MISSISSIPPI POWER & LIGHT COMPANY
Helping Build Mississippi
P. O. BOX 1640, JACKSON, MISSISSIPPI 39205

NUCLEAR PRODUCTION DEPARTMENT

December 3, 1982

U.S. Nuclear Regulatory Commission
Office of Nuclear Reactor Regulation
Washington, D.C. 20555

Attention: Mr. Harold R. Denton, Director

Dear Mr. Denton:

SUBJECT: Grand Gulf Nuclear Station
Units 1 and 2
Docket Nos. 50-416 and 50-417
License No. NPF-13
File 0260/0272/L-860.0
Final Information to Close
Humphrey Concerns
AECM-82/574

References: 1. Letter AECM-82/321 from L. F. Dale to H. R. Denton, dated July 15, 1982.

2. Letter AECM-82/497 from L. F. Dale to H. R. Denton, dated October 22, 1982.

Attachment 1 to this letter contains the balance of the information which Mississippi Power & Light Company (MP&L) committed in reference 1 to submit to your staff. This information completes the program which MP&L undertook to respond to the design questions raised by Mr. John Humphrey.

Attachments 2 and 3 to this letter contain reports which were prepared for MP&L and the Containment Issues Owners Group. Attachment 2 is a detailed review of the use of SOLAVOF in evaluating the effects of local encroachments on pool swell. Attachment 3 is a report on tests and analyses of the effectiveness of various RHR systems in eliminating suppression pool thermal stratification.

Attachment 3 contains information which is proprietary to General Electric Company. An affidavit identifying the reasons for maintaining this information as proprietary is included. All proprietary information is clearly identified as such.

Attachment 4 provides additional information to respond to questions raised by your staff and their consultants. A complete list of all questions which MP&L is attempting to answer is contained in

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reference 2. MP&L currently anticipates that all information requested by your staff and their consultants will be submitted by the end of December.

Yours truly,

L. F. Dale
Manager of Nuclear Services

RWE/SHH/JDR:lm
Attachments

cc: Mr. N. L. Stampley (w/o)
Mr. R. B. McGehee (w/o)
Mr. T. B. Conner (w/o)
Mr. G. B. Taylor (w/o)

Mr. Richard C. DeYoung, Director (w/a)
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AE3U2

G E N E R A L   E L E C T R I C   C O M P A N Y

AFFIDAVIT

I, Glenn G. Shewrood, being duly sworn, depose and state as follows:

1. I am Manager, Nuclear Safety and Licensing Operation, General Electric Company, and have been delegated the function of reviewing the information described in paragraph 2 which is sought to be withheld and have been authorized to apply for its withholding.

2. The information sought to be withheld is contained in the response to NRC questions related to Humphrey concerns.

3. In designating material as proprietary, General Electric utilizes the definition of proprietary information and trade secrets set forth in the American Law Institute's Restatement Of Torts, Section 757. This definition provides:

   "A trade secret may consist of any formula, pattern, device or compilation of information which is used in one's business and which gives him an opportunity to obtain an advantage over competitors who do not know or use it.... A substantial element of secrecy must exist, so that, except by the use of improper means, there would be difficulty in acquiring information.... Some factors to be considered in determining whether given information is one's trade secret are: (1) the extent to which the information is known outside of his business; (2) the extent to which it is known by employees and others involved in his business; (3) the extent of measures taken by him to guard the secrecy of the information; (4) the value of the information to him and to his competitors; (5) the amount of effort or money expended by him in developing the information; (6) the ease or difficulty with which the information could be properly acquired or duplicated by others."

4. Some examples of categories of information which fit into the definition of proprietary information are:

   a. Information that discloses a process, method or apparatus where prevention of its use by General Electric's competitors without license from General Electric constitutes a competitive economic advantage over other companies;

   b. Information consisting of supporting data and analyses, including test data, relative to a process, method or apparatus, the application of which provide a competitive economic advantage, e.g., by optimization or improved marketability;

c. Information which if used by a competitor, would reduce his expenditure of resources or improve his competitive position in the design, manufacture, shipment, installation, assurance of quality or licensing of a similar product;

d. Information which reveals cost or price information, production capacities, budget levels or commercial strategies of General Electric, its customers or suppliers;

e. Information which reveals aspects of past, present or future General Electric customer-funded development plans and programs of potential commercial value to General Electric;

f. Information which discloses patentable subject matter for which it may be desirable to obtain patent protection;

g. Information which General Electric must treat as proprietary according to agreements with other parties.

5. In addition to proprietary treatment given to material meeting the standards enumerated above, General Electric customarily maintains in confidence preliminary and draft material which has not been subject to complete proprietary, technical and editorial review. This practice is based on the fact that draft documents often do not appropriately reflect all aspects of a problem, may contain tentative conclusions and may contain errors that can be corrected during normal review and approval procedures. Also, until the final document is completed it may not be possible to make any definitive determination as to its proprietary nature. General Electric is not generally willing to release such a document to the general public in such a preliminary form. Such documents are, however, on occasion furnished to the NRC staff on a confidential basis because it is General Electric's belief that it is in the public interest for the staff to be promptly furnished with significant or potentially significant information. Furnishing the document on a confidential basis pending completion of General Electric's internal review permits early acquaintance of the staff with the information while protecting General Electric's potential proprietary position and permitting General Electric to insure the public documents are technically accurate and correct.

6. Initial approval of proprietary treatment of a document is made by the Subsection Manager of the originating component, the man most likely to be acquainted with the value and sensitivity of the information in relation to industry knowledge. Access to such documents within the Company is limited on a "need to know" basis and such documents at all times are clearly identified as proprietary.

7. The procedure for approval of external release of such a document is reviewed by the Section Manager, Project Manager, Principal Scientist or other equivalent authority, by the Section Manager of the cognizant Marketing function (or his delegate) and by the Legal

Operation for technical content, competitive effect and determination of the accuracy of the proprietary designation in accordance with the standards enumerated above. Disclosures outside General Electric are generally limited to regulatory bodies, customers and potential customers and their agents, suppliers and licensees only in accordance with appropriate regulatory provisions or proprietary agreements.

8. The information mentioned in paragraph 2 above has been evaluated in accordance with the above criteria and procedures and has been found to contain information which is proprietary and which is customarily held in confidence by General Electric.

9. The information, to the best of my knowledge and belief, has consistently been held in confidence by the General Electric Company, no public disclosure has been made, and it is not available in public sources. The material consists of information which is part of the General Electric technology base which has a value that is clearly substantial and would be lost if the information were disclosed to the public.

STATE OF CALIFORNIA)
COUNTY OF SANTA CLARA) ss:

Glenn G. Sherwood, being duly sworn, deposes and says:

That he has read the foregoing affidavit and the matters stated therein are true and correct to the best of his knowledge, information, and belief.

Executed at San Jose, California, this 2 day of December , 1982.

Glenn Sherwood

Glenn G. Sherwood
General Electric Company

Subscribed and sworn before me this 2nd day of December 1982.

Ruthe M. Kinnamon

NOTARY PUBLIC, STATE OF CALIFORNIA

OFFICIAL SEAL
RUTHE M KINNAMON
NOTARY PUBLIC - CALIFORNIA
SANTA CLARA COUNTY
My comm. expires APR 26, 1985
175 Curtner Avenue, San Jose, CA 95125

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ATTACHMENT 1

AECM-82/574

Action Plan 1

I. Issues Addressed

1.1 Presence of local encroachments such as the TIP platform, the drywell personnel airlock and the equipment and floor drain sumps may increase the pool swell velocity by as much as 20 percent.

1.2 Local encroachments in the pool may cause the bubble break-through height to be higher than expected.

1.4 Piping impact loads may be revised as a result of the higher pool swell velocity.

II. Program For Resolution

1. Provide details of the one-dimensional analysis which was completed and showed a 20% increase in pool velocity.

2. The two-dimensional model will be refined by addition of a bubble pressure model and used to show that pool swell velocity decreases near local encroachments. The code is a version of SOLA.

3. The inherent conservatisms in the code and modeling assumptions will be listed.

4. The modified code will be benchmarked against existing clean pool PSTF data.

5. A recognized authority on hydrodynamic phenomena will be retained to provide guidance on conduct of the analyses. This consultant will review the merits of performing these analyses with the modified version of the SOLA code.

6. The effects of the presence of local encroachments on pool swell will be calculated with the two dimensional code. Three-dimensional effects (such as bubble break through in non-encroached pool regions) will be included based upon empirical data.

III. Schedule

Items 1 and 2 were completed and included in AECM-82/353. Items 3, 4 and 6 were completed and included in AECM-82/497. Item 5 is completed and included in this submittal.

IV. Final Program Results

A report from Flow Sciences, Inc., is included in Attachment 2 to this letter.

Action Plan 2

I. Issues Addressed

1.3 Additional submerged structure loads may be applied to submerged structures near local encroachments.

II. Program for Resolution

1. The results obtained from the two-dimensional analyses completed as part of the activities for Action Plan 1 will be used to define changes in fluid velocities in the suppression pool which are created by local encroachments. Supporting arguments to verify that the results from two-dimensional analyses will be bounding with respect to velocity changes in the suppression pool will be provided.

2. The new pool velocity profiles will be used to calculate revised submerged structure loads using the existing or modified submerged structure load definition models.

3. The newly defined submerged structure loads will be compared to the loads which were used as a design basis for equipment and structures in the Grand Gulf Nuclear Station suppression pool.

III. Schedule

Item 1 was completed and included in AECM-82/497. Items 2 and 3 are completed and included in this submittal.

IV. Final Program Results

Item 2

The increases in drag load velocities which are caused by the TIP platform are enveloped by the 40 foot per second drag load velocity specified as the design basis for the Grand Gulf Nuclear Station in GESSAR II.

Item 3

The increased pressure loadings on piping and structures above the pool surface in the vicinity of the TIP platform as a result of encroachment effects are enveloped by the 60 psi design impact load as identified in GESSAR II. For piping and structures below the pool surface, the increased pressure loadings produced as a result of the encroachment are within the faulted stress allowables.

Action Plan 5

I. Issues Addressed

2.1 The annular regions between the safety relief valve lines and the drywell wall penetration sleeves may produce condensation oscillation (C.O.) frequencies near the drywell and containment wall structural resonance frequencies.

2.2 The potential condensation oscillation and chugging loads produced through the annular area between the SRVDL and sleeve may apply unaccounted for loads to the SRVDL. Since the SRVDL is unsupported from the quencher to the inside of the drywell wall, this may result in failure of the line.

2.3 The potential condensation oscillation and chugging loads produced through the annular area between the SRVDL and sleeve may apply unaccounted for loads to the penetration sleeve. The loads may also be at or near the natural frequency of the sleeve.

II. Program for Resolution

1. The existing condensation data will be reviewed to verify that no significant frequency shifts occurred. The data will also be reviewed to confirm that the amplitudes were not closely related to acoustic effects.

2. The driving conditions for condensation oscillation at the SRVDL exit will be calculated. Based on these calculations, existing test data will be used to estimate the frequency and bounding pressure amplitude of condensation oscillation at the SRVDL annulus exit.

3. A wide difference between the C.O. frequency and structural resonances will be demonstrated. The margin between the new loads and existing loads will be quantified.

4. Provide a detailed description of all hydrodynamic and thermal loads that are imposed on the SRVDL and the SRVDL sleeve during LOCA blowdowns.

5. Assure that thermal loads created by steam flow through the annulus have been accounted for in the design.

6. State the external pressure loads which the portion of the SRVDL enclosed by the sleeve can withstand.

7. Calculate the maximum lateral loads which could be applied to the sleeve by phenomena analogous to the Mark I and Mark II downcomer lateral loads.

III. Schedule

Items 1-7 are completed and included in this submittal.

IV. Final Program Results

Item 1

Condensation oscillation frequency shifts which occurred in the 1/9 area scale PSTF data are discussed in some detail in References 1 and 2. The unique size of the 1/9 scale PSTF vent caused these frequency shifts to occur. Late in the transient, the C.O. frequency content excited the quarter standing wave (20-24 Hz) in the PSTF pool. This caused the root mean square pressure amplitude to increase by a factor of approximately 2. The amplitude of oscillation is consequently related to acoustic effects only for the 1/9 area scale PSTF tests. Similar acoustic effects were not observed in 1/3 area scale or full scale tests.

The size of the SRVDL sleeve annulus is such that the C.O. frequency is much higher than the frequency which occurred in the 1/9 scale PSTF vent. The first fundamental frequency of sleeve C.O. is relatively close to the three quarter standing wave in the pool. However, when standing waves have been detected in Mark III pool tests, it is only the one quarter standing waves which have appeared. The very conservative analysis performed under Item 2 of this action plan demonstrates that a factor of 2 margin exists within the design basis which should easily encompass any acoustic effects.

The frequency in the sleeve is expected to decrease with time. Chugging should occur in the main vents effectively eliminating C.O. in the SRVDL-sleeve annulus before the C.O. frequency can approach a frequency capable of exciting the pool quarter standing wave.

Item 2

A calculation of the steam mass flux at the SRVDL sleeve discharge during a postulated LOCA shows that condensation oscillation (C.O.) can be expected to occur in the sleeve. The GESSAR II C.O. load definition pressure time-history was modified to include higher frequency components attributable to C.O. in the SRVDL sleeve. A comparison of response spectra (ARS) of the C.O. pressure time histories which included the contribution of the sleeve with chugging and pool swell load definitions shows that the C.O. loads produced in the sleeve are easily bounded by other Mark III load definitions.

SRVDL Sleeve Steam Mass Flux

The condensation mode (C.O. or chugging) is determined, to a large extent, by the steam mass flux. Thus, prediction of the condensation mode for discharges from the SRVDL sleeve annulus requires an estimate of the steam mass flux through the annulus. This estimate has been made by considering the SRVDL sleeves and the top row of main vents as parallel flow paths, each with a different resistance to flow. Since the sleeve annuli have a much smaller total area than the top vents, it is logical to expect that the total flow through the annuli will be small compared to the total vent flow. For parallel flow paths, the ratio of the mass fluxes can be determined from:

$$\frac{G_{sleeve}}{G_{vent}} = \sqrt{\frac{K_{vent}}{K_{sleeve}}}$$

where G is mass flux and K is a pressure loss coefficient,

$$K = \Delta P/(\rho V^2/2\ g_c)$$

Using the dimension of the Grand Gulf SRVDL sleeves

$\frac{G_{sleeve}}{G_{vent}}$ is approximately equal to 0.8

Since this ratio is relatively close to unity, C.O. will occur in the sleeve during nearly the same time period of a LOCA as it occurs in the vent. To illustrate this, Figure 5-1 shows the vent and sleeve steam mass flux time-history calculated with M3CPT04 (Reference 3) for a Grand Gulf DBA. Assuming that transition from C.O. to chugging occurs near 10 lb/ft2-sec, Figure 5-1 shows that generally the vent and sleeve will experience C.O. simultaneously.

Defining the Load on the Pool Boundary

The C.O. occurring in the SRVDL sleeve annuli is expected to add a high frequency component to the basic vent C.O. load definition. To evaluate the effect of SRVDL sleeve C.O., a modified C.O. pressure time history was developed by summing the individual components of the main vent and SRVDL sleeve C.O. pressure histories. It was assumed that the SRVDL sleeves behave as small horizontal vents, allowing application of the Mark III C.O. methodology.

No data on condensation in slanted annular geometry currently exists. Therefore, a very conservative load definition has been provided to bound these geometric uncertainties. Reference 4 suggests that the wall pressure amplitude varies as the ratio of vent area to pool surface area. To account for uncertainties in the condensation processes which might occur in the annular SRVDL sleeve opening, the assumption was made that the amplitude varies as the square root of the vent area to pool area ratio. This assumption increases the SRVDL sleeve C.O. amplitude by a factor of 4 over the result contained in Reference 4. This large factor of conservatism is used to assure that a bounding response is obtained.

For additional conservatism, the maximum local C.O. amplitude will be considered to act azimuthally on the entire pool boundary. Globally, the SRVDL sleeve C.O. effect will be smaller since there are only 20 SRVDL sleeves compared to the 45 sets of vents present. Thus, an additional factor of approximately 2 exists over the expected global response.

A C.O. pressure time-history was calculated as:

$$\Delta P(t) = \Delta P_{vent}(t) + \Delta P_{sleeve}(t)$$

Where $\Delta P_{vent}(t)$ is the pool pressure time-history as currently defined in the GESSAR II and using the best correlation of Mark III C.O. frequency and amplitude test data (Reference 5). The term $\Delta P_{sleeve}(t)$ represents the expected pool pressure time-history resulting from C.O. only in the sleeve. This term was calculated using the same techniques and data correlations as ΔP_{vent} but amplitude and frequency were modified by the scaling assumptions previously described. The sleeve C.O. pressure time history was determined to be:

$$\Delta P_{sleeve}(t) = \frac{AMP_s(t)}{2} \{0.8 \sin(2\pi\, \tau(t) f_s(t)) + 0.3 \sin(4\pi\, \tau(t) f_s(t)) + 0.15 \sin(6\pi\, \tau(t) f_s(t)) + 0.2 \sin(8\pi\, \tau(t) f_s(t))\}, \text{ psid}$$

where:

$\Delta P_{sleeve}(t)$ = pressure amplitude contribution of the SRVDL sleeve on the drywell wall

$AMP_s(t)$ = peak-to-peak amplitude variation with time, psid

$$= \sqrt{A_{sleeve}/A_{vent}} \times 5.5 \times PPA\,(G_s, a, T)$$

$$f_s(t) = \frac{D_{h_{vent}}}{D_{h_{sleeve}}} \times f_v\,(G_s, a, T)$$

τ = relative time within each cycle, seconds

t = time from initiation of LOCA blowdown, seconds

PPA = C.O. amplitude correlation on containment wall, psid

f_v = C.O. vent frequency correlation

G_s = sleeve steam mass flux, lb/ft^2-sec

a = vent air content, percent

T = bulk pool temperature, °F

D_h = hydraulic diameter

A = area

A portion of the resulting pressure time-history on the drywell wall for Grand Gulf is shown in Figure 5-2 (vent C.O. only) and Figure 5-3 (simultaneous vent and sleeve C.O.).

Significance of the SRVDL Sleeve C.O. Load

The pressure time-histories of Figures 5-2 and 5-3 were digitized and Amplified Response Spectra (ARS) plots were prepared. Peak broadening of 15 percent was used, as in the GESSAR II C.O. load, to account for uncertainty in the predicted frequencies. The ARS resulting from the time histories given in Figures 5-2 and 5-3 are shown in Figures 5-4 and 5-5. As is evident from these plots, the SRVDL sleeve C.O. has no impact below 30 Hz. Superimposed on Figure 5-5 is the ARS of the chugging load on the drywell wall (Reference 6). In the frequency range of the sleeve C.O. pressure, signal, the chugging load is bounding by a substantial margin, even though an unrealistically large pressure due to the sleeve was utilized and credit was not taken for attenuation of the SRVDL sleeve C.O. as distance away from the sleeve increases.

Figure 5-4 does not correspond directly to the design basis accident ARS presented by Grand Gulf in support of the LOCA Licensing defense. Due to limitations in the existing code, a smaller number of cycles was used in Figure 5-4 to obtain the (DBA) C.O. peak response at the low frequency range than were used in developing the DBA C.O. ARS. At the high frequency range, however, the number of cycles used is adequate to reach the peak response and Figures 5-5 and 5-6 adequately represent the maximum amplitudes produced by the high frequency components of the CO. load.

To determine the effect of the SRVDL sleeve C.O. on the containment wall loading, the drywell composite C.O. loading was attenuated to the containment wall. The resulting ARS is shown in Figure 5-6. As is evident from this curve, the ARS of the pool swell containment wall load definition bounds the combined effect of the main vent C.O. and the SRVDL sleeve C.O. Note that the global pool swell load is compared to the local SRVDL C.O. load, so the additional factor of conservatism previously discussed (on the order of 2) is present.

In summary, a bounding and extremely conservative analysis shows that the C.O. produced by the SRVDL sleeve adds high frequency components to the basic main vent C.O. load definition. This additional contribution is bounded by other loads. Also, since the response is increased in only the high frequency range, the structural impact of this loading is very small.

Item 3

As shown in Figures 5-5 and 5-6, the presence of C.O. in the SRVDL sleeve vent creates new pressure peaks at frequencies of approximately 50 hz and 100 hz. These frequencies are considerably above the structural resonance frequency of the drywell and containment walls which is 13.55 Hz.

As noted in the response to Item 2 above, the combined C.O. loads produced by C.O. in the main vents and in the SRVDL sleeve annulus are bounded by the existing local chugging load definition for the drywell wall and by the pool swell load definition for the containment wall. Item 7 in this action plan will quantify the potential chugging lateral loads. A margin of approximately 11 kip exists in the SRVDL sleeve design to account for the chugging lateral loads.

This 11 kip margin is conservatively low because the maximum thermal stress of 28.15 kip which is produced by the maximum calculated temperature difference across the sleeve is applied to the sleeve in combination with other loads. The maximum temperature difference is 218°F based upon the peak calculated blowdown steam temperature in the drywell (298°F) and the minimum suppression pool temperature outside the sleeve (80°F). These temperatures will never occur simultaneously since the temperature of the inside surface does not increase instantaneously to 298°F and equally, the local pool temperature near the sleeve will rapidly increase beyond 80°F. Since the thermal stresses account for as much as 57% of the total allowable stress on the sleeve, depending upon the actual temperature difference, the margin available to account for new loads produced at the SRVDL sleeve annulus will increase as the temperature difference across the sleeve decreases.

Item 4

A detailed description of the hydrodynamic and thermal loads included in the design basis of the SRVDL piping and the SRVDL sleeve is presented below.

SRVDL Piping

a. Hydrodynamic Loads

1) Dynamic response due to SRV (one, all, ADS) actuation
2) Horizontal Vent Chugging/Condensation Oscillation
3) Drywell Negative Pressure
4) Drag Loads due to Quencher Air Clearing
5) Steam Hammer due to Fast Valve Opening/Closing

b. Thermal Loads

Thermal loads on piping are based on 480°F maximum steam temperature in the entire line.

SRVDL Sleeve

a. Hydrodynamic Loads

1) Dynamic response due to SRV (one, all, ADS) actuation
2) Horizontal Vent Chugging/Condensation Oscillaton
3) Drag Loads due to Quencher Air Clearing
4) Seismic Pool Slosh
5) Vent Air Clearing
6) Impact, Drag, and Fallback Loads due to Pool Swell

b. Thermal Loads

Thermal loads are based on 330°F steam temperature inside the sleeve and an outside pool temperature of 80°F.

The hydrostatic loads imposed upon the SRVDL piping and sleeve are not identical because of the different geometry and orientation of the two targets.

Item 5

The thermal loads on the SRVDL piping imposed by steam flow through the SRVDL sleeve annulus during LOCA blowdown are bounded by the thermal loads in the original design. The piping was designed to withstand maximum internal operating temperature of 480°F which is well above the blowdown steam temperature of 330°F inside the SRVDL sleeve annulus.

The thermal loads imposed on the sleeve from steam flow through the annulus have been accounted for in the design. The resultant stress is within the maximum stress allowable.

Item 6

The maximum external pressure loads which the safety relief valve discharge lines can withstand in the region enclosed by the drywell wall penetration sleeve are 561 psi (upset) and 2716 psi (faulted). These pressure loads are orders of magnitude above the maximum calculated drywell pressure.

Item 7

Reference 7 provides a detailed discussion of the methodology which was used to define downcomer lateral loads for the Mark II containments. This methodology is excessively conservative for quantifying the lateral loads on the SRVDL sleeve due to chugging through the annular opening between the SRVDL and the SRVDL sleeve. These loads have been quantified using data from tests conducted by Kraftwerk Union (KWU) for General Electric and are discussed in Reference 8. The tests were performed in a large-scale vessel. Full-scale (600 mm) and half-scale (300 mm) vents with single- and multiple-vent configurations were used to investigate the influence of the following parameters:

- Pool temperature
- Mass flux
- Number of vents
- Type of vent bracing
- Transverse flow on the vessel
- Steam air-content

The multiple vents were connected by instrumented struts. The strut loads were recorded and evaluated statistically for individual tests. The results of these tests lead to the following conclusions:

1. The strut forces increase with water temperature below 80°C; the maximum forces occurred between 65 and 80°C.

2. The mass flux influence on strut forces depends on water temperature range.

3. The forces introduced into the struts by the individual vents are randomly distributed with respect to direction, magnitude and time.

4. When air was added to the steam, the strut forces were greatly reduced.

5. Transverse flow in the water pool had a dampening influence on strut forces and a 50% reduction was observed on some struts.

6. The vent diameter had an effect on the strut forces; the larger diameter having the higher forces. The effect is less than the ratio of the vent areas.

MP&L selected the test which showed the highest mean value for the lateral load (Test 7) to define the lateral load on the SRVDL sleeve. This was a single vent test with a mass flux of 3.28 lbm/ft^2-sec (the mass flux which produced the maximum load). A single vent test was used to define the load since the 20 SRVDL-sleeve vents are widely separated from each other in the drywell wall.

The mean load for events observed in this test was 2.5 kip. Test results showed that 97.2% of events from this test had a magnitude of less than 6.75 kips. This value, including dynamic effects, is well within the identified margin of 11 kips discussed under Item 3 of this action plan.

References:

1. A.M. Varzaly, et al, "Mark III Confirmatory Test Program -Test Series 6003," NEDE-24720P, January, 1980

2. GESSAR II, Question/Response 3B.11, 22A7000, rev 2, 1981

3. W.J. Bilanin, "The General Electric Mark III Pressure Suppression Containment System Analytical Model:, General Electric Report NEDO-20533, June 1974, and Supplement 1, September 1975

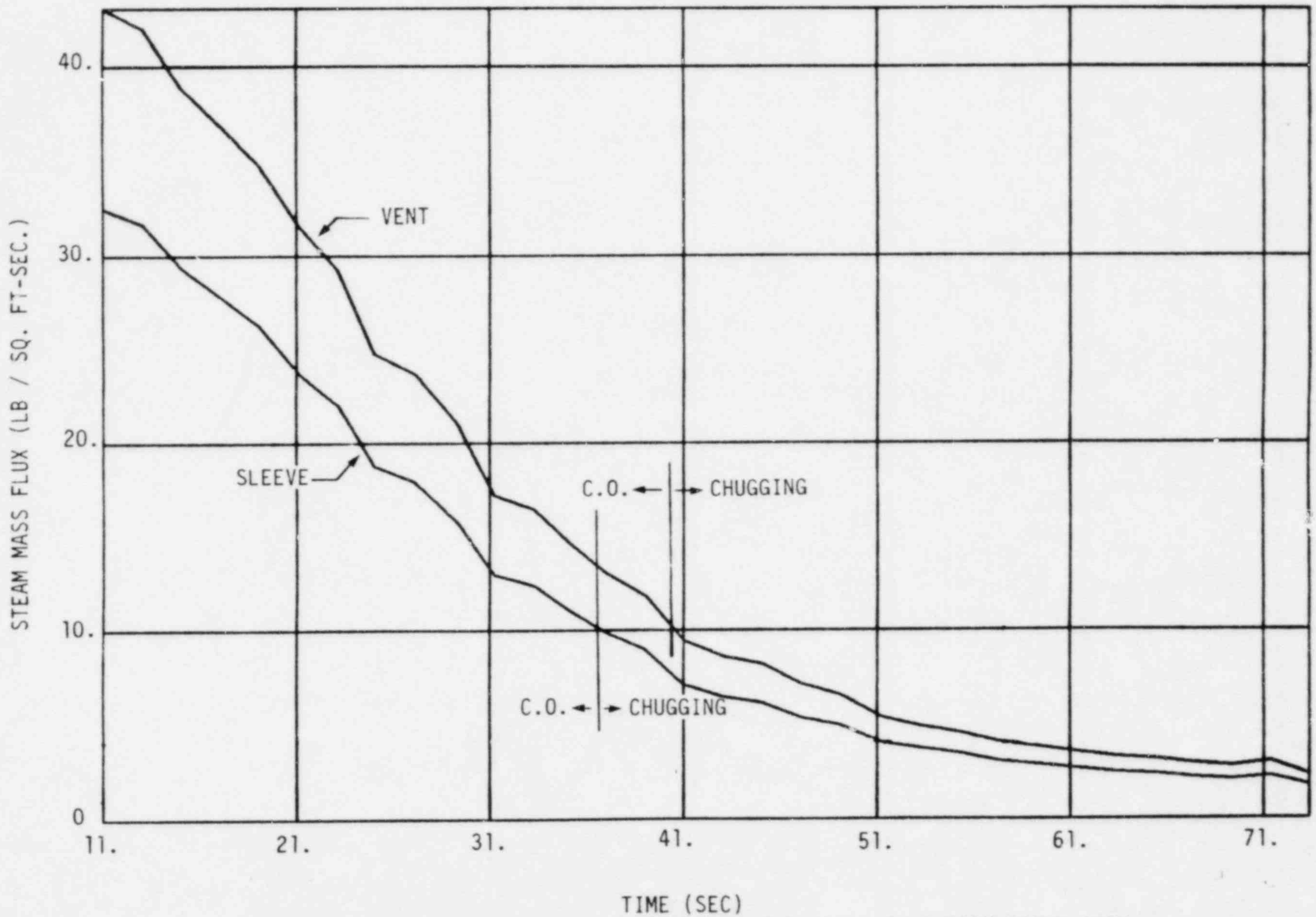
4. General Electric Company "Comparison of Single and Multivent Chugging at Two Scales," NEDE-24781-I-P, January 1980

5. A.M. Varzaly, et al, "Mark III Confirmatory Test Program -1/$\sqrt{3}$ scale Condensation and Stratification Phenomena - Test Series 5807," General Electric Report NEDE-21596-P, March 1977

6. GESSAR II, Figure 3B.18.2 page 3BO.3.2.18, 22A7000, June 1981

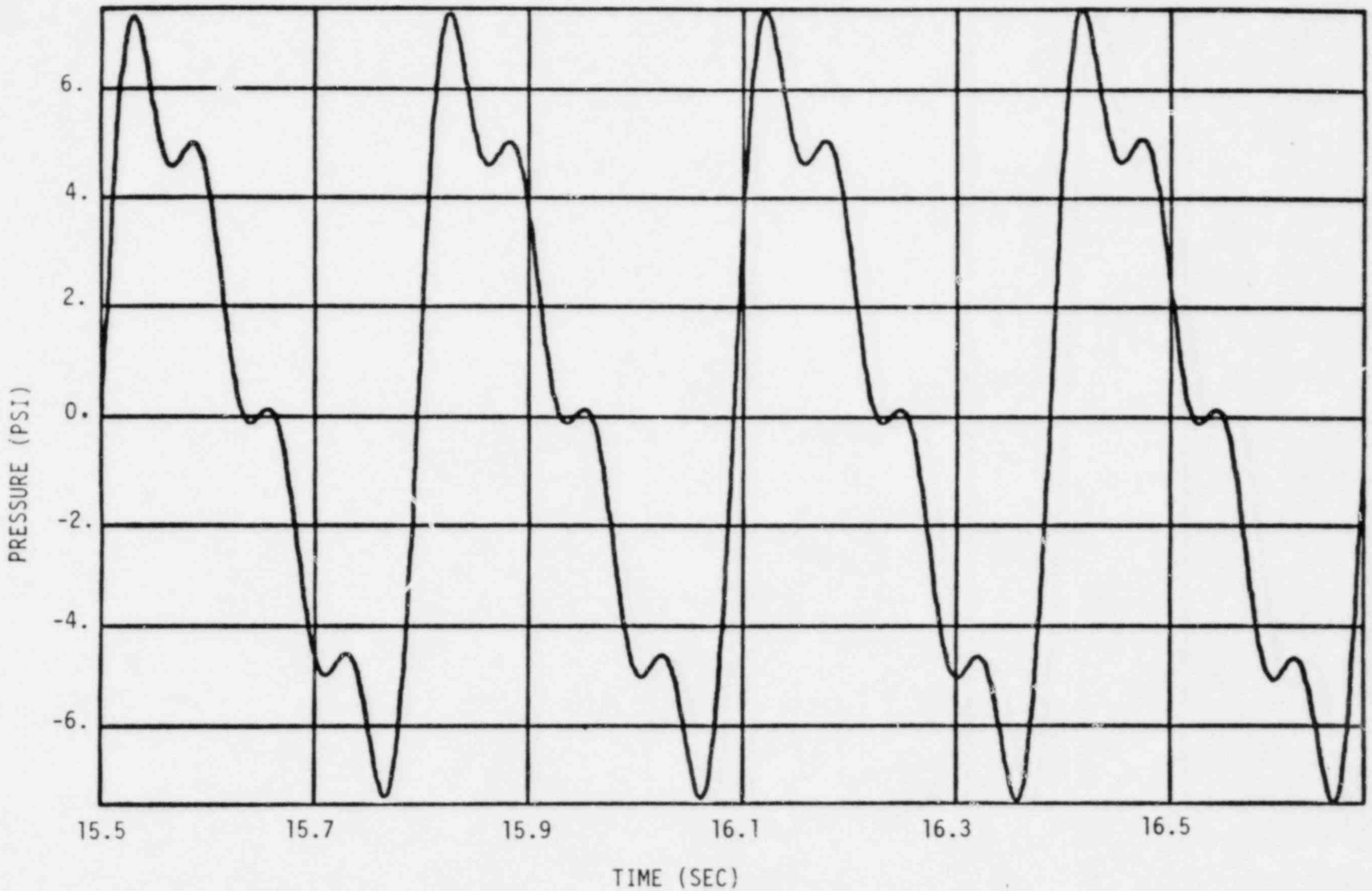
7. "Dynamic Lateral Loads on a Main Vent Downcomer - Mark II Containment," NEDE-24106-P, General Electric Company, March 1978

8. S.T. Nomanbhoy, "Loads on the Vent Struts Due to Condensation of Steam in a Water Pool," NEDE-23627-P, General Electric Company, June 1977



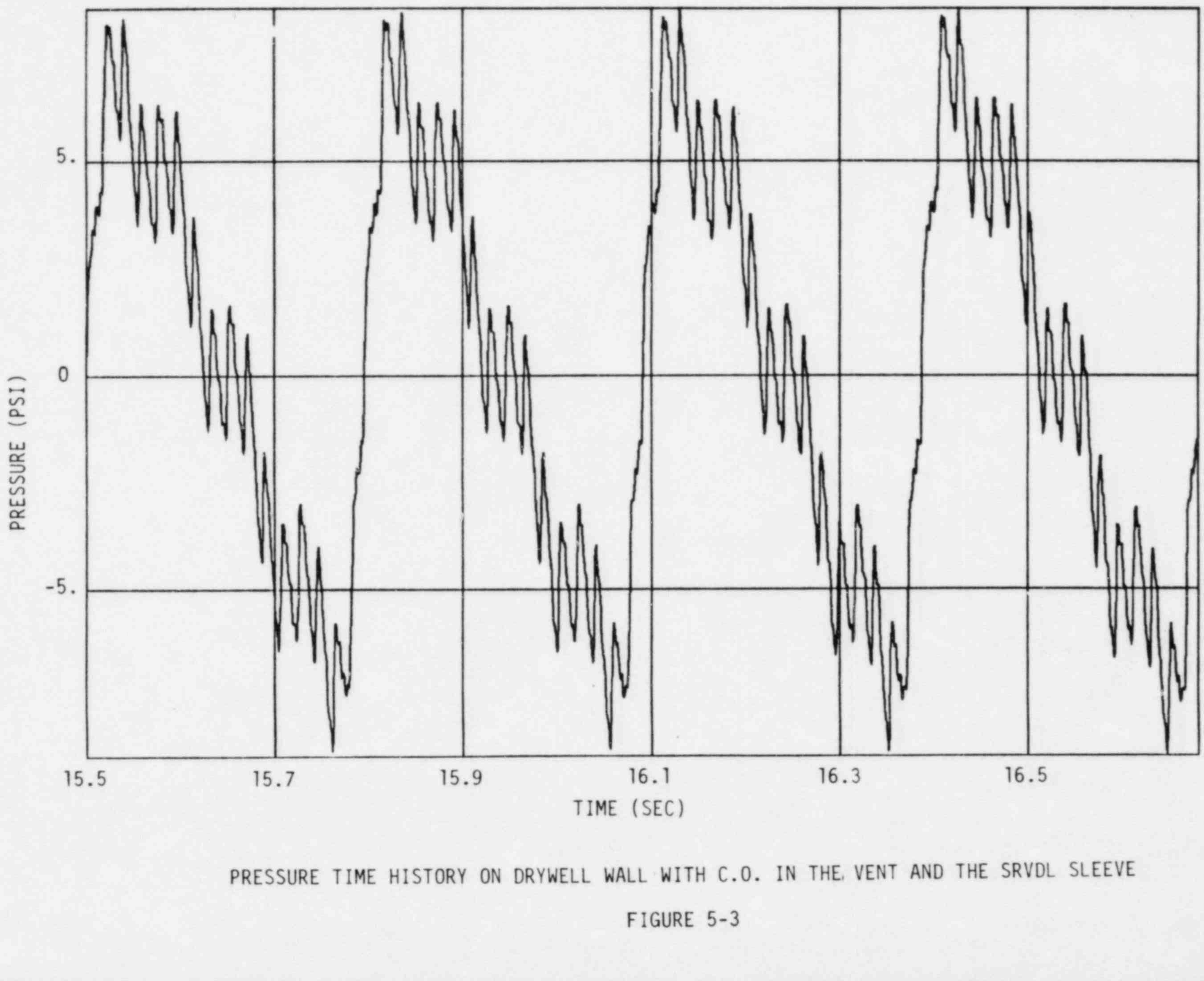
CALCULATED VENT AND SLEEVE STEAM MASS FLUX FOR GRAND GULF 100% BREAK SIZE

FIGURE 5-1



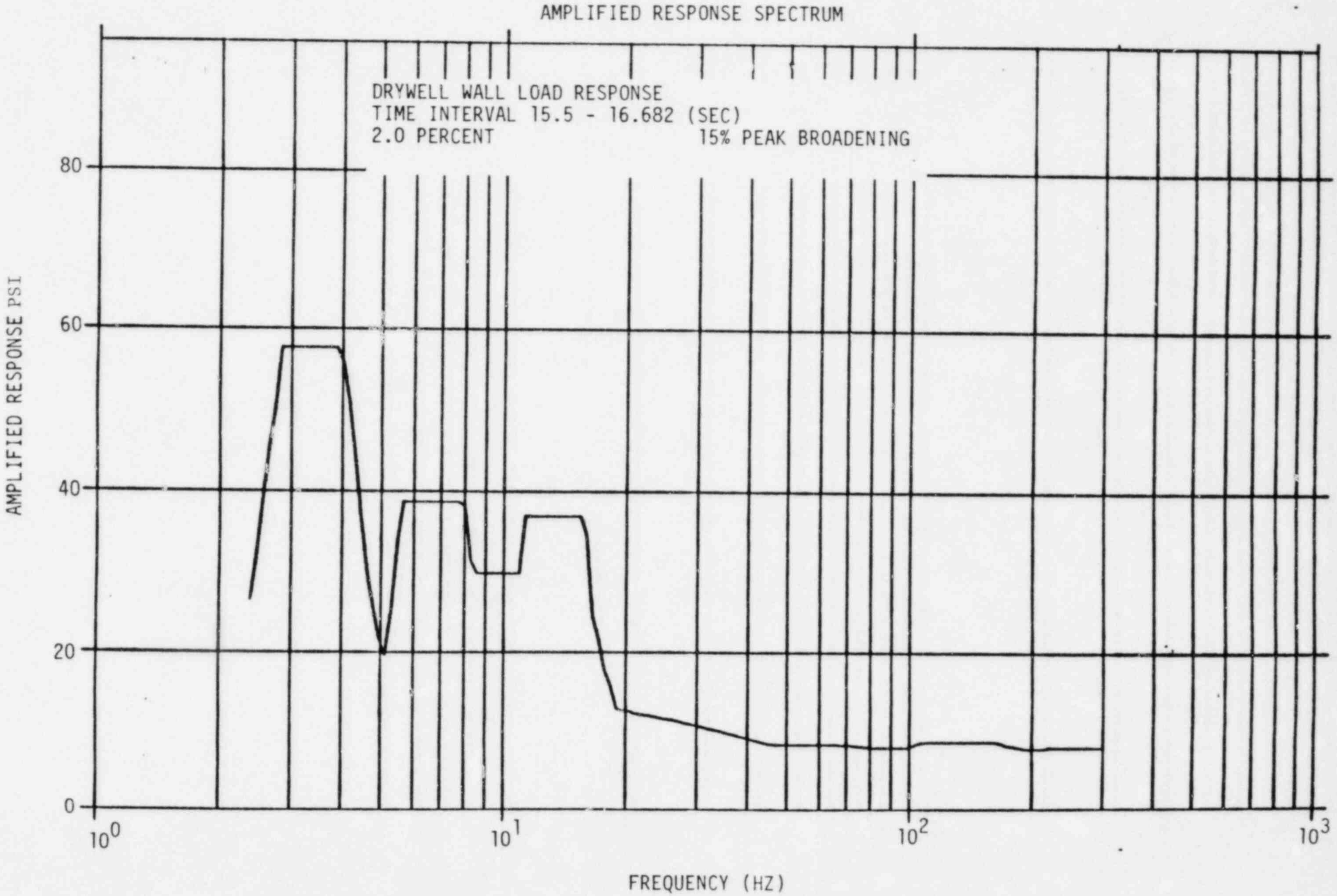
PRESSURE TIME HISTORY WITH C.O. IN THE VENT ONLY

FIGURE 5-2



PRESSURE TIME HISTORY ON DRYWELL WALL WITH C.O. IN THE VENT AND THE SRVDL SLEEVE

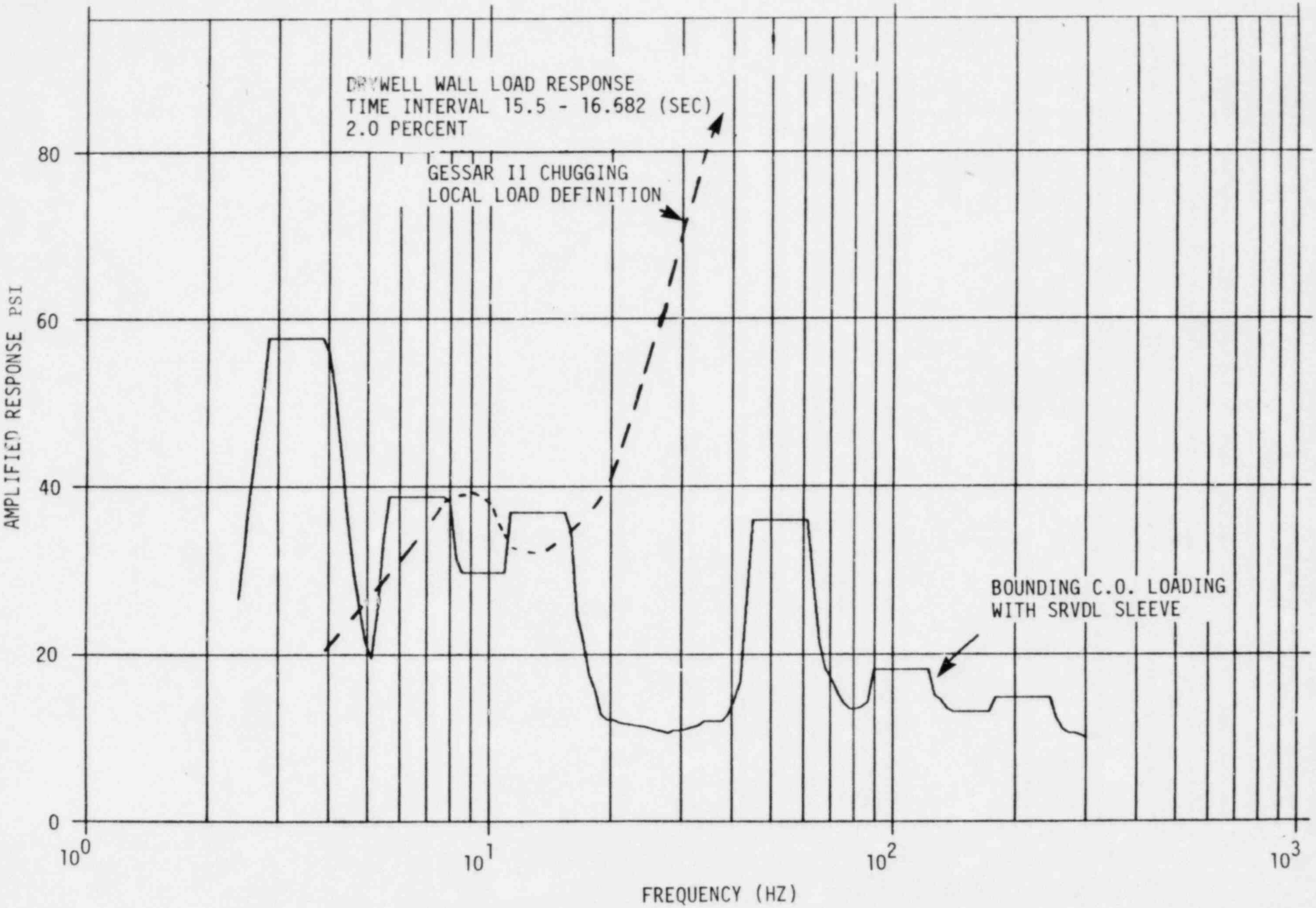
FIGURE 5-3



ARS OF PRESSURE TIME HISTORY WITH C.O. IN THE VENT ONLY

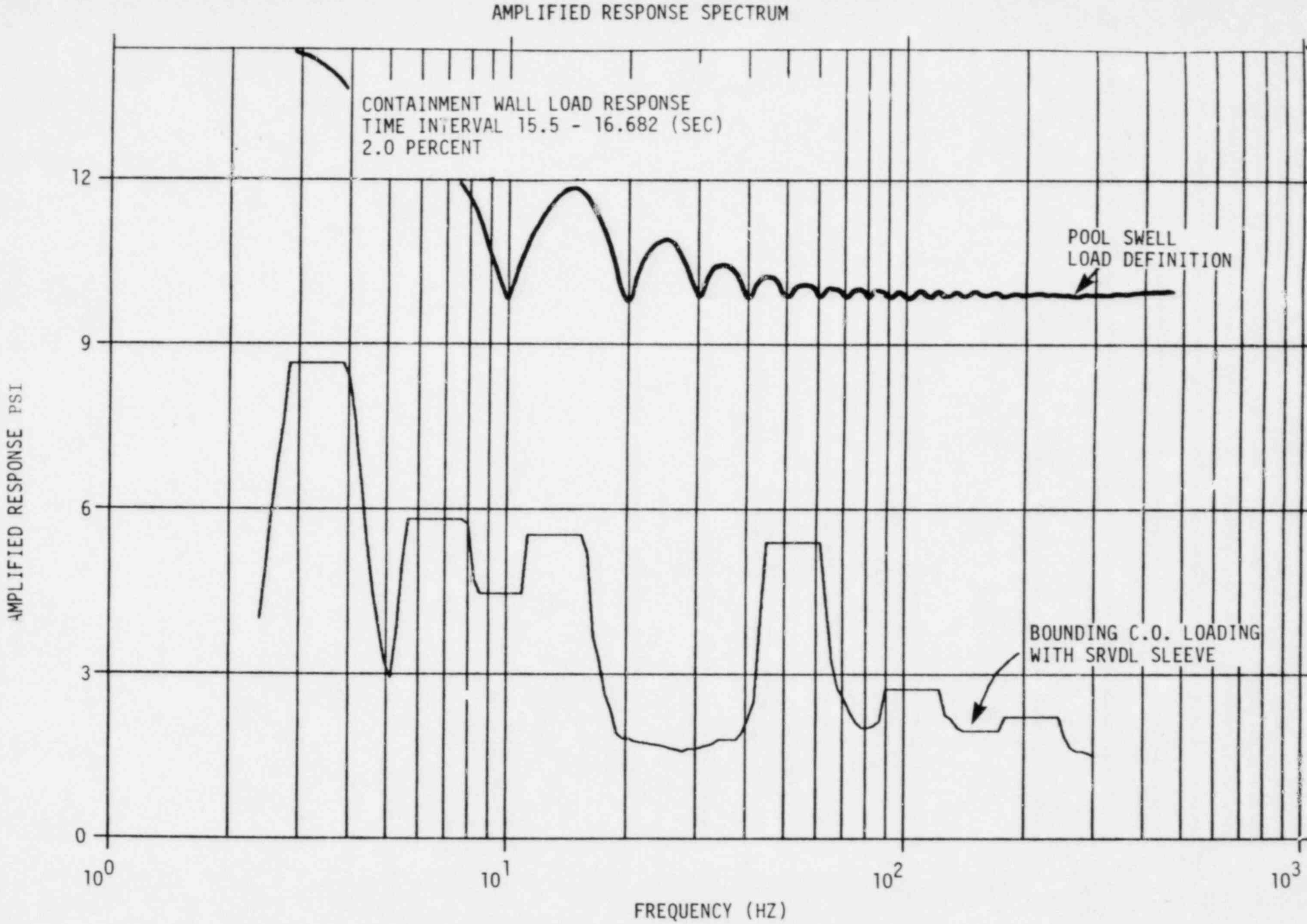
FIGURE 5-4

AMPLIFIED RESPONSE SPECTRUM



COMPARISON OF GESSAR II LOCAL CHUGGING LOAD DEFINITION AND
EXPECTED C.O. LOADING WITH SRVDL SLEEVE ON DRYWELL WALL

FIGURE 5-5



COMPARISON OF POOL SWELL LOAD DEFINITION AND EXPECTED C.O.
LOADING WITH SRVDL SLEEVE ON CONTAINMENT WALL

FIGURE 5-6

Action Plan 6

I. Issues Addressed

3.1 The design of the STRIDE plant did not consider vent clearing, condensation oscillation and chugging loads which might be produced by the actuation of the RHR heat exchanger relief valves.

3.7 The concerns related to the RHR heat exchanger relief valve discharge lines should also be addressed for all other relief lines that exhaust into the pool.

II. Program for Resolution

1. The vent which pressurizes the relief valve discharge line in the steam condensing mode will clear the water out of this line. Calculations will be submitted to demonstrate that there will not be a water leg in the discharge piping when the RHR system is in the steam condensing mode.

The following information will be submitted for all relief valves which discharge to the suppression pool:

2. Isometric drawings and P&IDs showing line and vacuum breaker location will be provided. This information will include the following: The geometry (diameter, routing, height above the suppression pool, etc.) of the pipe line from immediately downstream of the relief valve up to the line exit. The maximum and minimum expected submergence of the discharge line exit below the pool surface will be included. Also, any lines equipped with load mitigating devices (e.g., spargers, quenchers) will be noted.

3. The range of flow rates and character of fluid (i.e., air, water, steam) which is discharged through the line and the plant conditions (e.g., pool temperatures) when discharges occur will be defined.

4. The sizing and performance characteristics (including make, model, size, opening characteristics and flow characteristics) of any vacuum breakers provided for relief valve discharge lines will be noted.

5. The potential for oscillatory operation of the relief valves in any given discharge line will be discussed.

6. The potential for failure of any relief valve to reseat following initial or subsequent opening will be evaluated.

7. The location of all components and piping in the vicinity of the discharge line exit and the design bases will be provided.

III. Schedule

Item 1 was completed and submitted in AECM-82/497. Items 2-6 were completed and submitted in AECM-82/353. Item 7 is complete and included in this submittal. Additional discussion regarding Item 5 is also included in this submittal.

Item 5

A discussion of the potential for oscillatory operation of the RHR heat exchanger relief valve was provided in AECM-82/353. This discussion addressed oscillatory operation as a result of failures in the RHR pressure controller.

The relief valve will, in fact, cycle as a result of the differences between the maximum flow through the failed open pressure controller and the relief valve. The relief valve capacity is 310,000 lbm/hr and the maximum flow through the pressure control valve is 266,000 lbm/hr. Following failure of the pressure controller, heat exchanger and piping pressure will increase to the 500 psig set point for the relief valve. The flow from the relief valve will depressurize the heat exchanger and piping system to 450 psig at which point the relief valve recloses. The cycle time between relief valve actuations ranges between 2 and 5 seconds depending upon the water level assumed to be present in the heat exchanger.

Item 7

The locations for all components and piping in the vicinity of the RHR heat exchanger relief valve discharge were shown in the drawings included in Attachment 4 to AECM 82/353. The design basis for all piping and components located in the suppression pool is specified by the loading combinations listed in FSAR Table 3.9-17. The load combinations for design basis accident (DBA), small break accidents (SBA) intermediate break accidents (IBA) and safety relief valve (SRV) events contained in FSAR Table 3.9-17 include the submerged structure loads, structural response loads and other normal and accident loads. The loads produced by the RHR heat exchanger relief valves have been quantified in the results from Action Plan 8, Item 3. The submerged structure load caused by actuation of other ECCS relief valves are either enveloped by other events such as SBA, SRV, etc., or the loads are insignificant when compared to the existing design loads.

Action Plan 8

I. Issues Addressed

3.4 The RHR heat exchanger relief valve discharge lines are provided with vacuum breakers to prevent negative pressure in the lines when discharging steam is condensed in the pool. If the valves experience repeated actuation, the vacuum breaker sizing may not be adequate to prevent drawing slugs of water back through the discharge piping. These slugs of water may apply impact loads to the relief valve or be discharged back into the pool at the next relief valve actuation and apply impact loads to submerged structures.

3.5 The RHR relief valves must be capable of correctly functioning following an upper pool dump which may increase the suppression pool level as much as five feet creating higher back pressures on the relief valves.

II. Program for Resolution

1. A failure mode analysis on the pressure controller to establish all possible failure modes will be performed.

2. Analyses will be performed to demonstrate that water is not drawn into the relief valve discharge lines due to vacuum breaker performance. These analyses will show that the vent line which pressurizes the relief valve discharge line is adequate to maintain pressure in this line following relief valve closure.

3. An analysis will be completed to establish the force transients, water jet and air clearing loads which will be created by a first actuation of the relief valves in the steam condensing mode. The analyses will be repeated for a postulated relief valve failure in the shutdown cooling mode.

4. Additional calculations will be submitted to show that the vent line pressurizing the relief valve discharge line will eliminate potential water leg in the discharge piping even at higher pool levels corresponding to upper pool dump.

5. Analyses demonstrating that the heat exchangers are capable of withstanding an overpressure transient will be completed.

III. Schedule

Item 1 was completed and included in AECM-82/353. Items 2, 4, and 5 were completed and included in AECM-82/497. Item 3 is completed and included in this submittal.

IV. Final Program Results

The loads calculated as a result of actuating the RHR heat exchanger relief valves do not produce stresses in the piping and submerged structures which exceed the upset allowable stresses for the two operational modes examined. The first mode examined involves the RHR heat exchanger in the steam condensing mode, suppression pool at post upper pool dump levels (El. 116'-11") and the reactor vessel at design pressure (1165 psig). The second mode examined involves the RHR system in the suppression pool cooling mode, pool level at normal high water level (El. 111'10") and the heat exchangers pressurized to 210 psig.

For the first mode, load combinations evaluated include normal loads, OBE, all SRV structural response and SRV quencher air clearing loads. The water jet and air clearing loads for the RHR relief valve are negligible for this case since the RHR heat exchanger non-condensible vent pressurizes the relief valve discharge line before the relief valve can actuate.

For the second mode, load combinations include: normal, OBE, and RHR relief valve water jet or air clearing loads. No further evaluation of load combinations for the shutdown cooling mode are warranted since no failure mechanism can reasonably be postulated which involves actuation of a simple spring relief valve at a pressure of 140 psig below its set point.

As noted above, the resultant stresses were compared to upset allowable stresses This demonstrates the relatively inconsequental magnitude of these loads since they are within upset allowables even though evaluation against faulted stress allowables would be acceptable.

Action Plan 9

I. Issues Addressed

3.6 If the RHR heat exchanger relief valves discharge steam to the upper levels of the suppression pool following a design basis accident, they will significantly aggravate suppression pool temperature stratification.

II. Program for Resolution

1. The maximum quantity of energy which can be added to the suppression pool will be quantified. This will be based upon operator action to terminate relief valve discharge following discovery by the operator that the relief valve has actuated. This will include an evaluation of all scenarios which could lead to discharge from these relief valves.

2. The discharge plume from the relief valves will be investigated. This plume will establish the maximum area of the pool which can be affected.

III. Schedule

Item 1 was completed and included in AECM-82/497. Item 2 is complete and included in this submittal.

IV. Final Program Results

A suppression pool mixing and stratification model has been developed to provide a conservative estimate of the suppression pool thermal response to continuous discharge of steam through the residual heat removal system heat exchanger relief valve. The worst case event evaluated with this model postulates a failure of the pressure control during steam condensing mode startup such that none of the steam flowing to the heat exchanger is condensed. A continuous discharge of 266,000 lbm/hr steam has been evaluated which corresponds to the maximum choked flow which can be passed through the failed open pressure control valve.

As discussed in the additional information submitted for Action Plan 6, Item 5, the flow from the fully open relief valve of 310,000 lbm/hr exceeds the flow from the pressure control valve. Consequently, following actuation of the relief valve, the RHR heat exchanger and piping system will be depressurized below the pressure at which the relief valve closes. After the relief valve closes, the system will repressurize to the set point of the relief valve which will cause the valve to reopen. MP&L's consideration of discharge from the relief valve as continuous is therefore very conservative. The on/off charging of the pool will produce more mixing than would be accomplished by a steady uniform jet.

The length of the steam plume below the discharge line exit is slightly more than three feet based upon the known mass flux of the jet and the methodology contained in Reference 1. The steam jet momentum calculated for the given conditions is 1.35 x 105 lbm ft/sec2. For a hot water jet with the same momentum, the following properties can be calculated from Reference 2 assuming a turbulent jet discharging into an infinite pool. The properties below are calculated at a location of 1.75 feet above the bottom of the pool at the center of the discharge pipe exit.

Jet half width	=	approximately 1.02 ft
Centerline velocity	=	28.6 ft/sec
Total jet flow	=	14,156 lbm/sec

The postulated flow configuration is shown in Figure 9-1. Based upon the parameters calculated for an infinite pool, the appropriate parameters for the jet discharge in the finite suppression pool can be estimated.

A drawing showing the nodalization used for evaluating the discharge from the jet is shown in Figure 9-2. A jet plume node ("jet note") is located within a local sector of the pool defined as the mixing node. The entrainment into the jet is assumed to be the same as for a free field jet between the discharge pipe exit and 1.75 feet from the pool bottom. Flow which is entrained into the jet will come from and be returned to the mixing node. The only flow leaving the mixing node will be the inlet jet flow minus the amount the mixing node grows due to an increase in pool depth as a result of jet flow addition. Flow from the mixing node will enter a series of surface nodes which will stretch around the pool. Because of the symmetry of the pool, only half of the pool is modeled.

The mixing node consists of an 18° sector of the pool (which corresponds to a 36° sector in the full pool). The volume of the mixing node is approximately 12,554 ft3 in the pool or about 10% of the pool volume. Based upon the entrained flow for the circular jet in an infinite pool which was calculated above, the entire volume of the mixing node will be entrained and mixed through the jet plume every 56 seconds. The mixing node is assumed to be isolated from the rest of the pool and no flow is entrained by the jet from the remainder of the pool. This represents a significant conservatism in the analysis.

The model considers convective heat transfer from the mixing node to the wetwell air space and conductive heat transfer from the mixing node is assumed to pass into adjacent surface nodes so that the lower regions of the pool are effectively excluded from the analysis with the exception of conductive heat transfer.

The predictions for jet node exit temperature and mixing node temperature as functions of time are shown in Figure 9-3. The mixing node temperature represents the local pool temperature surrounding the jet discharge. During the initial part of the transient, the mixing node temperature rise is not quite linear but the rise is approximately 5.6° per minute. The mixing node temperature follows the jet node exit temperature within 5°F for the selected mixing node as 10% of the total pool volume.

Figure 9-4 shows the predicted pool surface temperature stratification profiles. The stratification profiles are only important in terms of their effect on the average pool surface temperature since the containment air space temperature will only be affected by the average pool surface temperature. The average pool surface temperature rise at the end of 12 minutes will be about 13°F. For a fixed volume and fixed air mass above the pool surface, this temperature rise could at most increase the wetwell air space pressure by about 2% which is clearly inconsequential.

The sensitivity of the predicted results to changes in key assumptions was evaluated as part of the study. Parameters investigated include volume of the mixing node, jet entrainment flow, surface node thickness and heat transfer from mixing and surface nodes. Table 9-1 summarizes the results of this sensitivity study.

As an example of how Table 9-1 should be interpreted, the model predicts a mixing volume temperature rise of about 70°F in 12 minutes. If the mixing volume were doubled, about a 50% decrease in the temperature rise could be expected, or about a 35°F rise over the same interval. Similarly, if we double the thickness of the surface nodes, the resulting temperature stratification profile would be reduced on average by about 20% near the mixing node and falling off more quickly moving away from the mixing node.

It is also interesting to observe that the jet entrainment flow has little effect on the mixing volume temperature. This results from the small mixing node volume and the high volumetric flow rate through the jet node. This sensitivity would change for a larger mixing node volume.

The most significant result from the model sensitivity studies is the confirmation that the size of the mixing node volume is the key parameter in the temperature predictions by the model. For the results shown in Figures 9-3, and 9-4, a mixing node volume was chosen such that the jet will mix the liquid inventory of the mixing node volume through the jet about once every minute. To demonstrate the effect of increasing this mixing volume size, Figure 9-5 shows the mixing node temperature history for various mixing node volume sizes. As this figure demonstrates, the mixing node temperature comes down rapidly as more of the pool becomes involved. It seems likely that as the RHR heat exchanger relief valve cycles open and closed, the turbulence produced by the starting and stopping of the steam jet will create sufficient mixing to involve a large sector of the pool. However, solving this complicated mixing problem would require a detailed nodal analysis of the transient jet in the pool.

The simplified model used in the present analysis is intended to demonstrate with suitably conservative model assumptions that, if the pressure control valve in the RHR heat exchangers system should fall, the operator would have serveral minutes to detect the overpressure in the RHR heat exchanger and take action before there would be sufficient pool heat-up to produce conditions in the pool which could lead to unstable condensation.

References

1. Stanford, L.E. and C.C. Webster, Energy Suppression and Fission Product Transport in Pressure Suppression Pools, ORNL-TM-3448, April 1972.

2. Schlichting, H., Boundary Layer Theory, 6th Ed. pp. 699-702, McGraw Hill Book Co., 1968.

Table 9-1

Model Input Sensitivity

Input Parameter	% Changed	% (T_{mix}-T_o)	% (T_n-T_o)
Mixing Node Volume	+10%	-10%	0.01%
Jet Entrainment Flow	±10%	~0%	0%
Surface Node Thickness	doubled	0%	-20% near mixing node increasing to a higher percentage far away
Heat Transfer From Mixing And Surface Nodes	set to Zero	~ 0%	0%

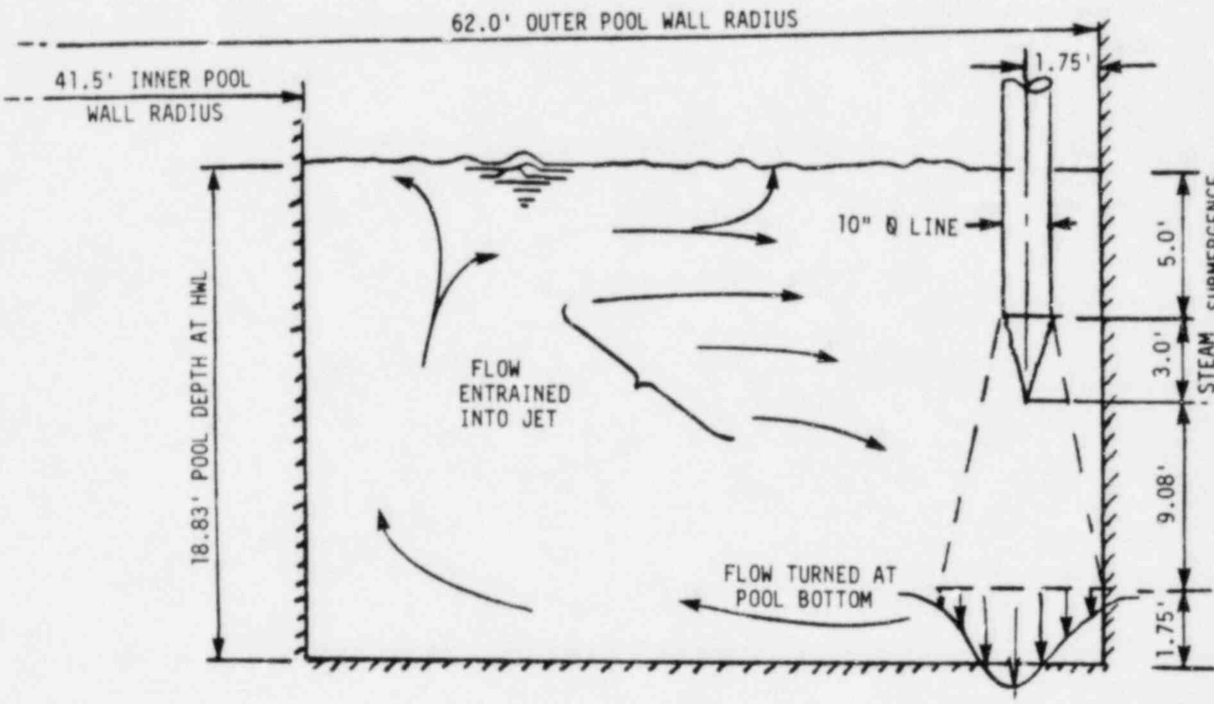


FIGURE 9-1
JET FROM RHR RELIEF VALVE DISCHARGE

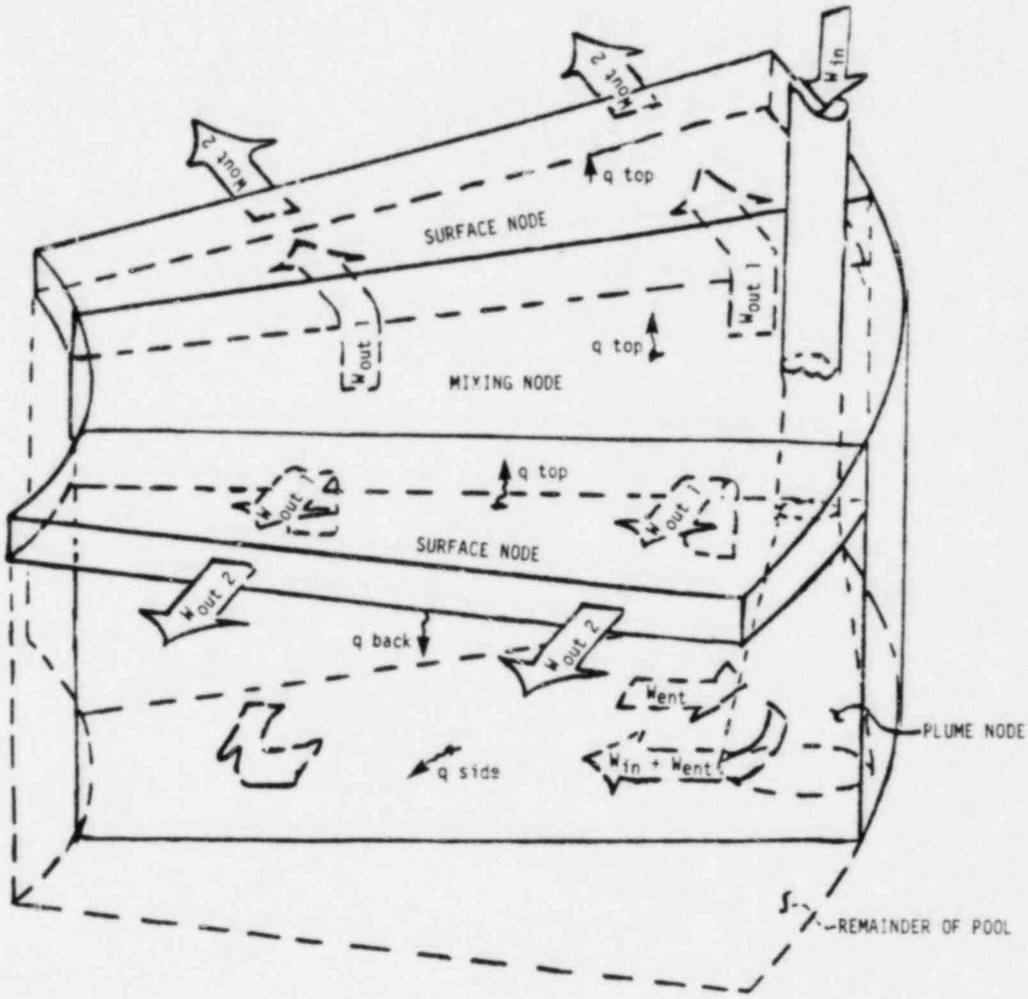


FIGURE 9-2
ISOMETRIC SKETCH OF JET DISCHARGE MODEL

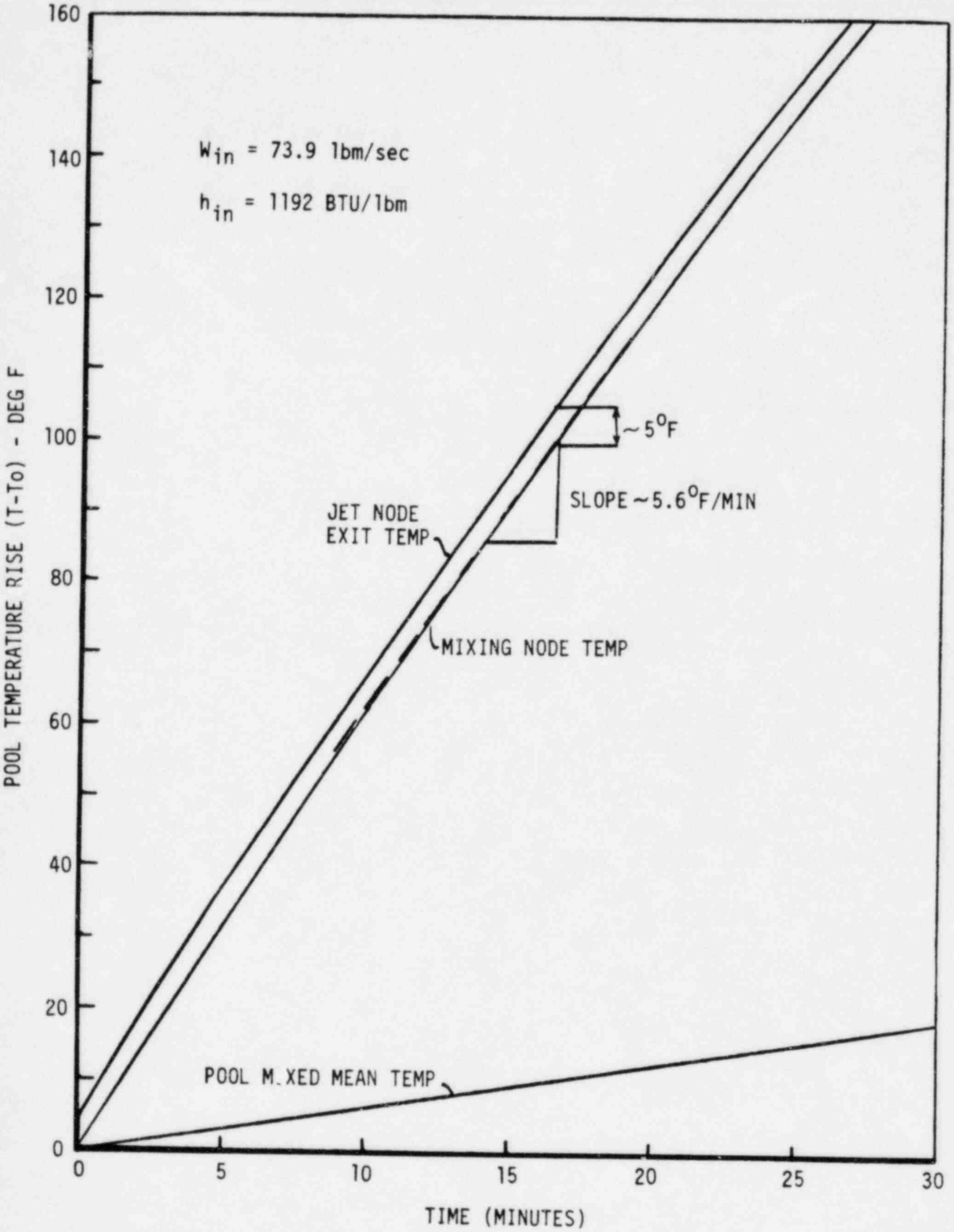


FIGURE 9-3
POOL HEAT-UP DUE TO RHR HEAT EXCHANGER DISCHARGE

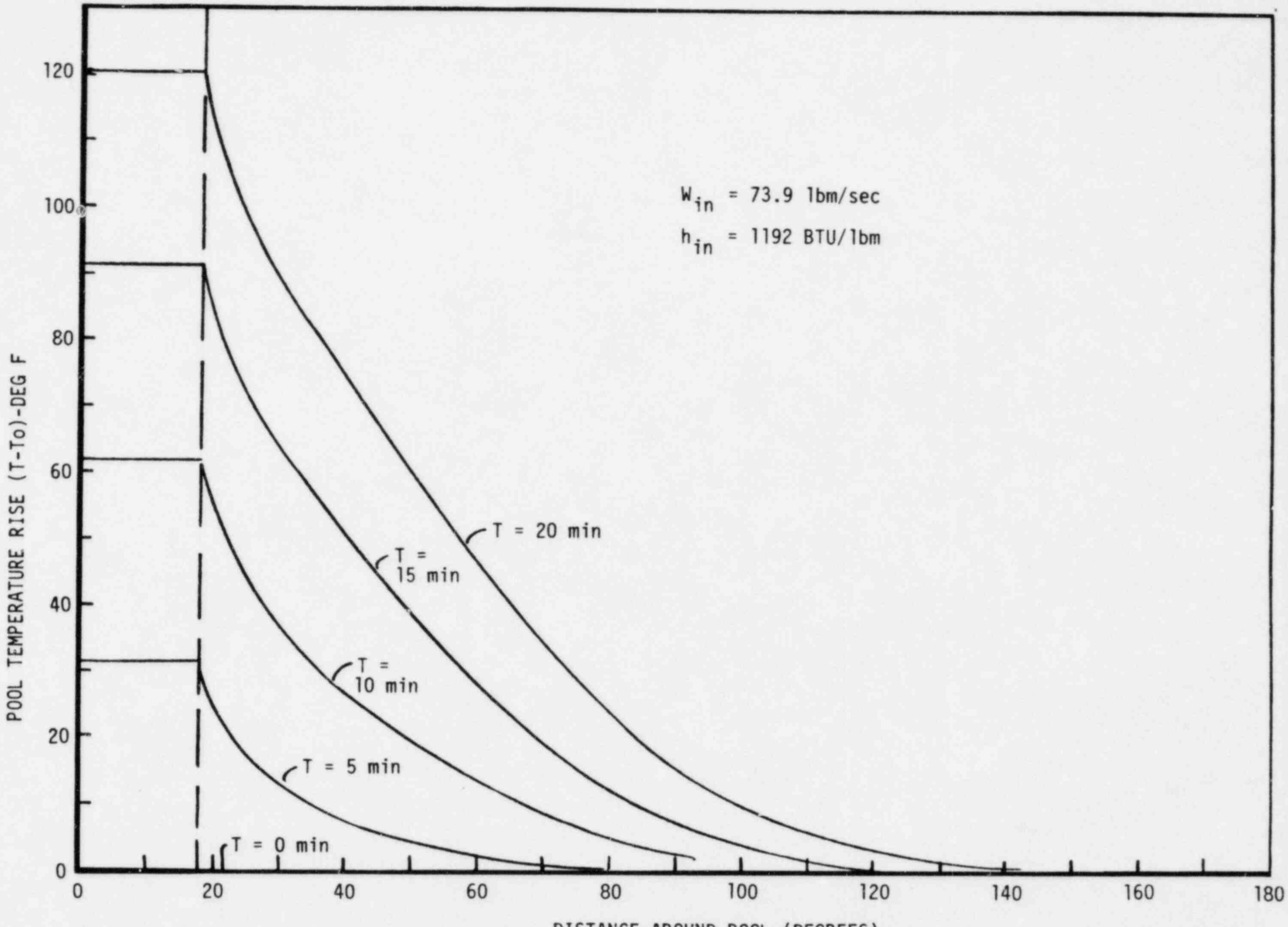


FIGURE 9-4
POOL STRATIFICATION PROFILES

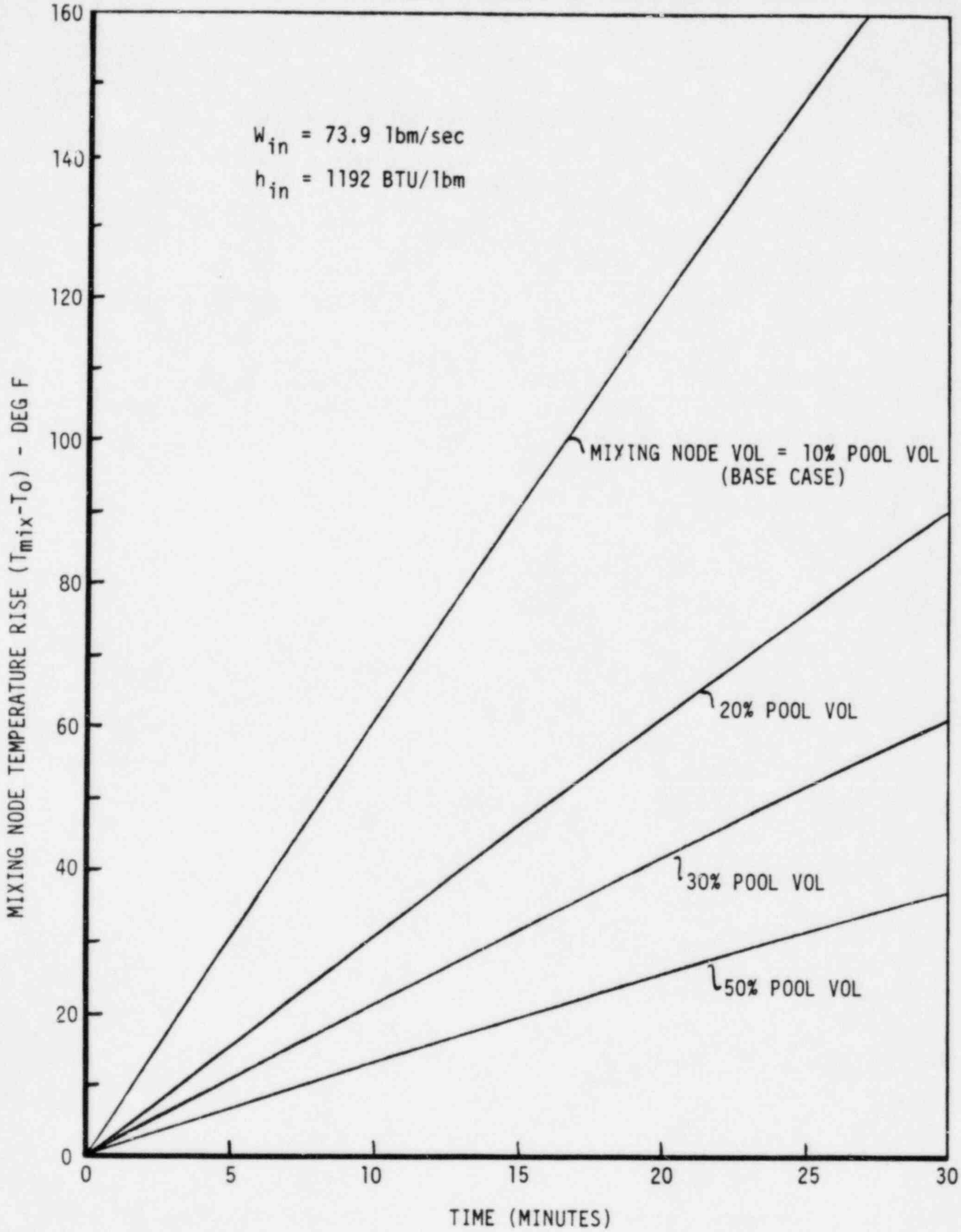


FIGURE 9-5
EFFECT OF MIXING NODE VOLUME ON LOCAL POOL TEMPERATURES

Action Plan 16

I. Issues Addressed

4.7 All analyses completed for the Mark III are generic in nature and do not consider plant specific interactions of the RHR suppression pool suction and discharge.

4.10 Justify that the current arrangement of the discharge and suction points of the pool cooling system maximizes pool mixing. (pp. 150-155 of 5/27/82 transcript)

II. Program for Resolution

1. An analysis will be provided to establish interaction effects.

III. Schedule

Item 1 is complete and included in this submittal.

IV. Final Program Results

A study has been performed using results from various RHR system effectiveness tests and analyses which have been completed. The results of this study are included in Attachment 3 of this submittal.

Action Plan 19

I. Issues Addressed

5.1 The worst case of drywell to containment bypass leakage has been established as a small break accident. An intermediate break accident will actually produce the most significant drywell to containment leakage prior to initiation of containment sprays.

5.6 The test pressure of 3 psig specified for the periodic operational drywell leakage rate tests does not reflect additional pressurization in the drywell which will result from upper pool dump. This pressure also does not reflect additional drywell pressurization resulting from throttling of the ECCS to maintain vessel level which is required by the current EPGs.

9.2 The continuous steaming produced by throttling the ECCS flow will cause increased direct leakage from the drywell to the containment. This could result in increased containment pressures.

II. Program for Resolution

1. A sensitivity study with varying break sizes will be completed neglecting depressurization of the drywell prior to initiation of containment sprays, but including the effects of containment heat sinks.

2. Analyses which will be completed will show that the allowable leakage of $A/\sqrt{K}$ equal to 0.9 is valid for Grand Gulf.

3. An evaluation of the need for reducing the allowable technical specification limiting conditions for drywell leakage will be provided. Any revised limit would be based upon a pressure of 6 psig in the drywell which would reflect the additional pressure produced by upper pool dump. In the evaluation, credit will be taken for drywell and containment heat sinks.

III. Schedule

Items 1 and 2 were complete and included in AECM-82/497. Item 3 is complete and included in this submittal.

IV. Final Program Results

The worst cast scenario for drywell bypass leakage was identified in the results from item 1 of this action plan. This scenario entails full capability bypass leakage of $A/\sqrt{K} = 0.9\ ft^2$, no depressurization of the drywell as a result of ECCS spillover from the break and a conservative treatment of the drywell and containment heat sinks. The containment sprays will actuate at 13 minutes into the accident and produce a decrease in containment pressure. At 30 minutes into the accident, the upper pool will dump to the suppression pool which will raise the pressure in the drywell due to the increased static pressure above the horizontal vents.

The upper pool dump will increase the bypass leakage which in turn will cause the containment pressure to begin to rise. The GGNS emergency operating procedures direct the operator to depressurize the reactor pressure vessel if containment pressure cannot be controlled. When the vessel is depressurized, the driving pressure causing the bypass leakage will be eliminated. This will eliminate the bypass leakage and permit the containment sprays to provide effective containment pressure control. Since the existing combination of engineered safeguard features and emergency operating procedures is adequate to accomodate full capability bypass leakage, MP&L has concluded that no changes to the GGNS technical specification leakage limits is warranted.

Action Plan 22

I. Issues Addressed

5.8 The possibility of high temperatures in the drywell without reaching the 2 psig high pressure scram level because of bypass leakage through the drywell wall should be addressed.

II. Program for Resolution

1. A new analysis will be performed using the capability bypass leakage. This analysis will show that a temperature of 330°F is not reached in the drywell until after ten minutes. In this interval, the operator will have received sufficient information to manually scram the reactor.

2. A detailed list of alarms and parameter displays will be developed which inform the operator of conditions in the drywell. This will include drywell cooling performance, temperature, airflows, leak detection, etc.

III. Schedule

Item 2 was completed and included in AECM-82/353. Item 1 is complete and included in this submittal.

IV. Final Program Results

An analysis has been completed to determine the maximum temperature increase in the drywell which can occur in a ten minute interval without reaching a drywell pressure of 2 psig which would cause a scram. The ten minute interval was selected as a conservatively long time period for the plant operator to determine that an accident has occurred in the drywell and execute appropriate actions such as manually scramming the reactor and depressurizing the vessel if necessary.

The analysis assumed a break in the reactor coolant pressure boundary piping, presence of normal drywell heat sources, total loss of drywell fan coolers and the full drywell to containment bypass leakage capability of 0.9 ft^2 $A/\sqrt{K}$. Credit was taken for the presence of drywell heat sinks. A spectrum of break sizes was evaluated to determine a limiting case which was established as .007 ft^2. The initial drywell temperature was assumed to be 135°F which is the technical specification limitation on drywell temperature.

The peak drywell temperature predicted by the analysis ten minutes after the occurrence of the postulated break is 246°F. This represents a 111°F increase from the assumed initial temperature of 135°F and the maximum temperature is well below the 330°F temperature limit.

The drywell heat sources used in the analysis were the Perry Nuclear Station heat sources. The drywell heat loads for GGNS are estimated to be no greater than 30% more than the heat sources for Perry. A 30% increase in heat sources would not increase the drywell temperature by 30%. However, even assuming a 30% increase in drywell temperature for GGNS, the temperature is still well below the 330° limit.

Action Plan 25

I. Issues Addressed

8.1 This issue is based on consideration that some technical specifications allow operation at parameter values that differ from the values used in assumptions for FSAR transient analyses. Normally analyses are done assuming a nominal containment pressure equal to ambient (0 psig) a temperature near maximum operating (90°F) and do not limit the drywell pressure equal to the containment pressure. The technical specifications permit operation under conditions such as a positive containment pressure (1.5 psig), temperatures less than maximum (60 or 70°F) and drywell pressure can be negative with respect to the containment (-0.5 psid). All of these differences would result in transient response different than the FSAR descriptions.

II. Program for Resolution

1. A detailed summary of all conservatisms which currently exist in the containment response analyses which are part of the FSAR will be provided. Conservatisms in the suppression pool temperature analysis will be identified in Action Plan 12.

2. MP&L will complete an end point analysis to demonstrate that with all initial containment parameters at worst case values, the containment design pressure is still not significantly exceeded.

3. Perform an analysis with worst case values taking credit for realistic temperature differences between containment and suppression pool and the containment heat sinks.

4. A complete review of the technical specifications for containment conditions versus accident analysis assumptions will be made. A comparison of technical specification values and values used as initial assumptions in the accident analysis will be submitted.

III. Schedule

Item 1 was completed and included in AECM-82/353 dated August 19, 1982. Items 2-4 are complete and included in this submittal.

IV. Final Program Results

Item 2

An end point analysis has been completed to establish the effect on peak containment pressure of varying assumed initial conditions for drywell temperature and drywell and containment pressure.

The cumulative effect of assuming worst case values for these parameters was then calculated. The initial relative humidity in both the drywell and containment was assumed to be zero percent in order to maximize initial air mass. The vapor pressure from spray water discharge at the peak calculated suppression pool temperature including feedwater addition (180°F) was added to the calculated end point pressure. The air was assumed to be completely dry for this study. The study is based upon use of the perfect gas law for a constant volume system with a final containment airspace temperature of 185°F.

Assumed initial pressure variations in the drywell were based upon technical specification limits on drywell to containment differential pressure of -0.15 psi and containment pressure of +1.0 psi. The minimum expected drywell temperature of 105°F was established on the basis of the minimum temperature for service water supplied to the drywell fan coolers.

The results from the GGNS study are shown in Table 25-1. Table 25-1 shows the individual effects of parameters changed and that the worst case containment pressure based on the cumulative effect of these initial condition changes is 14.8 psig. The containment design pressure is therefore not exceeded.

The completed end point analysis was extremely conservative. Table 12-1 submitted in AECM-82/353 demonstrated that the predicted containment airspace temperature may be conservative by as much as 48°F. If the 48°F margin is applied to the actual peak calculated long term containment temperature (180°F) response the actual final containment temperature may be as much as 61.7°F lower than the final containment temperature used in this sensitivity analysis. The peak long term containment temperature calculated for the GGNS LOCA is calculated to be 180°F as shown in the response to NRC question 021.9 in the GGNS FSAR. This is well below the 185°F temperature used in this analysis. In addition, MP&L has used the vapor pressure of sprays which are at the calculated peak suppression pool temperature (180°F). In fact, these sprays will first be cooled by the RHR heat exchangers which will substantially reduce the vapor pressure and the air temperature in the containment. Based upon the results of this extremely conservative end point analysis and the margins identified in MP&L's analysis, MP&L concludes that the existing analyses retain adequate margin to accommodate variations in assumed initial conditions.

Item 3

A more realistic analysis was completed using General Electric Company's long term containment response code, SHEX. This code was described in detail in Attachment 2 of AECM-82/353. The analysis was completed to quantify the conservatisms inherent in neglecting finite heat transfer between the suppression pool and the wetwell air space and in neglecting containment heat sinks. This analysis also used the combined effects of minimum drywell temperature (105°F) technical specification limit on drywell to containment differential pressure (-0.15 psi) and technical specification limits on initial containment pressure (+1.0 psi). Assumptions regarding heat sinks and heat transfer between the suppression pool and the wetwell airspace are identical to the assumptions used in the analysis completed for Action Plan 12 in AECM-82/353.

The resulting peak containment airspace pressure obtained from this analysis is 4.3 psi less than the peak pressure predicted with the GGNS FSAR assumptions.

Item 4

MP&L has completed a review of all assumptions for completion of accident and transient analyses identified in the GGNS FSAR against technical specifications which control plant operations. Assumptions made in the FSAR accident analyses regarding operator actions were compared with the GGNS emergency operating procedures (EOPs) and the off normal event procedures (ONEPs) to assure that actions specified in the accident analyses are adequately reflected in the EOPs. Discrepancies which were noted in this review and the appropriate resolutions for the discrepancies are discussed below.

1. FSAR Table 6.2-3 states that the MISV closure time assumed in the accident analysis is 5.5 seconds. The technical specifications in Section 3/4 4.7 require MSIV closure time to be between 3.0 and 5.0 seconds.

   Resolution

   The FSAR containment accident analysis is not strongly dependent on MSIV closure time. This FSAR accident analysis assumption is conservative for ECCS analyses, since the longer closure time leads to a greater quantity of energy released and a greater depletion of reactor coolant inventory.

2. FSAR Table 6.2-4 specifies a design thermal power of 3995 Mwt. FSAR Table 15.0-2 and Table b 2.3.1-1 of the technical specifications specify design thermal power as 3993 Mwt.

   Resolution

   A difference of 0.05% and thus insignificant.

3. FSAR Subsections 6.2.1.1.3.4.3, 6.2.1.1.3.4.5 and 6.2.1.1.4.1 all assume that relatively cool ECCS water will spray out the postulated break after the vessel is reflooded. Emergency procedure EP-1 Step 3.3 requires the operator to maintain vessel level between 10 and 52 inches (positive). Thus no ECCS water should flow out the postulated break.

Resolution

This is one of Mr. Humphrey's specific concerns regarding accident analysis assumptions. As such, MP&L has completed several additional analyses to demonstrate that this concern does not substantially alter existing FSAR analysis.

4. Subsection 6.2.1.1.4.1 of the FSAR utilizes a containment temperature of 80°F for the negative pressure analysis. The Technical Specification limit on containment temperature is 90°F per Section 3.6.1.8.

   Resolution

   The calculated negative pressure is less than -1.0 psi. The effect of an 11% change in initial containment temperature should not substantially change the margin between the peak negative pressure and the specified design value of -3.0 psi. In addition, MP&L has completed a new bounding negative pressure analysis as part of Action Plan 27.

5. The reactor is assumed to trip when the main steam isolation valves are 10% closed in the FSAR Subsections 15.1.3.3.2 and 15.2.4.3.2. The technical specifications, Table 2.2.1-1 #6 indicate reactor trip when the MSIVs are 6% closed.

   Resolution

   The FSAR analysis is conservative since the reactor scram occurs later in the calculated transient than it will if scram occured as required in the technical specifications.

6. The minimum critical power ratio used in the rod withdrawal error analyses discussed in FSAR Subsection 15.4.2.3.2 are not consistent with technical specification limits contained in Figures 3.2.3-1 and 3.2.3-2.

   Resolution

   FSAR Subsection 15.4.2 is being revised to eliminate this inconsistancy.

7. Table 15.4-2 in the FSAR lists the maximum linear heat generation rate (MLHGR) as 11.55 kw/ft. Technical specification 3.2.4 on Page 3/4 2-9 permits MLHGR up to 13.4 kw/ft.

   Resolution

   FSAR Table 15.4-2 is being revised to assure consistency between technical specification and the accident analyses assumptions.

8. The relief and safety valve set points which are identified in Table 15.0-2 of the FSAR are inconsistent with the set points identified in Section 3/4 4.2.1 of the technical specifications. The set points listed in the technical specifications are shown as ±1%. The following table summarizes technical specification (tech spec) set points, tech spec ±1% values, and accident analysis assumptions regarding set points with all numerical values in psig.

	Tech Spec	Tech Spec 1%	Accident Analysis
Safety	1165	1176.7	1175
Safety	1180	1191.8	1195
Safety	1190	1201.9	1215
Relief	1103	1114.0	1125
Relief	1113	1124.1	1135
Relief	1123	1134.2	1145

Resolution

The safety and relief valve set points for the transient analysis were set higher than the technical specification values to account for instrument uncertainties and to assure that results would be conservative.

If the lower safety and relief valve set points were used in the FSAR accident analysis the vessel peak pressure will be lowered. The heat flux and change in critical power ratio should not be altered significantly, and any change would involve reductions in peak values.

9. The level set points for ECCS activation and trip as identified in Table 15.0-2 and in Figure B 3/4-1 in the technical specifications are not identical. The discrepancies are summarized in the following table:

Setting	Accident Analysis (ft)	Tech Spec
L8	5.88	5.76
L4	4.03	4.03
L3	2.16	2.25
L2	-2.26	-2.17

Resolution

The differences between the transient analysis assumptions and the technical specifications are not significant. The analysis assumptions are conservative with respect to the technical specifications values.

TABLE 25-1

GGNS SENSITIVITY STUDY

	PARAMETER	EFFECT ON CONTAINMENT PRESSURE
1.	Minimum drywell temperature, 105°F	Peak Pressure P_{ww} = 28.17 psia
2.	Tech spec limit on containment pressure, +1.0 psi, P_{ww} = 15.7 psia	P_{ww} = 1.17 psi
3.	Tech spec limit on drywell containment P, -0.15 psi, P_{dw} = 15.55 psia	P_{ww} = 0.18 psi
4.	Total effect of Items 1, 2 & 3	P_{ww} = 29.52 psia P_{ww} = 29.52 - 14.7 psia = 14.82 psig

Action Plan 34

I. Issues Addressed

19.1 The chugging loads were originally defined on the basis of 7.5 feet of submergence over the drywell to suppression pool vents. Following an upper pool dump, the submergence will actually be 12 feet which may affect chugging loads.

II. Program for Resolution

3. The maximum, bounding effect of vent submergence on chugging loads will be quantified, and it will be shown that sufficient margin exists in the current GGNS load definition to bound any GGNS submergence conditions.

III. Schedule

Item 3 is complete and included in this submittal.

IV. Final Program Results

The rapid collapse of a condensing steam bubble in the suppression pool is regulated by two basic mechanisms. The first is the inertia of the water surrounding the bubble displaced during bubble collapse, known as hydrodynamic mass. The second mechanism is the pressure differential between the rapidly condensed bubble interior and the normal hydrostatic water pressure equivalent at the bubble submergence level.

Visual observations of chugging bubble collapse during full scale Mark III chugging tests indicate the bubble diameter is similar to the vent diameter, and the collapse location is near the top vent. At the normal top vent submergence of 7½ feet, the surface is approximately 7 bubble radii away from the bubble center for a 27 inch steam bubble. Applying these parameters to the spherical flow momentum equation listed in the results obtained from work completed as part of Action Plan 35 submitted in Reference 1, clearly shows the negligible effects of surface water on the collapsing steam bubble hydrodynamic mass. Therefore, the additional hydrodynamic mass effect of increased submergence is also negligible.

However, increasing the top vent submergence from 7½ to 12 feet does increase hydrostatic pressure at the vent location. Based on a wetwell atmospheric pressure of 17.5 psia during chugging, the 4½ foot increase in vent submergence results in a 9.4% hydrostatic pressure increase at the bubble collapse point. Thus, the maximum chug pressure spike is assumed to increase by 9.4% as it is directly related to hydrostatic pressure. The assumption of a direct percentage increase is conservative since it does not account for the sonic velocity limitation effect on the hydrodynamic mass acceleration.

In addition, suppression pool level increase will have two added effects. These effects are:

1. Alteration of the chugging pressure acoustic transmission from the point of bubble collapse to the pool boundaries as discussed in the results for Action Plan 35 in Reference 1.

2. Increase of the wetted wall area to receive acoustic transmission and chugging pressure loading.

Through utilization of a three-dimensional acoustic modeling technique to represent the two suppression pool configurations, the attenuation difference may be quantified. Table 34-1 contains the pressure load-increasing factors to be applied to the existing PSTF test results.

The degree of adequacy of the GESSAR II load definition to cover the effects of increased submergence can be assessed by use of amplified response spectra (ARS) of the load definition and expected loading conditions. In the answer to GESSAR II question 3B.18, the chugging local and global load definitions are compared with the maximum and mean chugs from the Pressure Suppression Test Facility (PSTF) chugging data base. These same comparisons will now be made with the PSTF data increased by the factors given in Table 1, to show that the load definitions have sufficient margin to bound any submergence effects.

Global load results were obtained by applying the increased pressure load factors to the PSTF mean chug and comparing with the global load definition. Figures 34-1, 34-2 and 34-3 clearly reveal the conservatism of the GESSAR II global load definition for the suppression pool walls and basemat boundaries.

Local load comparison plots are shown in Figures 34-4 through 34-6 for the three suppression pool boundary areas. Figure 34-4 shows the original drywell load definition easily bounds the increased loads for all frequencies. Figure 34-5 indicates slight load exceedance on the containment wall from 15 Hz to 32 Hz. From this plot, it is apparent that the total load definition area (indicative of energy) easily bounds that of the increased maximum chug load. In addition, the exceedence is judged to be negligible due to the degree of conservatism employed in developing the increased chug loading response.

Finally, Figure 34-6 indicates up to a 35% load exceedance in the frequency range of 15 Hz to 22 Hz on the basemat boundary. This exceedance is also negligible by the same reasoning as that applied in evaluating the exceedance shown in Figure 34-5. In addition, the hydrostatic head of the suppression pool on the basemat insures that a negative pressure will never be imposed on the liner. Therefore, since the liner is backed by concrete, no natural modes of vibration are excitable.

It is thus concluded that the existing local and global load definitions adequately bound increased submergence effects on chugging pressure loads, and the existing load definition is satisfactory.

Reference

1. Letter AECM-82/353 from L.F. Dale to H.R. Denton dated August 19, 1982

TABLE 34-1

EFFECT OF SUBMERGENCE ON CHUG LOAD

SUPPRESSION POOL BOUNDARY	SOURCE PRESSURE INCREASE	LOAD TRANSMISSION INCREASE	OVERALL LOAD INCREASE
DRYWELL WALL	9.4%	13.4%	24%
BASEMAT FLOOR	9.4%	10.0%	20%
CONTAINMENT WALL	9.4%	40.0%	53%

AMPLIFIED RESPONSE SPECTRUM

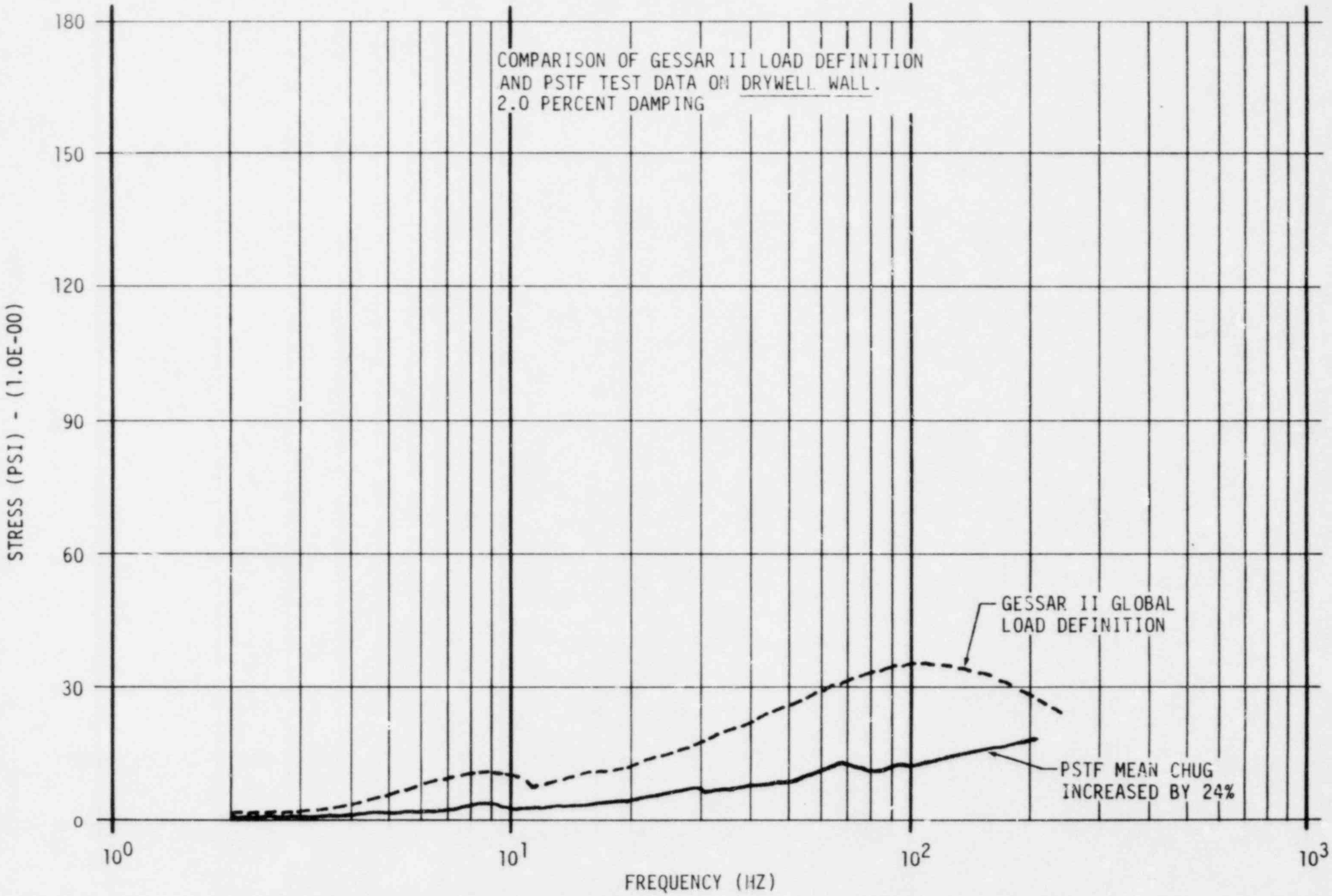


FIGURE 34-1

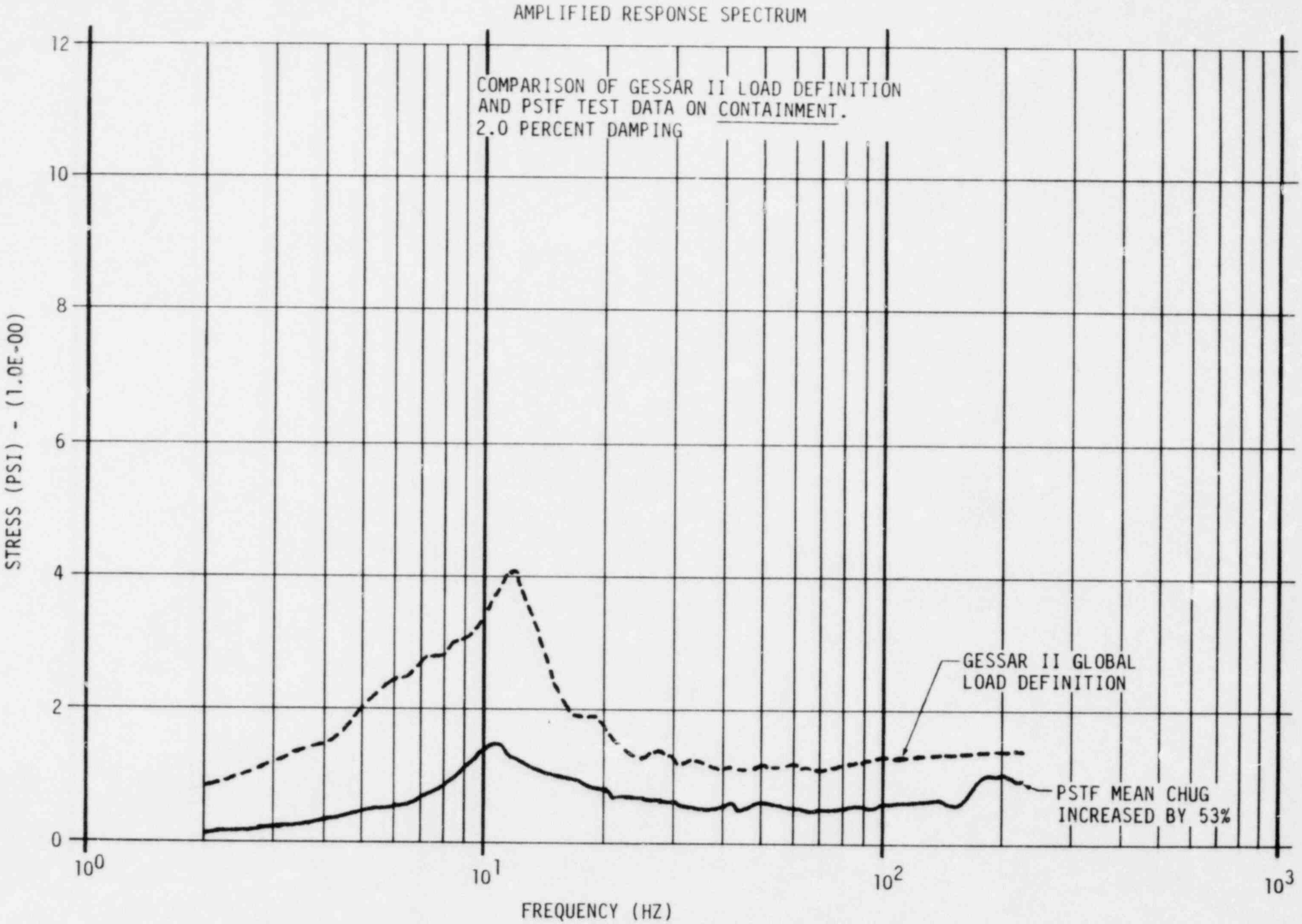

AMPLIFIED RESPONSE SPECTRUM
COMPARISON OF GESSAR II LOAD DEFINITION
AND PSTF TEST DATA ON CONTAINMENT.
2.0 PERCENT DAMPING
12
10
8
6
4
2
0
STRESS (PSI) - (1.0E-00)
10^0
10^1
10^2
10^3
FREQUENCY (HZ)
GESSAR II GLOBAL
LOAD DEFINITION
PSTF MEAN CHUG
INCREASED BY 53%

FIGURE 34-2

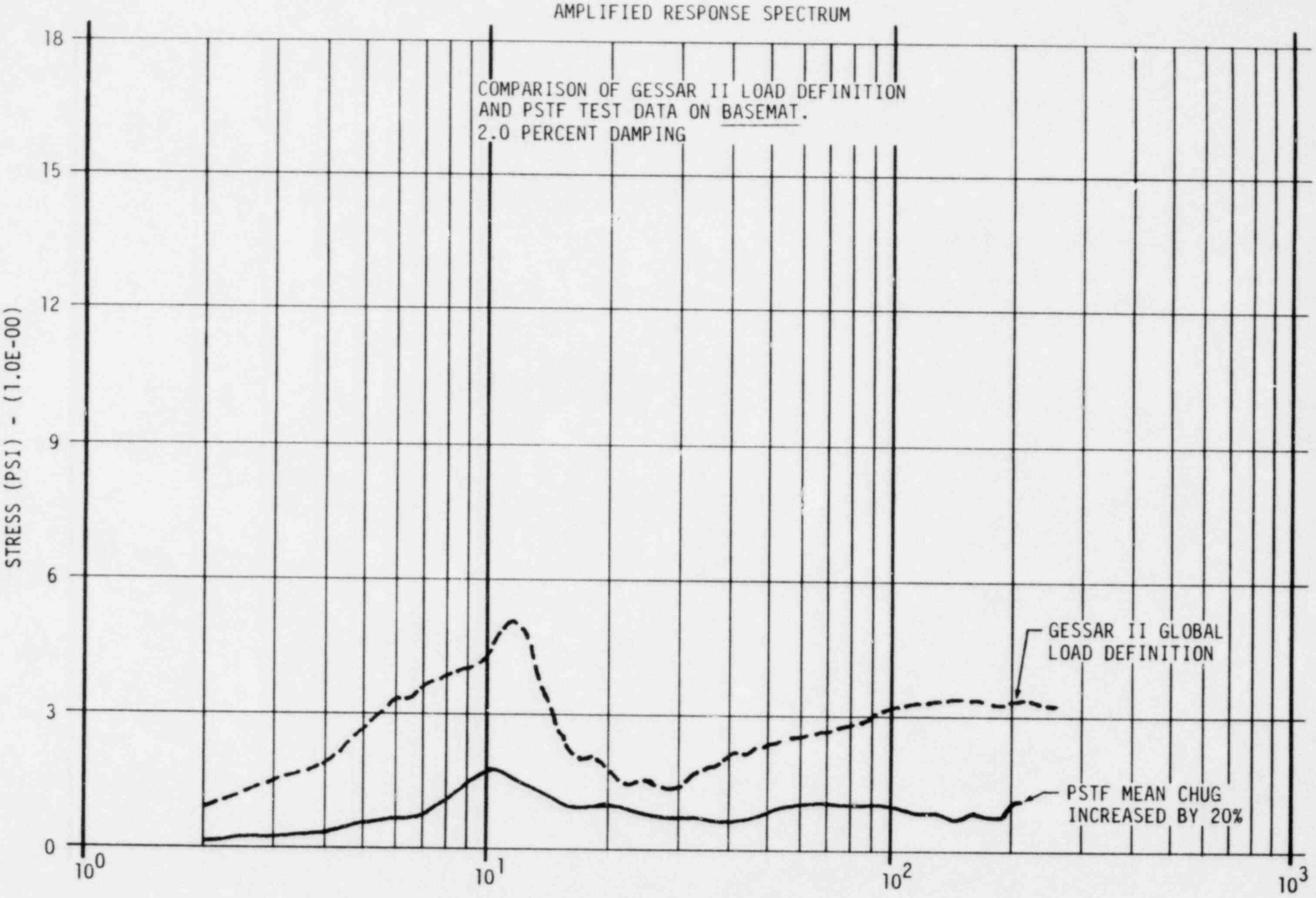

AMPLIFIED RESPONSE SPECTRUM
COMPARISON OF GESSAR II LOAD DEFINITION
AND PSTF TEST DATA ON BASEMAT.
2.0 PERCENT DAMPING
STRESS (PSI) - (1.0E-00)
18
15
12
9
6
3
0
10^0
10^1
10^2
10^3
FREQUENCY (HZ)
GESSAR II GLOBAL
LOAD DEFINITION
PSTF MEAN CHUG
INCREASED BY 20%

FIGURE 34-3

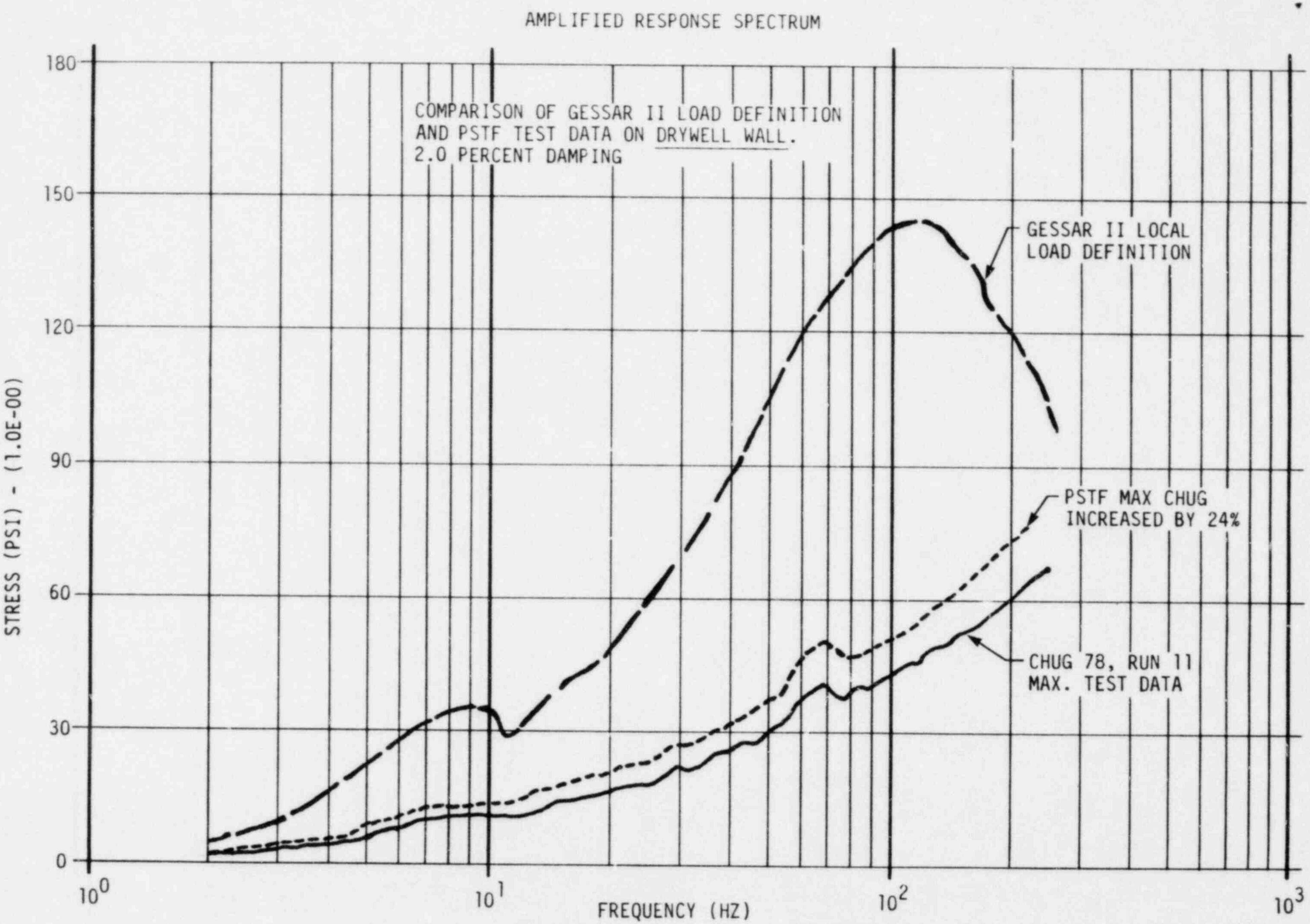

AMPLIFIED RESPONSE SPECTRUM
COMPARISON OF GESSAR II LOAD DEFINITION
AND PSTF TEST DATA ON DRYWELL WALL.
2.0 PERCENT DAMPING
GESSAR II LOCAL
LOAD DEFINITION
PSTF MAX CHUG
INCREASED BY 24%
CHUG 78, RUN 11
MAX. TEST DATA
STRESS (PSI) - (1.0E-00)
FREQUENCY (HZ)
180
150
120
90
60
30
0
10^0
10^1
10^2
10^3

FIGURE 34-4

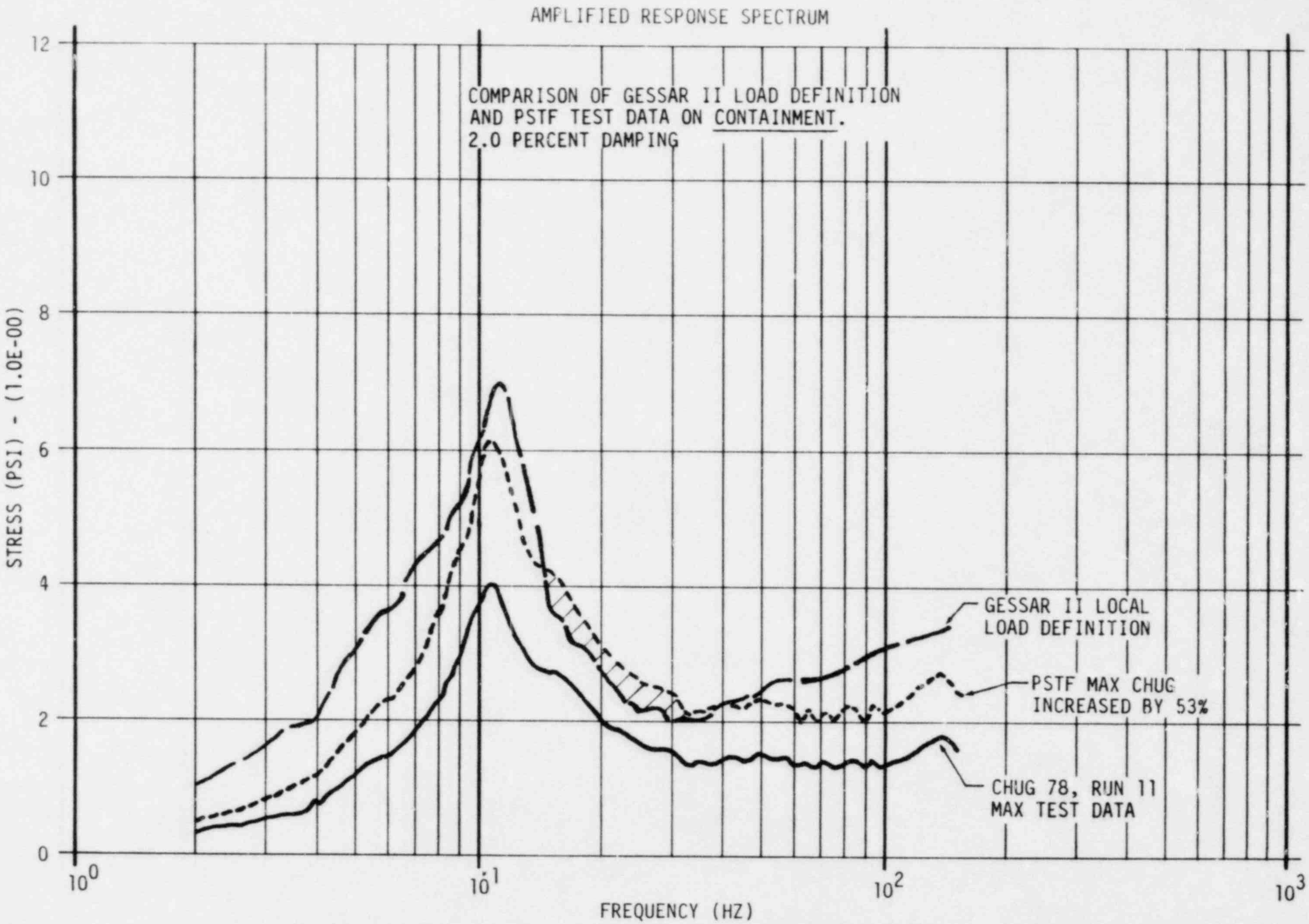

AMPLIFIED RESPONSE SPECTRUM
COMPARISON OF GESSAR II LOAD DEFINITION
AND PSTF TEST DATA ON CONTAINMENT.
2.0 PERCENT DAMPING
STRESS (PSI) - (1.0E-00)
12
10
8
6
4
2
0
GESSAR II LOCAL
LOAD DEFINITION
PSTF MAX CHUG
INCREASED BY 53%
CHUG 78, RUN 11
MAX TEST DATA
10^0
10^1
10^2
10^3
FREQUENCY (HZ)

FIGURE 34-5

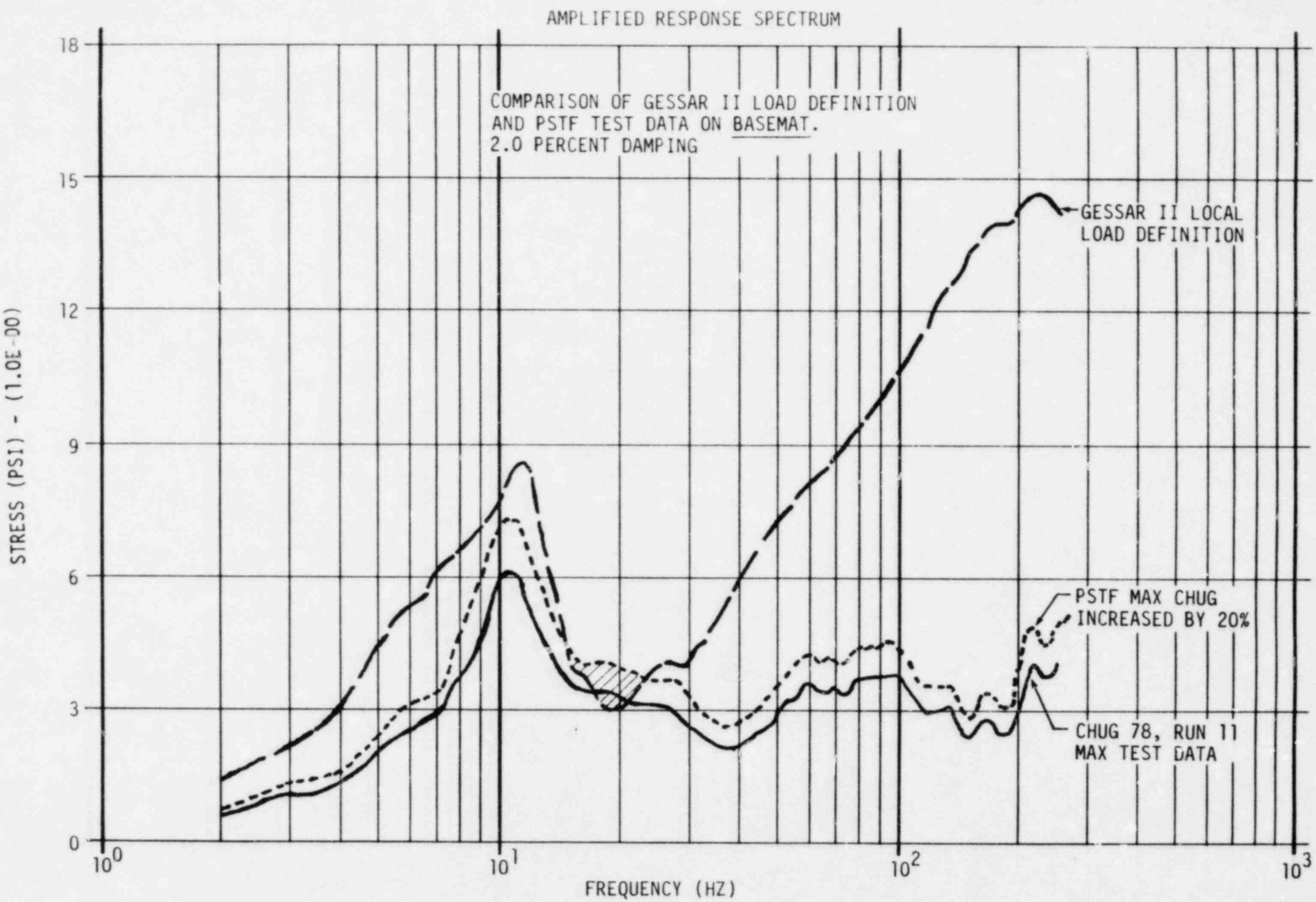

AMPLIFIED RESPONSE SPECTRUM
COMPARISON OF GESSAR II LOAD DEFINITION
AND PSTF TEST DATA ON BASEMAT.
2.0 PERCENT DAMPING
GESSAR II LOCAL
LOAD DEFINITION
PSTF MAX CHUG
INCREASED BY 20%
CHUG 78, RUN 11
MAX TEST DATA
STRESS (PSI) - (1.0E-00)
18
15
12
9
6
3
0
10^0
10^1
10^2
10^3
FREQUENCY (HZ)

FIGURE 34-6

ATTACHMENT 4

AECM-82/574

Letter AECM-82/497 from L. F. Dale to H. R. Denton, dated October 22, 1982, contains a list of questions which were raised by the NRC Staff and their consultants. This attachment contains additional information which has been prepared to respond to these questions.

I.14.1

Evaluate the feasibility of quantifying error bands on flow measurement made with drag disks. These flow measurements were used in MP&L's discussion of mixing produced by main vent chugging which was discussed in material submitted as part of Action Plan 14.

Response:

MP&L believes that quantificaton of possible variations in total flow as measured by the drag disks in the subject tests is not essential to the intent of the information submitted under Action Plan 14. The intent of the information which has been submitted is not to conclusively demonstrate that chugging is a more effective mixing mechanism than the RHR system. Rather, the intent is to conclusively demonstrate that the entire pool volume is mixed during top vent chugging. Consequently, errors of up to 400% in the total flow predicted using the drag disks would still result in complete pool turn-over times approximating the turnover time associated with RHR system operation. MP&L does not believe that the expenditure of additional effort is warranted with respect to quantifying these error bands.

I.14.2

Discuss any analytical methods which may be available to verify test results which showed mixing occurs as a result of main vent chugging.

Response:

The analytical tools available to evaluate mixing produced by main vent chugging are not sophisticated enough to provide meaningful evaluations of the physical processes involved. The analytical models which have been developed are empirical in nature and are designed primarily to replicate the test results.

I.14.3

Provide additional discussion to support the conclusion that the suppression pool will not experience significant thermal stratification when the RHR system is transferred from the suppression pool cooling mode to the containment spray mode.

Response:

The suppression pool will not experience significant thermal stratification when the containment spray actuates. The response to question I.14.4 indicates that the pool will continue to rotate following elimination of the suppression pool cooling jet. Analyses completed as part of Action Plan 18 demonstrated that the containment spray will not be actuated unless large quantities of bypass leakage are present. If this bypass leakage is occurring, then chugging which occurs through the top row of vents will create thorough mixing of the pool as discussed in the information submitted as part of Action Plan 14.