ITP Waste Tank Positive Pressure Inerting System

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Introduction

Several of the waste tanks employed in the In-Tank Precipitation (ITP) process are inerted with nitrogen in order to maintain an oxygen concentration less than a limit based on the Composite Minimum Oxygen Concentration (CMOC) required for combustion for the range of potential combustible gas mixtures which could be present. The tanks are also normally operated at a vacuum relative to the atmosphere in order to limit worker hazards (radiological, toxicological and asphyxiation) and contamination in the immediate vicinity of the tanks.

A positive pressure backup inerting system has been designed and installed which can be utilized to maintain the tank in an inerted condition in the event the purge and ventilation systems are unavailable for an extended period of time. To accomplish this, the system must be activated before the CMOC is reached and must supply sufficient flow to purge any residual air inleakage. However, unnecessary system activation is undesirable due to the personnel hazards and potential for contamination associated with positive pressure operation. Likewise, once the system is activated, an unnecessarily high flow rate is undesirable.

Three types of evaluations were carried out to support the design of the waste tank backup positive-pressure inerting system: (a) the maximum duration prior to activation of the backup inerting system in order to ensure inerting is preserved, (b) the minimum flow rate required to ensure that inerting is maintained once the system is activated, and (c) the extent of localized regions with O₂ concentrations above the CMOC near air inleakage points. Calculations were performed for both bounding and best-estimate conditions. This paper provides an overview of the methods used to perform the first two of these evaluations along with the most relevant results; the local O₂ concentrations evaluation is not discussed in this paper.

Description of Waste Tank and Normal Inerting System

The waste tanks of interest are 85' in diameter and have a height of 33'. Each tank has a total internal volume of 1.39x10⁶ gallons; the maximum permissible liquid volume is 880 kgal, so that the minimum vapor space is 510 kgal. The tanks have a number of risers (penetrations) through the top slab which provide access for permanently installed equipment and allow temporary access for measurements, sampling, and visual observations. The risers are sealed via gasketed
concrete plugs. The risers, along with the installed equipment and systems, also represent air inleakage points.

The tanks are iner ted with nitrogen in order to maintain an O₂ concentration less than a limit based on the CMOC. The highest CMOC value for the benzene-hydrogen mixtures of interest is 9%. An operating limit of 8% has been adopted based on this value; the tanks are typically operated at approximately 5%O₂ while iner ted. The tanks are operated at a vacuum (≈ 1/2” water column) relative to the atmosphere in order to limit worker hazards and tank top contamination. The vacuum is maintained by operation of a tank ventilation system. Hence, the normal nitrogen purge system flow rate must overcome air inleakage due to the vacuum; typical air inleakage rates are in the range of 100 scfm. The nitrogen is introduced to the tank through a 1” nozzle, and the relatively high flow rate required to preserve iner ting is also effective at both mixing the tank vapor space and diluting the combustible gas concentration; fuel (i.e. combustible gas) control is utilized as a defense-in-depth protection measure.

Methodology

Tank Hydraulic Resistance

The tank hydraulic resistance must be known in order to evaluate the air inleakage rate from imposed pressure differential. However, the hydraulic resistance cannot be calculated since the air inleakage paths are not well-defined. Experimental vacuum-flow rate measurements were made specifically for this purpose. The most relevant test data is for flow rates between 10 and 30 scfm [corresponding to vacuums of 10 to 40 milli-inches water column for riser leakage only (i.e. all engineered penetrations closed)]. These data were correlated using the expression given below:

\[ \Delta P = a \cdot Q + b \cdot Q^2 \]  

Where:  
\( \Delta P \) = Pressure differential,  
\( Q \) = Flow rate, and  
\( a, b \) = Empirical correlation coefficients.

Note that the use of this expression implicitly assumes a laminar (or fully turbulent) flow regime\(^1\); this assumption appears justified in the flow rate regime of interest since the data fit this representation reasonably well. It is noted that historical data corresponding to a 100 scfm flow rate are in good agreement with the data from the hydraulic resistance tests. While the tank hydraulic resistance is expected to vary over time due to variations in seal performance and equipment modifications, this agreement indicates that such changes have not been significant.

The best-estimate calculations were performed using the correlated test data directly. The tank and structures connected to the tank risers are seismically qualified; however, the tank hydraulic resistance could be increased as a result of a seismic event due to seal degradation. The nominal leakage was therefore doubled for the bounding calculations to account for potential seismic effects. Additional terms were included for the bounding calculations to account for engineered penetrations which were not seismically qualified.
Air Inleakage Modes

Four modes of air inleakage were evaluated: natural convection, atmospheric pressure fluctuations, wind effects, and molecular diffusion. Molecular diffusion was shown to be insignificant in comparison to the other modes. The basic modeling approach and data employed to describe each of these mechanisms are discussed below.

Natural convection can occur because the tank vapor space is generally at a higher temperature and has a lower average molecular weight than the outside environment. The pressure differential established across the tank top due to the density difference between the tank vapor space and outside environment (i.e., the buoyancy or natural convection driving force) can be written as:

$$\Delta P_{nc} = g \cdot \Delta z \cdot \Delta \rho$$

where:

- $\Delta P_{nc}$ = Natural convection (buoyancy) driving force,
- $g$ = Gravitational acceleration,
- $\Delta z$ = Elevation change, and
- $\Delta \rho$ = Density difference.

The elevation change across the tank top is 5'. The $\Delta z$ term in eqn.(2) was taken to be 50% larger (i.e., 7.5') in order to account for buoyancy driven flow in the absence of any $\Delta z$. The density difference between the tank vapor space and outside environment is given by the ideal gas law based on the composition and temperature of each region. The outside air was taken to be dry while the tank vapor space was assumed to be saturated in order to maximize the $\Delta \rho$ term; the $O_2$ depletion within the tank vapor space also increases the density difference. The maximum allowable liquid phase temperature is restricted to 40°C and this value was employed as the vapor space temperature for the bounding calculations; 30°C was taken to be the best-estimate value. A data base of daily temperature readings covering 34 years was employed to develop cumulative distributions for the minimum and average daily local temperature. The 95th percentile minimum daily temperature (-4°C) was employed for the bounding calculations; this is more conservative than the 95th percentile would indicate since this value is applied over an extended period. The 50th percentile average daily temperature (18°C) was used for the best-estimate case. In calculating the air inleakage rate, it was assumed that half the tank leak area was available for air to enter the tank, with the other half serving to allow vapor to flow out of the tank. Note that the air inleakage rate due to natural convection, for a given temperature and composition set, does not depend on the tank vapor space volume.

Low frequency atmospheric pressure fluctuations are effective at moving air into and vapor out of the tank; this has been generally referred to as tank “breathing”. The average air flow rate into the tank due to an atmospheric pressure increase can be expressed as:

$$Q_{pc} = \frac{\Delta V_{pc}}{\Delta t_{pc}} = \frac{V_v}{P_v} \cdot \left( \frac{\Delta P_{pc}}{\Delta t_{pc}} \right)$$

where:

- $Q_{pc}$ = Average flow rate due to an atmospheric pressure decrease,
\[ \Delta V_{pc} = \text{Volume which must be transferred to equalize atmospheric pressure,} \]
\[ \Delta t_{pc} = \text{Time period over which atmospheric pressure change occurs,} \]
\[ V_v = \text{Vapor space volume,} \]
\[ P_v = \text{Vapor space pressure (absolute), and} \]
\[ \Delta P_{pc} = \text{Amplitude of atmospheric pressure change.} \]

A prior evaluation of atmospheric pressure fluctuation data covering a five year period indicated average and 2-sigma daily fluctuations of 5 and 11 mbar/day, respectively; these values were employed for the best-estimate and bounding assessments. The 11 mbar/day value is more conservative than the 2-sigma notation would indicate since this calculation implicitly assumes that the atmospheric pressure is increasing while the data covered periods of increasing and decreasing atmospheric pressure. Note that the effective air inleakage rate due to atmospheric pressure fluctuations is proportional to the tank vapor space volume.

Wind effects can drive air into the tank via two separate mechanisms: the creation of localized high and low pressure areas due to wind impact on tank top structures, and tank top pressure fluctuations due to cyclic winds. The pressure associated with a given wind velocity, the dynamic wind pressure, can be calculated as:

\[ \Delta P_w = \frac{1}{2} \cdot \rho_e \cdot v^2 \]  \hspace{1cm} (4)

where: \( \Delta P_w = \text{Dynamic wind pressure,} \)
\( v = \text{Wind velocity.} \)

A data base of hourly average wind speeds for a 10 meter elevation covering a 5-year time period was utilized to derive wind speed values for this application. The bounding value was taken to be the 95th percentile value for a 4 hour period (3.8 m/s), while the best-estimate case employed the 50th percentile value for an extended period (2.7 m/s). The cumulative probability distribution generated using these data was employed in a Monte Carlo simulation to examine the initial tank vapor space response to a sustained purge.

For a cyclic wind, the volume which must be transferred to eliminate the imposed pressure differential is calculated directly from the dynamic wind pressure and tank vapor space volume. The cyclic wind amplitude was taken to be equal to the mean wind speed value. The average flow rate at which this transfer occurs is taken to be the half the initial flow rate calculated using eqns. (1) and (4), adjusted for the presence of any nitrogen purge flow into the tank. This allows a minimum wind cycle length and maximum average air inleakage rate to be calculated. Based on judgement, lower bounds on the minimum wind cycle length were taken to be 2 minutes for the bounding case and twice this value best-estimate case. Although a statistical analysis was not performed, the probability of a sustained cyclic wind with a frequency falling into this range over the entire time period of interest is judged to be low. The air inleakage rate calculated in this fashion was decreased by a scalar to account for the accumulator effect exerted by the volume sealed off within the risers; this scalar was set to 0.5 for the best-estimate case based on a simplified evaluation, and to 0.75 for the bounding case. The effective air inleakage rate due to
cyclic winds is proportional to the tank vapor space volume if the minimum wind cycle length is imposed, and is otherwise independent of vapor space volume.

A constant wind will create localized high and low pressure areas near tank top structures. However, the pressure variation will fall off rapidly with distance away from the structure and there are few such structures located in the immediate vicinity of risers. For the bounding case, the full dynamic wind pressure [eqn.(4)] was applied to 10% of the total tank top leak area; only 5% of the leak area was assumed for the best-estimate case. Of the impacted leak area, half was assumed to be available for air to enter the tank, with the other half serving to allow vapor to flow out of the tank. The effective air inleakage rate due to constant winds is independent of the tank vapor space volume.

There will be some interference between the air inleakage modes described above. For example, the same leak path cannot simultaneously be employed to push air into the tank via atmospheric pressure fluctuations or cyclic winds and be employed to draw gas from the tank via natural convection. As such, it is evident that the total air inleakage rate is not the sum of that due to each individual mechanism. However, as with cyclic and constant winds, there may be some coupling or overlap between the mechanisms so that the total rate may be larger than any one. The total air inleakage rate was therefore taken to be the square root of the sum of the squares for each mechanism, as suggested by the ASHRAE guidelines.

It should be clearly recognized that the results for the bounding case will actually be more conservative that any of the individual input data since most of the input data are independent. That is, 95th percentile atmospheric temperature was employed jointly with bounding vapor compositions, conservative tank hydraulic resistance data, and 2-sigma wind and atmospheric pressure fluctuation values. This is tempered to some degree by the dominance of air inleakage due to natural convection (i.e. the conservatism in the other air exchange modes does not have a severe impact on the calculated results).

**Bulk Oxygen Concentration**

The bulk oxygen concentration was calculated assuming a well-mixed vapor space. This is the expected behavior due to the air entry mode. Even if this is neglected, molecular diffusion would provide relatively good mixing since the relevant air inleakage rates are quite low.

**Results**

**Time Required to Reach the CMOC**

Figure 1 shows the bounding air inleakage rates over the range of potential liquid volumes along with the corresponding times required to reach the CMOC for initial oxygen concentrations of 4 to 8%. The air inleakage rate is only a weak function of liquid volume, decreasing from 11.7 down to 10.4 cfm as the liquid volume increases from 150 to 880 thousand gallons. This insensitivity occurs because the air inleakage rate is dominated by the contribution from natural convection (9.7 scfm). The decreased air inleakage rate at high liquid volumes is more than offset by the impact of the reduced vapor space volume on the time required to reach the CMOC. The time required to reach the CMOC decreases from 64 to 29 hours over this span of liquid volumes.
(150 to 880 kgal) for the nominal initial oxygen concentration (5%), and from 18 to 8.2 hours for the bounding initial oxygen concentration (8%). Hence, with the maximum initial oxygen concentration and bounding input data, at least 8 hours would be required to reach the CMOC.

Figure 1 Air Inleakage Rate and Time to CMOC (Bounding Case).
The air inleakage rates with the best-estimate data are a factor of 5 lower than those calculated using the bounding data. The time required to reach the CMOC decreases from 369 to 171 hours over the liquid volume range of interest (150 to 880 kgal) for the nominal initial oxygen concentration (5%), and from 103 to 48 hours for the bounding initial \( O_2 \) concentration (8%).

Sensitivity analyses were performed which demonstrated that the time to CMOC values are only significantly influenced by the temperature and hydraulic resistance values; this is to be expected since the natural convection air inleakage rate is dominant. The wind effects parameters, whose values were chosen in a generally conservative but somewhat subjective manner, have little impact on the time to CMOC. The time required to reach the CMOC was calculated over a range of nominal tank leakage multipliers in order to define the maximum allowable hydraulic resistance degradation (i.e. to define an leakage acceptance criteria for future use). Increasing the allowable tank leakage beyond the current value requires shortening the required time period to initiate backup purge flow below the 8 hour value given above for the bounding case. For example, using a tank leakage multiplier of 3.5 rather than 2 for the bounding case drops the required time from 8 hours down to 5 hours.

**Required Purge Flow Rate**

The positive pressure backup inerting system was designed to provide a flow of 30 scfm based on preliminary estimates, and hence the bulk of this evaluation was directed at examining the adequacy of a 30 scfm purge. This purge flow rate would maintain the \( O_2 \) concentration below the CMOC for air inleakage rates up to 21.7 scfm (purge gas contains a maximum of 0.3\% \( O_2 \)). For the 30 scfm purge to be effective, it must therefore pressurize the tank sufficiently to preclude sustained air inleakage rates in excess of this value. This, of course, will be the case since the bounding air inleakage rates with no purge (preceding section) are half this value.

Atmospheric pressure fluctuations occur slowly enough that even small purge flows are capable of preventing air inleakage due to this mechanism. The flow required to raise the tank pressure to the value required such that no air inleakage occurs due to natural convection under bounding conditions is 37 scfm; the maximum air inleakage rate due to natural convection under bounding conditions with a purge flow of 30 scfm would be 0.4 scfm. Air inleakage due to natural convection would be precluded by a 30 scfm purge flow given the nominal tank leakage and/or best-estimate driving force.

The flow rates required to preclude wind driven air inleakage are quite high. For example, to ensure no air inleakage due to wind effects with an 85\% confidence for a one week period (8.2 m/s wind velocity, the 99.9\% hourly average value) would require 91 and 195 scfm purge flows for the nominal and bounding tank leakage, respectively. With the bounding tank leakage, a 30 scfm purge flow would only preclude inleakage from wind velocities less than 2.7 m/s (≈ 50\% percentile hourly average velocity). However, high wind velocities must be sustained over an extended period in order to raise the \( O_2 \) concentration above the CMOC. For example, a wind velocity of 12 m/s (27 mph) would require approximately 13 hours to bring the \( O_2 \) concentration to the CMOC from an initial concentration of 8.6\% (bounding initial concentration prior to backup purge activation). Such high sustained winds are judged to be highly unlikely and would only be possible during severe storms. However, for these results to be relevant, such a storm would have to follow a precursor event capable of increasing the tank leakage to the bounding value imposed in this evaluation (such as a seismic event). Furthermore, reducing the initial \( O_2 \)
concentration to 5% increases the 12 m/s wind duration required to reach the CMOC out to approximately 2 days. This is relevant since a 30 scfm purge will lower the bulk O₂ concentration rapidly even under bounding conditions. Hence, a particularly severe storm with high sustained winds would have to occur immediately following the precursor event to actually be capable of driving the O₂ concentration to the CMOC; in conjunction with the bounding case input data set, such a combination of events is judged to be incredible.

Figure 2 gives the O₂ concentration history based on a Monte Carlo simulation. Wind velocities sampled from the hourly average wind velocity probability distribution were applied for one-hour periods in this simulation; bounding values were employed for all other input parameters. As can be seen, the O₂ concentration falls monotonically over the first 60 hours, dropping below the MOC for a H₂-air mixture (4.7%) in just under one day. High velocity winds have only a small impact; for example, a 10 m/s wind which occurs 100 hours into the simulation only raises the bulk O₂ concentration by about 0.3% (see fig.2). The equilibrium value is approximately 0.9%, which would correspond to an average air inleakage rate of just under 1 scfm, the air inleakage rate is therefore dominated by wind effects rather than natural convection.

A number of sensitivity studies were performed using Monte Carlo simulations. The results discussed above were not significantly impacted by reasonable variations in the parameters utilized in the wind-driven air inleakage rate models. Increasing the tank leakage multiplier to 3.5 rather than 2 for the bounding case increases the equilibrium O₂ concentration from 0.9 to 3.3%; the time required to fall below the H₂-air MOC approximately doubled. The effect of extended high wind periods was examined by extending the period over which a given wind velocity was applied from 1 hour up to periods as long as 6 hours; the sampling period does not effect the
equilibrium O₂ concentration, but does impact the transient behavior and approach to equilibrium. Increasing the sampling period from 1 to 4 hours with a tank leakage multiplier of 2 increased the maximum O₂ concentration after 4 days from 1.7 to 2.1%. With the increased tank leakage multiplier of 3.5, increasing the sampling period from 1 to 4 hours increased the maximum O₂ concentration after 4 days from 3.9 to 4.5%.

A single additional case was run at a purge flow rate of 20 scfm to illustrate the impact of reduced purge rates. A tank leakage multiplier of 3.5 was employed for this case. The reduced purge results in the air inleakage rate being dominated by natural convection rather than wind effects. The equilibrium O₂ concentration is approximately 6.3%, as compared to 3.3% for the corresponding case with a 30 scfm purge. Differences in the Monte Carlo simulations were consistent with the difference in the equilibrium O₂ concentrations for these cases. A 20 scfm purge would therefore be sufficient to preclude the O₂ concentration from exceeding the CMOC. However, under bounding conditions with a tank leakage multiplier of 3.5, the O₂ concentration would not fall below the H₂-air MOC.

**Conclusions**

The minimum time required for the bulk O₂ concentration to reach the CMOC (9%) from the maximum initial value (8%) would be 8 hours. The required time increases to 2 days under best-estimate environmental and hydraulic resistance conditions, and to more than a week at typical initial oxygen concentrations and liquid volumes. Sensitivity analyses demonstrated that these results are significantly influenced only by temperature and tank leakage. Increasing the tank leakage multiplier from 2 to 3.5 drops the time required to reach the CMOC from 8 to 5 hours.

A nitrogen purge flow rate of 30 scfm was shown to be more than sufficient to maintain inerting once the backup system is activated. This flow rate would limit the air inleakage due to natural convection to 0.4 scfm even under bounding conditions, and the total average air inleakage rate to less than 1 scfm. High wind velocities would have to be maintained over an extended period for the oxygen concentration to reach the CMOC even under bounding conditions. Monte Carlo analyses demonstrated that the oxygen concentration would fall rapidly upon activation of the backup purge system at 30 scfm and that high wind velocity periods, while resulting in small oxygen concentration increases, would not be capable of bringing the bulk oxygen concentration above the CMOC. The bulk O₂ concentration would fall below the MOC for H₂ in less than one day with an equilibrium concentration under 1%. Sensitivity studies were carried out which demonstrated that the conclusions stated above hold for reasonable variations in the key model parameters. The 30 scfm purge flow rate was shown to be acceptable with the tank leakage multiplier increased from a value of 2 to 3.5 even under bounding conditions. Reducing the purge flow rate to 20 scfm would still ensure the bulk O₂ concentration remained below the CMOC, but would allow it to exceed the MOC for H₂ under bounding conditions.

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References
