UNITED STATES
NUCLEAR REGULATORY COMMISSION
WASHINGTON, D. C. 20555

NOV 1 3 1980

Mr. Robert Buchholz, Manager
Safety and Licensing Operation
General Electric Company
M/C 682
179 Curtner Avenue
San Jose, CA 95125

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Dear Mr. Buchholz:

Enclosed is the first round of questions on the Mark III LOCA-related pool dynamic load criteria. Our review was based on the information contained in Appendix 3B, Rev. 0, of GESSAR-II and the reports referenced therein.

We believe a meeting between your staff and the NRC staff would be useful in resolving any questions you may have on the enclosed material. If you agree, please contact Mel Fields at 492-9417 to arrange for such a meeting.

Sincerely,

(for) Walter R. Butler
Lester S. Rubenstein, Assistant Director
for Core and Containment Systems
Division of Systems Integration

Enclosure:
As stated

cc: (w/o enclosure)
R. Tedesco
W. Butler
J. Ranlet, BNL
F. Eltawila
T. Greene
M. Fields

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REQUEST FOR ADDITIONAL INFORMATION
MARK III POOL DYNAMIC LOADS ISSUES

1. Our basic concern with all the areas of LOCA-related pool dynamic loads was the difficulty in following the rationale behind each load definition. We feel that a "roadmap" should be provided for each load definition. The test runs that were chosen to develop each design load should be identified, the reasons why these tests were chosen and others eliminated from consideration should be provided, and how these tests and supporting analytical methodologies bound the expected Mark III conditions. Bounding the expected Mark III conditions means not only examining the range of relevant test parameters and comparing them to the range of parameters expected in Mark III design basis accidents, but also the evaluation of the statistical significance of the test results needs to be provided and shown to be conservative.

2. The design peak pool velocity is based on the direct use of 1/3 area scaled test data. In light of tests* conducted at MIT, provide justification that this approach is conservative. The MIT tests showed that both a 1/3 and a 1/9 area scale distorted geometry system could underestimate the full scale pool velocity (and overestimate breakthrough elevation). The MIT tests were dissimilar from the Mark III case in that (a) the systems had a single vent rather than three, and (b) the drywell pressure was constant during the entire blowdown. Except for these differences, MIT's Test Cases 1 and 2 are similar to Mark III (i.e., the geometry, the submergence and the vent resistance). Therefore, although the MIT test results cannot be used to quantitatively predict the pool velocity difference that would exist between the

*N. G. Ruggieri and A. A. Sonin, "Investigation of Distorted-Geometry Simulation of Pool Dynamics in Horizontal-Vent BWR Containments", NUREG/CR-1444.

ENCLOSURE

1/3 area scale tests and actual DBA conditions, it does provide groundwork for a qualitative assessment.

To make a qualitative assessment easier to follow, assume that:

a) only the top vent plays an important role; and

b) the step-function drywell pressure of the MIT tests can be identified approximately with the peak drywell pressure in a typical Mark III situation.

If these assumptions apply, then Cases 1 and 2 of the MIT tests can be compared directly with the Mark III-related tests at the corresponding peak drywell/wet-well pressure ratios. Thus, we then could compare MIT's system C to Mark III's 1/9 area scale, MIT's system B to Mark III's 1/3 area scale, and MIT's system A to the full scale Mark III. The MIT systems are 9/150 scale geometric models, Moody-scale, of the respective full-scale systems.

For a scaled peak drywell pressure of 14.7 psig (MIT's Case 2), we find from Fig. 26 of NUREG/CR-1444 that the MIT results would imply a scaled-up peak pool speed of about 42 ft/s for both the 1/9 and 1/3 distorted geometry models (Systems B & C), and about 67 ft/s for the full-scale system (System A). GE's 1/3-scale Mark III system with air blowdown gives a speed of about 42 ft/s at $(P_D)_{max}$ = 14.7 psig (see the figure, attached), but GE's 1/9-scale Mark III gives a speed of only about 25 ft/s, admittedly with steam blowdown. Thus, a reasonable similarity exists between the MIT test results and the GE test results, although not good enough for any licensing decisions. Apparently, either the difference in the number of vents, the drywell pressure history, or the air vs. steam

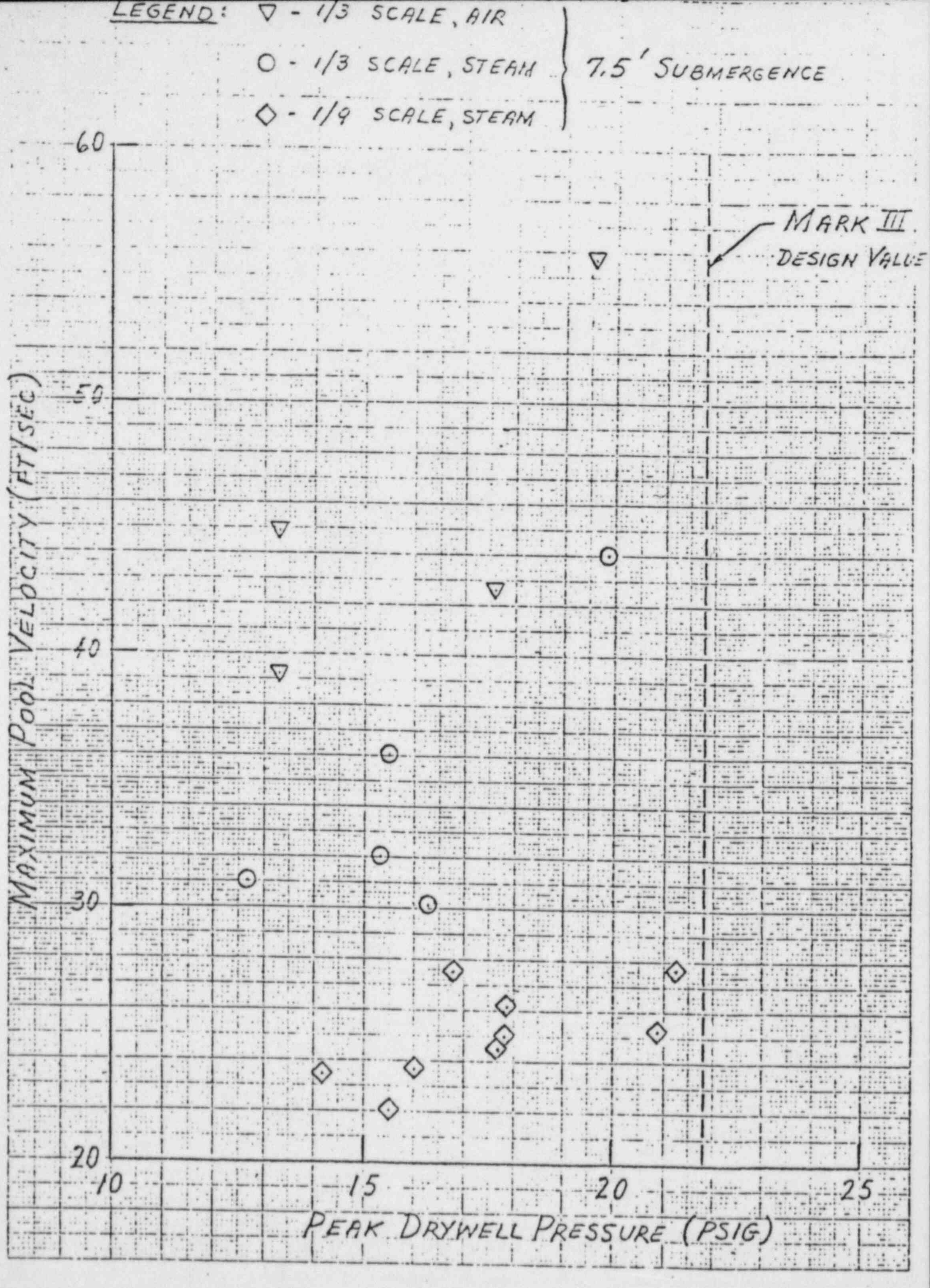
LEGEND: ▽ - 1/3 SCALE, AIR
○ - 1/3 SCALE, STEAM
◇ - 1/9 SCALE, STEAM
7.5' SUBMERGENCE
MARK III DESIGN VALUE
MAXIMUM POOL VELOCITY (FT/SEC)
PEAK DRYWELL PRESSURE (PSIG)
60
50
40
30
20
10
15
20
25

Fig 1

blowdown is playing a role. We cannot say whether the 67 ft/s "full-scale" value implied by the MIT test is realistic.

From MIT's Case 1, which corresponds to a "peak" drywell pressure of 29.4 psi, we get a scaled-up maximum pool speed of approximately 50-55 ft/s for all three systems. This happens to compare fairly well with the extrapolated 1/3-area scaled Mark III test results (see figure 1).

The only point we want to make with these comparisons is that it is difficult to justify the direct use of GE's 1/3 area scale data for peak pool swell velocity. The MIT investigation shows that the full scale pool velocity may be considerably higher, although the magnitude of the difference depends on conditions that cannot be easily predicted (a single "coefficient" is not sufficient).

3. The basis for GE's design basis pool swell velocity of 40 ft/s needs further justification. Viewgraph PPS-9* (pool swell loads) of the July 1980 meeting lists five 1/3-scale data points and one full-scale point, all taken with steam blowdowns, and all giving velocities of 40 ft/s or below. However, no indication is given of how the tests that were shown simulated full-scale conditions. In fact, none of the tests shown on PPS-9 attained peak drywell pressures comparable to the Mark III design value, the full-scale data point being particularly deficient in this regard. (The point from Test 5801-1 does not appear to belong on the figure, since it is a 5 ft. submergence run, not 7.5 ft).

The only 1/3-scale test which came close to Mark II design peak drywell pressure is one which is omitted from the viewgraph: Test 5801-12. This test gave

*NOTE: Throughout the remainder of this document references are made to the viewgraphs presented at the July 1980 meeting by simply stating the viewgraph number, for example: PPS-9.

a maximum pool speed of 44 ft/s which is higher than GE's specification of 40 ft/s.

Figure 2 shows a summary of the 1/3 and 1/9 scale data points for maximum pool speed at 7.5 ft submergence, plotted against the peak drywell pressure. The Mark III design peak drywell pressure is 21.8 psig. We note that:

(a) Air blowdowns in the 1/3-scale system gave consistently higher pool speeds (by about 10 ft/s) than steam blowdowns.

(b) 1/3-scale steam blowdowns gave consistently higher (by 10-15 ft/s) pool speeds than 1/9-scale steam blowdowns.

(c) Based on the 1/3-scale steam blowdown data, the most appropriate maximum pool speed corresponding to Mark III peak drywell pressure is about 45 ft/s.

(d) If we were to try to extrapolate linearly from 1/9-scale and 1/3-scale to 1/1-scale to obtain a Mark III maximum pool swell at 7.5 ft submergence and with steam blowdown, we would get a figure in the neighborhood of 60 ft/s.

This is not necessarily a conservative figure, since (i) the data are based on distorted-geometry steam blowdowns, not air blowdowns, and (ii) the MIT experiments on distorted-geometry testing show that such extrapolations can be quite nonlinear.

Please provide your comments on the above discussion and provide additional justification for the design basis pool velocity of 40 fps.

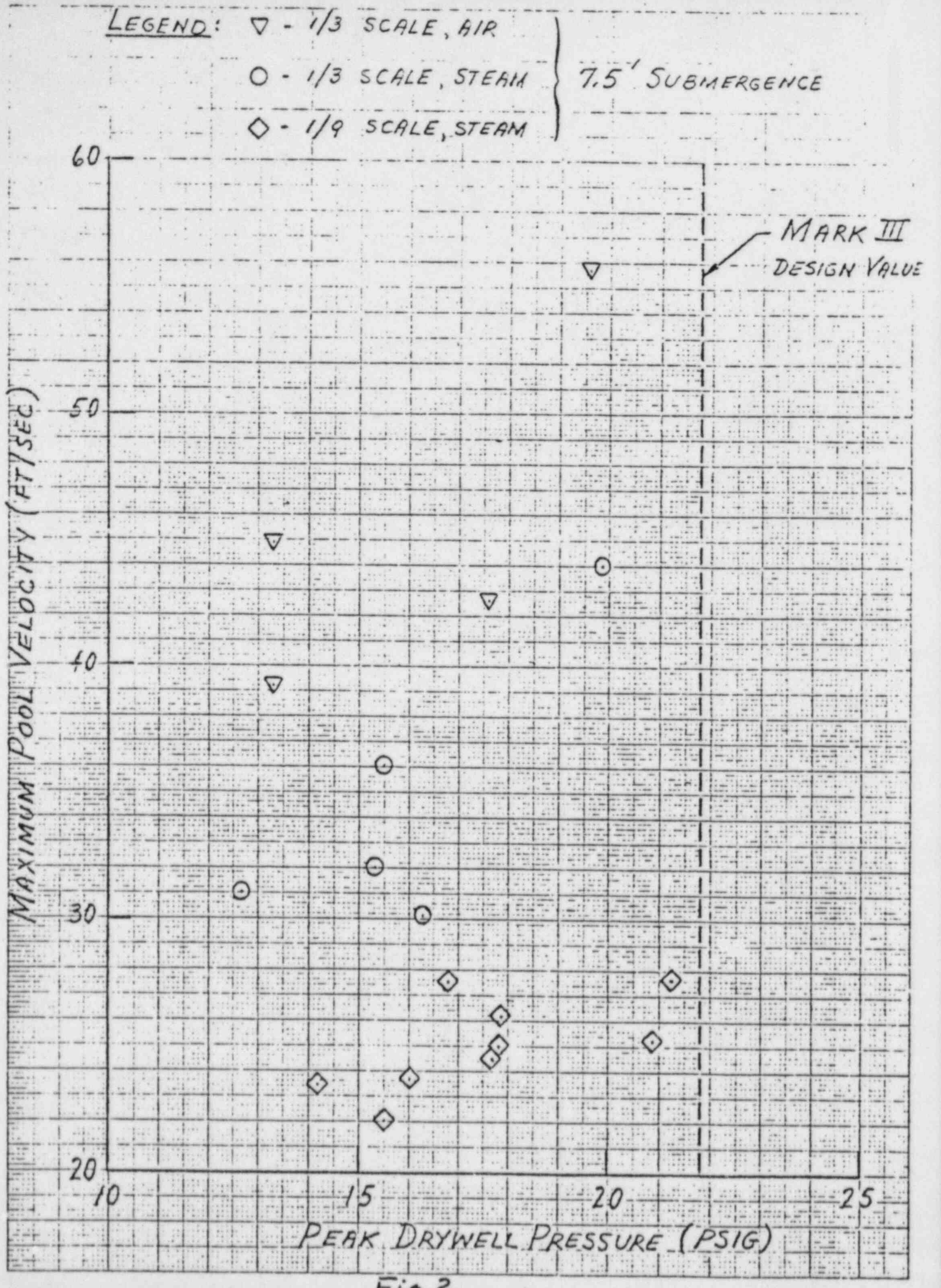
LEGEND: ▽ - 1/3 SCALE, AIR
○ - 1/3 SCALE, STEAM
◇ - 1/9 SCALE, STEAM
7.5' SUBMERGENCE
MARK III
DESIGN VALUE
MAXIMUM POOL VELOCITY (FT/SEC)
60
50
40
30
20
10
15
20
25
PEAK DRYWELL PRESSURE (PSIG)

Fig 2

4. The pool impact specification for "small structures" comes from one specific PSTF test: Test No. 5706/4. It is claimed that this test is representative of Mark III pool impact. Please explain how impact data from a test conducted at a pool velocity of 21 ft/sec is applicable for pool impact loads at 40 ft/sec.

5. The impact specification comes from one test run (5706/4) which had a specific pool inclination. According to the impact model (Equation (7), NEDE-20732-P) the pulse duration Δt is proportional to tan θ. It follows then that in regions of the pool where θ is half the inclination of Test 5706/4, the pulse duration would be 3.5 msec and the maximum pressure would be 230 psi (instead of 115). This pulse could result in substantially greater stress in the structure than the ICLR specification (actual factor depends on the natural frequency). What is the basis for assuming that this would not occur?

6. The froth impact specification is based on Test No. 5706/6. It is claimed that the froth velocity in Test 5706/6 is representative of actual Mark III conditions. The basis for this statement is Figure 18, NEDE-20732-P, which shows good agreement between calculated Mark III pool swell and measurements in Test 5706/6 for the first 5 feet of pool rise. According to ICLR, the pool would rise an additional 14 feet before breakthrough. Since the large-scale PSTF cannot sustain the Mark III drywell pressure much beyond vent clearing (see Figure 16, NEDE-20732-P), what assurance is there that Test 5706/6 would match the Mark III pool swell beyond the 5 foot rise?

7. The wetwell pressurization (below HCU floor) during froth flow through the floor is obtained from an analytical model (NEDO-20533-1). The conservatism of this model is claimed on the basis of comparison with data from Test Series 5801 and 5802. These correspond to vent submergences of 5 and 6 feet, respectively. The nominal Mark III submergence is 7.5 feet. Examination of the data shows a very large effect of vent submergence on wetwell pressurization as well as on the accuracy of the model. The model does not include vent submergence as a parameter.

What evidence is there to show that the model is reasonable at the real Mark III vent submergence, i.e., at 7.5 feet?

8. With regard to drag on gratings, why is the ΔP applied to the metal bars instead of the whole flow area? Application of momentum equation clearly shows that use of the total flow area is consistent with the definition of ΔP in Perry's Chemical Engineer's Handbook.

9. The load specification for structures above the pool is dependent on the elevation of each structure above the initial pool surface. The current load specification is based on solid water impact for structures at elevations of 18 feet and less, froth impact for structures at 19 feet or more, and a linear transition of the load specification for structures between 18 and 19 feet above the initial pool surface. Solid water impact loads are assumed when the water ligament thicknesses are greater than 2 feet. If "breakthrough" is defined as the point at which the ligament thickness if 2 ft or less, then the 18 ft elevation is acceptable as the breakthrough elevation. Indeed, based on the 1/3-scale data, one can make a case for the slug thicnkess being about 1 ft or less at the 18 ft elevation (see BNL's "Review of GE's Data Base for Predicting Mark III Pool Swell," 21 March 1977).

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might indeed be valid for certain classes of physical phenomena.* However, GE's 1/3 area scale system is not geometrically similar to a full-scale system (should the characteristic length in Eq. (1) be the vent diameter, which is $1/\sqrt{3}$ times the full-scale value of the submergence, which is equal to that in the full-scale system?), and the eliminiation of gravitational effects in the horizontal-vent CO has not been demonstrated to be valid (is the Froude number based on CO water speed and vent diameter very large in all cases?). Even if these two difficulties can be resolved, the scaling law is based on some further assumptions about the physical nature of CO, and should be at least spot verified in two different-scale systems.

One might note that if, in the Mark III horizontal vent system, CO is a local phenomenon at the vent exit vicinity, one which depends on the local undisturbed pool pressure, the water temperature, and the steam mass flux, but is independent of (a) the vent length and upstream system geometry, (b) the pool geometry or size (that is, the CO sees the pool as being effectively infinite) and (c) gravity, then the proposed method of scaling may be reasonable for local conditions near the vent exit, such as the "source pressure". Actual pressures in the pool far (many vent diameters) away from the vent exit may, of course, be governed by the pool geometry, which is incorrect in a distorted geometry model, and may therefore not satisfy the proposed scaling. In any case, if these conditions are satisfied, then GE's method of inferring a "source pressure" history for CO from the 1/3 area scale test data and using analysis to translate it to wall pressure histories in the (different) full scale pool may have some credence.

*See, for example, A. A. Sonin, "Scaling Laws for Small-Scale Modeling of Steam Relief into Water Pools", ASME Paper, 1980 ASME Winter Annual Meeting, Nov. 16 - 21, 1980, Chicago. (Attached)

However, three points must be made. First, a 1 ft slug with a 40 or 60 ft/s velocity does not simply disappear into spray over the entire pool cross-section in the course of its next foot of travel. Thus, there is the distinct likelihood that a 1 ft (or thinner) ligament will continue to travel past 18'. Secondly, near the pool walls, the ligament is thicker and may travel considerably beyond 18', albeit at lesser speed. Thirdly, a 1 ft slug impacting at top speed on a small structural member (say, 6" width) will give rise to the full pool swell impact load, not the froth impact load which is in GE's specification.

Therefore, provide further justification that "froth loading" is the proper specification above the 18-19 ft level for (a) small structures, less than a foot in width, and (b) structures like ledges which are near the pools' vertical walls.

10. The following question concerns the CO scaling laws presented at the July 1980 meeting. GE assumes that the 1/3 area scale tests give full-scale pressures at the corresponding mass flow densities, pool temperatures, etc., but that frequency must be scaled as .

$$f \propto \frac{1}{D_{vent}} \qquad (1)$$

where D_{vent} is the vent diameter. This scaling method needs careful justification.

It is possible to show that when gravitational effects are negligible, and when geometrically similar systems are used. then the above scaling law

Our specific requests are:

(a) Provide the rationale for the assumed-scaling laws.

(b) Provide the rationale for being able to use a geometrically distorted reduced scale system to represent the full scale one.

(c) Provide numerical justification that the assumptions on which the scaling is based are valid during CO.

(d) Document a careful comparison of the proposed scaling with the results from different scale, comparable systems. Include enough data to make the comparison plausible.

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11. Present, in detail, the experimental results used to confirm the scaling predictions for both frequency (f) and peak pressure amplitude (PPA). In particular, address the following points:

a) The range of frequencies for the 1/9 area scaled tests presented in TRM-21 is 4 to 9 Hz, yet it is stated in NEDE-24720-P (p. 5-7) that the observed range of frequencies is 7-10 Hz or 19-25 Hz. Similarly, for the 1/3 area scaled tests, frequency ranges are 2 to 5 and 3 to 7 Hz from TRM-21 and NEDE-21596-P, respectively. Explain these apparent discrepancies.

b) In NEDE-24720-P (p. 5-9) it is stated that "pressure magnitudes are always below 1-psi rms". Although the scaling analysis predicts a constant pressure amplitude, the maximum peak-to-peak value in the 1/3 area scaled tests is given as 10.6 psi on the containment wall with even higher values measured on the drywell wall. Given that the ratio $\frac{RMS}{PPA}\big|_{TEST}$ is about 0.2 (TRM-31), this predicts an increase from the 1/9 to 1/3 area scaled tests of more than a factor of two.

c) When presenting confirmatory data, specify the particular test and test conditions corresponding to the measurement, and use, whenever possible, measurements from different scales in which the initial pool temperature, mass flux and blowdown fluid are comparable.

d) If the scaling analysis is indeed correct, it must be applied only to the "source pressure" (i.e., at the top vent); pressures measured at remote locations will, in general, depend upon scale and geometry. It is therefore not appropriate to use such measurements directly to "confirm" your scaling predictions. In this context two questions arise:

(i) Can the magnitude of the source pressure be ascertained, with confidence, considering that the closest pressure transducers are positioned 2 ft and 4.7 above the vent axis (1/3 area scaled PSTF) and may be influenced by the free surface? Describe your methods for estimating source pressure which presumably will involve either a potential flow calculation to compute relative pressure magnitudes, or a method of extrapolation using carefully documented pressure measurements.

(ii) Do your estimated values of source pressure conform to the predictions of the scaling analysis?

12. Provide all information pertaining to the full scale observations of CO including pressure recordings, PSD's, and test conditions. Specify

which of these results are used to confirm pressure and frequency scaling predictions. (See also questions 11c and 11d.)

13. In reference to the functional relationships identified in TRM-17 ($PPA = F_1 (G, C_A, T_p)$ and $f = F_2 (G, C_A, T_p)$):

a) Describe the procedures used to obtain these functional relationships and show the general form they assume. That is, are $\partial F_{1,2}/\partial T$, $\partial F_{1,2}/\partial G$ and $\partial F_{1,2}/\partial C$ constant or do they vary with T_p, G, and C_A? If they are assumed constant, provide a rationale for that restriction.

b) Which experimental measurements (test run and measurement location) are used to obtain the form of these expressions? If any measurements were excluded, explain why.

c) Show that the design load bounds the entire range of expected conditions. To do so it must be demonstrated that, if, for example, the value of T_p used in the load definition is varied, then PPA at each frequency is bounded by the design load. Stated in different terms, is the design load chosen so that, <u>at each frequency</u>, either $\partial PPA/\partial G$, $\partial PPA/\partial A$ and $\partial PPA/\partial T$ are all equal to zero (and at a maximum of PPA), or PPA is at its maximum value?

14. In reference to the potential flow analysis used to obtain the design load pressure distribution:

a) Describe the assumptions, methods, and results of the potential flow analysis.

b) Show, using either 1/3 or 1/9 area scaled test measurements, that the appropriate potential flow analysis adequately predicts the observed pressure distributions.

c) Although the source is assumed to be located at the top vent, the closest pressure measurements were obtained at a distance 2 ft above the vent axis. State which measurements were used to determine source strength and show that the method used to predict source strength is conservative.

d) The pressure distribution function must depend upon frequency. Show that, as specified, it is conservative with respect to the range of frequencies anticipated.

15. The following assumptions must apply if Test series 5707 is to properly or conservatively represent Mark III chugging conditions:

a. The drywell volume per vent trio should not significantly affect the chugging. (The drywell volume was too small in 5707 compared with Mark III.)

b. All peak chugging loads should occur at mass flow rates below about 1/3 DBA. (Most of the 5707 tests fell below those mass flow rates.)

c. Steam discharge (vs. liquid discharge) should give the worst chugging loads under all conditions. (The 5707 data are for steam discharge.)

d. Pool thermal stratification should not control chugging (Thermal stratification was not properly simulated in the 5707 tests, partly because of the too low mass flow rates; see pp. 3-16 of NEDE-21853-P).

We will comment further on these points in turn, and define questions.

A. Drywell volume effect

The answer given at the July 1980 meeting is based on viewgraph JBH-39. Of the three points on that viewgraph, at drywell volume scale factors of 0.33, 0.6, and 1, the point at unity is for the 1/3-scale system and is not strictly comparable with the other two. Based on the other two, one might extrapolate to a higher pressure at higher drywell volume. Indeed, on pp. 3-15 of NEDE-21853-P it is stated that in Run 25 of 5707, which had the drywell volume scale factor of 0.33, there were "unrealistically large drywell pressure and weir water level oscillations between chugs". GE's case for assuming that a factor of 0.6 was equivalent to unity is based on a direct comparison with a different-scale system, which we cannot accept.

Therefore, a more convincing argument against drywell volume effects needs to be provided. Possibly a better case can be made by re-doing the JBH-39 figure, showing means and σ's of Runs 25 and 27, as well as peak values. If not, a factor for conservatism might be introduced on the design value.

B. Mass flow effects

At the July 1980 meeting many of the previously published statements regarding mass flow effects were withdrawn and a new interpretation of the data was given, claiming that mass flow did not affect chugging at either low pool temperature (where bubble collapse occurred inside the vent at all mass flows) or at a sufficiently high pool temperature (where collapse occurred outside the vent at all mass flows), although one presumes a mass flow effect will be present at some intermediate pool temperature (where a sufficiently high mass flow will push bubble collapse out of the vent).

The new data interpretation, and revised conclusions, need to be formally documented before a final review can be made.

C. Steam vs. liquid discharge

Provide the basis for assuming that liquid blowdowns do not give higher chugging loads than steam blowdown. On pp. 5-48 of NEDE-21596-P it is stated that the data base is insufficient to resolve this issue.

D. Pool thermal stratification

At the July 1980 meeting, GE's answer was that only pool temperature, not pool thermal stratification, affected chugging significantly, and that conservative data are used by picking extremes in initial pool temperature.

This response needs to be formally explained and quantitatively documented.

16. Show, by referring to the multivent 1/9-scale data, that the same degree of randomness of the peak signal occurs at corresponding points in the 3 different "cells" in a given chug as the degree of randomness over time in a single-cell system. This can be done, for example, by considering the statistical correlation of the peaks in 2 or 3 different cells of the multivent system for a given chug, and comparing with the correlation of 2 or 3 successive peaks in the single-cell full-scale system (or in a single cell of the 1/9-scale system).

17. Document your case that the pressure peaks are indeed uniformly random over the entire runs (or portions of runs) which represented the worst cases and over which averages were taken for a load definition. In addition, supply complete time traces (entire blowdown) for the runs/sensors indicated.

RUN	Pressure Sensor
5707-1	Top vent sensor exhibiting highest peak overpressure
5707-2	"
5707-11	Drywell wall sensor at elevation 13 ft
5707-12	"

Alternatively, provide tabulated values of peak pressures for each chug observed giving time at which chug was observed.

18. Provide ARS comparisons between design load definition and the following pressure signatures:

a) The top vent pressure trace which shows the highest peak overpressure observed during Test Series 5707 (540 psi).

b) The drywell wall pressure trace which shows the highest peak overpressure observed during Test Series 5707 (100 psi).

b) The weir wall pressure trace which shows the highest peak overpressure observed during Test 5707 (43 psi).

19 Discuss the non-exceedance probability levels associated with the prescribed chugging pressure amplitudes. Comment on how these are affected by the large measurement uncertainties indicated by the discussion given in Appendix A of NEDE-21853-P, particularly for the pool boundary loads.

20. Provide further justification for the use of the one degree of freedom model to determine the magnitude of FSI effects on chugging in the full scale PSTF, while the NASTRAN code was used to find the influence of FSI on 1/3 and 1/9 area scaled CO pressures. In particular, explain why it is sufficient to consider only the lowest natural frequency of the full scale PSTF when the excitation function for chugging has substantial energies at higher frequencies. If ample justification can be provided for the use of the simple model, provide the specific values chosen for the masses and "spring constant" and describe how each value was selected.

21. In reference to the graph of AB-11:

a) The "rigid wall" curve presumably is obtained from a potential flow analysis assuming rigid boundaries at the walls. The results shown, however, appear to be inconsistent with the load distribution specified for the full scale plant which was also computed from a potential flow analysis (see TRM-11) and which suggests that $P_{vent}/P_{cont.} > 6$. Although, admittedly, one result pertains to the 1/3 scale and the other to full scale, the differences seem too large to be explained by the difference in scale alone. Provide an explanation for these differences.

b) Provide a curve similar to AB-11 (using the same coordinates) for the 1/9 area scaled PSTF that we can use for direct comparison.

c) Identify the resonances that appear in the 1/3 and 1/9 area scaled transfer functions (see AB-11 and AB-15). In particular, describe the resonant mode that causes the first departure of the rigid wall and flexible wall curves.

22. Although we are not in a position to examine the details of the numerical calculations using the NASTRAN code, we should be given sufficient information to be convinced that the code is appropriate and has been applied correctly. With this aim, provide a brief description of the NASTRAN code including any simplifying assumptions that have been made, either fluid dynamic or structural. Explain, also in general terms, how this code has been applied in the 1/3 and 1/9 area scaled tests.

23. It should be possible to provide some confirmation of the transfer functions computed using NASTRAN through a direct comparison with experimental results. Show, using actual CO pressure measurements near the top vent and on the containment wall directly opposite the top vent, that the computed transfer functions are correct. Data from both the 1/3 and 1/9 area scaled PSTF's should be used and the comparisons should be made at several frequencies spanning the range of interest.

24. Along the same lines as question 23, it should be possible to substantiate your claims that FSI is relatively unimportant in the full

scale PSTF by comparing predicted pressure amplitudes (predictions based on a fluid dynamic analysis with the appropriate chugging source function) to actual pressure recordings. The necessary comparisons should be made at several locations on the drywell and containment walls. Please specify which data is being used for the comparison and give reasons for the exclusion of any apparently relevant data.

25. Describe the rationale used to arrive at the shape of the cycling fluid temperature time history as shown in Figure 4.11 of the ICLR. Demonstrate that the relative time durations of the plateaus (i.e., 0.2P for the steam temperature and 0.4P for the bulk temperature) are conservative representations of actual test data. In addition, specify how this history should be used to determine the loading condition on the drywell wall, particularly with respect to what heat transfer coefficients are appropriate.

26. A bulk pool temperature history, as mentioned in Section 4.6 of the ICLR, should be used to reduce the ΔT amplitude of the cycling fluid temperature time history. Provide a typical example of a bulk pool temperature history along with a description of the method used to obtain it.

27. Justify the conservatism of the application area used for the top vent chugging temperature profile as presented in figure 4.11 of the ICLR. Document the method and supporting data that were used to arrive at this specification. Plots of temperature and bubble travel as a function of time, at the measurement locations above the top vent, should be included for several of the initial pool temperature runs of test series 5707.

28. The suppression pool temperature profile for large breaks, as presented in Figure N-3 of attachment N, was calculated using the percentage energy deposition distribution from the 1/3 area scale tests. Justify the assumption that the 1/3 area scale results (Test Series 5807) are applicable for simulation of prototypical thermal stratification. Address

the possibility of non-prototypical pool mixing caused by the distorted geometry (i.e., shortened horizontal pool length).

29. Demonstrate that the location of the RHR suction and return lines will provide adequate pool mixing for an SBA. Specify the direction, velocity and volume flow rate for each discharge line and indicate how a net circumferential pool circulation pattern is achieved.

30. The negative drywell-wetwell differential pressure caused by ECCS reflood can result in the flow of water over the weir wall into the drywell. The following questions are pertinent to the description of this postulated event presented in Section 5.1.5 of the ICLR:

a. Describe how the drywell pressure history presented in Figure 5.7 and the peak velocity of 25 feet/second were calculated.

b. What structures (including pipes or beams) are located above the weir wall which could be affected by the flow of the water?

c. Justify why the structures in the path of the water are not designed for impact loads.

d. Specify the drag coefficients which should be used for the drag load calculations.

31. The following questions relate to the submerged structure loads:

a. <u>LOCA Water Jet</u>

Both experimental evidence and theoretical calculations suggest that structures not in the direct path of a LOCA Water Jet experience highest loads after vent clearing. It also appears that for those structures the LOCA Bubble Load calculation bounds the measured load after vent clearing. Due to the uncertainty in the width and penetration distance of the jet the definition of "not in the direct path" is imprecise. Some statement will therefore have to be made in the Acceptance Criteria, defining regions within which water jet loads will have to be considered if there are any structures located there. At present the combination of theory and available data suggests that a cylindrical region of $1.5D_{vent}$ diameter and $5D_{vent}$ length along the axis of each vent of diameter D_{vent} would be a conservative definition. Can such a criterion be used for the Mark III design?

b. <u>LOCA Bubble Load</u>

i) The application of a multiplier of 2 to account for the induced field due to bubble rise is conservative when applied at the time of peak acceleration due to bubble growth. Clearly, at a later time when the bubble radius decelerates this procedure may no longer be conservative. The conservatism of the overall procedure, therefore, depends on how the time history of the load is applied to the structures with a relatively low natural frequency. Explain the procedure for applying the load to the structure.

ii) The acceleration volumes shown in Tables 3BL-2 and 3BL-3 of GESSAR are based on potential flow theory. For a cylindrical geometry there is experimental verification of the validity of this result for accelerating flows. There is some evidence from oscillating flows that structures with sharp corners, that serve as vortex generators, can experience acceleration drag associated with acceleration volumes substantially above the potential flow results. The Mark I Acceptance Criteria 2.14.2 (2a and 2b) provide a simple "equivalent cylinder" calculational procedure that bounds the available data, including the standard drag contribution. If applying this same procedure to Mark III gives rise to any difficulties, please explain why and provide an alternate approach.

c. Condensation Oscillation Loads

i) No explanation for the basis of the source strength $\dot{S} = 188$ ft^3/sec^2 is given. It is not clear whether this number is based on some averaging procedure that is used for the boundary loads or on some bounding deduced source. If an averaging procedure was used then the source strength may not be conservative for computing local loads on structures near a vent which experiences a "maximum" CO event. Give a detailed description of the procedure by which $\dot{S}$ is computed.

ii) Whether or not standard drag can be neglected in the calculation of CO loads on submerged structures depends on the size of the structure and distance from the source. Using the Criteria 2.14.4 (2a) of the Mark I Acceptance Criteria the standard drag can be neglected for structures whose diameter is larger than $\dot{U}_\infty T^2/17.2$ where T is the period of condensation oscillation. For the source and structure used in the sample problem in GESSAR this criteria would impose a lower limit of about 6 inches. For a structure within 3 feet of the vent the lower limit would be about one foot. Clearly, the neglect of standard drag will be acceptable only within some constraints on structure size, distance from source and source strength. Review the types and location of structures in the pool and describe any potential difficulty in using the Mark I Criterion.

iii) The application to any structures with sharp corners requires the same modification as discussed under LOCA Bubble Loads. In addition, if the calculation of standard drag is necessary, the drag coefficient must be based on oscillatory flow data. The use of $C_D = 3.6$ as in the Mark I Acceptance Criteria should not be a problem as the standard drag contribution is at most a small correction to the acceleration drag. Therefore, verify that the application of Mark I Acceptance Criterion 2.14.4 (2a) can be applied to Mark III structures.

d. Chugging Loads

The acoustical model for the computation of chugging loads on submerged structures is conservative for any realistic structures in the Mark III pool, subject to the conservative modelling of the source strengths and locations. For local loads it is clear that 1) maximum, not average, chug strength is relevant and 2) non-synchronous chugging may lead to higher loads than totally synchronized chugs. Give a detailed description of the basis for the $(\Delta p_o r_o)$ = 2.53 psi ft chug strength, and the synchronous initiation of 2 msec chugs at all neighboring vents.

e. S/RV Submerged Structure Loads

i. The velocities and accelerations shown in the sample problem in GESSAR. do not appear consistent unless the time increments shown are much larger than the bubble oscillation frequency. For the structure chosen, the standard drag is negligible; therefore, the velocity is not improtant, but for other structures this may not be correct. Show why the velocity changes by only a fraction of the amount predicted by using an average acceleration between the time points.

ii) Loads on some structures may be maximized by assuming four identical bubbles in phase, while on other structures either different strength or out of phase bubbles may lead to maximum loads. Explain whether step 7 of section 3BL.3.2 of GESSAR implies such variation of phasing and/or strength relationships between bubbles at the same quencher or does it only apply to phasing between different S/RV's.