Development of TH Scenario Screening Criteria for Pressurized Thermal Shock Analysis

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ABSTRACT

The objective of this research is to develop a set of screening criteria to eliminate (both physically and probabilistically) scenarios that are insignificant to pressurized thermal shock (PTS). The screening criteria are developed based on probabilistic fracture mechanics (PFM) models. The idea is straightforward, going backward from a conditional probability of vessel fracture to compute corresponding thermal-hydraulic (TH) boundary conditions. The process is automated using Mathematica [1]. This paper discusses the format of TH scenario screening criteria for PTS and the way they are developed.

INTRODUCTION

The objective for this research is to develop a set of screening criteria to eliminate (both physically and probabilistically) scenarios that are insignificant to PTS. This practice is essential to limiting the number of scenarios that are going to be analyzed using system codes such as RELAP5. Because of the large number of scenarios identified by the probabilistic risk assessment (PRA), it is not feasible to perform a full-scale thermal hydraulic analysis for each one of these scenarios. Detailed RELAP5 calculation must be limited to a few important and representative scenarios. Meanwhile, those selected scenarios have to cover the whole range of physically and probabilistically feasible scenarios. To achieve this goal, some sort of gating criteria, which can screen out insignificant scenarios, should be developed. Using the PRA, those scenarios with small enough frequency may be discarded due to their probabilistic insignificance. However, for those scenarios that survive the PRA screening may still not be important if they do not lead to physical conditions (in terms of pressure and temperature behavior) important to PTS. Obviously, these scenarios should be evaluated using detailed PFM analysis to decide whether they are PTS-significant. Since the thermal boundary conditions are unknown for determination of PTS-significant scenarios, they must be obtained. These boundary conditions are obtained by performing the PFM analysis in a backward direction. That is, to find all the thermal conditions that lead to a given probability of vessel failure.

FORMAT OF THE SCREENING CRITERIA

In PTS studies, a thermal transient is characterized by three time varying history profiles, namely, the temperature of the coolant in the reactor vessel downcomer $T_{dc}(t)$, the vessel internal pressure $p(t)$ (i.e., the primary system pressure), and the fluid-film heat transfer coefficient at the inner vessel surface in the downcomer region $h(t)$. Since the goal is to find the screening criteria, it would be unnecessarily difficult, if not impossible, to treat all three as time variants. Therefore, a reasonable set of parameters must be defined such that they allow one to describe a scenario. For that purpose, the internal pressure and heat transfer coefficient are chosen to be constant whereas the downcomer temperature varies with time according to one of two very simple models: exponential and linear models.

In the exponential model, the downcomer temperature decreases with time to a final temperature according to a corresponding time constant [2]. For a given set of boundary conditions that define loop flow stagnation, the cooldown rate in the cold legs, downcomer, and lower plenum regions may be represented by an exponential model [3]. While the exponential form is an approximate model, because the objective is to develop a set of TH screening criteria for PTS, this assumption is well justified. Hence, the temperature varies with time according to:

$$T_{dc}(t) = T_f + (T_i - T_f)e^{-\frac{t}{\tau}} \tag{1}$$

where T_f is the final temperature, T_i is the initial temperature (usually around 550 °F), and τ is the time constant that reflects cooldown rate in the downcomer. As such, there are four parameters required to prescribe a transient: the internal pressure, the heat transfer coefficient, the final temperature, and the time constant.

Another model of interest is linearly decreasing temperature in the downcomer (because of its simplicity.) Accordingly, this form of boundary condition has been explored in this study. In the linear model, the downcomer temperature decreases with time linearly from an initial temperature of T_i to a final temperature of T_f at a given rate of temperature change (ROC) and is expressed in the following form:

$$T_{dc}(t) = T_i - ROC * t, \qquad if \ t \le \frac{T_i - T_f}{ROC}$$
$$T_{dc}(t) = T_f , \qquad if \ t > \frac{T_i - T_f}{ROC} \tag{2}$$

In this model, the cooldown rate (ROC) instead of the time constant is necessary to define a transient. The remaining boundary conditions are the same as the exponential model.

SCREENING CRITERIA DEVELOPMENT

PFM analysis for calculating vessel failure probability consists of the following components: thermal analysis, stress analysis, stress intensity factor calculation and failure probability calculation. These components have been discussed first, followed by explanation of the process through which the TH boundary conditions for an assumed vessel failure probability has been computed. Finally, the results have been discussed.

As mentioned earlier, to develop the screening criteria, a simplified PFM analysis should be performed in a backward direction. The PFM analysis is nonlinear in nature and cannot be solved in a closed form. Therefore, iteration seems to be the only solution. The process is automated using Mathematica.

Thermal Analysis

The temperature profile for the vessel is determined by solving the following one-dimensional partial differential heat conduction equation with time varying boundary conditions and constant initial conditions (constant initial temperature across the vessel wall).

$$r c_p \frac{\partial T}{\partial t} = \frac{1}{r} \frac{\partial}{\partial r}\left(kr \frac{\partial T}{\partial r} \right) \tag{3}$$

where

c_p = specific heat,
k = thermal conductivity,
ρ = density,
r = radius,
T = T(r, t) – the temperature profile inside the vessel wall,
t = time.

At the inner vessel surface,

$$q'' = h(T_{dc}(t) - T_c) = h[T_f + (T_i - T_f)e^{-\frac{t}{t}} - T_c] \tag{4}$$

where

h = inner surface heat transfer coefficient,
q’’ = heat flux,
T_c = the temperature on the inner surface in contact with the coolant.

Meanwhile, an adiabatic boundary condition is assumed on the outer surface of the vessel wall in contact with ambient atmosphere. The initial condition is simply

$$\mathrm{T}(\mathrm{r}, 0) = \mathrm{T_i} = 550 \quad {}^\circ\mathrm{F} \tag{5}$$

The heat conduction equation is a classical partial differential equation that can be solved either analytically (when the boundary conditions are simple enough) or numerically. Because of the time varying boundary conditions, numerical solution is preferred.

It is noteworthy that there is a thin layer of stainless clad attached to the inner wall. The thermal properties of cladding materials are different from the base materials of the vessel. Therefore, a composite wall should be modeled. Traditionally, this involves solving two heat equations simultaneously along with two interface boundary conditions. Since Mathematica cannot handle a system of partial differential equations with different intervals, only one equation is solved but material properties are changed over layers. This practice should not produce significant deviation from the traditional treatment. The former will generate continuous temperature distribution along the radius, while the latter generates a distribution that is continuous only to the first order derivative at the interface between the cladding and base material.

Stress Analysis

The calculation of stress intensity factor, $K_{I,}$ requires computation of stress distribution in the vessel wall. There are two kinds of stresses: thermal stress and mechanical stress (caused by pressure load.) The circumferential and longitudinal thermal stresses in a long cylinder subjected to a radical temperature distribution are given by [4]

$$\begin{aligned} \sigma_{T,r} &= \frac{\alpha E}{1-\nu}\frac{1}{r^2}\left[\left(\frac{r^2+c^2}{d^2-c^2}\right)\int_c^d Trdr + \int_c^r Trdr - Tr^2\right], \\ \sigma_{T,z} &= \frac{\alpha E}{1-\nu}\left[\left(\frac{2}{d^2-c^2}\right)\int_c^d Trdr - T\right] \end{aligned} \tag{6}$$

where α = coefficient of thermal expansion, υ = Poisson’s ratio, E = Young’s Modulus, c, d = inner and outer radii of the vessel, T = temperature in the wall at r and time t.

The evaluation is performed numerically because T(r, t) is evaluated numerically. Once again, the clad and base materials should be treated separately. As a result, there is discontinuity in the thermal stress at the clad-base materials interface, which presents a problem for K_I calculation. This will be discussed and addressed later.

The mechanical stresses caused by the internal pressure p are evaluated by

$$\begin{aligned} \sigma_{p,r} &= \frac{c^2 p}{d^2-c^2}\left(1+\frac{d^2}{r^2}\right), \\ \sigma_{p,z} &= \frac{c^2 p}{d^2-c^2} \end{aligned} \tag{7}$$

Because we are dealing with linear elastic fracture mechanics (LEFM), the total stress would be summation of both thermal and mechanical stresses for various locations and time steps.

Calculation of Stress Intensity Factor

The stress intensity factor (K_I) is calculated by the superposition technique [5]. The detailed discussion can be found in Ref. [5]. The idea is that instead of calculating the cracked structure using the actual loads, the calculation is performed with a distributed pressure applied to the crack surface only. This pressure is opposite in sign, but equal in magnitude and distribution, to the stress along the crack line calculated for the un-cracked structure with the actual loads applied. The stress distribution is fitted to a third-order polynomial as

$$\sigma(a') = C_0 + C_1\left(\frac{a'}{a}\right) + C_2\left(\frac{a'}{a}\right)^2 + C_3\left(\frac{a'}{a}\right)^3 \tag{8}$$

where a is the crack depth, and a’ is the radial location along the crack line. Then K_I is calculated from

$$K_I(a) = \sum_{j=0}^{3} K_{Ij}(a) = \sum_{j=0}^{3} C_j \sqrt{\pi a} K_j^*(a) \tag{9}$$

The quantity K_I*(a) is referred to as the stress-intensity-factor-influence-coefficient (SIFIC). Its values should be calculated by using a finite element method (using the ABAQUS, a nuclear quality assurance certified code [6].)

The presence of the thin layer of cladding on the inner surface poses difficulty to K_I calculation since it requires a third-order polynomial fit of the stress distribution. To get around this problem, SIFICs should be calculated for the cladding stresses alone; the corresponding K_I value can then be superimposed on the K_I value due to the stresses in the base material. This is done by first calculating K_I value for a continuous function obtained by linear extrapolation of the stress distribution in the base material into the cladding material and then calculating K_I value for an adjusted stress distribution in the cladding. The adjusted stress distribution is obtained by subtracting the extrapolated distribution from the actual distribution in the cladding. The final K_I is the sum of the two. Since the stress distribution in the cladding is essentially linear, only a first-order polynomial is used for the cladding SIFICs.

Vessel Failure Probability Calculation

A 2-inch axial flaw with aspect ratio of 10:1 was chosen for this analysis as in Ref. [7]. Note that the flaw characterization that is currently under development can be incorporated into this analysis in the future, if needed. While this analysis has focused on single flaws, one can also compute failure probability for vessels with multiple flaws from the single flaw case. An RT_{NDT} of 300 °F was chosen for this analysis. The probability of failure is determined by comparison of calculated K_I with K_{Ic} distribution at the deepest crack tip. The K_{Ic} distribution is a Weibull distribution whose parameters are determined from statistical analysis [7]. The failure probability is computed from

$$\begin{aligned}
&\Pr(F) = 1 - Exp\left[-\left(\frac{K_I - a}{b}\right)^c\right] \\
&a = 10.8957 + 23.4192\exp(0.0023\Delta T) \quad [\text{ksi - in}^{1/2}] \\
&b = 14.7582 + 42.6312\exp(0.0124\Delta T) \quad [\text{ksi - in}^{1/2}] \\
&c = 2.03025 + 0.4983\exp(0.0135\Delta T) \\
&\Delta T = T - RT_{NDT}
\end{aligned} \tag{10}$$

Backward Iteration to Compute TH Boundary Conditions

By using the PFM equations discussed above, we can obtain a relationship that correlates T_f, τ, p and h with the probability of vessel failure. Therefore, by fixing three parameters the fourth one can be calculated for a given probability of vessel failure. By systematically varying the three parameters the whole spectrum of the possible boundary conditions may be obtained. The computation is very complex because of the number of iterations. The process discussed above cannot be reversed to compute for example τ, from a known probability of failure. Therefore, iterations needed are clearly time consuming.

RESULTS AND DISCUSSION

The results are shown in Figure 1. There are six layers of surfaces in Figure 1 representing screening criteria for six conditional probabilities of vessel failure: 10^{-2}, 10^{-3},10^{-4},10^{-5}, 10^{-6}, and 10^{-7}. It is obvious that the upper layer is for 10^{-7}, while the lower layer is for 10^{-2} (small time constant means fast cooldown and high thermal stress). For a given probability of failure, this surface determines the boundary that differentiates PTS significant area from insignificant area. Any scenario that goes below the surface would be PTS significant and must be included in detailed PFM analysis. For example, in an extreme case, p may be at full pressure (2250 psi) while the final temperature reaches 100°F. If the cutoff conditional failure probability is equal to 10^{-7}, the required cooldown time constant should be around 10 hours. Since normal cooldown rate is ~8 hours, it means under given pressure and final temperature conditions, all normal cooldown scenarios would lead to vessel failure with probability of 10^{-7}. If we reduce the cutoff value to 10^{-2}, τ could be as low as 4.7 hours. It means, as expected, that 10^{-2} case is more restrictive than 10^{-7} case from screening point of view. Also, the cooldown time constant becomes more restrictive as temperature increases and as pressure decreases because less severe pressure and final temperature conditions demand faster cooldown rate to reach a given vessel failure probability goal. Note here that the heat transfer coefficient is kept constant at 1000 BTU/hr-ft^2-°F. Figure 1 shows that final coolant temperature is the driving force in determining the allowable cooldown rate. The internal pressure and prescribed probability cutoff value are also important, although not as significant as the coolant temperature.

The computational algorithm assumes that there is a corresponding value for the cooldown time constant for any combination of internal pressure, final temperature, heat transfer coefficient, and conditional probability of vessel failure. Also, time constant is always positive. This algorithm continues searching for appropriate time constant until τ is so small that it will not be physically feasible and achievable for a scenario (it is set to be 1 minute in this analysis). In this case, it may be claimed that such a combination of p, T_f, and h is not of PTS significance under the selected probability of failure. From Figure 1, it can be seen that such areas are located in high temperature and low pressure.

To verify the results from this approach, the analysis result is compared with the one performed by ORNL [7]. The comparison is illustrated in Figure 2. It can be seen that the results of this study are conservative since it generates higher probability of failure than FAVOR. A full validation of the results requires additional confirmation runs of FAVOR.

It should be noted here that the results are not sensitive to heat transfer coefficient. This conclusion removes uncertainty in a previous study on the impact of heat transfer coefficient on PTS (Ref. [2]). Ref [2] concludes that uncertainty and variations in heat transfer coefficient below a certain value "could be significant to PFM predictions." The current study shows, however, within the range from 200 to 1000 BTU/hr-ft^2-°F, the impact of heat transfer coefficient is clearly minimal (see Figure 3).

To facilitate scenario screening in terms of the ROC, the linear temperature model is also analyzed in this study. The results are shown in Figure 4. It is interesting to note that for all probabilities, there is a step increase in rate of change when the final temperature approaches 400 °F from 300 °F. For certain pressures, if the final temperature is sufficiently high (>400 °F) the probability of failure would not reach a certain value with a normal ROC (the operating procedures require that the ROC be less than 100 °F/hr under any circumstances). In other words, there is no PTS concern if the final temperature is too high regardless of the ROC since the small temperature gradient across the vessel wall is not sufficient to generate significant thermal stress. In addition, by comparing Figure 4a and Figure 4b one can see the step change happens at lower pressure for lower probability which is intuitively correct. That means in order to reach a higher probability of failure, the pressure has to be high enough given all other conditions remain unchanged. Figure 4c illustrates the screening criteria for different probabilities of failure ranging from 10^{-2} to 10^{-7}. Needless to say, the upper layer is for 10^{-2} probability level while the lower layer for 10^{-7}. Since a large ROC is more difficult to achieve than a small one, higher probability of failure is more selective than lower one from scenario screening standpoint.

CONCLUSIONS

In summary, this analysis provides a set of preliminary thermal hydraulic (TH) scenario screening criteria for PTS study. The developed screening criteria can be applied to a wide range of scenarios since both exponentially and linearly decreasing temperature models are analyzed. The criteria allow analysts to eliminate many scenarios, which are not significant from the PTS standpoint. By screening out those insignificant scenarios, the PRA and TH analysts can focus on those scenarios that may lead to PTS. Using the FAVOR code should validate the results should be further verified.

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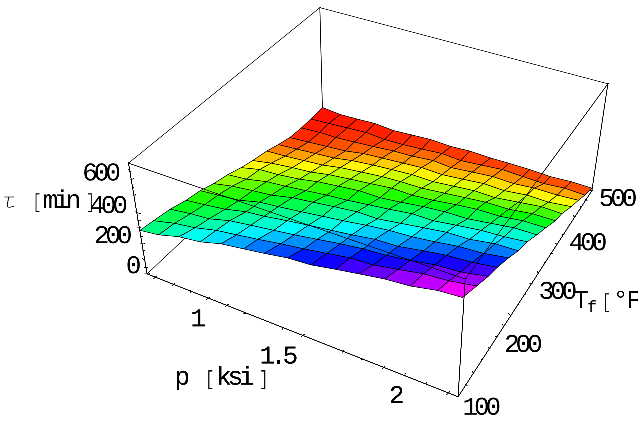


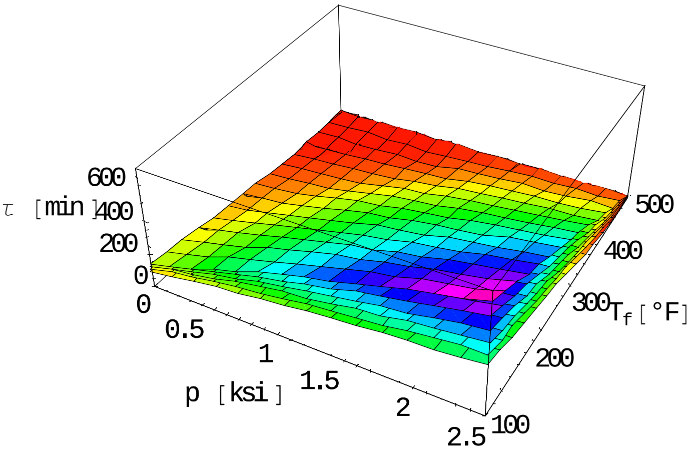
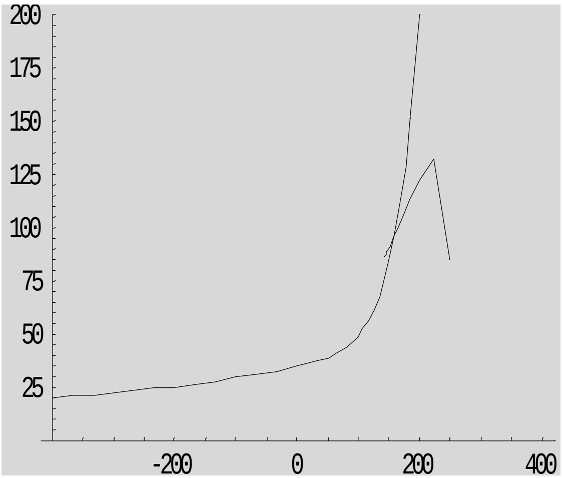


Figure 1a. PTS Screening Criteria for Probabilities of Failure: 10^{-7}

Figure 1b. PTS Screening Criteria for Six Probabilities of Failure: 10^{-2}, 10^{-3}, 10^{-4}, 10^{5}, 10^{-6}, and 10^{-7}



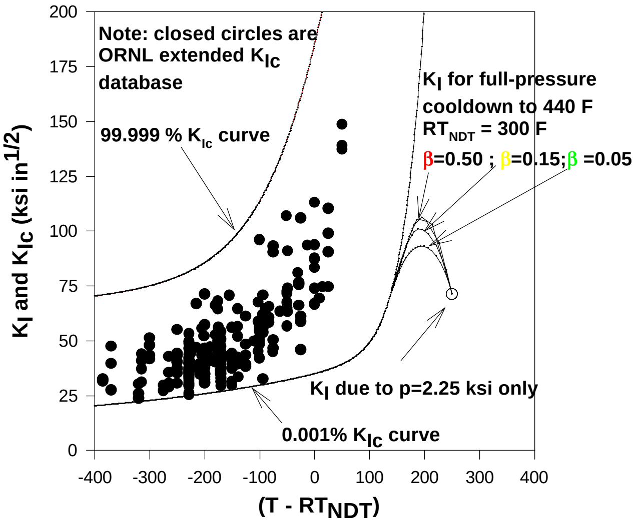


Figure 2 - 2 inch deep axially oriented inner-surface breaking flaw has a conditional probability of cleavage fracture < .00001 so long as the coolant temperature remains above 440 F and RT_{NDT} at crack tip remains at or below 300 F

(a) Mathematica Calculation

(b) FAVOR Calculation [7]

Figure 2. Comparison with FAVOR Calculations [7]

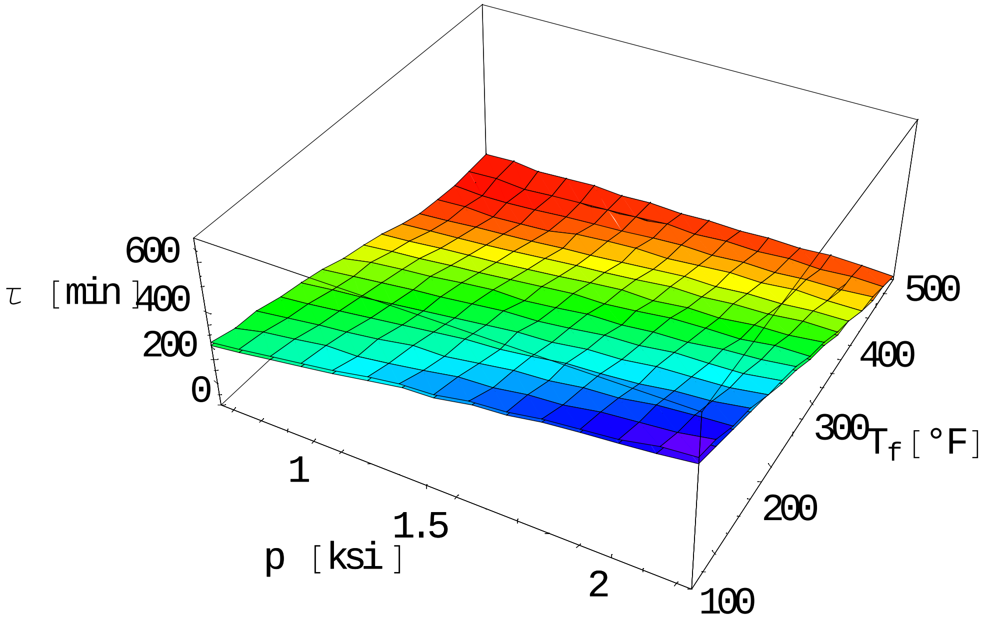


Figure 3. Pr(F) = 10^{-6}, h = 200 (upper) and 2000 (bottom) BTU/hr-ft^2-°F

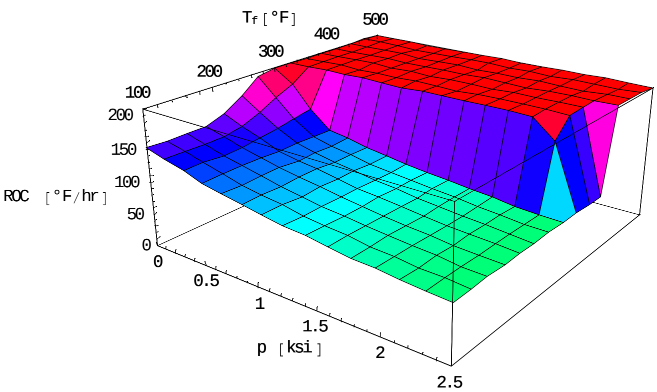


Figure 4a. PTS Screening Criteria for Probabilities of Failure: 10^{-2} (linear model)

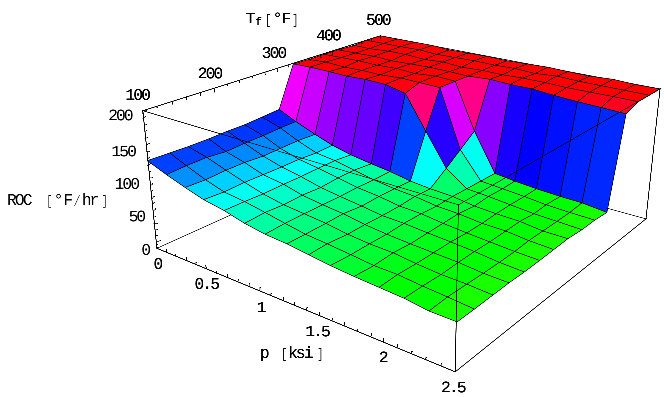


Figure 4b. PTS Screening Criteria for Four Probabilities of Failure: 10^{-4} (linear model)

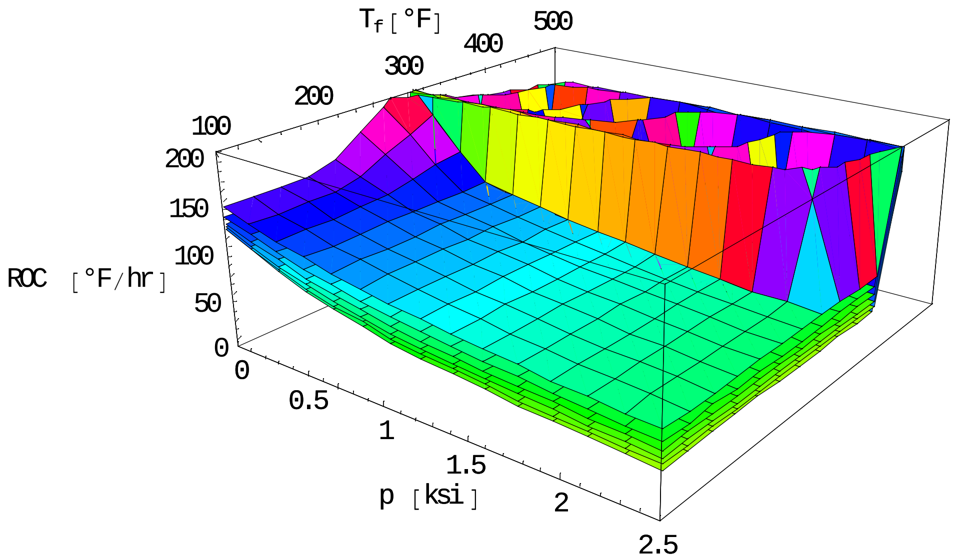
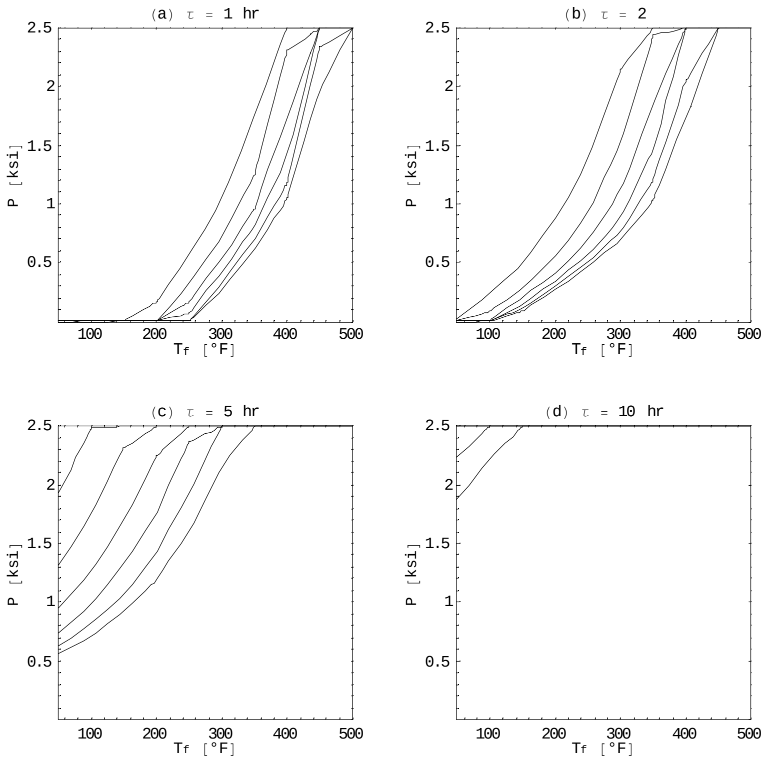


Figure 4c. PTS Screening Criteria for Six Probabilities of Failure: 10^{-2}, 10^{-3}, 10^{-4}, 10^{-5}, 10^{-6}, and 10^{-7} (linear model)

APPENDIX

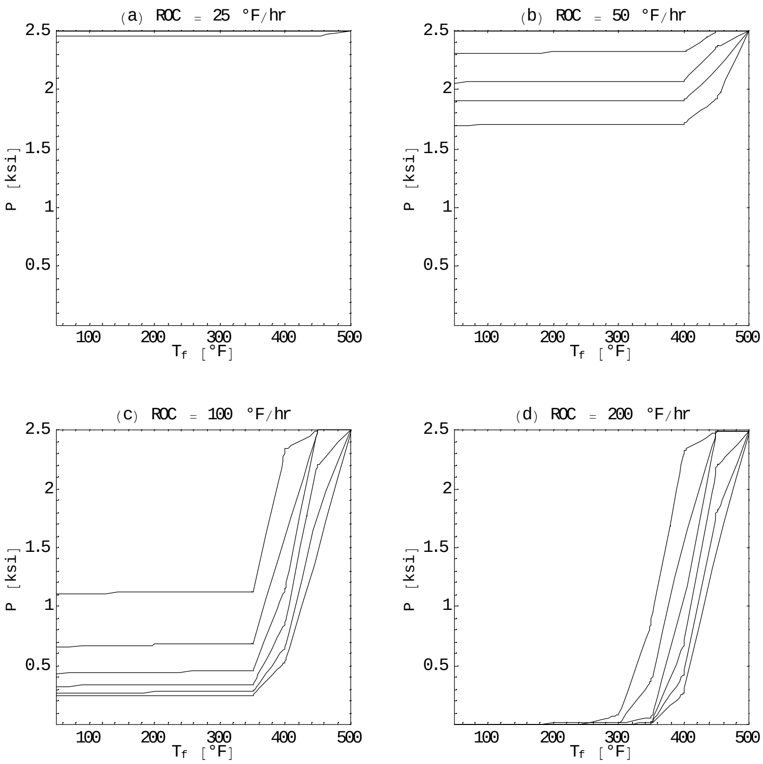
Since thermal hydraulics analysts request screening criteria in a different form than what is presented previously, we also generated pressure versus temperature plots at fixed time constant (τ) or rate of change (ROC) to satisfy their needs. In both Figure A.1 and Figure A.2, there are six curves associated with six probability levels in each plot. The upper one is for 10^{-2} while the lower one is for 10^{-7}. Note that the pressure ranges from 0 to 2.5 ksi and the temperature from 50 to 500 °F. If the pressure exceeds the range, it means the failure probability cannot be achieved.

It is noteworthy that shape of the plot is different from Figure A.1 to Figure A.2, meaning exponential model differs from linear model in terms of the impact of final temperature. In the exponential model, the pressure is sensitive to the final temperature since it is the final temperature plus time constant that determines downcomer temperature cooldown rate which influences magnitude of thermal stress in the vessel. On the other hand, in the linear model, downcomer temperature cooldown rate is solely determined by the rate of change given that the final temperature is low enough (<400 °F). As such, the pressure is not sensitive to the final temperature at low levels. Therefore, the driving force for vessel failure due to PTS is the ROC (when the final temperature is sufficiently low) if linear temperature applies.



Note: The upper curve is for 10^{-2}, while the lower one is for 10^{-7}.

Figure A.1 Pressure vs. Temperature for 6 probabilities (10^{-2} – 10^{-7}) (exponential model)



Note: The upper curve is for 10^{-2}, while the lower one is for 10^{-7}.

Figure A.2 Pressure vs. Temperature for 6 probabilities (10^{-2} – 10^{-7}) (linear model)