Technical Issues for Implementing Alternative Source Term at Nuclear Power Reactors - Aerosol Deposition Model in BWR Steam Lines

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1.0 Introduction

The Alternative Source Term (AST, References 1, 2, and 3) is a voluntary replacement for the traditional TID-14844 Source Term (References 4, 5, 6, and 7). It is the result of nearly two decades of analysis and experimentation that followed the accident at Three Mile Island Unit 2. It is used to assess the adequacy of the containment and of the containment air cleaning systems for a recovered core melt accident of a non-specified type, as well as the adequacy of certain site characteristics such as the size of the exclusion area.

The AST provides the type, timing, and form of the activity release from the damaged core to the containment. It differs from the TID-14844 source term in that the release is primarily an aerosol (i.e., particulate of a very small size). It recognizes that even for a recovered core melt accident, an aerosol mass of several hundred kilograms (perhaps even a tonne) could be released to the containment.

For BWRs, the main steam lines constitute an important release pathway from the containment to the environment. These lines represent a potentially significant bypass of the BWR secondary containment (designed to collect and process primary containment leakage) and are typically seismically-designed and designated as Safety-Related only up to the outer containment isolation valves (i.e., the outboard MSIVs). On many BWRs, a MSIV-Leakage Control System (MSIV-LCS) is incorporated into the plant design to provide a means of remote-manually depressurizing and venting the main steam lines to the secondary containment so that MSIV leakage would become essentially like any other containment leakage. On other BWRs, the main steam lines beyond the MSIVs and the main condenser have been assessed for seismic ruggedness, drain line pathway isolation valves have been powered from emergency busses, and exemptions to the Safety-Related definition of Reference 8 have been obtained to permit accident mitigation credit for hold-up and deposition in the non-Safety-Related portions of the steam lines and in the main condenser. The application of the AST can frequently permit the removal of the first kind of design feature (MSIV-LCS) without having to implement the second. In fact, some AST applications have simultaneously supported removal of the MSIV-LCS and a substantial increase in the allowable MSIV leak rate without having to effectively extend the original seismic qualification or Safety-Related boundaries of the steam lines.

To satisfactorily apply AST for the purpose of better addressing MSIV leakage, it is necessary to calculate the aerosol removal rate in the steam lines. It is the purpose of this paper to discuss technical issues related to that calculation.

2.0 Defining the Problem

2.1 Defending the Assumption that the Drywell is the Source for MSIV Leakage

MSIV leakage originates within the reactor vessel. The assumption that the activity release occurs to the BWR drywell (with a dilution volume many times that of the reactor vessel) would appear to be a substantial nonconservatism; but in fact, it is not. Figure 1 illustrates a case with a reactor vessel volume of V_{RV} = 2E4 ft^3, a drywell volume of 2E5 ft^3, and a volumetric flow rate, F, of 3000 cfm out of the reactor vessel, through the drywell, and into the torus/wetwell/ containment (for Mark I/II/III containments, respectively). The 3000 cfm is the drywell purge flow value permitted for Mark III containments in Reference 3.

Assuming a unit activity concentration, C_0, in the reactor vessel at t = 0, the activity would essentially be removed completely from the reactor vessel by 20 - 30 minutes for a purge flow rate of 3000 cfm. The integrated activity concentration for the reactor vessel would reach a maximum value of approximately C_0V_{RV}/F = 6.7 C-minutes at that time (for C_0 = 1 C). The activity concentration in the drywell would peak at about t = 17 minutes; and by two hours, the integrated drywell activity concentration would be almost as great as that for the vessel (i.e., about 5.4 C-minutes, as shown on the "Int DW" plot of Figure 1).

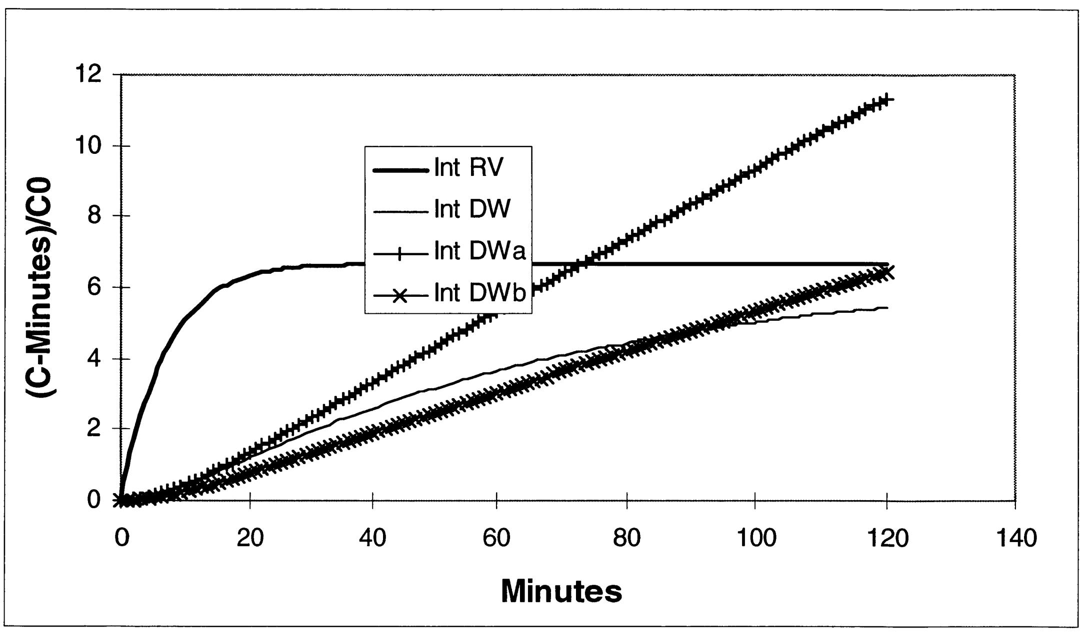

Figure 1. Integrated Concentration, Reactor Vessel vs. Drywell.

For a 1.5E5 ft^3 drywell, the two-hour integrated activity concentration would be 6.0 C-minutes. This illustrates the sensitivity of the two-hour integrated activity concentration in the drywell as a function of the fractional flow rate (volumes per unit time) out of the vessel and through the drywell.

The Reference 3 assumptions acceptable to the NRC do not permit credit for a drywell purge except for Mark III containment designs. A second case was run in which the purge flow from the drywell to the torus/wetwell/containment was set to zero ("Int DWa"), and this case shows that the two-hour integrated activity concentration in the drywell is about 1.7 times greater than that in the reactor vessel. A third case was run in which a torus/wetwell volume of 1.5E5 ft^3 was added to that of the drywell (i.e., a well-mixed Mark I/II volume, "Int DWb"); and even for this case, the two-hour integrated activity concentration in the drywell is about the same as that in the reactor vessel. Therefore, it is seen that two-hour integrated activity concentration in the drywell is about the same as (or even greater than) that in the reactor vessel.

In cases where natural aerosol and elemental iodine removal in the drywell is credited, the results are not greatly different from those given above because of the relatively small removal rates. If drywell sprays are credited, however, the integrated activity concentration in the drywell can be substantially reduced. Polestar has performed proprietary assessments of drywell sprays and has concluded that the drywell integrated activity concentration can still be justified as the assumed source for MSIV leakage even with sprays operating. These assessments are beyond the scope of this paper.

2.2 *Conversion of the MSIV Allowable Leak Rate (in SCFH) to a Volumetric Flow from the Drywell*

The MSIV allowable leak rate is stated as a mass flow rate of air expressed in SCFH. Since the test pressure is known (assumed in the following example to be 28 psig, a typical value), the flow area of the allowable MSIV leak path can be determined.

In this example, the MSIV leak path is assumed to behave like a nozzle. The critical pressure for such a flowpath, P_{cr}, is a function of the ratio of the specific heats for the medium (k), and can be expressed as:

$$P_{cr} = P \times \left(\frac{2}{k+1} \right)^{\frac{k}{k-1}} .$$

With P = 28 psig (or 6148 psfa) and k = 1.4, P_{cr} = 3248 psfa (22.6 psia). As the critical pressure corresponding to the test pressure is greater than ambient pressure, one can be assured that the volumetric flow through the leak path will remain constant for a constant temperature (i.e., the flow will be choked).

Knowing v, the specific volume of air at pressure, P, one may calculate a mass flow per unit flow area (G, usually expressed in units of lbm/sec-ft^2) as follows:

$$G = \sqrt{2 \times g_c \times \frac{k}{k-1} \times \frac{P}{v} \times \left(\left(\frac{P_{cr}}{P} \right)^{\frac{2}{k}} - \left(\frac{P_{cr}}{P} \right)^{\frac{k+1}{k}} \right)}.$$

Numerically, with P = 6148 psfa, P_{cr} = 3248 psfa and v = 4.59 ft^3/lbm,

$$G = \sqrt{2 \times 32.2 \times \frac{1.4}{1.4-1} \times \frac{6148}{4.59} \times \left(\left(\frac{3248}{6148} \right)^{\frac{2}{1.4}} - \left(\frac{3248}{6148} \right)^{\frac{1.4+1}{1.4}} \right)} = 142.1 \text{ lbm/sec-ft}^2.$$

Now, assuming that the allowable MSIV leakage under test conditions is 100 SCFH (i.e., 2.1E-3 lbm/sec), one may determine an equivalent flowpath size for the allowable MSIV leakage by dividing this mass flow by the G calculated above. One obtains an area A_{100} = 1.48E-5 ft^2. The corresponding diameter is D_{100} = 0.13 cm.

In like manner, for an 11.5 SCFH case, $A_{11.5}$ = 1.7E-6 ft^2 and $D_{11.5}$ = 0.045 cm.

Under accident conditions, steam rather than air is assumed to be leaking out of the drywell (note that k for steam is 1.3). Assuming a typical maximum accident drywell pressure of P = 46.9 psig (or 8870.4 psfa), and a typical maximum accident drywell temperature of T = 340 F, one can calculate the maximum accident volumetric flow.

The saturation temperature of steam corresponding to an absolute pressure of 8870.4 psfa (61.6 psia) is 294 F. The corresponding specific volume is v = 7.0 ft^3/lbm. However, the drywell temperature is assumed here to be superheated at 340 F; and thus, the specific volume will increase approximately by the ratio of the absolute temperatures. Therefore, one may assume v = 7.4 ft^3/lbm.

Under these conditions, and using the same expressions as before, one may calculate a critical pressure P_{cr} of 4841 psfa (33.6 psia > 14.7 psia), showing once again that the volumetric flow of steam through the MSIV leakage pathway would remain constant for a constant temperature.

Calculating G as before (but for accident conditions) and knowing v, one may calculate a volumetric flow of steam per unit flow area under accident conditions:

$$(v \times G)_{accident} = 970\ ft^3/sec\text{-}ft^2.$$

Multiplying the steam volumetric flow per unit flow area (970 $ft^3/sec\text{-}ft^2$) by the MSIV leakage areas corresponding to 100 SCFH and 11.5 SCFH, one obtains:

$$Q_{100} = 1.47E\text{-}5 \times 970 \times 3600 = 51.3\ cfh$$

$$Q_{11.5} = 1.7E\text{-}6 \times 970 \times 3600 = 5.9\ cfh.$$

While this method is rigorously correct for leak paths that can be assumed to behave as nozzles, there is sufficient uncertainty in that assumption to justify a greatly simplified approach.

To convert SCFH measured during a test of the MSIVs to a volumetric flow using a simpler method, one may: (a) obtain the test volumetric flow by multiplying the measured SCFH by the ratio of ambient pressure to absolute test pressure, (b) verify that the critical pressure corresponding to the test pressure is greater than ambient pressure (to establish that the volumetric flow through the leak path would be governed by the gas sonic velocity and would remain constant for a constant temperature), and (c) convert the volumetric flow of air at standard temperature to that of steam at maximum temperature by comparing sonic velocities. To illustrate with a 100 SCFH case:

(a) The volumetric flow rate under test conditions is CFH_{test} = 100 SCFH x 14.7 / (28 + 14.7) = 34.4 cfh,

(b) 22.6 psia > 14.7 psia,

(c) The ratio of the sonic velocities is as follows (‘ is used to identify accident conditions):

$$\frac{v'_{sonic}}{v_{sonic}} = \frac{\sqrt{(k'-1)\frac{c'_p}{M'}T'}}{\sqrt{(k-1)\frac{c_p}{M}T_{\text{standard}}}} = \sqrt{\frac{k'-1}{k-1}\frac{c'_p}{c_p}\frac{M}{M'}\frac{T'}{T}} = \left(\frac{(0.3)(8)(29)(800R)}{(0.4)(7)(18)(530R)}\right)^{0.5} = 1.444$$

One may now retrieve the volumetric flow rate under accident conditions multiplying the result under (a) by this ratio. One finds:

$$Q_{100} = 49.7\ cfh.$$

One can see that the result of this simplified method (for the assumed conditions) is about 97% of that for the more rigorous method (which, itself, contains uncertainties). Consequently, the following volumetric flows out of the drywell are considered typical values for the specified test allowables (expressed as SCFH):

SCFH per line	CFH per line
100	49.7
11.5	5.7

One should note again that these volumetric flows correspond to peak accident conditions in the drywell. Reference 3 permits a reduction in these leak rates by up to a factor of two after 24 hours if such a reduction can be justified by the thermodynamic state in the drywell after that time. The difficulty in taking advantage of this option is that the pressure must be made very low to reduce v x G to a value ≤ one-half 970 $ft^3/sec\text{-}ft^2$. In fact, this value (485 $ft^3/sec\text{-}ft^2$) is not reached until the drywell pressure is reduced to approximately 1.0 psig of steam (i.e., 15.7 psia at a saturation temperature of 215 F). At this low a pressure, the flow may be considered incompressible; and for a nozzle, $v \times G = \sqrt{2g_c v \Delta P}$ $ft^3/sec\text{-}ft^2$. For ΔP = 1.0 psi = 144 psf and v = 25.2 ft^3/lbm (saturated steam), v x G = 483 $ft^3/sec\text{-}ft^2$, almost exactly one-half v x G = 970 $ft^3/sec\text{-}ft^2$. If air is assumed to be present instead of steam, for ΔP = 1.85 psi = 266 psf and v = 13.6 ft^3/lbm (air at 150 F), v x G = 483 $ft^3/sec\text{-}ft^2$, almost exactly one-half v x G = 970 $ft^3/sec\text{-}ft^2$. The presence of hydrogen would increase the volumetric flow somewhat relative to that of pure air. Therefore, to justify a reduction to one-half the steam line leakage volumetric flow out of the drywell at 46.9 psig, the drywell pressure would have to be reduced to roughly 1.0 – 2.0 psig depending on the composition and temperature of the drywell.

Even if the one-half factor cannot be fully justified, it may be advantageous to develop a model in which the leak rate is coupled to the drywell thermodynamic state. If this is done, the thermal-hydraulic analysis supporting the coupling should be consistent with the type of accident that would produce the source term. Otherwise, illogical and possibly nonconservative results may be obtained.

2.3 Volumetric Flows in Steam Lines between Closed MSIVs

In the space between closed MSIVs, the pressure will be much closer to that of the drywell than to ambient. However, the temperature will be greater than that of the drywell. The high pressure in the space between the closed valves will reduce the flow out of the drywell (i.e., the pressure will be greater than critical pressure); however, for conservatism, this may be neglected. Therefore the volumetric flow out of the space between the two MSIVs may simply (and conservatively) be assumed to increase by the ratio of the sonic velocity; i.e., the square root of the temperature ratio. Assuming the temperature in the space between closed MSIVs to be 550 F (and 340 F in the drywell), the multiplier is

1.12. Using this multiplier, the volumetric flows between closed MSIVs for the typical conditions stated above become:

SCFH per line	CFH per line
100	55.6
11.5	6.4

2.4 *Volumetric Flows in Steam Lines beyond Closed MSIVs*

Beyond the closed MSIVs, the conditions in the steam lines may be assumed to be ambient pressure and 550 F. Therefore, compared to the flow out of the drywell (at 61.6 psia and 340 F for the typical conditions assumed), the volumetric flow will increase by a multiplier of (61.6/14.7)(1010/800) = 5.29. Using this multiplier, the volumetric flows beyond closed MSIVs for the typical conditions stated above become:

SCFH per line	CFH per line
100	263
11.5	30

2.5 *Summary of Problem Definition*

The drywell may be assumed to be the source of MSIV leakage. Assuming that the aerosol concentration builds up in an essentially linear fashion over the two-hour release period to a value of 5E5 grams/2E5 ft3 or 2.5 grams/ft3, and then is removed at a rate of one per hour beyond two hours, the integrated concentration would be 5 gram-hours/ft3. For a typical "high" volumetric flow out of the drywell of 49.7 cfh, the release would be approximately 250 grams per steam line. For a typical "low" volumetric flow out of the drywell of 5.7 cfh, the release would be approximately 28.5 grams.

A typical steam line inside diameter that may be used is 0.6 m or 23.6". The cross-sectional area of such a steam line is about 3.0 ft^2. For such a steam line, the plug-flow velocities would be:

SCFH per line	FPM per line
100	0.31
11.5	0.04

in the space between closed MSIVs and:

SCFH	FPM

per line	per line
100	1.46
11.5	0.17

beyond the closed MSIVs. Circulation (mixing) velocities could be greater. If calculated circulation velocities are less than the values cited for plug flow, then plug flow is a reasonable assumption. However, to the degree that circulation velocities exceed the plug-flow velocity, a well-mixed treatment becomes increasingly appropriate.

These mass releases, flows, and velocities provide a context in which to consider aerosol removal in the steam lines.

3.0 Further Background

Steam line aerosol removal may occur as the result of a number of mechanisms (phoretic deposition, diffusion, turbulence, inertial impaction in bends or restrictions, etc), but the dominant mechanism is gravitational settling or sedimentation. Sedimentation is the only mechanism that will be discussed in this paper.

Once the decision to credit steam line sedimentation is made, a number of other modeling decisions must follow. For example, what are the steam line control volumes and are those control volumes well-mixed or not? In the case of crediting sedimentation, it's obvious that only horizontal runs of piping should be credited, but what about multiple steam line control volumes in series; i.e., under what circumstances may concentration differences along the steam lines be credited?

In the case of Perry, NRC did not feel comfortable giving credit for plug flow (with a continuous concentration gradient along the steam line), arguing instead for a well-mixed steam line treatment (no concentration gradient along the steam line). Polestar and First Energy complied. The NRC's well-mixed treatment (as it was developed in the course of the Perry review) is described in Appendix A of Reference 9. NRC did concede (as stated in Reference 9) that the median sedimentation velocity from that treatment would be appropriate to use due to the inherent conservatism of the well-mixed model (as compared to that of plug flow) as applied to Perry.

By way of explaining the different treatments of steam line sedimentation, it is instructive to consider plug flow first, show the relationship to a well-mixed assumption later, and then finally, comment on the way in which sedimentation is treated by both the NRC and Polestar.

4.0 Sedimentation Removal in Plug Flow

Figure 2 illustrates sedimentation in plug flow. It is assumed that as each portion (or "slice") of the contaminated flow makes its way downstream, there is no longitudinal mixing. Therefore, a concentration gradient exists along the length of the pipe (although the concentration in the plane of the slice is assumed to be uniform). Between any two stations along the axis of the pipe, the settling velocity, v_{sed}, may be calculated to be a certain value for particles of a given size, shape, and density. If the particles of that given size, shape, and density are uniformly distributed throughout the slice at the first station (solid circle at right), then they may be assumed to settle uniformly by the second station (illustrated by the dashed circle at the right). The fraction of the second circle that remains within the first circle (equal to twice the cross-hatched area, A1, divided by πr^2) is the fraction remaining. The fraction of the second circle outside the boundary of the first circle, $A2/(\pi r^2)$, is the fraction removed.

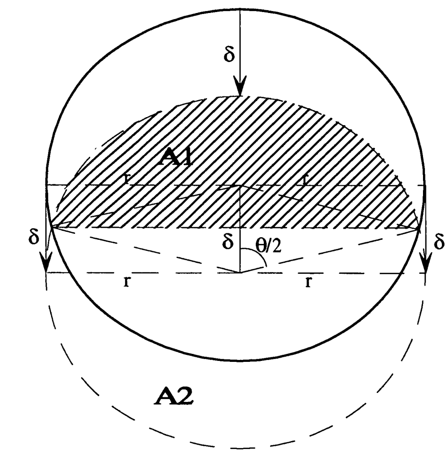

Figure 2. Sedimentation in Plug Flow.

Calculating A1 and A2 (and, therefore, the fractional removal between the two stations) is straightforward. If one assumes that the displacement of the second circle relative to the first, δ, is equal to $v_{sed}t$ (where "t" is the time required for the slice to translate from the first station to the second station at the plug-flow velocity, v_{plug}), then the cosine of angle $\theta/2$ is equal to $\delta/2r$. Knowing $\theta = 2\arccos(\delta/2r)$, one can solve for A1 from the expression $A1 = r^2(\theta - \sin\theta)/2$. Then one is able to solve for A2 based on the expression that $A2 = \pi r^2 - 2A1$. The fraction removed then becomes equal to $(1 - (\theta - \sin\theta)/\pi)$. For $\delta = 0$, $\theta = \pi$; and the fraction removed is zero. For small, non-zero values of δ, the removal fraction is approximately $2\delta/\pi r$ (i.e., the value for A2 approaches $2r\delta$). This is illustrated on Figure 3 by plotting A2 and $2r\delta$ vs. δ for a pipe of unit diameter. One may note also from the chart that when $\delta = 1$ (i.e., when the second circle moves outside the first circle and is just touching the first circle at a single point with $\theta = 0$), the value for A2 is $\pi/4$ (i.e., the area of a unit circle) while the product of the unit diameter and the unit displacement is one. This (as well as the chart) illustrates that the product of the diameter (2r) and $v_{sed}t$ is always greater than the actual removal; but for small displacements, the actual removal approaches $2rv_{sed}t$, and the actual fraction removed approaches $2v_{sed}t/\pi r$.

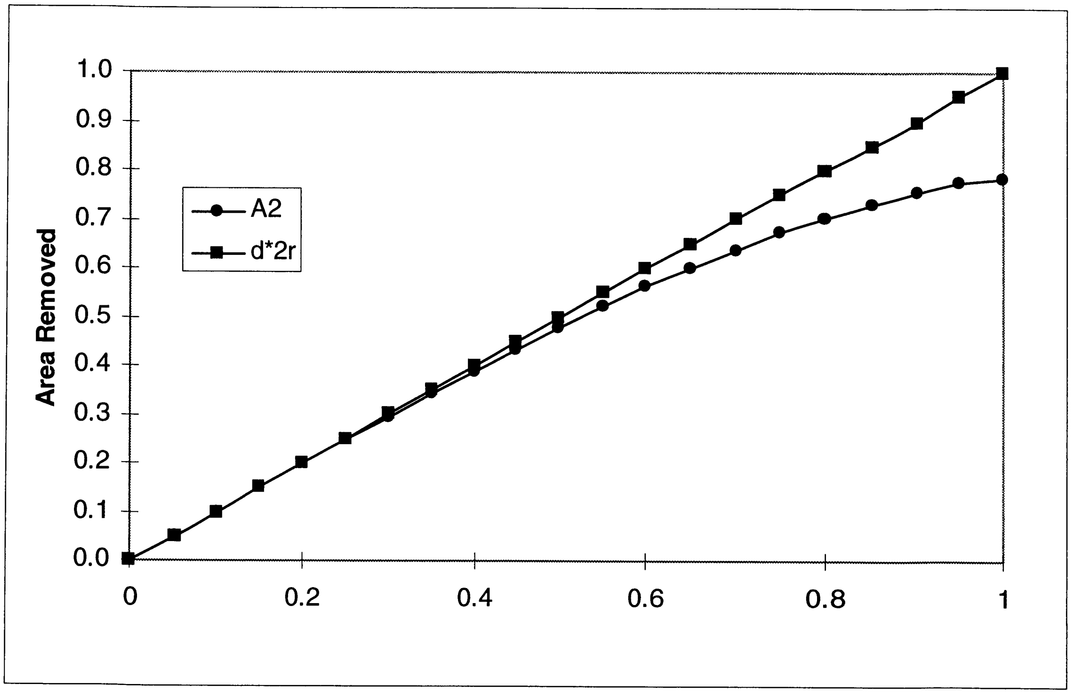

Figure 3. Comparison of A2 and δ*2r vs. δ for Unit Circle.

Having shown that using the product of the diameter of the pipe, the sedimentation velocity, and the time it takes for the "slice" to move a small distance down the pipe divided by the cross-sectional area of the pipe is an acceptable approximation of the removal fraction in the slice as it translates from the first station to the second station, the discussion of plug-flow sedimentation continues to the next removal step. It is assumed (in the next removal step) that the particles of the given size, shape, and density are once again redistributed across the cross-section of the pipe, but this time at a lower concentration. This process is assumed to continue along the entire length of the pipe.

Using this kind of model, dC/dt (the rate of concentration change along the length of the pipe) = $C(2v_{sed}/\pi r)$; and the integrated removal along the length of the pipe may be characterized by the expression $C_{2i} = C_{1i}\exp(-\lambda_i t)$ where C_{2i} is the concentration of the i^{th} aerosol specie (size, shape, and density) at the second station compared to the concentration C_{1i} at the first station, and λ_i is the product of the sedimentation velocity for the i^{th} specie times $2/\pi r$. This is the model that First Energy and Polestar proposed for certain portions of the main steam lines for Perry.

For Perry, it was assumed that the density and shape factor for each specie was the same and constant along the length of the pipe, but the size distribution was assumed to vary both temporally and spatially. The size distribution changes along the length of the pipe and becomes "smaller" (i.e., is characterized by smaller particle sizes) in downstream sections and control volumes. Thus, particulate removal becomes more difficult as the "easy" particles are removed upstream. Agglomeration (which could mitigate the decreasing particle size distribution somewhat) was not considered in this model for two reasons: (1) because it is computationally difficult and (2) because in many cases for which plug flow could be justified (i.e., wherein internal circulation velocities are small relative to the plug-flow velocity), residence times are also typically short which limits agglomeration. In any case, not including agglomeration is conservative.

The Brockman model for sedimentation described in Reference 10 is essentially the same as the Polestar plug-flow model. By defining removal efficiency as $(1 - C_2/C_1)$ in the Polestar model, one obtains the form $(1 - e^{-\lambda t})$ for the expression of removal efficiency. This is the same as the Brockman model. In the Brockman model, the sedimentation velocity is multiplied by the pipe internal surface area $2\pi rL$ (where L is the pipe segment length) and is divided by π and the volumetric flow in the line, Q, to obtain the removal rate. Since $Q = v_{plug}(\pi r^2)$ and since t is equal to L/v_{plug}, $Q = L(\pi r^2)/t$; and the sedimentation velocity ends up being multiplied by $2/\pi r$ to obtain the "lambda". This is the same sedimentation velocity multiplier as the Polestar plug-flow model uses to obtain the lambda values, except that since particle density, shape and size distribution are rigorously calculated in the Polestar model (for specific plant conditions and along the length of the pipe), the sedimentation velocity is not a single value (rather, it is a distribution, both temporally and spatially), and the corresponding distributed lambdas are applied to the mass distribution (corresponding to the particle size distribution) and not just as a single lambda value for the entire mass. This allows the particle size distribution to change along the length of the pipe.

In the Reference 10 Brockman model, there is only a single lambda calculated for a fixed particle size and density (i.e., the lambda value is constant along the entire length of the pipe). This is potentially nonconservative, although based on the Reference 10-suggested values of one micron diameter and one gram/cc, the fixed values appear to be quite conservative, at least for most situations. A comparison of removal efficiencies calculated with the Brockman model described in Reference 10 to that of Appendix A of Reference 9 (AEB-98-03) is provided in Figure 4. This is for the range of MSIV leak rates discussed above and for a steam line segment 0.6 m in diameter and four meters in length. Reference 9, Appendix A curves are provided for the median sedimentation velocity (1.17E-3 m/s), the 10^{th} percentile sedimentation velocity (2.1E-4 m/s), and the sedimentation velocity predicted for the Brockman model (2.8E-5 m/s). It may be noted that based on a Polestar curve-fit, the Brockman sedimentation velocity is about the 1.6^{th} percentile of the Reference 9, Appendix A, sedimentation velocity distribution.

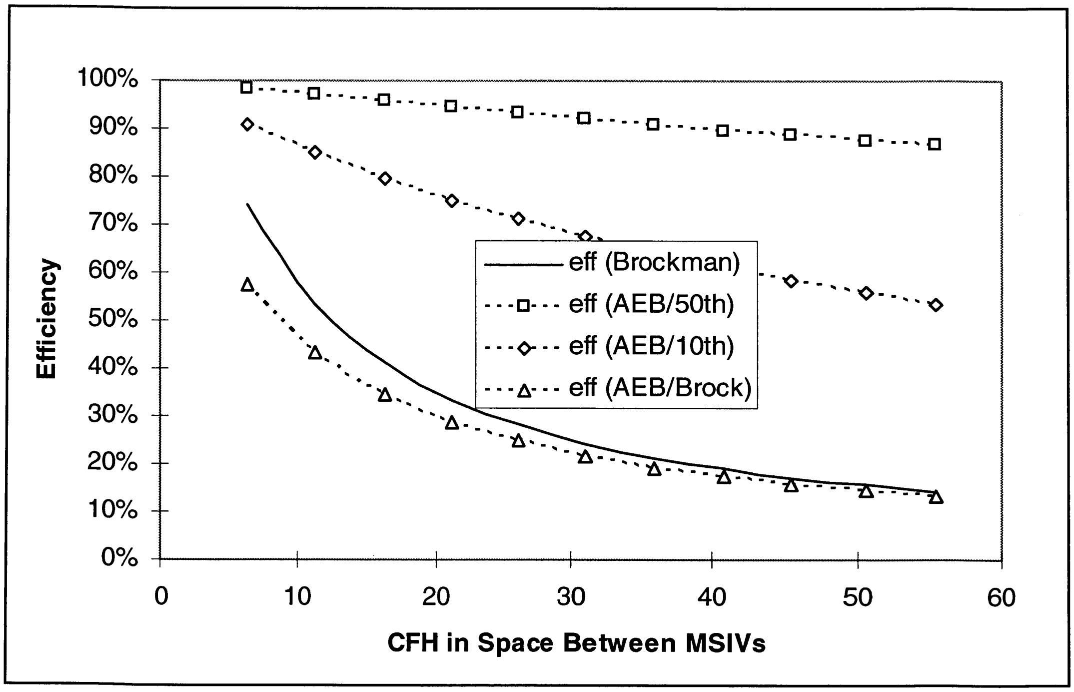

Figure 4. Brockman vs. AEB-98-03 Aerosol Removal Efficiency.

For very low flow rates (i.e., in cases where well-mixed conditions become more likely), it may be inappropriate to use the median sedimentation velocity for the Reference 9 model; and this would reduce the low-flow Reference 9 efficiencies to values closer to those of the Brockman model (see, for example, the 10th percentile Reference 9 curve). However, because of the selection of particle size and density suggested by Reference 10 as part of the Brockman model, the inherently less conservative plug-flow assumption is overwhelmed by the conservatism of the inputs; and the Reference 9 model is nearly always the less conservative of the two modeling approaches, even for the 10th percentile sedimentation velocity case.

It is also interesting to note that for very high flow rates, the inherent conservatism of the well-mixed model is reduced relative to that of the plug-flow model; i.e., for very high flow rates, the same model inputs yield similar results for both approaches (see, for example, the Brockman curve and the Reference 9 curve using the Brockman sedimentation velocity). Therefore, the very large difference in removal efficiency seen for high flow rates in Figure 4 is the direct result of the factor of 40 greater sedimentation velocity of Appendix A of Reference 9 (median value) as compared to that of the Reference 10 Brockman model. It is the inherently more optimistic character of the plug-flow model (which, nevertheless, may be appropriate in some circumstances) that brings the Brockman and the Reference 9 50th and 10th percentile curves closer together for low flow rates. The interesting reality is that while the greatest advantage of the plug-flow model is realized at low flow rates, it is precisely at low flow rates that the application of the plug-flow model is the most difficult to defend.

Having explained the plug-flow expression for sedimentation as described in Reference 10 (the so-called Brockman model), it is important to note that it is not actually the plug-flow model applied in the RADTRAD 3.02a code. This is because the RADTRAD 3.02a code contains two models for sedimentation; and although one is the model described in Reference 10, the model selection is based on an internally-calculated Reynolds Number. This Reynolds Number is always low enough (for any reasonable MSIV leak rate) to force selection of the other model. According to the model developers, the first model (the one described in Reference 10) is based on turbulent flow, while the other model is based on laminar flow. The model developers refer to both of these as "the Brockman model" even though only one is presented in Reference 10.

The other model (referred to as Model X for the purposes of discussion) is also a plug-flow model and appears to be based on a concept similar to that of Figure 2. The difference between the simple plug-flow model and Model X appears to be basically that mixing within the plane of the pipe cross-section does not occur between stations in the Model X formulation (perhaps because of the assumption of laminar flow). This means (for illustration) that when the dashed circle on Figure 1 falls outside the solid circle (i.e., when $v_{sed}L/2r\ v_{plug} \geq 1$), the removal of all particulate is complete (i.e., efficiency = 100%). By comparison, for the first model, when $v_{sed}L/2r\ v_{plug} = 1$ (and $L/v_{plug} = 2r/v_{sed}$), $\lambda t = 4/\pi$ and efficiency = only 72%.

The Model X formulation is not exactly like Figure 2. When the coding of RADTRAD 3.02a is examined (which has to be done because the model is not described elsewhere), and the efficiency is plotted as a function of unit circle displacement, δ (i.e., as a function of $v_{sed}L/2rv_{plug}$), one finds that the efficiency equals 100% when $\delta = 1.33$ rather than when $\delta = 1.0$. This is because the coding defines a variable called SETTLE_PAR that is equal to 0.75L/2r times another variable called VEL_REL. VEL_REL, in turn, is defined as v_{sed}/v_{plug}. The removal is not complete (i.e., the efficiency does not reach 100%) until SETTLE_PAR = 1.0 which is when $(0.75L/2r)(v_{sed}/v_{plug}) = 1.0$, and that is when $\delta = 1.33$ for a unit circle.

The efficiency from Model X is plotted as a function of δ on Figure 5 ("eff"), and it is compared with A2 from Figure 3 divided by the area of a unit circle ("A2/Aref"). The two plots agree well for small values of δ, but for $\delta = 1$, A2/Aref = 100% while eff = 88%. If, on the other hand, SETTLE_PAR is redefined to be equal to the unit circle δ, then the efficiency is 100% at $\delta = 1.0$, but is nonconservatively high for all other values of δ ("eff'"). Therefore, it appears that the expression in RADTRAD 3.02a for Model X may have had a derivation similar to that implied by Figure 2 (without mixing across the pipe cross-section), but with a slightly different set of assumptions and/or approximations. In any case, the key feature of Model X is that it is one in which the efficiency can analytically reach 100% (complete aerosol removal).

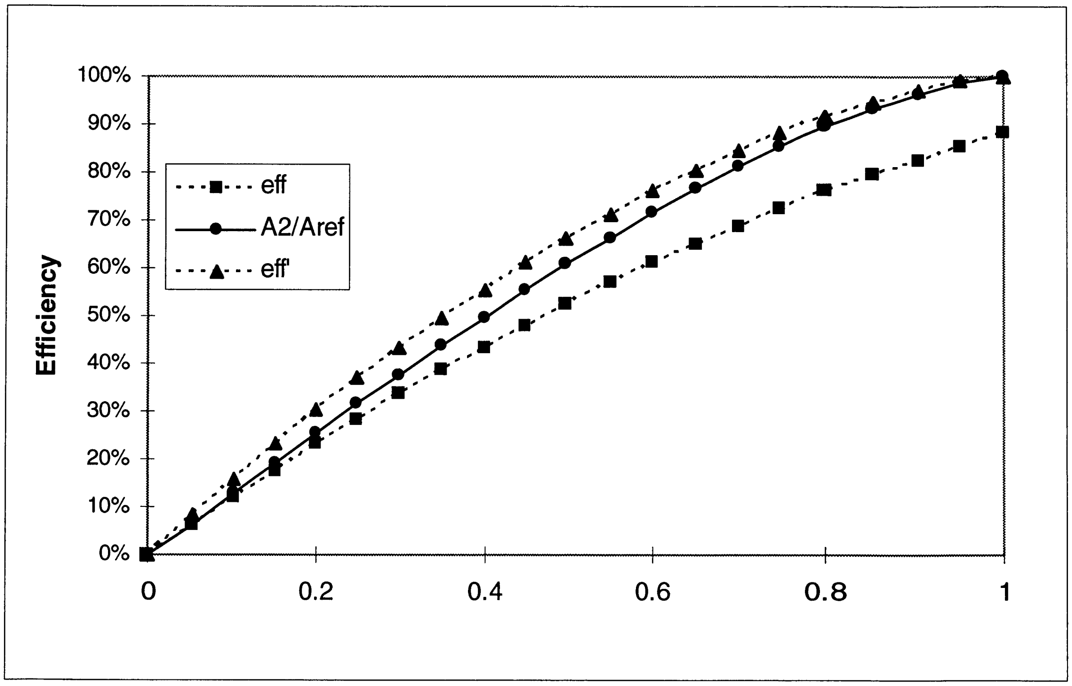

Figure 5. Removal Efficiency vs. δ for Unit Circle.

The question then arises as to how realistic a completely stable, laminar plug flow in the steam lines is. In Reference 11, free convection within closed spaces is treated (in connection with insulation design) for vertical "boxed" plates set a distance "b" apart and closed top and bottom. Convection currents are set up (and the insulation characteristics begin to degrade) when the Grashof Number reaches about 6E4 (based on the distance "b") and become fully turbulent when the Grashof Number reaches about 2E5. For steam at about 550 F (the approximate temperature inside the steam lines) the Grashof Number is about 1E5 times the characteristic dimension cubed (in feet) and the temperature difference between the gas and the surface (in F). This means that fully turbulent cells could form within the steam line when the temperature difference between the surface and the gas could be as little as 0.25 F (assuming that the steam line diameter of about two feet is the appropriate characteristic dimension for calculating the Grashof Number in this situation). While the manner in which this characteristic dimension is established is certainly open to question in this example, the indication remains strong that suppression of mixing in the cross-section of the steam line (even if substantial longitudinal concentration gradients exist) is very difficult to achieve and, moreover, that the potential for turbulent mixing in the plane of the pipe cross-section may have very little to do with the Reynolds Number calculated in the way that it is in the RADTRAD model (i.e., based only on the plug-flow velocity). Therefore, Polestar believes that industry efforts should be directed towards defining the conditions in which a "turbulent" plug-flow model (i.e., the Brockman model as described in Reference 10) may be applied rather than trying to defend a "laminar" plug-flow model. This is the approach alluded to by the NRC in Reference 3.

One interesting aspect of plug-flow removal (as it's used in RADTRAD 3.02a and also by Polestar) is that it's applied as an efficiency. Therefore, residence time in the line is assumed to be zero even though the flow may require some time to reach the end of the pipe. Polestar will occasionally apply the calculated efficiency at the inlet of the pipe with the pipe represented as a well-mixed control volume in the dose calculation. This is an attempt to get some hold-up credit, although the "well-mixed" hold-up credit calculated in this way would not be as great as the plug flow would actually exhibit.

5.0 Sedimentation Removal in Well-Mixed Flow

Having covered plug flow, well-mixed flow can now be discussed.

In this type of modeling, there is no concentration gradient along the axis of the pipe. The concentration in the pipe, C_2, is uniform throughout (and not just in the cross-section of the pipe), so that the overall lambda (the composite of the individual specie removal rates) is also uniform everywhere in the pipe (i.e., it varies temporally but not spatially). For this set of assumptions, $dC_2/dt = Q(C_1 - C_2)/(L\pi r^2) - \lambda C_2$; and for steady-state, $C_1 = C_2(Q + \lambda(L\pi r^2))/Q$. The value for λ is calculated in the same way as for plug flow (meaning <u>turbulent</u> plug flow, not Model X) except that it is conceptually on a volume basis. Numerically, it is still equal to the sedimentation velocity times $2/\pi r$, the pipe length canceling out in both the sedimentation area in the numerator and the pipe volume in the denominator. The residence time, t, is defined as $(L\pi r^2)/Q$ which is numerically the same as the L/v_{plug} used with plug flow.

For the steady-state, well-mixed expression, $C_2/C_1 = 1/(1 + \lambda t)$ and the removal efficiency is $1 - C_2/C_1 = \lambda t/(1 + \lambda t)$. The conservatism of the well-mixed assumption is evident from a comparison of removal efficiencies for plug flow vs. well-mixed for $\lambda t = 3$ (a typical value for 100 SCFH in the space between closed MSIVs). For plug flow, the removal efficiency is $(1 - e^{-\lambda t}) = 95\%$. For the well-mixed assumption, the removal efficiency is $\lambda t/(1 + \lambda t) = 75\%$. Another example might be the one discussed above (in connection with Model X) in which $\lambda t = 4/\pi$. For $\lambda t = 4/\pi$, the Reference 10 (i.e., turbulent) plug-flow model gives an efficiency of 72%, Model X (the laminar plug-flow model) gives an efficiency of 100%, and the well-mixed model gives an efficiency of only 56%. Thus, the question of plug flow vs. well-mixed is by no means trivial.

It is interesting to note that if a steam line were to be divided into an infinite number of well-mixed control volumes, the overall efficiency would become identical to that of a turbulent plug-flow model representing the entire steam line.

6.0 Sedimentation Velocity - NRC Treatment

Up to this point, this paper has discussed only sedimentation models. All of these models are keyed to a single parameter, the sedimentation velocity (or settling velocity), v_{sed}. The sedimentation velocity is, itself, a function of particle size, effective density (including consideration of voids and shape), and carrier gas viscosity (a function of gas temperature and composition). For very small particles, it is also a function of the Cunningham Slip Factor, C_s, but since the very small particles have little mass, the effect of C_s may be neglected for the purpose of this discussion (i.e., it may be assumed to be unity).

Reference 9, Appendix A provides a distribution of steam line sedimentation velocities used by NRC in the review of Perry. This distribution was based on "containment" particle size distributions, and while this may be acceptable for the first steam line control volume connected to an unsprayed drywell, the question of how to handle a sprayed drywell as a source (or a downstream steam line control volume coming after the first steam line control volume) must be addressed in order to apply the results to any steam line control volume other than the first one. This is because studies made by Polestar have confirmed the importance of the downward shift in the downstream particle size distribution arising from removal of large particles in an upstream control volume (or by sprays in the drywell).

For example, in one study performed by Polestar, 26.5 grams of particulate were released from an unsprayed drywell into a well-mixed steam line control volume in which the sedimentation lambda calculated by Polestar's STARNAUA aerosol mechanics code (Reference 12) was 5.2 per hour. The residence time was 6.5 hours (typical for the space between Mark I containment MSIVs for an allowable leak rate of 11.5 SCFH and a plug-flow velocity of 0.04 fpm). The calculated leakage was 0.78 grams (i.e., the observed removal efficiency was 97%).

For a lambda of 5.2 per hour and a residence time of 6.5 hours, the expected removal efficiency would have been $(5.2)(6.5)/(1 + (5.2)(6.5)) = 97\%$, so there is very good agreement. Since the lambda is the product simply of the sedimentation velocity times $2/\pi r$, the "effective" (i.e., single-value) STARNAUA sedimentation velocity would have been about 6.8E-4 m/s to have produced a lambda of 5.2 per hour (assuming a steam line diameter of about 0.6 m). Therefore, the STARNAUA "effective" value is only about 60% of the Reference 9 median value of 1.17E-3 m/s and represents about the 30^{th} percentile of the Reference 9 distribution. This means that the STARNAUA model is more conservative than the Reference 9 model, at least as applied to this first control volume. Had the Reference 9 median sedimentation velocity been used, the removal efficiency would have been 98% rather than 97%.

In a second, downstream control volume receiving the 0.78 grams from the first control volume, the STARNAUA-calculated lambda was 2.0 per hour and the residence time was 3.7 hours (i.e., representing about 38 feet of steam line at atmospheric pressure and 0.17 fpm). The effective sedimentation velocity in the second control volume must then have decreased by 62% (i.e., to about 2.6E-4 m/s) to have yielded a lambda of 2.0 per hour. The leakage from the second control volume was calculated to be 0.095 grams for an observed efficiency of 88%. The expected efficiency would have been (2.0)(3.7)/(1 + (2.0)(3.7)) = 88%, again excellent agreement.

The mass into the first control volume (26 grams), the mass out of the first control volume and into the second control volume (0.78 grams), and the mass out of the second control volume (0.95 grams), all as calculated by STARNAUA, are shown on Figure 6 as the first three entries.

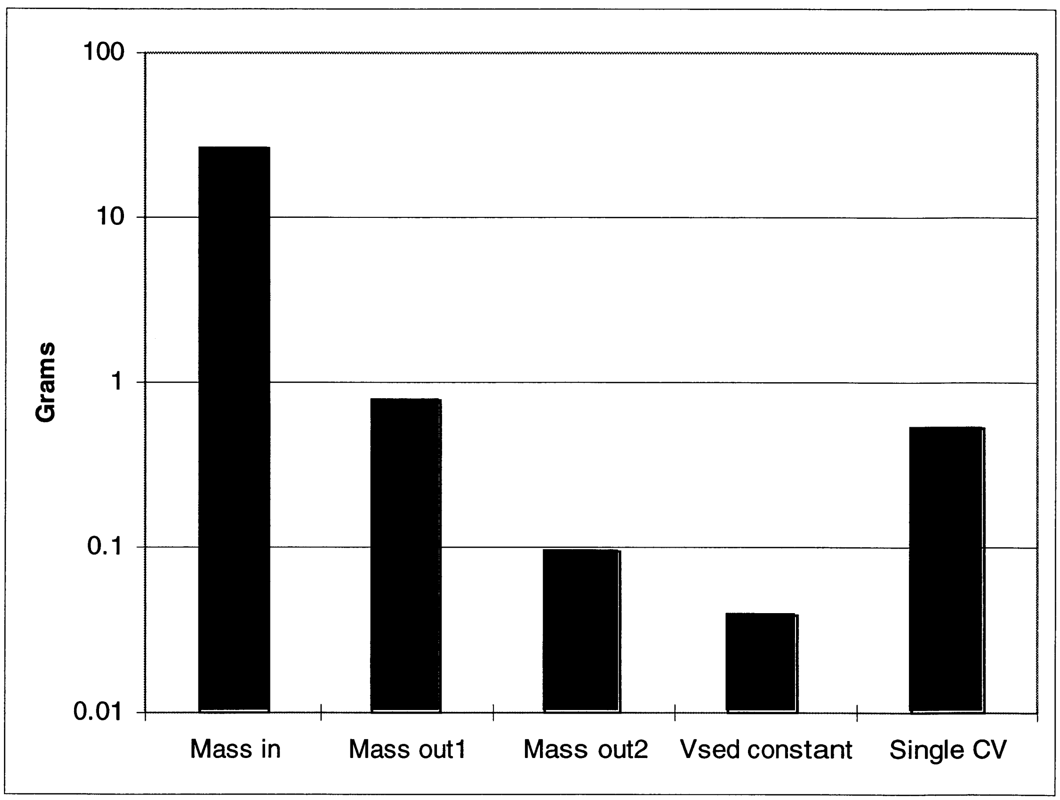

Figure 6. Mass In and Out of Steam Line.

If one had made the erroneous assumption that the downstream steam line control volume had the same sedimentation velocity as the first steam line control volume, the expected efficiency would have been (5.2)(3.7)/(1 + (5.2)(3.7)) = 95%, and the leakage would have been calculated to be 0.039 grams instead of 0.095 grams, 41% of the actual release. Therefore, the release would have been understated by a factor of about 2.4. This is shown as the fourth entry on Figure 6 ("Vsed constant").

One possible solution would be to simply combine the two serial control volumes into a larger, single control volume. The expected efficiency for this arrangement would be (5.2)(10.2)/(1 + (5.2)(10.2)) = 98% (where 10.2 hours is the combined residence time), so the leakage would be expected to be (0.02)(26.5 grams) = 0.53 grams. This is value shown as the fifth entry on Figure 6 ("Single CV"), and it has also been confirmed by an actual single control volume calculation using STARNAUA. However, this 0.53 grams is 5.6 times the "actual" leakage of 0.095 grams (i.e., the leakage calculated for the two control volumes in series). Even if the median sedimentation velocity from Reference 9 had been used in the single control volume calculation, the efficiency would have been just over 99%, and the release about 0.29 grams, still a factor of three greater than the "actual" 0.095 grams. Therefore, while combining the two serial steam line control volumes into a single control volume may be simple and conservative, there is a question as to whether or not it is too conservative.

So to summarize for this example, if one had used the same sedimentation velocity in the second control volume as in the first, the mass released would have been understated by a factor of 2.4; and if to avoid this problem, one had combined the two control volumes into a single control volume, the mass released would have been overstated by a factor or 5.6. Clearly, the latter approach is acceptable, although perhaps excessively conservative.

In Polestar's view, the Reference 9 model using the median sedimentation velocity may always be applied to a single control volume as long as the flow rates are high enough to justify the median value. Low flow rates (i.e., low MSIV leak rates), control volumes in series, or a sprayed drywell as the source would not support use of the Reference 9, Appendix A, median sedimentation velocity. Under these conditions, a lower sedimentation velocity from the Reference 9 distribution would need to be justified and applied.

7.0 Sedimentation Velocity - Polestar Treatment

It is possible to modify the Reference 9 sedimentation velocity by a number of techniques guided by the insights of a rigorous methodology such as STARNAUA. Polestar has used one proprietary approach in an NRC submittal being prepared at the time of this writing; and another, related proprietary approach (applied to the study described above) yielded the result that the sedimentation velocity for the downstream control volume should be reduced by a factor of five (to 2.3E-4 m/s) because 97% of the mass had been removed in the first control volume. This being the case, the lambda in the downstream control volume would become about 1.8 per hour, and the removal efficiency would become about (1.8)(3.7)/(1 + (1.8)(3.7)) = 87%. For such a case, the released mass would be (0.13)(0.78 grams) = 0.10 grams, about the same as the STARNAUA-based result.

For an illustrative comparison (to illustrate the sensitivity of the downward shift in sedimentation velocity to the upstream removal efficiency), a removal efficiency of 90% in the first control volume would have reduced the downstream control volume median sedimentation velocity to about 3.5E-4 m/s, a factor of 3.4 reduction. These modified Reference 9 approaches can also be used for consideration of a sprayed drywell as the source for MSIV leakage.

8.0 Other Steam Line Modeling Considerations

The discussion to this point has concentrated on four key modeling aspects governing steam line aerosol removal: (1) the source of the MSIV leakage, (2) the volumetric flows associated with MSIV leakage, (3) the degree of mixing to be assumed in the steam line control volume(s), and (4) the selection of a sedimentation velocity. There are two other points to cover: the definition of control volumes and the consideration of single failures.

With respect to the definition of control volumes, one must first decide if the steam lines between the reactor vessel and the inboard MSIV are to be credited. Much of this piping is vertical and would contribute little in the way of sedimentation area. However, the horizontal portion could potentially be credited.

When evaluating what steam line piping can be credited, consider that the LOCA leading to this source term may originate with a steam line break (References 1 and 4 are both nonspecific as to the accident leading to the core damage); and such a break could occur at the MSIV, taking the vessel-to-MSIV piping for the damaged line out of the release pathway. The remaining vessel-to-MSIV piping should be credited only if aerosol removal in the drywell is limited to natural removal. Polestar's view is that none of the steam line piping from the reactor vessel to the inboard MSIVs should be credited if drywell sprays are credited.

The next issue is steam line control volumes in series. Arbitrarily dividing the steam lines into well-mixed control volumes in series represents a means of taking a penalty for some degree of longitudinal mixing along the axis of the steam lines but not to the extent of treating the entire steam line (whatever portion is being modeled) as well-mixed. Arbitrarily increasing the plug-flow velocity by a multiplier and then applying the plug-flow model has the same effect. Polestar has usually taken the position that only real, physical barriers should be considered as control volume boundaries; e.g., a closed, outboard MSIV. Since one would clearly expect a difference in activity concentration on either side of a closed MSIV, this legitimate difference in activity concentration is reflected by modeling two steam line control volumes in series with the closed (but leaking) MSIV as the boundary between them.

There is also the question of how one should (or should not) establish and credit control volumes downstream of the outboard MSIVs (if such piping is qualified) if no "third isolation valves" are used (as they are on Mark III containments) and if the status of the turbine stop valves and turbine bypass valves cannot be established (i.e., if they are not Safety-Related or exempted as such). If there were the opportunity for cold air to enter the steam lines at the turbine end, the natural circulation of that cold air in the steam line could substantially reduce the residence time in the affected portion of the steam line.

Finally, with respect to single failures, Polestar has found that assuming an MSIV to remain open in one of the steam lines is usually the limiting single failure. This eliminates the opportunity to establish a control volume between the MSIVs in that line and usually creates the circumstance where the greatest dose contribution is coming from that line. Therefore, the maximum Technical Specification MSIV leak rate for a single line (for example, 100 SCFH or 150 SCFH out of a total Technical Specification limit of 250 SCFH) should be assumed to exist in the line with the stuck-open MSIV. The remaining leakage should be assumed to be distributed in a way that minimizes residence time in the remaining lines. For example, if a "per line" maximum of 150 SCFH (out of a total of 250 SCFH) is assumed to exist in the line with the MSIV assumed to be stuck open, then the remaining 100 SCFH should be assumed to exist in one of the remaining lines. If the "per line" maximum is 100 SCFH, then 100 SCFH should be assumed to exist in the line with the stuck-open MSIV, 100 SCFH in one of the remaining lines, and 50 SCFH in another of the remaining lines. The process simply consists of applying the maximum value to as many lines as possible (beginning with the line with the assumed single-failure), and then applying whatever is left to a remaining line. It follows that some lines will be assumed to be leak-tight, but this process is still conservative.

9.0 Summary

This paper covers the most important aspects of aerosol removal in steam lines. It discusses how post-accident steam line volumetric flow rates can be established based on tested MSIV leakage, it addresses sedimentation theory and the degree of internal mixing to be assumed in calculating the rate of aerosol sedimentation, it presents points of view on aerosol sedimentation velocities, and it gives some insight into the definition of steam line control volumes and single failure assumptions. Specifically, it provides simple modeling concepts and ways of applying the NRC's method of sedimentation discussed in Appendix A of Reference 9, as well as explaining and quantifying the limitations of that simplified approach.

10.0 References

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