Illinois Power Company

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Docket No. 50-461

February 4, 1983

Director of Nuclear Reactor Regulation
Attention: Mr. A. Schwencer, Chief
Licensing Branch No. 2
Division of Licensing
U.S. Nuclear Regulatory Commission
Washington, D.C. 20555

Reference: IP letter U-0562 dated 11/23/82, G. E. Wuller to C. O. Thomas, NRC, subject: Submittal addressing some John Humphrey concerns.

Dear Mr. Schwencer:

Subject: Clinton Power Station Unit 1
Humphrey Concerns

The referenced letter addressed some of the John Humphrey concerns as applicable to the Clinton Power Station (CPS). Enclosed are CPS responses on some additional Humphrey issues for NRC Staff review. Included are Action Plans #7, 9, 10, 11, 12, 13, 16, 17, 20, 23, 24, 27, 30, 34, 35, 36 and 37. We believe that these responses will resolve the particular concern involved.

If there are any questions regarding this material, please contact me or J. H. Shepard (217) 424-6785.

Sincerely,

G. E. Wuller

G. E. Wuller
Supervisor-Licensing
Nuclear Station Engineering

GEW/jmm

Enclosure

cc: Dr. H. Abelson, NRC Clinton Project Manager
Mr. H. H. Livermore, NRC Resident Inspector
Illinois Department of Nuclear Safety
R. W. Evans, Quadrex Corporation

B001

Action Plan 7

3.2 The STRIDE design provided only nine inches of submergence above the RHR heat exchanger relief valve discharge lines at low suppression pool levels.

Response

To address this concern, an evaluation of the Humboldt Bay pressure suppression test data is submitted to show that the maximum discharge from the relief valves can be quenched under all possible submergence conditions.

Figure 7-1 shows condensation effectiveness data obtained during the Humboldt Bay pressure suppression tests (Reference 1). These tests investigated condensation effectiveness at vent submergences from 12 to -3 feet (i.e. 3 feet clearance between the discharge of the 14-inch diameter vertical vent and the pool surface) at vent steam mass fluxes, G, of $50 < G < 250$ lbm/ft^2/sec. This mass flux considerably exceeds the mass flux of 198 lbm/ft^2./sec associated with the CPS RHR heat exchanger relief valve discharges.

Condensation effectiveness is characterized by comparing the measured and calculated pressure in the suppression chamber air space. The calculated suppression chamber air space pressure is based on the assumption of complete condensation of steam in the pool. That is, if the steam actually escapes to the air space without being condensed, the measured suppression chamber air space pressure would be much higher than the calculated value. The figure shows that nearly complete condensation of the steam still occurs when the vent exit is 2 feet above the water surface. Steam bypass is evident in the case of 3 feet clearance.

Since the minimum submergence of the RHR SRV discharge is 2 inches, complete condensation is assured for CPS. Consequently no pool bypass can occur and this issue is closed.

Reference

1) C. H. Robbins, "Tests of a Full Scale 1/48 Segment of the Humboldt Bay Pressure Suppression Containment", GEAP 3596, November, 1960.

HUMBOLDT BAY PRESSURE SUPPRESSION
TEST MAXIMUM SUPPRESSION CHAMBER
PRESSURE COMPARISON (Plotted from Ref. 1)

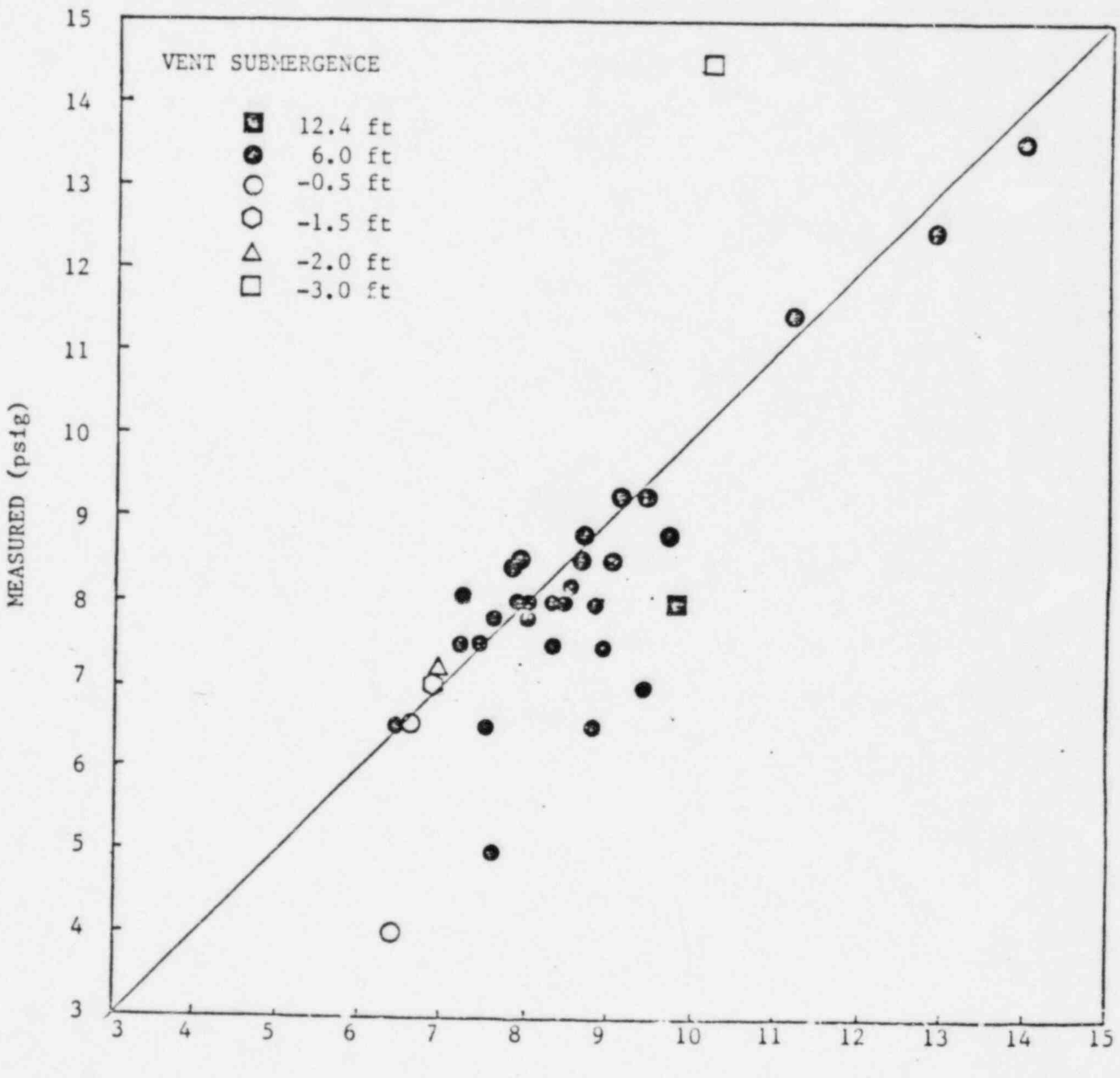

Figure 7-1

Action Plan 9

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.

Response

To address this concern, the maximum quantity of energy which can be added to the suppression pool was quantified. This was based upon operator action to terminate relief valve discharge following discovery by the operator that the relief valve had actuated. Included, was an evaluation of all scenarios which could lead to discharge from these relief valves.

The maximum quantity of energy which could be added to the suppression pool is 1.71×10^7 BTU, assuming that the pressure control valve fails full open; all steam flowing through the control valve exists through the relief valve prior to reaching the heat exchanger; no heat is removed from flow into the heat exchanger. This quantity of energy added to the suppression pool is limited to a two-minute discharge from the relief valve prior to termination of the event by operator action.

The maximum discharge time of two minutes for the relief valve before the operator terminates the event is based upon multiple sequences of control operation required prior to placing the steam condensing mode in service. Initiation of the RHR steam condensing mode is highly operator intensive and requires essentially continuous monitoring of heat exchanger pressure, temperature and water levels. The implementation of stable steam condensing operation normally requires a minimum of 30 minutes following initiation.

If the operator encounters situations in which important heat exchanger parameters cannot be effectively controlled, e.g. the pressure control valve fails open, the operator will promptly close the steam supply block valve. A high temperature alarm downstream of the pressure control valve is set to alarm on reaching a temperature, corresponding to saturation at a pressure slightly higher than 200 psig - the upper end of the prescribed control range. This alarm will alert the operator that the control valve has failed. The operator will immediately isolate the steam supply to the heat exchanger within one minute of receipt of the alarm. The steam supply block valve will be fully closed within approximately two minutes of receipt of alarm.

The maximum choked flow which can pass through the failed open pressure control valve is 431,000 lbm/hr. The flow from the fully open RHR relief valve is 560,000 lbm/hr. Consequently, following actuation of the relief valve, the RHR HX will depressurize 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. This on/off charging of the pool will produce more mixing than would be accomplished by a steady, uniform jet.

The maximum quantity of energy postulated to be added to the suppression pool is quite conservative. The value of energy calculated is based upon full flow instead of partial flow through the relief valve for two minutes. The assumption that no energy is transferred out of the steam flowing to the heat exchanger is extremely conservative. Also, the mixing produced by the cyclic actuation of the relief valve will prevent any significant pool stratification. Therefore, this issue is closed.

Action Plan 10

4.1 The present containment response analyses for drywell break accidents assume that the ECCS systems transfer a significant quantity of water from the suppression pool to the lower regions of the drywell through the break. This results in a pool in the drywell which is essentially isolated from the suppression pool at a temperature of approximately 135°F. The contai :ent response analysis assumes that the drywell pool is : .oroughly mixed with the suppression pool. If the inventory in the drywell is assumed to be isolated and the remainder of the heat is discharged to the suppression pool, an increase in bulk pool temperature of 10°F may occur.

Response

Due to the similarities of the Grand Gulf and Clinton suppression pools, this issue can be closed out generically for Clinton. The following provides details of General Electric's analysis which predicted a maximum temperature increase of 6°F.

General Electric estimated the effect on suppression pool peak long term temperature, caused by formation of the drywell pool, using certain Mark III sensitivity studies produced in 1974. These studies yielded plots of peak pool temperature versus pool volume for various RHR heat exchanger performance values. Extrapolating from these plots using GGNS suppression pool, drywell pool, and RHR heat exchanger values, the effect on peak pool temperature caused by drywell pool formation was estimated to be 6°F. (Clinton has a larger suppression pool, so this is a bounding value). Various analysis assumptions have changed since these studies were done (e.g., improved modeling for feedwater). These studies are adequate for determing the order of magnitude value appropriate to the issue.

As discussed in Attachment A of this Action Plan, GE's proprietary long-term containment response code, SHEX, models the drywell floor area as a drywell pool. The SHEX was used to perform a GGNS plant unique containment response with the standard FSAR assumptions. This SHEX analysis was modified by a mass-energy balance to simulate the isolated 135°F drywell pool. Maintaining a 135°F drywell pool temperature is a bounding condition since SHEX calculates an average 230°F drywell pool temperature.

Results from the above bounding analysis show that isolation of the drywell pool increased the peak suppression pool temperature by 10°F. This bounding peak bulk pool temperature increase is small compared to the 20°F conservatisms contained in the FSAR licensing assumptions.

ATTACHMENT A

Description of Containment Pressure/Temperature Response Methodology

The methodology used to calculate the Mark III containment pressure/temperature responses is delineated in References 1 and 2. The basic methodology including equations described in the licensing topical report are incorporated into the short-term, M3CPT, and long-term, SHEX, containment response computer codes.

The purpose of the M3CPT (short-term) code is to calculate the dynamic pressure and temperatures responses associated with the inertia of clearing the vents of water. As documented in Reference 1, the short-term analytical M3CPT model is conservative whem compared to pressure suppression test data. Details of the M3CPT code modeling are shown in Figure 1.

The purpose of the SHEX (long-term) code is to calculate the pressure and temperature responses associated with decay heat, vessel blowdown energy, heat exchangers and containment sprays. The SHEX code performs mass and energy balances on models of the reactor vessel, drywell airspace, drywell pool, weir annulus, suppression pool and containment airspace. The SHEX code has a static vent clearing model but incorporates a complex system model which permits realistic simulation of the ECCS systems, containment spray and upper pool dump. Details of the SHEX code modelling are shown in Figure 2. Table 1 presents a summary of the major assumptions and conservatisms contained in the FSAR pressure, temperature analysis as used in GE's short and long-term codes.

References

1. W. J. Bilanin, "The General Electric Mark III Pressure Suppression Containment Analytical Model", June 1974, (NEDO-20533-1).

2. W. J. Bilanin, "The General Electric Mark III Pressure Suppression Containment Analytical Model", Supplement 1, September 1975, (NEDO)-20533-1).

TABLE I

Major Modeling Assumptions

Model	Assumption
Heat sinks	No credit
Worst single failure	Loss of diesel generator which results in only one RHR available for pool cooling.
Containment airspace	Same temperature as pool temperature, i.e., isothermal.
Pump heat	Considered as additional energy source.
Break flow	Moody critical flow.
DBA break	Double-ended guillotine instantaneous break.
Heat exchanger	Fouling and plugged-tubes accounted for to conservatively underestimate heat removal capability.
Post-LOCA power ramp down and decay heat	Incorporated with initial power at 104.5% (for Grand Gulf) and conservative decay heat ANS-5 20/10.
Feedwater addition	Maximum at initial temperature of $125^{o}F$, which implies upper pool follows main suppression pool.
NSSS metal sensible heat	Incorporated
Core fuel relaxation energy	Incorporated

TABLE I (continued)

Major Modeling Assumptions

Model	Assumption
Metal-water reaction energy	Incorporated
RHR initiation time	30 minutes post-LOCA
Service water temperature	Site 10-year projected maximum.
Participation of non-ESF systems	No credit

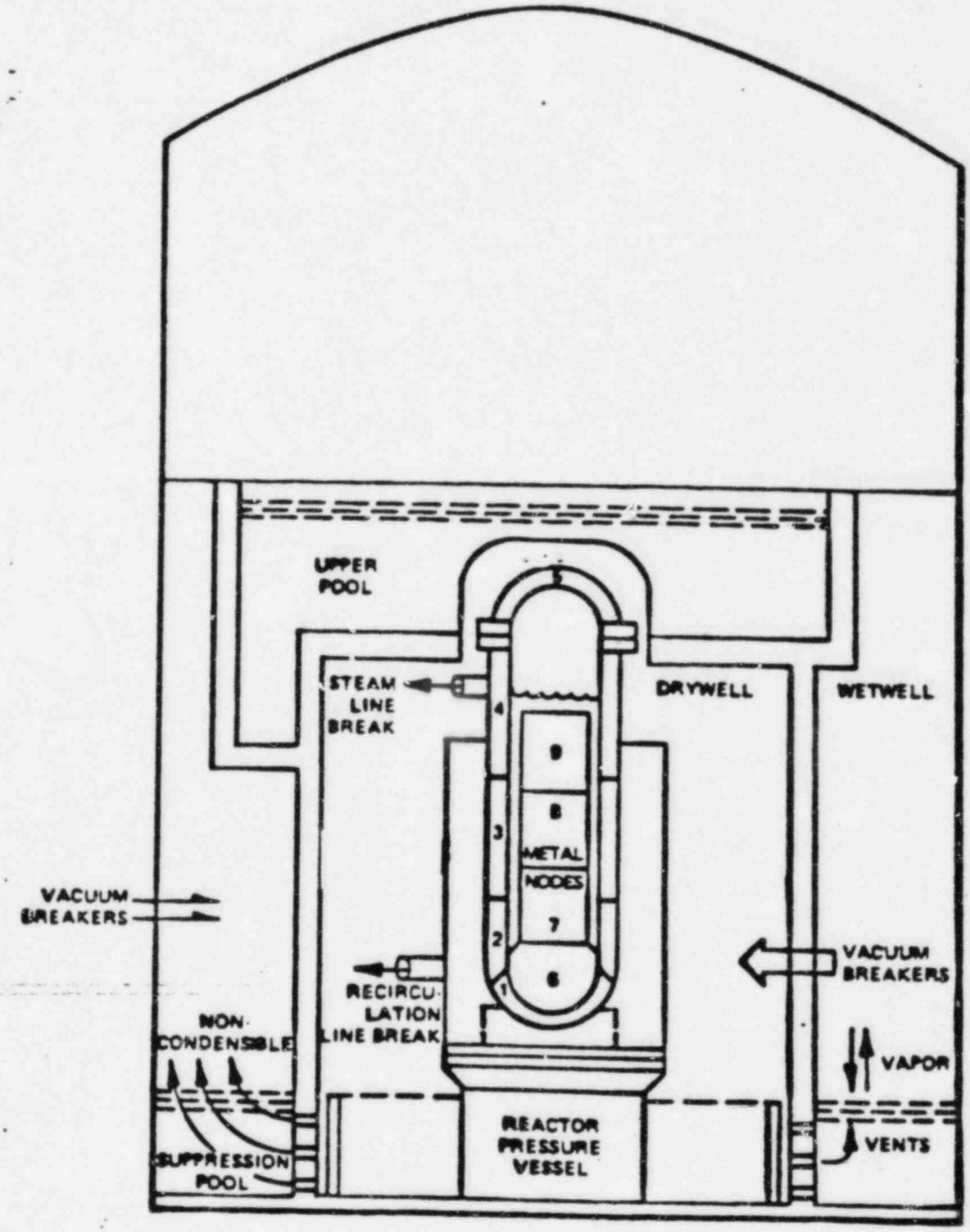

Figure 1. Schematic of Vessel, Drywell, and Wetwell Systems Modeled by M3CPT04

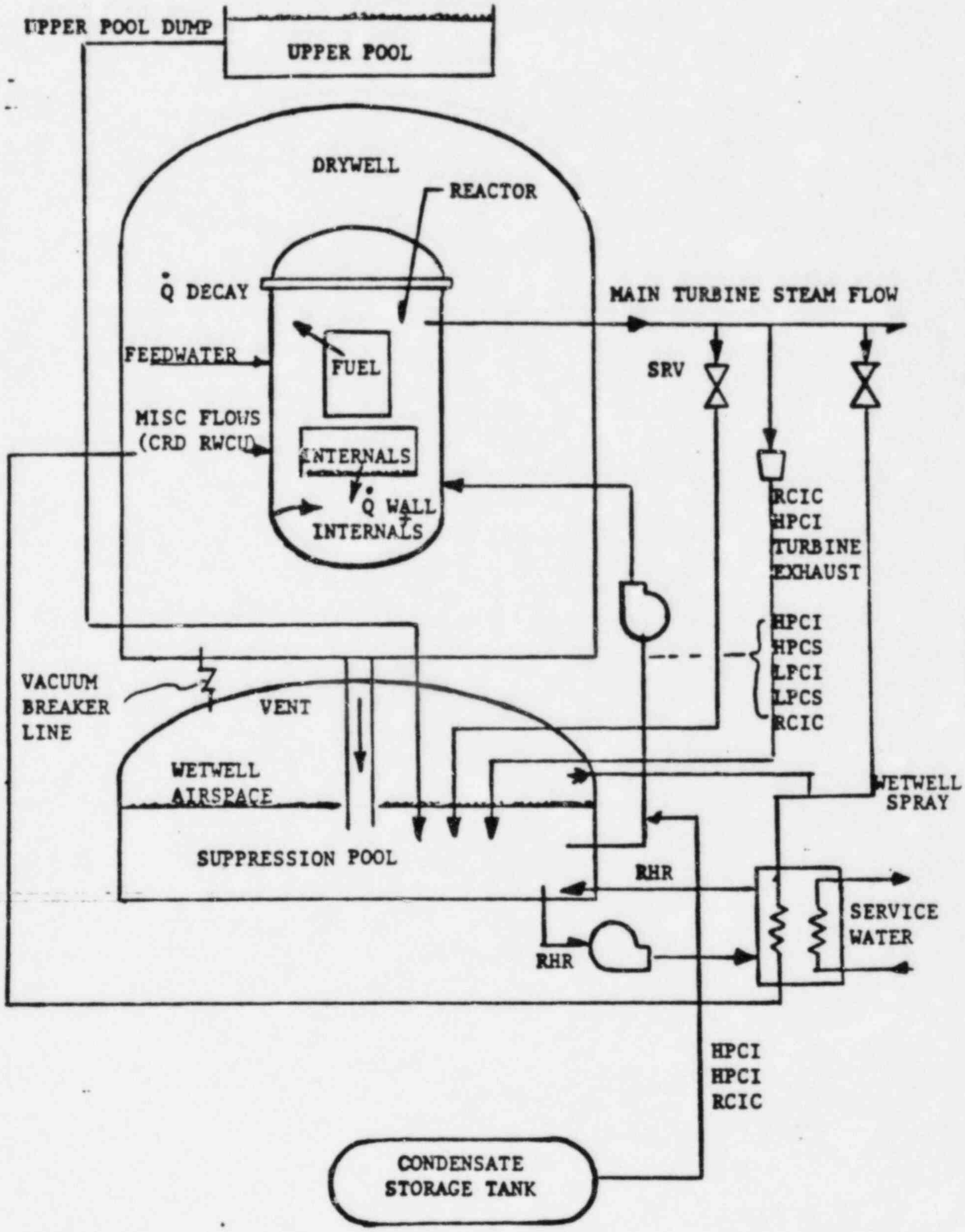

Figure 2. SHEX Code Modelling

Action Plan 11

4.2 The existence of the drywell pool is predicated upon continuous operation of the ECCS. The current Emergency Procedure Guidelines require the operators to throttle ECCS operation to maintain vessel level below Level 8. Consequently, the drywell pool may never be formed.

9.1 The current FSAR analysis is based upon continuous injection of relatively cool ECCS water into the drywell through a broken pipe following a design basis accident. The EPG's direct the operator to throttle ECCS operation to maintain reactor vessel level at about level 8. Thus, instead of releasing relatively cool ECCS water, the break will be releasing saturated steam which might produce higher containment pressurizations than currently anticipated. Therefore, the drywell air which would have been drawn back into the drywell will remain in the containment and higher pressures will result in both the containment and the drywell.

Response

11.1 Demonstrate that the failure to form a drywell pool will not entail adverse consequences. The calculations will quantify the variation of suppression pool level without formation of the drywell pool and with upper pool dump.

11.2 Review interactions between ESF system operation and suppression pool level to assure that higher suppression pool level will not degrade performance.

11.3 Analyze the effects of failure to recover the drywell air mass. This analysis includes the effects of containment heat sinks and the mitigating effects of containment spray.

Item 11.1 addresses the consequences of operator action to control RPV water level following main steam line break. Such actions potentially affect peak pool temperature, and suppression pool level. With no ECCS water flooding the break, drywell depressurization at the end of blowdown does not occur. Therefore, containment air is not redistributed through the vacuum breakers, and the drywell pool is not formed.

a) Impact on Containment Pressure

A simple end-point calculation was performed to evaluate the containment pressure at the peak calculated containment temperature of 181^{o}F for DBA (peak pool temperature in table 6.2 - 13 of GGNS FSAR. This is comparable to Clinton's value of 180.3^{o}F from FSAR Section 6.2.1.1.3.1). This was based on the conservative assumption of thermodynamics equilibrium between suppression pool and containment airspace, and no redistribution of air between drywell and containment. The containment pressure, as calculated at the peak temperature, is 12.5 psig which is well below the design limit of 15 psig. (This is comparable to Clinton's peak value of 8.74 psig).

b) Impact on Suppression Pool Temperature

The peak pool temperature resulting from a main steam line break with no drywell pool forming was 165°F. Except for the constant vessel level, and upper pool temperature of 95°F, standard FSAR LOCA assumptions were used, including the worst single failure of one RHR system. All the break flow was conservatively assumed to go directly to the suppression pool. The suppression pool temperature is not adversely affected.

c) Maximum Suppression Pool Level

The maximum suppression pool depth variation is between 26.2 ft. (no drywell pool and upper pool dump) and 14.5 ft. (minimum required vent submergence).

Two limits define the range of possible suppression pool water level variation (to address Item 11.2):

1. On the low-water-level side, the post-LOCA pool drawdown is limited by the design requirement that the Suppression Pool Makeup System (SPMS) maintain the pool level at least 2 feet above the upper row of horizontal vents. Design calculations to size the necessary SPMS dump volume are based on conservative assumptions regarding the quantities of suppression pool water which will be redistributed to the drywell, reactor vessel, containment pools, and main steam lines.

2. On the high-water-level side, the maximum pool surface elevation is established by pool normal high water level combined with upper pool dump, approximately 2 minutes of feedwater flow addition, and weir annulus water volume down to upper rows of vents (cleared by drywell pressure).

The above extremes give a variation (based upon General Electric's calculations) of suppression pool depths from 14.5 to 26.2 feet for GGNS. These depths compare to 15.1 and 26.1 for CPS. This variation in pool surface elevation has no deleterious effect on the ECCS systems insofar as there being adequate net positive suction heat (NPSH) at the pump suction. Since higher pool surface elevations increase the available NPSH at the pump suction, the most limiting NPSH conditions would occur at low pool surface elevation. The GGNS ESF system designs take account that suppression pool drawdown can occur and the ECCS pumps are designed on the basis that the pool surface could correspond to the minimum elevation noted above (i.e., 2 feet above the top of the upper row of vents). Furthermore, the pump NPSH design calculations assume the pool water at this elevation is saturated at atmospheric pressure (212°F, 14.7 psia). These conservative ECCS design bases assure that the pump will have adequate NPSH for the complete range of possible post-LOCA pool surface elevations.

For Item 11.3, a realistic analysis using GE Proprietary Computer Code VACBR04 was performed to show that even with no redistribution of containment air to drywell, adequate margin exists between the peak calculated and the containment design pressures. In this analysis, drywell and containment structural heat sinks, and heat and mass transfer between pool and containment airspace were both modelled. However, conservatively low free convection heat transfer coefficients were used to minimize the effect of heat transfer to the heat sinks, and higher mass and heat transfer coefficients at suppression pool surface were used to maximize the heat source. All the break flow energy was conservatively assumed to enter directly into the suppression pool via and weir annulus and vent system, completely bypassing the drywell pool.

The results from GE's analysis for GGNS show that the containment airspace temperature is always less than that of the pool. At the time of peak pool temperature (which occurs at about 3.8 hrs), the pool-to-airspace temperature difference was approximately 36°F. After 3.8 hours, this temperature difference narrows at a rate of about 2.7°F/hr. The containment pressure, at the time of peak pool temperature, was 5.1 psig, and increasing at a rate of about 0.2 psi/hr thereafter. Linear extrapolation of these trends, which is conservative, shows that the containment pressure will increase at most by another 3 psi (at about 17 hours, when the pool-to-airspace T would be reduced to zero). This shows that, realistically, the containment pressure will not exceed 8.1 psig, even with no containment air redistribution, and that there is more than adequate margin between the containment peak calculated pressure and the design pressure. The containment sprays will not be automatically activated.

The results of GE's analysis are bounding for Clinton due to the larger net free volume in the containment and a larger suppression pool Therefore, these issues are closed.

Action Plan 12

4.3 All Mark III analyses presently assume a perfectly mixed uniform suppression pool. These analyses assume that the temperature of the suction tc the RHR heat exchangers is the same as the bulk pool temperature. In actuality, the temperature in the lower part of the pool where the suction is located will be as much as $7\frac{1}{2}^{o}$ cooler than the bulk pool temperature. Thus, the heat transfer through the RHR heat exchanger will be less than expected.

Responses

An assessment was made of the various factors affecting suppression pool temperature and heat transfer to determine if they could result in lower heat transfer through the RHR heat exchanger. The factors which were considered were:

1. Major conservatisms used in the RHR suppression pool cooling performance analysis.

2. Temperature difference between bulk suppression pool temperature and the RHR heat exchanger and the RHR heat exchanger inlet temperature.

3. Heat and mass transfer between the suppression pool and the wetwell airspace.

4. The effect of RHR heat exchanger operation on suppression pool stratification reduction.

5. RHR heat exchanger test data.

6. Effects of vent chugging.

A review of the major conservatisms used for deriving the suppression pool cooling performance have realistically reduced the containment airspace peak pressure and peak temperature by 5.6 psi and $48^{o}F$ respectively, in addition to reducing the peak suppression pool temperature by $20^{o}F$. This information is based on new calculations using the old GE SHEX code and is summarized in the attachment which is Table I. Table I bounds CPS design.

The theoretical and empirical correlations used in the SHEX code for heat and mass transfer between the suppression pool and the wetwell airspace are shown in Attachment A.

An analysis of RHR heat exchanger operation effect on suppression pool thermal stratification has been carried out analytically and through in-plant testing.

Analytically a Mark III standard 238 plant was modeled using the GE RELAP4/MOD5 code for an intermediate break accident which uncovers the top row of vents. This situation produces the most severe thermal stratification. The Mark III RELAP analysis predicted a 24°F temperature difference would exist between the RHR suction and return locations at the start of the RHR operation. After 15 minutes of operation, the RHR reduces the temperature difference to 2°F. This is small compared to the 20°F conservatism originally contained in the FSAR licensing assumption.

Actual in-plant test of the RHR system confirm the above analysis. Actual temperature measurements indicated that the maximum temperature difference within the pool was about 2°F after 20 minutes of RHR operation. If vent chugging effects were added, the temperature difference would be even less.

In addition to the conservatisms of analysis and operation demonstrated above, an evaluation of the RHR heat exchangers was made. It showed that previous startup tests with the RHR system has demonstrated that the RHR heat exchanger thermal performance is considerably better than the design minimum by 40 to 60%. RHR heat exchangers are used infrequently and it is standard practice to lay these units up with demineralized water in the tube side. This practice, coupled with periodic cleaning ensures adequate heat removal throughout the life of the plant.

Based on the above statements, heat transfer through the RHR heat exchanger will be more than expected.

TABLE 1

SUMMARY OF MAJOR GGNS FSAR CONSERVATISMS

Parameter	FSAR Assumption	Realistic Assumption	Reduction in Peak Suppression Pool Temp. °F	Reduction in Containment Airspace Temp. °F	Reduction in Peak Containment Pressure psi
1. Decay heat	ANS-5 20/10	ANS-5.1	8	8	1.7
2. Initial Conditions					
a) Rated power,%	104.7	100			
b) Vessel pressure, psia	1060	1040	8	8	1.6
c) Suppression pool level	Low	Normal			
d) Upper pool temperature, F	125	100			
e) Service water temperature, °F	95	87			
3. RHR heat exchanger coefficient, Btu/sec	540	626	6	6	1.2
4. Suppression pool Containment airspace Thermal equilibrium	Isothermal	Realistic heat and mass transfer coefficients (see Attachment A)	0	19	5.0
5. Pool-airspace thermal equilibrium with heat sinks	Isothermal, no heat sinks	Realistic heat and mass transfer coefficients with Uchida coefficient in drywell and natural convection in wetwell	0	47	5.6
6. All parameters	See Items 1-5	See Items 1-5	20	48	5.6

Attachment A

Heat and Mass Transfer between Suppression Pool and Wetwell Airspace

This attachment documents the theoretical and/or empirical correlations for evaluating heat and mass transfer between the suppression pool and the wetwell airspace which are used in the M3CPT, SHEX and VACBR codes.

HEAT TRANSFER

For heat transfer evaluations, it is assumed that the suppression pool surface acts as a flat plate, and the airspace is filled with air. At ordinary pressure and temperature, the natural convection heat transfer coefficient, h, is given by:

1. Heated Plated Facing Up ($T_{pool} > T_{ww}$):

   Turbulent Range ($N_{Pr} \cdot N_{Gr} > 2 \times 10^7$)

   $$h = 0.22 \Delta T^{1/3}$$

   Laminar Range ($N_{Pr} \cdot N_{GR} < 2 \times 10^7$):

   $$h = 0.27 \left(\frac{\Delta T}{L}\right)^{0.25}$$

2. Cooled Plate Facing Up ($T_{pool} < T_{ww}$):

   Laminar Range:

   $$h = 0.12 \left(\frac{\Delta T}{L}\right)^{0.25}$$

In the above expressions,

N_{Pr} - Prandtl number $\frac{\mu c_p}{k}$

N_{Gr} - Grashof number $\frac{L^3 \rho^2 \beta \Delta T g}{\mu^2}$

L - Length of flat plate (assumed to be square)

μ - Dynamic viscosity

C_p - Specific heat at constant pressure

K - Thermal conductivity

T - Temperature Difference Across Plate

β - Coefficient of Volumetric Expansion

The product of Prandtl number and Grashof number can be rewritten as

$$N_{pr} \cdot N_{Gr} = \psi \cdot \Delta T \cdot L^3$$

Where

$$\psi = \frac{p^2 g \beta C_p}{\mu K}$$

For air over the temperature range of 32°F to 1000°F, can be expressed by the following equation (based on curve-fit of Table A-25 of Reference 1):

$$\psi(T) = \text{Exp}\,(6.4621 - 3.9468 \times 10^{-3} T + 2.181 \times 10^{-6}\, T^2)$$

where T = temperature, °F

MASS TRANSFER

The rate of evaporation from the suppression pool surface is given by (reference 2):

$$\dot{M}_{evp} = A_{sp} \cdot h_D \cdot \frac{(P_{sp} - P_{wws})}{R_s \quad T_{sp}}$$

where

A_{sp} - Surface area of suppression pool

h_D - Pool surface mass transfer coefficient

R_s - Gas constant for steam

P_{sp} - Saturation pressure corresponding to suppression pool temperature

P_{wws} - Partial pressure of vapor in wetwell airspace

T_{sp} - Suppression pool temperature

The mass transfer coefficient, h_D, can be obtained by (Reference 3):

$$h_D = \frac{h}{C_p \rho} \cdot \left(\frac{D_v}{a}\right)^{2/3}$$

where

h - Coefficient of heat transfer between suppression pool and wetwell airspace, Btu/ft^2 sec F

C_p- Specific heat at constant pressure of wetwell airspace mixture (assumed to be air), Btu/lb^oF

ρ- Density of wetwell airspace mixture (assumed to be air), lb/ft^3

D_v- Mass diffusivity between water vapor and air 0.99 ft^3/hr

a- Thermal diffusivity of wetwell airspace mixture ($\sim$0.9 ft^3/hr) (assumed to be air)

References

1. McAdams, W. H., "Heat Transmission", 3rd Edition, McGraw-Hill, 1954

2. Rohsenow and Choi, "Heat, Mass and Momentum Transfer", Prentice-Hall, New York, 1961

3. Kreith, F., "Principles of Heat Transfer", 3rd Edition, Harper & Row, 1973

Action Plan 13

4.4 The long term analysis of containment pressure/temperature response assumes that the wetwell airspace is in thermal equilibrium with the suppression pool water at all times. The calculated bulk pool temperature is used to determine the airspace temperature. If pool thermal stratification were considered, the surface temperature, which is in direct contact with the airspace, would be higher. Therefore the airspace temperature (and pressure) would be higher.

7.1 The containment is assumed to be in thermal equilibrium with a perfectly mixed, uniform temperature suppression pool. As noted under Topic 4, the surface temperature of the pool will be higher than the bulk pool temperature. This may produce higher than expected containment temperatures and pressures.

Response

The nominal increase in peak long-term suppression pool bulk temperature is 3°F due to thermal stratification between RHR suction and return temperatures. The thermal stratification model and PSTF test data indicate that nominal pool surface temperature is identical to the RHR return location temperature. This effect increases the peak containment airspace temperature by 3°F and pressure by less than 0.1 psi. These containment airspace pressure and temperature increases that result from pool stratification are small compared to conservatisms (5.6 psi and 20°F) associated with the thermal equilibrium assumption (i.e., assuming containment airspace temperature equals pool bulk temperature at all times during the transient).

Action Plan 16

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 arrangements of the discharge and suction points of the pool cooling system maximizes pool mixing.

Response

An evaluation was made for the effect of the interaction of the RHR suppression pool suction and discharge geometry and orientation of discharge flow on suppression pool cooling system mixing.

In the Clinton Plant, there is a difference of six feet vertically between the RHR suction strainers and RHR discharge nozzles which minimizes stratification of water temperature in the suppression pool.

In the pool-cooling mode, the role of the RHR system is:

1. To mix the water in the suppression pool to avoid any hot spots in the vicinity of the quenchers and to eliminate thermal stratification.

2. To remove thermal energy from the pressure suppression pool in a manner that will reduce the temperature uniformly throughout the pool.

It is desirable to withdraw water at the point where the highest temperature exists. However, if the pool is well mixed, it does not make any difference where the suction takes place as long as returning cold water is not short-circuited back into the suction line.

At Clinton, RHR discharge nozzles are directed such that a circumferential flow is established in the suppression pool. The one-tenth scale test performed for Perry Nuclear Power Plant revealed that this type of flow is the optimum for pool mixing. Clinton's configuration and nozzle orientation prevent short-circuiting of the flow since its discharge nozzles are 180° apart.

Only one RHR loop is necessary to adequately cool and mix the suppression pool. However, if two RHR loops are used, the cooling and mixing would be enhanced.

The Perry tests showed that:

1. The optimum jet angle was found to be 55^{o} from the radial axis which is the same discharge angle as Clinton. No stangnant areas around quenchers or elsewhere were observed.

2. These studies showed that short-circuiting of flow in the suppression pool did not occur.

In-Plant Test at Monticello, Caorso, and Kuo-Sheng Nuclear Power Plants have also been conducted for the questions in point. Tests showed the following results:

1. The RHR system discharge jet is an effective means of producing bulk motion and adequate thermal mixing of the suppression pool. Three to four minutes of operation of one RHR system loop can produce an average suppression bulk velocity in an initially quiescent pool which is more than adequate.

As regards the bulk motion of the suppression pool, several points should be remembered:

1. Circumferential bulk motion is the type of flow which minimizes local hot spots in the pool from stuck open SRV and local exhaust points.

2. In fact, many other mechanisms contribute to and are essential for thermal mixing such as:

   Secondary flow patterns induced by RHR suction, ECCS suction and discharge, quencher discharge, and turbulence caused by submerged structures in the suppression pool.

   Free convection which is particularly effective in pool mixing when the cold water is exhausted near the top of the pool.

   Existing tests provide sufficient support for the above conclusions; additional testing is not necessary and this issue is closed.

Action Plan 17

4.8 Operation of the RHR system in the containment spray mode will decrease the heat transfer coefficient through the RHR heat exchangers due to decreased system flow. The FSAR analysis assumes a constant heat transfer rate from the suppression pool even with operation of the containment spray.

Response

To address this concern, additional analyses were completed which incorporated lower RHR heat exchanger heat transfer coefficients during the period when the RHR system is in the containment spray mode. The analyses were performed both with and without the presence of the bypass leakage capability.

Realistic analyses accounting for non-equilibrium between the suppression pool and containment atmosphere have been completed as part of Action Plan 10. These analyses have conclusively demonstrated that even if the drywell remains pressurized, the containment pressure does not increase to the set point for spray actuation when drywell bypass leakage is not present.

A new analysis of long term containment response, assuming full capability bypass leakage, was completed as part of Action Plan 19. This analysis showed that the peak suppression pool temperature does not exceed the design temperature with the containment spray operating continuously. The analysis accounted for the lower heat transfer coefficient which would result from reduced flow through the heat exchanger. The analysis assumed that the pool is not in thermal equilibrium with the containment atmosphere and that heat transfer will occur between the atmosphere and containment and drywell heat sinks.

IP has demonstrated that with full bypass leakage capability, the long term suppression pool temperature does not exceed design values. Other analyses have shown that the containment sprays will not be actuated when bypass leakage is not present. Therefore, the effect of reduced RHR heat exchanger heat transfer coefficient when the RHR system is in the spray mode is insignificant and this issue is closed.

Action Plan 20

5.4 Direct leakage from the drywell to the containment may dissipate hydrogen outside the region where the hydrogen recombiners take suction. The anticipated leakage exceeds the capacity of the drywell purge compressors. This could lead to pocketing of hydrogen which exceeds the concentration limit of 4% by volume.

Response

The CPS design has hydrogen compressors as described in the CPS FSAR Section 6.2.5 rather than drywell purge compressors. The CPS design has a definite advantage because it takes suction from the drywell and exhausts to the suppression pool rather than exhausting to the pressurized drywell as the GGNS system is designed. Therefore, direct leakage from the drywell to the containment is reduced because of a lower drywell pressure.

A review of the CPS design was done with results summarized below showing that the CPS design has no problem of hydrogen pocketing which may exceed the concentration limit of 4% by volume.

1. All electrical penetrations through the drywell are located between elevations 755'-0" and 789'-0". The majority of the penetrations are located in the HCU floor area between elevations 755'-0".

2. Division I and II Hydrogen Recombiner Systems have suction and returns on the containment side of these floors. The Division I suction is located at elevation 789'-0" and the return is located at elevation 760'-0". For the Division II system, both the suction and return are located at elevation 760'-0".

3. The floors at elevation 755'-0" and 778'-0" are mainly grating and contain no enclosed areas or cubicles.

Therefore, any hydrogen leaked through an electrical penetration would be discharged into an open area of the containment which is serviced by the hydrogen recombiner system. A detailed review of the CPS design drawing has further confirmed that potential areas of pocketing of the hydrogen are non-existent. Also, it has been confirmed that structural protrusions which may obstruct flow in this area do not exist. Thus, this issue is closed for Clinton.

Action Plan 23

6.3 The recombiners may produce "hot spots" near the recombiner exhausts which might exceed the environmental qualification envelope or the containment design temperature.

6.5 Discuss the possibility of local temperatures due to recombiner operation being higher than the temperature qualification profiles for equipment in the region around and above the recombiners. State what instructions, if any, are available to the operator to actuate containment sprays to keep this temperature below design values.

Response

To resolve this for Clinton a description of the recombiner system and an evaluation of the exhaust temperatures from the recombiners will be discussed.

As described in the Clinton FSAR Section 6.2.5, the hydrogen recombiner package is outside the containment and the most significant heat released from the exothermic reaction is removed by the control buildings HVAC system. The water vapor returning back to the containment is less than 150°F and is no threat to containment integrity or any safety related equipment in the region around the recombiner exhaust. Thus, this issue is closed for Clinton.

Action Plan 24

7.2 Incorrect parameters used to calculate environmental qualifications.

Response

Environmental qualification parameters associated with containment and suppression pool temperatures were determined by taking into account the addition of feedwater due to a SBA plus containment spray effects. An analyses of the environmental qualification curves was based on NUREG-0588. Standard licensing assumptions were utilized for the CPS unique margin analyses except that finite mass and heat transfer coefficients between the suppression pool and air space were used to a more realistic air space pressure and temperature response.

CPS margin analyses show that the NUREG-0588 criteria have been met and that the predicted parameters are significantly lower than the CPS environmental qualification parameter requirements.

Action Plan 27

8.4 Describe all of the possible methods both before and after an accident of creating a condition of low air mass inside the containment. Discuss the effects of the containment design external pressure of actuating the containment type.

Response

Scenarios leading to reduction of containment air mass:

(a) Initiating events: small, undetected break or leak in the containment airspace.

Sequence of events:

- o containment airspace temperature rises due to energy input from the break
- o containment air is being lost through the open ventilation system due to thermal expansion, and steam addition to the airspace.
- o containment isolation on high radiation level or operator action.

(b) Initiating event: loss of containment HVAC.

Sequence of events:

- o containment airspace temperature rises
- o containment air is being lost through the open ventilation system due to thermal expansion.

(c) Initiating event: LOCA

Sequence of events:

- o containment isolation on LOCA signal (psig in drywell)
- o upper pool dump (UPD) occurs at 30 minutes post - LOCA
- o hydrogen mixing compressors are put in operation by the operator
- o air is being transferred from containment to drywell, until the top row of vents is uncovered. That requires, assuming previous UPD, approximately 6 psi pressure differential between drywell and containment.

Effect on negative pressure transients due to spray operation:

In scenarios (a) and (b) the containment heat-up is sufficiently slow for the operator to react before any significant loss of containment air occurs. It is estimated that it would take more than 2 hours to increase the containment temperature by 20°F, in the case of the small steam break, and more in the case of HVAC failure. The operator will have enough time and information (containment temperature indication) to prevent excessive containment air loss. Therefore, the containment negative design pressure will not be exceeded even if the sprays are actuated.

Scenario (c) will not have adverse impact on the containment negative pressure transient because the air will be again redistributed via the vent sysyem.

Action Plan 30

10.2 Describe the interface requirement (A-42) (sic) that specifies that no flooding of the drywell shall occur. Describe your intended methods to follow this interface or justify ignoring this requirement.

Response

The requirements of GE specification (A62-4300) 22A7411, Paragraph 4.2.5 as mentioned in Item 1 of the Program for Resolution reads "The suppression pool weir wall height shall provide sufficient freeboard volume to accept a dump of the upper pool without resulting in overflow flooding into the drywell. The freeboard height shall be measured between the top of the weir wall and HWL which is 7'-6" above the top vent center line."

The initial approach used to satisfy this requirement assumed a normal upper pool level and a high suppression pool water level (and did not consider maximum negative drywell pressure and/or encroachments). The suppression would not overflow the weir wall in the event of upper pool dump with these initial assumptions.

Based upon the above response, Illinois Power considers this issue closed.

Action Plan 34

19.1 Submergence Effects on Chugging Loads

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 effect chugging loads.

Response

An evaluation was made to see if there was an effect or relationship of mass flux, submergence, or low velocity bubbles upon chugging loads from vent noncondensibles.

In addition, the bounding effect of vent submergence on chugging loads was quantified, and it was shown that existing local and global load definitions adequately bound increased submergence effects on chugging pressure loads.

In order to perform this evaluation, it was necessary to do an analysis of the situation using the spherical flow momentum equation, a three-dimensional modeling technique; and reviewing test data from a GE - "Full Scale Condensation and Stratification Phenomena - Test Series 5707".

It was found that 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 submergence level.

The hydrodynamic mass, M_h, associated with spherical bubble collapse is found from:

$$\tfrac{1}{2} M_h \dot{R}^2 = \int_R^{\infty} \tfrac{1}{2}(\rho 4\pi r^2 dr)\dot{R}^2 \left(\frac{R}{r}\right)^4 \qquad (1)$$

where R = bubble radius
$\dot{R}$ = bubble surface collapse rate

Here, the right hand side represents the summing of the kinetic energy of each differential element of mass between the bubble surface and infinity. The result is that:

$$M_h = 3\left(\rho \frac{4}{3}\pi R^3\right) \qquad (2)$$

Equation (2) implies that the majority of the mass affecting bubble collapse is contained within a volume of water surrounding the bubble that is three times that of the bubble volume. This volume of water is contained in a shell of thickness

$$t = R\left(\sqrt[3]{4} - 1\right)$$

or $\quad t = 0.6R$

The results obtained from this equation show that the effects on the hydrodynamic mass of the steam bubble by surface water effects is negligible. It also shows that the collapse of steam bubbles is inertia controlled and not controlled by mass flux or low velocity bubbles. In fact, when one speaks of chugging loads we are referring to the oscillatory pressure loads imparted to structures as a result of the unsteady, transient behavior of the condensation of the steam (released during a LOCA) at lower vent-flow rates which are characterized by a series of random pulses that are typically a second or more apart.

Visual observations of chugging bubble collapse during full scale Mark III chugging tests indicate the bubble radius is comparable to the vent radius, and the collapse location is near the top vent. This means at the normal top vent submergence of 7.5 feet, the surface is approximately 7 bubble radii away from the bubble center for a 27 inch steam vent pipe bubble and that the additional hydrodynamic mass effect of increased submergence is also negligible. This means that the first mechanism affecting condensing steam bubble collapse is unaffected but that the second mechanism is.

The second mechanism of steam bubble collapse which is the differential pressure between the condensed bubble interior and the normal hydrostatic water pressure will now be increased 4.5 feet above the top of the submerged vent. Based on a wetwell atmospheric pressure of 17.5 psia, the maximum chug pressure spike will be increased by 9.4% in hydrostatic pressure.

In addition, chugging pressure acoustic transmission will be altered and there will be an increase of the wetted wall area to receive acoustic transmission and chugging pressure loading.

A three-dimensional acoustic model of the suppression pool was used to quantify the effects of the increased hydrostatic pressure and chug pressure on chugging loads. Actual test data from the Full Scale Condensation Test -5707 which yielded the maximum and mean chug pressure in a clean pool was input into the acoustic model with and without encroachments in place. Using amplified response spectra, pool boundary loads were developed for the drywell wall, containment mat, and basemat. The results gave us a comparison between chugging local and global loads with maximum and mean chugs from the Full Scale Test Data. These same comparisons were then made with factors in Table 1 which increase the Full Scale Test Data by a factor of 9.4% to account for increased submergence and hydrostatic pressure.

Table 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%

Global load results were then obtained and compared with the GESSAR II global load definition for the three suppression pool boundary areas. The results showed that the GESSAR load definitions have sufficient margin to bound any submergence effects. Figures 1, 2, & 3 show those results.

Local load results were then obtained as shown in Figures 4, 5 & 6. The results showed the following:

1. The drywell wall GESSAR II local load definition bounds the increased maximum chug load or the encroached load everywhere as in Figure 4.

2. The containment wall has a slight load exceedance from 15 Hz to 32 Hz; however its energy input is slight compared to the total load energy definition as in Figure 5.

3. The basemat has an exceedance of 35% in the frequency range in Figure 6 to 12 Hz to 22Hz. This load exceedance is not of any consequence since it is a local load and the basemat liner is the structure involved. The hydrostatic head of the suppression pool on the basemat insures that a negative pressure will never be imposed on the liner. In addition, the liner is backed by concrete so that 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.

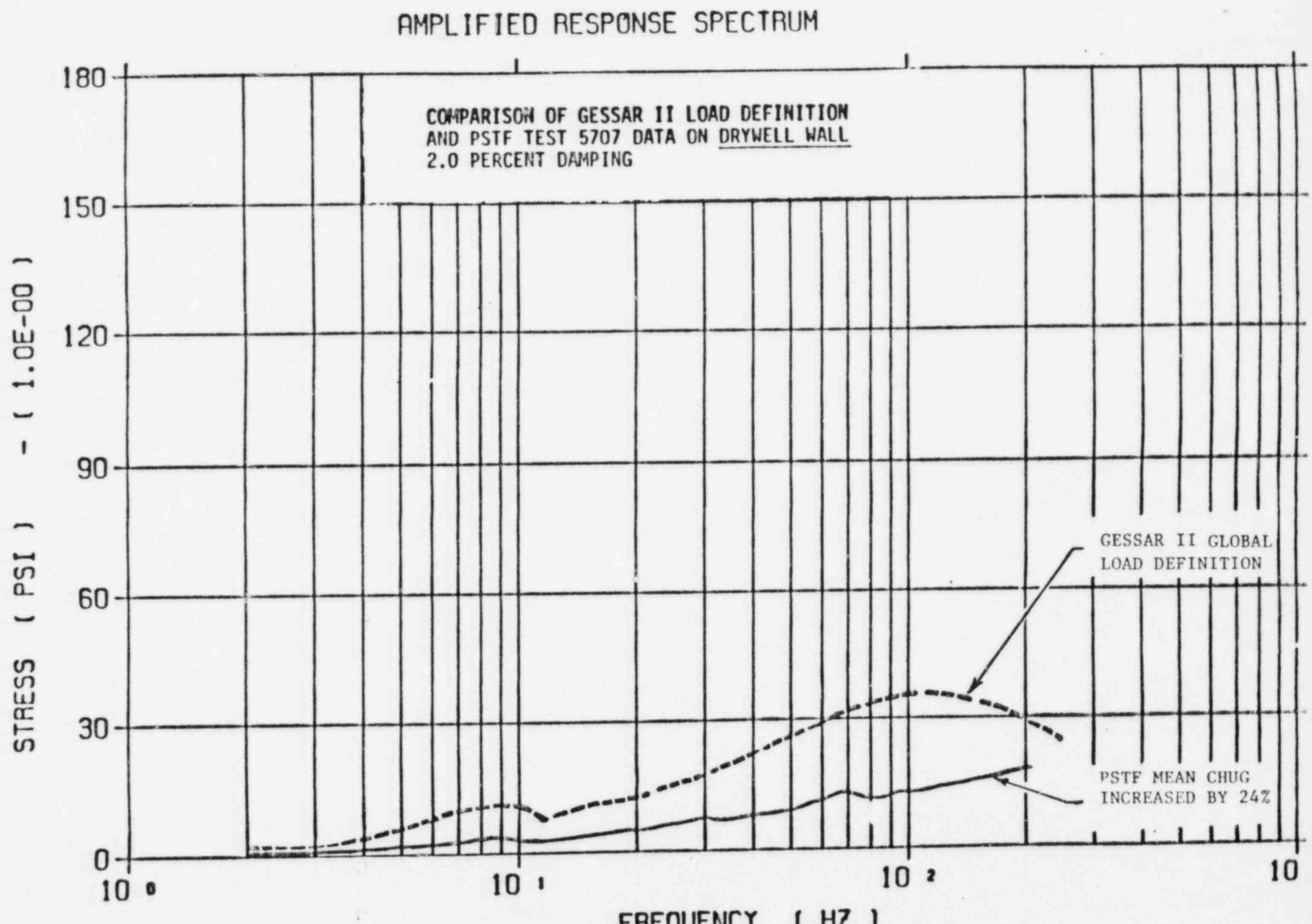
AMPLIFIED RESPONSE SPECTRUM
COMPARISON OF GESSAR II LOAD DEFINITION
AND PSTF TEST 5707 DATA ON DRYWELL WALL
2.0 PERCENT DAMPING
180
150
120
90
60
30
0
STRESS (PSI) - (1.0E-00)
10 0
10 1
10 2
10
FREQUENCY (HZ)
GESSAR II GLOBAL
LOAD DEFINITION
PSTF MEAN CHUG
INCREASED BY 24%

FIGURE 1

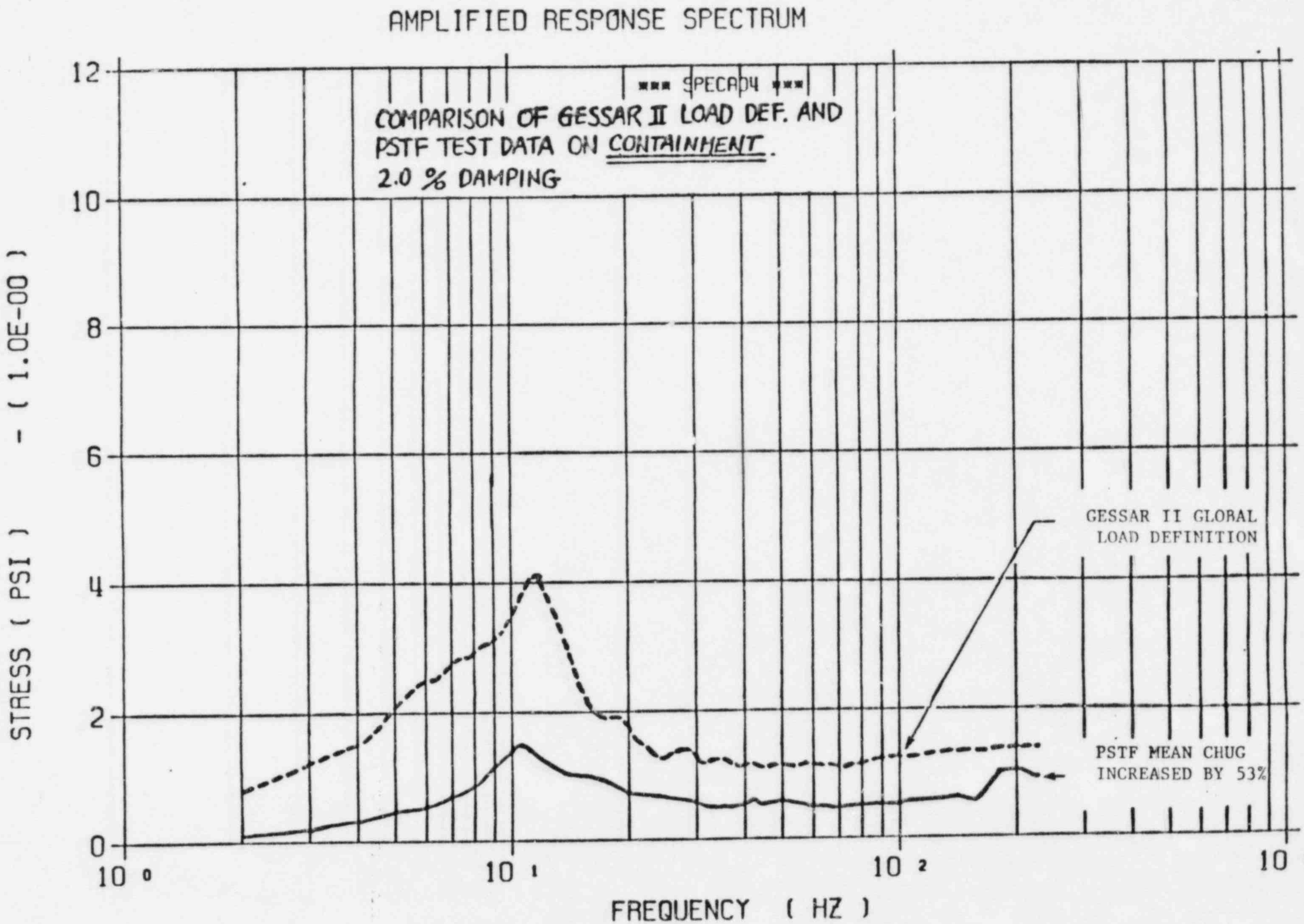
AMPLIFIED RESPONSE SPECTRUM
*** SPECAP4 ***
COMPARISON OF GESSAR II LOAD DEF. AND
PSTF TEST DATA ON CONTAINMENT.
2.0 % DAMPING
GESSAR II GLOBAL
LOAD DEFINITION
PSTF MEAN CHUG
INCREASED BY 53%
STRESS (PSI) - (1.0E-00)
FREQUENCY (HZ)
0
2
4
6
8
10
12
10 0
10 1
10 2
10 3

FIGURE 2

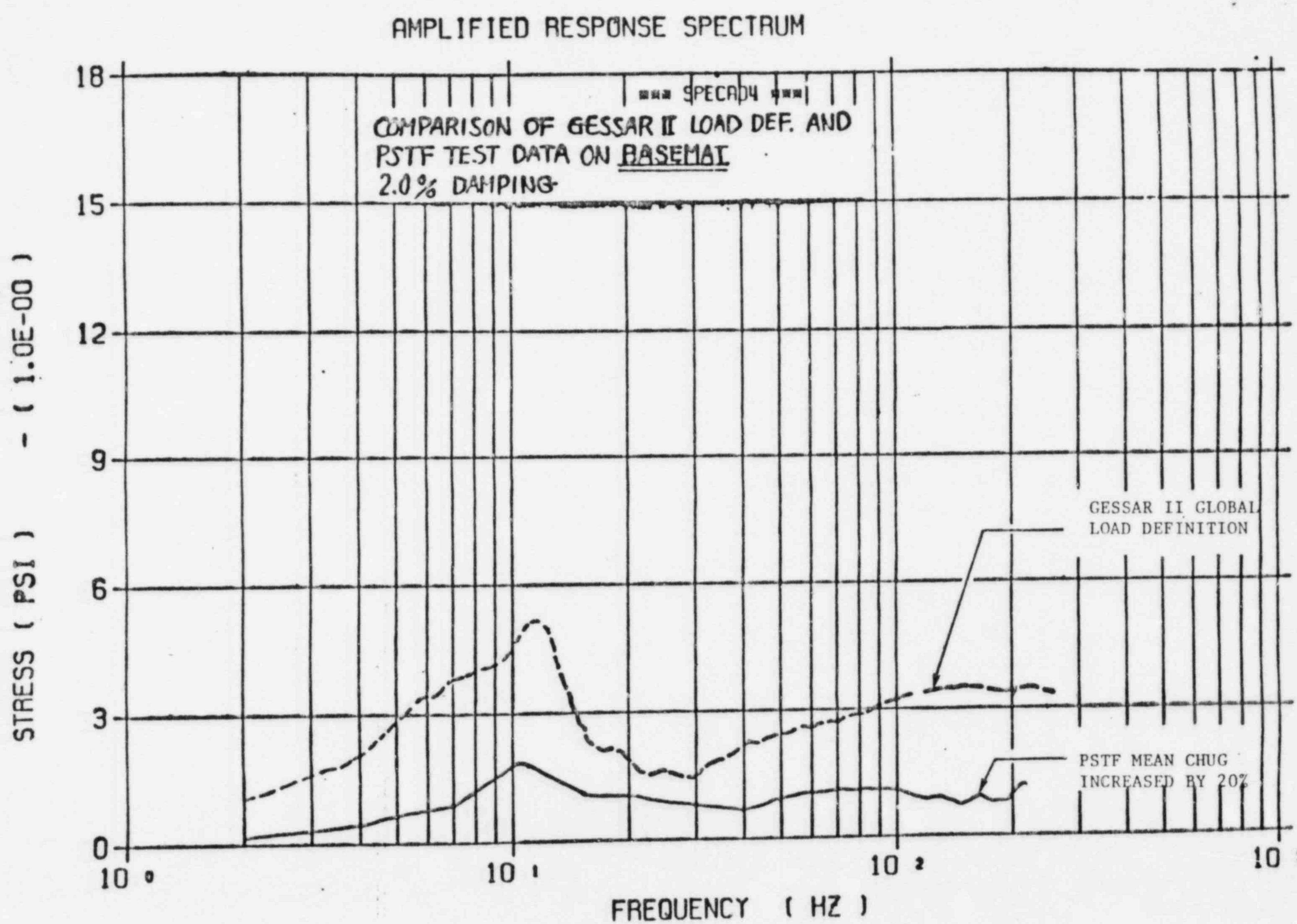
AMPLIFIED RESPONSE SPECTRUM
*** SPECAD4 ***
COMPARISON OF GESSAR II LOAD DEF. AND
PSTF TEST DATA ON BASEMAT
2.0% DAMPING
GESSAR II GLOBAL
LOAD DEFINITION
PSTF MEAN CHUG
INCREASED BY 20%
STRESS (PSI) - (1.0E-00)
FREQUENCY (HZ)
18
15
12
9
6
3
0
10 0
10 1
10 2
10 3

FIGURE 3

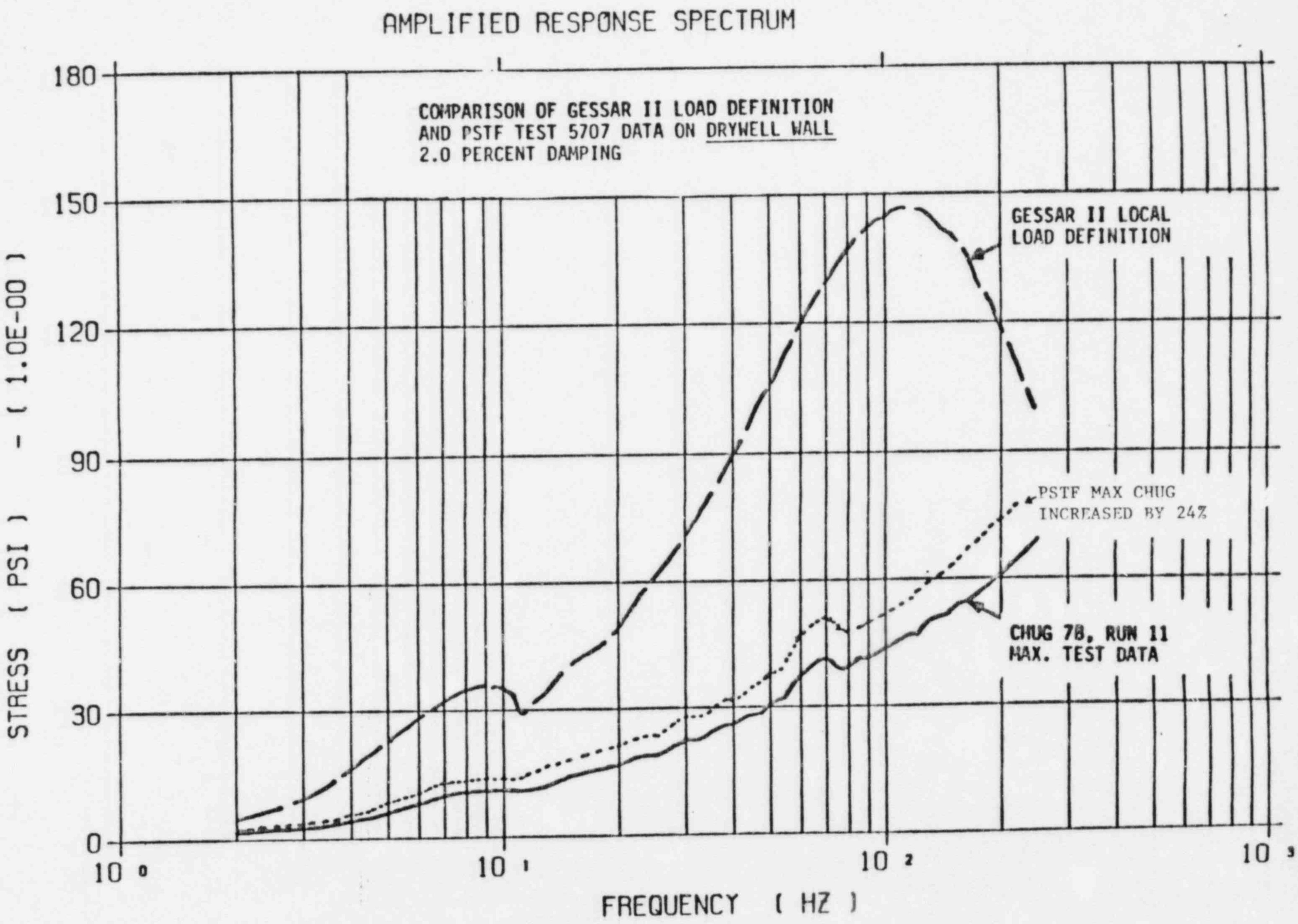
AMPLIFIED RESPONSE SPECTRUM
COMPARISON OF GESSAR II LOAD DEFINITION
AND PSTF TEST 5707 DATA ON DRYWELL WALL
2.0 PERCENT DAMPING
GESSAR II LOCAL
LOAD DEFINITION
PSTF MAX CHUG
INCREASED BY 24%
CHUG 7B, RUN 11
MAX. TEST DATA
STRESS (PSI) - (1.0E-00)
180
150
120
90
60
30
0
10 0
10 1
10 2
10 3
FREQUENCY (HZ)

FIGURE 4

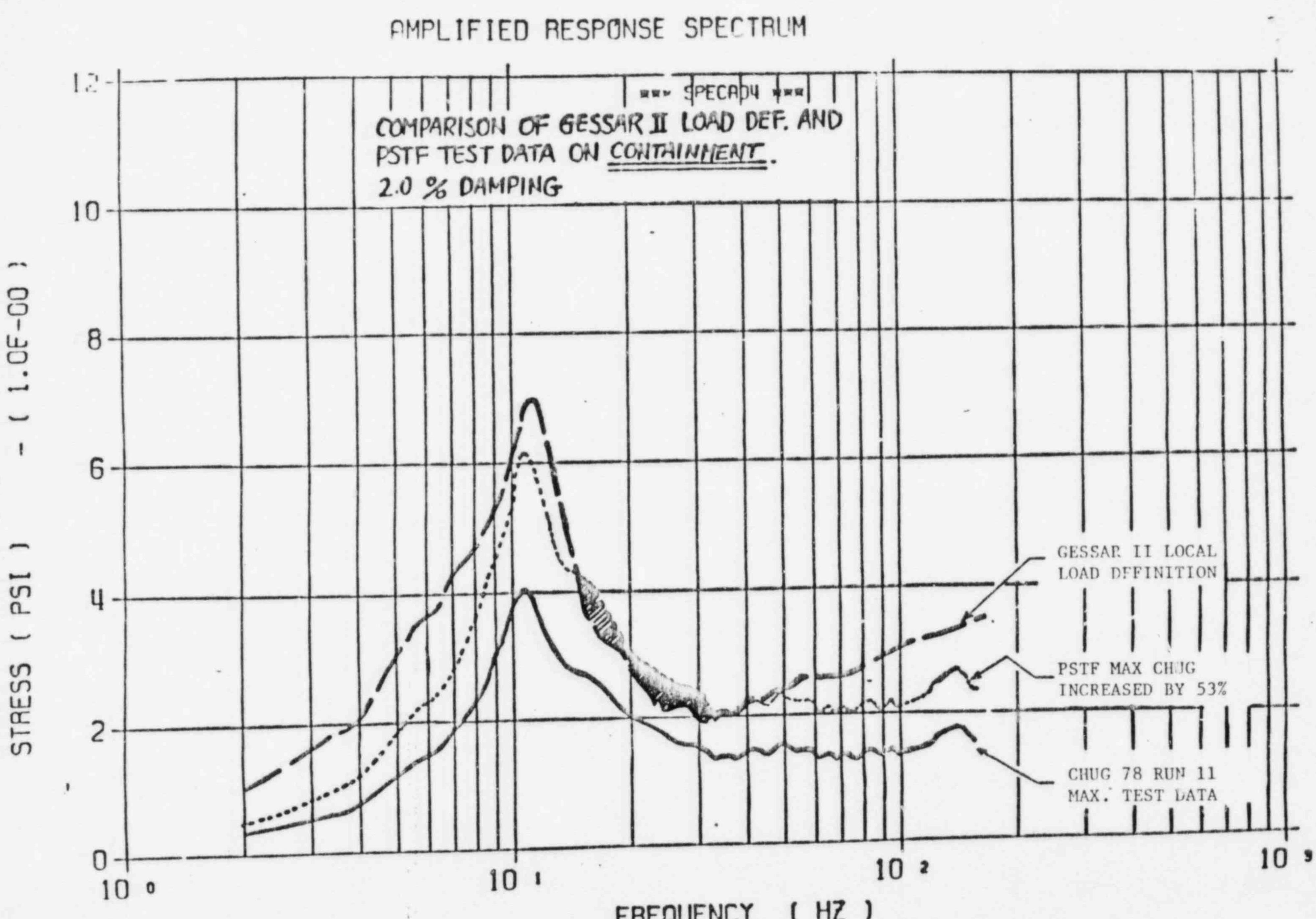
AMPLIFIED RESPONSE SPECTRUM
*** SPECAD4 ***
COMPARISON OF GESSAR II LOAD DEF. AND PSTF TEST DATA ON CONTAINMENT.
2.0 % DAMPING
GESSAR II LOCAL LOAD DEFINITION
PSTF MAX CHUG INCREASED BY 53%
CHUG 78 RUN 11 MAX. TEST DATA
STRESS (PSI) - (1.0E-00)
FREQUENCY (HZ)

FIGURE 5

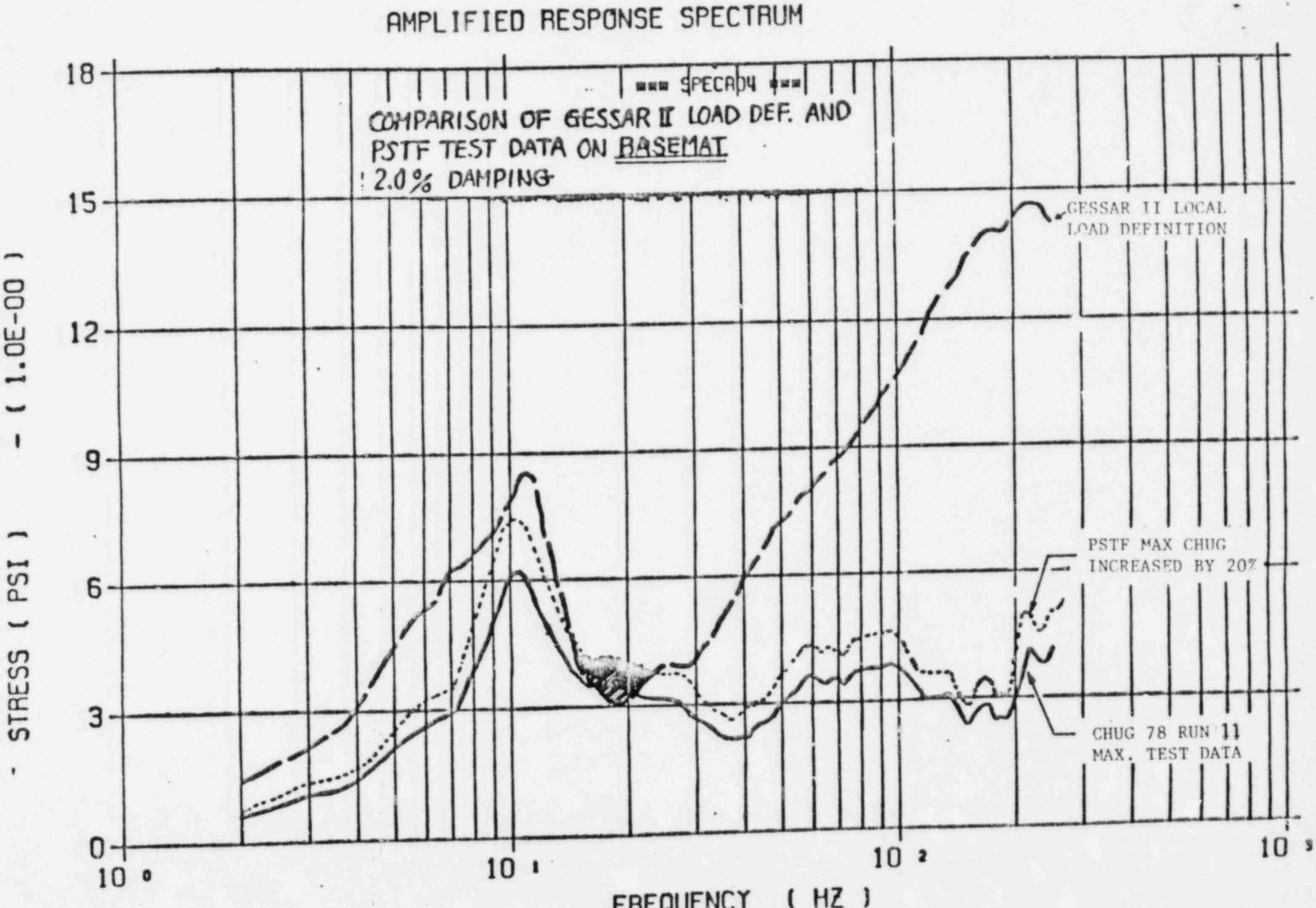
AMPLIFIED RESPONSE SPECTRUM
*** SPECA04 ***
COMPARISON OF GESSAR II LOAD DEF. AND
PSTF TEST DATA ON BASEMAT
2.0% DAMPING
GESSAR II LOCAL
LOAD DEFINITION
PSTF MAX CHUG
INCREASED BY 20%
CHUG 78 RUN 11
MAX. TEST DATA
- STRESS (PSI) - (1.0E-00)
FREQUENCY (HZ)
18
15
12
9
6
3
0
10 0
10 1
10 2
10 3

FIGURE 6

Action Plan 35

19.2 Effect of local encroachment on chugging.

The effect of local encroachments on chugging loads needs to be addressed.

Response

An evaluation was made to quantify inertial impedance effects and the impact of longer acoustic paths on chugging loads. The evaluation was conducted using the same techniques as in 19.1. The same conditions exist in 19.2 as in 19.1.

Since the collapse of steam bubbles during chugging is inertia controlled, there will be no encroachment effect on bubble collapse rate. The excitation of the suppression pool is independent of the pressure of an encroachment. Chug source pressures measured in unencroached test such as GE-"Full Scale Condensation and Stratification Phenomena-Test Series 5707" are valid as input to the three-dimensional acousitc model of the suppression pool. Actual test data from the Full Scale-Test yielding the maximum chug pressure in a clean pool was input into the acoustical model with and without the encroachment in place. The maximum pressure at each location on the containment wall was averaged to obtain the area-averaged peak pressure on the wall for both the encroached and unencroached cases. A ratio of the pressures between cases was found. This ratio was applied to the amplified response spectra (ARS) of the maximum measured PSTF chug and then compared with the ARS of the local load definition. The local load definition was used for comparison because the TIP encroachment only covers a small portion of the total pool (< 5%).

The above procedure was also performed on the drywell wall and the basemat. Comparison plots are shown in Figures 1 through 3. Figure 1 shows that a small amount of exceedance exists on the containment wall. This exceedance is 15% at its maximum and is judged to be negligible because, as can be seen, the integrated area of exceedance (indicative of energy) is very small in the range of concern. Furthermore, the total load definition energy easily bounds that of the encroached signal energy. Figure 2 shows that the drywell wall load definition bounds the encroached load everywhere. Finally, Figure 3 shows that there is up to 60% exceedance of the basemat local load definition in the frequency range from 12 Hz to 22 Hz.

This load exceedance is not of any consequence since it is a local load and the basemat liner is the structure involved. The hydrostatic head of the pool is greater than 8 psid (18.5 feet of submergence). This 8 psid insures that the liner will never see a negative pressure in the frequency range of exceedance, and, since the liner is backed by concrete everywhere, no natural modes in this range are excitable.

It is therefore concluded that the existing load definition adequately covers the localized effects of the TIP encroachment.

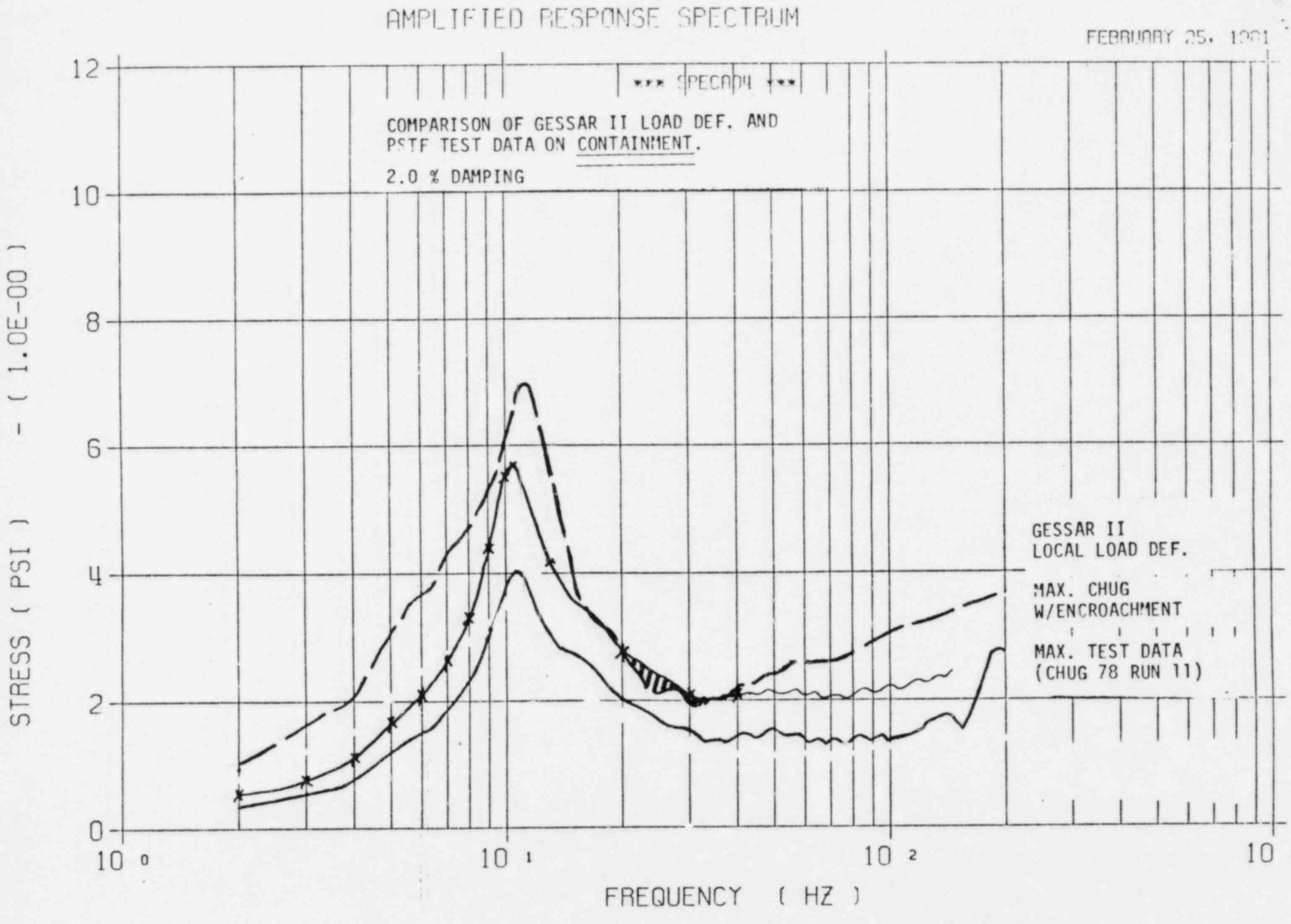
AMPLIFIED RESPONSE SPECTRUM
FEBRUARY 25, 1981
*** SPECA04 ***
COMPARISON OF GESSAR II LOAD DEF. AND
PSTF TEST DATA ON CONTAINMENT.
2.0 % DAMPING
GESSAR II
LOCAL LOAD DEF.
MAX. CHUG
W/ENCROACHMENT
MAX. TEST DATA
(CHUG 78 RUN 11)
STRESS (PSI) - (1.0E-00)
12
10
8
6
4
2
0
10 0
10 1
10 2
10 3
FREQUENCY (HZ)

Figure 1

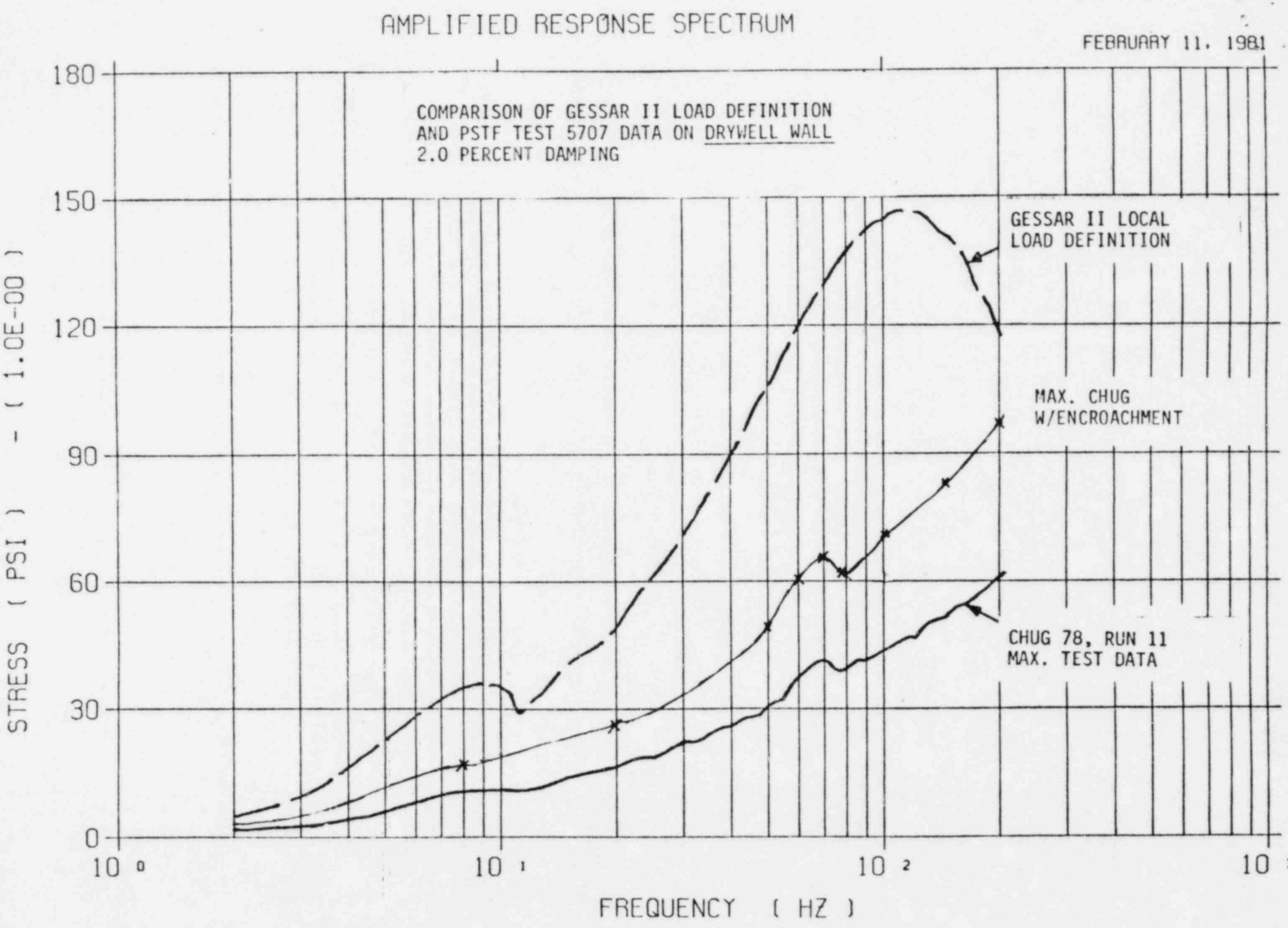
AMPLIFIED RESPONSE SPECTRUM
FEBRUARY 11. 1981
COMPARISON OF GESSAR II LOAD DEFINITION
AND PSTF TEST 5707 DATA ON DRYWELL WALL
2.0 PERCENT DAMPING
GESSAR II LOCAL
LOAD DEFINITION
MAX. CHUG
W/ENCROACHMENT
CHUG 78, RUN 11
MAX. TEST DATA
STRESS (PSI) - (1.0E-00)
180
150
120
90
60
30
0
10^0
10^1
10^2
10^3
FREQUENCY (HZ)

Figure 2

PL 1

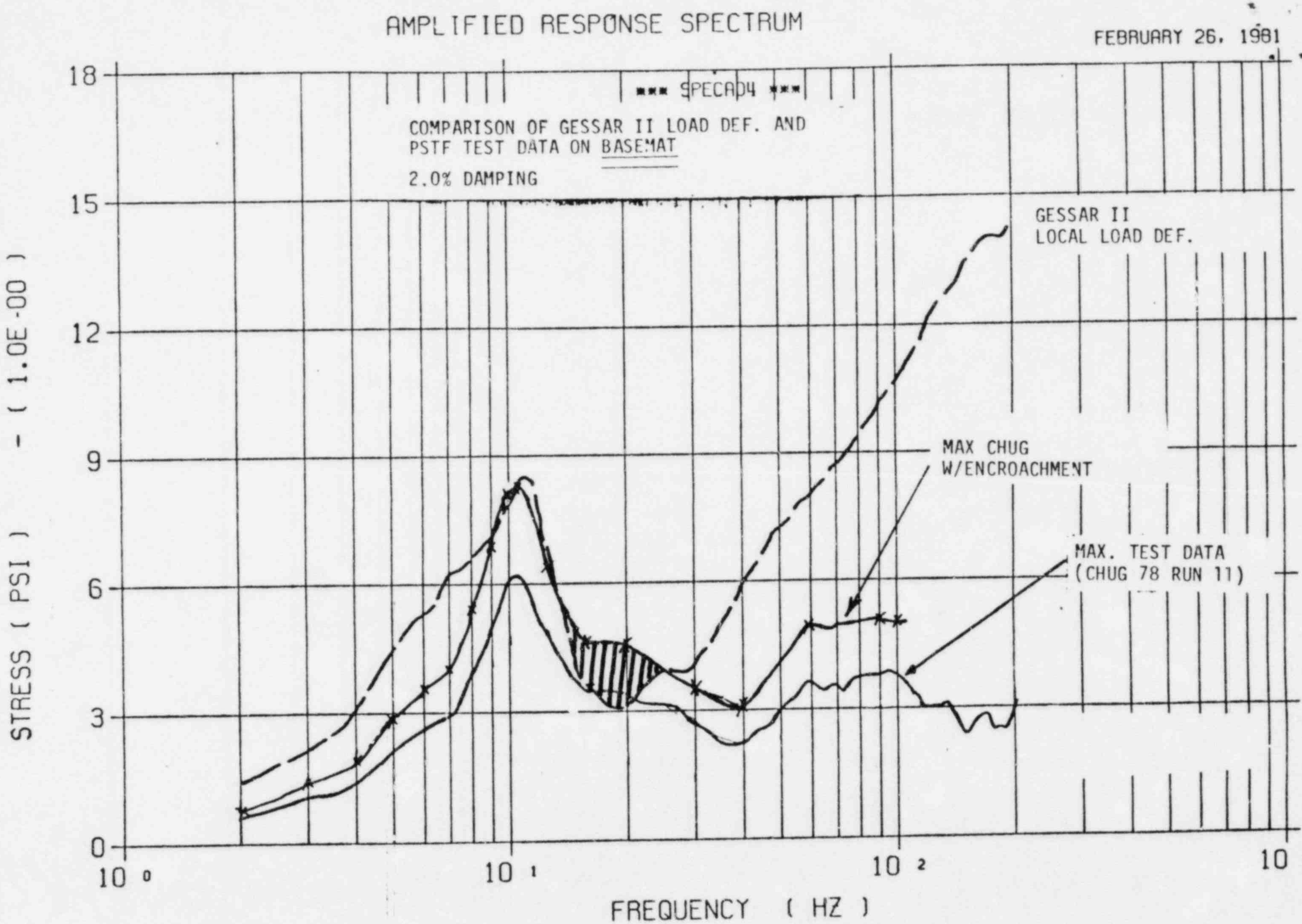
AMPLIFIED RESPONSE SPECTRUM
FEBRUARY 26, 1981
*** SPECAD4 ***
COMPARISON OF GESSAR II LOAD DEF. AND
PSTF TEST DATA ON BASEMAT
2.0% DAMPING
GESSAR II
LOCAL LOAD DEF.
MAX CHUG
W/ENCROACHMENT
MAX. TEST DATA
(CHUG 78 RUN 11)
STRESS (PSI) - (1.0E-00)
18
15
12
9
6
3
0
10 0
10 1
10 2
10 3
FREQUENCY (HZ)

Figure 3

PL 1

Action Plan 36

20.0 During the latter stages of a LOCA, ECCS overflow from the primary system can cause drywell depressurization and vent backflow. The GESSAR defines vent backflow, vertical impingement, and drag loads to be applied to drywell structures, piping, and equipment, but no horizontal loading is specified.

Response

No action is required based on discussion between MP&L and the NRC Staff. The basis for this decision is applicable to CPS.

Results were submitted in MP&L's submittal (Reference No. AECM-82/353 dated August 19, 1982). This item is closed for CPS.

Action Plan 37

22.0 The EPGs currently in existence have been prepared with the intent of coping with degraded core accidents. They may contain requirements conflicting with design basis accident conditions. Someone needs to carefully review the EPGs to assure that they do not conflict with the expected course of the design basis accident.

Response

The Owners Group believes that the development program through which the emergency procedure guidelines have passed has adequately addressed this concern. As a result of this issue, the Mark III Owners Group has brought this concern to the attention of the BWR Owners Groups. A generic resolution of this issue will be pursued with the BWR Owners Group. IP believes that for Clinton Power Station, this issue is closed.