Overpressure During Emergency Relief Venting in Bubbly and Churn-Turbulent Flow

Vessel overpressure behavior during emergency relief venting in both bubbly and churn-turbulent flow regimes was obtained analytically. The transient predictions are shown to be in good agreement with the more exact numerical integration scheme. The applications of current results in relief vent sizing for both reactor vessels and storage tanks are demonstrated via numerical examples.

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Introduction

The purpose of this paper is to extend previous analyses (Leung, 1986a) on emergency relief venting of reactors and storage vessels to include bubbly and churn-turbulent flow regime considerations in vessel hydrodynamic behavior. In the earlier work, closed-form analytical solutions were obtained for three limiting regimes: all-vapor venting, all-liquid venting, and homogeneous-vessel venting. These are highly idealized flow situations with complete vapor-liquid phase separation in the first two cases and no separation at all in the last case. While a real venting process generally falls somewhere between all-vapor venting and homogeneous venting, the amount of phase separation or disengagement depends largely on the particular fluid behavior and the vapor superficial velocity. From studies of gas sparging through liquid columns, two rather distinct flow regimes have been identified, the bubbly and the churn-turbulent regimes. These flow regimes are best described by the drift flux model of Zuber and Findlay (1965) and Wallis (1969), and have been used extensively to characterize the in-vessel fluid behavior during the course of the DIERS (Design Institute for Emergency Relief Systems) research program. Based on these flow regimes, partial vapor-liquid disengagement modeling were discussed previously by Fauske et al., (1983), Grolmes (1983), Grolmes and Fauske (1983), and in a recent DIERS report (1983).

The present treatment is limited to vapor pressure ("tempered") systems where evaporative cooling is available. The derivations are aimed at defining the "overtemperature" during relief venting, and the corresponding overpressure is defined only for the constant-volatility case, i.e., that in which the pressure-temperature relationship is insensitive to composition change. The varying-volatility cases are thus outside the scope of the overpressure considerations of this work, even though the following temperature formulations remain applicable.

Exit Quality Estimation

The above-mentioned DIERS study shows that the vapor superficial velocity at the top of the vessel can be given by Eq. 1; a complete derivation is included in the appendix.

$$j_g = \frac{\alpha_T(1-\alpha_T)^{n-1}U_\infty}{1-\left(\dfrac{\alpha_T}{1-\alpha_T}\right)\left(\dfrac{1-x_1}{x_1}\right)\dfrac{v_f}{v_g}} \tag{1}$$

where α_T is the void fraction at the top of the vessel and x_1 is the exit quality or the vapor mass fraction. Wallis has shown that n is about two for bubbly flow and is near zero for churn flow. Here the bubble rise velocity is given by

$$U_\infty = A_K\left[\frac{\sigma g(\rho_f-\rho_g)}{\rho_f^2}\right]^{1/4} \tag{2}$$

where A_K according to Wallis ranges from 1.18 to 1.53.

For uniform volumetric vapor generation in the churn flow regime, the DIERS study shows that α_T is related to the vessel average α via,

$$\frac{\alpha_T}{1-\alpha_T} = \frac{2\alpha}{1-\alpha} \tag{3a}$$

For bubbly flow, no simple relationship has been found. (Due to the particular shape of the drift flux function for $n = 2$ [bubbly], no simple steady-state solution exists for volumetric vapor generation; see a similar discussion by Epstein, 1975). It is sufficient to approximate α_T by α, which is generally conservative from the vent-sizing point of view:

$$\alpha_T = \alpha \tag{3b}$$

Finally, the vapor phase continuity consideration at the entry location to the vent line provides the necessary relation between j_g and the discharge mass flow,

$$\rho_g j_g A_x = W x_1 \tag{4}$$

where W is also known as the relief vent rate (Huff, 1982). Now Eqs. 1, 3, and 4 allow x_1 to be expressed in terms of two key variables, α and W, as

$$x_1 = \frac{\alpha(1-\alpha)K_2 + \left(\frac{\alpha}{1-\alpha}\right)\frac{v_f}{v_g}}{1 + \left(\frac{\alpha}{1-\alpha}\right)\frac{v_f}{v_g}} \tag{5a}$$

for the bubbly flow regime and

$$x_1 = \frac{K_2 + \frac{v_f}{v_g}}{\left(\frac{1-\alpha}{2\alpha}\right) + \frac{v_f}{v_g}} \tag{5b}$$

for the churn flow regime. Here $K_2 \equiv U_\infty A_x/(v_g W)$. In the following discussion, the vessel will assume quasi-steady void development such that the above equations are applicable.

Approximate Analytical Solutions

From an earlier development (Leung, 1986a), the macroscopic mass and energy balance equations can be written as

$$\frac{dm}{dt} = -W = -GA \tag{6}$$

$$mC_v \frac{dT}{dt} = Q - Wh_{fg}\left(x_1 + \frac{v_f}{v_{fg}}\right) \tag{7}$$

Here Q is the total heat input and is given by the product of the instantaneous mass m and the reaction heat release rate per unit mass q for the case of runaway reaction. Using similar approximations, namely,

1. Relief vent rate W being constant during the overpressure transient
2. Assuming an average value for q in runaway reaction and a constant value Q_T for external heating
3. Constant-property assumption

the above equations can be solved in principle analytically together with the expression for exit quality, Eq. 5. Here a simplifying approximation is made regarding this exit quality expression so as to reduce the mathematical complexity. For the bubbly flow case, the second term in the denominator $[\alpha/(1-\alpha)](v_f/v_g)$ is typically much less than unity and is therefore ignored. ($v_f/v_g = \rho_g/\rho_f \ll 1$ for typical fluids far removed from critical point and $\alpha/(1-\alpha)$ is in the order of unity.)

Likewise, for the churn flow case the second term in the denominator, v_f/v_g, is small relative to the first term and can be neglected. Now with vessel average void fraction replaced by the instantaneous mass [by noting that $\alpha \doteq (V - mv_f)/V$ since $m_f \gg m_g$], the approximate expressions for exit quality become

$$x_1 = K_2 \frac{mv_f}{V}\left(1 - \frac{mv_f}{V}\right) + \frac{v_f}{v_g}\left(\frac{V}{mv_f} - 1\right) \tag{8a}$$

and

$$x_1 = 2\left(K_2 + \frac{v_f}{v_g}\right)\left(\frac{V}{mv_f} - 1\right) \tag{8b}$$

for bubbly flow and churn flow, respectively. The steps taken to arrive at the final analytical solutions are similar to previous treatments (Leung, 1986a), namely:

1. Integration of Eq. 6 to yield $m = m_o - Wt$
2. Substitution of this result into Eq. 7 and subsequent integration to yield temperature as a function of time
3. Solving for temperature turnaround time τ by setting $dT/dt = 0$

In order to facilitate the integration process and to present the final results in compact form, the following dimensionless variables and constants are defined:

$$\tau^* = \tau/t_e = \tau W/m_o \quad \text{(dimensionless turnaround time)}$$

$$N_T = \frac{C_v \Delta T}{h_{fg}} \quad \text{(dimensionless overtemperature)}$$

$$K_0 = \frac{m_o q}{W h_{fg}}, \quad K_1 = \frac{Q_T}{W h_{fg}}$$

$$K_2 = \frac{U_\infty A_x}{W v_g}, \quad K_3 = \frac{v_f}{v_{fg}}$$

$$K_4 = \frac{v_f}{v_g}, \quad K_5 = \frac{V/m_o}{v_{fg}}$$

$$K_6 = \frac{v_g}{v_{fg}}, \quad K_7 = \frac{K_o}{K_2} = \frac{m_o q v_g}{U_\infty A_x h_{fg}}$$

$$K_8 = \frac{K_1}{K_2} = \frac{Q_T v_g}{U_\infty A_x h_{fg}} \tag{9}$$

The analytical results are presented below under either runaway reaction type of emergency venting or constant external heating type for both flow regimes.

Runaway Reaction with Bubbly Regime

$$(1-\tau^*)^3 + \frac{K_0 - (1-\alpha_o)K_2}{(1-\alpha_o)^2 K_2}(1-\tau^*)^2 + \frac{K_4 - K_3}{(1-\alpha_o)^2 K_2} \cdot (1-\tau^*) - \frac{K_4}{(1-\alpha_o)^3 K_2} = 0 \tag{10}$$

$$N_T = [K_0 - (1-\alpha_o)K_2]\tau^* + (K_3 - K_4)\ln(1-\tau^*) + (1-\alpha_o)^2 K_2\left(\tau^* - \frac{\tau^{*2}}{2}\right) - \frac{K_4}{(1-\alpha_o)}\left(\frac{\tau^*}{1-\tau^*}\right) \tag{11}$$

Constant External Heating with Bubbly Regime

$$(1-\tau^*)^3 - \frac{(1-\tau^*)^2}{(1-\alpha_o)} + \frac{K_1 - K_3 + K_4}{(1-\alpha_o)^2 K_2}(1-\tau^*) - \frac{K_4}{(1-\alpha_o)^3 K_2} = 0 \quad (12)$$

$$N_T = -(K_1 - K_3 + K_4)\ln(1-\tau^*) - K_2(1-\alpha_o)\left[\alpha_o \tau^* + (1-\alpha_o)\frac{\tau^{*2}}{2}\right] - \frac{K_4}{1-\alpha_o}\left(\frac{\tau^*}{1-\tau^*}\right) \quad (13)$$

Runaway Reaction with Churn Regimen

$$(1-\tau^*)^2 + \left(\frac{2K_2 - K_3 + 2K_4}{K_0}\right)(1-\tau^*) - \frac{2(K_2+K_4)}{(1-\alpha_o)K_0} = 0 \quad (14)$$

$$N_T = K_0\tau^* - (2K_2 - K_3 + 2K_4)\ln(1-\tau^*) - \frac{2(K_2+K_4)}{(1-\alpha_o)}\left(\frac{\tau^*}{1-\tau^*}\right) \quad (15)$$

Constant External Heating with Churn Regime

$$(1-\tau^*) = \frac{2(K_2+K_4)}{(1-\alpha_o)(K_1 + 2K_3 - K_3 + 2K_4)} \quad (16)$$

$$N_T = -(K_1 + 2K_2 - K_3 + 2K_4)\ln(1-\tau^*) - \frac{2(K_2+K_4)}{(1-\alpha_o)}\left(\frac{\tau^*}{1-\tau^*}\right) \quad (17)$$

For the constant external heating case (i.e., Q_T = constant), the solution scheme may proceed as follows. For a given relief vent rate W, Eq. 12 is solved for τ^* for the bubbly case. Substitution of this τ^* value into Eq. 13 and solving for N_T will then yield the overtemperature (or temperature rise above set condition). The corresponding overpressure is then evaluated based on the constant-volatility assumption. But for the case of a runaway reaction, q is assigned an average value during the overpressure period, i.e., $q = (q_s + q_p)/2$ where q_p is dependent on the temperature at turnaround and therefore is not known *a priori*. Rather than guessing at this temperature for a given W and iterating until the solution converges, a different approach is suggested. Here one chooses the overpressure and the corresponding ΔT instead, thus fixing the value for both q_p and q. Then the value of τ^* is now sought that will satisfy both equations. Alternatively, the first equation for $(1-\tau^*)$ can be rearranged to express K_2 explicitly in terms of τ^* and other known constants. Thus Eqs. 10, 12, 14, and 16 become, respectively,

$$K_2 = \frac{\dfrac{K_4}{(1-\alpha_o)^3} - \dfrac{K_4 - K_3}{(1-\alpha_o)^2}(1-\tau^*)}{(1-\tau^*)^3 + \dfrac{K_7 - (1-\alpha_o)}{(1-\alpha_o)^2}(1-\tau^*)^2} \quad (10a)$$

$$K_2 = \frac{\dfrac{K_4}{(1-\alpha_o)^3} - \dfrac{K_4 - K_3}{(1-\alpha_o)^2}(1-\tau^*)}{(1-\tau^*)^3 - \dfrac{(1-\tau^*)^2}{(1-\alpha_o)} + K_8\dfrac{(1-\tau^*)}{(1-\alpha_o)}} \quad (12a)$$

$$K_2 = \frac{(K_3 - 2K_4)(1-\tau^*) + \dfrac{2K_4}{(1-\alpha_o)}}{K_7(1-\tau^*)^2 + 2(1-\tau^*) - \dfrac{2}{(1-\alpha_o)}} \quad (14a)$$

$$K_2 = \frac{(K_3 - 2K_4)(1-\tau^*) + \dfrac{2K_4}{(1-\alpha_o)}}{(K_8 + 2)(1-\tau^*) - \dfrac{2}{(1-\alpha_o)}} \quad (16a)$$

Substitution of the above K_2 expression in the corresponding equation for N_T would yield one final transcendental equation for τ^* in each case. Once the solution for τ^* is found (via numerical scheme), the appropriate K_2 expression would give the required relief vent rate W.

Comparison with Exact Transient Solution

The present analytical solutions are compared with the more exact DIERS computer code (Grolmes and Leung, 1985) results based on a styrene runaway example discussed in a previous publication (Leung, 1986a). Similar to the DIERS code input, A_K in Eq. 2 is assigned a value of 1.18 for bubbly flow and 1.53 for churn flow, resulting in U_∞ values of 0.143 and 0.185 m/s, respectively. The vessel cross-sectional area is given as 4.57 m^2. For the same relief vent rate W of 180 kg/s, the temperature predictions during the overpressure period as shown in Figure 1 are in good agrement with the detailed numerical calculations for both bubbly and churn flow regimes. The slightly higher peak temperatures predicted by the analytical method merely lead to somewhat conservative results, which may be desirable in practice.

Relief Vent Rate Comparison

The required relief vent rates as a function of allowable overpressure are compared for the styrene polymerization example

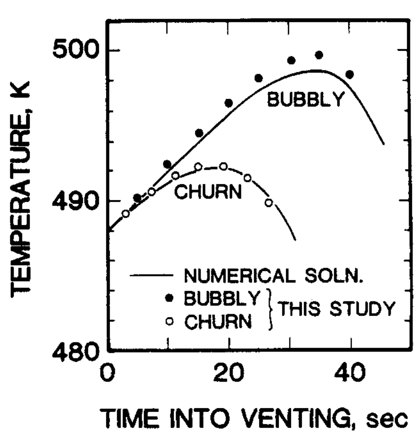

Figure 1. Transient solutions, styrene runaway example.
Relief vent rate, 180 kg/s

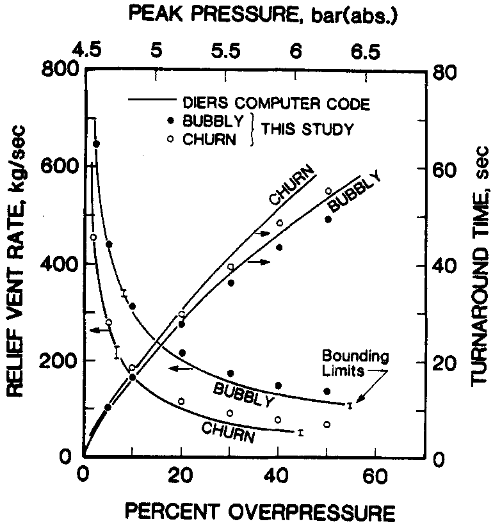

Figure 2. Relief vent rate and turnaround time, styrene runaway example.

in Figure 2. The analytical results are in excellent agreement with the code predictions at low overpressure and slightly more conservative at higher overpressure for both flow regimes. An approximation in the analytical approach was to assume W to be constant, while in the code calculation W was in fact varying. This variation is shown in Figure 2 by the band depicting the upper and lower bounds. Also shown for comparison are the turnaround time predictions, which are also in good agreement with the code results.

Similar comparison based on a previously discussed phenolic reaction (Leung, 1986a) is illustrated in Figure 3 with equally good agreement. In this example the vessel cross-sectional area is given as 3.14 m^2 by Booth et al. (1980). The calculated bubble rise velocities are 0.18 and 0.24 m/s for bubbly and churn regimes, respectively. Here homogeneous venting and all-vapor venting requirements are also shown with expected results. The bubbly flow regime yields results closer to homogeneous-vessel behavior, while the churn flow regime gives rise to substantial reduction in relief vent rate.

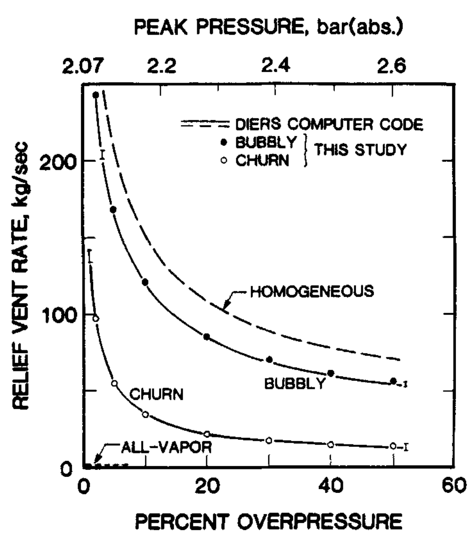

Figure 3. Relief vent rates, phenolic reaction example.

Relief Vent Area Evaluation and Comparison

In order to obtain the equivalent ideal vent area A from the required relief vent rate, the exiting mass velocity G has to be determined. Here G is normally a function of the exit quality x_1 in addition to P, T, h_{fg}, v_{fg}, and C_p. Leung (1986b) has recently proposed a generalized correlation for homogeneous equilibrium critical flow model (the model is generally regarded as conservative in relief venting). In this model the scaling parameter ω is given entirely in terms of known stagnation properties as

$$\omega = \frac{x_1 v_{fg}}{v} + \frac{C_p T P}{v}\left(\frac{v_{fg}}{h_{fg}}\right)^2 \tag{18}$$

Here $v = v_f + x_1 v_{fg}$. In equation form, the generalized correlation gives the following normalized mass flux $G/\sqrt{P/v}$:

For $\omega \geq 4.0$ (low-quality region)

$$G/\sqrt{P/v} = [0.6055 + 0.1356(\ln\omega) - 0.0131(\ln\omega)^2]/\omega^{0.5} \tag{19a}$$

and for $\omega < 4.0$ (high-quality region)

$$G/\sqrt{P/v} = 0.66/\omega^{0.39} \tag{19b}$$

The method suggested for evaluating a mean exit quality is as follows. First the mass remaining at the turnaround time is estimated via

$$\frac{m_p}{m_o} = 1 - \frac{\tau W}{m_o} = 1 - \tau^* \tag{20}$$

and using the relation $m = \rho_f(1 - \alpha)V$, the vessel void fraction at τ can be calculated

$$\alpha_p = 1 - (1 - \alpha_o)(1 - \tau^*) \tag{21}$$

By defining an average void fraction $\bar{\alpha}$ as being $(\alpha_o + \alpha_p)/2$, the mean exit quality $\bar{x}_1$ can be estimated from the appropriate expression of Eq. 8. Hence ω is evaluated at the known vessel condition at relief set pressure (i.e., P_s, T_s, h_{fg}, v_{fg}, C_p) and at this mean exit quality. The resulting mass velocity G obtained from Eq. 19 is, strictly speaking, more representative at the set pressure level. (Using this mass velocity at the set pressure will result in a more conservative vent area, as was done in Leung 1986a.) However, by defining an average pressure $\bar{P}$ during the overpressure period as being $(P_s + P_p)/2$ and by noting that G is nearly proportional to P, one can evaluate an average G simply as

$$\bar{G} = G\bar{P}/P_s \tag{22}$$

The ideal vent area can now be found

$$A = W/\bar{G} \tag{23}$$

Comparison of these vent area calculations with the computer predictions are shown in Figure 4 for the styrene example. The agreement is good for both the bubbly and churn flow regimes. Again the current analytical predictions are slightly more con-

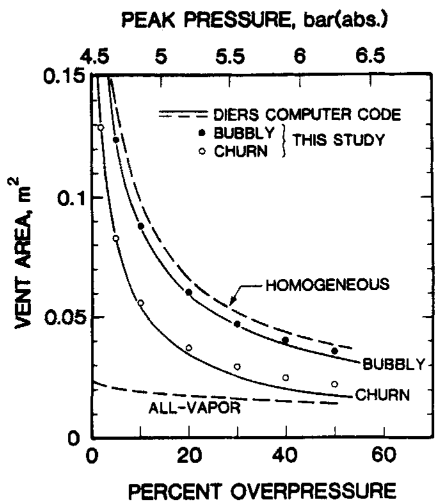

Figure 4. Vent area predictions, styrene runaway example.

servative at high overpressure. Like the relief vent rate results for the phenolic example, the required vent size for the bubbly regime is nearly the same as the homogeneous regime, while the vent size for the churn regime is substantially smaller. For example, at an overpressure of 20%, churn flow requires twice the all-vapor venting area, while bubbly flow or homogeneous venting requires three times that area. It is observed also that at higher overpressure the churn prediction comes very close to the all-vapor venting prediction. However, this behavior is quite dependent on the specific problem, as illustrated next.

The last example treats a fire exposure problem of a liquid propane gas (LPG) storage tank as discussed in Leung (1986a). Here a vertical vessel of 4.71 m dia. and 5.75 m height is subjected to a uniform fire heat flux of about 30 kW/m^2 at the wall, resulting in a total heat load of 3,126 kJ/s (or kW). The calculated bubble rise velocities are 0.14 and 0.18 m/s, respectively, for the bubbly and churn flow regimes. The results of this comparison are shown in Figure 5. Unlike the previous example, the vent size for bubbly flow is substantially smaller than the homogeneous-vessel case, indicating significant vapor-liquid disengagement. This result is due in large measure to the difference in the total energy release (or input) rate, Q. In the styrene case the total Q at set condition was 10.3 MW, while in the LPG case it was 3.1 MW. Thus the lower heat input in the latter would imply smaller relief vent rate, which in turn favors smaller superficial velocity and hence more disengagement. In fact, for the churn flow case, Figure 5 shows that complete disengagement occurs at about 10% overpressure such that all-vapor venting design appears adequate if indeed the fluid can be characterized as possessing churn behavior (Fauske and Leung, 1985). (With regard to adequacy, NFPA-30 [1984] allows 20% overpressure in above-atmospheric storage vessels in a fire exposure situation when the relief set pressure is at the design pressure of the vessel.)

Discussion

Asymptotic solutions

Asymptotic solutions were obtained for the churn regime case by noting that if $K_4 \ll K_2$ [i.e., $v_f/v_g \ll U_\infty A_x/(W v_g)$] and $K_3 \simeq K_4$ (i.e., $v_f/v_{fg} \simeq v_f/v_g$) in Eqs. 14 through 17, then for the runaway

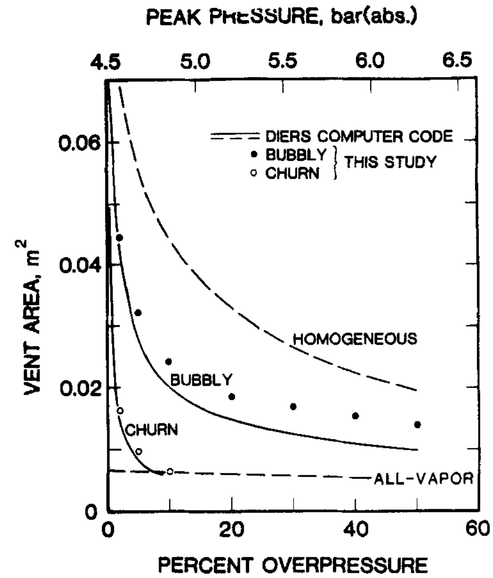

Figure 5. Vent area predictions, LPG storage tank fire exposure example.

reaction with churn regime:

$$m_p/m_o = 1 - \tau^* = -\frac{1}{K_7} + \left[\frac{1}{K_7^2} + \frac{2}{(1-\alpha_o)K_7}\right]^{1/2} \tag{24}$$

$$W = \frac{U_\infty A_x}{v_g} \cdot \frac{h_{fg}}{C_v \Delta T}\left[K_7 \tau^* - 2 \ln(1-\tau^*) - \frac{2}{(1-\alpha_o)}\left(\frac{\tau^*}{1-\tau^*}\right)\right] \tag{25}$$

and for external heating with churn regime:

$$m_p/m_o = 1 - \tau^* = \frac{2}{(1-\alpha_o)(K_8+2)} \tag{26}$$

$$W = \frac{U_\infty A_x}{v_g} \cdot \frac{h_{fg}}{C_v \Delta T}\left[-(K_8+2)\ln(1-\tau^*) - \frac{2}{(1-\alpha_o)}\left(\frac{\tau^*}{1-\tau^*}\right)\right] \tag{27}$$

The approximation taken is equivalent to:

a. Ignoring v_f/v_{fg} relative to x_1 in Eq. 7, and

b. ignoring the term v_f/v_g in Eq. 5b (or $j_f \ll j_g$ in the drift flux formulation.)

For most practical applications, the above solutions yield quite accurate results, as illustrated by the phenolic example in Figure 6. The asymptotic behavior with overpressure is clear. This analysis also shows that the fraction of mass remaining at the turnaround time, m_p/m_o, is, to a first order, independent of the relief flow W, as is evident from Eqs. 24 and 26. This stems from the fact that at turnaround the required vapor mass flow Wx_1 is related to the available superficial velocity j_g, via Eq. 4, which in turn is governed by the vessel bulk void fraction, via Eq. 1. Figure 6 shows that the m_p/m_o variation is confined mostly to the low overpressure region and that it attains the asymptotic behavior quickly at about 10% overpressure.

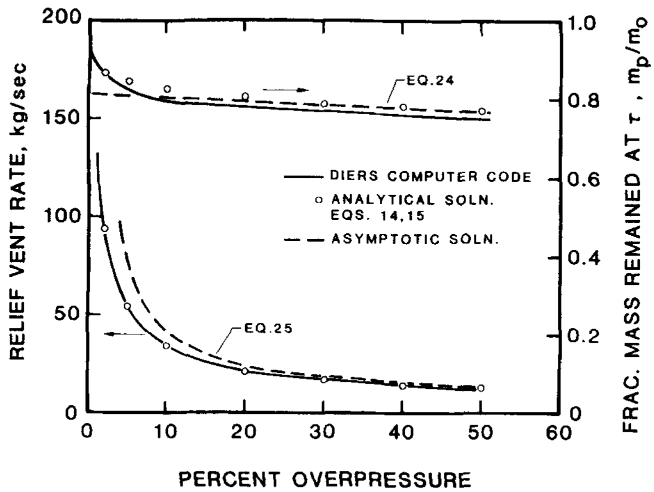

Figure 6. Asymptotic venting behavior of phenolic reaction reactor in churn regime.

For the bubbly regime no such useful asymptotic solution was obtained. The above approximations are no longer valid because the exit quality x_1 is generally of the same order of magnitude as the density ratio v_f/v_{fg}.

Limitation

To avoid applying these results outside their expected domain of applicability, it is prudent to consider the implications of the assumptions made in this paper. Again it should be stated that the present analyses of the overtemperature response are correct regardless of any changes in system volatility during relief. However, the corresponding overpressure response is treated only for the constant-volatility case. It can be argued that the present results are applicable to systems with decreasing volatility; i.e., reaction products are less volatile than reactants. Huff (1982) found that generally conservative results were obtained in these systems.

The present treatment assumes a radial void distribution parameter C_o of unity in the drift flux relation. This not only simplifies the mathematics considerably, but yields the most conservative results (Grolmes and Leung, 1985). However, in the case of external heating such as fire exposure, this assumption will lead to overly conservative results, as the bubble boundary layer analysis by Grolmes and Epstein (1985) reveals a highly nonuniform radial void distribution.

The relief vent area is evaluated so far in terms of an ideal nozzle area with discharge coefficient of unity. The application to actual relief design is beyond the scope of this work.

The assumptions made in arriving at the analytical results do not seem to result in any nonconservatism, as the example comparisons clearly illustrate. No treatment of flow regime transition from bubbly to churn has been attempted, for indeed such a transition criterion has been lacking. For a system where surface tension information is available (U_∞ is dependent on σ to the 0.25 power, hence not too sensitive; most fluids yield U_∞ in the vicinity of 0.15 m/s) the bubbly flow results are recommended in the absence of any flow regime characterization. As shown before, this could yield vent sizes that are significantly smaller than homogeneous-vessel venting for some low energy release rate systems. Finally, the churn flow results are to be employed when the system fluid is known to behave in a nonbubbly (nonfoamy) manner under similar venting conditions.

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Notation

- A = ideal vent area
- A_k = constant in bubble rise velocity expression, Eq. 2
- A_x = vessel cross-sectional flow area
- C_p = liquid specific heat at constant pressure
- C_v = liquid specific heat at constant volume
- G = discharge mass velocity mass flow rate per unit area
- g = gravitational constant, 9.81 m/s
- h_{fg} = latent heat of vaporization
- j_g = vapor superficial velocity
- $K_0 - K_8$ = dimensionless variables or constants, Eq. 9
- m = instantaneous mass in vessel
- m_o = initial mass in vessel
- n = flow regime index, Eq. 1
- N_T = dimensionless overtemperature, Eq. 9
- P = system pressure
- q = heat release rate per unit mass
- Q = total heat input or release rate
- t = time
- t_e = emptying time as given by m_o/W
- T = system temperature
- U_∞ = bubble rise velocity, Eq. 2
- v = two-phase specific volume = $v_f + xv_{fg}$
- V = internal volume of vessel
- W = relief mass flow rate or vent rate
- x_1 = quality or mass fraction of vapor exiting vessel

Greek letters

- α = vessel average void fraction
- α_T = void fraction at top of vessel
- ρ = density
- ΔT = temperature rise above set, corresponding to overpressure
- τ = turnaround time
- τ^* = dimensionless turnaround time, Eq. 9
- σ = surface tension
- ω = critical flow scaling parameter, Eq. 18

Subscripts

- 1 = location at vent line entry point
- f = liquid phase
- fg = difference between vapor phase and liquid phase
- g = gas or vapor phase
- o = initial
- p = peak or turnaround condition
- s = set condition
- T = total

Appendix: Derivation of Eq. 1

The starting point is the consideration of vapor and liquid phase continuity at the top of the vessel; thus

$$Wx_1 = \rho_g j_g A_x \tag{A1}$$

$$W(1 - x_1) = \rho_f j_f A_x \tag{A2}$$

Dividing Eq. A1 by Eq. A2 and rearranging:

$$j_f = j_g \left(\frac{1 - x_1}{x_1}\right) \frac{v_f}{v_g} \tag{A3}$$

The drift flux defined as (Wallis, 1969)

$$j_{gf} \equiv (1 - \alpha_T)j_g - \alpha_T j_f \tag{A4}$$

can be correlated by the general form

$$j_{gf} = \alpha_T(1 - \alpha_T)^n U_\infty \tag{A5}$$

Equations A3, A4, and A5 allow elimination of j_f and j_{gf}, and the resulting equation for j_g as given by Eq. 1 is obtained.

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