transient being simulated. For both models, the nodalization is setup to perform a detailed calculation of an affected core region (for a single or multiple tube rupture or blockage). The balance of the core is lumped into a single tube to allow the RELAP5 model to predict needed fluid conditions in the steam drum and inlet distribution headers. It was felt that a simple single tube model, with boundary conditions for these regions, would not allow sufficient degrees of freedom in the calculation to provide accurate results. The nodalization schemes for both models are shown in Figure 1.

The 1/4 core model assumes a 1/4 core symmetry for the RBMK, and contains two parallel fuel regions for the reactor core. A 1/4 core model is the minimum size needed to include a steam drum model, and is readily adaptable for assuming conservative core power distributions (i.e. assuming high/low power regions). The two fuel region model allows for one or more 'affected' tubes (fuel channels) to be modeled separate from the intact core for events such as tube rupture or blockage. The 1/2 core model contains three parallel fuel regions for the core. The 1/2 core representation allows a more accurate calculation of the core average conditions as the RBMK core is split in-two hydraulically. The three fuel channel model can model the same transients as the two channel model, but also includes the ability to model the core cavity pressure response for a tube rupture, including heat transfer between the discharged coolant and the core graphite and internal structures. The third core region is used to simulate the adjacent tubes that are in the 'sphere



* TDV BC - Time Dependent Volume Boundary Condition

Figure 1. RELAP5 RBMK Nodalization

of influence' of the affected tube. Thus, the 1/2 core model is a more robust model, while the 1/4 core model, being smaller, is faster executing (less computational time) and can be easily setup to be conservative.

For both models, the affected tube is modeled hydraulically using 9 inlet connector volumes, 16 axial fuel volumes (14 active fuel regions), 6 upper tube volumes, and 5 outlet connector volumes. This nodalization allows for detailed pressure and temperature monitoring, and ease of defining the tube rupture location for different events without significant changes to the base model. The intact core is modeled using 5 inlet connector volumes, 7 axial fuel volumes (5 active fuel regions), 5 upper tube volumes, and 5 outlet connector volumes (these outlet connectors are set up to allow for future model expansions as needed). In the three channel model, a third channel is modeled hydraulically with the same detail as the affected core. Overall, the two channel model (1/4 core) represents 416 fuel channels, typically a single 'affected' channel and 415 lumped channels. The three channel model (1/2 core) represents 830 fuel channels, typically a single 'affected' channel and the surrounding 24 tubes (a 5x5 array depicting the 'sphere of influence') and the remaining 805 channels.

The steam drum separator is modeled using 14 volumes. This is shown in Figure 1. This modelling detail allows for a more accurate inventory calculation, and in particular, a more accurate prediction of the fluid conditions for reverse flow into the affected tube(s). There are additional volumes for the inlet sparger volume (for feedwater return), an outlet downcomer for coolant return to the primary coolant pumps (PCPs), and steam piping volumes leading to the turbines. The turbines, PCPs, and feedwater return pumps are not modeled explicitly. They are approximated using time dependent volumes to supply the necessary boundary conditions, with the fluid conditions taken from plant operating data. The steam drum is sized to represent a single drum for the 1/4 core model, and two steam drums for the 1/2 core model.

The heat structures modeled include the fuel pins and carrier rod, pressure tube and surrounding graphite, and the inlet and outlet connector piping walls. No heat structures are modeled at this time for the steam separator. The affected tube for both models contains two fuel pin heat structures that represent an equivalent of 6 and 12 fuel pins lumped together to represent the 18 fuel pins per rod bundle. This allows radial power peaking to be modeled for the 6 inner and 12 outer fuel rings of the fuel bundle. The unaffected tubes are modeled with a single heat structure representing an equivalent of 18 fuel pins lumped together.

For the affected core, the RELAP5/MOD3 radiative heat transfer model is used. Radiative heat transfer between the inner fuel ring, outer fuel ring, carrier rod, and tube wall is modeled. Appropriate view factors were calculated for each heat structure component. Preliminary calculations were made to investigate the surface emissivity for the fuel cladding and tube wall. Emissivity values of 0.5, 0.6 and 0.7 were evaluated. This range is considered to be a typical range for Zr (the cladding and tube wall material). An average value of 0.6 was chosen for the calculations presented here, as the preliminary results did not show a strong dependence over this range of emissivity.

Reactor Power

Reactivity feedback is not modeled with the RELAP5 model. Instead, reactor power is maintained constant until a reactor scram is expected to occur. A power decay curve is then used to predict the power runback. This is considered acceptable for rupture or blockage events involving only a limited number of tubes. Overall core power will not be significantly impacted due to reactivity feedback, and conservative peaking factors could be chosen to account for local power perturbations in the affected tube(s). Axial and radial peaking factors are adjusted as desired for the transient being analyzed. The base peaking values assumed are a radial peaking of 1.0 (an average power tube) and an axial peaking of 1.09 (chopped cosine). Heat generation within the core is assumed to be split 94.5% in the fuel and 5.5% in the graphite.

Control Systems

Detailed control system data were not available when the model was developed, and so simplified control systems were assumed based on expected performance characteristics. For the transient analyses presented in this report, these assumptions have either minimal impact or are set to have conservative responses. Steam drum pressure is controlled by the turbine throttle (a valve leading to a low pressure time dependent volume). Steam drum level control is controlled by throttling a feedwater return valve via a three element controller that monitors level and the steam and feedwater flow imbalance. The feedwater return pumps are approximated using a constant pressure time dependent volume. The primary coolant pumps are also approximated with time dependent volumes, with controls that maintain a constant differential pressure between the steam drum downcomer (essentially the PCP suction) and the distribution headers (essentially the PCP discharge). The reactor loop flow is set by adjusting the inlet control valves to the pressure tubes until the desired flow is established in the affected and unaffected pressure tubes.

Pressure in the Graphite Stack

The prediction of the local pressure in the core cavity volume V_l as a function of time is based on the following assumption:

- Adjacent graphite block heat structures in the core cavity are modeled, as they are considered to be in the 'sphere of influence' for a tube rupture. This has been assumed to be a maximum sized cube of approximately 1.25 m on edge immediately surrounding the rupture location (the equivalent of a 5x5 tube array and 1.25 m high).

- Steam, generated on the surface of the graphite stack and construction material, flows through narrow channels between the rings and graphite blocks (or potentially fragments of graphite blocks).

- The hydraulic diameter of the rupture varies in our calculation from a diameter of the pressure tube (guillotine break) to a hydraulic diameter obtained from tube rupture accident at the Leningrad NPP [2].

The proposed RELAP5 model is shown in Figure 2. As one can see from the model of the RDIPE RBMK core cavity pressure (Fig. 2a), it is assumed that there were no drag forces for the steam flow between the sphere of influence where the steam has been generated, and the top reactor plate. In our model (Fig. 2b), steam generated in the sphere of influence flows through the annular gap around the pressure tube. This allows the RELAP5 model to predict the local pressure developed in V_l.

The equation of conservation of mass for V_l is:

$$\frac{d\rho''}{d\tau} = \frac{3}{4}\frac{\dot{m}}{D_l} \qquad (3)$$

Neglecting the specific volume of water compared to the specific volume of a saturated steam, and using equations of state for saturated steam, we find:

$$\frac{dp}{d\tau} = \frac{3}{4}\frac{\dot{m}}{D_l\left(\frac{1}{RT_s}-\frac{1}{r}\right)} \qquad (4)$$

Mass flow rate m is a resulting flow, consisting of the three components:

$$\dot{m} = \dot{m}_{cr} + \dot{m}_{ev} - \dot{m}_{dis} \qquad (5)$$

m_{cr} is the critical (choking) flow rate from the pressure tube rupture.

Steam generated on the graphite and metallic surfaces M_{ev} is calculated in RELAP5/MOD3 for the boundary condition $\partial T/\partial R = \delta(T_{gr}-T_s)$ and for initial

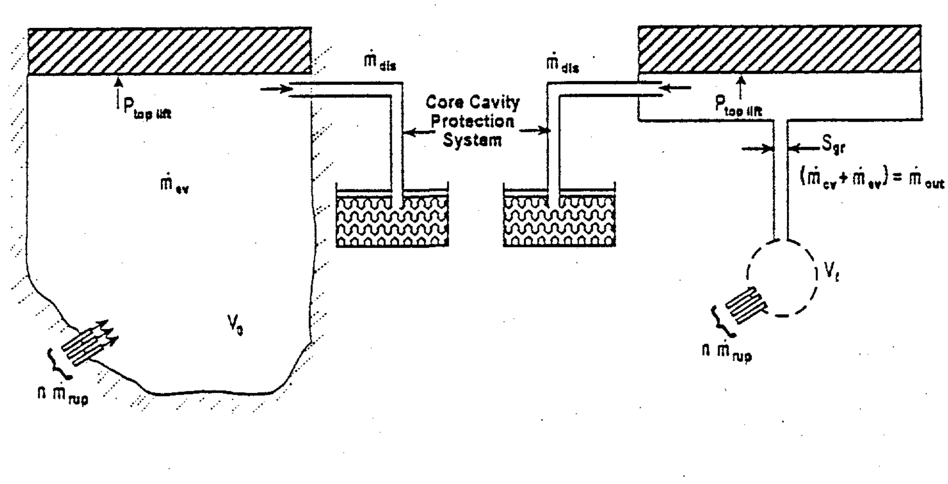


Figure 2.a) Model of RDIPE for RBMK Core Cavity

Figure 2.b) RELAP5 Model for RBMK Core Cavity

condition T_{gr} = const. The coefficient of heat transfer, α, is representative of the value for film or transition boiling. The maximum radius of influence R_l is calculated for a square cell with 5 tubes on a side. For a given Biot number, the heat flux from the graphite to wet steam is found as a function of time. The mass of steam generated in the graphite stack is $M_{ev}=Q/r$, and mass flow rate of steam generated in the time interval $\Delta\tau$ is:

$$\dot{m}_{ev} = \frac{M_{ev}}{\Delta\tau \cdot S_{gr}} \qquad (6)$$

S_{gr} is the cross section of the channel at the graphite (see Fig. 2b). m_{dis} in eq (4) is given for a specific NPP by characteristics of the piping system discharging steam from the reactor core cavity to the pressure-suppression pool.

III. CODE VERIFICATION AND ANALYSIS OF RESULTS

The first stage of the verification involved calculations of steady-state parameters in the RBMK. The general operating parameters calculated by RELAP5 for the RBMK are given below. Prior to initiating each transient case, a null transient is run to ensure that steady-state conditions have been reached. This solution provides the starting ($\tau=0$) conditions for the transient calculations. The steady-state calculations compare well with the design parameters of RBMK.

- 3197 MW thermal core power (equivalent full core).
- 1.93 MW affected tube power (determined for Leningrad tube rupture).
- 1.48 axial P/A (maximum value, used for inlet tube rupture).
- 1.09 axial P/A (average value, used for Leningrad tube rupture simulation).
- 6.97 MPa steam drum pressure.
- 8.27 MPa inlet header pressure.
- 37,600 tonne/hr total loop flow, an average of 6.288kg/s per tube.
- 5220 tonne/hr steam flow (equivalent full core).
- 13.8% average exit quality.
- 287°C coolant outlet temperature (saturated).
- 270°C coolant inlet temperature.
- 100 mm drum level.

The second verification involves comparison against known experimental data (or correlations) and RBMK transient data. For the evaluations presented in this report, this will include comparison against known correlations for two-phase choked flow, comparison of the estimated time to tube rupture for the Leningrad tube rupture, and comparison of the core cavity pressure for the Leningrad tube rupture. In addition, a review was performed for the physical reasonableness and consistency of the RELAP5 results. This was important for the fuel temperature and steam drum responses as little or no data were available for the transients evaluated.

Inlet Tube Rupture

An inlet tube rupture was analyzed for an inlet rupture immediately upstream of the fuel region. One and two tube ruptures were considered, however, only the results of the two tube rupture case are presented. It was assumed that reactor power would remain constant, and that no control system feedback or inventory makeup would occur.

Three important evaluations were made with this case. First, the RELAP5 choke flow model was compared against accepted correlations for two-phase choke flow. This is shown in Fig. 3a. The comparison of the RELAP5/MOD3 calculations for the critical flow from the ruptured tubes with accepted correlations [4,5,6] has shown that the calculations correlate well with expected values. Second, the capability of the system model, and in particular the steam drum model, to provide physically meaningful and consistent results for RBMK small break LOCAs was evaluated. The results presented here are for the case of two simultaneous tube ruptures. Break flow, system and drum mass, drum level and void fraction in the drum for first 1800s of the transient are shown in Figs. 3 (b,c,d). The third part of this evaluation is for the transient response itself, as an inlet tube rupture represents a potential challenge to both inventory control and fuel integrity.

Within the first seconds pressure of the affected tube has fallen abruptly to the critical flow pressure. Figure 3a shows the variation of mass flow rate. The flow after ~20s is equal to the critical flow for two-phase media. During the inlet tube rupture simulation, the affected tubes were cooled by reverse flow from the steam drum with a heat transfer rate similar to the normal operating condition. Therefore, initial fuel and cladding heatup were not significant. Potential core cooling problems could exist due to long term steam drum inventory and primary coolant flow control. In Figs. 3b, c, and d are shown system and drum mass, steam drum level and void fraction in the steam drum separator. After 1000s, the level in the steam drum has fallen and drum void fraction

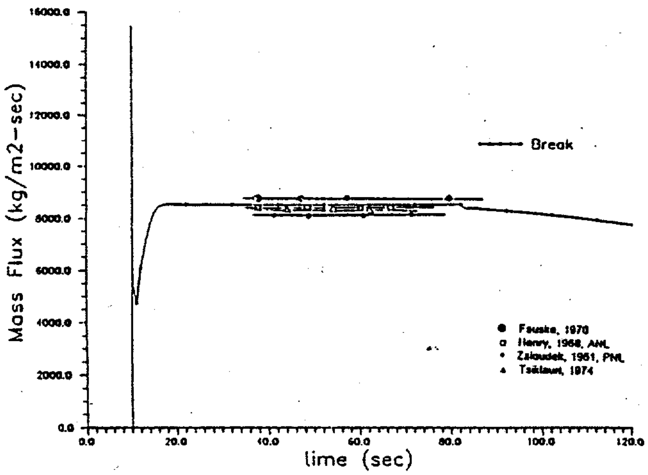


Figure 3.a

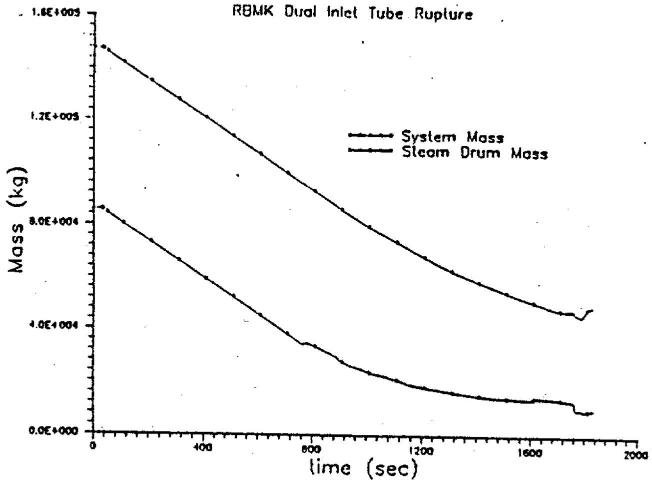


Figure 3.b

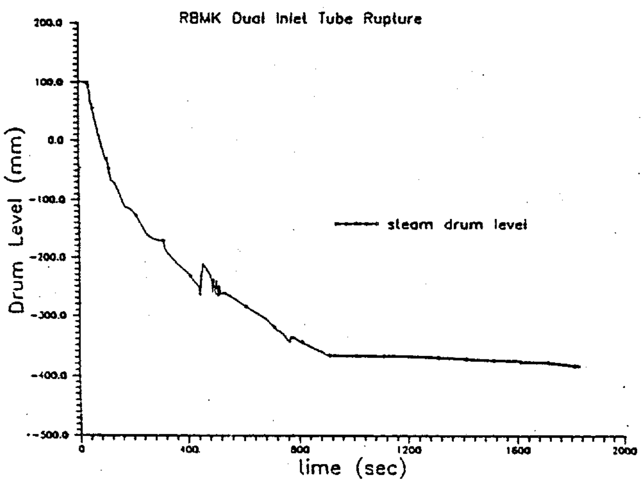


Figure 3.c

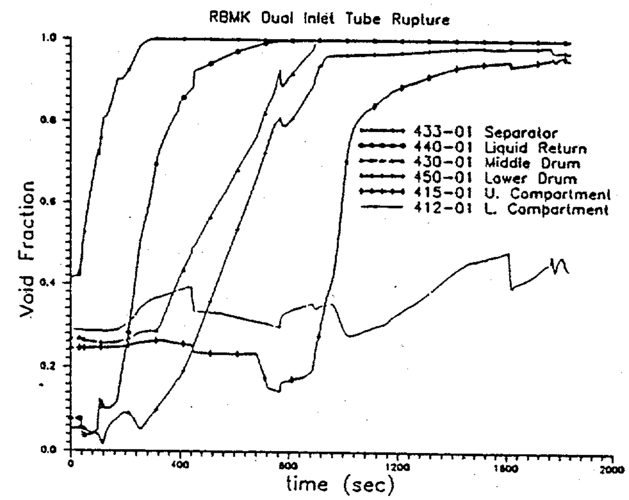


Figure 3.d

(Fig. 3d) has reached ~1.0 even at compartment number 450 of the drum (Fig.1). This means that at the inlet of the affected tubes the density of the coolant and heat transfer rate have decreased and the heatup of the channel could result. The general response of the RELAP5 for the steam drum and system inventory appear to be reasonable (for the assumptions made).

Tube Blockage

A series of tube blockage cases were evaluated with the RELAP5 two region core model. Briefly, the Leningrad tube rupture was initiated by a failure of the inlet flow control valve to one core pressure tube. It was estimated from post-accident reviews that this failure resulted in a flow reduction of the inlet flow to less than 10% of the initial tube flow. The flow reduction initiated a fuel temperature excursion and also elevated the pressure tube wall temperature due to radiative and convective heat transfer between the fuel and the pressure tube wall. Approximately 40-45 seconds after the inlet valve failure, the pressure tube ruptured in the upper core. A reactor shutdown was initiated 3.7 seconds after the tube rupture due to high core cavity pressure.

To evaluate this event, a parametric study was performed over the potential range of inlet flow blockage. Each calculation made assumed an instantaneous reduction in the inlet valve flow area to simulate the valve failure of the Leningrad event. A total of five calculations were made, varying the inlet flow blockage to obtain a

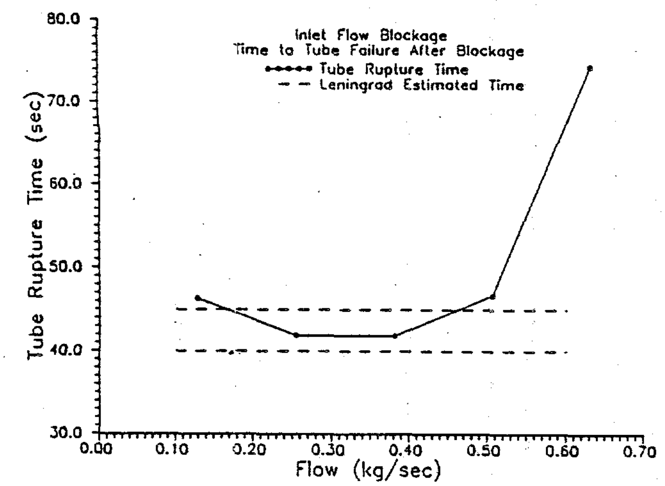


Figure 4.a

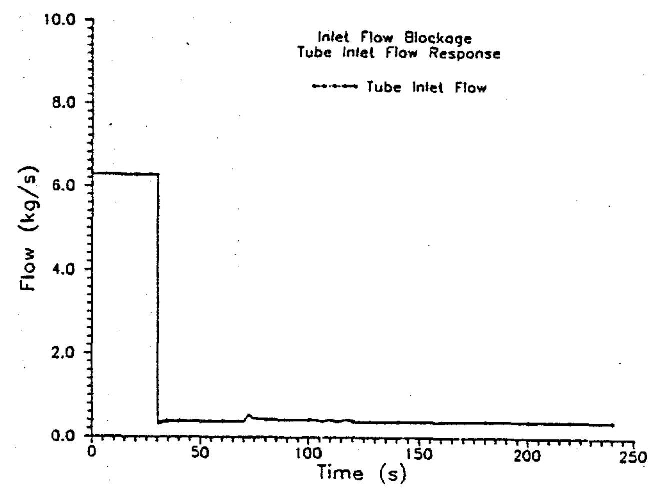


Figure 4.b

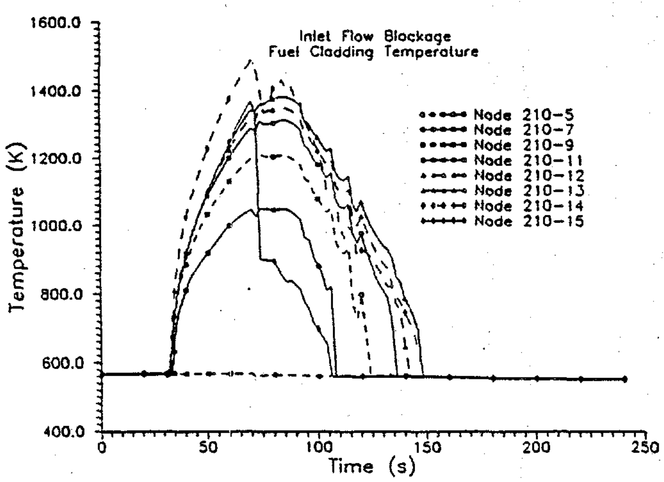


Figure 4.c

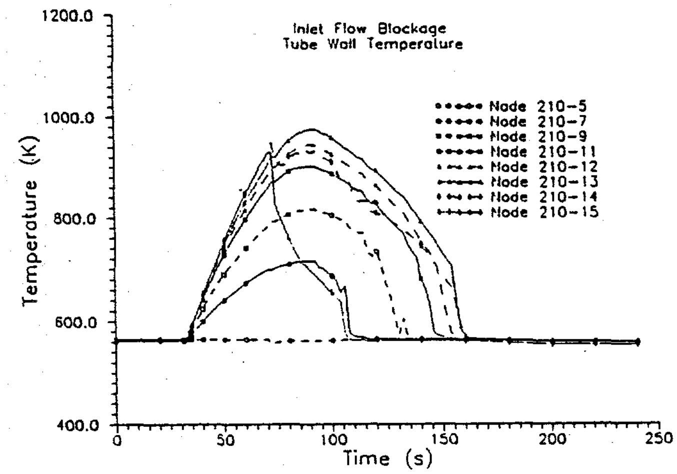


Figure 4.d

range of flow reduction between 2%-10% of the initial tube flow. Initial tube flow was 6.288 kg/s. Fuel cladding and pressure tube wall temperatures were evaluated every 0.5m with the two core region RELAP5 model. It was assumed that pressure tube failure (rupture) would occur at an average tube wall temperature of 650°C (923K). This is the temperature at which tube softening is estimated to occur that then results in tube rupture. With 0.5m volume nodalizations for the 7m active fuel region, the tube failure location was calculated to be either at 6.25m or 6.75m core elevation, depending upon the individual case. A plot of the time to pressure tube failure was made of the five calculations, and is shown in Figure 4.a. A minimum time to tube rupture of approximately 42 seconds was calculated (compared to the estimated time of 40-45 seconds).

An evaluation was also made of the general transient response, with reactor shutdown, for one of the calculations. Figures 4.b through 4.d show results from an approximate 6% flow blockage calculation. Initial tube flow for this calculation was reduced from 6.288 kg/s to 0.377 kg/s. In each of these figures, a null transient is run to ensure steady state conditions have been reached prior to initiation of the blockage (e.g., the blockage occurs at 30 seconds). Figure 4.b is the pressure tube inlet flow. Figure 4.c is the fuel cladding temperature response for selected nodes, and figure 4.d is the pressure tube inner wall temperature for the same nodes. Refer to Figure 1 for the nodalization numbers. For this calculation, the tube was estimated to rupture 42 seconds after initiation of the blockage. A reactor shutdown was initiated 3.7 seconds after the rupture, resulting in the eventual quenching of the fuel cladding and pressure tube wall. Peak cladding temperature was calculated to be 1217°C (1490 K) for this case. The general responses for this transient appear to be physically consistent.

Core Cavity Pressure

An evaluation of the reactor core cavity pressure response was made for the Leningrad tube rupture event. In addition, a parametric study was performed to evaluate different modeling assumptions and their impact on the core cavity pressure response. These calculations were made using the three core region RELAP5 model. This model is very similar to the two core region model, but allows more explicit modeling of the graphite block heat structures in the core cavity. The 6% blockage calculation from above was used as the basis of this study. For the cavity pressure response, the cavity pressure relief was based on an unmodified steam discharge piping for the Smolensk power plant I/II, RBMK 1000 [3]. This is shown in Figure 5.a. The pressure relief is modeled using this flow versus cavity pressure relationship, and assuming only vapor is discharged.

The base 6% blockage calculation assumed that the reactor coolant would discharge into the gap region between the pressure tube and graphite blocks and destroy or relocate the graphite rings so that only the annular gap provided any significant flow restriction before discharging into the main cavity area. For this base case it was assumed that only the immediate graphite block was available for heat transfer to the discharging reactor coolant (over only a 1.25 m, height adjacent of the break location). For all graphite heat structures, it was conservatively assumed that heat transfer was to the hot, outer surface of the 25 cm square blocks. The cavity pressure response for the base calculation is shown in Figure 5.b. Also shown are the Leningrad cavity pressure and two additional parametric calculations. The

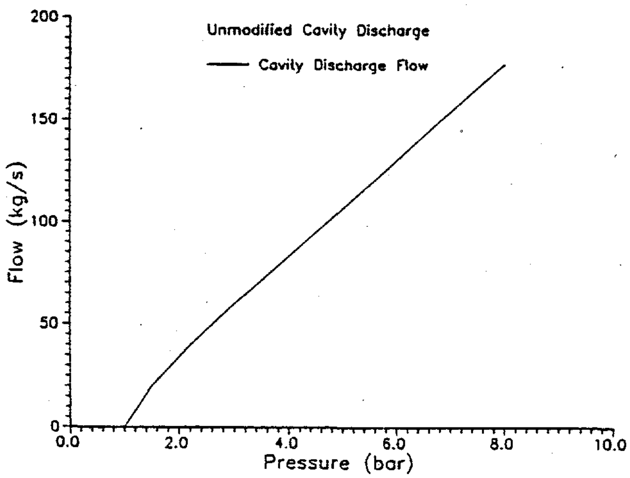


Figure 5.a

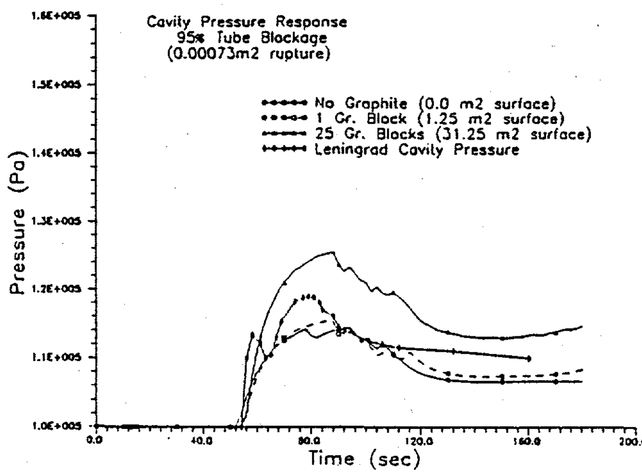


Figure 5.b

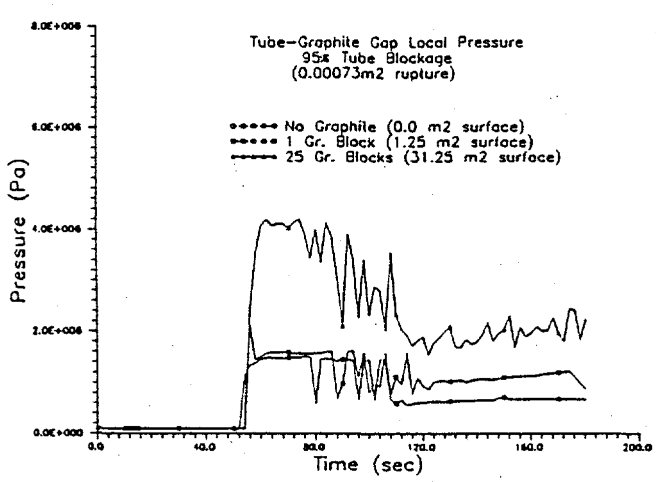


Figure 5.c

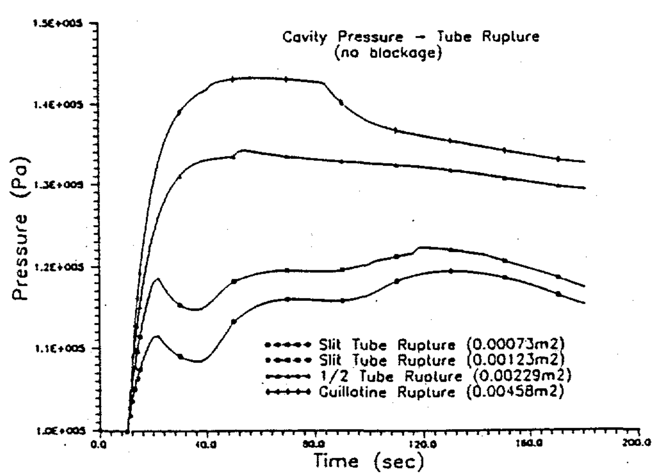


Figure 5.d

parametric calculations assumed no graphite blocks for heat transfer, and 25 blocks (the maximum 'sphere of influence', a 5x5 array surrounding the ruptured tube.) In terms of surface area available for heat transfer, these equate to $0m^2$, $1.25m^2$ (the base calculation), and $31.25m^2$. It is noted that pressure in the tube-graphite gap region exceeded 20 bar for the base calculation with the assumption of no graphite rings for flow restriction. If the rings were to remain intact, pressure in this gap region could be expected to reach 50-60 bar (effectively the reactor system pressure). The gap pressure response is shown in Figure 5.c for the 0, 1 and 25 graphite block cases.

A parametric study was also made for the potential rupture area of the tube. However, these calculations differ from the base 6% calculation in that no initiating tube blockage is assumed. The tube is assumed to rupture due to a random failure. The 6% base calculation used a rupture area of $0.00073m^2$, based on results of the tube inspection after the Leningrad event, and assuming that the fuel bundle becomes lodged in the slit opening. For this study, additional rupture areas of $0.00123m^2$, $0.0022908m^2$, and $0.0045816m^2$ were assumed. These four areas correspond to; the fuel bundle lodged to minimize rupture area; the fuel bundle lodged to maximize rupture area; the tube cross-sectional area with fuel intact; and a double-ended guillotine rupture with fuel intact. The cavity pressure response for these calculations is shown in Figure 5.d. The single tube guillotine rupture compares well with the results indicated in [3], which calculated a peak cavity pressure of ~1.4 bar.

SUMMARY AND CONCLUSION

In general the RELAP5/MOD3 code calculations for graphite-moderated light water reactors (RBMK) for the above three test series compared well with the existing transient data and experimental correlations. The results of calculations show that for the inlet pressure tube rupture simulation, initial fuel and cladding heat up did nor occur, however, long term steam drum inventory and primary coolant flow control could be a concern for long term core cooling. For the tube blockage case, simulating the Leningrad accident, minimum time to pressure tube rupture was calculated to be 42 seconds after blockage, comparing well to the estimated time of 40-45 seconds. The calculated pressure in the core cavity also compared well with the Leningrad accident measurements. However, the local pressure in the tube graphite gap region exceeded 20 bar. This could potentially cause the graphite stack to move and rupture neighboring pressure tubes. Determination of multiple tube ruptures would require further assessment.

ACKNOWLEDGMENTS

This work was sponsored by the United States Department of Energy under Contract DE-AC06-76RLO. The authors are indebted to Dr. Darrell Newman and Mr. Scott Heaberlin for their assistance and support. The manuscript was prepared by Ms. Diana L. Tallett and Ms. Pamela Sheeran.

NOMENCLATURE

p - Pressure
d - void fraction
W_g - velocity of steam
W_ℓ - velocity of liquid phase
h_ℓ - liquid enthalpy
h_g - steam enthalpy
ρ'' - saturated steam density
τ - time
M - mass
m - mass velocity
D_ℓ - diameter of sphere of influence
V_ℓ - volume of sphere of influence
T - temperature
R - gas constant for steam
r - latent heat
H - level

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Attachment B

"RBMK Pressure Tube Rupture Assessment"

ABSTRACT

The Russian RBMK reactor core design consists of multiple parallel pressure tube channels that contain Zr clad, UO_2 fuel pin bundles. These parallel channels are contained within graphite moderator blocks which are, in turn, contained within a sealed core cavity. Current safety evaluation efforts of the RBMK reactors have been concentrating in the area of tube ruptures within the core cavity and, in particular, multiple tube ruptures that could threaten the reactor core integrity. Tube rupture events result in a pressurization of the reactor core cavity. The original design overpressure for the cavity region was based on a single tube rupture, resulting in considerable margin to the top plate lift pressure. The top plate lift pressure is 3.1 bar, and a single tube rupture would result in approximately 1.4 bar. RBMK plant specific cavity pressure relief designs provide for between three and nine simultaneous tube ruptures before exceeding the top plate lift pressure. Thus, current safety evaluations have begun to examine the potential for multiple tube ruptures that could exceed the current cavity pressure relief designs.

One such scenario being examined is a partial rupture in a group distribution header that results in stagnated (low) flow to up to 40 pressure tubes. The subsequent fuel heatup in these reduced flow tubes could result in multiple tube ruptures beyond the design relief capacity of the core cavity. This paper examines several key issues in evaluating this transient, including: 1) the effects of low flow, 2) the effects of axial peaking, and 3) the effects of radial peaking, all relative to the time to tube rupture. These issues each play a significant role in attempting to evaluate the likelihood and severity of multiple tube ruptures for a partial group distribution header break. This work was sponsored by the United States Department of Energy under Contract DE-AC06-76RLO 1830.

INTRODUCTION

The Russian RBMK reactor core design consists of multiple parallel pressure tube channels that contain Zr clad, UO_2 fuel pin bundles. These parallel channels are contained within graphite moderator blocks which are, in turn, contained within a sealed core cavity. Current safety evaluation efforts of the RBMK reactors have been concentrating in the area of tube ruptures within the core cavity and, in particular, multiple tube ruptures that could threaten the reactor core integrity. Tube rupture events result in a pressurization of the reactor core cavity. The original design overpressure for the cavity region was based on a single tube rupture, resulting in considerable margin to the top plate lift pressure. The top plate lift pressure is 3.1 bar, and a single tube rupture would result in approximately 1.4 bar, [1,2]. RBMK cavity pressure relief designs provide for between three and nine simultaneous tube ruptures before exceeding the top plate lift pressure, depending upon on the plant specific designs. With this finite margin to safety, current safety evaluations have begun to examine the potential for multiple tube ruptures that could exceed the current cavity pressure relief designs.

In January of 1994, a topical meeting was convened by the International Atomic Energy Agency in Moscow, Russia [3]. As reported in a draft report from this meeting, it is has been concluded that propagation of tube ruptures is unlikely for the RBMK reactor, but that "the potential for multiple tube ruptures following partial breaks needs to be further investigated[1]." Following a partial rupture of a group distribution header (GDH) for a critically sized break, flow stagnation in the affected fuel channels results in a fuel temperature excursion, leading ultimately to tube rupture. RBMK specialists argue that the rupture of a single tube will alleviate the flow stagnation in the header as this effectively creates a larger break size. It is argued that this allows sufficient reverse flow to be established, from the steam drum, to cool the fuel in the remaining unbroken tubes. Limited results for calculations of group distribution header partial failures were reported. The analysis utilized multiple tube regions on a single group header in order to model an assumed power distribution for the header. The power distribution used is given in Table 1 (and assumed 42 tubes on a GDH).

In essence, however, the analysis investigated only the failure of the peak power tube. Depending upon the actual power distribution for tubes along a given header, it is possible for multiple tubes to be operating at, or near, the same peak powers. Thus, it would be possible for several tubes to fail before sufficient cooling is re-established following any initial tube rupture. This can be best visualized by assuming the limiting case of 40 pressure tubes on a GDH operating at the same power. For a critically sized break in the group header that results in stagnant flow, all 40 tubes would experience similar heatup rates, and could thus be theorized to fail simultaneously. It is known that such a 'flat' power distribution is not realistic, however, the purpose of this comparison is simply to illustrate that it is necessary to evaluate a limiting condition of operation for the

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[1] International Atomic Energy Agency, DRAFT REPORT of a Consultants Meeting on "Multiple Pressure Tube Rupture in Channel Type Reactors", RBMK-SC-014, dated February 16, 1994.

power distribution along a group header. With this, it is then possible to assess the likelihood and the number of additional tubes that may fail after an initial tube has ruptured.

Table 1. GDH Power Distribution

Power (MW)	0.99	1.275	1.89	2.38	3.0
No. of Channels	2	7	22	10	1

In order to adequately assess this issue, it is necessary to perform calculations for different sized header breaks, and with different power distributions. Because there are virtually an unlimited number of tube power distributions within a group header, this could theoretically require an unlimited number of calculations to be made (one for each power distributions). Clearly, it is not practical to attack this issue with an unlimited number of calculations. Instead, it is proposed to characterize tube failure times for different power (and flow) distributions and then cross-reference this information with additional analyses of partial header breaks. The header break analyses would define the minimum number of tubes that would be required to fail in order to establish adequate reverse flow. At this time, it is assumed that one tube rupture may not be sufficient to alleviate the stagnant flow condition prior to additional tube ruptures. Cross-referencing the results of these two evaluations, a reference data base could be established that can identify the number of tubes that could be expected to fail for a given power distribution and partial header break.

This paper examines the minimum time to tube failure for a variety of power distributions and stagnant flow conditions that could be expected for a group header failure in order to address the potential for multiple tube ruptures. The results of this analysis will form only the initial phase of the group header failure analysis. The second phase, which is proposed to be completed at a later date, will characterize the break itself. This would provide the necessary estimate of the stagnant flow conditions and the time required for reverse flow to be established in the unbroken tubes after an initial tube has ruptured. Once this is accomplished, a comparison of actual power distributions within operating RBMKs can be made against this data base that can estimate the maximum number of tube ruptures that could occur, without having to perform new calculations for each unique power distribution.

RBMK REACTOR DESIGN

The RBMK reactor is a graphite-moderated, vertical pressure tube, boiling water reactor. The reactor type characterized in this report is for an RBMK-1000 reactor. The reactor core and reflector form a cylindrical stack with diameter of 13.6m and height of 8m. There are 1661 pressure tubes penetrating the stack, centered in 25cm square graphite blocks that form the graphite moderator. The fuel contained within each pressure tube is 2% enriched uranium oxide with a Zr-1% Nb cladding. The pressure tubes are Zr-2% Nb. There are two 18-pin fuel bundles in each pressure tube, approximately 3.5m each. The bundles hang from a carrier rod, and the fuel forms two concentric rings of 6 and 12 fuel pins. Hydraulically, the reactor consists of two primary coolant loops, each servicing half the core. Each loop has two steam drum separators, that are common with the other loop at the steam header, and four main circulation pumps, three operating and one for reserve (for a total of eight coolant pumps, six operating). The main coolant pumps discharge into two main headers that supply 22 distribution headers each, for a total of 44 headers. These distribution headers supply coolant to the 1661 pressure tubes, with approximately 40 pressure tubes each. The coolant is directed up through the fuel channels at 37,600t/hr. The average core outlet quality is 14.5%. Individual tube flow is adjusted manually via an inlet throttle valve. Outlet connector piping then routes the two-phase coolant up to the steam drum separators, located above the core, for vapor separation. The drum separators essentially use screen dryers to produce steam at 0.1% humidity (moisture). Nominal operating pressure at the drum is 6.97MPa. Steam is then supplied to one or both of two 500MWe turbines. Condensate from the turbine condensers is reheated to 270°C, and returned to the primary loop at the steam drums.

RELAP5 MODEL DESCRIPTION

A base RELAP5 model was developed that represents one-quarter of an RBMK reactor core, approximately 416 fuel channel tubes. The nodalization is setup to perform a detailed calculation of an affected core region (such as for a single or multiple tube rupture or blockage). The balance of the 1/4 core is lumped into a single tube to allow the RELAP5 model to predict fluid conditions in the steam drum and inlet distribution headers. It was felt that a simple single tube model, with boundary conditions for these regions, would not allow sufficient degrees of freedom in the calculation to provide accurate results. The nodalization scheme in shown in Figure 1. A 1/4 core model is used as this is the minimum size needed to include a steam drum model. The two fuel region model allows for one or more affected tubes (fuel channels) to be modeled separate from the intact core with only minor modifications required specific to the transient being simulated.

The affected tube is modeled hydraulically using 9 inlet connector volumes, 16 axial fuel volumes (14 active fuel regions), 6 upper tube volumes, and 5 outlet connector volumes. This nodalization allows for detailed pressure and temperature monitoring, and ease of defining the tube rupture location for different events without significant changes to the base model. The intact core is modeled using 5 inlet connector volumes, 7 axial fuel volumes (5 active fuel regions), 5 upper tube volumes, and 5 outlet connector volumes. Overall, the 1/4 core model represents 416 fuel channels, typically a single affected channel and 415 lumped channels.

The steam drum separator is modeled using 14 volumes. This is also shown in Figure 1. This modelling detail allows for a more accurate inventory calculation, and in particular, a more accurate prediction of the fluid conditions for reverse flow into the affected tube(s). There are additional volumes for the inlet sparger volume (for feedwater return), an outlet downcomer for coolant return to the primary coolant pumps (PCPs), and steam piping volumes leading to the turbines. The turbines, PCPs, and feedwater return pumps are not modeled explicitly. They are approximated using time dependent volumes to supply the necessary boundary conditions, with the fluid conditions taken from plant operating data. The steam drum is sized to represent a single drum for the 1/4 core model.

The heat structures modeled include the fuel pins and carrier rod, pressure tube and surrounding graphite, and the inlet and outlet connector piping walls... No heat structures are modeled at this time for the steam separator. The affected tube for both models contains two fuel pin heat structures that represent an equivalent of 6 and 12 fuel pins lumped together to represent the 18 fuel pins per rod bundle. This allows radial power peaking to be modeled for the 6 inner and 12 outer fuel rings of the fuel bundle. The unaffected tubes are modeled with a single heat structure representing an equivalent of 18 fuel pins lumped together.

For the affected core, the RELAP5/MOD3 radiative heat transfer model is used. Radiative heat transfer between the inner fuel ring, outer fuel ring, carrier rod, and tube wall is modeled. Appropriate view factors were calculated for each heat structure component. Preliminary calculations were made to investigate the surface emissivity for the fuel cladding and tube wall. Emissivity values of 0.5, 0.6 and 0.7 were evaluated. This range is considered to be a typical range for Zr (the cladding and tube wall material). An average value of 0.6 was chosen for the calculations presented here, as the preliminary results did not show a strong dependence over this range of emissivity.

Reactivity feedback is not modeled with the RELAP5 model. Instead, reactor power is maintained constant until a reactor scram is expected to occur. A power decay curve is then used to predict the power runback. This is considered acceptable for rupture or blockage events involving only a limited number of tubes. Total core power will not be significantly impacted due to reactivity feedback, and conservative peaking factors can be chosen to account for local power perturbations in the affected tube(s). Axial and radial peaking factors are adjusted as desired for the transient being analyzed. The base peaking values assumed are a radial peaking of 1.0 (an average power tube) and an axial peaking of 1.09 (chopped cosine). Heat generation within the core is assumed to be split 94.5% in the fuel and 5.5% in the graphite.

Detailed control system data were not available when the model was developed, and so simplified control systems were assumed based on expected performance characteristics. For the transient analyses presented in this paper, these assumptions have negligible impact. Steam drum pressure and level are assumed to remain relatively constant over the time interval of the flow reductions investigated, 20 to 60 seconds, and so simple controls are modeled to maintain constant pressure and level. The primary coolant pumps are also approximated with time dependent volumes, with controls that maintain a constant differential pressure between the steam drum downcomer (essentially the PCP suction) and the distribution headers (essentially the PCP discharge).

The reactor loop flow is set by adjusting the inlet control valves to the pressure tubes until the desired flow is established in the affected and unaffected pressure tubes.

Trip logic is added to provide additional control of the transient. This includes logic to initiate the desired event, such as a failed inlet control valve to the pressure tube, and logic to initiate a tube rupture upon detecting high tube wall temperature (at a predetermined failure temperature).

ANALYSIS DISCUSSION AND RESULTS

A partial group distribution header failure results in stagnant (low) flow to multiple process channels. Simulation of a tube failure due to this stagnant flow condition was performed for a single tube in order to evaluate time to failure for various power and flow distributions. These analyses assumed no reactor shutdown would occur until after tube rupture has occurred. The simulation was accomplished by reducing inlet flow to a single channel to a predetermined flow, effectively simulating a partial header failure for that tube. Based on results presented in [4,5], and flow distribution calculations presented in this paper, the flow reduction was set to 6% of the initial tube flow for the power distribution cases. This results in the minimum time to tube failure. Zero flow (fully stagnant) yields slightly longer times to failure, as do higher flows. Tube failure is assumed to occur at an average tube wall temperature of 650°C, [1,2].

Results are presented for each of three areas investigated. First, the stagnant (low) flow condition is investigated. Analyses of the Leningrad tube rupture indicate that the minimum time to tube failure occurs at a flow reduction between 4% and 6% of the initial tube flow, [4,5]. Fully stagnant and higher flows yield longer times to tube failure. Similar phenomena would be expected for a header break. Second, axial power distribution is investigated for both the axial peak-to-average (P/A), and the location of the peak. High axial peaking coupled with top skewed profiles would be expected to yield faster tube failure times as the upper fuel region would receive only steam cooling for a longer period of time. Third, the radial power distribution is investigated for tube powers between the core average and peak power tube. The Leningrad tube rupture is estimated to have occurred within 40-45 seconds after the flow reduction. However, this was for an average power tube, 2.0MW, and the maximum RBMK tube power allowed is 3.0MW. Higher power tubes would be expected to fail sooner. Each of these analyses is discussed separately below.

Flow Distribution Results

The flow distribution calculations were performed for flow reductions varying between 2% and 12% of the initial tube flow, for tube powers of 2.0MW, 2.5MW and 3.0MW. Initial tube flows for these tube powers were assumed to be 6.30, 7.86, and 8.43kg/sec, respectively. These were performed for an axial peaking of 1.10, at a relative core axial elevation of 0.667. Figure 2 illustrates the calculated times to tube failure versus the reduced flow. The minimum tube failure time is seen between the reduced flows of 4% to 8% for each tube power. This minimum is attributed to optimization of the heat transfer to the tube wall between radiative and convective heat transfer. For extremely low flows, heat transfer to the wall is dominated by radiative heating from the hot fuel. At slightly higher flows, the fuel heat is more efficiently transported to the tube wall with radiative and convective heat transfer, but the flow rate is insufficient to transport the energy out of the fuel region. At even higher flows, the fuel heat is transported out of the core region, maintaining lower fuel and tube wall temperatures.

Power Distribution Results

The power distribution calculations were performed in two steps. First, axial power peaking was investigated for peak-to-average (P/A) ratios of 1.10, 1.30, and 1.50. These values are expected to be representative for RBMK reactors. The profiles are illustrated in Figures 3a,b,c. These axial peaking values were analyzed at relative axial core elevations of 0.333, 0.50 and 0.667 for a tube power of 2.0MW (an average power tube). From above, a reduced flow of 6% of the initial flow was assumed for each calculation to obtain the minimum time to tube failure. Figure 4 illustrates the calculated times to tube failure. The minimum tube failure times for the 0.50 and 0.667 axial peak are dominated by the higher linear heating rates at the axial peaks. However, for the 0.333 axial peak, the flatter axial profile yielded faster times to failure. This is attributed to the higher heating rates at the core end region. Tube failure was predicted to occur in the last meter of the core, between six and seven meters, for all three axial P/A curves for the 0.5 and 0.667 x/l profiles. For the 0.333 x/l profiles, the 1.30 and 1.50 axial P/A curves resulted in tube failure occurring between five and six meters, one to two meters below the top of the core. The flatter 1.10 axial profile, though, still predicted tube failure in the last meter due to the higher core end region power. As would be expected though, faster tube failure times are seen for similar linear heating rates at higher axial elevations in the core.

Second, radial peaking (tube power) was investigated for tube powers ranging from 2.0 to 3.0MW, at 0.25MW intervals. These were performed at axial peakings of 1.10 and 1.30, and at relative axial core elevations of 0.50 and 0.667. Again, a reduced flow of 6% of the initial flow was assumed to obtain the minimum time to tube failure. Figure 5 illustrates the calculated times to tube failure. The tube failure times are fairly linear with the total tube power for a given axial P/A. Minimum time to tube failure is approximately 23 seconds for a 3.0MW tube power, as compared to the 44 seconds that was calculated for a 1.10 axial P/A at 2.0MW (approximately the Leningrad tube conditions).

CONCLUSIONS

The RBMK process tube channels were investigated for time to failure over a range of power distributions and stagnant (low) flow. As reported in [4,5] for an average power tube, over a range of low flow to a channel the minimum time to tube failure will occur between 4% and 6% of initial flow. This is consistent with the results of this report. This is important relative to the partial header break in that fully stagnant (zero) flow it is not necessarily the worst condition for evaluating the critical break size. A break size resulting in 6% of the initial channel flow results in a minimum tube failure time and, creates the added penalty of requiring the initial tube rupture(s) to overcome (reverse) this positive flow before sufficient reverse flow is established in order to preclude additional tube failures in the remaining affected tubes. That is, additional tube failures may be necessary before sufficient reverse flow is established.

Axial peaking is important to the initial heatup. For a given peak linear heating rate, the higher the relative location of that peak the sooner the tube will fail. This is true except for a relatively flat profile as seen for the 1.10 P/A results. For this profile, the core end region power becomes more significant than just the peak linear heating rate. This presents a trade off against flattening core axial peaking in that flatter profiles can become more limiting if core end region powers become too high. In short, extremely flat profiles and top skewed profiles with a high P/A will result in the fastest tube failure times.

Clearly, tube power plays a strong role in determining minimum time to failure following a flow reduction. Failure times varied fairly linearly for tube powers between 2.25 and 3.0MW, from approximately 34 to 22 seconds, respectively. This is extremely important when evaluating the likelihood of additional tube failures when one considers the possible ranges of tube power distributions along a group header. For the distribution shown in Table 1, the difference in time to failure for 2.38 versus 3.0MW calculated in this report is approximately 10.0 seconds. This has the potential for allowing sufficient time to establish reverse flow (assuming one tube failure is sufficient) that would cool the remaining tubes. However, if the two peak tube powers are 2.38 and 2.5MW (for example a group header supplying the core fringe), then the difference in time is only approximately 2.0 seconds. Without additional analysis, it is not clear that this is sufficient time to quench the next hottest tube.

By themselves, these results are inconclusive about the likelihood of multiple tube ruptures for a group distribution header partial failure. However, they are intended to form an initial data base that would be cross-referenced with additional calculations of partial group header breaks. Once calculations have been made that adequately characterize the establishment of reverse flow following the initial tube rupture, cross-referencing the minimum times to failure for any power distribution can produce an estimate of the number of tubes that could be expected to fail for a partial header break. The characterization of the header break is the next step for this proposed approach.

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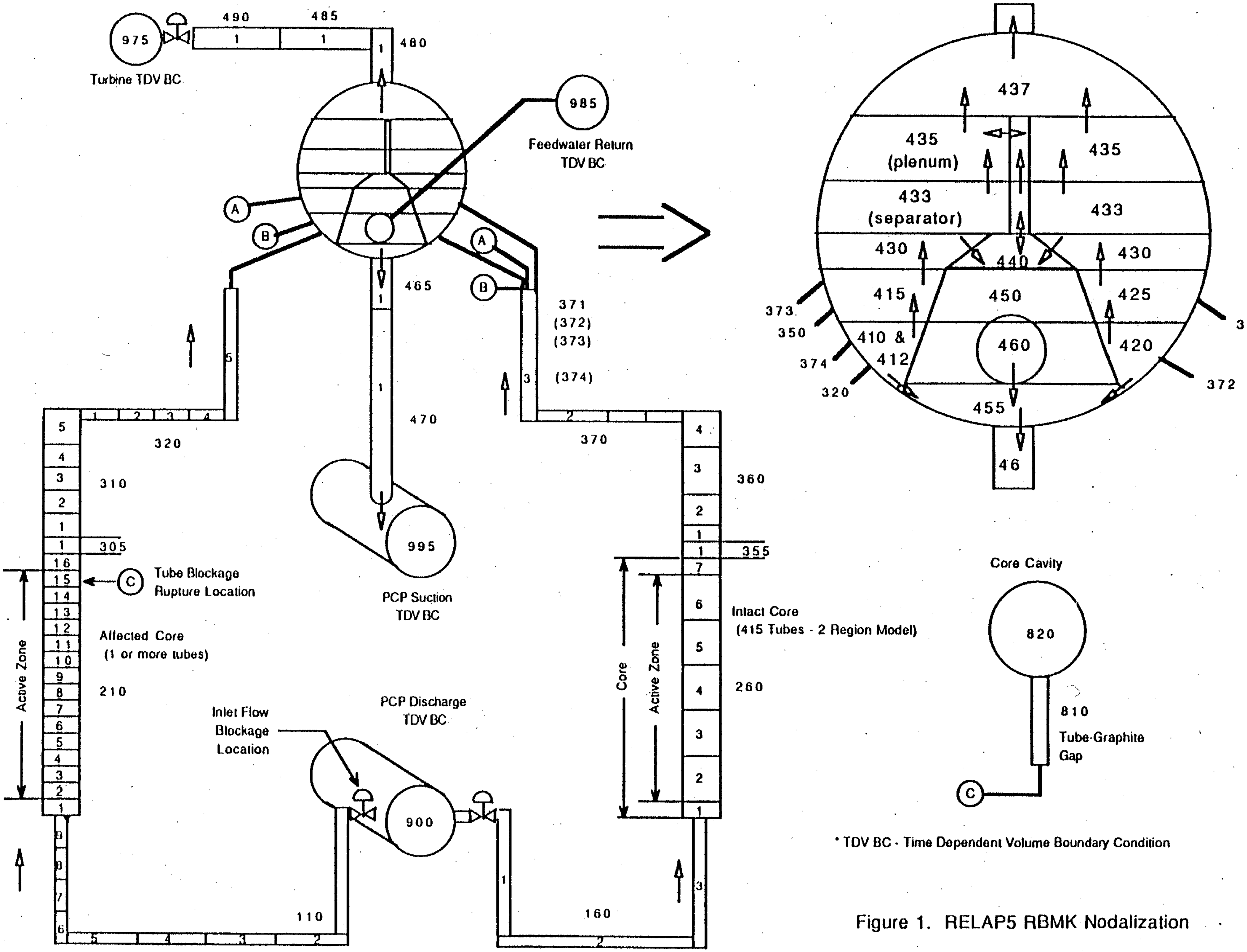

* TDV BC - Time Dependent Volume Boundary Condition

Figure 1. RELAP5 RBMK Nodalization

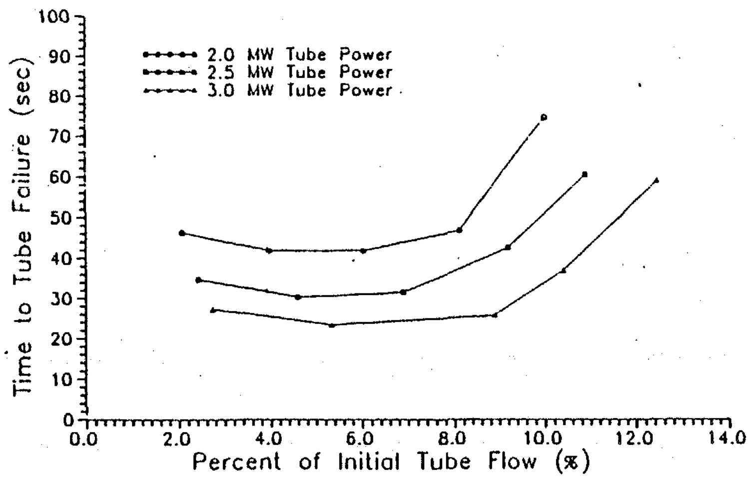

Figure 2) Tube Failure Time versus Percent Initial Flow

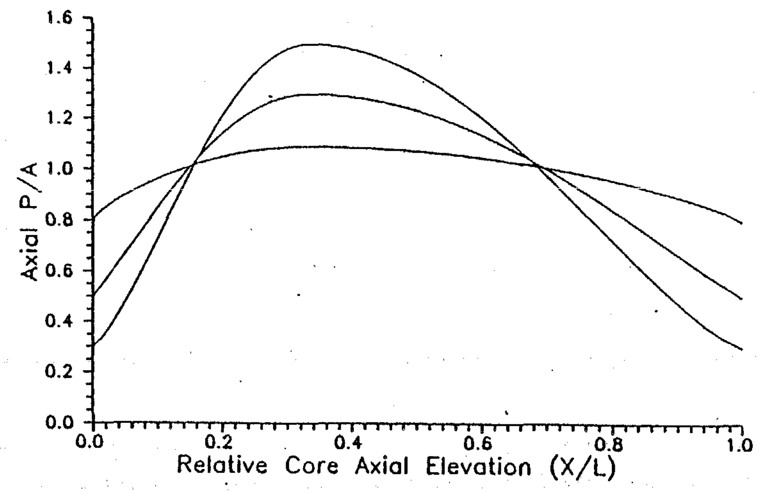

Figure 3a) Bottom skewed axial power profile X/L=0.333 for P/A=1.10,1.30,1.50

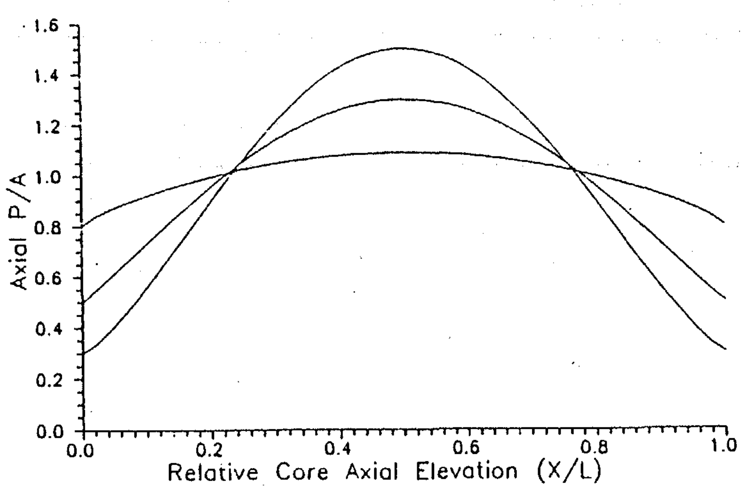

Figure 3b) Symmetrical axial power profile X/L=0.50 for P/A=1.10,1.30,1.50

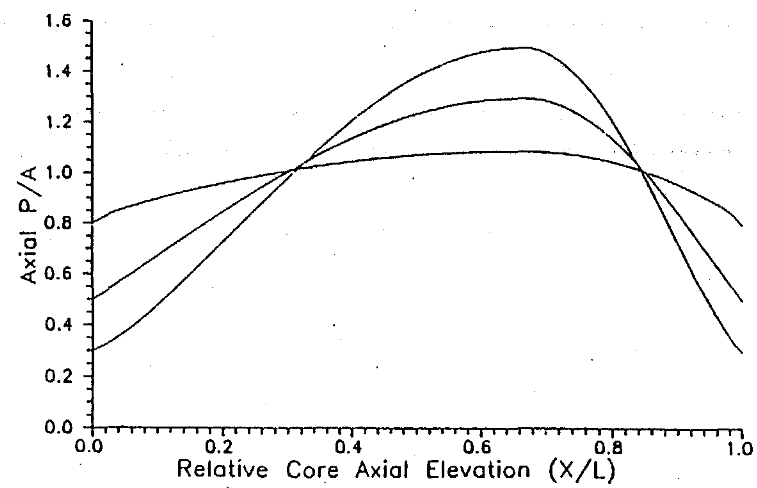

Figure 3c) Top skewed axial power profile X/L=0.667 for P/A=1.10,1.30,1.50

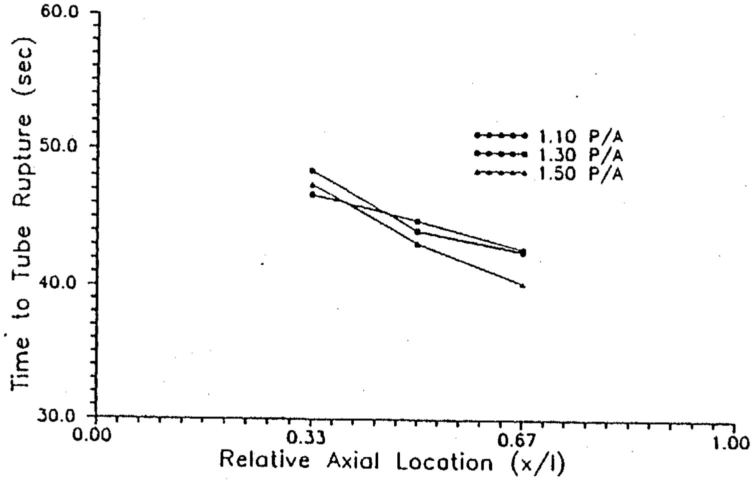

Figure 4) Tube Failure versus Axial Peaking
(for 2.0MW tube power)

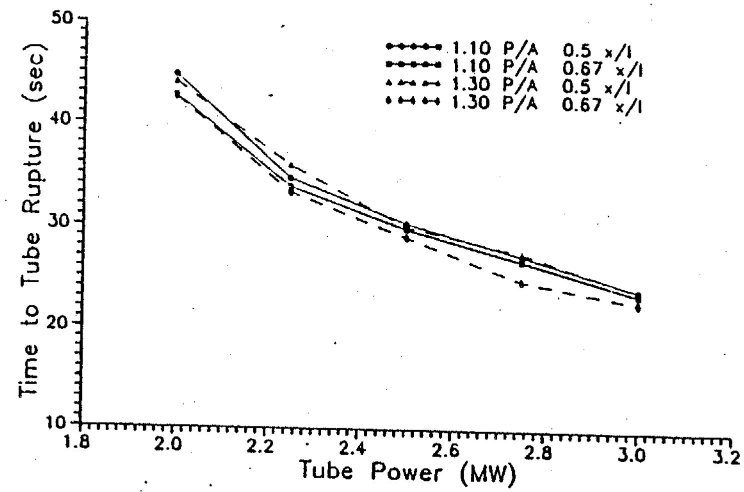

Figure 5) Tube Failure versus Tube Power
(for different peaking)

Attachment C

"Thermal-hydraulic Instabilities in Pressure Tube Graphite-moderated Boiling Water Reactors"

THERMAL-HYDRAULIC INSTABILITIES IN PRESSURE TUBE GRAPHITE - MODERATED BOILING WATER REACTORS

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ABSTRACT

Thermally induced two-phase instabilities in non-uniformly heated boiling channels in RBMK-1000 reactor have been analyzed using RELAP5/MOD3 code. The RELAP5 model of a RBMK-1000 reactor was developed to investigate low flow in a distribution group header (DGH) supplying 44 fuel pressure tubes. The model was evaluated against experimental data.

The results of the calculations indicate that the period of oscillation for the high power tube varied from 3.1s to 2.6s, over the power range of 2.0 MW to 3.0 MW, respectively. The amplitude of the flow oscillation for the high powered tube varied from +100% to -150% of the tube average flow. Reverse flow did not occur in the lower power tubes. The amplitude of oscillation in the subcooled region at the inlet to the fuel region is higher than in the saturated region at the outlet. In the upper fuel region and outlet connectors the flow oscillations are dissipated.

The threshold of flow instability for the high powered tubes of a RBMK reactor is compared to Japanese data and appears to be in good agreement. This work was sponsored by the United States Department of Energy under Contract DE-AC06-76RLO 1830.

INTRODUCTION

On March 24, 1991, the Unit 3 reactor at Leningrad Nuclear Power Plant, a 1000 MW pressure tube graphite moderated reactor, was automatically shutdown because of a pressure tube rupture in the upper part of the reactor core cavity [1,2]. The rupture occurred due to a failure of the inlet flow control valve to one of the core pressure tubes. It was estimated from a post-accident review, that this failure resulted in flow reduction of the inlet flow to less than 10% of the initial tube flow. The flow reduction initiated a fuel temperature excursion and also elevated the pressure tube wall temperature due to radiative heat transfer between the fuel rods and tube wall. Approximately 40-45 seconds after the inlet valve failure, the pressure tube ruptured. The reactor shutdown was initiated 3.7 seconds after the pressure tube rupture due to the high core cavity pressure.

Similar events are also possible for a partial break of the distribution group header, when quasi-stagnation or flow fluctuation at near zero pressure drop ΔP occurs. At this condition, the post-dryout heat transfer under low flow is not sufficient to prevent a pressure tube wall temperature excursion. The purpose of this paper is to validate RELAP5 for two-phase flow dynamic instability problems in RBMK reactors. The work includes two related accident analysis:

- Blockage of coolant at the pressure tube inlet.
- Blockage of DGH or partial break of DGH.

The general characteristics of the RBMK type reactor are as follows:

- Thermal core power 3200 MW.
- 1661 fuel tubes, 7 m active core, average linear heat flux 153 W/cm.
- Operating pressure 7 MPa.
- 37,600 tonne/hr total loop flow, an average of 6.288 kg/s per tube.
- 40 DGH with 42 pressure tubes in each.

The reactor has four steam drum separators, two hydraulic loops common at the steam header and 8 main circulation pumps (PCP), (6 operating, and 2 reserved).

RELAP5 Models for RBMK

Two RELAP5 models were developed that represent a 1/4 core and 1/2 core of an RBMK reactor. With these two models, minor modifications were made specific to the transient being simulated. For both models, the nodalization is setup to perform a detailed calculation of an affected core region (for a single or multiple tube rupture or blockage). The balance of the core is lumped into a single tube to allow the RELAP5 model to predict needed fluid conditions in the steam drum and inlet distribution headers. It was felt that a simple single tube model, with boundary conditions for these regions, would not allow sufficient degrees of freedom in the calculation to provide accurate results. The nodalization schemes for both models are shown in Figures 1 and 2. RBMK design data were provided by [1,2,3].

The 1/4 core model assumes a 1/4 core symmetry for the RBMK, contains two parallel fuel regions for the reactor core, and uses boundary conditions for the main coolant pump (Figure 1). A 1/4 core model is the minimum size needed to include a steam drum model, and is readily adaptable for assuming conservative core power distributions (i.e. assuming high/low power regions). The two fuel region model allows for one or more 'affected' tubes (fuel channels) to be modeled separate from the intact core for events such as tube rupture or blockage. The 1/2 core model contains four parallel fuel regions for the core, and a pump model to provide a complete loop simulation. The 1/2 core representation allows a more accurate calculation of the core average conditions as the RBMK core is split in-two hydraulically.

For both models, the affected tube is modeled hydraulically using 9 inlet connector volumes, 16 axial fuel volumes (14 active fuel regions), 6 upper tube volumes, and 5 outlet connector volumes. This nodalization allows for detailed pressure and temperature monitoring, and ease of defining the tube rupture location for different events without significant changes to the base model. The intact core is modeled using 5 inlet connector volumes, 7 axial fuel volumes (5 active fuel regions), 5 upper tube volumes, and 5 outlet connector volumes (these outlet connectors are set up to allow for future model expansions as needed). In the three channel model, a third channel is modeled hydraulically with the same detail as the affected core. Overall, the two channel model (1/4 core) represents 416 fuel channels, typically a single 'affected' channel and 415 lumped channels. The four channel model (1/2 core) represents 830 fuel channels, typically one or more 'affected' channels, two sets of parallel channels for the balance of the 44 tubes on one DGH, and the remaining 786 lumped channels.

The steam drum separator is modeled using 14 volumes. This is shown in Figures 1 and 2. This modelling detail allows for a more accurate inventory calculation, and in particular, a more accurate prediction of the fluid conditions for reverse flow into the affected tube(s). There are additional volumes for the inlet sparger volume (for feedwater return), an outlet downcomer for coolant return to the main coolant pumps (MCPs), and steam piping volumes leading to the turbines. The turbines and feedwater return pumps are not modeled explicitly. They are approximated using time dependent volumes to supply the necessary boundary conditions, with the fluid conditions taken from plant operating data. The steam drum is sized to represent a single drum for the 1/4 core model, and two steam drums for the 1/2 core model.

The heat structures modeled include the fuel pins and carrier rod, pressure tube and surrounding graphite, and the inlet and outlet connector piping walls. No heat structures are modeled at this time for the steam separator. The affected tube for both models contains two fuel pin heat structures that represent an equivalent of 6 and 12 fuel pins lumped together to represent the 18 fuel pins per rod bundle. This allows radial power peaking to be modeled for the 6 inner and 12 outer fuel rings of the fuel bundle. The unaffected tubes are modeled with a single heat structure representing an equivalent of 18 fuel pins lumped together.

For the affected core, the RELAP5/MOD3 radiative heat transfer model is used. Radiative heat transfer between the inner fuel ring, outer fuel ring, carrier rod, and tube wall is modeled. Appropriate view factors were calculated for each heat structure component. Preliminary calculations for the tube blockage event were made to investigate the surface emissivity for the fuel cladding and tube wall. Emissivity values of 0.5, 0.6 and 0.7 were evaluated. This range was considered to be typical for Zr (the cladding and tube wall material). An average value of 0.6 was chosen for the calculations presented here, as the preliminary results did not show a strong dependence over this range of emissivity.

The 1/2 core model was developed to investigate low flow induced oscillation (Figure 2). The model contains 4 core regions, three within the affected DGH representing 44 tubes, and one for the balance of the core. The three affected tube regions were defined as 4 high power tubes (ranging from 2.2 MW to 3.0 MW per tube), 18 medium power tubes (set at 2.2 MW per tube), and 22 low power tubes (set at 1.6 MW per tube). This distribution was based on previous work done at PNL for post-Chernobyl neutronics analysis [4].

The low flow condition for the affected DGH was simulated by defining a time dependent junction at the inlet to the DGH to provide the desired flow conditions. Total power for the 4 tube core region was set at a predetermined power for each case analyzed (2.2 MW, 2.4 MW, 2.6 MW, and 3.0 MW). The model was run to achieve a steady state solution for full power/full flow, and then flow reduced to the affected DGH slowly until the point of flow instability was seen. The point of flow instability was defined as an oscillation amplitude of +/-30%. Flow to the DGH was then held constant at the point of instability to observe the "stabilization" of the flow instability.

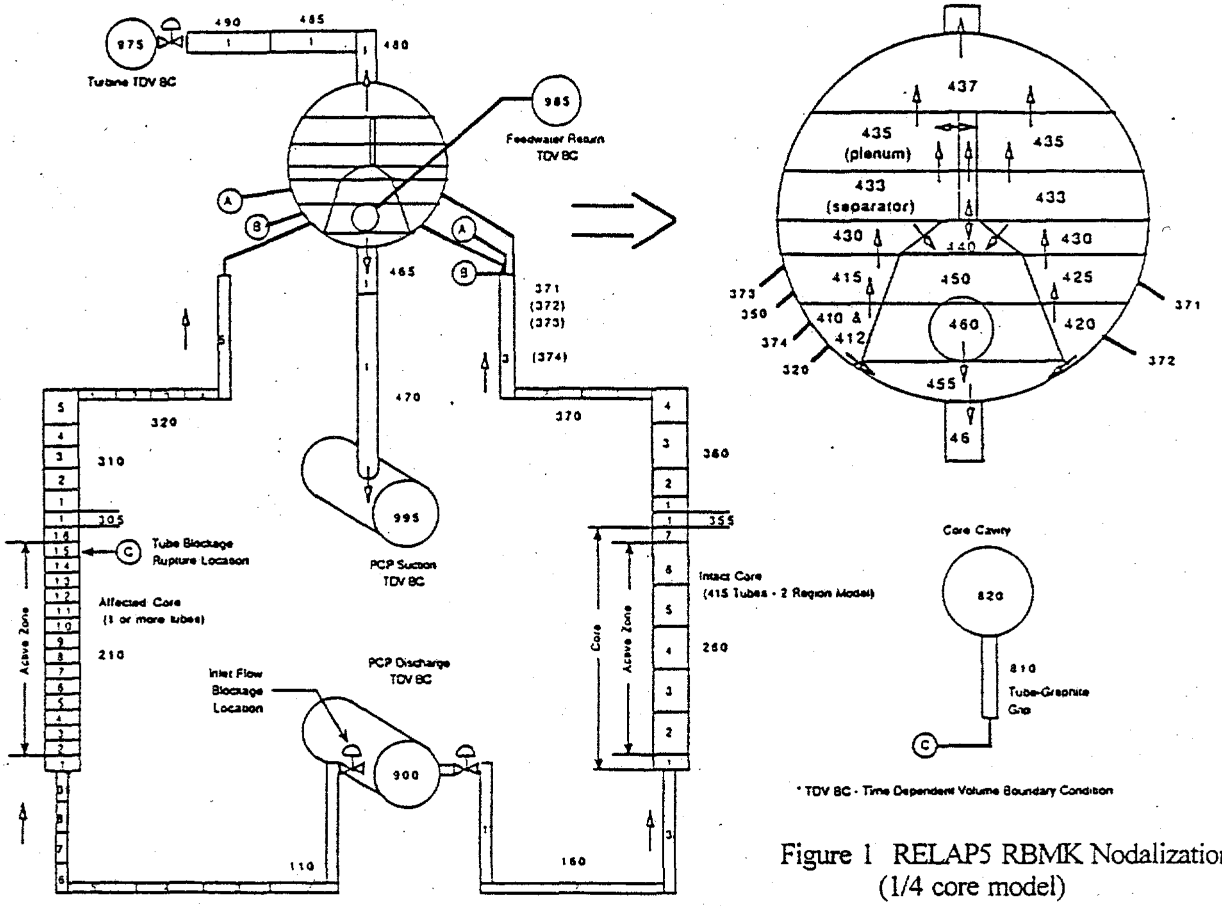

Figure 1 RELAP5 RBMK Nodalization
(1/4 core model)

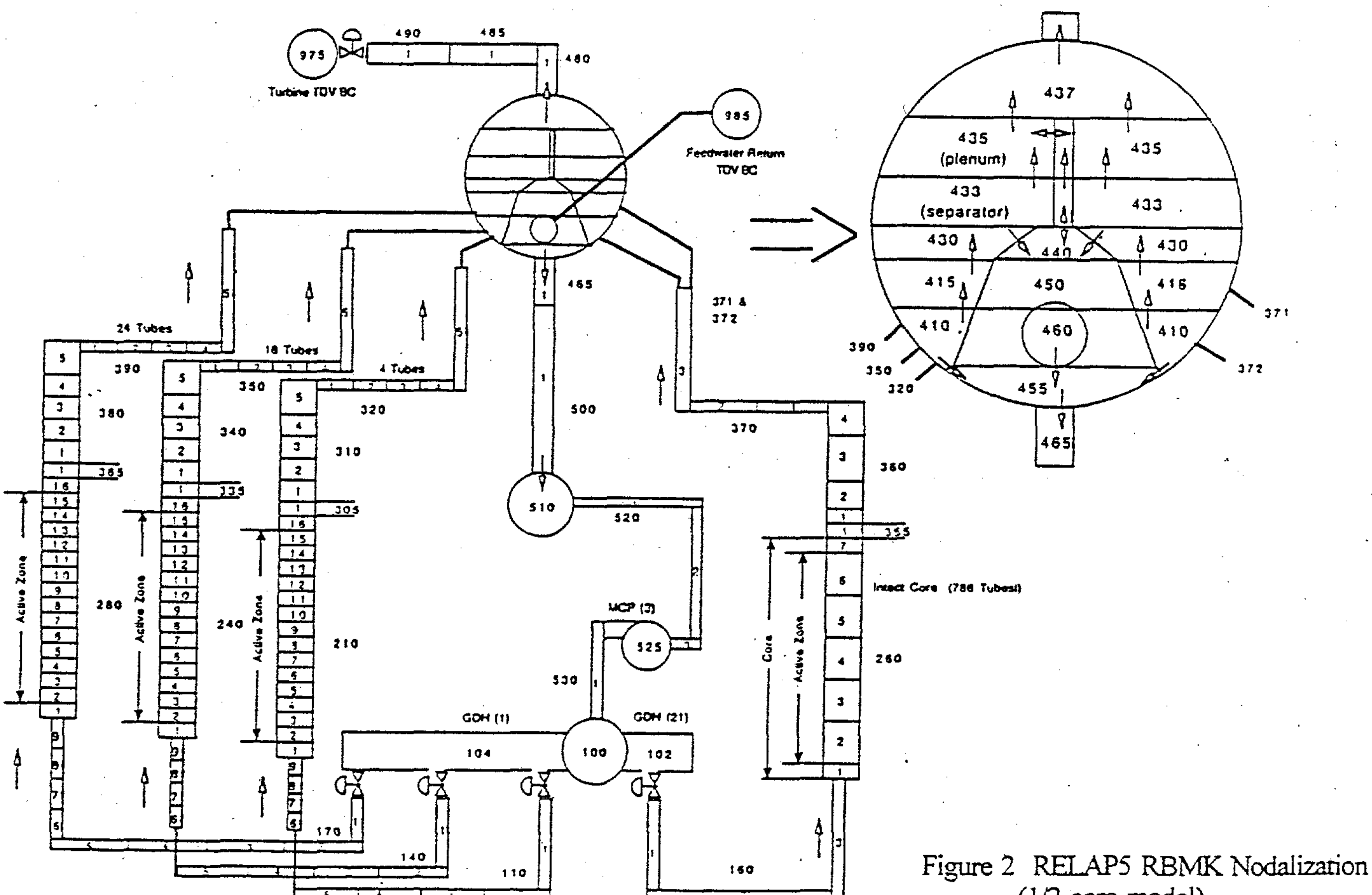

Figure 2 RELAP5 RBMK Nodalization
(1/2 core model)

CODE VERIFICATION

The first stage of verification includes calculation for steady-state parameters in the RBMK and some transient calculation against known experimental data. Limited results for code verification were presented at [5]. Results from the investigation of a tube blockage are presented.

The seconds stage of verification is for low flow instability. The RBMK calculations are compared against Japanese experimental data [6] for Type II threshold of flow instability. A sensitivity study of the RELAP5 model is included with the comparison.

RESULTS AND ANALYSIS

Tube Blockage

A series of tube blockage cases were evaluated with the 1/4 core model. Briefly, the Leningrad tube rupture was initiated by a failure of the inlet flow control valve to one of the core pressure tubes. It was estimated from a post-accident review, that this failure resulted in a flow reduction of the inlet flow to less than 10% of the initial tube flow. The flow reduction initiated a fuel temperature excursion and also elevated the pressure tube wall temperature due to radiative and convective heat transfer between the fuel and the pressure tube wall. Approximately 40-45 seconds after the inlet valve failure, the pressure tube ruptured in the upper core. A reactor shutdown was initiated 3.7 seconds after the pressure tube rupture due to the high core cavity pressure.

To evaluate this event, a parametric study was performed over the potential range of inlet flow blockage. Each calculation assumed an instantaneous reduction in the inlet valve flow area to simulate the valve failure of the Leningrad event. A total of five calculations were made, varying the inlet flow blockage to obtain a range of flow reduction between 2%-10% of the initial tube flow. Initial tube flow was 6.3 kg/s. Fuel cladding and pressure tube wall temperatures were evaluated every 0.5m with the 1/4 core model. It was assumed that pressure tube failure (rupture) would occur at an average tube wall temperature of 923K (650°C). This is the temperature at which tube softening is estimated to occur that then results in tube rupture. With 0.5m volume nodalizations for the 7m active fuel region, the tube failure location was calculated to be either at the 6.25m or 6.75m core elevation, depending upon the individual case. A plot of the time to pressure tube failure was made for the five calculations, and is shown in Figure 3a. A minimum time to tube rupture of approximately 42 seconds was calculated (compared to the estimated time of 40-45 seconds).

An evaluation was also made of the general transient response, with reactor shutdown, for one of the calculations. Figures 3b through 3d show the results from an approximate 6% flow blockage calculation. Initial tube flow for this calculation was reduced from 6.3 kg/s to 0.38 kg/s. In each of these figures, a null transient is run to ensure steady state conditions have been reached prior to initiation of the blockage (e.g., the blockage occurs at 30 seconds). Figure 3b is the pressure tube inlet flow response. Figure 3c is the fuel cladding temperature response, and Figure 3d the pressure tube inner wall temperature response, for selected nodes. Refer to Figure 1 for the nodalization numbers. For this calculation, the tube was estimated to rupture 42 seconds after initiation of the blockage. A reactor shutdown was initiated 3.7 seconds after the rupture, resulting in the eventual quenching of the fuel cladding and pressure tube wall. Peak cladding temperature was calculated to be 1490K (1217°C) for this case. The general responses for this transient appear to be physically consistent.

Blockage of DGH

Low flow, high power instabilities were investigated using the 1/2 core model shown in Figure 2. The instability is initiated by reducing flow to the affected DGH, using a time dependent junction (simulating a partial blockage), while maintaining constant power. A nodalization and time step size sensitivity study was also performed. The results of the sensitivity study are presented first.

Sensitivity Study

Three areas of modeling sensitivity were investigated. These were core nodalization, outlet (steam) pipe nodalization, and time step size. The core nodalization study investigated three fuel region nodalizations; 7, 14 and 28 axial fuel nodes. The steam pipe nodalization study investigated three steam outlet pipe nodalizations; 2, 5 and 10 steam pipe nodes. The time step study was performed for three different time steps sizes; 2ms, 10ms and 12.5ms, for two different core nodalizations, 7 and 14 fuel region nodes. For the two nodalization studies, the time step size used was 12.5ms. This time step size is the inherent RELAP5 Courant limit for the model.

The nodalization study was performed by initializing the model with a 60 second null (steady state) transient, then reducing flow to the affected DGH from 276.5kg/s to 50kg/sec between 60 and 560 seconds. The 50kg/s flow is then maintained constant from 560 to 660 seconds to observe the flow instability. The time step study was performed by initializing the model with a 60 second null transient, then reducing flow to the affected DGH from

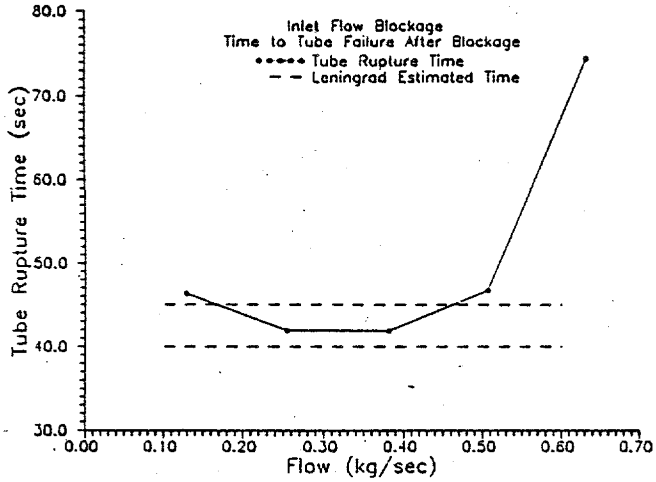

Figure 3a Time to Failure

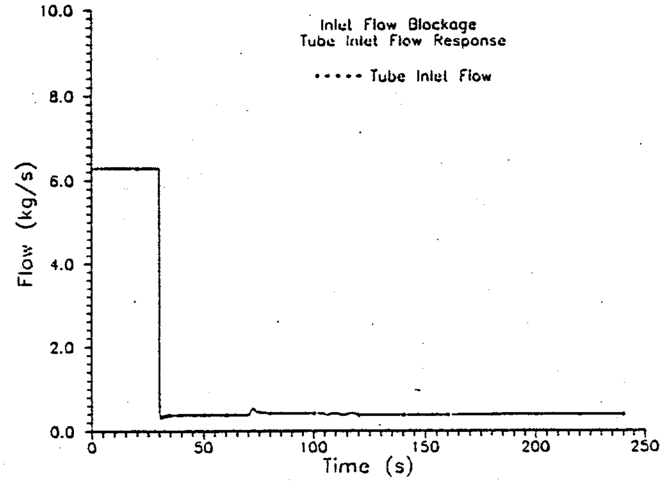

Figure 3b Inlet Flow

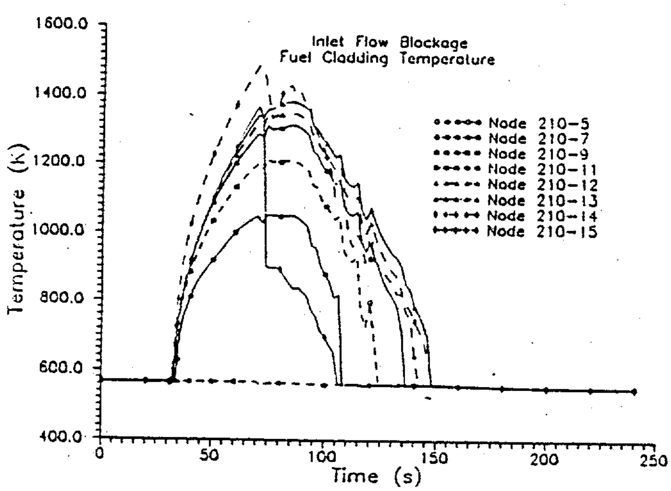

Figure 3c Cladding Temperature

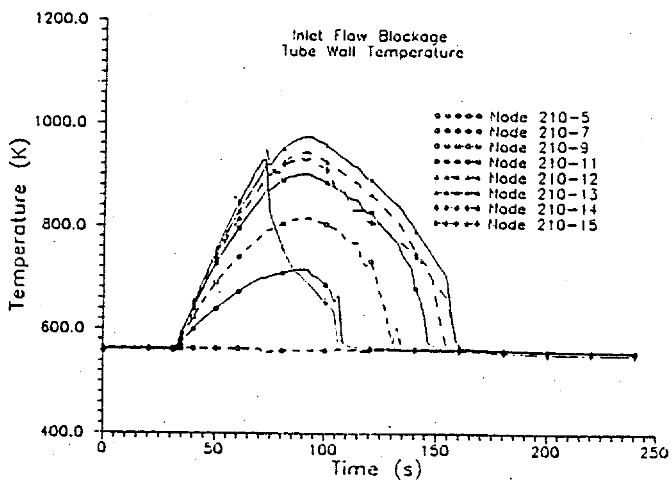

Figure 3d Tube Wall Temperature

276.5kg/s to 60kg/sec between 60 and 120 seconds. The 60kg/s flow is then maintained constant from 120 to 180 seconds to observe the flow instability (a flow of 60kg/s was chosen as this was closer to the point of instability for the DGH than 50kg/s).

The core region nodalization study was performed for three noding schemes; 7, 14 and 28 fuel region nodes. The results are shown in Figures 4a and 4b. The 14 and 28 fuel node results behave very similarly. They exhibit initiation of flow instability at very nearly the same flow, and although they differ slightly during the first 10 seconds of instability (Figure 4a, from 450 to 460 seconds), once the instability has reached a stable period they maintain similar frequencies of oscillation (although off-set slightly). The 7 node results, however, show a significantly lower point of instability initiation (Figure 4a) and frequency of flow oscillation (Figure 4b). The 7 and 14 node results do show similar amplitudes of oscillation, with the 28 node results showing a larger amplitude.

The steam pipe nodalization study was performed for three noding schemes; 2, 5 and 10 steam pipe nodes. The results are shown in Figure 5b. The results for the 5 and 10 steam pipe nodes behave very similarly. They exhibit similar initiation of flow instability and period of oscillation, differing in amplitude of oscillation only slightly during the first 10 seconds of instability (Figure 5, from 450 to 460 seconds). Once the instability has reached a stable period, they maintain similar flow oscillation amplitude and frequency. The 7 node results, however, show a significantly lower point of instability initiation, and frequency of flow oscillation. All three cases show similar amplitudes of oscillation.

The time step sensitivity study was performed using the 7 and 14 node fuel region models (with 5 steam pipe nodes), and three different time step sizes for each; 12.5ms, 10ms, and 2ms. Figures 6a and 6b compare the time step study for the 7 and 14 node fuel regions, respectively. The 7 node model shows minor deviations between the 12.5ms and 10ms, with more significant deviations in the period of oscillation for 2ms. The 14 node model shows excellent similarity for all three time step sizes.

Blockage Calculation

The base nodalization for the DGH partial blockage was for a 14 node fuel region, a 5 node steam pipe region, and a 12.5ms time step size. The instability study was performed by initializing the model with a 60 second null transient, then reducing flow to the affected DGH from 276.5kg/s to just above the point of flow instability in 20 seconds. This point was determined with preliminary calculations for each case evaluated. The DGH flow was then slowly reduced over 40 seconds to point of instability, and then maintained constant. Four different high tube powers were evaluated; 2.2MW, 2.4MW, 2.6MW, and 3.0MW.

The results of the calculations indicate that the period of oscillation for the high power tube varied from 3.1s to 2.6s, over the power range of 2.2MW to 3.0MW. This is shown in Figure 7a. The amplitude of the flow oscillation for the high powered tube varied from +100% to -150% of the tube average flow (based on the "steady state" flow just prior to initiation of the flow instability). Figures 7b, 7c and 7d present the results for one of the cases evaluated, a tube power of 2.4MW. The lower power core regions of the affected DGH experienced the same period of oscillation, but with lower amplitude. They also did not experience reverse flow. In addition, the lowest powered core region experienced flow oscillations of smaller amplitude than the medium powered core region.

The amplitude of oscillation was referenced to the inlet flow of the fuel region, Figure 7b. In the upper fuel regions and outlet connector, the amplitude of the flow oscillation was dissipated in the upper regions of the core.

The fuel cladding and tube wall temperatures were monitored for three core elevations; the lower core, mid-core, and upper core (Figures 7c and 7d). The magnitude amplitude of the cladding temperature oscillation varied from +/-40 to +/-70K over the range of tube powers from 2.2 MW to 3.0MW, respectively. In the lower core region (node 3), temperature oscillations show alternation of wet and post-dry-out zones. In the upper core regions, where post-dryout has already occurred, temperature oscillations are due to flow and heat transfer coefficient changes. The amplitude of the tube wall temperature oscillation varied from +/-10 to +/-20K over the same range of power. Although the calculations were run long enough to produce a "stable" flow oscillation, the cladding and tube wall temperatures oscillations had not yet reached an "equilibrium" condition. For the highest power analyzed, 3.0MW, the tube wall temperature had nearly reached an "equilibrium," averaging approximately 805K, with an oscillation amplitude of +/-20K. The critical temperature for the RBMK pressure tube for tube rupture has been determined to be approximately 923K (650^{0}C). Additional calculations are needed to evaluate the potential for tube rupture. Cladding temperature is far below the critical temperature for oxidation (1473K).

The results of the calculation clearly indicate that dryout in the upper regions of the core will occur prior to oscillation of the cladding temperature. Cladding temperature rises slowly in the upper core after initiation of the flow instability, then temperature rises sharply at the dryout point (Figure 7c) and reaches a new "equilibrium" temperature (the critical heat flux of the second mode) that continues to slowly rise. The cladding temperature

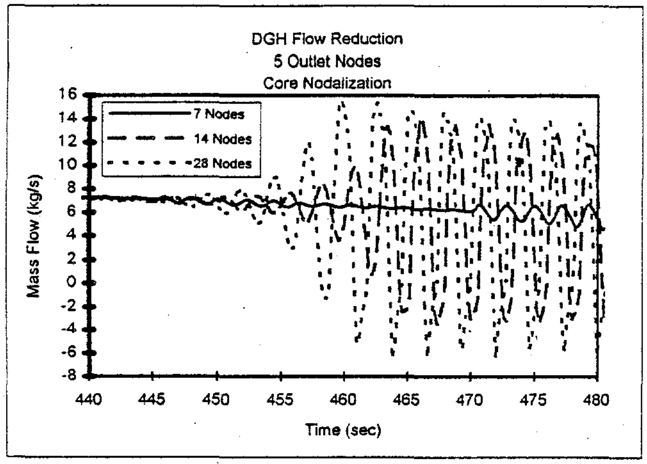

Figure 4a Core Nodalization

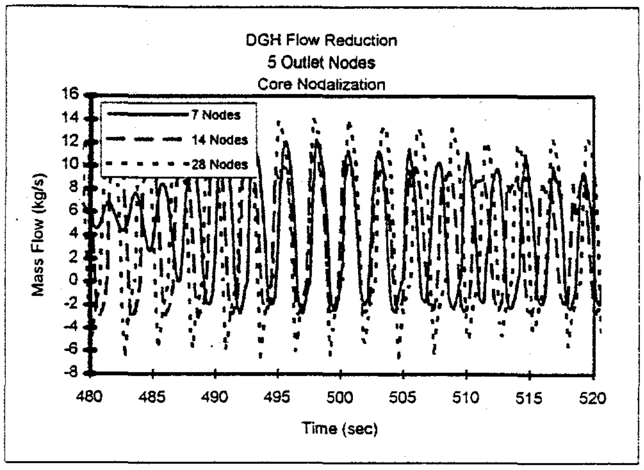

Figure 4b Core Nodalization

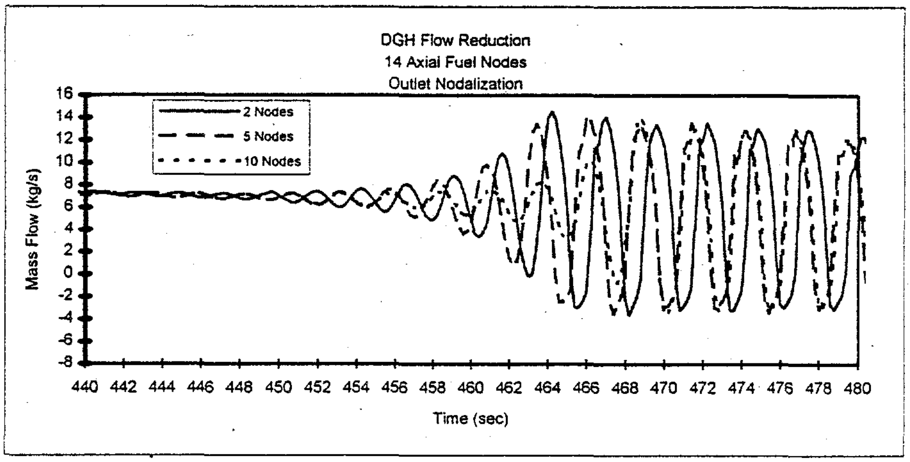

Figure 5 Outlet Nodalization

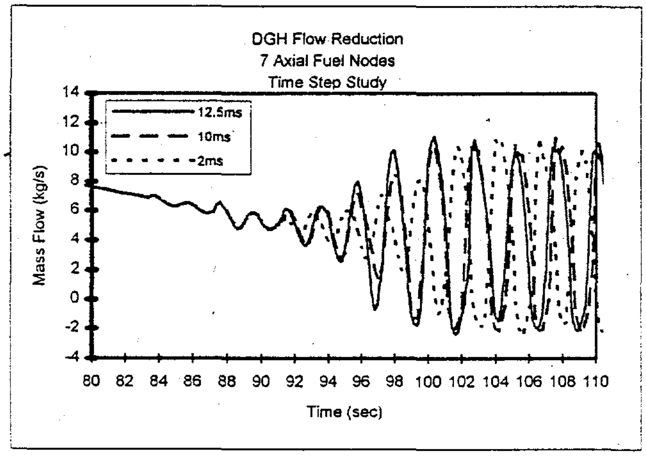

Figure 6a Time Step - 7 Fuel Nodes

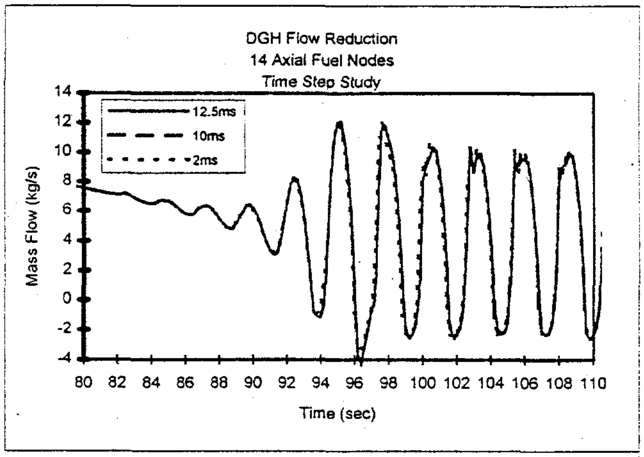

Figure 6b Time Step - 14 Fuel Nodes

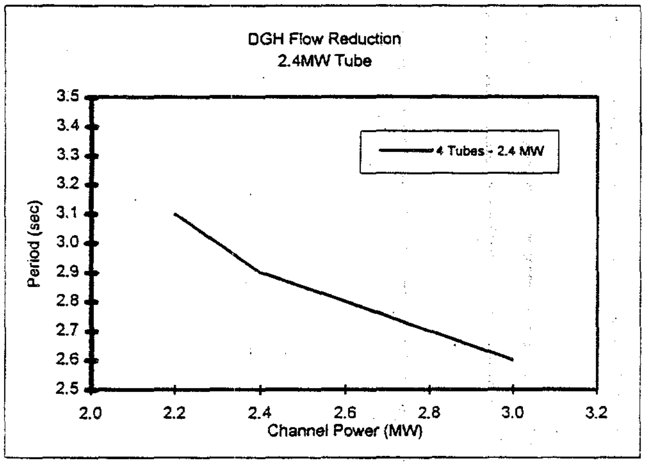

Figure 7a Period of Oscillation

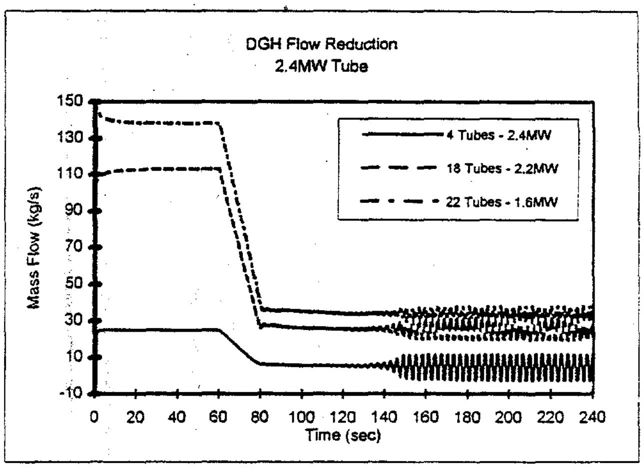

Figure 7b Tube Inlet Flow

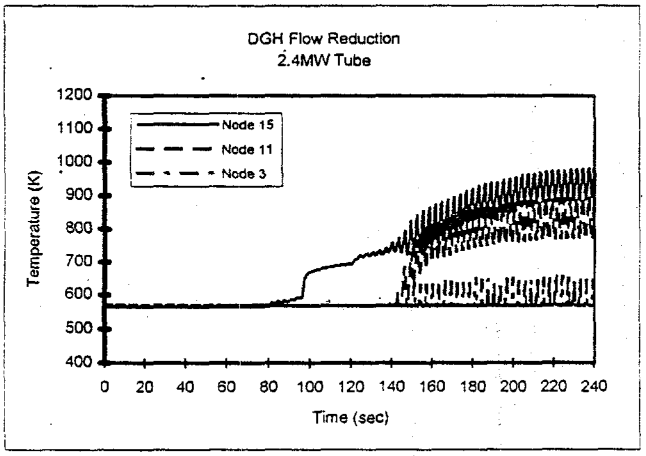

Figure 7c Cladding Temperature

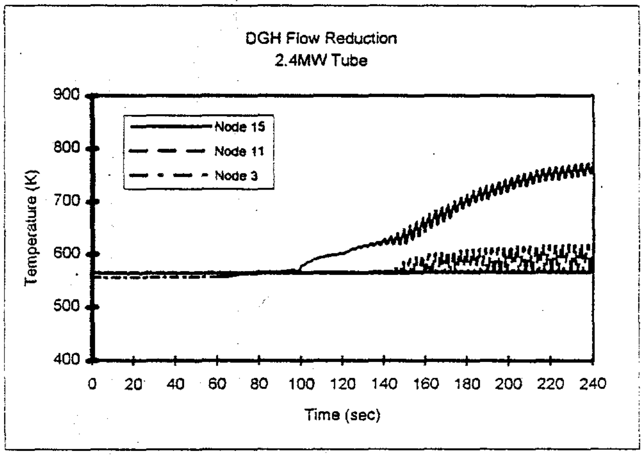

Figure 7d Tube Wall Temperature

oscillation is induced by the continued flow oscillation and moving of the boundary between the dry region and liquid.

The threshold of flow instability was calculated for each of the different powers for the high powered tube. These were compared to the data presented in Figure 8, Mochizuki [6]. The calculated threshold for the RBMK-1000 model appears to be in good agreement with this data. The data presented in Figure 8 suggest that for a DGH with high powered tubes, Type II instability is reached if flow is reduced below 1 to 2 kg/s, over the power range of 2.2 to 3.0 MW, respectively. These calculations were made for a limited power-flow range, and it is necessary to continue the analysis for flows less than 1 kg/s and powers less than 2.2 MW.

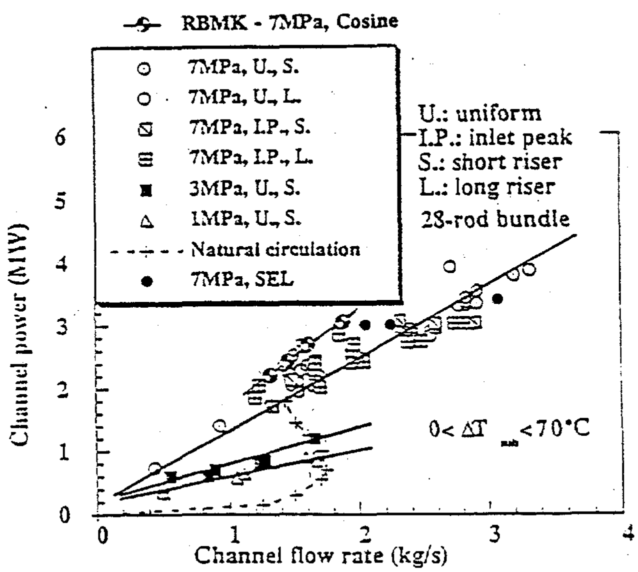

Figure 8 Type II Instability Threshold

CONCLUSIONS

Results of a single tube blockage show good agreement with the available data for the Leningrad tube rupture event. The model was able to reasonably predict the time of tube wall failure for the expected flow blockage. Comparison of the threshold of Type II flow instability shows reasonable agreement over the range of RBMK tube power investigated, and can potentially be used for safety analyses of the DGH blockage events. Modeling sensitivity studies indicate the instability analysis results were not sensitive to the nodalization scheme and time step sizes used. This was for a 14 node fuel region, 5 node outlet (steam) pipe region, and a time step size of 12.5ms. For this nodalization, there was little sensitivity to time step between 2ms and 12.5ms. The results do indicate that fewer than these number of nodes in these two regions can significantly effect the results.

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