Omaha Public Power District

444 South 16th Street Mall
Omaha NE 68102-2247

June 4, 2003
LIC-03-0081

U. S. Nuclear Regulatory Commission
Attn: Document Control Desk
Washington, D.C. 20555

References:
1. Docket No. 50-285
2. Letter from OPPD (D. J. Bannister) to NRC (Document Control Desk) dated October 8, 2002, Fort Calhoun Station Unit No. 1 License Amendment Request, "Reactor Coolant System (RCS) Pressure and Temperature Limits Report (PTLR)" (LIC-02-0109)
3. Letter from NRC (A. B. Wang) to OPPD (R. T. Ridenhoure) dated May 21, 2003, Request for Additional Information Related to Fort Calhoun Station Pressure-Temperature Limit Report Submittal (TAC No. MB6468) (NRC-03-103)

SUBJECT: Response to Request for Additional Information, Pressure-Temperature Limits Report Amendment Request; Low Temperature Over Pressure

In support of the license amendment request, "Reactor Coolant System (RCS) Pressure and Temperature Limits Report (PTLR)" (Reference 2), the Omaha Public Power District (OPPD) provides the attached response to the Nuclear Regulatory Commission's (NRC's) Request for Additional Information of Reference 3.

I declare under penalty of perjury that the forgoing is true and correct (Executed on June 4, 2003). No commitments are made to the NRC in this letter.

If you have any questions or require additional information, please contact Dr. R. L. Jaworski of the FCS Licensing staff at (402) 533-6833.

Sincerely,

R. T. Ridenoure
Division Manager
Nuclear Operations

RTR/RLJ/rlj

A001

Attachment:
Response to NRC Request for Additional Information Pressure-Temperature Limits Report (PTLR); Low Pressure Over Temperature

c: T. P. Gwynn, Acting Regional Administrator, NRC Region IV
A. B. Wang, NRC Project Manager
J. G. Kramer, NRC Senior Resident Inspector

Attachment

Response to
NRC Request for Additional Information
Pressure-Temperature Limits Report
Low Temperature Over Pressure

Response to NRC Request for Additional Information
Pressure-Temperature Limits Report (PTLR); Low Pressure Over Temperature

NRC Question 1:
The LTOP analysis employed RELAP5/MOD3.2 which is not the latest version. RELAP5/MOD3.3 contains improved water property data at low pressure. Why was not RELAP5/MOD3.3 used and what would have been the impact on the LTOP transients?

OPPD Response:
RELAP5/MOD 3.3 was not used to perform the low temperature overpressure protection (LTOP) analysis due to the analysis being completed prior to the release of the RELAP5/MOD 3.3 code. The impact it would have had on the analysis is described in Appendix 1.

NRC Question 2:
Did ITS Corporation perform the LTOP analysis using the same version RELAP5 as that used by OPPD? If not what were the differences and do they impact the analysis?

OPPD Response:
ITS Corporation did not run RELAP5 to perform their LTOP analysis review. They analyzed the model and performed a series of hand calculations to verify that RELAP5/MOD 3.2 was predicting correct results (Reference A). Please refer to Appendix 2 for ENERCON Services, Inc discussion of Reference A.

NRC Question 3:
Code benchmarking and validation is presented in the attachment to the October 8, 2002 submittal named NEPTUNUS. Did INEEL use the same version as that used by OPPD in the LTOP analysis? (The INEEL RELAP5/3-D version differs from the ISL version used by OPPD). Did OPPD benchmark the version obtained from ISL? Please provide the validation results justifying the use of RELAP5/MOD3.2d for the LTOP analysis.

OPPD Response:
In the report NEPTUNUS, INEEL used RELAP5/MOD 3.2 as noted in the cover page of Reference B. OPPD's benchmark of RELAP5/MOD 3.2 is described in Section 5.1.5, page 15 of Attachment 1 to Reference C and pages 158 – 163 of Reference D.

NRC Question 4.A:
NEPTUNUS simulated pressurization (and subsequent depressurization) with sprays and an initial void in the pressurizer. Many of the LTOP analyses were run for a water solid condition. What data were used to validate the RELAP5 for water solid conditions?

OPPD Response:
The water solid transient involves only a small flow rate of water at near constant temperatures into a fixed volume. The consequence is a pressure rise until the power operated relief valve (PORV) setpoint is reached and then water flows out of the PORV after a suitable time delay.

The RELAP result consists of the pressure rise rate and the PORV flow rate. The pressure rise rate was verified to be acceptable by a hand-calculation as discussed below in Response 9. The PORV flow rate was verified to be reasonable by a hand calculation and discussed below in Response to NRC Question 5. These were considered sufficient validation since they are the only parameters of real interest.

NRC Question 4.B:
The NEPTUNUS pressurizer nodalization employed 12 cells while the LTOP Fort Calhoun analysis utilized 6 cells. Please provide the sensitivity study justifying the Fort Calhoun study.

OPPD Response:
The use of six nodes was based on a standard pressurizer model obtained from a sample input deck. The noding was not made finer because the transient analyzed did not require it. Specifically, for cases with a steam bubble in place, the inrush of cold water would be expected to form thermal stratification. This is what is observed. Please refer to Section Pressurizer in Appendix 2. With this hot-water-on-top stratification, buoyancy cells will not form so there is no need for side-by-side flow nodes. The insurge of water is relatively mild so inlet plumes are not expected to be dramatic or affect the temperature of the final layer that is in contact with the steam bubble. For the water solid case, the insurge is slightly warmer due to the conservative assumption of loss of decay heat removal simultaneous with the transient. ITS in its review recommended a single pressurizer node to generate equilibrium mixing. We do see a slight temperature inversion, however, this does not impact either the pressure rise or the PORVs ability to relieve water, and therefore does not impact the peak pressure predictions.

NRC Question 4.C:
What sensitivity studies were performed for time-steps and number-of-cells, which justify the time steps and number of cells in the Fort Calhoun model?

OPPD Response:
The cell nodalization in the Fort Calhoun model (this refers to all cells, not just the pressurizer) was based primarily on the existing CESEC plant model, since this allowed the use of consistent data. Great care was taken during the model construction to avoid any unusually small or large nodes. The minimum time step used is a millionth of a second, and the maximum time step used for model development was on the order of 0.1 seconds. After completing the model, smaller maximum time steps were utilized until the results were not affected. The final runs were performed with a very small maximum time step (0.001 seconds for the period of transient activity after initial equilibrium is reached) to assure that time step choice would not affect the final results.

NRC Question 4.D:
The Massachusetts Institute of Technology (MIT) pressurization test series showed that for pressurizer insurge the peak pressure was controlled by wall heat transfer rather the water-steam interfacial heat transfer. Please show the wall nodalization justifying the OPPD modeling approach.

OPPD Response:
The Fort Calhoun model does not credit heat loss to the walls of the entire Reactor Coolant System (RCS). That is, our pressurizer model is an adiabatic model. This is discussed in more

detail in the Response to NRC Question 8 below. In brief, the water solid transients are mild enough that the temperature rise in the pressurizer is only a few degrees so the adiabatic assumption is conservative and small. The steam bubble cases involve a slow collapse of the bubble that also results in only a few degrees of increase in the steam region. The heat input into the RCS in general in the steam bubble cases is assured to be conservative by the assumption of loss of shutdown cooling simultaneous with a startup of a reactor coolant pump at extremely conservative RCS-secondary side temperature differential.

NRC Question 5:
The power operated relief valves (PORV) discharge coefficient was based on high pressure steam conditions. Was the coefficient also used for liquid conditions at low pressure? If so, justify the use of the discharge coefficient.

OPPD Response:
The ITS Corp report notes that the PORV at Fort Calhoun has a much greater capacity than is required to mitigate these LTOP transients. The PORV is conservatively modeled as providing zero flow until 1.5 seconds when testing shows the PORVs will be fully opened (and even then we model the PORVs as ramping open over an additional 0.5 seconds). Once the PORVs are fully opened, in all cases the flow rate is well above that required to mitigate the transient. Hence even large errors in flow rate will have no effect on peak pressure.

A PORV discharge coefficient was not used to perform the LTOP analyses. Instead the flow rate for liquid conditions is based on a constant area. The flow area of 0.94 square inches was reduced to 0.77 square inches for this analysis as described in the Section entitled "PORV Flow Rate" on page 26 of Reference D. The liquid flow rate is then generated by RELAP based on the pressure drop across a flow area of 0.77 square inches. The resulting RELAP flow rates are further discussed in response to question 14 where the flow rates are seen to be at least 2.5 times greater than the injection flow rates. In summary, conservatism in the peak pressure is assured by a conservatively slow PORV opening time and by the fact that the PORV flow capacity is much greater than required to mitigate these events.

Finally, the PORV flow rate was independently checked by the use of the American Petroleum Institute Standard 520 relief valve flow rate methodology. See Appendix 3 for the comparison calculation.

NRC Question 6:
The benchmarking is insufficient for over-pressurization events. There are relevant data from Shippingport, Connecticut Yankee, and Millstone 2. Also a series of insurge non-equilibrium experiments at Massachusetts Institute of Technology (MIT) by Griffith which covers low pressure. Please justify the adequacy of the benchmarking or show the results with the above data. Also provide a comparison of RELAP5 with data in a water solid condition. Please discuss the data in the literature and your reasons for your choice of separate effects and integral experiments.

OPPD Response:
The benchmarking was performed to demonstrate accurate RELAP results for sample inputs that are provided with the code, and relevant cases as discussed in Response to NRC Question 3. Further benchmarking is contained in Appendix 2, pages 87 and 88 to Reference D that verifies the specific model was consistent with expected flow rates and pressure drops. Discussion regarding the MIT data is provided in Response to NRC Question 8 below. Based on all the benchmark results stated previously in Reference C, and per Response to NRC Question 8 below, OPPD considers that the benchmarking is adequate and sufficient in determining RELAP5/MOD3.2's capability to determine the peak pressure following LTOP transients. Please refer to Section 5.1.5, page 15 of Attachment 1 to Reference C for OPPDs reasoning in determining the verification and validation of using RELAP5/MOD 3.2 for performing LTOP analyses.

NRC Question 7:
Once residual heat removal (RHR) conditions are met, the reactor coolant system (RCS) can develop a bubble in the top of the vessel. Please discuss the effect of the bubble in the reactor vessel. It is anticipated that a bubble in the upper head would not affect the peak pressure but only the timing of pressure increase. Please discuss whether a bubble in the upper head impacts the results and conclusions of the analysis.

OPPD Response:
The key in determining the peak pressure is the rate of pressurization. In every scenario that opens a PORV, the analytical question is "What is the peak pressure between the time that the setpoint is reached and the PORV fully opens?", due to once the PORV opens its large capacity provides an immediate depressurization. Anything that could help the elasticity of the RCS will slow the rate of pressurization. It is noted in Section 2.3.3.1 of Reference E that it is conservative to not credit letdown, RCS volume expansion or RCS metal thermal inertia. A reactor head bubble would similarly be a non-conservative assumption since it would be an RCS volume expansion benefit. Therefore, the bubble that could develop in the upper head would act to reduce the peak pressure.

NRC Question 8:
In many of the LTOP events, collapse of the bubble in the pressurizer will occur. Please explain how the bubble collapses during the insurge prior to opening of the PORV. It appears that the nodalization in the pressurizer is too coarse so that artificial mixing of the fluid during the insurge when there is a bubble in the pressurizer will reduce the magnitude of the pressurization. During such an insurge, the increase in the liquid is expected to compress and superheat the upper steam region. Some heat transfer between the liquid and steam region will occur initially, however, the liquid surface will saturate and a thermal layer will form insulating the steam from the lower cooler liquid region. Under these conditions, the upper steam region would not be expected to totally collapse as the RELAP5 can predict. Please discuss the above comparison with the MIT pressurization tests will show these non-equilibrium effects.

OPPD Response:
This NRC question was discussed further in a telephone conversation between the analysis authors and the reviewer on April 23, 2003. It was decided that the best approach would be to apply the existing Fort Calhoun LTOP pressurizer component to the same type of transient as that run by the MIT researchers. This was done and is presented in Appendix 4. The conclusions are as follows:

- The Fort Calhoun LTOP pressurizer model is adiabatic, so it in fact exaggerates the superheat effect noted in Response to NRC Question 8. At the conditions of the MIT experiment, an adiabatic assumption causes conservatively high pressure predictions, and this is verified by applying our LTOP pressurizer model to the MIT dimensions and insurge flow. Evaluation of the results show that the steam bubble, initially at 303 °F, reaches 490 °F if the wall heat transfer is ignored, and the pressure rise is 114 psi compared to data showing only 11 psi (this over prediction of the adiabatic approach is consistent with both the MIT paper and a related ICONE paper (Appendix 5) as discussed in Appendix 4). However, the MIT test conditions are not similar to the Fort Calhoun LTOP transient.
- The interfacial area between the liquid and steam phases is much smaller for the MIT test set up, and the insurge is much more dramatic in terms of percent volume decrease. If those two factors are corrected to match the Fort Calhoun conditions (same interfacial area to bubble height, same percentage of reduction in bubble size) the pressure rise predicted by the model is only 14 psi. That is, the adiabatic assumption becomes much less important for the conditions addressed in LTOP transients than for the MIT test conditions.
- The purpose of the LTOP model is to provide a conservatively high peak pressure. The adiabatic assumption increases the peak pressure of the model, and is therefore conservative in that measure. The adiabatic assumption does make it more likely that a PORV will open, but in most transients that does not occur even with the adiabatic assumption (the only case with a bubble that results in a PORV opening is Case 12 of Reference D, which assumes unusual initial conditions, and then the PORV opening is seen to easily handle the transients). In other words, the adiabatic case is also conservative with regards to whether or not the PORV lifts. The only direction of non-conservatism relates to whether the transient will collapse the steam bubble or not, assuming wall heat transfer would decrease the bubble size. However, the transients performed at Fort Calhoun show very little temperature rise (as witnessed by the very small change in bubble pressure). For example, the steam bubble temperature in case HP509S30 of Reference D, the worst case in terms of shrinking the bubble, sees the pressurizer steam temperature rises from 323 °F to only 327 °F. Thus the heat transfer to the walls would be very small if modeled. Since the transients always demonstrate large remaining bubbles of over 100 cubic feet after 600 seconds, the adiabatic assumption does not affect the conclusion that bubbles remain until the RCS and SGs equilibrate. Please refer to Appendix 4 for more details.

NRC Question 9:
Please explain why the events with injection from a liquid-solid condition do not result in an immediate and faster pressurization.

OPPD Response:
The pressurization rate is verified by checking against hand calculations to be reasonable. The ITS reviewer did this by the use of an Excel spreadsheet model (Please refer to Reference A for more information). In brief, the RCS is a large volume and the water injected takes as long as RELAP predicts to cause the pressure to rise to the PORV setpoint.

NRC Question 10:
Which critical flow model was used in the RELAP5 model and what is the basis for the choice?

OPPD Response:
The choice was to use the default RELAP5 model, and the basis was that the PORV capacity is so much greater than required that the specific flow model has no significance to the final result. As noted in response to the ITS review (Appendix 2), we did perform an independent verification of the flow rate using the same pressure differentials and a methodology developed by the American Petroleum Institute (API) for flashing flow through a constant area. The API approach agreed well with the RELAP model results (Please refer to Appendix 3 for the specific comparison).

NRC Question 11:
Non-condensables collect in the pressurizer and the upper vessel head. Please describe the impact of non-condensables on the LTOP analysis. Are there any scenarios where non-condensables can affect the calculated peak pressure and the development of the LTOP limits?

OPPD Response:
As noted in Response to NRC Question 7, any increase in the elasticity of the RCS slows the pressurization rate, so the presence of non-condensables in the water solid transients is a benefit because it decrease the pressure rise between the time that the setpoint is reached and the PORV opens. Since the need is to remove volume from the RCS, and gas flow through a PORV has a much higher volumetric flow rate than water flow, any non-condensables present at the time of PORV opening would also be a benefit for the water solid transients. For steam bubble cases, the effect of non-condensables could be to increase pressure, since the non-condensable gas could not condense to a liquid phase. A confirming evaluation was performed and is attached as Appendix 6. However, the final conclusion is that the slightly higher peak pressures are still well below the PORV setpoints. An additional case in Appendix 6 verifies that if the PORV setpoint was reached due to unusual initial conditions, the non-condensable gas provide adequate volumetric relief. This additional case verified the final conclusion of Case 12 in Reference D, i.e., the PORV lifting on a steam bubble cases provides fast pressure relief and a less limiting peak pressure than the water-solid transients.

NRC Question 12:
Was inadvertent actuation of emergency sprays evaluated for the cases where the pressurizer is water solid?

OPPD Response:
Fort Calhoun Station (FCS) does not have automatic initiation of emergency (i.e., auxiliary) sprays.

NRC Question 13:
What assumptions are made regarding the quench tank? Once the quench tank ruptures, would this result in higher pressurizer pressure due to the additional quench tank resistance? What is the relief area from the quench tank compared to the PORV? Please show that the analysis without a quench tank model is bounding.

OPPD Response:
The FCS LTOP model is only concerned with the peak pressure. The peak pressure occurs as soon as the PORVs open, since from that point on the relief flow rate is much greater than the injection flow rate. OPPD conservatively modeled the backpressure as the relief valve setpoint (of the quench tank) plus 5 psi even though the initial lift (with a non-pressurized quench tank) is the only lift of significance for this analysis. During an actual event, the backpressure at the initial lift would be atmospheric plus line losses. Modeling the higher backpressure assures that the initial lift with its slightly higher decay heat bounds any later reseat and re-lift.

NRC Question 14:
For each case, show the PORV mass flow rates as compared to the injection rate. Also show the void fraction in the top cell and the temperature distribution in the pressurizer for each case.

OPPD Response:
The PORV mass flow rate vs. injection rates are given for Cases 2, 7 and 8 of Reference D. These are the only significant cases for mass flow rate vs. injection rate because Cases 1 through 6 are all at about the same PORV relief pressure, so the flow rates are about the same. In each case, the PORV flow rate is more than 2.5 times the injection rate.

(Text from Case 2)
The peak mass flow rate out of the PORV is shown in the output file to be 49.5 lb_m/s. At 50°F, the density of water is about 62.4 lb_m/ft^3. Therefore the flow rate is:

$$49.5\ lb_m/\text{second} * 1/62.4\ lb_m/ft^3 * 7.481\ \text{gallons}/ft^3 * 60\ \text{seconds/min} = 356\ \text{gpm}$$

This is well above the injection flow rate of 132 gpm. As expected, modest errors in PORV flow rate may affect the depressurization rate, but they have no impact on the peak pressure

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(Text from Case 7)
This is the first case since Case 2 that the PORV flow rate is significantly different because this is the first case with a different driving pressure. The computer output shows the peak PORV flow rate is 76.4 lb_m/second. At 255°F, the density of water is 58.7 lb_m/ft^3. The volumetric flow rate is therefore:

$$76.4\ lb_m/second*1/58.7\ lb_m/ft^3*7.481\ gallons/ft^3*60\ seconds/min = 584\ gpm$$

This is well above the injection flow rate of 215 gpm at this pressure. As expected, modest errors in PORV flow rate may affect the depressurization rate, but they have no impact on the peak pressure.

(Text from Case 8)
The computer output shows the peak PORV flow rate is 90.4 lb_m/second. At 305°F, the density of water is 57.1 lb_m/ft^3. The volumetric flow rate is therefore:

$$90.4\ lb_m/second*1/57.1\ lb_m/ft^3*7.481\ gallons/ft^3*60\ seconds/min = 710\ gpm$$

This is well above that needed to offset the injection and decay heat. As noted, the injection flow rate is limited to the 132 gpm supplied by the charging pumps since the pressure is greater than the HPSI pump shutoff.

NRC Question 15:
How were the quality assurance findings identified in the ITS Corporation letter dated September 9, 2002, addressed relative to their impact on the LTOP analysis? Please discuss each of the findings and their impact on the analysis.

OPPD Response:
Please refer to Appendix 2 of this attachment.

NRC Question 16:
Discuss the pertinence of the INEEL validation presented for the SCDAP/RELAP5 simulation of the TMI-2 accident in view of the fact that the SCDAP/RELAP5 code differs from RELAP5/MOD3.2 used in the OPPD analysis; the nodalization is very different from the OPPD model, and TMI-2 is a different design compared to the CE-designed FCS. The SCDAP/RELAP5 simulation does not validate nor justify the application of a different version RELAP5/MOD3.2d for use in simulating LTOP events in a CE-designed plant. As such the benchmarking is weak. Additional benchmarking using the same version of RELAP5 that was used in LTOP analysis by OPPD needs to be employed in the analysis. Please consider the MIT pressurization test data, as well as RELAP5 simulations of over-pressurization events in CE-designed plants. Benchmarking of the code against water solid relief is also needed.

OPPD Response:
The benchmarking was performed to demonstrate that the code is functioning correctly on the OPPD system and could adequately predict the peak pressure following an LTOP transient. The same version of the RELAP code (and, in fact, the same computer) was used to perform both the benchmarking and the LTOP analysis. An attempt was made to find documented benchmarking data for cases as similar as possible. The adequacy of the RELAP program to model a transient like this is discussed in Response to NRC Question 6.

It is stressed that the Fort Calhoun LTOP transients are relatively mild. The mass addition case is water added to a closed system causing that system to pressurize. ITS hand calculation shows the pressurization rate is accurate as mentioned in the Response to NRC Question 9. The water relief aspect is merely calculating a flow rate. The conservatism regarding PORV flow relief comes from a conservative delay time prior to opening, and the peak pressure as noted is insensitive to the flow rate. In any case, calculations in Appendix 1 show good agreement with the RELAP. The MIT pressurization test data is considered in Appendix 4 and hence the heat addition cases were demonstrated to be conservative. Therefore sufficient validation and verification has been performed to ensure that RELAP5/MOD 3.2 is adequate in predicting a conservative peak pressure following an LTOP event.

NRC Question 17:
Please explain why the pressure does not cycle open and close the PORV as steam is initially vented and then remain at the PORV setpoint when the discharge transitions to a pure liquid condition, stabilizing at the condition where injection into the RCS equals the PORV discharge flow. Please show the injection rates compared to PORV mass flow and the quality exiting the pressurizer for those cases.

OPPD Response:
The exact flow rate through the PORV is not critical to the analysis if the flow is sufficient to halt the pressure rise. That is, if as soon as the PORV is full open, the flow out of the PORV is greater than the RCS volumetric increase (due either to mass addition or heat expansion), then the pressure will fall. Small errors in the PORV flow rate will affect the rate of depressurization, but not the peak pressure. This analysis is only concerned with peak pressure.

For transients where the pressure is not immediately relieved at PORV lift, the RCS pressure will continue to rise until an equilibrium condition exists. Should this scenario occur, the accuracy of the PORV equation is important to the peak pressure. Conservatism is assured in this analysis by the use of a high backpressure of 90 psia. As demonstrated in the Results section of Reference D, all PORV lifts immediately reduced the transient pressure; hence modest changes in the PORV flow rates would not affect the peak pressures.

In summary, this model is only concerned with peak pressure. In every case, the initial PORV lift immediately relieved the pressure transient with more than adequate flow. Since the initial lifts are performed at a bounding conservative high backpressure, and subsequent lifts will occur with less decay heat input, it is not necessary to model the transient past the peak pressure point and the model does not attempt to simulate this period. The PORV mass flow rates versus injection rates are included in Response to NRC Question 14 above.

References:

A) Letter from ITS Corporation (K. Ross) to OPPD (F. James Jensen) dated September 9, 2002, "ITS Corporation's Cursory Review of OPPD's LTOP Analysis." [Note: This Reference was included in Letter from OPPD (D. J. Bannister) to NRC (Document Control Desk) dated October 8, 2002, Fort Calhoun Station Unit No. 1 License Amendment Request, "Reactor Coolant System (RCS) Pressure and Temperature Limits Report (PTLR)" (LIC-02-0109)]
B) R5-02-01, Validation Report for NEPTUNUS Pressurizer using RELAP5/MOD 3.2, dated April 12, 2002. [Note: This Reference was included in Letter from OPPD (D. J. Bannister) to NRC (Document Control Desk) dated October 8, 2002, Fort Calhoun Station Unit No. 1 License Amendment Request, "Reactor Coolant System (RCS) Pressure and Temperature Limits Report (PTLR)" (LIC-02-0109)]
C) Letter from OPPD (D. J. Bannister) to NRC (Document Control Desk), dated October 8, 2002, "Fort Calhoun Station Unit No. 1 License Amendment Request, "Reactor Coolant System (RCS) Pressure and Temperature Limits Report (PTLR)" (LIC-02-0109)
D) FC06877, Rev. 0, "Low Temperature Overpressure Protection (LTOP) Analysis, Revision 1." [Note: This Reference was included in Letter from OPPD (D. J. Bannister) to NRC (Document Control Desk) dated October 8, 2002, Fort Calhoun Station Unit No. 1 License Amendment Request, "Reactor Coolant System (RCS) Pressure and Temperature Limits Report (PTLR)" (LIC-02-0109)]
E) Letter from the NRC (S. A. Richards) to the CEOG (R. Bernier) dated March 16, 2001, "Safety Evaluation of Topical Report CE NPSD-683, Revision 6, "Development of a RCS Pressure and Temperature Limits Report (PTLR) for the Removal of P-T Limits and LTOP Requirements from the Technical Specifications" (TAC No. MA9561)

Appendix 1
Comparison between RELAP5/MOD 3.2 and RELAP5/MOD 3.3

An evaluation was performed to compare the results of RELAP5/MOD 3.2 and RELAP5/MOD 3.3 for a few select low temperature overpressure protection case runs (Reference D). For each overpressure event (i.e. heat and mass addition) two case runs were performed using RELAP5/MOD 3.3. No modeling changes were performed for any of the cases.

For the heat addition event (HA), Cases 11 and 12 were performed. Please refer to Figures 1 and 2. These figures demonstrate that using RELAP5/MOD 3.3, the resultant peak pressure is significantly lower and depicts the two codes having essentially the same trend. The conclusion is that both codes seem capable in determining the peak pressure following a HA event and demonstrate essentially the same trends. For both HA cases, it appears that RELAP5/MOD 3.2 conservatively predicts a higher peak pressure.

For the mass addition (MA) event, Cases 2 and 6 were performed. Please refer to Figures 3 and 4. These figures demonstrate that using RELAP5/MOD 3.3, the resultant peak pressure is essentially identical to that predicted by RELAP5/MOD3.2 and depicts the two codes predicting essentially the exact same trend. The conclusion is that both codes are capable of determining the peak pressure following a MA event and demonstrate essentially the same peak pressure and trend.

The overall conclusion of the comparison between RELAP5/MOD 3.3 and RELAP5/MOD 3.2 is that it appears the improved water property tables provide an improvement (i.e. a lower peak pressure) in determining the peak pressure following a HA event. In the four test cases that were performed, the peak pressure is higher using RELAP5/MOD 3.2 (HA events only) and both codes predict essentially the same trends. Thus it is concluded that RLAP5/MOD 3.2 provides conservative results and thus its application for the Fort Calhoun Station LTOP analysis is also valid and conservative.

Reference D: FC06877, Rev. 0, "Low Temperature Overpressure Protection (LTOP) Analysis, Revision 1." [Note: This Reference was included in Letter from OPPD (D. J. Bannister) to NRC (Document Control Desk) dated October 8, 2002, Fort Calhoun Station Unit No. 1 License Amendment Request, "Reactor Coolant System (RCS) Pressure and Temperature Limits Report (PTLR)" (LIC-02-0109)]

Figure 1

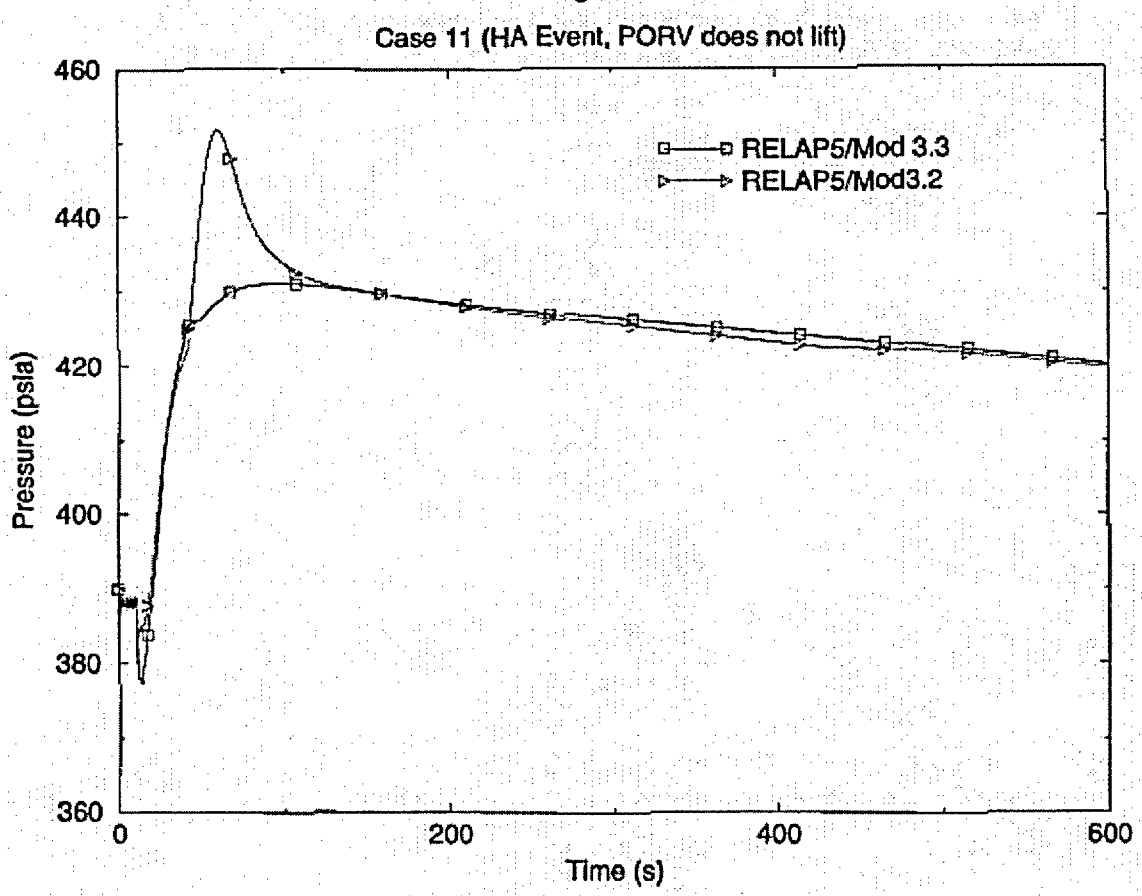

Case 11 (HA Event, PORV does not lift)
RELAP5/Mod 3.3
RELAP5/Mod3.2
Pressure (psia)
Time (s)
460
440
420
400
380
360
0
200
400
600

Figure 2

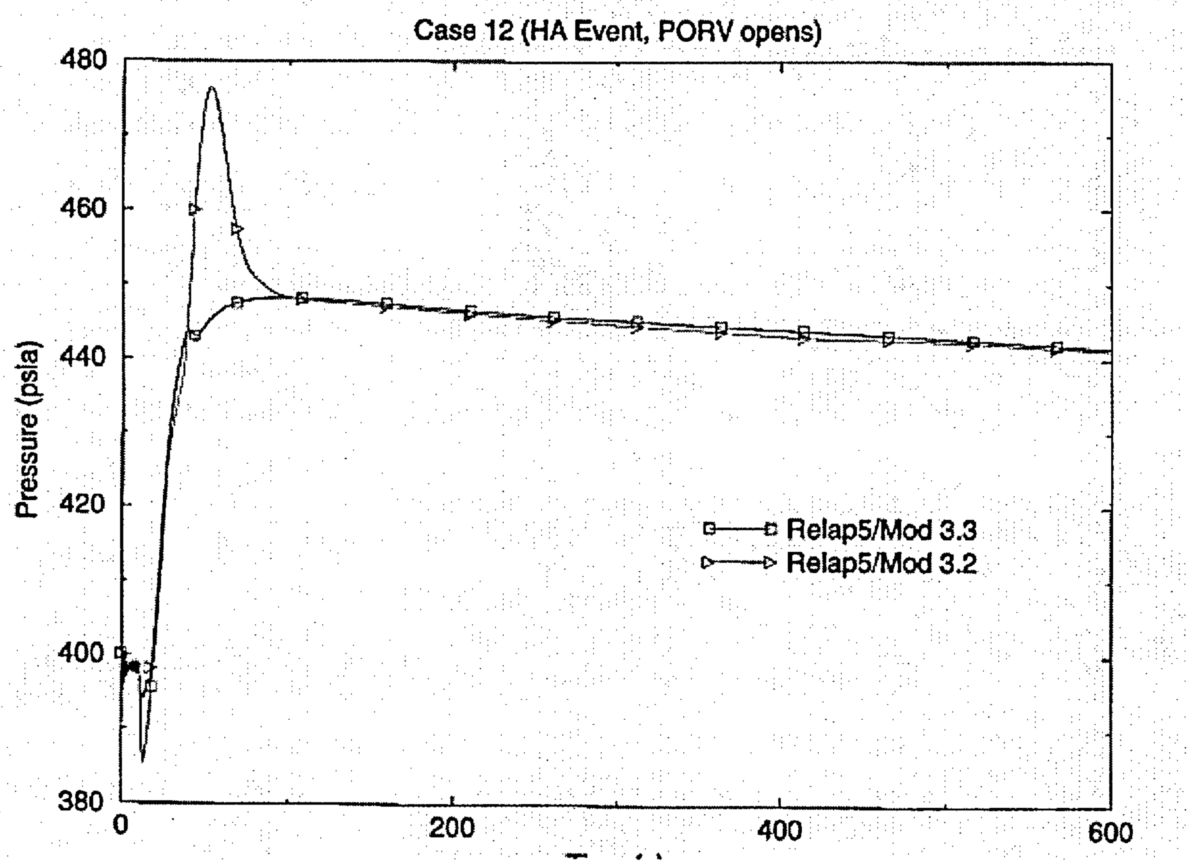

Case 12 (HA Event, PORV opens)
480
460
440
420
400
380
Pressure (psia)
0
200
400
600
Relap5/Mod 3.3
Relap5/Mod 3.2

Figure 3

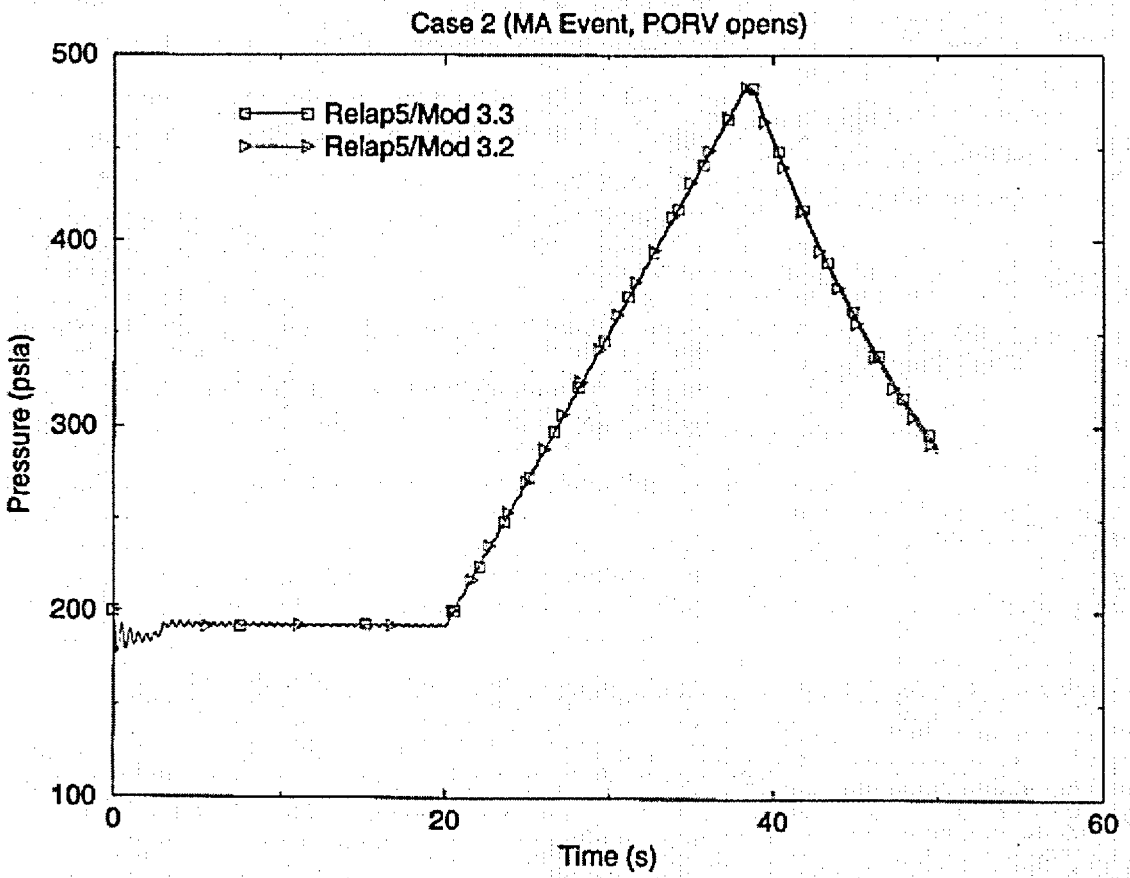
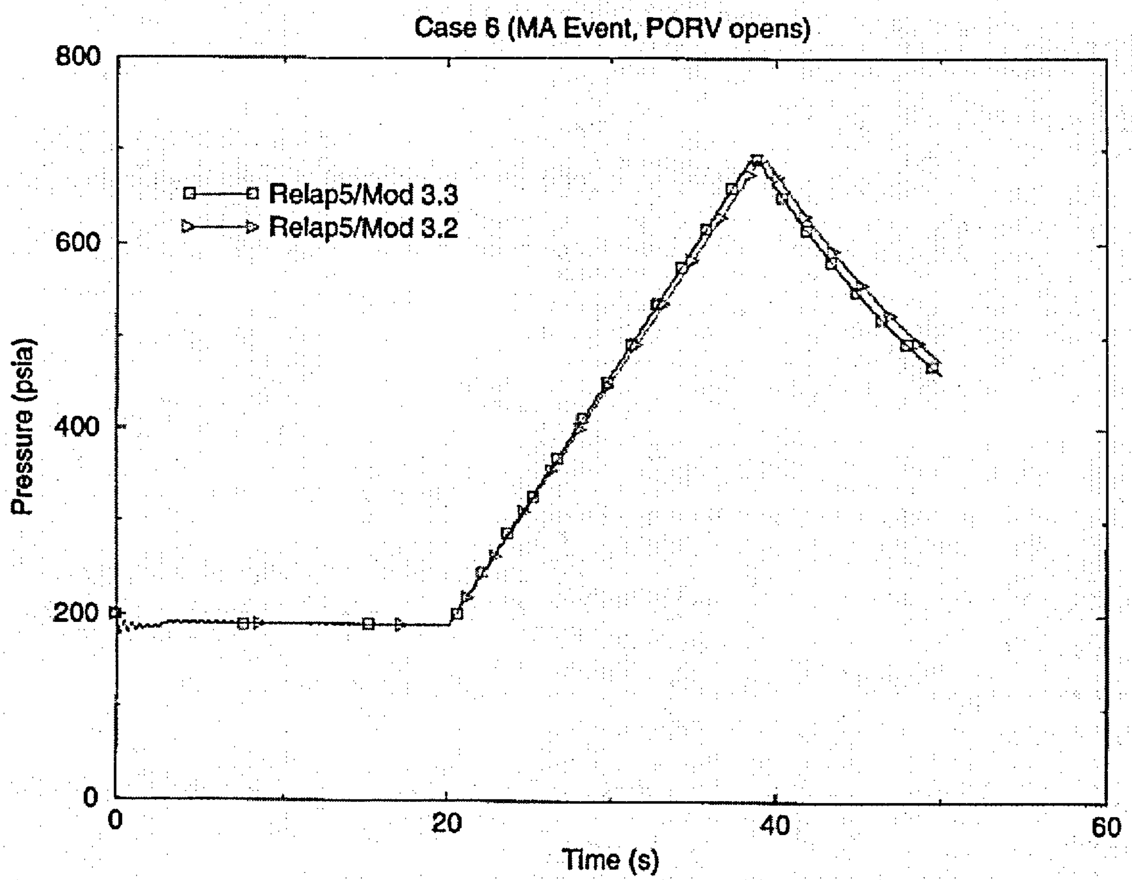

Case 2 (MA Event, PORV opens)
Relap5/Mod 3.3
Relap5/Mod 3.2
Pressure (psia)
Time (s)
500
400
300
200
100
0
20
40
60

Figure 4


Case 6 (MA Event, PORV opens)
Relap5/Mod 3.3
Relap5/Mod 3.2
Pressure (psia)
800
600
400
200
0
0
20
40
60
Time (s)

Appendix 2
Letter OPP1-LTR-007

OPP1-LTR-007
September 30, 2002

Omaha Public Power District
Attn. Mr. F. James Jensen III
444 South 16th Street Mall
Omaha, NE 68102-2247

Subject: ITS Review of the Low Temperature Overpressure Protection (LTOP) Analysis

Reference: 1) ITS Corporation's Cursory Review of OPPD's LTOP Analysis, ITS 01-OPPD-02-004-01-101, 9/9/02
2) Fort Calhoun Low Temperature Overpressure Protection Final Report, Revision 1, 3/15/02, ENERCON Services

Dear Mr. Jensen,

In Reference 1, ITS Corporation describes its cursory review of the RELAP model developed by ENERCON Services and OPPD for analyzing postulated Fort Calhoun transients having potential to exercise the Low Temperature Overpressure Protection (LTOP) System (Reference 2). This letter responds to the key points raised in the ITS review. As noted by ITS, no modeling concerns were raised to question the conclusions of the analysis.

Sections of text taken from the referenced document are reproduced here in italics.

Injection Water Temperature

The temperature of the injection water in the mass addition scenarios was taken to be 250 °F. This temperature is unrealistically high for safety injection water and for makeup (charging) water under cold shutdown conditions. The reasoning behind the originators use of elevated injection water temperature seems questionable. However, the LTOP report argues convincingly that the elevated temperature is conservative; the reasons being that:

1. *Injection mass flow rates were specified assuming that the injection water was cold*
2. *The peak reactor coolant system (RCS) pressure predicted by RELAP for a particular scenario was compared to the allowable pressure on the P/T curve associated with the temperature of the RCS at the beginning of the scenario (as opposed to the higher pressure on the*

P/T curve associated with the higher temperature of the RCS at the time the peak pressure occurred).

The argument that the use of elevated injection water temperature is conservative is believable. However, a review recommendation is that any future RELAP LTOP calculations be made with realistic injection water temperatures.

Response: There is a trade-off between density and enthalpy effects associated with the water temperature assumption. Cooler water has greater density increasing mass injection; hotter water has greater enthalpy increasing energy injection. It is not possible to use a realistic injection water value without numerous sensitivity analyses for each scenario to determine the worst-case value. We simplified the effort by using extreme non-mechanistic bounds – density just above freezing and enthalpy associated with 250° F. The 250° F value admittedly would require unusual scenarios and might only exist for a brief period, but it was based on the maximum conceivable VCT temperature. Any other temperature would have required more justification. The energy difference between 250 and 120 (if justifiable) would be about 130 Btu/lbm. At 132 gpm, that amounts to about 2.4 MWs, calculated as 130 Btu/lbm*132 gal/m* 8 lbm/gal*1/60 s/m*1055 Ws/Btu*1e-6 MW/W. This extra energy of 2.4 MWs is less than 10% of the 25.7 MW decay heat, so the water injection temperature conservatism is small compared to the conservative way we addressed decay heat.

PORV Flow Resistance

A hand calculation was made to verify the flow resistance offered by the PORV in the RELAP model. This was done on account of questions that arose in the course of the review regarding the adequacy of the PORV modeling for subcooled liquid flow. In several of the mass-addition LTOP scenarios, RCS temperature remains below the saturation temperature downstream of the PORV. In these scenarios, the PORV is flowing liquid water. The hand calculation is included as Attachment 1. The results of the hand calculation were compared to the results of the RELAP calculation for the base mass-addition LTOP scenario. The RELAP calculation had to be extended for a comparison to be made. The specific comparison was of the steady-state pressure drop across the PORV given a cold water-solid RCS and a fixed charging flow rate. Critical in considering the mass addition scenarios involving the charging pumps is realizing that these pumps at FCS are positive displacement pumps (as opposed to centrifugal pumps). Such pumps develop a certain flow irrespective of head. As such, the pressure excursion that would be experienced by FCS given spurious operation of all charging pumps (and one operating PORV) in a cold shutdown condition would be largely different (smaller) than what would be experienced by a plant having centrifugal charging pumps. For 132 gpm charging flow, the hand calculation and the RELAP calculation predict a pressure drop across the PORV of 54 and 57 psid, respectively. The RELAP modeling then of the flow resistance offered by the PORV to subcooled liquid flow shows to be accurate and on the conservative side. (A review recommendation is, *however, that PORV modeling be done differently in future RELAP LTOP calculations. The current RELAP modeling of the PORV for subsonic single-phase flow (e.g., cold liquid water flow) is not clean. Use of the abrupt expansion model should be replaced by the inclusion of a physical flow coefficient (Cv) table. The current PORV modeling is conservative because the area of the orifice in the valve has been defined 18% smaller than physical. Were a physically representative orifice area defined, the flow resistance offered to cold flowing liquid would be too low and non-conservative. A physically representative Cv value of 26.99 for a full-open FCS PORV is calculated in Attachment 1.)*

Response: Other than a conservative, brief ramp to open, the PORVs are modeled as constant area. The modeling is consistent with other sites, the RELAP manual examples, and with American Petroleum Industry relief valve equations. The valve area used was back calculated using trial and error to produce flows consistent with the design condition (this is what resulted in the area being 18% smaller than listed in the spec sheet). The area reduction could be considered as equivalent in bottom line effect to determining the valve full-open Cv value and helps to explain the good agreement between the reviewer's hand calc and the RELAP result. RELAP addresses subcooled liquid as well as liquid that flashes to two phases. Of course, in general, it is best to use manufacturer's Cv values, but such data was not available. We are confident that the flow equations used are good for this purpose. We also note in the final report that the flow rate at the time of PORV opening is much greater than injection rate, and so small errors in PORV flow rate will have no impact on peak pressure since the PORV is more than adequate at reducing the pressure as soon as it is opened for all scenarios. (Note: the reviewer calculated different pressure drops for the same flow rate. In LTOP analysis, the PORV setpoint and the assumed downstream pressure determine the pressure drop, and the flow rate is calculated.)

RCS Pressurization Rate in Mass-Addition Scenarios

A hand calculation was made to verify the time taken for the RCS to pressurize to the PORV set point in the base mass-addition LTOP case. This was done on account of questions that arose in the course of the review regarding the seemingly slow pressurization rate in the RELAP calculation of further water addition to a water-solid system. The calculation is included as Attachment 2. It simply relates the charging flow rate to the volume of the RCS and the compressibility of liquid between the initial RCS pressure and the PORV set point. The hand calculation and the RELAP calculation predict an elapse of 19.5 and 18.3 sec, respectively, from the time charging flows initiate to the time the PORV set point is reached. This fair comparison satisfied the review questions regarding pressurization rate.

Response: ENERCON also did hand calculations to estimate pressurization rate and found agreement with the RELAP model.

Reactor Coolant Pumps

The heating of RCS inventory associated with irreversible flow losses in the system is accounted for in the RELAP model by depositing energy in the fluid as it flows through the reactor coolant pumps. This is appropriate but there is a conservatism here that may have been overlooked. By default, RELAP deposits the irreversible loss associated with wall friction into the fluid locally as heat. Typically wall friction accounts for roughly half of the flow loss in an RCS; the other half being attributed to "minor"-type flow losses through fittings, abrupt expansions and contractions, etc. Minor-type flow losses are not deposited in the fluid as heat by RELAP. The heat additions made then to the RELAP calculations to account for reactor coolant pump operation are roughly 50% higher than physical.

Response: Early runs without RCP heat slabs showed that the RCP heat was not being added through friction at a conservative rate, perhaps for the reason that the reviewer notes. The heat slabs were added as an after thought to assure model conservatism. The additional friction heat conservatism was not mentioned because it was small compared to other conservatisms, and it was difficult to explain and quantify.

It was noticed that in cases where a reactor coolant pump was not operating, the pump component was removed from the RELAP model and a simplistic control-volume component was substituted. It is unclear why this was done. A substantial effort was clearly made in the modeling the pumps as evidenced by the complete set of homologous curves defined. It would be good to take advantage of the thorough pump modeling given the reverse loop flows that develop in many of the LTOP scenarios. If the reason for removing the pumps was robustness-related (e.g., code stops), it would have been good to state this in the LTOP report. In any case, it would have been good to include a description of how the resistance offered by a stopped reactor coolant pump to reverse flow was captured in the surrogate component.

Response: the text of Reference 2 states: "Fort Calhoun RCPs have anti-reverse-rotation devices, so all the secured RCPs (which will all have reverse flow) are modeled with a loss coefficient as described in Attachment 2." In attachment 2, the loss coefficient is described and the reference given. This is consistent with other Fort Calhoun models.

Volume Control Flags

It was noticed in the course of the review that a handful of control volumes had the calculation of wall friction disabled. It is unclear why this was the case. If this was inadvertent, it would be good to enable friction in these control volumes for consistency.

Response: wall friction decisions were described in Attachment 2 to Reference 2. Cases where wall friction was set to zero were based on consistency with existing plant models, such as the design basis CESEC model. For example, under component 330, "CESEC Node 12 neglects friction losses (hydraulic diameter = .9E99) so this model does likewise. The hydraulic diameter is set to 100 ft and the control flag is set to 0010 to ignore friction losses."

Pressurizer

The pressurizer is modeled as a single stack of 6 control volumes. With respect to interfacial heat and mass transfer considerations, it would be better to use either 2 or more adjacent stacks of cells or simply a single cell to represent the pressurizer. The reason for this is the tendency for unrealistic stratification to develop. In an actual pressurizer, the liquid inventory is well mixed by circulative natural convection flows. In a single stack of control volumes, RELAP has no way to develop such flows. Consequently, stratified layers of largely varying temperature can develop. Relatively cold layers can unrealistically sit atop relatively hot layers. This unphysical stratification can impact the realism of the interfacial heat and mass transfer calculated by RELAP between the liquid region and vapor space of the pressurizer.

Response: The OPPD LTOP pressurizer model is consistent with other sites and RELAP manual examples. The "reverse stratification" does not occur for cases with pressurizer bubbles because the pressurizer is the hottest location in the RCS. This means cooler water is introduced through the surgeline. If ever the surgeline flow were hotter than the saturated water at the top, it would boil. The output contained in file hp503s30.o, for example, ends up in the final edit statement with pressurizer temperature from bottom to top of 268° F, 314 ° F, 350 ° F, 395 ° F, 423 ° F, and 449 ° F. However, for the mass addition cases that are originally water solid with constant temperature RCS, slightly warmer fluid is introduced as the decay heat warms up the fluid. The temperatures in the final edit statement in m305p2.o are, from bottom to top, 310.4 °, 310.1 °, 307.4 °, 306.5 °, 306.4 ° and 306.3 ° F. This has no impact on peak pressure because the PORV flow rate easily causes the pressure to fall, that is, there is no consequence to a small change in PORV flow due to a few degrees difference. Note: use of a single cell as recommended by ITS would be less accurate for pressurizer bubble cases, since it would eliminate the temperature stratification that we expect to be there.

The pressurizer inventory in the heat addition LTOP scenarios was appropriately initialized saturated. In the mass addition cases, however, the pressurizer inventory was initialized at the initial temperature of the RCS. This seems questionable given that 1) before the spurious injection, the pressurizer inventory would have been saturated at the initial pressure of the RCS, and that 2) the pressurizer heaters are assumed to be operating as the pressurizer fills with liquid. It might be more defendable to start mass-addition scenarios with a realistic pressurizer condition (i.e., saturated with level in the nominal range) and then *allow the pressurizer to fill with the heaters operating. It could be that the pressure drop across the PORV will differ meaningfully dependent upon the temperature of the liquid in the pressurizer. (This might especially be true if the liquid temperature were greater than the saturation temperature downstream of the PORV.)*

Response: In the mass addition cases, there is no pressurizer bubble so there is no reason for the pressurizer to be at saturated temperature. Reference 2 does include one case (Case 4) with saturated pressurizer and bubble to see if the bubble made a difference, and determined that it had no significant impact since the bubble collapsed prior to PORV lift. As to PORV flow rate differences, as noted in Reference 2, the PORV flow rate is much higher than is needed to start a pressure decrease, so changes in flow due to different inlet temperatures do not affect peak pressure.

Steady State

A review recommendation is that in future LTOP analyses documentation, results be presented of an extended steady-state RELAP calculation. The objective of including the steady-state results would be to identify close correspondence between the RELAP LTOP model and actual FCS monitored parameters. The calculation should have reactor power at the full operating value, and should include realistic feedwater temperature and active steam generator level control. The goal here would be to convincingly illustrate the base realism of the RELAP model.

Response: We did compare realistic steady-state results to other models in Reference 2, Table A2-3, as recommended.

Steam Generators

The secondary side of the steam generators and the steam generator tubing metal mass were conservatively excluded from the mass-addition scenarios. In the heat addition scenarios, the generators were initialized entirely full of liquid which was hot relative to RCS temperature. Initializing the steam generators full of liquid seems unrealistically conservative. A suggestion of the review is that future heat addition LTOP calculations be initialized with steam generator level in the nominal range consistent with where the operators would maintain it.

Response: It is agreed that the Reference 2 steam generator treatment is clearly conservative, but it allowed for a simpler boundary of the model. If we had used a realistic amount of water, we would also have to include heat transfer from the outer steam generator walls. We would also have had to model steam condensing as the secondary side cooled, including models of the metal mass and surfaces in the steam region. Note: the model does include the steam generator tubing metal mass in the heat addition scenarios.

Summary

In summary, the model shortcomings identified in the course of the review are not thought to have the potential to meaningfully impact the conclusions of OPPD's current RELAP LTOP calculations. The RELAP model seems well suited to performing LTOP transients and is very well documented. Modeling uncertainties appear to have been consistently addressed in a conservative manner.

Response: While we appreciate the conclusion, we do not agree that any model shortcomings exist. Where the reviewer identifies conservatisms, we believe the conservatisms are small and justified for their simplification of the model. Since better PORV flow data is not available, and pressurizer reverse stratification does not affect the peak pressure calculation, we do not see any advisable changes to the model.

Questions on this response can be addressed to Ralph Berger or myself at 510-632-1734.

Sincerely,

Tien Lee
Engineering Manager
Enercon Services, Inc.

TPL/jtn

Appendix 3
Verification of PORV Flow Rates
-
Independent Check Of RELAP PORV Water Flow Rates Based On Inlet Conditions, Exit Pressure And 0.77 Square Inches Relief Area

Three cases from Reference D were checked: Case 2, Case 7 and Case 8. The methodology used is taken from the American Petroleum Industry (API) Standard 520. It is important to note that this methodology requires a relief valve coefficient k_d. API recommends, in the absence of other data, to assume k_d = 0.85 (the sensitivity is such that higher values of k_d result in higher flow rates, since flow is proportional to k_d). Trial and error found that a value of k_d of 0.62 provided a close match, which means that the RELAP water flow rate is lower by about 15% relative to the default API approach for a generic relief valve. In the below cases, the value of the three coefficient product $k_d k_b k_c$ is set to 0.62, however evaluation of the methodology identifies that k_b and k_c should be 1.0 under these conditions so this term really represents just k_d..

Results:

Case	Upstream P (psia)	Upstream T (°F)	Downstream P (psia)	RELAP Flow Rate (lb_m/s)	API Flow Rate k_d=0.62 (lb_m/s)
2	484	50	90	49.5	50.1
7	1071	255	90	76.4	76.6
8	1522	305	90	90.4	91.3

<u>CASE 2 API SOLUTION</u>

CALCULATION OF TWO-PHASE FLOW RATE

This calculation is based on the specification of an inlet state, an outlet pressure, and a relief path, including area and loss terms. The flow rate through this path is calculated based on the following references:

- The American Petroleum Institute (API) Recommended Practice 520, Sizing Selection and Installation of Pressure Relieving Devices in Refineries, Appendix D, 7th edition, January 2000.
- The Crane Manual, also known as Flow of Fluids through Valves, Fittings, and Pipe, Technical Paper No. 410, The Crane Company, Twentieth printing, 1981.
- Easily Size Relief Devices and Piping for Two-Phase Flow, Joseph Leung, Chemical Engineering Progress, December 1996.

These references are referred to below as the API, Crane, and Leung, respectively.

INLET STATE

The water inlet state is subcooled water. The pressure is 484.0 psia and temperature is 50.0°F.

There is no non-condensable gas present.

Water/steam state properties are as follows, where f indicates fluid, g indicates gas, and o indicates inlet state. Water/steam saturation properties are given for a temperature of 50.0°F and pressure of 0.18 psia.
Enthalpies: ho=19.43, hfo =18.05, hgo =1083.40 Btu/ lb_m
Specific Vols: vo=0.01586, vfo =0.01602, vgo =1704.80000 ft3/ lb_m
Densities: po=63.0673, pfo =62.41, pgo =5.8657E-4 lb_m/ft3
Entropies: so=0.04729, sfo =0.03610, sgo =2.12620 Btu/ lb_m°F
Spec. heats: cpo=1.002, cpfo =1.002, cpgo =0.444 Btu/ lb_m°F

Based on the backpressure of P2 = 90.0 psia, flashing will not occur.

CALCULATION

Step 3: Calculation and Final Result

The specific heat ratio cp/cv is calculated using Figure A-9 from Crane. A value of 1.2751 is interpolated based on a temperature of 50.0°F and pressure of 484.0 psia.

This is subcooled water with no non-condensables present and Po<1604 psia and To<634.5°F. The appropriate formula for the omega factor is D.8 from the API:

$$omega=0.185/vo*cpfo*(To+460)*Ps*(vgo-vfo)^2/(hgo-hfo)^2$$

Ps is the saturation pressure associated with To. With To = 50.0°F, the saturation pressure is 0.2 psia. Here the specific volume and enthalpy changes are evaluated at Ps and are: vgo=1704.800 ft3/ lb_m, vfo=0.01602 ft3/ lb_m, hgo=1083.4 Btu/ lb_m, and hfo=18.05 Btu/ lb_m. Putting this into the equation for omega gives an omega value of 2717.02.

To determine whether this is a low subcooling or high subcooling region, we calculate the parameter nst = 2*omega/(1+2*omega) to be 0.9998. Since the saturation pressure at a To of 50.0F, calculated to be 0.178 psia, is less than nst*Po = 483.911 psia, this is a high subcooling region. Since the saturation pressure is less than the downstream pressure P2, which is 90.0 psia, critical flow is not achieved. The mass flux G is given by the API Equation D.11:

G=96.3*SQRT[(Po-P2)/vfo]

G is calculated to be 15100.888 lb_m/ft^2.

The value of 0.62 was provided for KdKbKc.

The flow rate through an area of 0.7700 square inches is given by the formula W = kdKbKc*A*G/0.04 and is 180229.10 lb_m/hr or 50.0636 lb_m/s. This is equivalent at a specific volume of 0.0159 ft^3/ lb_m to 47.629 cubic feet/minute, or 371.981 gpm.

The exit state is subcooled water at 90.0 psia and 49.02°F. Properties are:
Enthalpy: h2=18.45 Btu/ lb_m
Specific Vol: v2=0.01585 ft^3/ lb_m
Density: p2=63.0868 lb_m/ft^3
Entropy: s2=0.04568 Btu/ lb_m°F
Spec. heat: cp2=1.002 Btu/ lb_m°F

CASE 7 API SOLUTION

CALCULATION OF TWO-PHASE FLOW RATE

This calculation is based on the specification of an inlet state, an outlet pressure, and a relief path, including area and loss terms. The flow rate through this path is calculated based on the following references:

- The American Petroleum Institute (API) Recommended Practice 520, Sizing Selection and Installation of Pressure Relieving Devices in Refineries, Appendix D, 7th edition, January 2000.
- The Crane Manual, also known as Flow of Fluids through Valves, Fittings, and Pipe, Technical Paper No. 410, The Crane Company, Twentieth printing, 1981.
- Easily Size Relief Devices and Piping for Two-Phase Flow, Joseph Leung, Chemical Engineering Progress, December 1996.

These references are referred to below as the API, Crane, and Leung, respectively.

INLET STATE

The water inlet state is subcooled water. The pressure is 1071.0 psia and temperature is 255.0°F.

There is no non-condensable gas present.

Water/steam state properties are as follows, where f indicates fluid, g indicates gas, and o indicates inlet state. Water/steam saturation properties are given for a temperature of 255.0°F and pressure of 32.53 psia.
Enthalpies: ho=225.97, hfo =223.67, hgo =1165.75 Btu/ lb_m
Specific Vols: vo=0.01702, vfo =0.01705, vgo =12.74300 ft^3/ lb_m
Densities: po=58.7698, pfo =58.66, pgo =0.0785 lb_m/ft^3
Entropies: so=0.37099, sfo =0.37485, sgo =1.69305 Btu/ lb_m°F
Spec. heats: cpo=1.014, cpfo =1.014, cpgo =0.511 Btu/ lb_m°F

Based on the backpressure of P2 = 90.0 psia, flashing will not occur.

CALCULATION

Step 3: Calculation and Final Result

The specific heat ratio cp/cv is calculated using Figure A-9 from Crane. A value of 1.2564 is interpolated based on a temperature of 255.0°F and pressure of 1071.0 psia.

This is subcooled water with no non-condensables present and Po<1604 psia and To<634.5°F. The appropriate formula for the omega factor is D.8 from the API:

omega=0.185/vo*cpfo*(To+460)*Ps*(vgo-vfo) ^{2}/(hgo-hfo) ^{2}

Ps is the saturation pressure associated with To. With To = 255.0°F, the saturation pressure is 32.5 psia. Here the specific volume and enthalpy changes are evaluated at Ps and are: vgo=12.743 ft^3/lb_m, vfo=0.01705 ft^3/lb_m, hgo=1165.8 Btu/lb_m, and hfo=223.67 Btu/lb_m. Putting this into the equation for omega gives an omega value of 46.78.

To determine whether this is a low subcooling or high subcooling region, we calculate the parameter nst = 2*omega/(1+2*omega) to be 0.9894. Since the saturation pressure at a To of 255.0F, calculated to be 32.532 psia, is less than nst*Po = 1059.674 psia, this is a high subcooling region. Since the saturation pressure is less than the downstream pressure P2, which is 90.0 psia, critical flow is not achieved. The mass flux G is given by the API Equation D.11:

G=96.3*SQRT[(Po-P2)/vfo]

G is calculated to be 23101.321 lb_m/ft^2.

The value of 0.62 was provided for KdKbKc.

The flow rate through an area of 0.7700 square inches is given by the formula W = kdKbKc*A*G/0.04 and is 275714.26 lb_m/hr or 76.5873 lb_m/s. This is equivalent at a specific volume of 0.0170 ft3/ lb_m to 78.190 cubic feet/minute, or 610.667 gpm.

The exit state is subcooled water at 90.0 psia and 255.56°F. Properties are:
Enthalpy: h2=226.54 Btu/lb_m
Specific Vol: v2=0.01702 ft^3/lb_m
Density: p2=58.7543 lb_m/ft^3
Entropy: s2=0.37179 $Btu/lb_m{}^\circ F$
Spec. heat: cp2=1.014 Btu/ $lb_m{}^\circ F$

CASE 8 API SOLUTION

CALCULATION OF TWO-PHASE FLOW RATE

This calculation is based on the specification of an inlet state, an outlet pressure, and a relief path, including area and loss terms. The flow rate through this path is calculated based on the following references:

- The American Petroleum Institute (API) Recommended Practice 520, Sizing Selection and Installation of Pressure Relieving Devices in Refineries, Appendix D, 7th edition, January 2000.
- The Crane Manual, also known as Flow of Fluids through Valves, Fittings, and Pipe, Technical Paper No. 410, The Crane Company, Twentieth printing, 1981.
- Easily Size Relief Devices and Piping for Two-Phase Flow, Joseph Leung, Chemical Engineering Progress, December 1996.

These references are referred to below as the API, Crane, and Leung, respectively.

INLET STATE

The water inlet state is subcooled water. The pressure is 1522.0 psia and temperature is 305.0°F.

There is no non-condensable gas present.

Water/steam state properties are as follows, where f indicates fluid, g indicates gas, and o indicates inlet state. Water/steam saturation properties are given for a temperature of 305.0°F and pressure of 72.19 psia.
Enthalpies: ho=277.54, hfo =274.85, hgo =1181.15 Btu/ lb_m
Specific Vols: vo=0.01740, vfo =0.01750, vgo =6.02910 ft^3/ lb_m
Densities: po=57.4676, pfo =57.14, pgo =0.1659 lb_m/ft^3
Entropies: so=0.44080, sfo =0.44395, sgo =1.62910 Btu/ lb_m°F
Spec. heats: cpo=1.028, cpfo =1.028, cpgo =0.556 Btu/ lb_m°F

Based on the backpressure of P2 = 90.0 psia, flashing will not occur.

CALCULATION

Step 3: Calculation and Final Result

The specific heat ratio cp/cv is calculated using Figure A-9 from Crane. A value of 1.2529 is interpolated based on a temperature of 305.0°F and pressure of 1522.0 psia.

This is subcooled water with no non-condensables present and Po<1604 psia and To<634.5°F. The appropriate formula for the omega factor is D.8 from the API:

$$omega=0.185/vo*cpfo*(To+460)*Ps*(vgo-vfo)^2/(hgo-hfo)^2$$

Ps is the saturation pressure associated with To. With To = 305.0°F, the saturation pressure is 72.2 psia. Here the specific volume and enthalpy changes are evaluated at Ps and are: vgo=6.029 ft3/lb_m, vfo=0.01750 ft^3/lb_m, hgo=1181.2 Btu/lb_m, and hfo=274.85 Btu/lb_m. Putting this into the equation for omega gives an omega value of 26.55.

To determine whether this is a low subcooling or high subcooling region, we calculate the parameter nst = 2*omega/(1+2*omega) to be 0.9815. Since the saturation pressure at a To of 305.0°F, calculated to be 72.185 psia, is less than nst*Po = 1493.866 psia, this is a high subcooling region. Since the saturation pressure is less than the downstream pressure P2, which is 90.0 psia, critical flow is not achieved. The mass flux G is given by the API Equation D.11:

G=96.3*SQRT[(Po-P2)/vfo]

G is calculated to be 27547.283 lb_m/ft^2.

The value of 0.62 was provided for KdKbKc.

The flow rate through an area of 0.7700 square inches is given by the formula W = kdKbKc*A*G/0.04 and is 328776.82 lb_m/hr or 91.3269 lb_m/s. This is equivalent at a specific volume of 0.0174 ft^3/lb_m to 95.351 cubic feet/minute, or 744.694 gpm.

The exit state is subcooled water at 90.0 psia and 307.12°F. Properties are:
Enthalpy: h2=279.75 Btu/lb_m
Specific Vol: v2=0.01743 ft^3/lb_m
Density: p2=57.3869 lb_m/ft^3
Entropy: s2=0.44353 Btu/ lb_m°F
Spec. heat: cp2=1.029 Btu/ lb_m°F

<u>Reference D:</u> FC06877, Rev. 0, "Low Temperature Overpressure Protection (LTOP) Analysis, Revision 1." [Note: This Reference was included in Letter from OPPD (D. J. Bannister) to NRC (Document Control Desk) dated October 8, 2002, Fort Calhoun Station Unit No. 1 License Amendment Request, "Reactor Coolant System (RCS) Pressure and Temperature Limits Report (PTLR)" (LIC-02-0109)]

Appendix 4

Evaluation of the Massachusetts Institute of Technology (MIT) Test Results

A concern was raised in the review of Reference 4-3 (below) that the code used, RELAP5/ MOD3.2, had been shown to inaccurately predict the pressure behavior discovered in Reference 4-1. Two reasons for this difference are apparent at first view.

The first difference is that the MIT experiment (Reference 4-1) is more dramatic than the Fort Calhoun LTOP transients analyzed in Reference 4-3. The level in the pressurizer model rises from 17 inches to 34 inches in 31 seconds (Experiment BB4) which compresses the steam volume vertical distance from an initial 28 inches to 11 inches (from 100% volume to 36% volume) in 31 seconds. By comparison, the Reference 4-3 transient Case 10 is from 350 ft^3 to 200 ft^3 in 100 seconds (100% to 57%), or roughly six times slower.

The second difference is that the test setup had a very small interface surface area compared to volume (8 inch diameter versus 28 inch height, whereas the Fort Calhoun steam bubbles had a diameter = 6.86 feet and a height 9.75 feet).

The Reference 4-3 model was adiabatic, in that no wall heat transfer was assumed in the pressurizer (this was identified as a known conservatism). Reference 4-2, Figure 1 implies that adiabatic models greatly over predict pressure rise when trying to model a small, skinny tank with high rate of bubble compression. The MIT paper also concludes adiabatic models will greatly over predict pressure when modeling this transient setup.

The experiments in the MIT tests that are applicable to Fort Calhoun's LTOP analyses are the insurge to partially filled tanks (cases ST4, BB4, and TR8). This can be simulated with the FCS pressurizer model assuming the following changes:

Existing Model	New Model
Vol = 900 ft^3	Vol = pi*$(8/24)^2$*45/12 = 1.309 ft^3
Length = 24.364 ft	Length = 45/12 = 3.75 ft
Area = 36.94 ft^2	Area = 0.394 ft^2

Input for Experiment BB4
Initial P = 70.1 psia
Initial T = 303°F
Initial water level = 17 inch
Insurge T = 70°F
Insurge flow rate = level change * area/time = 16/12*0.394/31 = 0.017 ft^3/s = 7.6 gpm = 1.06 lb_m/s at a density of 62.3 lb_m/ft^3
Insurge flow time = 31 seconds
Data for pressure history: Figure A.1.1 pg 67

The Fort Calhoun pressurizer model from Reference 4-3 is as follows:

```
* PRESSURIZER
4100000  pres  pipe
4100001  6
4100101  36.94,6
4100201  36.94,5
4100301  2.9232, 5
4100302  9.744, 6
4100601  90.  6
4100801  0.00015  0.0  6
4100901  0.0   0.0   5
4101001  0,6
4101101  0,5
*Manually set Przr pressure and water level
4101201  2  95.  0.0    0.  0 0,5
4101202  2  95.  1.0    0.  0 0,6
4101300  1
4101301  0.0   0.0   0.0,5
```

Experiment BB4

The Fort Calhoun Pressurizer model is revised to match the experiment case, for an initial water level of 17 inch (1.42 ft) with 5 volumes of height 1.42/5=0.283 and the bubble volume of height (45-17)/12 = 2.333 ft, and initial temperature of 70.1°F.

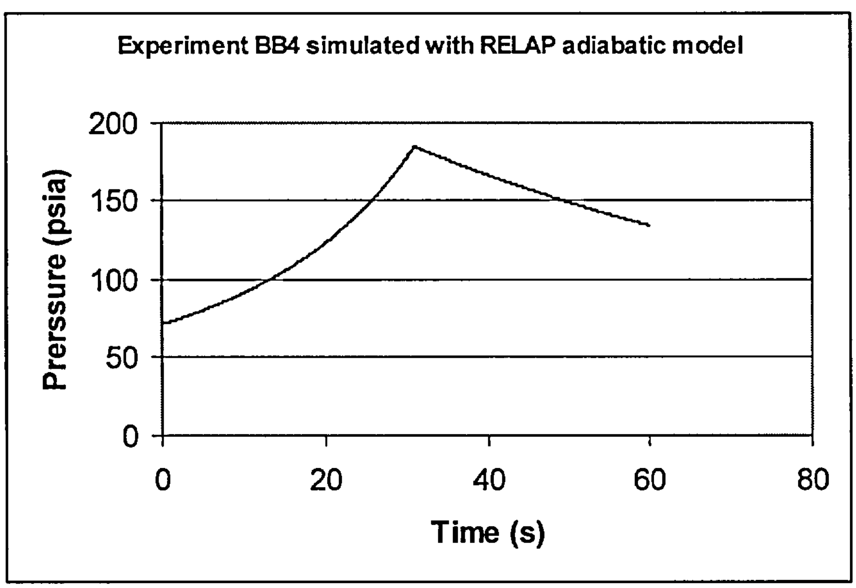
```
* PRESSURIZER
4100000  pres  pipe
4100001  6
4100101  0.394,6
4100201  0.394,5
4100301  0.283, 5
4100302  2.333, 6
4100601  90.  6
4100801  0.00015  0.0  6
4100901  0.0   0.0   5
4101001  0,6
4101101  0,5
*Manually set Przr pressure and water level
4101201  2  70.1  0.0    0.  0 0,5
4101202  2  70.1  1.0    0.  0 0,6
4101300  1
4101301  0.0   0.0   0.0,5
```

The boundary condition is setup with a forced flow rate 1.06 lb_m/s for 31 seconds. This is simulated with two components, a 70°F reservoir and a time dependent junction. The complete input file is as follows:

```
=MIT Experiment Model
*
*
100 new transnt
102 british british
105
*****
*  time step control
*
201  90.0  1.0-6 0.001 5 100 250    1000
*
* Output control
*
301 p 410060000
*
*  trip cards
*  501 run stop time
*  502  insurge start time
501  time  0         ge  null  0    60.0      1
502  time  0         lt   null   0    31.0       n
600  501
*
* Source of insurge to set P & T
2510000      si2      tmdpvol
* flow area, length, volume, horiz angle, vert angle
2510101      50.0  10.0      0.0     0.0     0.0
* elev change, roughness, hydraulic diameter, flags
2510102      0.0      0.0       0.0    00
* 3 makes 201 card P&T, trip number
2510200       3
2510201      0.0    90.0     70.0
2510202     1000.0   90.0     70.0
*
* Insurge flow rate
2520000      insurge tmdpjun
* from, to, area
2520101      251000000    410000000    0.1
* 1 means mass flows/0=velocity, trip number, table var & location
2520200     1            502        p     410010000
* pressure, liq lbm/s, vapor velocity, interface (=0) 1 Pump Curve
2520201        0.0        0.0           0.0        0.0
2520202       10.0        1.06         0.0        0.0
2520203     2000.0       1.06         0.0        0.0
```

```
*
* PRESSURIZER
4100000  pres  pipe
4100001  6
4100101  0.394,6
4100201  0.394,5
4100301  0.283, 5
4100302  2.333, 6
4100601  90.  6
4100801  0.00015  0.0  6
4100901  0.0  0.0  5
4101001  0,6
4101101  0,5
*Manually set Przr pressure and water level
4101201  2  70.1  0.0   0.  0 0,5
4101202  2  70.1  1.0   0.  0 0,6
4101300  1
4101301  0.0  0.0  0.0,5
*
* end of cases
.
```

What happens is similar to what is shown on page 62 of Reference 4-1, or in Figure 1 of Reference 4-2. The FCS adiabatic model shows a much higher pressure rise. The peak pressure calculated was 184 psia, and peak temperature was 490°F.



However, this is not similar to the Fort Calhoun LTOP transient. As noted, the Reference 4-3 flow rate is much slower and the relative surface area to volume is much greater. To do a better comparison, one should use a consistent area to height ratio and a consistent level rise rate. To get a consistent area to height ratio for the test bubble height of 2.333 ft, the interfacial area should be:

=ht * LTOP Area/LTOP bubble height = 2.33 * 36.94/9.75 = 8.83 ft^2 instead of 0.394 ft^2

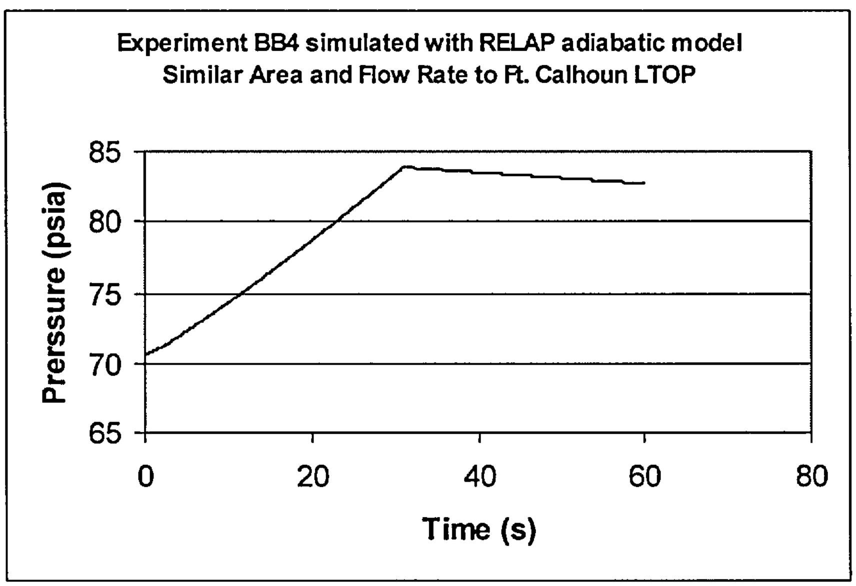
The insurge rate to get a decrease in volume from 100% to 57% in 100 seconds would be:

= volume/time = 8.83*.43*2.33/100 = 0.0885 ft^3/s = 5.51 lb_m/s

Making these two changes by adding the following lines to the input file:

```
*Changes for a better comparison
4100101  8.83,6
4100201  8.83,5
2520202     10.0      5.51      0.0      0.0
2520203   2000.0      5.51      0.0      0.0
```

Gives the following pressure trace



This is the pressure predicted by RELAP for a similar bubble shrinkage rate (as occurs at Fort Calhoun during a heat addition case) for the MIT test case if the MIT test case had a surface area in proportion to the bubble height. Here the effects of the rapid shrinkage is reduced and the heat transfer to the liquid phase is increased. The pressure rise is still probably higher than actual (MIT's case BB4 had a peak pressure of 81 psia) but the effect of neglecting heat transfer to the pressurizer wall are obviously much reduced.

Conclusions

The RELAP model used at Fort Calhoun is an adiabatic model. References 4-1, 4-2 and our simulation agree that the adiabatic model predicts extremely high pressures for rapid insurge, small area tanks. The reason is that the steam space gets extremely hot due to compression and the water/steam surface area is insufficient to remove the heat. Our simulation of the test BB4 predicted a steam temperature of 490°F, while the test measured temperature was only about 310°F. Obviously, in the test case, heat has been transferred from the steam to the pressurizer vessel.

There are two things to note for the LTOP transient. The first is that the adiabatic assumption has a much less effect for our slower transients and much larger interfacial area. Had the MIT experiment been performed with a surface area to height ratio and bubble compression rate similar to the Fort Calhoun transient, the heat transfer to tank walls would have been a much less significant factor. The adiabatic pressure rise predicted by RELAP for this proposed test is only 14 psi.

The second thing to note is that the Fort Calhoun pressurizer model is conservative in terms of predicting peak pressure. Crediting the heat transfer to the pressurizer walls would provide a mechanism for removing energy from the primary system and keeping the pressure lower. It is true that additional heat transfer to the pressurizer walls might collapse the steam bubble faster; however, this is not a significant effect because even with the adiabatic assumption the steam temperature does not rise very much (in Case 10 of Reference 4-3, the temperature rises only 4° F), so the heat transfer would be small. In any case, the remaining bubbles (for all but one case where the PORV lifted) stay well above 100 ft^3.

Overall, the effects noted in the MIT tests (i.e. the adiabatic assumption) are insignificant for a real pressurizer LTOP event geometry and insurge rates, so it is concluded that the adiabatic assumption is both of minor consequences and conservative in terms of calculating peak pressure.

<u>References</u>:

4-1.1. Insurge Pressure Response and Heat Transfer for PWR Pressurizer, Hamid Reza Saedi, Masters Thesis MIT, 11/82.
4-1.2. Prediction of MIT Pressurizer Data using RELAP5 and TRAC-M, Shumway, Bolander, and Aktas, ICONE-10 paper 22580, 10th International Conference on Nuclear Engineering, 4/14-18/02, Alexandria, VA (Located in Appendix 5).
4-1.3. FC06877, Rev. 0, "Low Temperature Overpressure Protection (LTOP) Analysis, Revision 1."[Note: This Reference was included in LIC-02-0109.]

Appendix 5

Prediction of MIT Pressurizer Data using RELAP5 and TRAC-M

Proceeding of ICONE-10
10th International Conference on Nuclear Engineering
April 14-18, 2002, Alexandria, VA

ICONE-10 22580
DRAFT

Prediction of MIT Pressurizer Data using RELAP5 and TRAC-M

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ABSTRACT

Tests simulating Pressurized Water Reactor pressurizers under inflow and outflow conditions have been performed at MIT. Prediction of pressurizer pressure requires accurate models of wall heat transfer as well as interfacial liquid-steam heat transfer. The US NRC has two computer programs used for predicting thermal hydraulic behavior in reactors; RELAP5 and TRAC-M. TRAC-M is the Consolidated Thermal-hydraulics Code developed by combining models from TRAC-B and RELAP5 into a modernized version of TRAC-P. The component models from RELAP5 and TRAC-B have been ported to TRAC-M but not the constitutive models. A suite of assessment cases are being developed to guide the constitutive model improvement process. Assessment against data will determine which constitutive relations need to be ported to TRAC-M. This paper compares the RELAP5 and TRAC-M codes against MIT pressurizer data. As water is injected into the bottom of the pressurizer the steam pressure in the top of the pressurizer rises. The pressure increase rate is controlled by wall and interface condensation rates. Both codes predict this complex compression process reasonably well. The effect of time step size and code options are explored in this paper. The benefits of using two codes to analyze thermal-hydraulic processes are evident from the results.

NOMENCLATURE

h:	heat transfer coefficient
k:	liquid thermal conductivity
δ	film thickness

INTRODUCTION

Experimental and analytical work on pressurized water reactor pressurizers was performed at MIT (Saedi and Griffith, 1983). This paper concentrates on Test number ST4 by comparing the TRAC-M (Aktas and Uhle, 2000) and RELAP5 (ISL Inc., 2001a) computer codes to the data. TRAC-M began as a modernized version of TRAC-P (Spore et al., 1993). The RELAP5 and TRAC-M thermal-hydraulic codes are used by the US NRC to aid reactor safety decision making.

Important phenomena include: wall condensation, mixing of incoming cold water with already present hot water in the vessel and free surface heat transfer.

Predictions from RELAP5/MOD3.3 version al and TRAC-M version 3927 are shown. Code options examined include: thermal front tracking, level tracking, numerical implicitness and time step size sensitivity.

TEST DESCRIPTION

Test ST4 was an insurge experiment. Water, subcooled by 130K, was injected into a stainless steel vessel which was partially filled with saturated water at a pressure of 0.49 MPa. The steam in the upper part of the vessel was compressed. As the saturation temperature rose, the vessel walls became subcooled and film condensation occurred. The condensate ran down the walls to meet a rising water level. A balance between interfacial and wall steam condensation and steam compression determined the pressure response.

The test vessel was 114.3 cm high, had an I.D. of 20.3 cm, and a wall thickness of 0.818 cm.

Water injection into the bottom of the vessel varied over the first 40.6 seconds at which time it was stopped. The injection rate translated into a vessel water level rise rate of about 1 cm/s.

The vessel was insulated to diminish energy losses. Calibration tests were used to estimate the losses at 1.1 kW (Kim and Griffith, 1987).

CODE MODEL

The vessel was modelled using 10 fluid cells. A more accurate prediction could be obtained with more cells, however, models of reactor pressurizers usually have less than 10 cells.

The water level was initially in cell 4 (the void fraction was 0.22) and reached its maximum value in cell 8 (the void fraction was 0.69).

The experimenters did not report on the type and thickness of the insulation covering the vessel. The code model used 8.9 cm of fiber glass insulation. Steady state calculations were performed to adjust the insulation conductivity so that the steady state heat loss agreed with the reported value.

CODE RESULTS

Measured pressure in the top of the vessel peaked at about 0.59 MPa as shown in Fig 1. After the subcooled water insurge stops, the pressure falls due to further steam condensation. The complex physical processes occurring are: wall heat transfer, steam-water interfacial heat transfer, and thermal mixing between the cold and hot water.

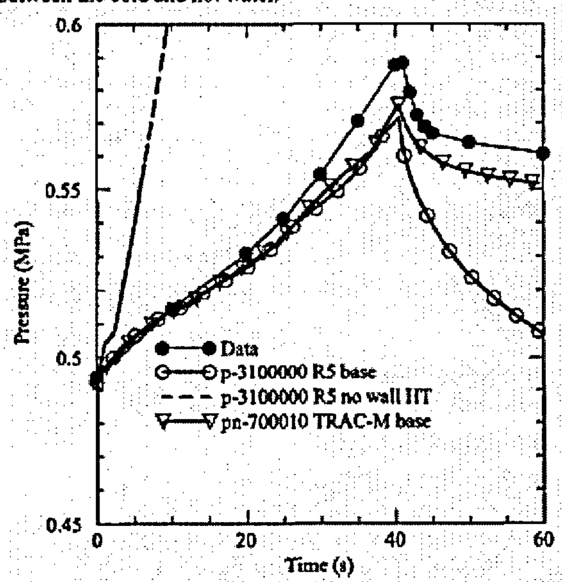


Figure 1. Base case prediction of pressure.

The two codes show fairly accurate results during the compression period but RELAP5 has too much condensation after the water insurge was stopped.

Wall and Interface

During the compression process, wall condensation is the controlling phenomena. Figure 1 shows the pressure rise rate when the vessel wall heat transfer is removed from the RELAP5 model. This demonstrates wall heat transfer is very important. Both codes use the filmwise condensation coefficient correlation developed by Nusselt (1916).

RELAP5 predicts a liquid to interface heat transfer coefficient times area value of about 3000. When the liquid and vapor interfacial heat transfer coefficients were set to 1.0 internally, the effect on the peak pressure was negligible. This implies interfacial heat transfer is not important.

A study of the reason for the pressure drop rate differences between the two codes, after the water insurge stopped, showed model deficiencies in both codes in the cell with the water level. TRAC-M switches from wall condensation heat transfer mode to liquid convection mode when a water level enters a cell. Shutting off wall condensation when the water level reached cell 8 caused the noticeable pressure increase change at 36 seconds in Fig. 1.

When a water level enters a RELAP5 cell, the code partitions the wall energy transfer into the regions above and below the water level. However, the condensation film thickness is based upon the average liquid flow across both the cell inlet and outlet junctions. When water enters from below, only the flow from above should be used to determine the film thickness used in the Nusselt (1916) condensation heat transfer coefficient:

$$h = \frac{k}{\delta} \tag{1}$$

Using the average liquid flow rate results in a large thickness and a small heat transfer coefficient when water is flowing into the cell from below. When the water flow stops, the calculated thickness is small and the heat transfer coefficient suddenly increases resulting the large pressure decrease shown in Fig. 1. Another problem is the liquid temperature assumed for the water in the film. When a water level enters a cell, the liquid temperature used to evaluate the wall film condensation heat flux should be the above cell liquid temperature instead of the cell liquid mixture temperature.

RELAP5 was altered to perform like TRAC-M; i.e. turn off wall condensation when a water level enters a cell. Results are shown in Fig 2. The predicted pressure is improved with the altered code. However, the physics is still not modeled correctly.

The condensation mechanism errors in each code would not likely have been uncovered if each code were assessed independent of the other code.

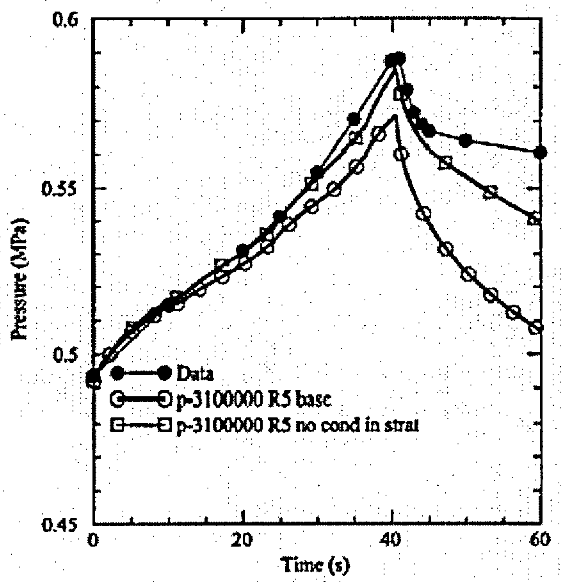


Figure 2. RELAP5 pressure prediction with no wall condensation in vertical stratification.

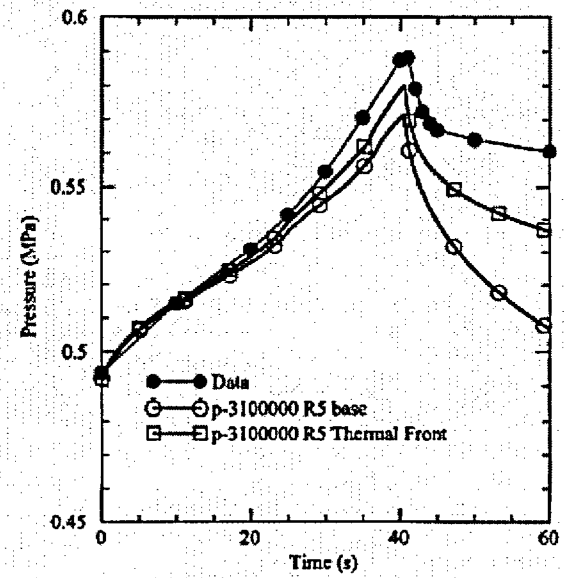


Figure 3. Pressure prediction with thermal stratification model.

Thermal Front

A thermal stratification model is included in RELAP5 to improve the accuracy of solutions when there is warm fluid above cold fluid in a vertical stack of cells (ISL Inc., 2001a). Because default RELAP5 uses a first-order semi-implicit upwind differencing scheme, axial numerical diffusion of cold water occurs. This has an unfavorable effect on the accuracy of the temperature profile. The model is activated when there is a density difference between upstream and downstream cells. The model achieves a sharp temperature profile by specifying the liquid energy crossing a cell junction to be the downstream cell energy. The model is turned off when the cell liquid energy equals the upstream cell liquid energy.

The calculated pressure in the top of the vessel using the thermal stratification model is shown in Fig. 3. Improved results are observed during and after the liquid insurge into the vessel.

A reason for the improved pressure behavior is demonstrated in the fluid temperature response shown in Fig. 4. The thermal front model sharpens the axial temperature profile. The pressure is higher with the model active because the liquid at the vapor-liquid interface is hotter; limiting condensation.

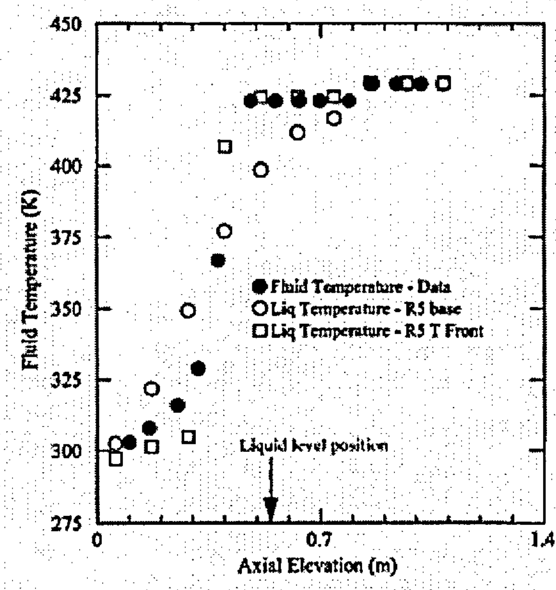


Figure 4. Axial fluid temperature profile at 35 seconds.

Level Tracking

The level tracking model implemented in RELAP5 includes a detection of the level appearance, calculation of mixture level parameters such as position and velocity of the level and void fractions above and below the level, mixture level movement from cell to cell, mass and energy equation modifications, and heat transfer calculation modifications.

RELAP5 level tracking model applied to this problem showed only slight improvements in the predicted pressure. During the pressure rise portion of the transient the predicted pressure laid nearly on top of the RELAP5 base run. The more notable improvement in the predicted pressure was after the water insurge was stopped. The predicted pressure after the water insurge was stopped, laid between the base run and the case where the thermal front model was activated.

TRAC-M has new level tracking logic as discussed in Aktas, (2002). When the level tracking logic is applied to this problem, the results are greatly improved as shown in Fig. 5. The peak pressure rises when the level tracking model is active. The logic uses the semi-implicit numerical scheme and assumes the interfacial heat transfer is zero in the cell with the water level.

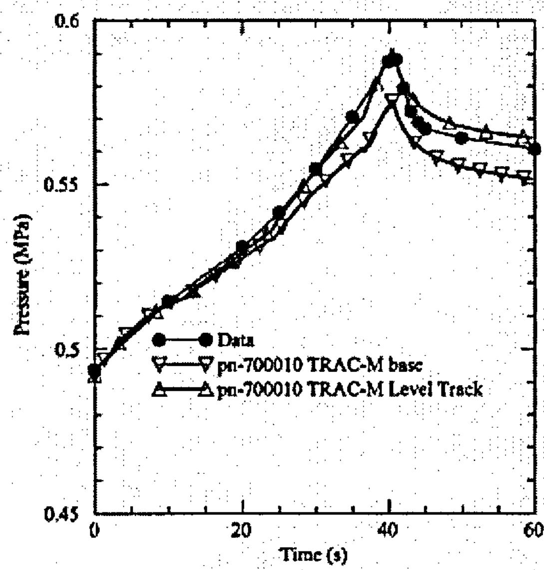


Figure 5. TRAC-M pressure prediction using the level tracking model.

RELAP5, with level tracking on, results in the same small value of interfacial heat transfer as with it off. This is because the default RELAP5 model, known as the "vertical stratification" model, already sets the interfacial area to be the cross-sectional area.

Numerical Implicitness

RELAP5 base calculations employ the semi-implicit numerical scheme to solve the conservation equations as recommended in the users guide (ISL Inc., 2001b). TRAC-M base results use the implicit advancement scheme since it is the default. The base maximum time step size was set at 0.01 seconds for both codes.

Figure 6 shows that the RELAP5 predicted pressure rise using the nearly-implicit solution has some problems when the water level crosses cell boundaries at about 12, 24 and 36 seconds.

TRAC-M does not encounter the same type of cell boundary crossing problem as does RELAP5 (see Fig. 1). The TRAC-M semi-implicit results overlay the implicit results at the base run time step size of 0.01 second.

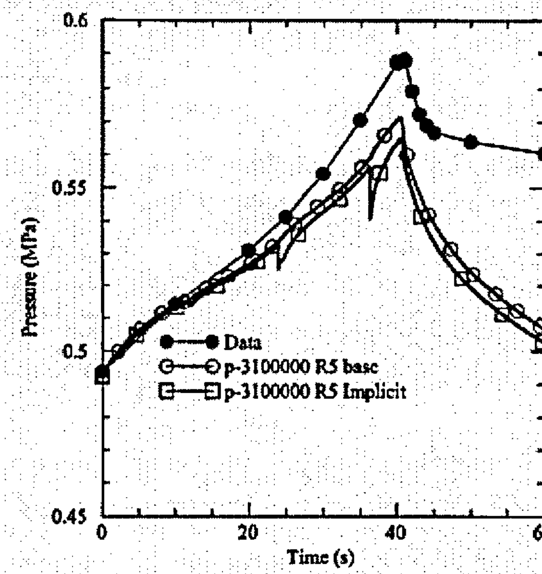


Figure 6. RELAP5 pressure using implicit numerics.

Time Step Size

A key to obtaining satisfactory predictions is controlling the time step size. The implicit numerical solution method allows for the time step size to be larger than the material Courant limit while the semi-implicit method does not.

Figure 7 shows time step size versus time using the base codes with the maximum time step size raised from 0.01 to 0.5 seconds. TRAC-M reached the maximum time step size for four intervals during the transient. The decreases in the time step are related to the liquid level crossing cell boundaries. RELAP5, however, was limited by the Courant time step size (dtcrnt) since the base case runs in semi-implicit mode.

Figure 8 demonstrates that time step size sensitivity

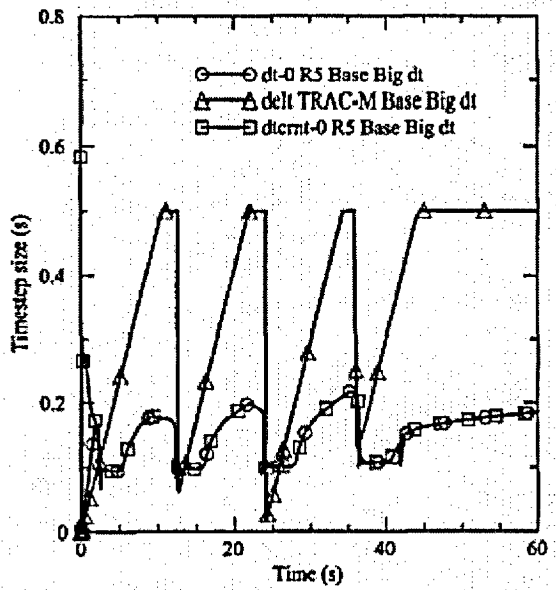


Figure 7. Time step comparison for a maximum time step size of 0.5 seconds.

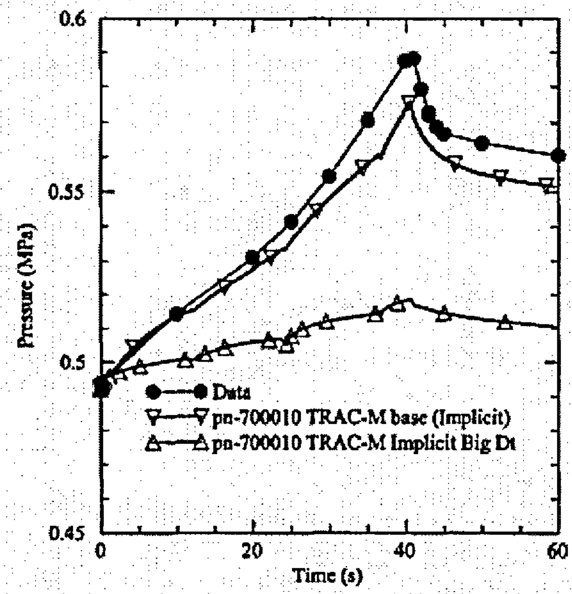


Figure 8. Effect of time step size on TRAC-M prediction.

calculations should be performed before accepting a prediction. Shown are TRAC-M implicit pressure predictions using time step sizes of 0.01 and 0.5 seconds. One possible reason for the lower pressure in the large time step size case is the conduction solution is not implicitly coupled to the hydraulic solution. This would allow the wall heat flux from vapor to be too large when the saturation temperature is rising.

Figure 9 shows that RELAP5 poorly predicts the pressure after 12 seconds when using nearly-implicit numerics and allowing large time steps. RELAP5 has the ability to perform a conduction solution implicitly coupled to the hydraulic solution. However, checks showed the nearly-implicit hydraulic solution was so bad that implicit conduction coupling did not yield improved results.

CONCLUSIONS

Both RELAP5 and TRAC-M predict the MIT pressurizer data reasonably well provided the time step sizes are not "large". The effects of choosing various code options have been demonstrated. The benefits of performing calculations with two codes makes code errors more obvious and the codes can be more rapidly improved.

REFERENCES

Aktas, B. and Uhle, J., 2000, "USNRC Code Consolidation and Development Effort," *Proceedings of the OECD-CSNI*

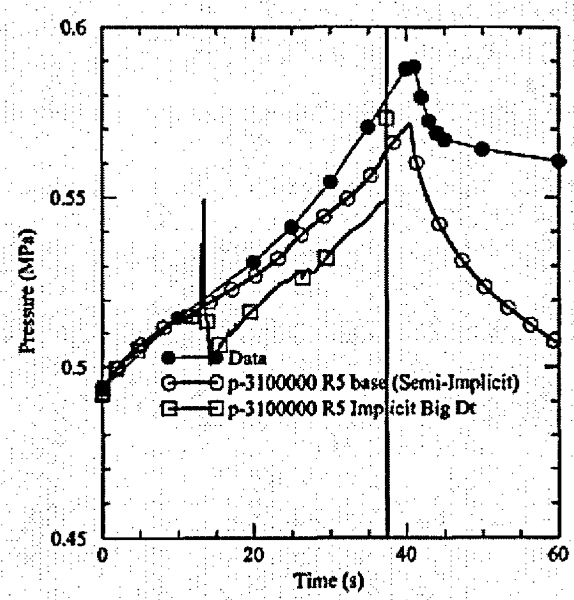


Figure 9. Relap5 prediction using implicit numerics with a large maximum time step size.

Workshop on Advanced Thermal-Hydraulic and Neutronic Codes: Current and Future Applications, Barcelona, Spain.

Aktas, B., 2002, "Tracking Interfaces in Vertical Two-phase Flows," *Tenth International Conference on Nuclear Energy*, the American Society of Mechanical Engineers, Virginia.

Kim, S. N., and Griffith, P, 1987, "PWR Pressurizer Modeling", *Nuclear Engineering and Design 102*, pp. 199-209.

Nusselt, W., 1916, "Die Oberflachenkondensation des Wasserdampfes," *Z. Ver. Deutsch. Ing., Vol. 60*, pp. 541-569

ISL Inc., 2001a, "RELAP5/MOD3.3Beta Code Manual Volume I: Code Structure, System Models, and Solution Methods", NUREG/CR-5535/Rev 1.

ISL Inc., 2001b, "RELAP5/MOD3.3Beta Code Manual Volume 5: User Guidelines", NUREG/CR-5535/Rev 1, pp 85.

Saedi, H. R., and Griffith, P, 1983, "The pressure Response of a PWR Pressurizer During an Insurge Transient," *Transactions of ANS, 1983 Annual Meeting*, Detroit, Michigan, June 12-16, pp. 606-607.

Spore, J. W., et al., 1993, "TRAC-PF1/MOD2 Theory Manual" LA-12031-M, Vol I, NUREG/CR-5673.

Appendix 6
Effect of Non-Condensables in Pressurizer

A confirming evaluation was performed which concludes that assuming non-condensable gases present within the steam in a pressurizer bubble has no significant impact. The worst case in terms of shrinking the bubble, Case 10 HP509S30 of Reference D, was rerun with an assumed 10% non-condensable gas. The bubble was slightly smaller and the pressure slightly higher, but the change does not impact the analysis conclusions. Case 12 of Reference D, where the input is adjusted to result in a PORV opening, was also re-performed with 10% non-condensables.

Model changes (only two cards change):

1) Added default non-condensable gas (air was used but nitrogen could have been used without a significant difference since air is 80% nitrogen).
2) Changed bubble card to be same pressure of 95 psia, but slightly lower temperature to keep steam saturated. That is, the steam temperature was set to the saturation temperature at 85.5 psia (316.7°F) since that is the partial pressure of steam, i.e., 0.9*95, at 90% quality where here quality is defined as fraction of steam/(steam + air).

Old card:
4101202 2 95. 1.0 0. 0 0,5

New cards:
110 air
4101202 4 95. 316.7 0.9 0 0,6

For Case 12 the change is to:
4101202 4 400. 434.4 0.9 0 0,6

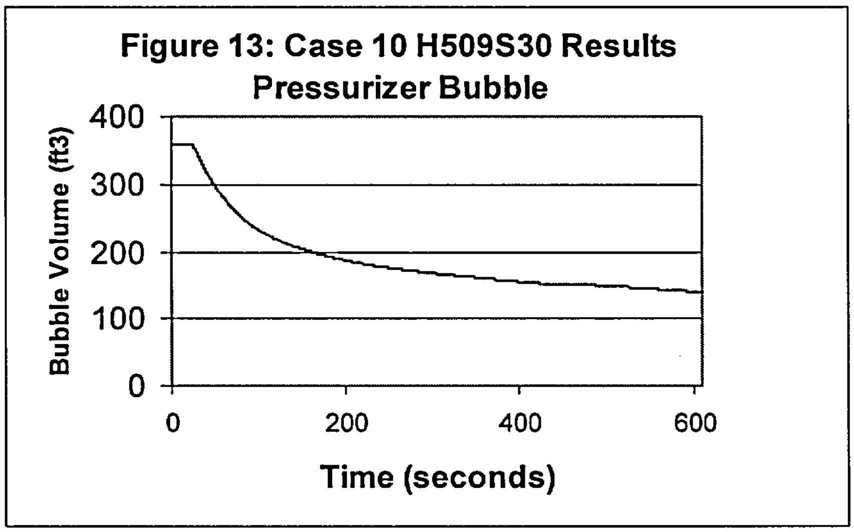
Results:
For Case 10, there is still a significant bubble left at 600 seconds, but it is slightly reduced from 139.0 ft^3 to 130.1 ft^3. Without non-condensables, the pressure only went up to 100.3 psia and then came back down. For this analysis it rose to 112 psia, and then returned slowly to 122 psia at 600 seconds. Please refer to the figures below for comparison.

The conclusion is that the non-condensable gases have no significant impact. The bubble is very slightly smaller, but there is still adequate bubble remaining after 10 minutes. The pressure is slightly higher, but that is unimportant since the PORV setpoint is still far away from being reached. Case 12 was run to show that even if the PORV does lift (due to an assumed high initial pressure) the transient is successfully mitigated. Re-performing Case 12 showed a tiny increase in the peak pressure (from 476 to 478 psia), but more rapid depressurization after the valve opens and a growing bubble at 600 seconds.

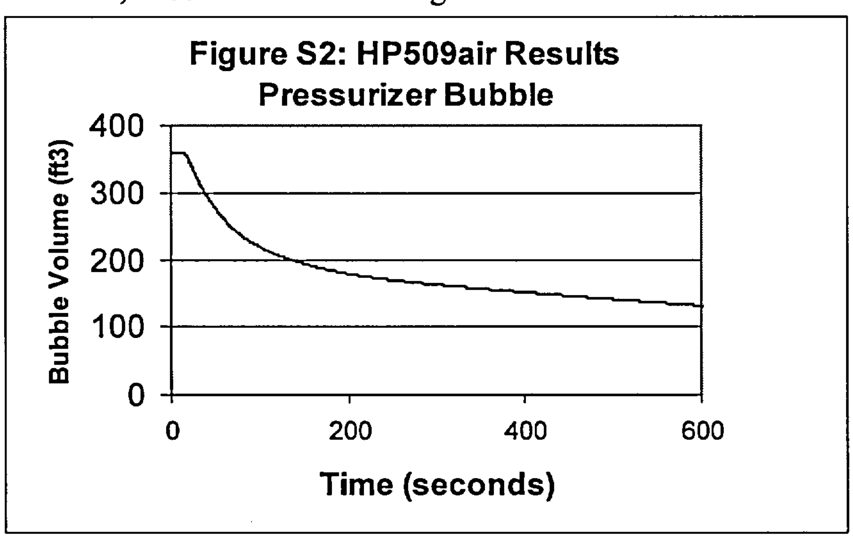
It was determined that these cases were sufficient in determining the effect of non-condensables on the LTOP analysis and were not needed on the mass addition cases. The resultant conclusion would be unchanged since none of the heat addition cases are limiting when compared to the mass addition events.

Reference D: FC06877, Rev. 0, "Low Temperature Overpressure Protection (LTOP) Analysis, Revision 1." [Note: This Reference was included in Letter from OPPD (D. J. Bannister) to NRC (Document Control Desk) dated October 8, 2002, Fort Calhoun Station Unit No. 1 License Amendment Request, "Reactor Coolant System (RCS) Pressure and Temperature Limits Report (PTLR)" (LIC-02-0109)]

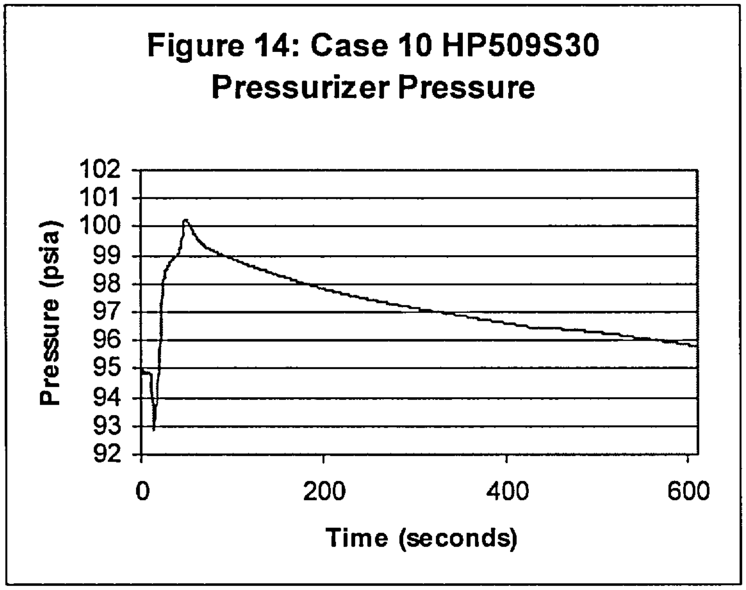
Old case, no non-condensable gas



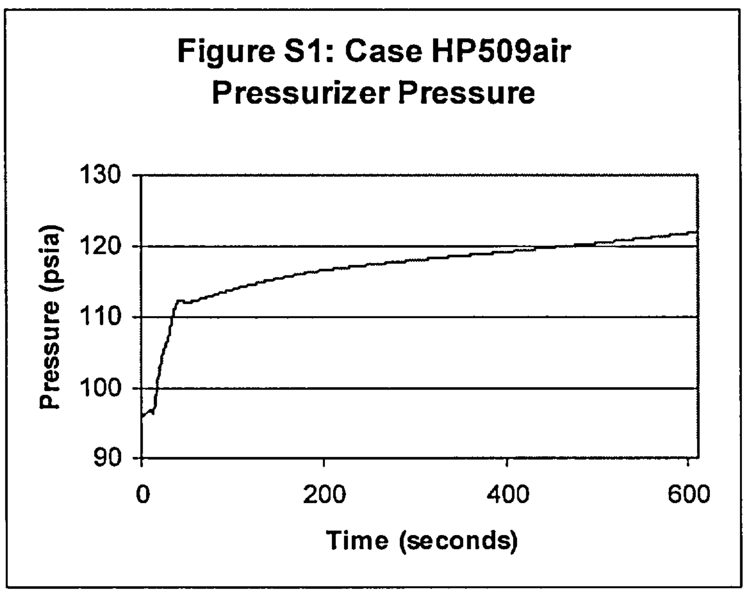
New case, 10% non-condensable gas



Old case, no non-condensable gas

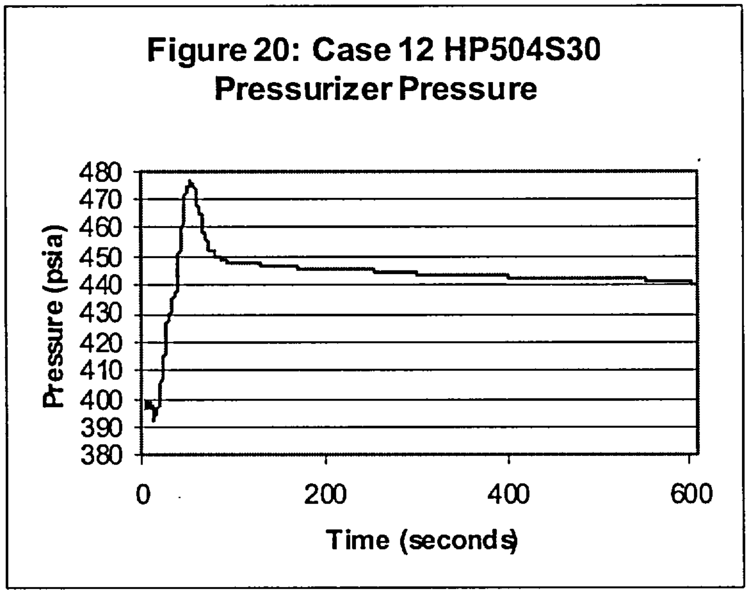


New case, 10% non-condensable gas

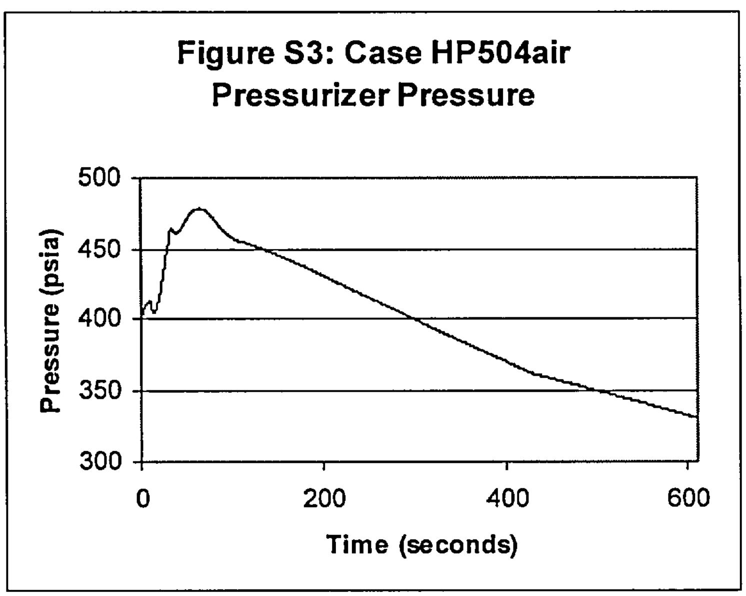


These are from Case 12 with the same changes made to HP504S30.txt:

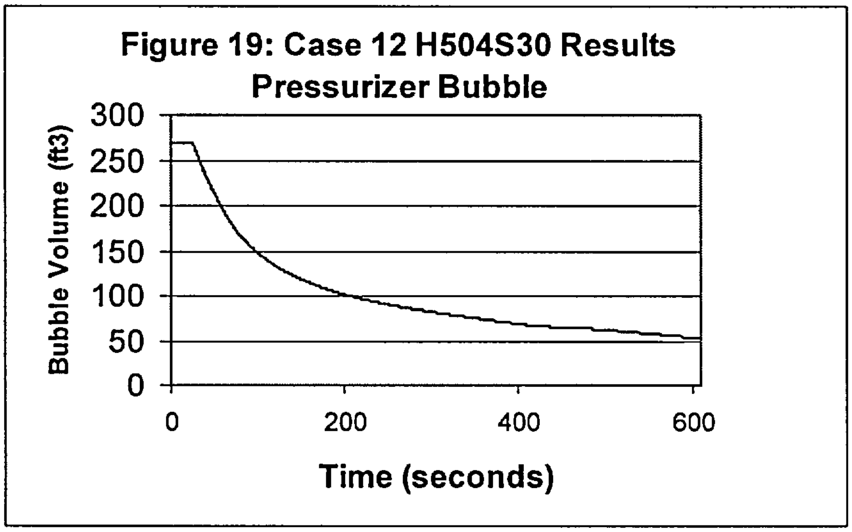
Old Pressure curve:



New Pressure curve:



Old Bubble curve, Case 12:



New Bubble: