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DISTRIBUTION FOR REACTOR VESSEL SUPPORT INFO
FOR OPERATING REACTORS PER MR. TRAMMELL 7-12-76

ENCLOSURE

Reactor Support System Evaluation of Margins ....consisting of report furnishing analysis of reactor pressure vessel support system loads subsequent to a postulated cold leg break & restraint capability....

3/8 inches

40 ENCL.

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FLORIDA POWER & LIGHT COMPANY

August 30, 1977
L-77-265

Office of Nuclear Reactor Regulation
Attn: Mr. Don K. Davis, Acting Chief
Operating Reactors Branch #2
Division of Operating Reactors
U. S. Nuclear Regulatory Commission
Washington, D.C. 20555

Dear Mr. Davis:

Re: St. Lucie Unit No. 1
Docket No. 50-335
Reactor Pressure Vessel Support System

By letter of February 9, 1976 (L-76-49) Florida Power & Light committed to perform an analysis to determine reactor pressure vessel support system loads subsequent to a postulated cold leg break, and to evaluate the restraint capability and compute the safety margins of that system.

Such analysis is now complete and a report responsive to that commitment is attached.

Yours very truly,

J. A. De Mastry

fm Robert E. Uhrig
Vice President

REU/MV:ltm
Attachment

cc: Robert Lowenstein, Esq.

7712560164

REACTOR SUPPORT SYSTEM
EVALUATION OF MARGINS

St Lucie 1 - Docket No. 50-335

St Lucie 2 - Docket No. 50-389

August 1977

1.0 INTRODUCTION

In May 1975 the NRC Staff was informed by a pressurized water reactor licensee that loads resulting from a hypothetical rupture of the reactor coolant cold leg pipe in the immediate vicinity of the reactor pressure vessel (RPV) may have been underestimated. The Staff evaluated the hypothesis and methodology required to analytically predict these loads and concluded in November 1975 that the consideration of these loads should be evaluated on a generic basis.

Florida Power and Light's (FP&L's) letter(s) of 30 December 1975 responded to the Staff's generic letter of 28 November 1975. It indicated that the support system design incorporated the ability to accommodate the reaction forces associated with the large arbitrary reactor coolant pipe ruptures postulated to assess ultimate capability of the containment structure and the emergency core cooling system. It went on to note that the St Lucie design had been evaluated, and shown to acceptably accommodate, the additional loads associated with differential pressures within the reactor cavity.

The Staff requested that further internal asymmetric load (IAL) evaluations be conducted. FP&L's letter of 9 February 1976 documents the Company's commitments in this regard. Supplement 2 to the Unit 1 Safety Evaluation Report (SER) dated 1 March 1976 and Supplement 1 to the Unit 2 SER dated 3 March 1976 restate this commitment.

FP&L in conjunction with the CE - Reactor Owners Group (CE-ROG) initiated an evaluation of reactor coolant pipe failures. Science Applications Incorporated (SAI) conducted the study on behalf of the CE-ROG. The SAI study provides a new and substantive aspect, as discussed hereinafter, to this generic concern. FP&L's letter (s) of 28 April 1977 summarize the results of this study. It strongly suggests that further evaluation of the IAL may not be warranted. This nonwithstanding, the NRC's letter of 6 June 1977 indicated that the Staff would still require FP&L to evaluate the reactor vessel (RPV) supports in accordance with the commitments made in the 9 February 1976 letter.

The discussion that follows provides the technical evaluation data required by the 9 February 1976 letter. It is provided to be responsive to the Staff's wishes in this regard. In FP&L's opinion, this type of evaluation is not appropriate until the SAI report and similar studies are fully evaluated by NRC and the ACRS.

2.0 EVALUATION VIS-A-VIS ALTERNATIVES

The IAL load would exist and presumably can be accurately predicted analytically subsequent to an essentially instantaneous cold leg break in the immediate vicinity of the RPV. The resulting decompression wave can be followed by pseudo-three dimensional computer models to estimate forces resulting from transient pressure imbalances within the RPV and across RPV internals. To place the phenomenon in perspective, it must be recognized that a very short break opening time must be postulated to analytically predict large loads. If the break opening time is equal to, or longer than the time required for a sonic wave to move from the fault location to the opposite side of the vessel and return (15 msec), then the IAL will be inconsequential when compared to other loads. Thus, only break opening times less than 10 to 15 msec are relevant to IAL considerations.

In addition to a very short opening time, the break must be postulated at a location very close to the RPV. If the break were located outboard of the RPV's biological shield wall, the reactor cavity external asymmetric load (EAL) would fall to zero. Thus, the most severe loading conditions results from breaks inboard of the RPV biological shield, i.e., where both the IAL and EAL contribute to the load imposed on the RPV support.

The SAI report "An Analysis of the Relative Probability of Failure at Various Locations in the Primary Cooling Loop of a Pressurized Water Reactor Including the Effects of a Periodic Inspection" (SAI-001-PA) is currently under NRC/ACRS review. It indicates that the probability of a large rupture in the primary coolant loop is very low. It also demonstrates that inservice inspection can appreciably reduce this ratio.

The CE-ROG (by letter dated 9 June 1977) has submitted for inclusion in the record of the 206th ACRS Meeting its position with regard to the SAI report. This statement is provided as Attachment 1. It summarizes the CE-ROG effort and opinion with regard to the SAI evaluation.

The size of the flaw required to initiate a cold-leg guillotine has been considered in the SAI report and is about 0.862" deep x 8" long. The size of a flaw which could conceivably grow to this size over a 10 year period is about 0.2" deep x 2" long. This flaw size is above the code allowable flaw size and is well above the lower level of detectability of currently available non-destructive examination techniques. The probability of not detecting this flaw size is negligible.

In summary, the IAL condition is of concern for essentially instantaneous pipe breaks at very specific locations in the reactor coolant system. Large pipe breaks in the reactor coolant system are very low probability events, and those relevant to the IAL of even lower probability. Thus it can be concluded, that for operating plants such as Unit 1 and replicate facilities with a construction permit (Unit 2), detailed evaluations of the IAL are unwarranted. This is consistent with the intent of 10 CFR 50.109 since substantial additional protection of the public health and safety will not result from additional analyses.

The SAI report demonstrates that a viable alternate to detailed analytical evaluations is available through augmented inservice inspection. For facilities, where physical means to incorporate augmented inservice inspection (ISI) capability exists, a significant reduction in primary loop failures relevant to the IAL can be achieved. Augmented ISI will enhance plant safety through elimination of flaws that could conceivably lead to pipe failure. Whereas, analyses of the ability to accommodate loads resulting from arbitrary pipe failures would define, at great cost, RPV support margins. Augmented ISI eliminates the cause in lieu of attempting to mitigate the consequences.

3.0 SUMMARY OF ANALYTICAL TECHNIQUES

FP&L's letter of 9 February 1976 documented its comitment to evaluate the IAL for a guillotine break at the cold-leg nozzle. The IAL would be analyzed in conjunction with the previously analyzed pipe reaction and EAL loads to:

1. determine the loads in the reactor vessel support system,

2. evaluate the full restraint capability of the support system, and

3. compute the safety margins of the support system.

The Staff's SER supplements indicated that in the analysis, "mechanistic models for postulated pipe break and analytic simplifications may be utilized provided justification is furnished."

A pivotal consideration to the appropriateness of this phenomenon is whether the arbitrary breaks postualted are likely to occur. To pursue this, FP&L as a member of CE-ROG supported the effort required by SAI culminating in the report discussed supra.

Additional studies were conducted independently by FP&L utilizing appropriate analytical simplifications to estimate the IAL and reactor vessel support system's ability to accommodate this additional load. These studies are discussed infra.

With the exception of the reactor cavity wall, the Unit 2 reactor coolant system support design duplicates the Unit 1 design. With regard to cavity wall design, the Unit 2 design has been modified, in accordance with a Staff request, to provide additional margin above that shown to be acceptable for Unit 1. The structural analyses discussed hereinafter assume the Unit 1 cavity wall design, thus the results are applicable to both Units' 1 and 2.

3.1 BREAK SELECTION

The IAL is additive to thrust, seismic and EAL, if and only if, the break location is within the RPV biological shield wall. If, the break is outboard of this wall the EAL is zero because a reactor cavity transient will not result from these break locations. Since the EAL is a significant load component, governing break locations with regard to RPV support capability must be within the RPV biological shield wall.

The IAL results from the decompression wave associated with an essentially instantaneous pipe break traveling through the subcooled fluid from break location to the RPV and thence through the RPV internal structures. The three-dimensional hydraulic wave phenomenon is quite complex. However, in general, the closer the break location to the RPV the larger the force on the vessel. (See the discussions on the applications of acoustic theory provided by Section 3.2.2). Thus, the IAL will be greatest for a location at the RPV nozzle. Additionally, the EAL is maximized at this location since any cavity bypass, i.e., flow out of the faulted piping penetration is minimized.

Immediately after a postulated cold leg break occurs, a decompression wave will be generated causing a sudden decrease of the pressure at the inlet plenum near the ruptured nozzle. Before this decompression wave reaches the opposite side of the downcomer and the outlet plenum within the core barrel, the pressure at these locations will be essentially the same as system pressure prior to the break. Thus a short-lived significant pressure difference will exist across the RPV's internal structures. This time changing differential pressure will create a transient horizontal force on the RPV in the same direction as the initial jet force and the EAL. A hot leg break would not result in this additional horizontal force. It is significant to note that this transient horizontal force occurs sooner than the peak EAL and that absolute additions of these peak loads results in a conservative evaluation of the RPV support margins.

In light of the preceding considerations, an arbitrarily postulated cold-leg break at the RPV nozzle would result in the maximum IAL related loading condition on the RPV supports. Thus a cold-leg break is the worst case for assessment of ultimate support restraint capability.

Appendix I to the SAI report cited above discusses the question of circumferential versus longitudinal failure. The stress distribution at two reactor coolant system joints are analyzed and it is shown that the locations analyzed are at least 3 to 5 times more likely to fail in a circumferential direction than in an axial direction. The report concludes that there "is no reason to believe that these results are not representative, and it can therefore be concluded that circumferential failures (double guillotine) are much more likely to occur than axial failures (long slot)."

The Combustion Engineering Report CENPD-168 Revision 1 "Design Basis Pipe Breaks for the Combustion Engineering Two Loop Reactor Coolant

System" also addresses the likelihood of slot breaks for the CE System 80 design. This report shows that flaws large enough to grow through the pipe wall during service life must be much larger than ISI detection capabilities available; if a large enough flaw did exist, radial crack growth would predominate so that significant leakage would occur before the crack could grow to critical axial length; and if a crack grew radially until leakage occurred, critical crack length would not occur until after years of additional plant life. St Lucie Units 1 and 2 have leak detection systems, as well as, a technical specification on allowable unidentified leakage. Since substantial leakage is associated with a through crack, it would be detected well in advance of achieving critical crack length.

Based on the SAI report evaluation, which appears to be reinforced by the conclusions reached in the CE System 80 report, the appropriate postulated break is the guillotine failure. This is intuitively obvious because slot breaks do not result in horizontal thrust forces which are colinear with cavity and internal forces.

Based on the above, the arbitrarily postulated cold-leg guillotine break at the RPV nozzle was selected for evaluation of the ultimate restraint capability of the reactor coolant system supports. The commitment to analyze this break was provided by FPL's 9 February 1976 letter.

3.2 DEVELOPMENT OF THE IAL

In order to estimate the design margins remaining in the reactor vessel supports of St Lucie, all loads to which the supports can be subject must be examined.

The original design of the supports did not explicitly consider the transient differential pressures in the annulus region between the reactor vessel and the cavity wall, usually referred to as "cavity loads" or external asymmetric load (EAL), and across the core barrel, referred to as "internal loads" or internal asymmetric load (IAL).

Subsequent analyses, which are discussed in the Safety Analysis Report included the contribution of the "cavity loads". These analyses demonstrated that the support system was capable of bearing these additional loads with considerable margin.

To quantitively assess the potential for exceeding the design margin of the RPV support system, when the "internal loads" are also considered, or alternatively to determine what margin remains in the design, it is necessary to determine the magnitude of these loads.

The proper determination of the time-history of the "internal loads" at the RPV supports requires that the fluid forces on the vessel, barrel, internals etc., be first determined. These forces must then be applied to the vessel, barrel, internals etc., to compute the in-motion and the reaction forces applied to the RPV supports.

Evaluation of the fluid forces, particularly as affected by the fluid coupling and the fluid-structural interaction, is a complex problem. Various complicated schemes, the adequacy of which is still be assessed, have been proposed for this evaluation. Essentially all rely either on the numerical solution of the conservation equations in one or possibly two dimensions (RELAP 4, FLASH 4, TWODTRAN and SOLA), or wave superposition techniques (WHAM and WATHAM) in a model which consists of various legs connected by junctions. Fluid structure interactions can be modeled in an interative manner by recomputing internal dimensions following displacements and allowing the decompression phenomenon to continue for another time step along the newly defined system.

It is possible, however, to determine a reasonable estimate on the magnitude of the hydraulic forces (internal loads) without resorting to such complex computer programs. This can be accomplished by using simple acoustic theory.

3.2.1 ACOUSTIC THEORY

The unsteady motion of fluid, such as that occurring in the reactor vessel following a break in the primary system, can be described by a set of three conservation laws (mass, momentum and energy); the equation of state for the fluid; the heat transfer equations for the container walls (vessel, internals, fuel, etc.); and the initial and boundary conditions for all dependent variables.

Since the duration of the transients of interest is extremely short (of the order of tens of milliseconds), there is no expected appreciable variation in temperature in either the liquid or the container surfaces. For these reasons, heat transfer and the energy equations can be disregarded.

It is well known that the remaining equations of continuity and momentum can be reduced to the familiar wave equation describing pressure disturbances in a medium, wherever local pressure changes between consecutive time intervals are not large (i.e., isentropic processes with small pressure changes within a time step), and the density of the fluid is essentially constant.[1,2,3]

The wave equation for generalized n^{th} dimensional [4] propagation is written as:

$$\nabla^2 P - \frac{1}{C^2}\frac{\partial^2 P}{\partial t^2} = 0 \qquad (1)$$

Where P is pressure.

For symmetric propagation in n- dimensional space the above reduces to:

$$\frac{1}{r^{n-1}}\frac{\partial}{\partial r}\left[r^{n-1}\frac{\partial P}{\partial r}\right]-\frac{1}{c^2}\frac{\partial^2 P}{\partial t^2} = 0 \tag{2}$$

Equation (2) can be immediately written in the familiar form of one space dimension:

$$\frac{\partial^2 P}{\partial r^2}-\frac{1}{c^2}\frac{\partial^2 P}{\partial t^2} = 0 \tag{3}$$

two dimensions (symmetrical expansion);

$$\frac{1}{r}\frac{\partial}{\partial r}\left[r\frac{\partial P}{\partial r}\right]-\frac{1}{c^2}\frac{\partial^2 P}{\partial t^2} = 0 \tag{4}$$

and three dimensions (spherical propagation);

$$\frac{1}{r^2}\frac{\partial}{\partial r}\left(r^2\frac{\partial P}{\partial r}\right)-\frac{1}{c^2}\frac{\partial^2 P}{\partial t^2} = 0 \tag{5}$$

The latter can, of course, be written as follows:

$$\frac{\partial^2}{\partial r^2}(rP)-\frac{1}{c^2}\frac{\partial^2}{\partial t^2}(rP) = 0$$

which is recognized as equation (3) with the variate P replaced by the variable (rP).

The solution to the one dimension wave equation is well known, and can be expressed by[4];

$$P(r,t) = \frac{1}{2}\left[P_o(x + ct) + P_o(x-ct)\right] \tag{7}$$

for the case of a disturbance having an initial amplitude P_o with zero initial velocity, as would be the case for a pressure wave originating at some point in a pipe. Here c is the speed of sound and r the dimension along the axis of propagation, say the X axis

Equation (7) shows that a wave disturbance of amplitude P_o with zero initial velocity is made up of two identical waves of half the amplitude. One traveling in the +x direction and the other in the -x direction at sonic speed. The superposition of these two waves at t=o yields the original amplitude P_o.

The general solution to the propagation of a disturbance in one dimension (such as a plane wave in a pipe) is given as: [5]

$$P(x,t) = f(x-c\ ) + g\ (x+ ct) \qquad (8)$$

where f and g are general functions representing waves propagating in the +x and -x directions respectively. The superposition of these waves will give the pressure P at any point x at time t.

Immediately one can write the generalized solution to the spherically symmetric propagation problem from equation (6). Hence;

$$P(r,t) = \frac{1}{r}\left[f(r-ct) + g\ (r + ct)\right] \qquad (9)$$

where; $r^2 = x^2+y^2+Z^2$

Here f, and g represent spherical waves traveling outward from the origin and toward the origin at sonic speed. Their superposition yields the pressure P at any point in space and time.

It is informative to examine both equations (8) and (9). Equation (8) indicates that the pressure at any point x at time t is given by the superposition of a family of waves. The shape and amplitude of a disturbance does not change with time for a one dimensional wave. Equation (9) states that the shape of the pulse does not vary with time, but its amplitude decreases as 1/r for a three-dimensional wave.

The propagation of a disturbance from the broken nozzle to the downcomer region can be described as the propagation of a wave in one dimension. Between nozzle and downcomer, at the nozzle region, some spherical propagation will take place. But, inside the annular downcomer region, the propagation is probably best described as two dimensional, along a plane down through the circumference of the annulus.

Propagation of disturbances in two dimensions is different than propagation in one and three dimensions. The solution x to the wave equation in two dimensions for the pressure at the origin from a disturbance P_o at some point r ($r^2 = x^2 + y^2$) is given by: [4]

$$P(o,t) = \frac{Po}{2\pi}\left[\frac{-ctU\ (ct-r)}{\left(C^2t^2 - r^2\right)^{3/2}} + \frac{\delta(ct-r)}{\sqrt{C^2t^2 - r^2}}\right] \qquad (10)$$

where; $U(ct-r) = \begin{cases} 0 \text{ if } ct \le r \\ 1 \text{ if } ct > r \end{cases}$

$\delta(ct-r) = \begin{cases} o \text{ if } ct \ne r \\ 1 \text{ if } ct = r \end{cases}$

One notes that the effect of the disturbance is not felt until $r=ct$, at which time a pulse P_o arrives at the origin. However thereafter this pulse is followed by a wake described by the first term of the equation, which for $ct \gg r$ decays as $1/c^2t^2$. This result is important because it shows that two dimensional propagation exhibits a sharp front like the one and three dimensional cases. However, unlike the odd dimensional propagation where the wave shape is maintained, the shape of the wave is altered by the wake.

In addition to the changes in shape, the cylindrical wave is also attenuated. This is obvious since the total energy flux distributed over a cylindrical surface remains constant. Since the cylindrical area increases as r as the wave propagates, the wave intensity must diminish as $1/r$. Hence its amplitude decreases as $1/\sqrt{r}$. (Recall that a spherical wave amplitude decreases as $1/r$ since it has a front which expands as r^2).

The $1/\sqrt{r}$ decay of the amplitude is easily proven by assuming $P(r,t) = f(r)\, e^{-iwt}$ (a single frequency wave). Then equation (4) transforms into:

$$e^{-iwt}\,\frac{1}{r}\frac{\partial}{\partial r}\left(r\frac{\partial f}{\partial r}\right) + \frac{w^2}{c^2}\,e^{-iwt} f = 0 \qquad (11)$$

$$\frac{1}{r}\frac{\partial}{\partial r}\left(r\frac{\partial f}{\partial r}\right) + k^2 f = 0$$

where: $k^2 = \dfrac{w^2}{c^2}$.

Equation (11) is the Bessel equation of zero th order. The solution of which is given by the J_o Bessel function.

For an outgoing traveling wave then the solution is given by ; 6 /

$$P(r,t) = A\, e^{-iwt}\, H_o^{(1)}(kr) .$$

The asymptotic solution of which is;

$$P(r,t) = A\sqrt{\frac{2}{\pi}}\;\frac{\exp\left[i(kR - wt - \frac{1}{4}\pi)\right]}{\sqrt{kR}} \qquad (12)$$

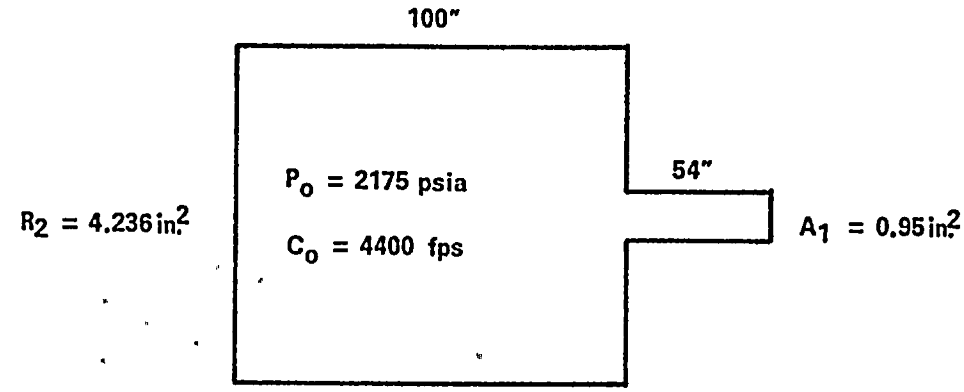
In the case of the propagation of decompression (and compression) waves in the downcomer annulus, therefore the wave will attenuate as $1/\sqrt{R}$ and the shape of the wave will be altered. The latter feature makes wave superposition more difficult.

3.2.2 Applications of Acoustic Theory

Moody[7/] has shown that the theoretical fluid force, which can occur when an infinite reservoir is discharged through an opening of area A, is given initially by P_oA (where P_o is the pressure in the reservoir) and eventually can build up to $2P_oA$ for steady flow with no friction. The maximum value of the force during the acoustic decompression, which is of interest in the rapid transients associated with internal loads, is however P_oA. This result can be readily shown by application of acoustic theory.

Acoustic theory can be applied successfully to more complex systems. For instance, the force experimentally measured in Hansen's experiment[8/] is given in Figure 3.2-1, together with the force theoretically predicted by acoustic theory.

The Hansen experiment is described by the sketch below.

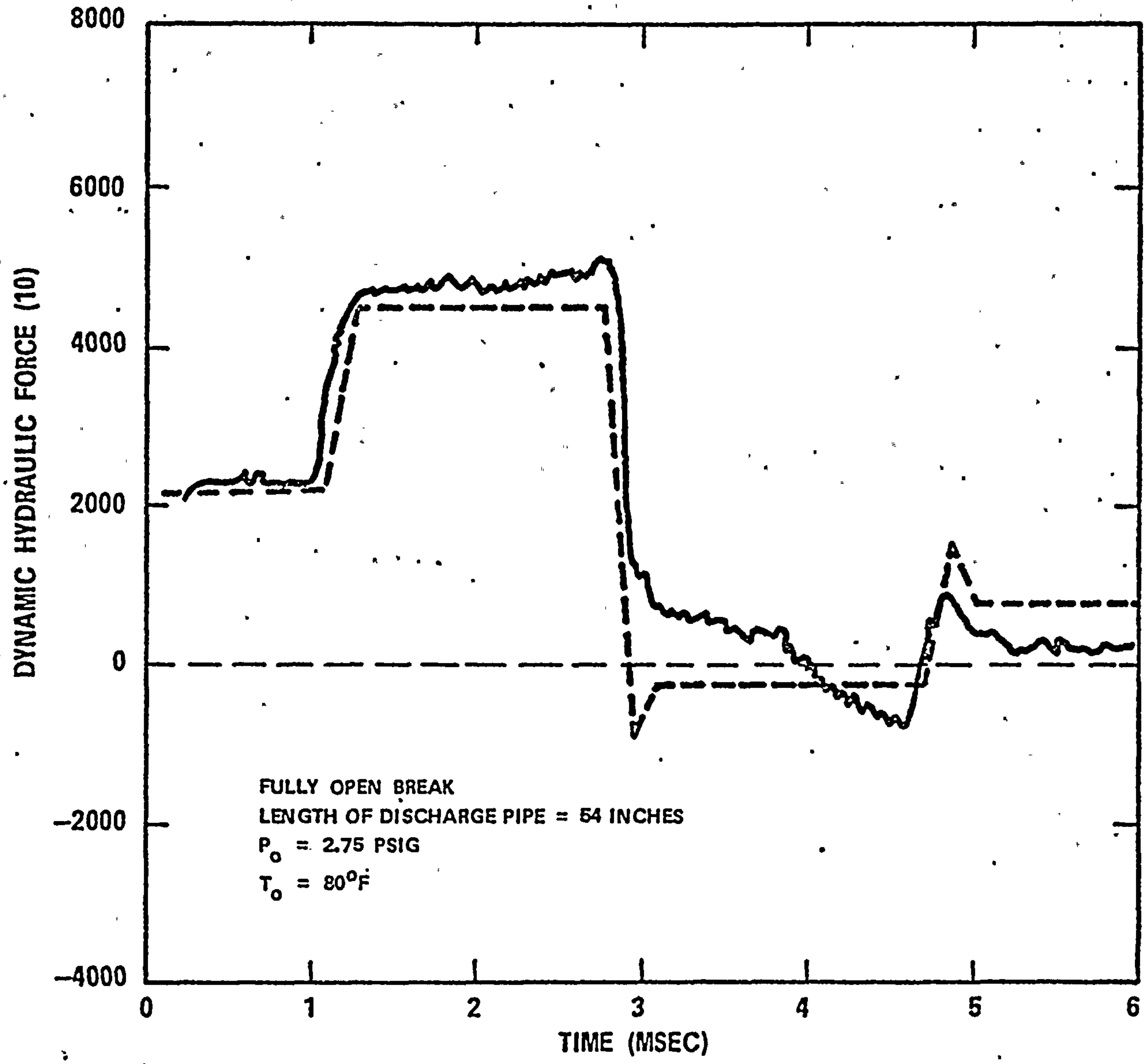


The sketch below illustrates the acoustic decompression of this simple system. At time t=0 a decompression wave of amplitude 2175 psia travels toward the larger pipe. It reaches the junction at 1.02 msec. Here this rarefaction wave is reflected as a compression wave by the partially open end. The amplitude of the reflected compression wave is given by the reflection coefficient;

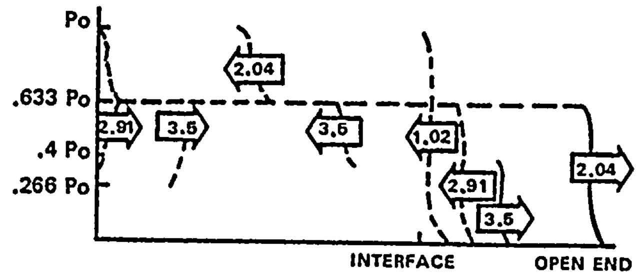
$$R = \frac{A_2 - A_1}{A_2 + A_1} = 0.6336$$

DYNAMIC HYDRAULIC FORCES, DURING SUBCOOLED BLOWDOWN; COMPARISON OF EXPERIMENTAL AND HAND CALCULATED FORCES FOR PIPE EXPERIMENT TEST 29 (FULLY OPEN BREAK).

FIGURE 3.2-1



A transmitted rarefaction wave of amplitude $0.3664P_o$ continues down the large tube where it is reflected as a rarefaction wave of the same amplitude by the closed end at t=2.91 msec.



The originally reflected compression wave reaches the open end at 2.04 msec and is reflected as a rarefaction of the same magnitude.

The force acting on the system is given by the expression

$$F = 4.236P_1 - 3.286P_2$$

Where P_1 is the pressure at the closed end and P_2 is the pressure at the interface between the two pipes.

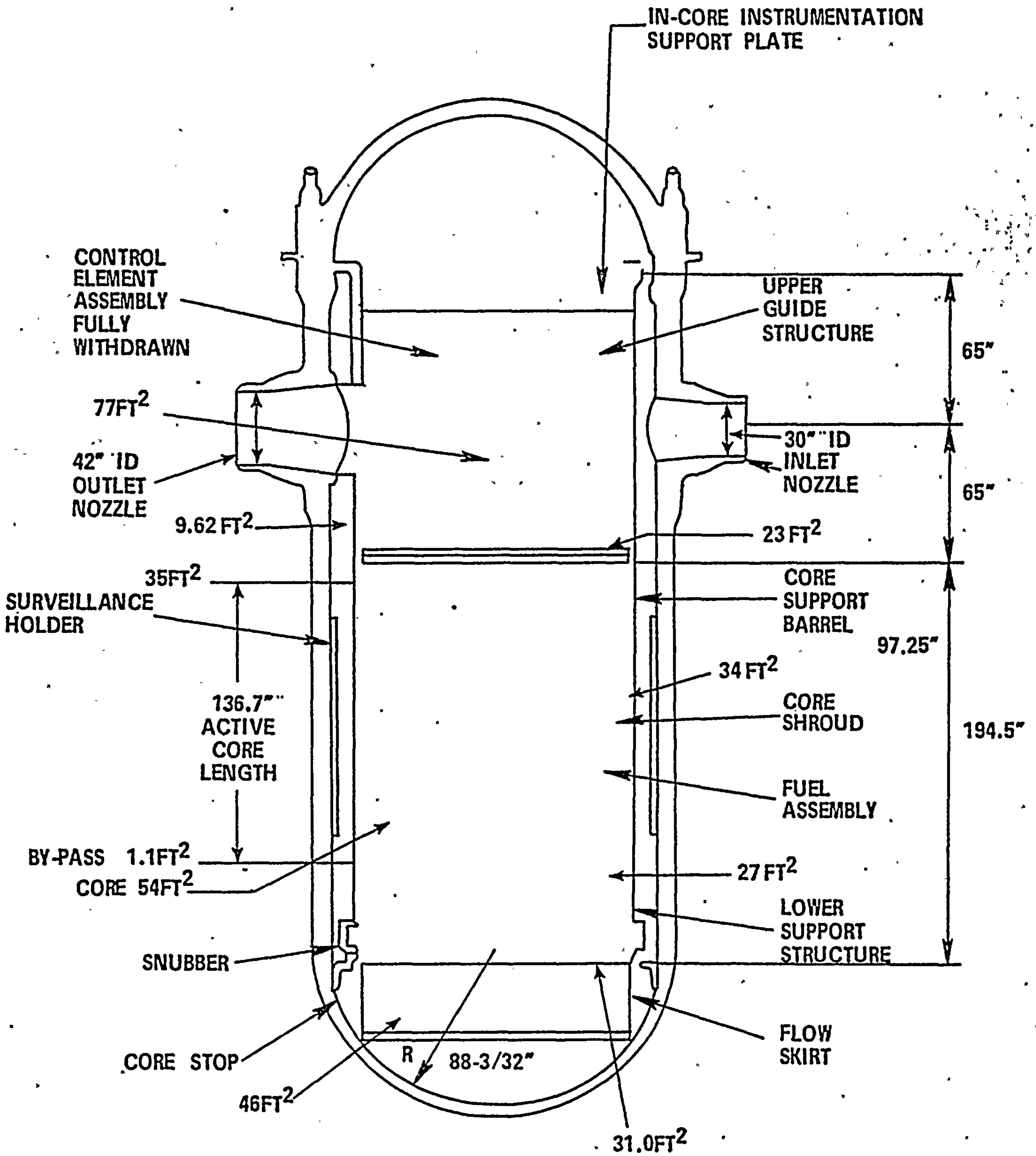
Figure 3.2-1, traces the history, both experimental and derived by this method. At a time between 1.02 msec and 2.91 msec the pressure at the closed end of the pipe is 2175 psia and that at the interface is 0.6336 (2175) psia. Hence the force is equal to 2.16 times P_oA, or nearly $2P_oA$.

The reason that Hansen's experiment was chosen as an example to compare results derived from acoustic theory with experimental data is threefold. First the dimensions of the experiment closely correspond to the acoustic dimensions of the nozzle-downcomer region of the St Lucie RPV. From Figure 3.2-2, the area of the broken cold leg is 4.91 $ft.^2$ A reasonable one dimensional approximation of the St Lucie geometry can employ as the area into which the initial rarefaction wave from the nozzle propagates, either the full downcomer annulus area, which averages 34 ft^2, or an area described by the side surface of a cylinder having a thickness equal to the annulus width and a radius of 65" (distance from pipe centerline to the top of the downcomer). The latter area is 26 ft^2. The ratios of these areas is either roughly 7:1 or 5:1 which reasonably approaches the 5:1 area ratio of Hansen's experiment.

Secondly, the close agreement between the simple hand calculation and the experiment serves as confirmation that a simple hand calculation can provide excellent answers, and offers proof that

REACTOR ARRANGEMENT VERTICAL SECTION

FIGURE 3.2-2



(CORE) VESSEL I.R. 86"

(CORE) BARREL O.R. 75.75"

(ABOVE CORE) BARREL O.R. 76.5"

acoustic theory is valid for the rapid transients in question.

Thirdly, examination of the time at which the maximum force occurs shows that it occurs when the left side (closed end) is still compressed, while the right side at the nozzle interface has decompressed.

If the length of the large pipe (downcomer region) is shorter than three times the length of the nozzle section, then the interface will only have suffered one decompression before the closed end also decompresses and the maximum force can be written as;

$$F_{max} = P_o A_2 - (A_2 - A_1) RP_o$$

which reduces to;

$$F_{max} = P_o A_1 \left(\frac{3A_2 - A_1}{A_2 + A_1}\right)$$

Substitution of the values for A_2 and A_1 show that $F_{max} = 2.16P_oA_1$ as obtained previously.

If, however, the length of the large pipe exceeds three times the length of the nozzle section, the interface will be successively decompressed by succeeding rarefaction waves which have reflected from the nozzle open end (break). Each of these rarefaction waves will be reduced in intensity by a factor R with respect to the proceeding one, and will leave the interface pressure R times lower than prior to its arrival. In general, therefore for a large section n times longer than the nozzle section and a constant sonic speed, the max-force can be immediately written as:

$$F_{max} = P_oA_2 - (A_2-A_1)\ R^{n/2}P_o \quad \text{for n even}$$

or,

$$= P_oA_2 - (A_2-A_1)R^{\frac{n+1}{2}}\ P_o \quad \text{for n odd}$$

This indicates that the largest force is to be expected for the break occuring as close to the large vessel as possible. For an example showing how a simple one dimensional approach can be employed for St. Lucie refer to Attachment 2.

3.2.3 Simplified Acoustic Theory Calculation of Internal Loads

To determine the hydraulic force resulting from a cold-leg guillotine break at the reactor vessel nozzle, the following model is employed:

1. The model is one-dimensional for the extent of the nozzle extending from the break location to the downcomer annulus.

2. The downcomer annulus is described as shown in Figure 3.2-3. Cylindrical wave motion is assumed to take place in the x-y plane. No losses are assumed for the change from a plane wave in the nozzle to the cylindrical wave in the downcomer.

3. The reflection coefficent at the nozzle to downcomer junction is predicated on an area ratio of 34/4.91

4. The break is assumed to occur in 1 msec, and the decompression wave is then chosen from the empirical expression derived from the LOFT experiments and reported in reference 1. Hence,

$$P_{(t)} = P_o\ (1.0 - 0.0606t - 0.209t^2 + 0.3801t^3 + 0.866t^4 - 4.2134t^5 + 2.6128t^6)$$

   where t is in milliseconds.

5. No consideration is given to the partial flashing occuring in the broken nozzle. This has the effect of overestimating the speed at which nozzle recompression can take place and thus decompresses the nozzle downcomer interface region faster than will occur in reality.

The relatively short wave length (~ 4 ft.) is chosen because the characteristic acoustic length of the pipe (15") is small enough to allow full decompression of the pipe essentially simultaneously as the break occurs.

The initial rarefaction wave has an amplitude of 1,302 psi (2247-945) since saturation pressure is 945 psia; and reaches the interface between the nozzle and the downcomer at 0.715 msec as shown in Fig 3.2-4.

It is reflected as a compression wave of amplitude 972 psi which reaches the open end at 1.43 msec, and is reflected as a second rarefaction of the same magnitude.

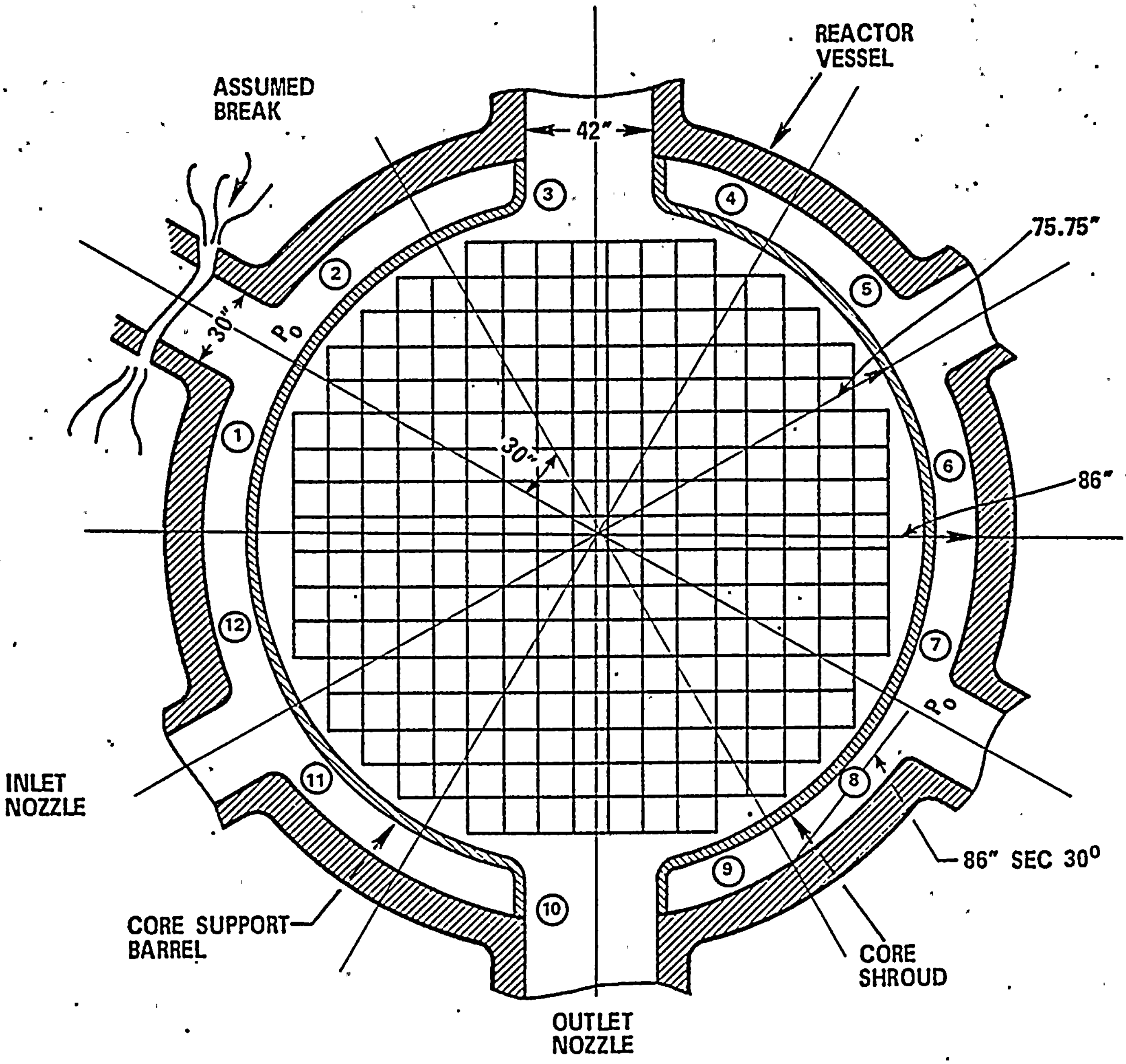
The pressure at the interface can and does fall below that of the decompression wave since the wave length is longer than the nozzle and a portion of the rarefaction wave has already been transmitted where full recompression occurs.

However, Figure 3.2-4 does not show the recompression phenomenon which is caused by the transmission back in the nozzle of the partial recompression which accompanies the two dimensional, time and distance decaying rarefaction wave propagation in the downcomer region. The reason this is not shown is that this recompression changes amplitude with time and distance.

In fact the pressure at the interface has the following values at the following time:

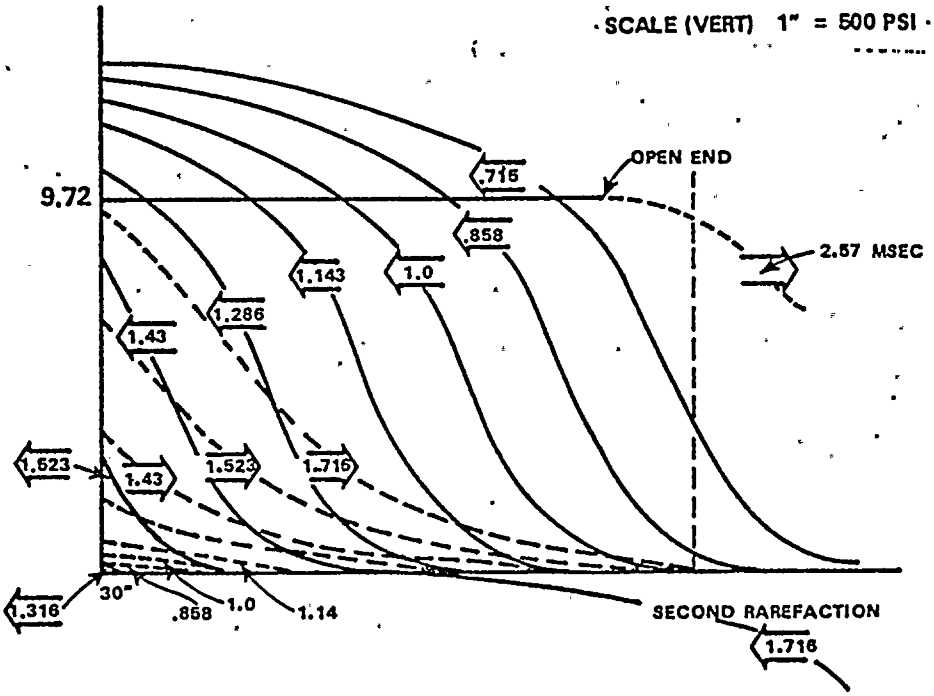
REACTOR CROSS SECTION

FIGURE 3.2-3



NOZZLE FIRST RAREFACTION AND RECOMPRESSION

FIGURE 3.2-4



t (msec)	$P_{interface}$ (psia)
1.43	1893
6.78	1647

The assumption here is made that reflections of the cylindrical decompression waves in the downcomer do not propagate back with the nozzle as further rarefaction waves, as the nozzle would appear to them as a closed side wall, whereas disturbances do transmit from the nozzle to the downcomer region.

Figure 3.2-5 illustrates the progress of the decompression in the downcomer region. The initial transmitted rarefaction wave, with an amplitude of 330 psi, propagates out as a cylindrical wave. From equation (12), its amplitude decays as $1/\sqrt{r}$. The initial radius of the cylindrical wave is 15 inches. At 60 inches therefore, its amplitude will have decayed to half its original value.

The original rarefaction of -330 psi will reach the bottom of the vessel (flow-skirt region), and the opposite side of the vessel almost at the same time (~ 6.8 msec), but by this time its amplitude will be only -85 psi. At the bottom it will reflect as a rarefraction and transmit as a rarefaction through the flow skirt. At the opposite end of the vessel it will reflect as a rarefaction (in actuality the rarefaction fronts will add, which is the same thing as a perfect reflection). This will result in a fast decompression of the far side of the vessel, and hence in a reduction in the horizontal "internals" force. Thus, the peak force is likely to occur just before this happens, i.e. where the break side of the vessel is decompressed and the far side is still fully compressed.

The portion of the cylindrical rarefraction wave traveling upward experiences a perfect reflection as a rarefaction wave of the same amplitude. This contributes to a faster decompression of the break side of the vessel.

At t=2.14 msec, the second rarefaction wave front arrives at the nozzle and the back of the rarefaction arrives 1.15 msec later. The third rarefaction reaches the downcomer region at 3.67 msec and the fourth one at 5.1 msec. The latter however, does not reach full magnitude before reflection of the first rarefaction wave takes place at the opposite end of the vessel.

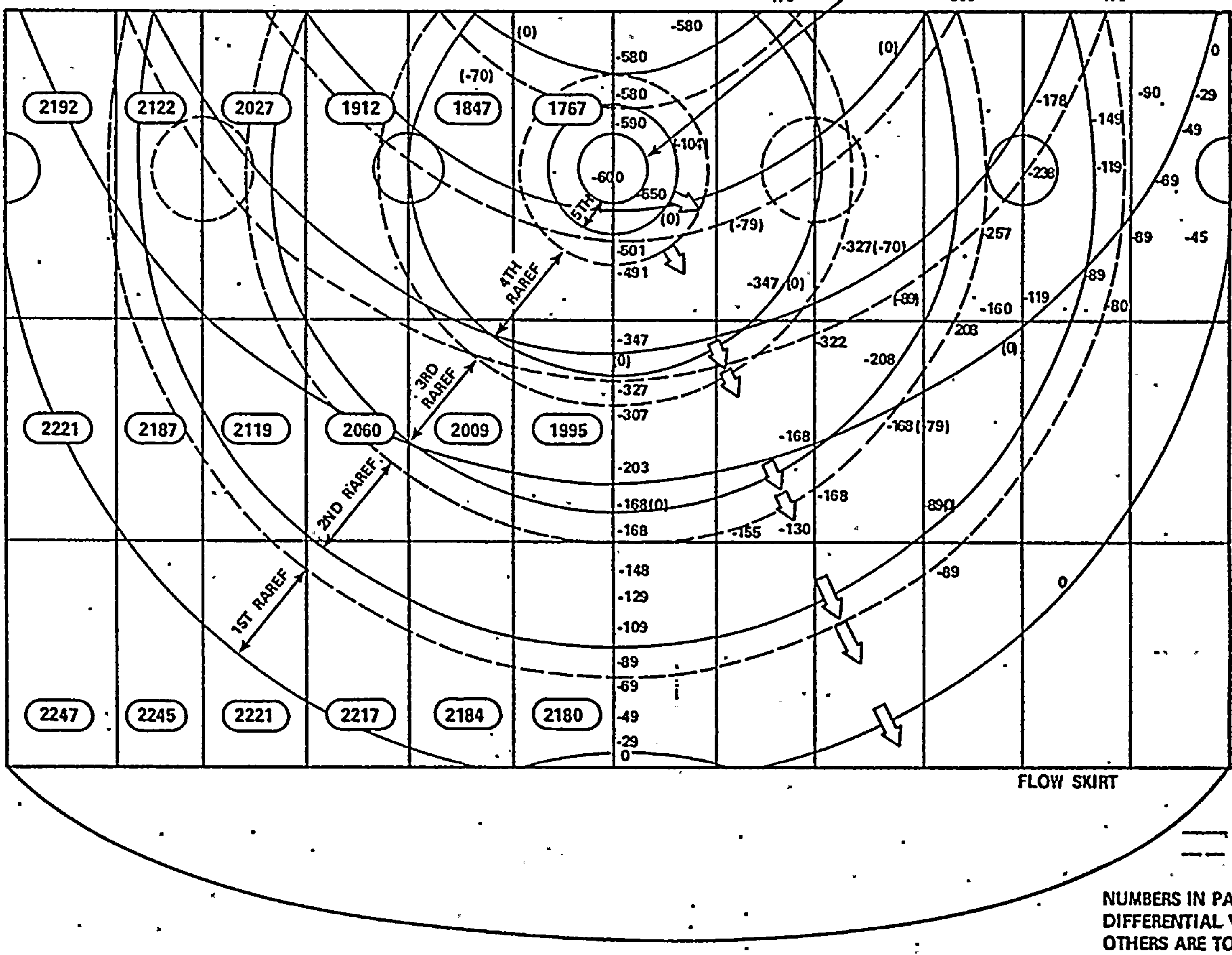
All of these rarefaction waves are shown in Figure 3.2-5 at t=6.78 msec and with the appropriate magnitudes, including their reflections from the top of the downcomer region. The magnitude quoted for the end of the front corresponds to the $1/\sqrt{r}$ reduction of the full rarefaction wave at the nozzle.

The tail of the disturbance which is characteristic of two dimensional propagation, decays sufficiently fast (as $1/r^2$) that a

DOWNCOMER DECOMPRESSION
AT 6.785MSEC
FIGURE 3.2-5

SCALE 1" = 60"
WIDTH OF WAVE = 48"

NUMBERS IN SQUARES ARE AVERAGE PRESSURES USED FOR FORCE CALCULATION



— FRONT OF WAVE
– – BACK OF WAVE

NUMBERS IN PARENTHESIS ARE PRESSURE DIFFERENTIAL VALUES OF EACH WAVE. OTHERS ARE TOTAL PRESSURE DIFFERENTIALS sharp back edge may be assumed (see equation 10). This back edge restores the medium over which it passes to the full rarefaction.

To calculate the internals force alone, exclusive of thrust which had been included in the original calculations for the design of the reactor pressure vessel supports, the following formula is used:

$$F_{int} = \sum_i P_{b_i} A_{v_i} - \sum_i P_{f_i} A_{v_i} - \sum_i A_{b_i} (P_{b_i} - P_{f_i}) \qquad (14)$$

The total force would be;

$$F_{total} = \sum_i P_{b_i} A_{v_i} - \left(\sum_i P_{f_i} A_{v_i} - P_{break} A_{break} \right) - \sum_i A_{b_i} (P_{b_i} - P_{f_i}) \qquad (15)$$

It is recognized that $P_{break} A_{break}$ is nothing but the classical thrust load previously considered in RPV support design, hence, its neglect in equation 14 results in the prediction of the hydraulic forces due to the "internals" effect alone.

A word of explanation on the meaning of P_{b_i}, P_{f_i}, A_{v_i}, and A_{b_i} is necessary. Since the pressures in the vessel side opposite the break are not uniform, P_{b_i} represents the average pressure of i^{th} area in the vessel side opposite the break, and P_{f_i} the average pressure of the i^{th} area in the break side of the vessel at any time t. A_{v_i} and A_{b_i} are the i^{th} area projections in the direction of the thrust.

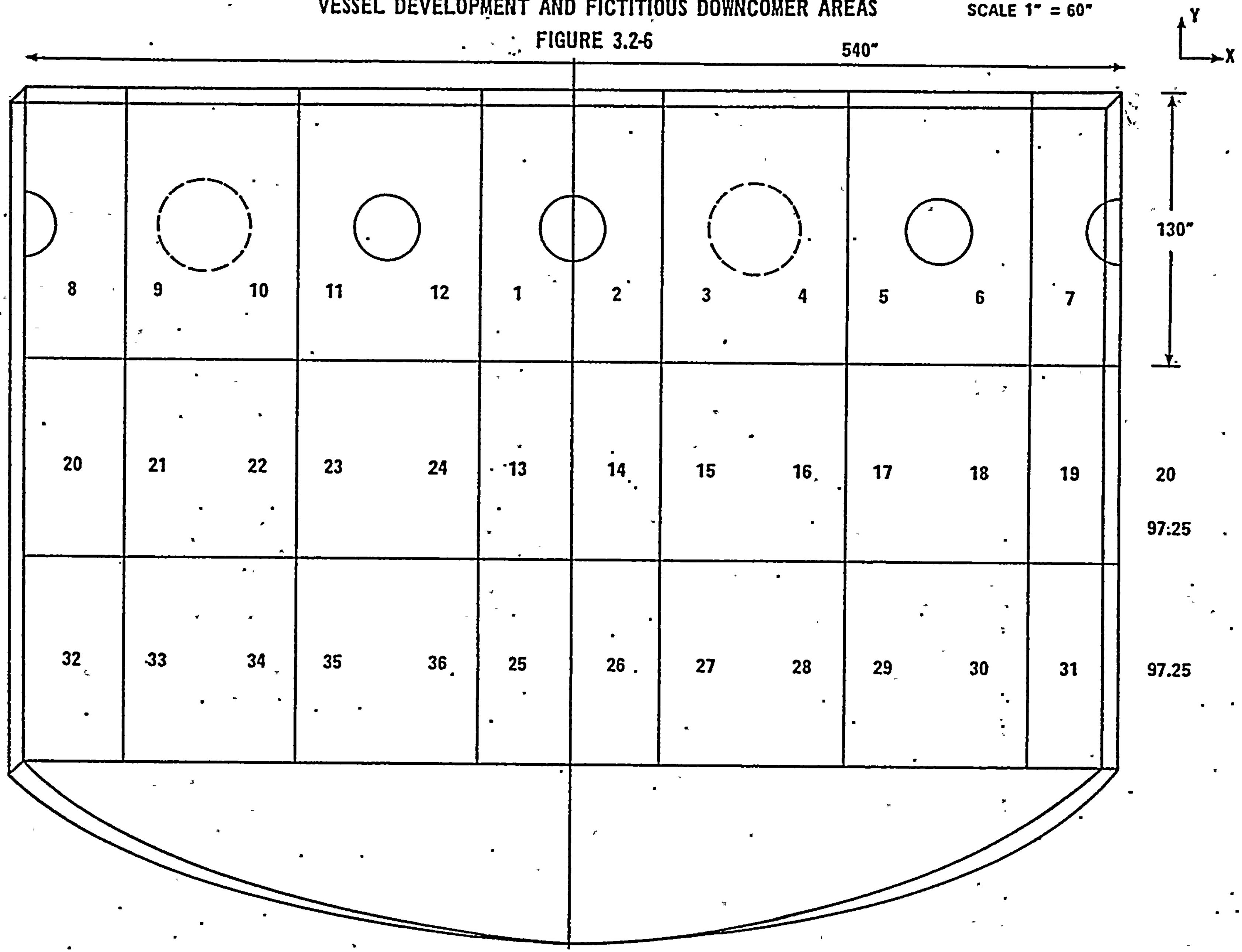
For the purposes of this simplified calculation the vessel barrel and downcomer regions have been subdivided into 36 areas as shown in Figure 3.2-6. Half of the areas are on the break side of the vessel and half on the opposite side.

The resulting "internals" hydraulic load has been calculated at 6.78 msec. This time is expected to correspond to the maximum force. The computed force is 1.47×10^6 pounds. It is recognized that inaccuracies are present both in the model, in the calculation of the superposition of the waves, and in the proper averaging of the pressures in the various areas. Hence, the accuracy of this hand calculation is expected to be no better than ± 20 percent. For this reason it was felt that a check on its validity was appropriate. The hydraulic force has also been computed by utilizing the computer program WHAM6,[17] which has been shown to give excellent agreement, at least for vertical loads, with experiment [8,9]. It must be reemphasized that consideration of fluid-structure interaction has shown that the loads developed based on rigid internals calculations are very conservative.

VESSEL DEVELOPMENT AND FICTITIOUS DOWNCOMER AREAS

FIGURE 3.2-6

SCALE 1" = 60"



3.2.4 Acoustic Theory References

1. S. Fabic, "Computer Program WHAM for Calculation of Pressure, Velocity, and Force Transients in Liquid Filled Piping Networks", Kaiser Engineers Report No. 67-49-R, Nov. 1967.

2. J. Pamakian, "Water-Hammer Analysis", Prentice Hall (1955).

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7. F.J. Moody, "Time Dependent Pipe Forces Caused by Blowdown and Flow Stoppage", 73-FE-23, Journal of Heat Transfer.

8. G. H. Hanse, "Subcooled Blowdown Forces on Reactor System Components - Calculational Method and Experimental Confirmation".

9. "Tree-Nureg-1004" EGG Report - Idaho National Engineering Laboratory, October 1976.

3.3 CONFIRMATION OF THE IAL PREDICTION

Section 3.2 discussed the theoretical and experimental basis utilized to establish a value of the IAL suitable for use in assessment of reactor coolant system restraint capability. To confirm this estimate the WHAM code was modeled to represent the St. Lucie RPV internals. The minor differences between Unit 1 and Unit 2 RPV internals design would not alter the conclusions reached hereinafter.

A beneficial, i.e., load reducing effect will also manifest itself as the decompression wave moves through the RPV's internals. The force across the core barrel will cause it to move. The annulus space of the downcomer region and the inlet plenum near the faulted line is reduced in volume, and the decompression is consequently decreased. Also, the movement of the barrel creates more volume space in the opposite side of the downcomer which in turn causes reduction of pressure in that side of the downcomer region. The pressure reduction further reduces the pressure differences across the barrel.

Any deformation of the core barrel will generate a new decompression wave in the core barrel traveling in the opposite direction to the initial decompression.

The load transfer to the RPV supports as the core barrel impacts on its stops is beneficial since it is in the opposite direction as the initial load caused by the internal asymmetric pressure distribution.

The fluid-structure interaction process has been modeled, analyzed and the results discussed in the public record. It appears that the modeling of these phenomena can reduce the IAL by about 30 percent. The discussion below assumes rigid RPV internals, i.e., no fluid-structure interaction. Thus, the IAL value predicted by the WHAM simulation is conservative.

3.3.1 MODEL DESCRIPTION & ASSUMPTIONS

The WHAM model employed to determine the internal asymmetric pressure differential load resulting from subcooled decompression within the reactor vessel following a postulated cold-leg circumferential break consists of 75 "legs" shown in Figure 3.3-1. The inlet nozzle has been modeled as the No. 1 leg with the break as the No. 1 junction. The other three inlet nozzles are considered as three individual legs connected to a common pressurizer at the normal operating inlet pressure. The two outlet nozzles are assumed to be connected to a pressurizer at the normal operating outlet pressure.

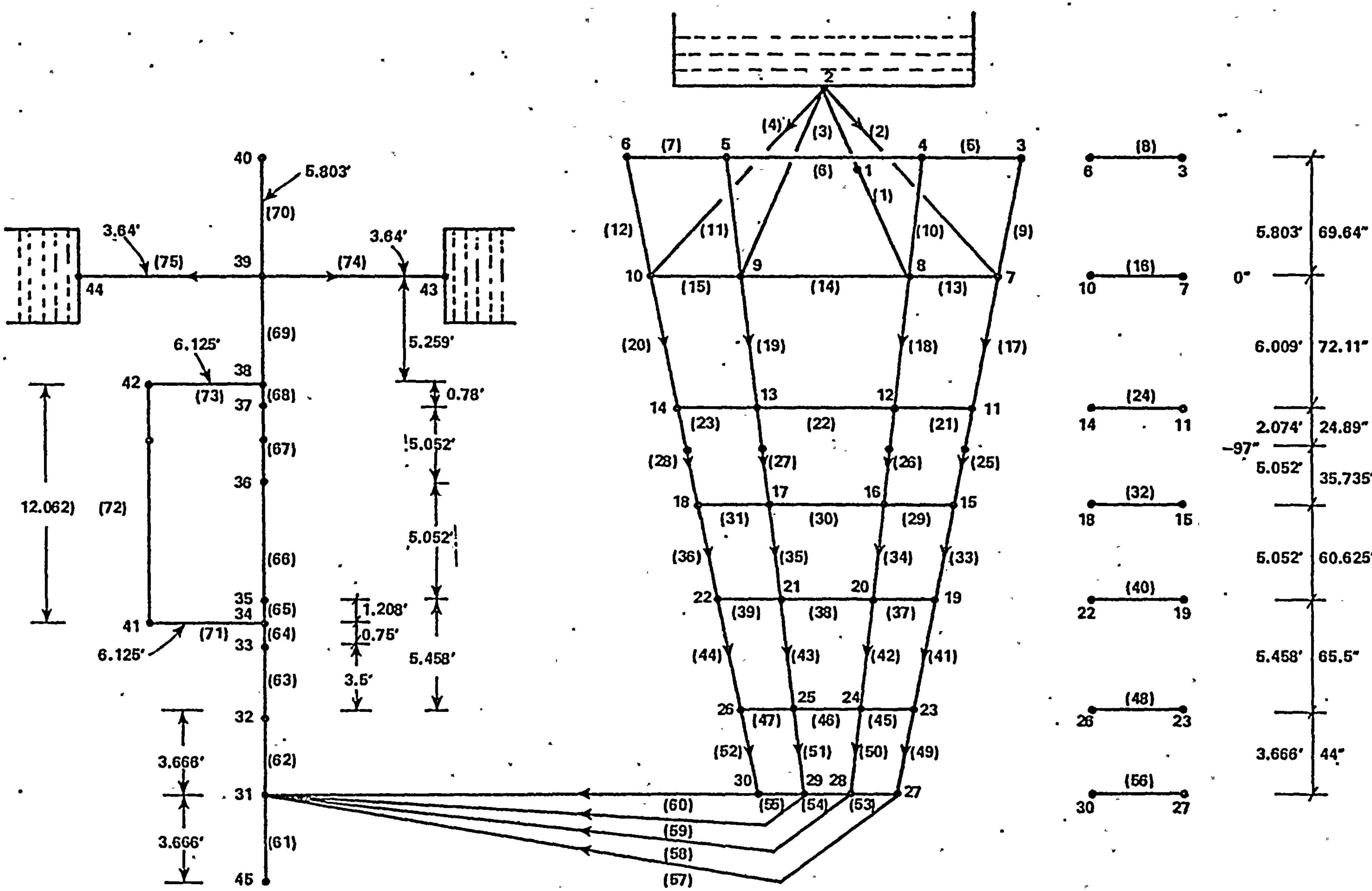
The equivalent piping network for the downcomer region has been constructed as follows. The downcomer region is first divided into 4 vertical channels with each channel connected to an inlet nozzle leg, then each vertical channel was subdivided into six elements at six different elevations. The inter-connection of the adjacent elements consists of a hydraulically equivalent piping network. As shown in Figure 3.3-1, the bypass flow in the core region has been considered as a channel in parallel to the center core channel.

The cold-leg circumferential break area was taken as 4.9 ft^2; the break opening time as 1 msec; and the waveform duration as 1 msec. The waveform was generated by WHAM/6, which is based on a six-degree polynomial curve-fit of the LOFT experimental data.

The asymmetric pressure loading function can be calculated from the pressure field generated by the WHAM/6 code as described in Section 3.3.2. This analysis conservatively assumes that both the core barrel and RPV are rigidly held for the purpose of simplifying the analysis.

WHAM MODEL

FIGURE 3.3-1



3.3.2 CALCULATION OF THE LOADING FUNCTION

The centerline of the hot-leg nozzle was defined as the x-axis and the perpendicular line in the same horizontal plane as the z-axis. The forces acting upon the core barrel and RPV at time (t), in each elevation of the downcomer region, can be determined from:

$$_nF_{ix} = (P_{ia} - P_{ib})\, H_i \frac{\sqrt{3}}{2} D_n \qquad (1)$$

$$_nF_{iz} = (P_{ic} - P_{id})\, H_i \frac{D_n}{2} \qquad (2)$$

Where $_nF_{ix}$ represents the force in the x direction acting on the core barrel (n=1) or RPV (n=2) at the <u>ith</u> elevation: and $_nF_{iz}$ is the force acting in the Z direction on the core barrel or RPV at the ith elevation; H_i is the height difference associated with the <u>ith</u> elevation as defined by:

i	1	2	3	4	5	6
H_i (ft.)	2.902	5.906	5.531	5.502	5.255	2.729

D_n is the outer diameter of the core barrel if n=1,or the inner diameter of the RPV if n=2; and Pia, Pib, Pic and Pid are the average pressure in legs ia, ib, ic, id respectively, with

$$\begin{aligned} ia &= 7 + 8\,(i-1) \\ ib &= 5 + 8\,(i-1) \\ ic &= 8 + 8\,(i-1) \\ id &= 6 + 8\,(i-1) \end{aligned} \qquad (3)$$

with i = 1, ...,6.

The net resulting force on the RPV in the x and z directions can be determined from:

$$\Delta F_x = \sum_{i=1}^{6} {}_2F_{ix} - \sum_{i=1}^{6} {}_1F_{ix} \qquad (4)$$

$$\Delta F_z = \sum_{i=1}^{6} {}_2F_{iz} - \sum_{i=1}^{6} {}_1F_{iz} \qquad (5)$$

By using Equations (1) and (2), Equations (4) and (5) can be re-written as:

$$\Delta F_x = \sum_{i=1}^{6} {}_1F_{ix} \left[\frac{D_2 - D_1}{D_1}\right] = f_x \left[\frac{D_2}{D_1} - 1\right] \tag{4a}$$

$$\Delta F_z = \sum_{i=1}^{6} {}_1F_{iz} \left[\frac{D_2 - D_1}{D_1}\right] = f_z \left[\frac{D_2}{D_1} - 1\right] \tag{5a}$$

Where: $$f_x = \sum_{i=1}^{6} {}_1F_{ix} \tag{6a}$$

$$f_z = \sum_{i=1}^{6} {}_2F_{ix} \tag{6b}$$

The total resultant force acting on the vessel can then be obtained from:

$$R_v = \sqrt{\Delta F_x^2 + \Delta F_z^2}$$

and its direction from:

$$\Theta = \tan^{-1} \left[\frac{F_z}{F_x}\right]$$

Where, with respect to the x-axis Θ is positive in a counterclockwise direction. Again, examination of the previous equations show that the traditional thrust force is excluded and the forces obtained are for the internal asymmetric loads only.

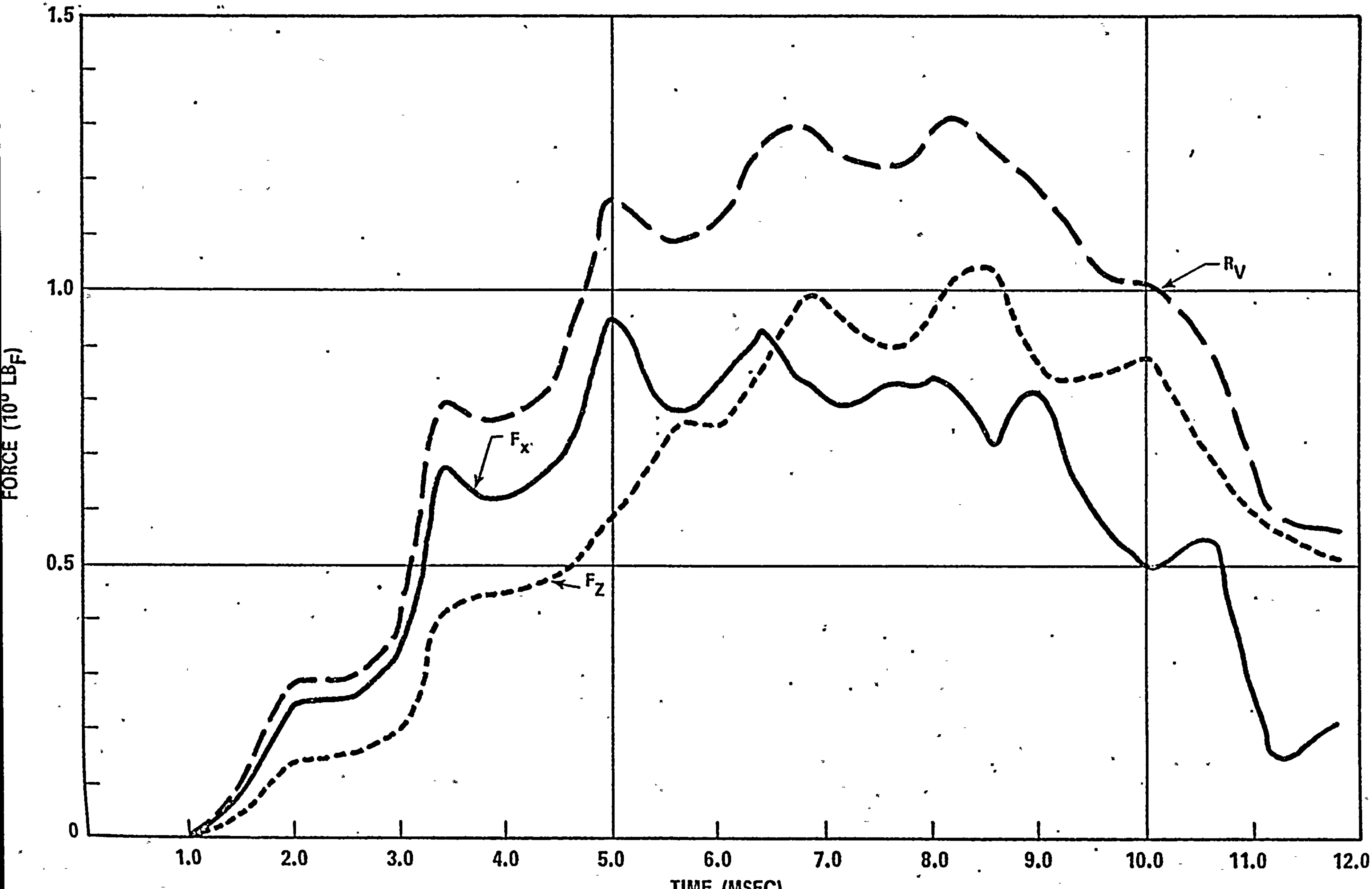
3.3.3 WHAM MODEL RESULTS

A plot of the results of the WHAM analysis is provided by Figure 3.3-2. The internal asymmetric load predicted (R_v) is quite representative of the loads predicted in Section 3.2. The maximum loading occurs 8.2 msec after pipe rupture, with a force of 1.3×10^6 lbs.

The results of the rigid internals analysis utilizing WHAM/6 provide results similar to those obtained from the acoustic theory considerations discussed in Section 3.2. Thus, based on two independent methods it is concluded that the IAL is well represented by an additional horizontal load of 1.5×10^6 lb, where additional margin has been added to account for model uncertainties, in spite of the fact that fluid-structure interaction effects would reduce the loads.

"WHAM" RESULTANT INTERNAL LOAD

FIGURE 3.3-2



3.4 REACTOR SUPPORT STRUCTURE

The intent of the reactor support structure evaluation is to assess the maximum restraint capability of the RPV support system for a cold-leg guillotine break. The analysis arbitrarily increases horizontal RPV support loads until maximum allowable stress conditions are achieved.

The calculated hydraulic loads act on the vessel, internals, etc. and are transmitted to the entire primary system. The primary system in turn reacts to these hydraulic forces and transmits loads to the reactor support system.

The amplification of the forces as a result of acceleration through minimal gaps as well as the dynamic nature of the forces cannot be precisely determined without resorting to complex structural dynamic analysis, which can lead to overconservative results unless the interaction of fluid and structures is properly accounted for.

However, because of the very small gaps, the dynamic amplification factor is likely to be nearly 2.0

Thus the total reaction force resulting from the internal asymmetric pressures is approximately 3.0×10^6 lb_f. This load is shared by two supports, each experiencing approximately 1.5×10^6 lb_f

3.4.1 REACTOR CAVITY AND STEEL SUPPORT STRUCTURE

The reactor cavity and supporting steel system was analyzed by finite element models, similar to those used previously and shown in the Unit 1 FSAR, Appendix 3H. The criteria and assumptions used in this analysis are the same as those outlined in Appendix 3H except the maximium allowable stress for steel in the minimum yield stress. This is appropriate for determining ultimate capability. The loading combination used was the loss of coolant accident (LOCA) during the steady state power condition with design basis earthquake (DBE).

The steel beam column assembly has been analyzed for the following loading combination:

$$W = D+T_1 + DBE+T_s+P$$

where:

W =The total loading applied to the steel beam-column assembly

D =Dead loads including the forces transmitted from the reactor coolant system to the RPV support system.

T_s =Forces due to temperature effects transmitted from the reactor coolant system to the RPV supports.

T_1 =The temperature distribution during normal operating conditions.

DBE =Forces caused by a design basis earthquake

P =Forces acting on the RPV support system and caused by a postulated pipe break.

The concrete reactor cavity has been analyzed for the following load combinations:

where: $U = T_1 + 1.0 \quad (D+L'+A+P+Q+DBE)$

U =The ultimate strenght of the concrete structural components required to resist the factored loads listed below.

D =Dead load consists of the dead weight of the reactor building internal structure, the weight of the structural steel and miscellaneous building items within the containment vessel.

A =The pipe or equipment anchor loads are the loads exerted upon the various structural elements in the reactor building internal structure by the pipe or equipment restraints for normal thermal expansion of the various piping systems.

T_1 =Moments and forces caused by the temperature distribution during normal operating, steady state condition.

DBE =Moments and forces caused by a design basis earthquake.

Q =Loads exerted upon the reactor building internal structure by a pipe or a piece of equipment as a result of a postulated LOCA.

P =Pressure loads within the primary shields area, including jet impingement, resulting from a LOCA.

L' =The dead weight of the various pieces of equipment, including water steam or the other enclosed fluids, supported by the reactor building internal structure.

The temperature within the cavity was taken as the normal operating temperature, 120F, and the transient pressure within the cavity as shown in Unit 1 FSAR Figures 6.2-19a through 6.2-19r for cold leg guillotine break.

The acceptance criteria applied to the concrete stress analysis was that of ACI 318-63 except as regards the tensile strength of concrete (see Section C.1.b of FSAR Appendix 3H). Also, no strength reduction factor is applied to steel stresses. The structural steel analysis meets the following criteria for element stress versus allowable stress:

flexure = 1.0 y
shear = 1.0 y / $\sqrt{3}$

Table 3.4-1 provides RPV support loads based on all loads except the IAL for a cold-leg-guillotine break. The horizontal loads were increased until the allowables cited above were reached.

Figure 3.4-1 shows the reactor support loading diagram for the limiting case.

3.4.2 REACTOR VESSEL SUPPORT PADS

Combustion Engineering has conducted an evaluation of the capability of the RPV support pads to withstand the loads associated with a LOCA occurring in the reactor cavity.

The capability of the support pads is calculated from the stress report, assuming faulted conditions (pipe rupture + SSE), and further assuming that the stresses resulting as a combination of vertical and horizontal load components cannot exceed the stresses allowed by the ASME Code for such faulted conditions.

The procedure of computing support pad capabilities assures that there is margin in the design, in the sense that additional support capability exists in the plastic domain; however, this is deflection limited.

The maximum allowable loads on an elastic basis in the horizontal and vertical directions are given in Figure 3.4-2.

4.0 RESULTS OF ULTIMATE REACTOR SUPPORT CAPABILITY EVALUATION

The methods utilized in Section 3.0 were utilized to determine the IAL and the ultimate restraint capability of each RPV support system component. The results on a per support basis are:

Steel support structures	6550 Kips
Reactor cavity wall	6300 Kips
RPV support pad	7200 Kips

The maximum load for all load contributions except the IAL is 4500 kips per support. Thus there is an 1800 kip per support margin to accomodate the IAL, which is not expected to exceed 1500 kips per support.

5.0 CONCLUSION

Since the identification of the internal asymmetric load as a possible new additional accident loading condition in May, 1975 considerable work has been done to evaluate this phenomenon. The work indicates that the arbitrariness of the large reactor coolant pipe ruptures now being considered is not fully appreciated. These assumed pipe ruptures were originally postulated to assess the functional performance characteristics of the containment and the

TABLE 3.4-1

ST. LUCIE PLANT

REACTOR VESSEL SUPPORT LOADS (X10^6 lb)

REACTOR SUPPORTS

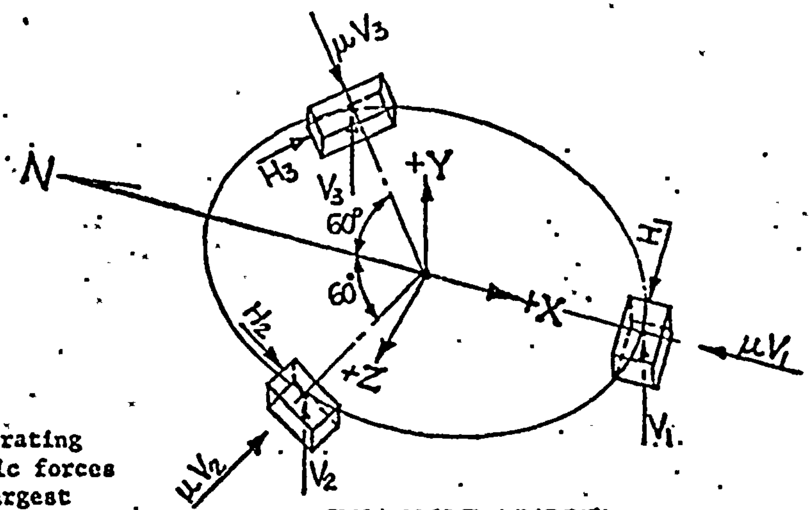
Condition Load	Normal Operating Dead Weight	Normal Operating Thermal +D. WT	OBE Seismic ±X	OBE Seismic ±Y	OBE Seismic ±Z	DBE Seismic ±X	DBE Seismic ±Y	DBE Seismic ±Z	Pipe Accident (1) A*	Pipe Accident (1) B*	Pipe Accident (1) C*	Pipe Accident (1) D*
H1	0	.028	.005	.002	-.644	.011	.005	-1.288	-4.500	4.500	4.500	-4.500
V1	.666	1.155	.032	.335	-.046	.064	.670	-.092	2.000	2.000	2.000	2.000
μV1	±.195	±.350										
H2	0	-.091	1.226	.001	+.355	2.452	.003	+.710	-4.400	3.000	4.400	-3.000
					-.264			-.528	1.500	1.500	1.500	1.500
V2	.634	.726	.017	.253	.380	.035	.507	.761				
μV2	±.195	±.215										
H3	0	.079	1.139	-.019	.270	2.278	-.038	.540	3.000	-4.400	-3.000	4.400
				-.060	-.349		-.120	.698	1.500	1.500	1.500	1.500
V3	.634	.741	.367	.623	.743	-.006	.506	.746				
μV3	±.195	±.215										

*A - Cold Leg Guillotine Leg 1B (Northeast Quadrant)

*B - Cold Leg Guillotine Leg 1A (Northwest Quadrant)

*C - Cold Leg Guillotine Leg 2B (Southwest Quadrant)

*D - Cold Leg Guillotine Leg 2A (Southeast Quadrant)



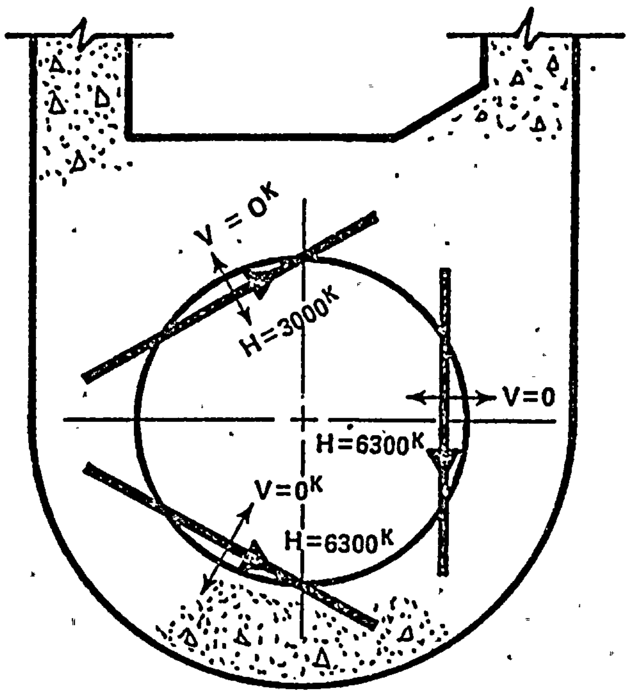
REACTOR VESSEL SUPPORT REACTIONS.

Note 1: Pipe accident loads include a dynamic factor of 2, normal operating loads and maximum seismic loads. The direction of the seismic forces included in each accident load was chosen to result in the largest load at each component

N ←

LOCA CASE:

1. COLD LEG GUILLOTINE



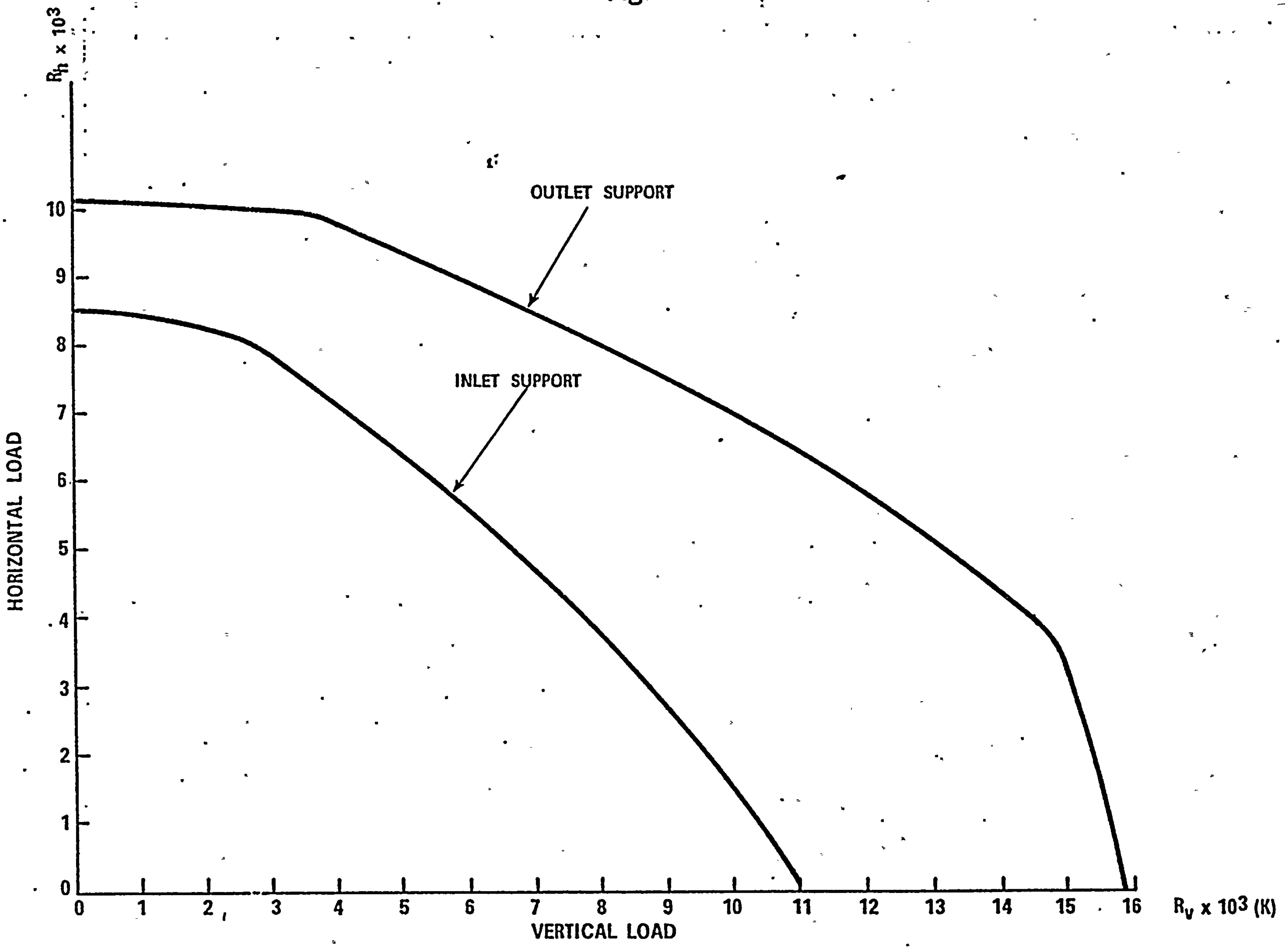
PLAN

NOTE: LOADS SHOWN ARE LOADS TRANSMITTED FROM REACTOR TO REACTOR SUPPORTS.

FLORIDA POWER & LIGHT COMPANY ST. LUCIE PLANT
REACTOR SUPPORT LOADING DIAGRAM
FIGURE 3.4-1

REACTOR VESSEL SUPPORT PAD CAPABILITY

Figure 3.4-2



emergency core cooling system, i.e., they were postulated as an analytical tool only.

The SAI study employed fracture mechanic techniques, design cyclic conditions to be experienced during the facility lifetime and reactor coolant system stress distributions to evaluate the propriety of assuming large arbitrary reactor coolant system failures. The SAI report demonstrates that the specific pipe failures that must be postulated to analytically predict the IAL and EAL are of very low probability.

The above nonwithstanding the following loads per support are appropriate for a cold-leg break at the RPV nozzle:

Classical Thrust force (PA)	1700 kips
Deadweight + thermal + seismic + thrust (PA) + external asymmetric load (EAL)	4500 kips
Internal asymmetric load (IAL)	1500 kips

Thus the total load associated with a cold leg guillotine at the RPV nozzle is 6000 kips per support. The RPV supports have been analyzed and shown to acceptably accomodate an assumed load of 6300 kips per support.

The analysis of the postulated cold leg guillotine indicated that the RPV supports can acceptably accommodate with margin the limiting load combinations associated with a cold leg guillotine break. In reality, the margin is greater than the 300 kips per support indicated above. The IAL is expected to be less than 1500 kips per support. Detailed structural-thermal hydraulic calculations to assess the load reducing benefit of flexible internal structures would be expected to reduce the IAL in the order of 30 percent. In addition the analysis assumed that the IAL and EAl peak values were additive. In reality, the internal load peak would precede the peak of the transient load within the reactor cavity. However, the results of this study obviate the need to quantify this effect. Thus, only secondary internal load peaks of reduced intensity would combine on a time-history basis with the cavity transient.

The events that have been analyzed are over in a fraction of a second. If the break opening time were to exceed about 10 to 15 msec, the IAL contribution to the RPV support load would be insignificant when compared to other loading conditions, which have been previously analyzed within the Safety Analysis Report. Thus, it is reasonable to question whether or not the analytically predictable IAL is representative of real-world conditions.

Based on the very low probability of the events required to predict the IAL and the demonstrated capability of the ability of the Unit 1 and Unit 2 support system to acceptably accommodate all loads that could result from a cold leg guillotine, no further work on the capability of the RPV supports is required.

ATTACHMENT 1

CE-REACTOR OWNERS GROUP STATEMENT TO THE 206TH ACRS MEETING ADEQUACY OF REACTOR VESSEL SUPPORT SYSTEM (NORTH ANNA SYNDROME)

The CE-Reactor Owners group wishes to thank the members of ACRS and their staff for the opportunity to introduce this statement in the records of the meeting.

The group as presently constituted comprises the following utilities: Baltimore Gas and Electric, Florida Power & Light, Consumers Power, North-East Utilities, Omaha Public Power District, Maine Yankee, and General Public Utilities.

Initially the group met with Combustion Engineering to discuss the schedule which CE could support to provide a detailed analysis of the analytically predicted loads related to cavity and vessel adequacy, and the cost that such an analysis would entail.

The primary reasons for which the group initially got together were the following:

a) Sharing of costs and schedular advantages by performing generic analyses simultaneously.

b) Development of a consistent technical position for CE reactors.

From this first and subsequent meeting, however, it became quite obvious that

a) the cost of analysis alone would be in the multimillion dollars bracket, and could take one and one half year or more to complete.

b) not all utilities would require the same number, types, or depth of analyses.

c) the internal and external asymmetric loads are strongly dependent on the particular primary system (A/E specific) support and cavity design, thus generic analyses may be difficult.

d) the internal and external asymmetric loads are additive and hence limiting for design, only as they result from the postulation of large, essentially instantaneous pipe breaks at very specific primary coolant loop locations, namely within the reactor cavity.

e) the analytical techniques were still under development and were undergoing review by the NRC staff.

ATTACHMENT 1) (Continued)

Accordingly the group felt that a crucial consideration to the appropriateness of evaluating these loads is whether or not the postulated breaks are likely to occur.

To pursue this pivotal issue the group engaged Science Applications, Inc. (SAI) to conduct a study intended to:

a) quantify the probability of the event, namely the large break in the cavity.

b) see how the probability of the event is affected by inservice inspection.

The latter was undertaken in the hope that results from such analysis might point to an alternate way of coping with this potential regulatory problem area, which some of the utilities may want to consider.

Due to similarities of the plants involved, one generic study was conducted initially, but later Maine Yankee independently also engaged SAI in performing an identical study for their plant, which differs from the others by being a three loop plant.

In addition we understand that the B&W Owners group has also independently engaged SAI to perform an evaluation for its reactors, and that the conclusions are consistent with the findings of the CE owners group sponsored work. If so, we believe this reaffirms the propriety of the SAI approach. Their report is scheduled for submittal in July, 1977.

At the same time the CE-Owners group requested proposals from several consulting firms, knowledgeable in the field of thermal hydraulics and structural dynamics to perform the analyses. Review of these proposals together with information gathered from ongoing ACRS subcommittee meetings showed that the state-of-the-art of the analytical techniques was not commensurate with the problem at hand. This appears to be the case still to date.

The SAI study entitled "An Analysis of the Relative Probability of Failure at Various Locations in the Primary Cooling Loop of a Pressurized Water Reactor Including the Effects of a Periodic Inspection" SAI-001-PA, was discussed with members of the NRC staff about five months prior to submittal as a generic topical report.

No formal questions have been received to date by the group or SAI on the report from the NRC staff. There have been informal questions, mostly requesting additional information on weld details, welding procedures, etc. received directly from EG&G Idaho, during their review of the report. The results of the EG&G and staff reviews have not been discussed with SAI and the CE-Owners group.

The important assumptions and results of the SAI study were presented to the ACRS subcomittee in Los Angeles, on December 1, 1976. However, it is our feeling that neither the NRC staff nor the ACRS members had

ATTACHMENT 1 (Continued)

apparently had sufficient time to study and evaluate the report, prior to that meeting. Therefore, Dr. Plesset stated that the committee might again wish to meet with the group following completion of the NRC evaluation, but that meanwhile the staff should examine the likelihood and potential of water hammer in the CE primary system.

We would like to again summarize the salient assumptions and results of the SAI study here.

The fundamental assumption underlying the study is that crack growth by fatigue is the mechanism leading to the large pipe failures of importance in the evaluation of the reactor supports. This is considered a reasonable assumption in view of the absence of the possibility of significant fluid hammer transient loads in larger pipes in a CE primary system. The as-fabricated flaw depth distribution in the weld region of the piping is used in conjunction with the probability of detecting a flaw as a function of its size to derive flaw size distribution prior to the plant being placed in operation. Changes in the flaw size distribution during service life are calculated by fracture mechanics techniques utilizing conservatively derived cyclic stress ranges at each of the piping locations examined. These cyclic stresses occur due to the various transients anticipated to occur during the plant's service life. It is significant to note that the more severe cyclic stresses are due to transients of low probability. The changes in flaw size distribution, and hence the distribution in time and space of flaws having a critical size, i.e., a size which may grow to a large failure, allows probabilities of large failures at various locations to be determined as a function of time.

The results of the study indicate that the probability of a large rupture in the primary coolant loop is very low, and further, that if a large rupture were to occur, only one in four or five would occur in areas resulting in additive internal as well as external (cavity pressure) asymmetric loading, namely in the reactor cavity annulus. Further the study demonstrates that augmented nondestructive examination within the cavity beyond Section XI requirements can, depending on the inspection interval, reduce this ratio by two to four orders of magnitude.

These results, we believe, reflect the adequacy of th RPV supports and also strongly suggest that a detailed re-evaluation of the reactor vessel supporting system is unwarranted, because of the low probability of the initiating event. In addition, we do not believe that substantial additional protection to the public health and safety will result from the multimillion dollar expenditure required to perform these analyses.

The CE-Owners' group is very disappointed with the lack of feedback from the NRC staff. The only indirect feedback that we have obtained is that at the ACRS Subcommittee Meeting of May 26, 1977 in Los Angeles, the NRC stated that they do not agree that the probability of the event is sufficiently low.

ATTACHMENT 1 (Continued)

This judgement is predicated on EG&G's evaluation of the SAI report, which concluded that there is insufficient applicable data available to support the analyses.

The staff further indicated that the Probability Study would serve as justification for allowing existing plants to continue operation for the time period required to complete a detailed fluid-structural analysis, but that they would require the full analysis.

Their apparent rejection of the methodology utilized to quantitatively assess the probabilistic behavior of flaws in large piping systems, without technical dialogue with SAI and the CE-Owners' group, may foreclose the basis for any alternatives to analyses. We believe that such alternatives, which could include augmented inservice inspection at selected points, as appropriate, would serve a far more useful purpose than analysis of incredible events with tools, the adequacy of which is still doubtful.

We hope to meet with the staff as soon as possible to discuss their evaluation of the SAI report, including the EG&G evaluation which we have not seen, and would welcome the opportunity to discuss the report with the ACRS Committee members.

Considering the scope of work required to pursue further evaluation of the effects of postulated combined internal and external asymmetric loadings for operating facilities, the state-of-the-art of the analytical techniques, and the results of the SAI study showing the extemely low probability of the initiating event, we believe that further analyses should not be pursued, and would urge further evaluation of the SAI report and its conclusions, as a better alternative.

ATTACHMENT 2

ONE DIMENSIONAL ACOUSTICS FOR ST. LUCIE

In the instance of St. Lucie the closest place at which a break can occur is the safe end of the nozzle, which is approximately 30 inches from the downcomer region. The characteristic length of the downcomer region is the half circumference which is approximately 255 inches as meausred from the edge of the nozzle. Hence the maximum force from acoustic decompression which one could foresee, if the vessel could be modelled in one dimension, with no interior obstacles is;

$$F_{max} = P_o A_2 (A_2 - A_1) R^4 P_o$$

For the St Lucie nozzle and downcomer area with A_2 = 34 sq. ft, R=0.748, thus

$$F_{max} = 4.94 \; P_o \; A_{break}$$

This expression neglects the fact that the recompression waves from the interface region to the break plane will travel at lower sonic speeds since they will move in a low quality two phase fluid. This effect results in a lowering of the force to values closer to $3P_o A_{break}$. Certainly, the barrel acts at least as an interior obstacle. A better one dimensional representation of the vessel is given in Figure A-1(a).

The area ratio between nozzle and large pipe is kept at 5:1 for consistency with the Hansen's experiment. The ratio of nozzle to total length is decreased, but is not made too large, to see if peak forces could occur after the first reflection at the closed end. The dimensions of the obstacle are arbitrary and chosen for convenience.

For such a system the initial decompression reaches the large pipe in 3 msec. Here it is relfected as a compression wave of amplitude. $0.667P_o$, which reaches the open end and is reflected as an equal rarefaction at 6 msec. The transmitted rarefaction wave reaches the partially closed end configuration given by the large pipe and the rigidly held plug at 4 msec. Here it is transmitted and reflected as rarefaction waves.

The transmitted wave has amplitude $\dfrac{A_2 + A_3}{2A_3} P'$,

where P' is the incident rarefaction wave of amplitude $0.333P_o$. The reflected rarefaction wave has amplitude

$$\frac{A_2 - A_3}{2A_3} P'$$

The waves at selected times are shown in Figure A-1(b).

The force acting on the system is also given in Figure A-1(c). It is seen that its maximum value occurs between 9 and 10 msec, at which times the presure at the closed end is still 3000 psig, while the pressure in the nozzle area has decayed to 0 psig. At this time the force equals 6000 lbs, which equals $2P_oA_1$.

The same dimension system with no rigid internal blockage would have had a maximum force of 2.33 P_oA_1. Hence it appears that the effect of internal blockage on maximum forces is beneficial.

It is equally clear that the effect of the blockage is to depressurize the region between the blockage and the break (as well as the region between the blockage and closed end) faster than without blockage so long as the interface dimension is shorter than the nozzle dimension. Further the larger the blockage area the smaller the maximum force. This, of course, is to be expected since for no blockage and a very long large pipe, the max-force will approach P_oA_2, which for complete blockage the force is just P_oA_1, which represent the two extremes of possible acoustic hydraulic forces.

In the case of St Lucie, the real decompression phenomenon cannot in all probability be truly modelled in one dimension as stated previously. In fact the phenomenon is tri-dimensional, but the acoustic disturbance propagation in the downcomer annular region can be described as a two-dimensional cylindrical wave.

One dimensional approaches appear more valid if one wishes to evaluate the average pressures in the top and bottom plenums, but not the local pressures around the barrel.

However, in terms of fluid decompression, apart from the question of dimensionality, the downcomer region would appear to the incoming rarefaction wave as an unrestricted path up to a closed end (the opposite side of the vessel, bottom, etc.). One can then expect a pressure behavior similiar to that exhibited in the Hansen experiment, as modified by the presence of the internals.

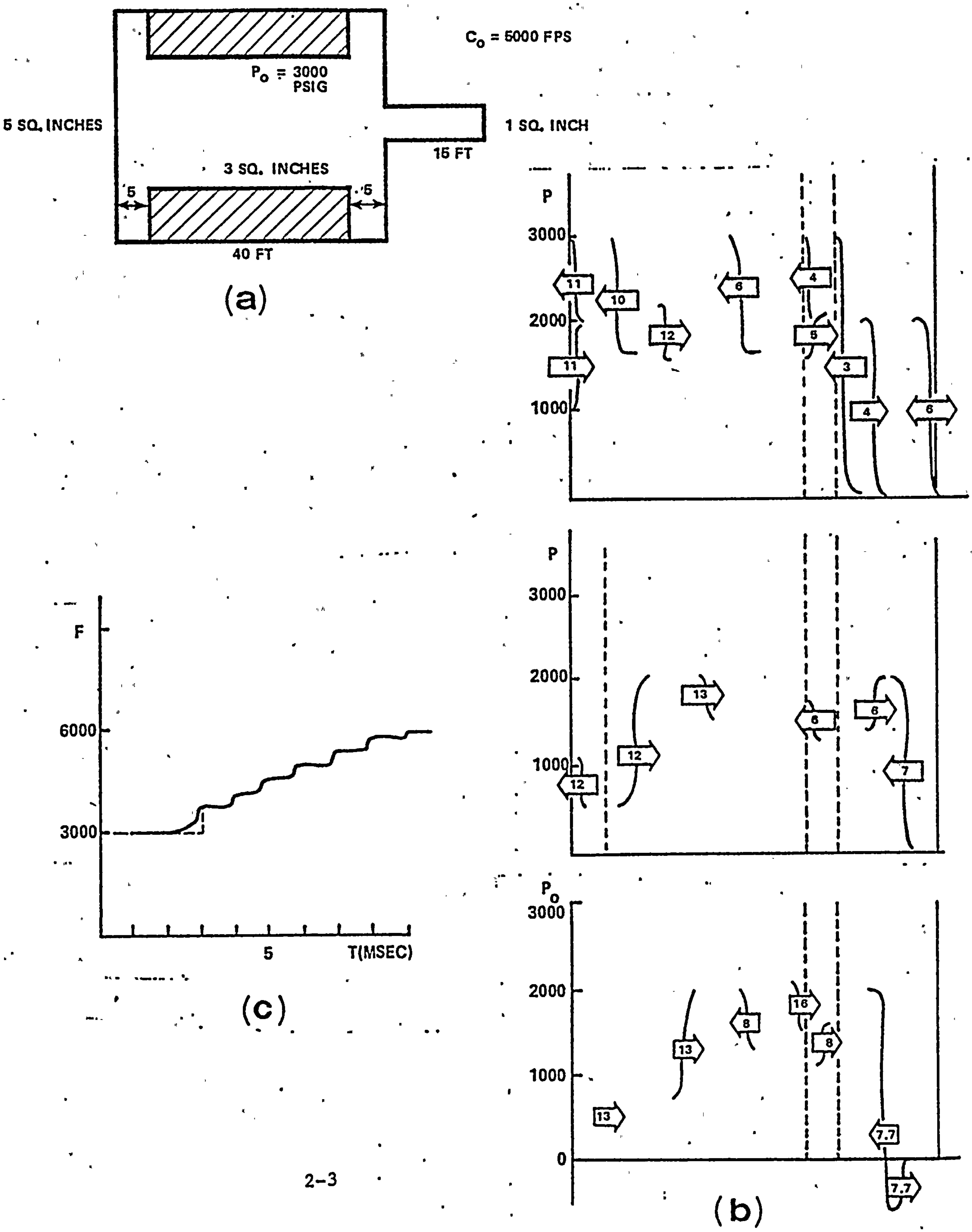
Since the system is modelled in one dimension, there is some question as to which are the proper vessel and internals areas projected normal to the thrust which one should use. Two cases can be examined. In the first one, the area A_2 is chosen a 34 ft^2 which is the horizontal cross sectional area of the downcomer annulus. In this case, the one dimensional vessel area normal to the thrust is taken as the horizontal cross sectional area of the vessel. The internals occupy 78 percent of this area. The one dimensional model becomes that of Figure A-2. The one-dimensional load which is given by

$$F_{TOT} = P_o A^{\text{Projected}}_{\text{vessel}} - P_o A^{\text{Projected}}_{\text{Internals}} + R^4 P_o A^{\text{Projected}}_{\text{Internals}}$$

$$- R^4 P_o (A^{\text{Projected}}_{\text{vessel}} - A_{\text{break}})$$

ONE DIMENSIONAL MODEL WITH RIGID INTERNALS

FIGURE A-1



becomes

$$F_{TOT} = 0.22P_oA_{vessel}^{Projected} - .313\left[0.22P_oA_{vessel}^{Projected}\right] + .313P_oA_{break}$$

$$= 0.151P_oA_{vessel}^{Projected} + .313P_oA_{break}.$$

But since

$$A_{vessel}^{Projected} = 6.93\ A_{break}$$

$$F_{TOT} = (1.05 + .313)P_oA_{break} = 1.35P_oA_{break}$$

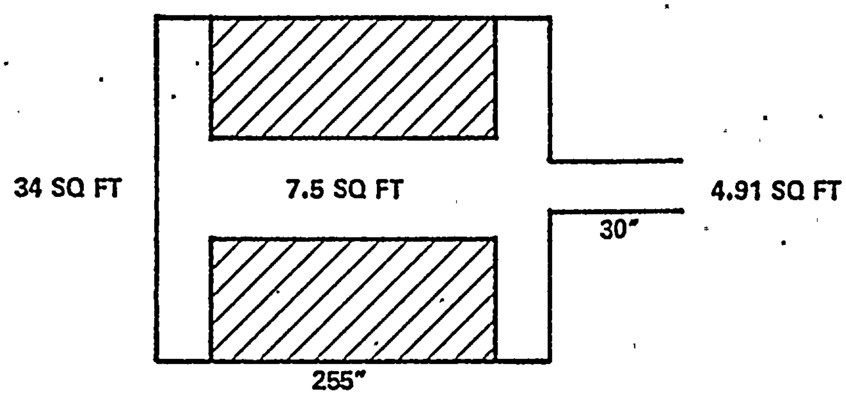
In the second case the projected vessel area is the vertical cross-sectional area of the vessel corrected for a vertical distance equal to the half circumference (time prior to reflection), i.e., A_2 = 283 sq. ft. The internals and barrel occupy 88 percent of this area. The one dimensional model is that of Figure A-3. In this instance R = 0.965 and since $A_{vessel}^{Projected} = 57.0\ A_{break}$,

$$F_{TOT} = 1.845P_oA_{break}.$$

Therefore on the basis of simplistic one dimensional models, the additional force due to "internals" effects only should be between 0.36 and 0.845 times the traditional thrust load.

ST LUCIE ONE DIMENSIONAL
MODEL WITH RIGID INTERNALS
(ALTERNATE 1)

FIGURE A-2



ST LUCIE ONE DIMENSIONAL
MODEL WITH RIGID INTERNALS
(ALTERNATE 2)

FIGURE A-3