# SIMULATION OF DARLINGTON LOSS OF FLOW EVENT

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## SUMMARY

In this paper, the validation of several physical models (pump rundown characteristics, stepback program, bleed condenser and pressurizer) using the single heat transport pump trip data from the Darlington NGS event on September 24, 1993 is briefly described. Emphasis is placed on the sensitivity study of important physical models rather than on a detailed description of code predictions. The predicted results show that the input data and the models implemented in the TUF code are appropriate for operational support analysis.

## 1. INTRODUCTION

Large thermal-hydraulic system codes are widely used to perform operational support and safety analyses of nuclear power plants. Evaluation of the capabilities of these codes is accomplished by comparing code predictions with the measured experimental data obtained from various test facilities. In general, most plant models in system analysis codes have been qualified to a certain extent based on small-scale test data. Using plant transient data to qualify the plant model has been emphasised in the TUF code development since each plant has unique characteristics that are important in operational support analysis.

In recent years, some attempts have been made to establish methodologies to evaluate the accuracy and the uncertainty of code predictions to provide judgement on the acceptability of the codes for certain applications. Sensitivity analysis is a key component in the uncertainty methodology. The most sensitive parameters or the most relevant models have to be identified in sensitivity analysis. Also the relevant input data that are not well defined in experiments or plant data should be assessed before sensitivity studies are considered. For example, the bleed condenser control valve position is strongly dependent on the input valve discharge coefficient for the level control valves of the bleed condenser. This particular value can significantly influence the predicted liquid level transient in the bleed condenser. Also, the enthalpies in the dead-end piping require special attention since they are not available in the plant data. One of the purposes of this paper is to identify the relevant parameters for the uncertainty studies of the loss of flow event.

Tripping of the primary heat transport (HT) pumps in CANDU reactors can be caused by a loss of power to the Class IV 13.8 kV bus or by a trip signal due to the activation of electrical protection. The trip coverage for the Shut Down Systems (SDS1 and SDS2) for a single HT pump trip event is studied in detail in safety analyses. To support such analyses, data from the loss of a single HT pump tests in CANDU power plants (for example the pump tests at Point Lepreau and Darlington NGS) and the single pump trip event (for example the single pump trip event in 1993 at Darlington Unit 4) are considered. Those data provide vital information about the plant characteristics that are important in the plant support analyses.

Several sets of plant transient data have been used in the suite of integration tests for the TUF code development. In this paper, the validation of several physical models (pump rundown characteristics, stepback program, bleed condenser and pressurizer) using the single HT pump trip data for the Darlington NGS event on September 24, 1993 is described. The results presented here are based on the development test. Emphasis is placed on sensitivity studies of important physical models rather than on detailed descriptions of code predictions.

## 2. SIMULATION OF THE EVENT

### Event Description

Prior to the event, Unit 4 was operating at 100%FP. While the electrical panels associated with the electrical protection and control circuits for HT pump #2 were being cleaned and sanded, pump #2 was tripped due to the activation of electrical protection. A unit stepback took place to approximately 40 %FP. During the stepback, the MCA rod #4 did not respond and remained out of the reactor core. The turbine/generator was runback to unit service load with CSDVs controlling boiler pressure (reactor operation was in the alternate mode). After the event, the HT pressure dropped to 8.4 MPa and it took a long time to recover via the pressurizer heaters. Once the HT pressure recovered, reactor power rose to approximately 48 %FP. After the cause of the motor trip was determined, the pump was restarted without incident. Following the unit recovery, the reactor power increased to about 60 %FP as NOP margins permitted.

The initial transient (about 30 seconds) provides useful information for the validation of reactor system codes that are used in the studies of trip coverage. The station measurements for this event are extracted from the DCC data by using the plant data distribution system. The parameters of interest during this event (ROH pressure, surge tank pressure and level, bleed condenser pressure and level) were recorded every 6 seconds. The neutron power transient was recorded every 2 seconds. Although the recorded data are not adequate to carefully examine the details of the plant transient, they can be used to validate the reactor system codes in the following areas: (1) stepback program (neutron power), (2) pump rundown characteristics (ROH pressure), and (3) bleed condenser and pressurizer modelling (pressures and levels).

After the pump trip, the following transient results are observed: (1) Reactor stepback occurred with the neutron power reducing to approximately 40 %FP where the stepback end-point power for a single pump trip was 60%FP. (2) Reactor trip did not occur. (3) Pressure at the ROH just upstream of the tripped pump built up. (4) Liquid relief valves did not open which indicates that the maximum ROH pressure was below 10.55 MPa. (5) Pressure dips occurred in all ROHs. (6) Pressurizer steam bleed valves were opened which resulted in a pressurization process in the bleed condenser. (7) System pressure continued dropping to about 8.4 MPa before it recovered via the pressurizer heaters.

The overall thermal-hydraulic phenomena of this event are similar to those observed in the Point Lepreau pump trip test in 1986. However, there are several differences: (1) The reactor is tripped due to the low-pressure signal at 13 seconds in the Point Lepreau test. (2) The pressure at the ROH upstream of the tripped pump increases by about 200 kPa in Point Lepreau but 500 kPa in Darlington. (3) The timing for the ROH pressure dips is about 10 seconds after the pump trip in Point Lepreau but 6 seconds after the pump trip in Darlington. The magnitude of the pressure dips is about 500 kPa in Point Lepreau and 200 kPa in Darlington in the unaffected ROHs. These differences result from the fact that the void in the HT system under normal operating condition is higher in Point Lepreau NGS.

This event is one of the data sets used in the integration tests for the TUF code development (prior to any version release). The main physical parameters considered in this event are in the modelling of the bleed condenser and pressurizer.

### Results of Simulation

The full circuit of Darlington Unit 4 (a total of 505 nodes and 585 links), consisting of two figure-of-eight HT loops, feed and bleed system, D2O purification system, secondary side system and emergency coolant injection system, was simulated. The reactor core was represented by four core passes, each quadrant representing 120 channels. This circuit model is identical to that used in the previous plant simulations (loss of Class IV power, reactor trip, load rejection and liquid relief valve (LRV) test). Figure 1 shows the location of the tripped pump (P2).

There are three modifications in the input data set in the present simulation: (1) The valve discharge coefficient for the bleed condenser level control valve is 3.5.E-3 m**3/s/(kPa**0.5). (2) The pump reference torque divided by the inertia is 20 (l/s**2). (3) There is a delay of one sampling time in the absorber clutch closing. The first modification is based on the results obtained from the previous LRV test. As a result, the position of the level control valve under the normal operating condition changes from 0.29 to 0.15. The second modification is based on the result calculated from the Darlington four pumps trip test at 0%FP hot condition. The last modification is based on the actual plant power level.

After the pump trip at time zero, the reactor stepback is initiated at 0.5 second. The stepback routine initiates the MCA rod drop. At 1.75 seconds, the projected neutron power is below 60 %FP (or 1.5 seconds if all four MCA rods are available, 2 seconds for one bank and 2.5 seconds for one MCA rod). After a delay of one sampling time, the clutch closing is initiated at 2 seconds and the rods stop at 2.43 seconds. The predicted neutron power is compared with the plant data in Figure 2. It indicates that the assumption of a time delay of one sampling time in the MCA clutch closing is reasonable.

Following the pump trip, the coolant flow through the NE quadrant decreases, causing the upstream ROH (HD3) pressure to rise. At 10 seconds, the flow rate in the core of the NE quadrant decreases to 74 % of the nominal flow rate. It then reduces to 70% at time 30 seconds. The increase in HD3 pressure causes the pressurizer steam bleed valves to open early in the transient resulting in a pressurization process in the bleed condenser. The pressure transients at all ROHs are plotted in Figure 3. The predicted maximum ROH pressure is 10.52 MPa at 11.6 seconds. Pressure dips that were observed in all ROHs at about 6 seconds are predicted at 10 seconds. After these dips, pressures recover at about 12 seconds and then continue to decrease due to the power reduction. Except at the transient time of 6 seconds, the predicted pressures agree well with the plant data. Several attempts (for example by including the shutdown cooling piping in the circuit or using different Fouling heat transfer coefficients in the steam generators) in trying to understand the physics that causes a faster pressure dip in ROHs observed at 6 seconds have been made but unsuccessful. Probably the initial plant condition at the time of pump failure may slightly deviate from the true steady state condition.

The predicted pressure and water level transients in the pressurizer are compared with the plant data in Figure 4. The corresponding results for the bleed condenser are shown in Figure 5. Excellent agreement is observed between the predictions and the plant data. This indicates that the bleed condenser and presssurizer have been properly modelled in the code.

## 3. SENSITIVITY STUDIES OF PHYSICAL MODELS

The advantage of using a mild plant transient in examining the physical parameters or models is in the identification of sensitivity parameters. Also, the variations in the results of using different sensitivity parameters are much larger than the cases with severe transients.

Several physical parameters or models that possibly can affect the prediction of this event can be identified as follows: pump rundown rate, heat removal rate and two-fluid effects. They are briefly described below.

### Pump Rundown Characteristics

Four quantities are involved in the pump characteristics: the dynamic head, the discharge flow rate, the shaft torque and the rotational speed. Two of these quantities are considered independent. Two basic assumptions are usually made in the pump model: steady state characteristics hold for unsteady state situations, and the homologous relationships are valid. In addition to having the proper data for pump characteristics, information is also needed for the moment of inertia of the pump impeller and the entrained liquid, plus that of the motor rotor, shaft and couplings. These data are normally obtained from the manufacturers. The Darlington HT pump characteristics have been revised for the pump head and torque curves based on the data from the Darlington HT pump tests, the Bingham Williamette pump characteristics and the Bruce pump locked and turbing rotor tests. There are four curves in the homologous plot for the head and also four for the torque. In the model, each curve is represented by a polynominal approximation.

For a pump rundown, two equations are solved simultaneously for each time step: the head balance equation across the pump and the torque-angular deceleration equation for rotating masses. In the HT pump rundown model, the pump torque is written as a quadratic function in pump speed. It consists of hydraulic, dynamic frictional and static frictional terms. The hydraulic torque is evaluated using the homologous torque curves. It is a function of the volumetric flow, void fraction, coolant density, and pump angular speed. For Darlington NGS, the coefficients of each term are estimated, together with the pump inertia, from the pump rundown tests performed at the Kipling Pump Test Complex in 1985. Also the rundown model has been verified against the Darlington pump test data in 1992. Figure 6 shows the comparison between the predicted pump speed and the data from the Darlington four pumps trip test at 0%FP hot condition on May 31, 1992. These results provide some confidence in the pump model used in the operational support analysis. Nevertheless, uncertainty in the estimated parameters for pump characteristics may still exist. The main effect of the pump rundown rate on the HT system behavior is on the upstream ROH pressure. In the sensitivity study, the pump rundown equation can be written as

$$\frac{d\omega}{dt} = -C\tau \qquad (1)$$

where $\omega$ is the pump speed, $\tau$ is the total pump torque divided by the pump inertia and C is a sensitivity parameter. The estimated maximum uncertainty in the pump rundown rate is about 20%. This value is estimated from the comparison of the pump rundown rates between the ANC pump model and the pump model developed at OPG.

The influence of the pump rundown rate on the system behavior can be illustrated in the pressure transient of the ROH upstream of the rundown pump. Figure 7 shows the HD3 pressure transients with three C values (1, 0.8 and 1.2). It illustrates that the pressure built-up magnitude is a function of the pump rundown rate. The faster the pump rundown rate, the higher the pressure builds up in the ROH just upstream of the tripped pump.

**Reactor Stepback Program**

Unlike the setback routine, the stepback routine is a separate computer program, entirely independent of the reactor regulator system program for CANDU reactors (except Pickering A NGS). This separation provides immunity to malfunctions in the regulating programs and makes stepback a credible backup program for the purpose of preventing loss of regulation. Reactor stepbacks are designed for major upsets and must be used only when necessary. For this reason, both control computers must request a stepback before the clutches are disengaged. This reduces the probability of spurious stepbacks. The stepback program monitors the plant operating condition and takes fast action to reduce the reactor power if necessary. These fast power reductions are performed by gravity drop of four MCA rods into the core. In most reactor control systems some form of electromagnetic clutch is installed between the control rod mechanism driving motor and the rod withdrawal mechanism. In the event of the power supply to the electromagnetic clutch being cut off, the driving motor is disconnected and the control rod is free to fall. While the rods are dropping, the stepback routine scans the reactor flux power and re-energizes the absorber clutches when the stepback conditions clear or when the projected reactor power is dropped below its end-point value. In the reactor system codes, simulation of the motion of each MCA rod consists of three modes of operation: drive mode, stepback mode, and brake mode. In the drive mode, the clutch is energized under the reactor regulator system control. The total reactivity for MCA rods is about -9.5 mk at nominal equilibrium burn-up conditions for Darlington NGS. The reactivity rate when all MCA rods are falling under gravity is approximately -2 mk per second.

Typical unit parameters that trigger stepback for CANDU reactors are: reactor trip, turbine trip, load rejection, HT pump trip, HT high pressure, high reactor power or rate, high zone flux, and loss of booster cooling (only for Bruce NGS). The stepback program has been activated many times in each CANDU unit during operation. In the event of reactor trip, the stepback program does not significantly affect the power reduction since the total reactivity of the MCA rods is much lower than that for the shut-off rods. Nevertheless, reactor power still can be reduced to a low power level after dropping any one of the MCA rods.

The following program rules for reactor stepback are normally applied in the control computers: (1) Execute this routine every 0.25 seconds. (2) If a stepback is not in progress, execute this routine every 0.5 second (two sampling intervals). (3) Read signals from several ion chambers and flux detectors and find the median values of the rational log rate and neutron power. (4) Initiate stepbacks if conditions are met and open all clutch contacts. When stepback conditions occur the normal drive demand is overridden and the MCA rods are dropped from the existing positions. (5) Calculate the projected neutron power for the next sampling time. (6) When all the stepback conditions are cleared or the projected neutron power is below the end-point power, the clutch contacts are closed to stop the drop. Except for rule (3) where the bulk neutron power is used, these program rules have been emulated in the TUF code.

The pre-selected end-point power level for a turbine or single pump trip is about 50 or 60 %FP for CANDU reactors. However, it has been found from plant data that the actual end-point powers are much lower than the pre-set values due to the high zone flux signal. For examples, the end-point power was 53 %FP (pre-set value was 60 %FP) for the load rejection event in Darlington Unit 2 on September 25, 1995, 40 %FP (pre-set value was 60 %FP) in this event and 44 %FP (pre-set value was 50 %FP) for the single pump trip test done at Point-Lepreau NGS in August 1986. Unfortunately, the high zone flux stepback signal is not emulated in the code since the flux tilt can not be simulated in the point reactor model. As a result, to simulate those transients two approaches can be adopted in reactor system codes: either use the plant data as input data or modify the reactivity worths due to the MCA rods motion in the point reactor model. The second approach is used in the present simulation. To account for the lack of a high zone power signal (this signal is expected to occur when any one of the MCA rods is not available during the stepback action) in the stepback routine, an option to delay the clutch contact closing has been set up in the code. This option is only applicable to cases where plant data for the end-point power is available. In the sensitivity study, each MCA rod position can be written as

$$s = s_0 + \int_{t_0}^{t_1} v(t)\, dt + 0.25\, n\, v_1 + \int_{t_1 + 0.25n}^{t} v(t)\, dt \tag{2}$$

where s is the rod position, $t_o$ is the time when the rod drop is initiated, $t_1$ is the time when the projected power is below the end-point power, v is the rod speed and n is the sensitivity parameter related to the delay in clutch closing. The calculated end-point neutron power is strongly dependent on the timing to close the clutch. In the present simulation, a delay of one sampling time (0.25 second or n=1) for the clutch contact closing is assumed.

With a stepback end-point power of 60%FP, Figure 8 shows the neutron power transients for the cases with and without a delay in the clutch contact closing time (the MCA rod #4 is assumed not available). It indicates that a delay of one sampling time gives a reasonable explanation why the reactor power dropped to 40%FP in this event.

**Heat Removal Rate**

The physical properties for the fuel and the sheath are more or less well established. Also the coolant heat transfer coefficients under a high-flow and high-pressure condition used in the thermal-hydraulic codes have been verified against various experiments to some extent. The main uncertainty in the physical parameters of the fuel model is the gap heat transfer coefficient between the fuel and the sheath.

The initial stored energy and subsequent temperature distribution in the fuel during the transient depends on the fuel-to-sheath gap width and the corresponding gap conductance. The lower value of gap conductance results in a higher fuel surface temperature. After a period of reactor operation, the gap will contain a mixture of the original fill gas (helium) and fission product gases such as Xe and Kr. Also the fuel pellet will swell and crack and will actually come into contact with the sheath in many locations. In general, the gap heat transfer coefficient is the sum of the gap conductive and radiation heat transfer coefficients. The gap conductance is the reciprocal of gap resistance that consists of two parts: gas resistance and contact resistance. This gap conductance should take into account the deformation of the fuel and sheath due to thermal expansion and also due to the difference between gap pressure and coolant pressure outside the sheath (for example the Ross-Stoute gap conductance model in the ELESTRES code). Nevertheless, uncertainty still remains large in this particular value since it is very difficult to estimate the fraction of the contact area. The uncertainty in the gap heat transfer coefficient between the prediction and the measurement can be up to 100%.

In the present simulation, the value of 10 kW/(m**2 K) is used for the gap heat transfer coefficient. To examine the sensitivity of the gap heat transfer coefficient on the prediction, the case with the value estimated from the FACTAR code for each ring at the bundle close to the middle of channel is also simulated. The averaged value over all rings is about 22 kW/m**2/K. Figure 9 shows the comparison of the HD3 pressure transient. It shows that the effect of gap heat transfer coefficient on the transient is small and can be neglected in the operational support analysis.

Similarly, the variations in the predictions after using different sensitivity parameters (with reasonable constant factor or different correlation) for the wall heat transfer coefficients in fuel and boiler have been examined. The same conclusion can be drawn for the wall heat transfer coefficients in this simulation.

**Bleed Condenser and Pressurizer Models**

For the bleed condenser, the important physical parameters that can affect the transient are the steam condensation rate, the tube wall heat transfer rate and the level control valve characteristics. The steam condensation rate is a function of the operating conditions of spray and liquid relief flows, and the water level. When the water level is below the entrance of the liquid relief line, a droplet flow regime is used for the liquid relief flow. The reflux condensation heat transfer coefficient is used for the tube wall heat transfer in the steam region and a convection type correlation is used for the liquid region, depending on the location of the water level. The bleed condenser level control valve positions are calculated from the level controller and the bleed cooler temperature controller. The minimum output signal from these two controllers is used to set the demand valve position. For level control valves, the valve discharge coefficient plays a key role in determining the valve discharge flow and consequently the water level in the bleed condenser. The Darlington LRV test data has been used to qualify the bleed condenser model implemented in the code.

The pressurizer is modelled as a single control volume with distinct phase separation. The interfacial heat transfer rate is dependent on the spray flow rate, entrained bubbles and the operating conditions of heaters and steam bleed valves. The void fraction of entrained bubbles in the liquid region is calculated from the level swell model. The total energy of the heaters, which are located at the lower region of the pressurizer, is calculated from a first-order delay equation. The steam bleed valve demand lift is calculated based on the ROH pressure error. A deadband of 0.03 MPa is introduced into the error term to prevent simultaneous operation of the steam bleed valves and the heaters. The thermal-hydraulic model for the pressurizer implemented in the code has been validated against the NPD pressurizer tests and the Marviken pressurizer top blow-down test.

The main uncertainty in the modelling of the bleed condenser and the pressurizer is in the phase change rate. Since the pressures in the bleed condenser and the pressurizer are strongly dependent on the phase change rates, the maximum (thermal equilibrium) and minimum (adiabatic) phase change rates are examined in the sensitivity study. The following two models for the bleed condenser and the pressurizer are also considered:

$$\begin{aligned} \Gamma &= \Gamma_{eq} \quad (thermal\,equilibrium) \\ \Gamma &= 0 \quad\;\; (adiabatic) \end{aligned} \tag{3}$$

where $\Gamma$ is the vapour generation / condensation rate and $\Gamma_{eq}$ is the thermal equilibrium value.

The comparisons of the pressure transients in the pressurizer and the bleed condenser using various models are shown in

Figures 10 and 11, respectively. It indicates that the adiabatic model is not appropriate in this simulation.

**Two Fluid Effects**

The two-fluid effect in the thermal-hydraulic model varies with the coolant conditions. Usually it is expected that the two-fluid effect on the transients in operational support analysis will not be significant since the two phases are close to thermal equilibrium under a high-flow high-pressure condition. Unfortunately, this expectation may not be true during the initial transient period. The main two-fluid contribution in operational support analysis is in the vapour generation / condensation rate which subsequently affects the pressure transient. The model implemented in the TUF code has been verified by using different experimental data, for example the CWIT series, the RD-14 series and the condensation induced water hammer experiments at OPT. In the two-fluid model, the pressure transient can be expressed by

$$dp = \frac{\partial p}{\partial M} dM + \frac{\partial p}{\partial U} dU + \frac{\partial p}{\partial M_g} dM_g + \frac{\partial p}{\partial U_g} dU_g \tag{4}$$

where p is the pressure, $\partial p/\partial X$ are the pressure derivatives, M is the total mass, U is the total internal energy and the subscript g denotes the vapour phase. The vapour generation/condensation rate is used in the $M_g$ and $U_g$ equations. The one-fluid result can be obtained by using the following expressions:

$$\begin{aligned} d M_g &= [\, x - \frac{\partial x}{\partial u} u \,] \, dM + M \frac{\partial x}{\partial p} dp + \frac{\partial x}{\partial u} dU \\ d U_g &= u_g \, dM_g + M_g \frac{d u_{gs}}{dp} dp \end{aligned} \tag{5}$$

where u is the specific internal energy, $u_{gs}$ is the saturated vapour internal energy, x is the quality and the quality derivatives are obtained from the condition of thermal equilibrium. As a result, the pressure transient in the one-fluid model can be expressed as a function of change rates in total mass and total internal energy.

The pressure transients at the ROH just upstream of the tripped pump predicted by the one-fluid and two-fluid models are plotted in Figure 12. It shows that in this event, the magnitude of the pressure built-up in the affected ROH not only depends on the pump rundown rate but also on the predicted phase change rate during the initial transient period. The HD3 pressure reaches the LRV setpoint of 10.55 MPa at time 10.1 seconds in the one-fluid model, which was not observed in the plant data. The corresponding result for the other ROH in the affected loop is plotted in Figure 13. These results also show that except the initial transient (up to 12 seconds) the overall transient predicted by the one-fluid model still agrees well with the two-fluid result.

## 4. CONCLUSION

The single pump trip event at Darlington NGS on September 24, 1993 has been simulated using the TUF code. The predicted results have shown that the input data and the models implemented are appropriate for operational support analysis. It has been found that the pump rundown rate and the two-fluid effects are the important parameters in the sensitivity study of this event.

**ACKNOWLEDGEMENT**

We dedicate this paper to the memory of our distinguished colleague and friend Mr. Tung Toong, who passed away on January 8, 2000.

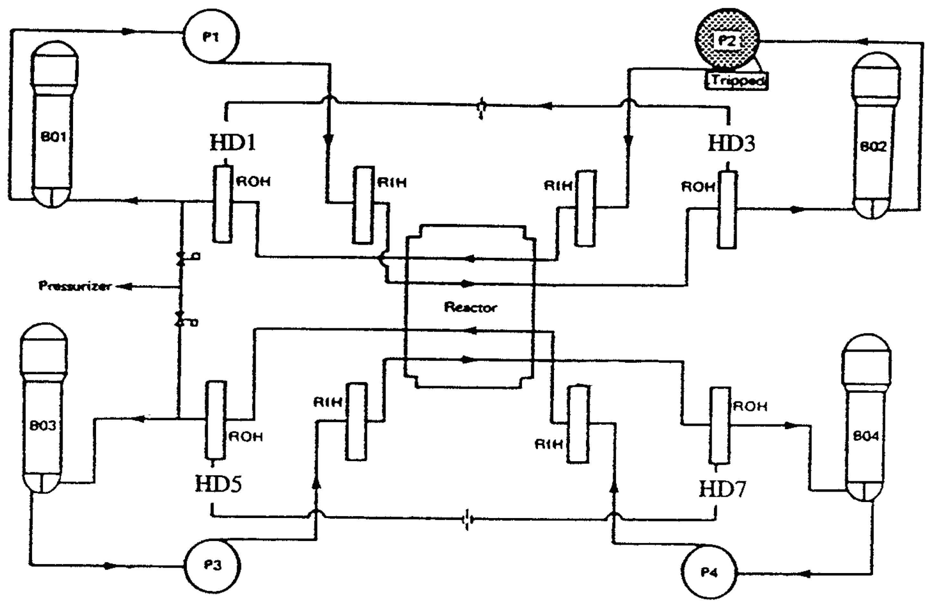



Figure 1. Location of tripped pump in the PHT circuit

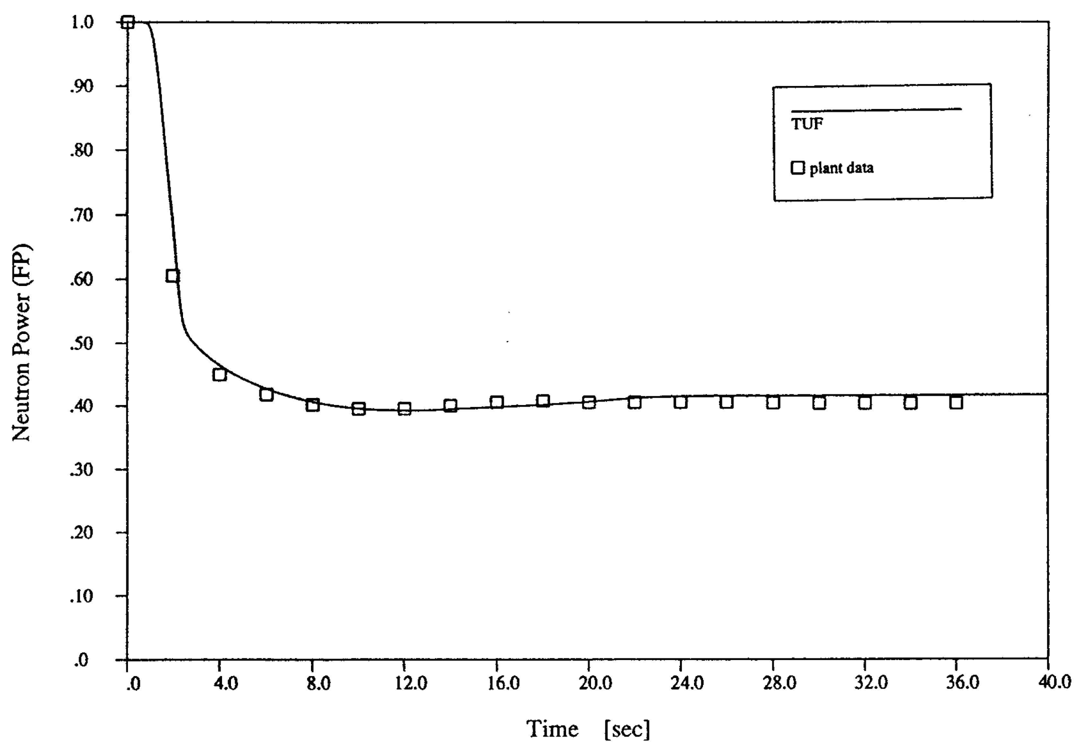



Figure 2. Comparison of neutron power transient with plant data

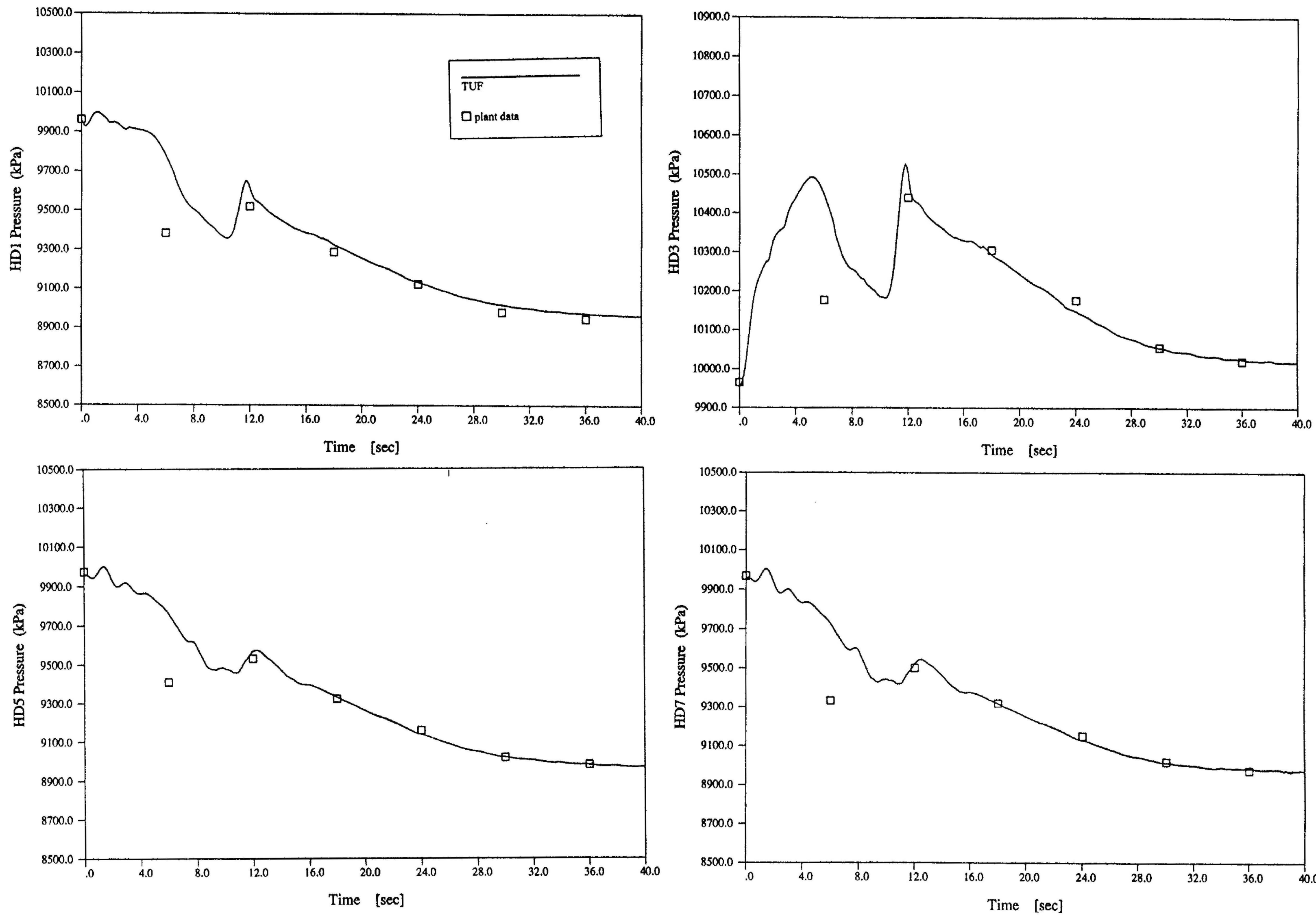
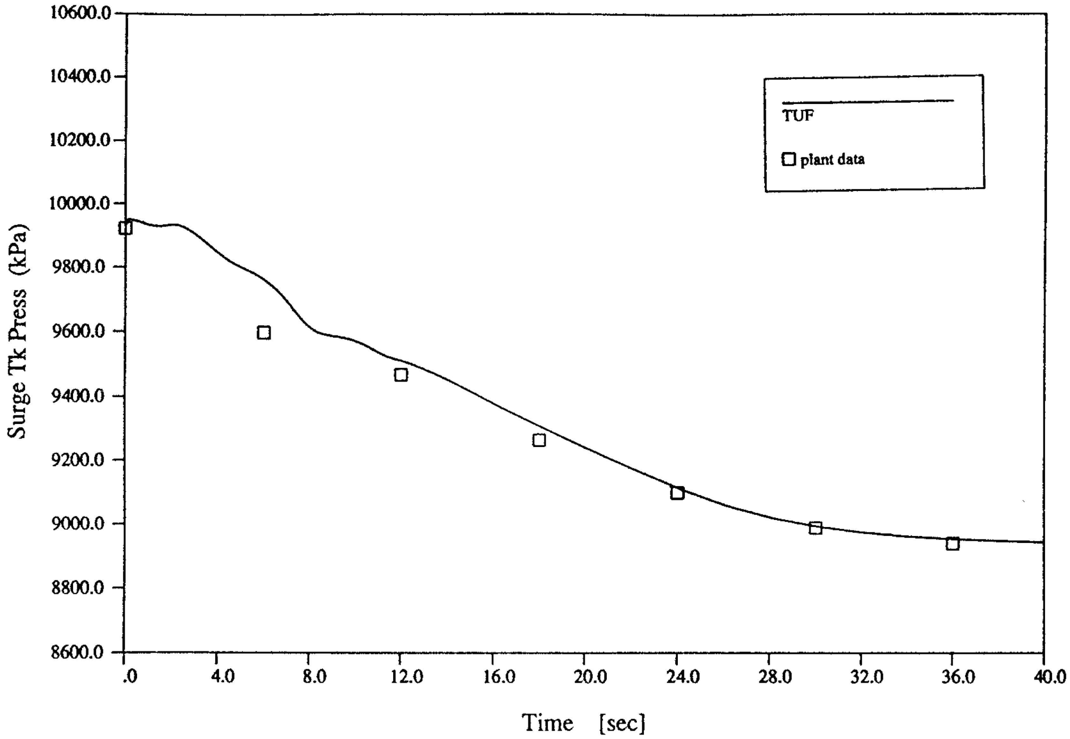



Figure 3. Comparisons of pressure transients at reactor outlet headers with plant data



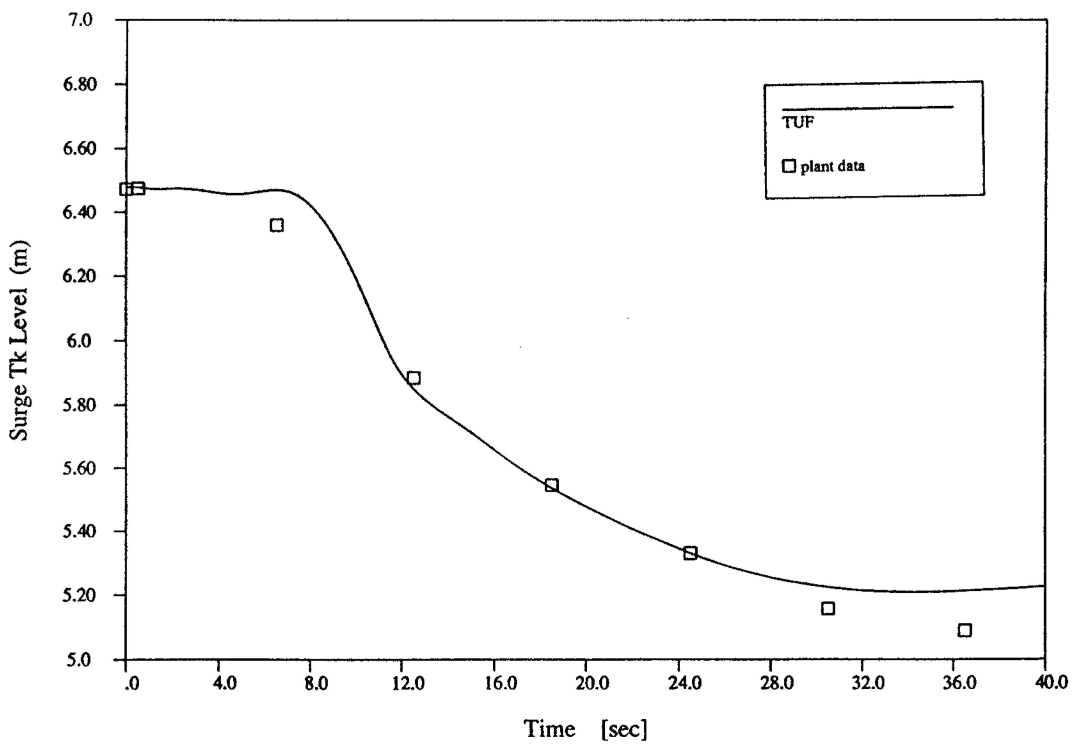
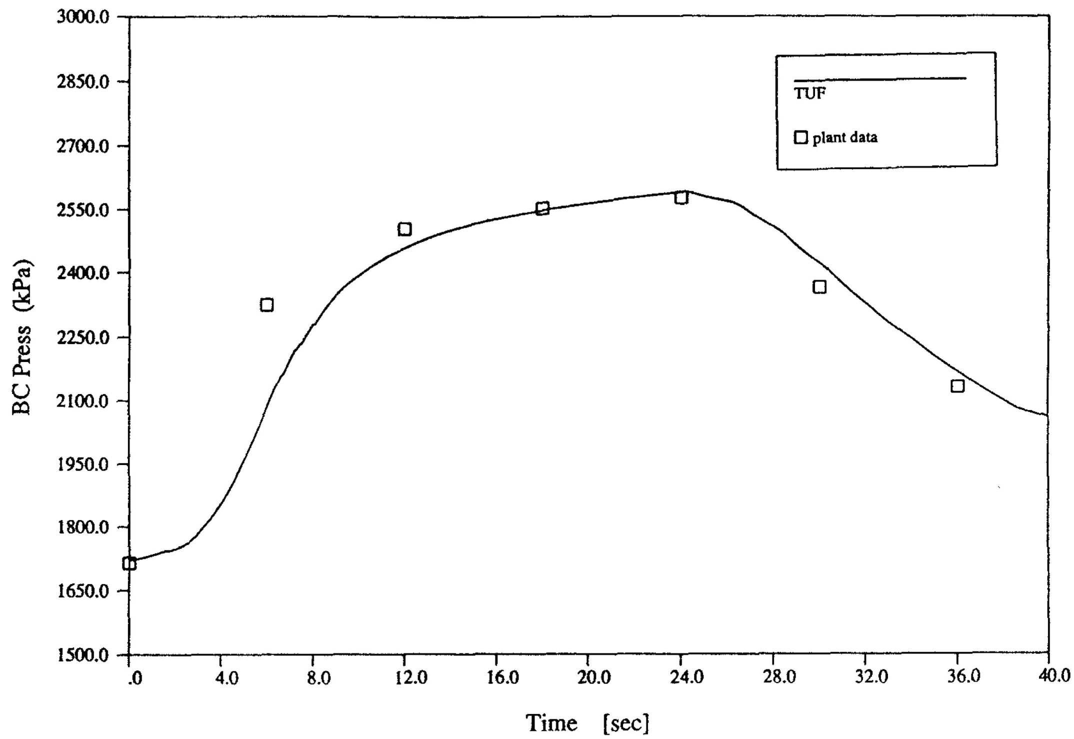




Figure 4. Comparisons of pressure and water level transients at pressurizer with plant data



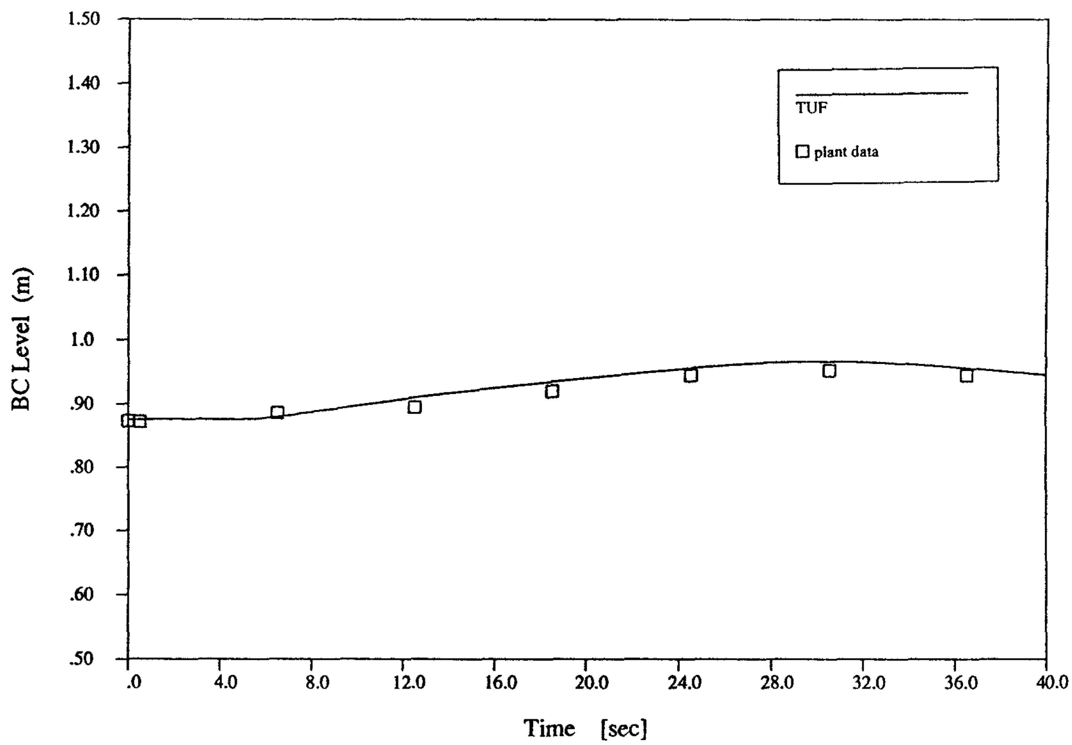




Figure 5. Comparisons of pressure and water level transients at bleed condenser with plant data

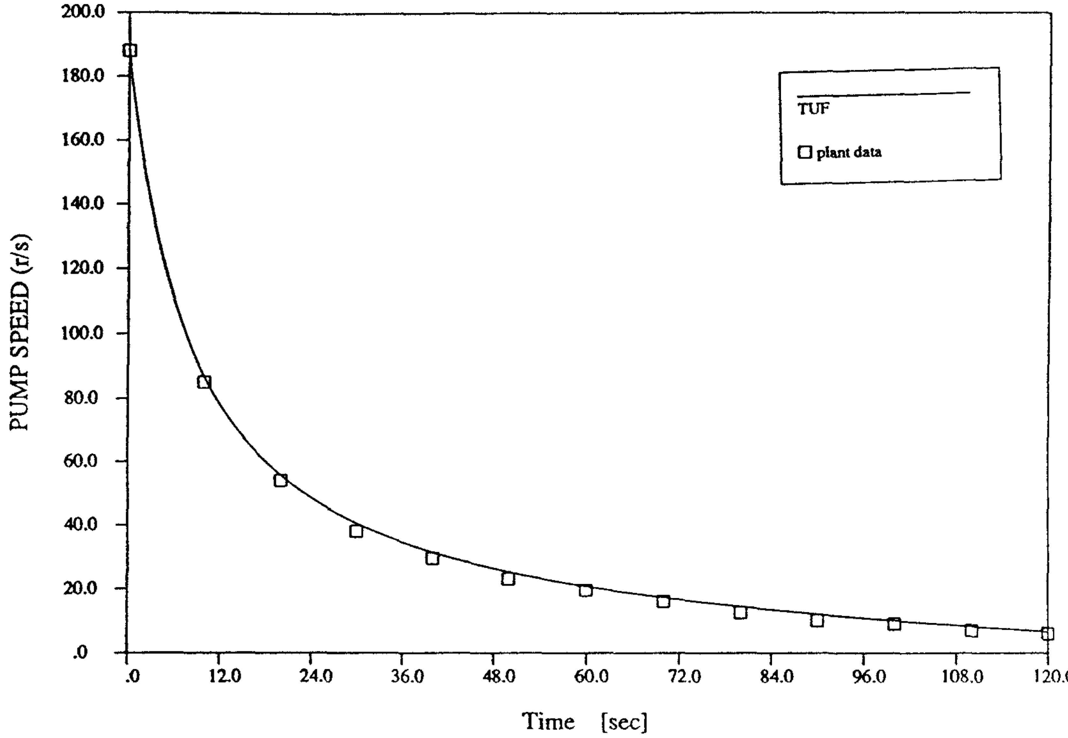



Figure 6. Comparison of pump speed with data from Darlington four pumps trip test

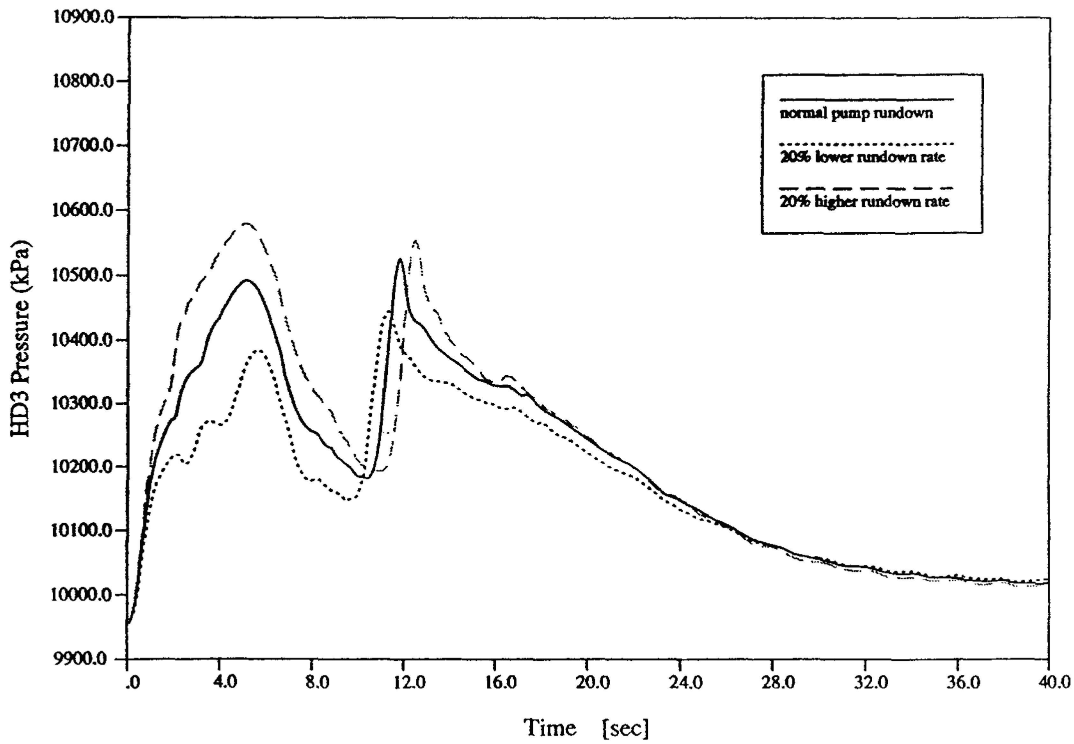



Figure 7. Comparison of HD3 pressure transients with different pump rundown rates

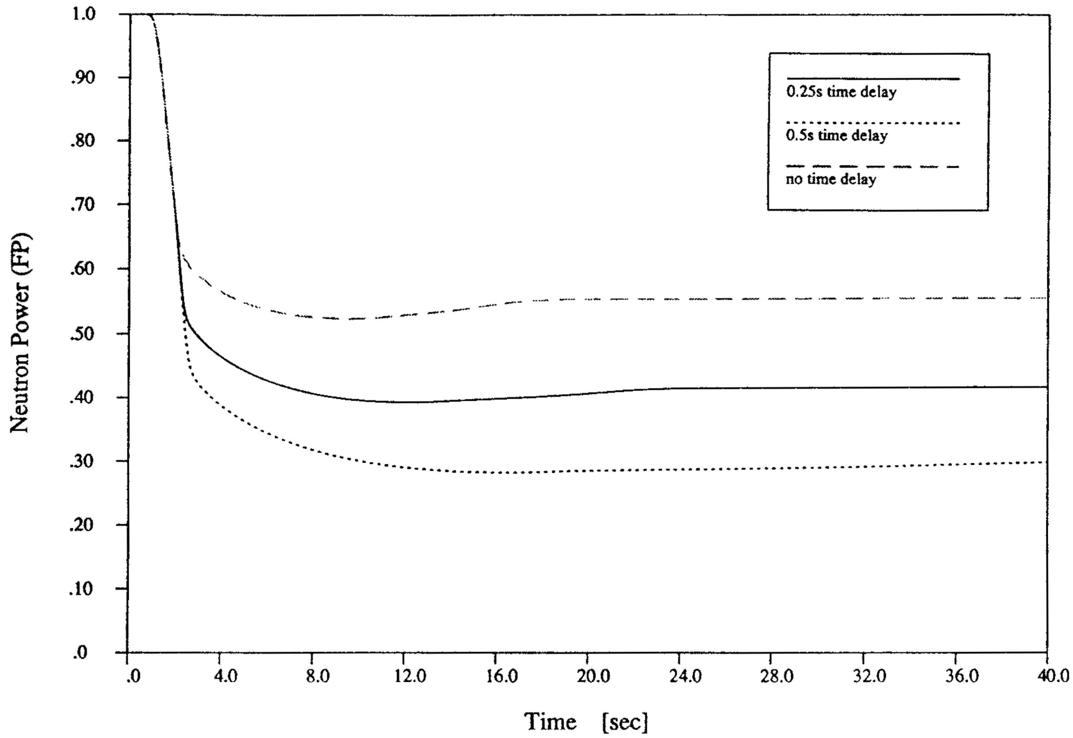



Figure 8. Comparison of neutron power transients with different actions in clutch closing

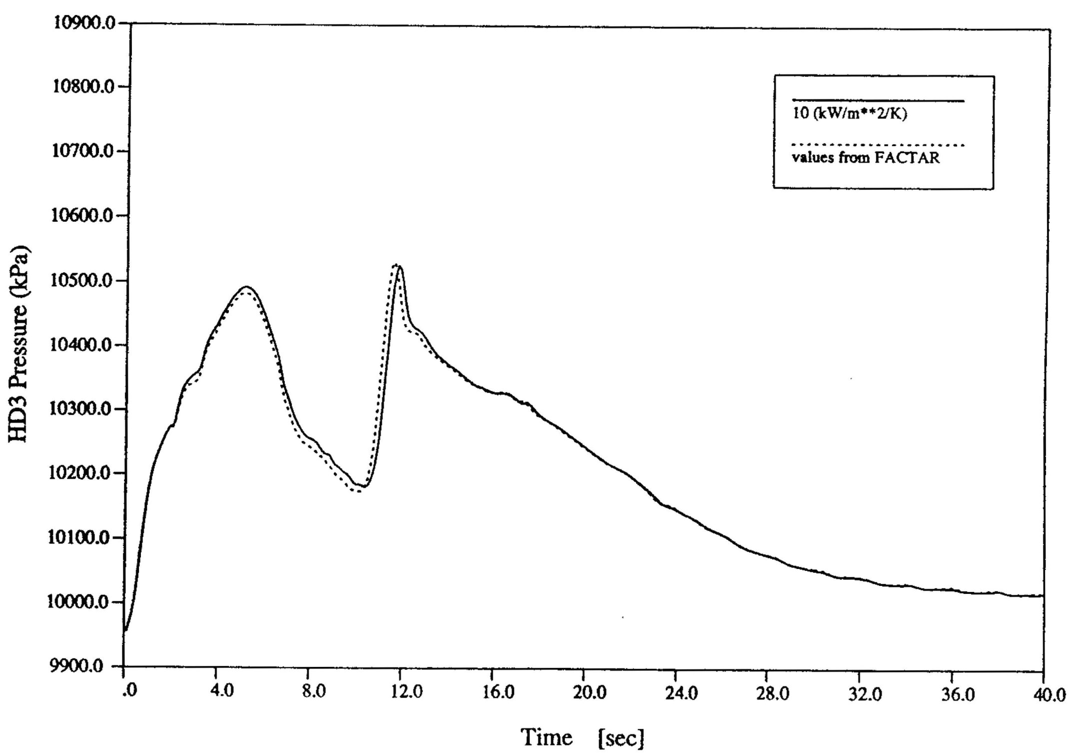



Figure 9. Comparison of HD3 pressure transients with different gap heat transfer coefficients

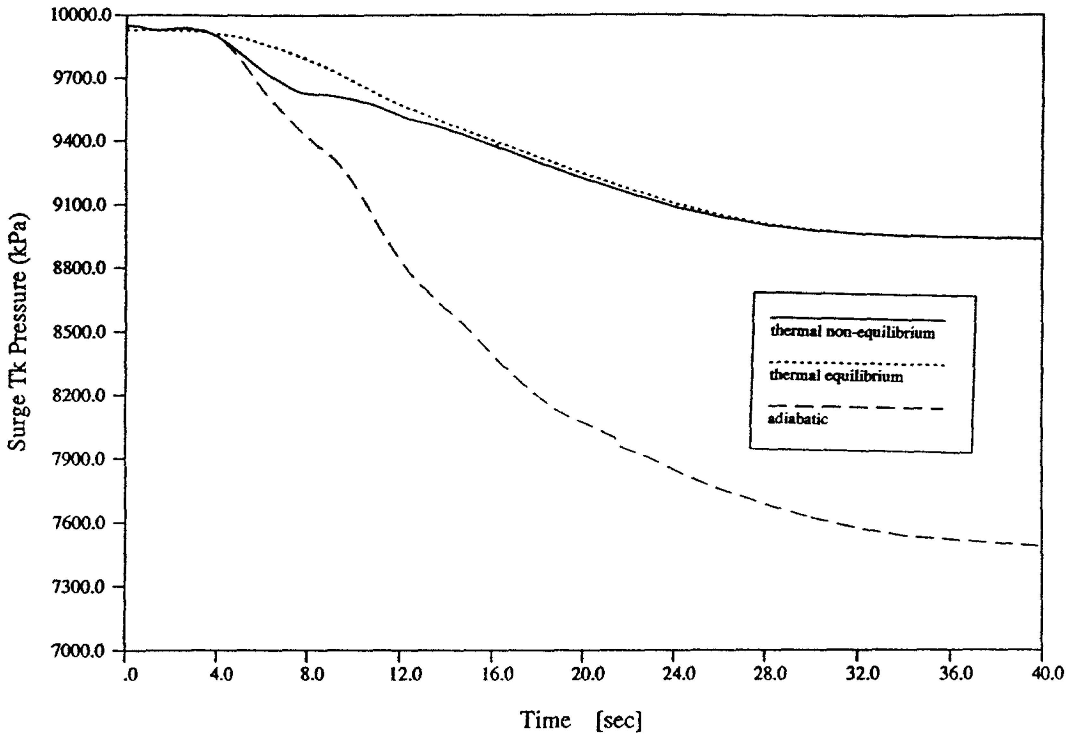



Figure 10. Comparison of pressurizer pressure transients with different models

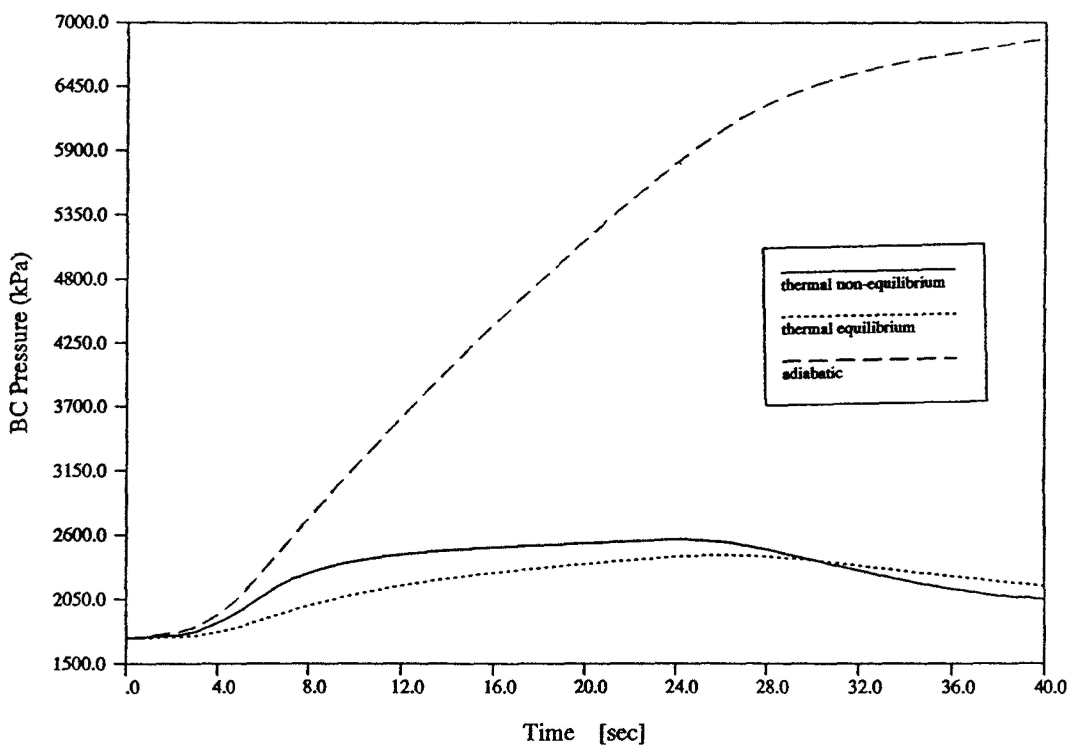



Figure 11. Comparison of bleed condenser pressure transients with different models

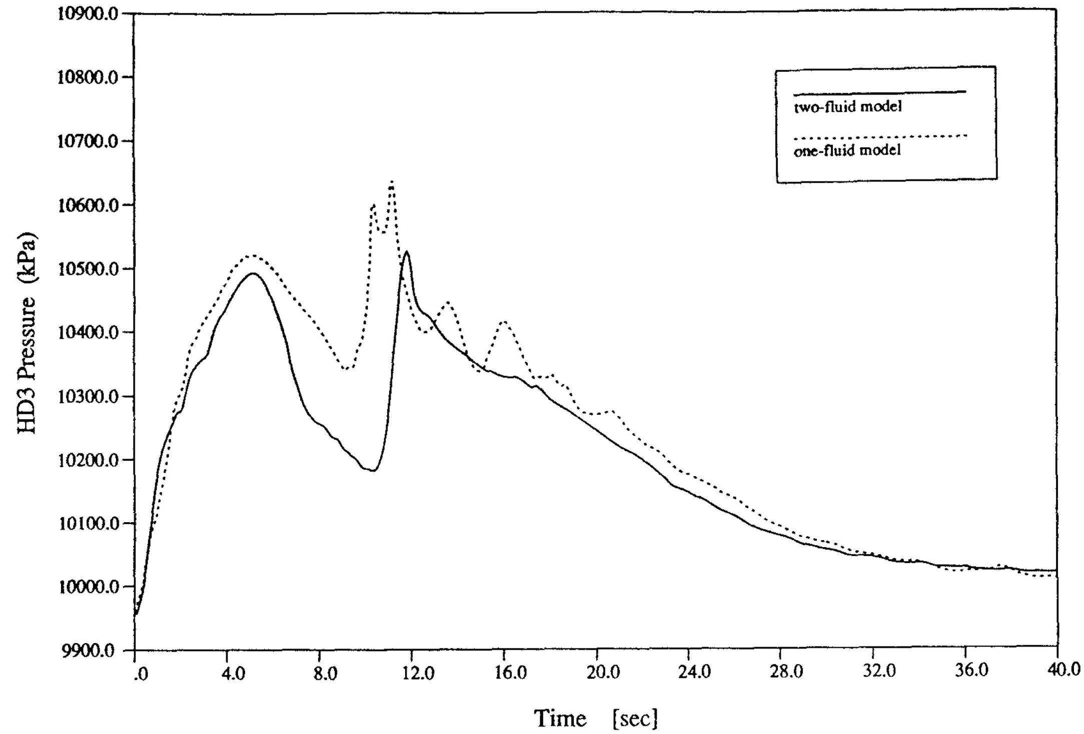



Figure 12. Comparison of HD3 pressure transients between one-fluid and two-fluid models

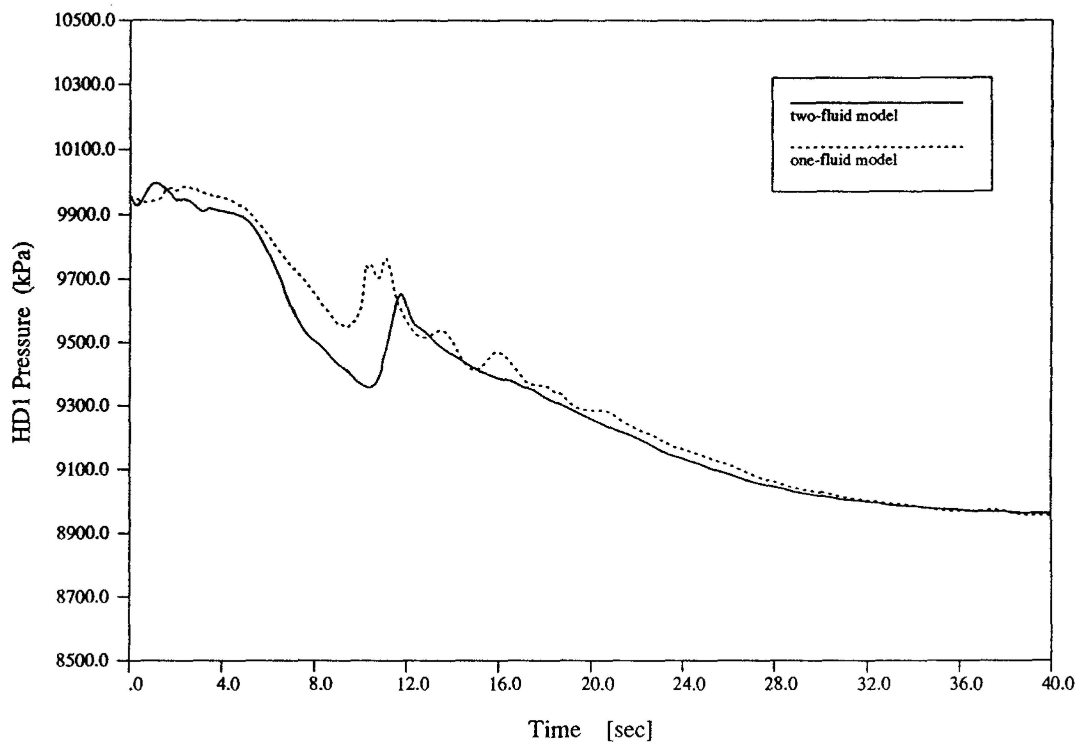



Figure 13. Comparison of HD1 pressure transients between one-fluid and two-fluid models