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PRESSURE SUPPRESSION CONTAINMENT
DESIGN-CURRENT STATE OF THE ART
by D.R. MILLER
Journal of Engineering for Power,
January 1969

March 13, 1986

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Pressure Suppression Containment Design-Current State of the Art[1]

A summary of the thermodynamic design limits for pressure suppression containments for nuclear reactors is presented. Those parameters which must adhere to tested values are tabulated and discussed. An analytical model is described and is shown to accurately predict the existing test data. A graphical technique for predicting the transient peak containment pressure, based on the model, is presented for use in containment design.

Introduction

THIS PAPER is presented as a summary of the current design philosophy of the pressure suppression containment concept. Basically a pressure suppression containment utilizes a heat sink (water in this case) in which to quench and store the energy released due to postulated loss-of-coolant accidents, thereby substantially reducing the required primary containment volume.

A great many test data have been collected which prove the pressure suppression concept. Most notable of these are the tests performed by the Pacific Gas & Electric Company and the General Electric Company at the former's Moss Landing facilities in support of the Humboldt Bay [1][2] and Bodega Bay [2] pressure suppression containment designs. These tests form the basis of all current pressure suppression containment designs. Because detailed analytical models of the transient pressure response have not been available until recently, nearly all pressure suppression designs have been based on duplication of nearly all the parameters of the Bodega Bay tests. Although the phenomenological condensation process has not been modeled yet, the pressure transient itself can be accurately reproduced using an analytical model which will be described in this paper. This model permits a greater degree of design freedom with respect to selection of volumes, vent flow areas, vent resistance, and design pressure than was possible by the strict adherence to all of the Bodega Bay parameters while at the same time retaining the test parameters of significance to the condensation process. The key design limits from these tests will be summarized and the accuracy of the analytical models now in use will be demonstrated.

A complete containment design must consider many requirements in addition to transient pressure performance too numerous to cover here. However, sufficient detail will be presented to allow sizing of pressure expression containments of both the steel, lightbulb-torus design and the new concrete, over-under design in so far as the short term pressure transient requirements are concerned.

Key Design Considerations

As a basis for containment design an instantaneous rupture of the largest pipe in the primary system has been hypothesized. In a pressure suppression containment the steam released due to this accident would cause the pressure in the containment vessel which houses the reactor, called the drywell (see Fig. 1), to rise, which would cause flow through the vents into the suppression chamber. The steam flow would be condensed in the water and the entrained noncondensible gases would bubble to the surface and collect in the suppression chamber air space. After the reactor vessel blowdown is over, flow through the vents diminishes, and the drywell and suppression chamber come to the same equilibrium pressure. A typical pressure transient is shown in Fig. 2.

Given the rupture of the largest pipe as a design basis, there are two primary design objectives to be considered in connection with pressure suppression containment designs: (*a*) Insure complete and rapid condensation of the steam in the suppression chamber. (*b*) Provide appropriate system volumes and vent flow area to limit the transient drywell pressure to within the containment design pressure. To a large extent the above objectives are uncoupled. That is, design parameters which may be important for insuring condensation in the suppression

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[2] Numbers in brackets designate References at end of paper.

Contributed by the Nuclear Engineering Division and presented at the Winter Annual Meeting, Dec. 1–5, 1965, New York, N. Y., of THE AMERICAN SOCIETY OF MECHANICAL ENGINEERS. Manuscript received at ASME Headquarters, August 6, 1965. Paper No. 65-WA/NE-1.

Nomenclature

A = area

C_p = specific heat capacity at constant pressure

C_v = specific heat capacity at constant volume

E = total energy

e = specific energy

$\frac{fL}{D}$ = vent loss coefficient

h = specific enthalpy

M = total mass

$\dot{m}$ = mass flow rate

P = pressure

$\hat{P}_D$ = transient peak drywell pressure

$P_v(T)$ = vapor pressure water at temperature T

R = gas constant

T = temperature

V = volume

$v(T)$ = specific volume at temperature T

X = steam quality

Subscripts

a = air

b = postulated break in reactor system

d = drywell

f = saturated water

fg = property of change of state, liquid to steam

fr = liquid in reactor system excluding feedwater

g = saturated steam

l = subcooled liquid water

R = reference value

r = reactor system

s = suppression chamber

t = total

v = vent

w = water (liquid and steam combined)

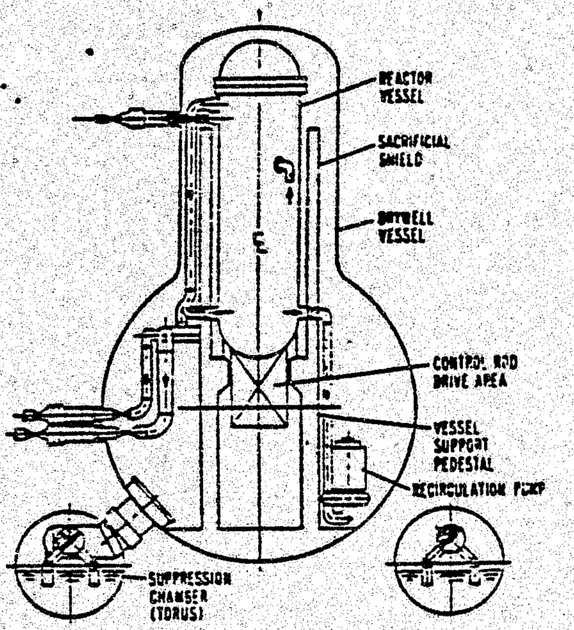

Fig. 1 Cross section of a typical pressure suppression containment

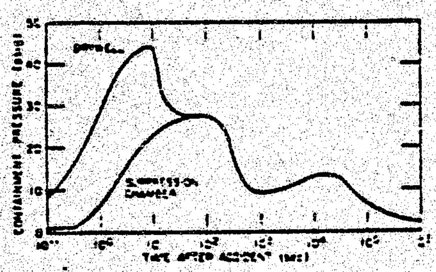

Fig. 2 Typical containment pressure following design basis accident

chamber have little influence on the containment pressure transient provided complete condensation is achieved. At the same time, parameters which strongly influence the pressure transient have only second order effects on the condensation phenomena.

Quantitative analysis of the condensation process is very difficult based on current technology. Because of this lack of a suitable analytical model, a very conservative approach is taken in the selection of design criteria which relate to the condensation process. Specifically, these design parameters are established in strict adherence to the configurations tested at Moss Landing, although fundamental considerations lead to the conclusion that it would be very difficult to prevent complete condensation.

The pressure transient in the containment, however, is amenable to analysis, and analytical tools have been developed which accurately reproduce both the Humboldt and Bodega test data. As a result of these tools, major changes in containment volumes, vent flow area, vent resistance, and containment design pressure are now possible without requiring additional testing. This capability opens many avenues to system optimization from both an economic and safety point of view.

Analytical Models

Reactor Vessel Blowdown. The first step in the analysis of a containment pressure transient is the calculation of the reactor vessel blowdown following a postulated pipe rupture. The flow rate exiting the rupture is of specific interest in pressure suppression containments since this flow serves as the driving force for the containment pressure response. Therefore, it is important that the calculated blowdown flow rates be conservative.

There are several models available for calculating the critical flow rate of a single-component, two-phase mixture. The model developed by F. J. Moody [3] has been adopted for use with pressure suppression containment analyses because the model is believed to represent a theoretical maximum critical flow rate; and, therefore, bounds the flow rate for containment design purposes. The effects of friction, vapor entrainment, thermodynamic phase non-equilibrium, and near homogeneous two-phase flow pattern will tend to reduce the actual flow rate to something less than the value predicted by Moody's ideal model. Hence, the use of Moody's model without friction conservatively maximizes the loading imposed upon the containment.

Containment Pressure Transient. Assumptions. Giving the loading which is imposed on the containment by the reactor vessel blowdown as calculated using Moody's critical flow model, the containment pressure transient can be calculated. An analytical model has been developed for this purpose which accurately predicts both the Humboldt and Bodega transient pressure data when allowances are made for known uncertainties. The key assumptions employed by the model are:

(*a*) Thermodynamic equilibrium of a saturated mixture of steam, water, and air exists in both drywell and suppression chamber. (*b*) The composition of the fluid flowing in the vents is based on a homogeneous mixture of the fluid in the drywell. (*c*) The flow in the vents is compressible except for the liquid phase. (*d*) No heat is lost from the contained gases.

Drywell State Equations. Assumption (*a*) allows simple mass balance, energy balance, and state relationships to be written which define the drywell state. Specifically, the mass balance on the drywell can be expressed as:

$$\dot{M}_{wd} = \dot{m}_{g} + \dot{m}_{f} - \dot{m}_{g} - \dot{m}_{f} \qquad (1)$$

$$\dot{M}_{ad} = -\dot{m}_{a} \qquad (2)$$

The above assumes the vent flow rates are known. Actually these flow rates are a function of the results of the mass and energy balances so that iteration is required. The calculation of the vent flows will be discussed later.

Assuming no heat losses the energy balance on the drywell can be expressed as:

$$\dot{E} = h_{g}\, \dot{m}_{g} + h_{f}\, \dot{m}_{f} - h_{g}\, \dot{m}_{g} - h_{f}\, \dot{m}_{f} - C_{pa}\, T_{d}\, \dot{m}_{a} \qquad (3)$$

and the state equation of saturated mixture of steam, air, and water can be expressed as:

$$E_{d} = \left[e_{f} + \frac{e_{fg}}{v_{fg}} \left(\frac{V_{d}}{M_{wd}} - v_{f} \right) \right] M_{wd} + C_{va}\, T_{d}\, M_{ad} \qquad (4)$$

From equations (1) through (3) the mass and energy content of the drywell is determined on a step-by-step basis. Equation (4) is then solved by iterating for the value of T_d and the associated properties (e_f, e_{fg}, v_{fg}, and v_f) which satisfy the equality. Because the system is saturated, knowing the drywell temperature specifies the drywell pressure, i.e.

$$P_{d} = \frac{M_{ad} R T_{d}}{V_{d}} + P_{s}(T_{d})$$

Suppression Chamber Equations. In order to calculate the suppression pool temperature response energy and mass balances are performed on the pool mass

$$\dot{E}_{lw} = h_{g}\, \dot{m}_{g} + h_{f}\, \dot{m}_{f} + C_{pa}(T_{d} - T_{l})\dot{m}_{a}$$

$$\dot{M}_{lw} = \dot{m}_{g} + \dot{m}_{f}$$

Knowing the energy and mass at any point in time specifies the pool temperature, i.e.,

$$T_{lw} = f(e_{lw}) = f\left(\frac{E_{lw}}{M_{lw}}\right)$$

Since all of the noncondensible gases collect in the suppression chamber air volume, the mass balance on this air volume can be written as:

$$\dot{M}_{as} = \dot{m}_{a}.$$

The assumption is made that the air temperature is equal to the water temperature so that

$$T_s = T_f.$$

and

$$P_s = \frac{M_{as} R T_s}{V_{as}} + P_v(T_s)$$

where

$$V_{as} = V_s - v_f(T_s) M_{fs}$$

Drywell Vent Flows. Now at any point in time the drywell and suppression chamber pressures are known and it is possible to calculate the flow rate through the drywell vents. The flow rates are calculated basically as the compressible flow of an ideal gas in a duct with friction; however, the effects of the liquid being carried over into the vents is also considered. The resulting equations relate flow rate to pressure ratio, quality, and friction factor (fL/d), i.e.,

$$\dot{m}_t = \dot{m}_a + \dot{m}_g + \dot{m}_f = f\left(\frac{P_s}{P_d}, x, \frac{fL}{D}\right)$$

Once the total mass flow rate is known, the flow rates of each of the individual components is calculated based on the mass fractions of the components in the drywell, for example:

$$\dot{m}_{at} = \frac{M_{ad}}{M_{ad} + M_{fd} + M_{gd}} \dot{m}_t$$

The required equations for the transient response are now completely specified and it is merely a matter of performing the calculations on a step-by-step basis.

Comparison of Model with Test Results. Phenomena Affecting Comparison. The above analytical model has been checked against all of the existing data from both the Humboldt and Bodega series of pressure suppression tests and has been found to be highly accurate. However, there are three phenomena which must be given proper attention when comparing the model with the test data. These are: *(a)* Reduced reactor vessel blowdown flow rates due to the effects of friction and vapor entrainment in the exiting flow. *(b)* Reduced drywell pressure due to the effects of condensation on the drywell vessel walls. *(c)* Variations in the amount of liquid which is carried over into the vents rather than remaining in the bottom of the drywell vessel.

Concerning item *(a)*, if the size of the break is large enough compared to the cross sectional area of the vessel, vapor bubbles which form in the vessel will be swept out of the break rather than rising to the surface. Vapor entrained in the exiting flow significantly reduces the total mass flow rate out of the break. This effect is predicted by Moody's model, but agreement between test and theory requires knowledge of the quality of the exiting flow which can only be approximated from the data.

Condensation on the drywell walls reduces the drywell pressure by removing energy from the stream atmosphere. This effect was particularly significant in the tests due to the relatively large drywell surface area and cold drywell walls, however tests in which condensation was eliminated by preheating of the drywell walls are accurately predicted by the model.

Increased liquid carryover into the drywell vents increases the drywell pressure by increasing the flow pressure drop through the vents. The effect of this parameter on the pressure transient can be determined from some of the data from the Humboldt series of tests.

Humboldt Tests. The uncertainties due to the three phenomena discussed above were minimized in some of the Humboldt tests, and good agreement with the model is obtained with these data. Specifically: *(a)* The flow area of the vessel nozzle was relatively small so that the vapor entrainment at the break and fric-

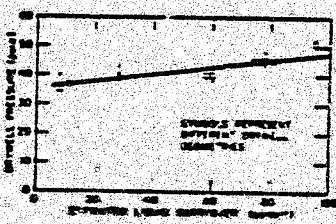

Fig. 3 Effect of liquid carryover on drywell pressure

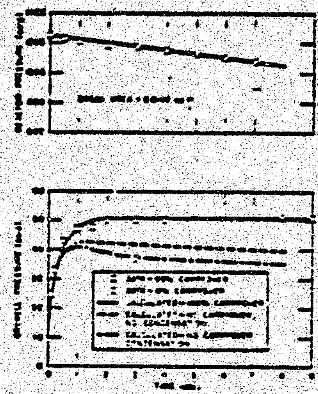

Fig. 4 Humboldt Bay pressure suppression data. Break area = 0.0147 sq ft

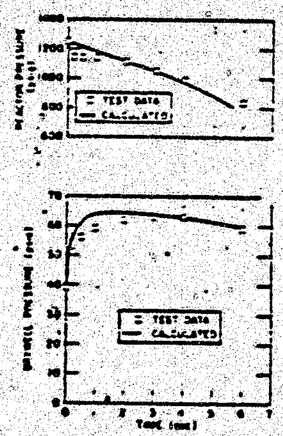

Fig. 5 Bodega test no. 32, break area = 0.0573 sq ft

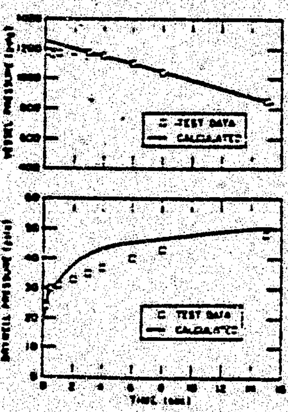

Fig. 6 Bodega test no. 26, break area = 0.0218 sq ft

tion were negligible and the Moody critical flow model accurately predicts the vessel blowdown [4]. (b) In some of the tests the drywell was preheated which eliminated condensation on the drywell walls. (c) The degree of liquid carryover was varied by changing the drywell geometry, and the liquid remaining in the drywell was measured and recorded at the end of each test.

First consider the variation in liquid carryover. In Fig. 3 the measured drywell pressure is plotted against the percent liquid carryover. Each symbol indicates a particular geometry. This geometry dependence is primarily due to the orientation of the inlet duct from the reactor vessel, i.e., downward oriented inlet ducts tended to promote carryover by preventing the formation of a pool of water in the bottom of the drywell. Also note that the drywell pressure is essentially linear with liquid carryover.

The accuracy of the reactor vessel blowdown calculation is illustrated in Fig. 4. The agreement is very good, which (a) lends support to the Moody critical flow model and (b) provides good input for checking the drywell pressure model.

In the lower half of Fig. 4, the drywell pressure as a function of time from two of the tests from Fig. 3 is shown. The upper data are from a test which had preheated drywell walls which eliminated condensation and a drywell geometry which promoted complete liquid carryover. The agreement with the calculated response is within a few percent. The lower data are from a test which had an initially cool drywell and essentially no liquid carryover. Calculated responses for the no-carryover condition with and without condensation are shown and the agreement is excellent if the effects of condensation are considered. Based on this comparison with the test data, a good deal of insight into the problem is obtained and considerable confidence in the analytical model is gained. In fact, this series of test provides a real cornerstone of confidence in the ability to calculate containment pressure transients.

The effects of condensation can also be demonstrated using some of the Bodega data. In Fig. 5 the data are shown for a test in which the drywell was completely purged with steam before the transient was initiated. This "prepurging" heated the drywell walls thereby eliminating condensation and generally promoting complete liquid carryover. The agreement between test and model is very good. Data from a test without the prepurging are shown in Fig. 6 and as expected the drywell pressure is overpredicted. Since the reactor vessel blowdown is accurately reproduced, the overprediction of the drywell pressure is believed to be due to the effects of condensation on the drywell walls. This presumption is supported by the improved agreement between analysis and test 15 seconds into the transient by which time the drywell walls would have been heated sufficiently to eliminate condensation.

While overprediction of the reactor vessel blowdown rate complicates accurate checking of the model with larger simulated break areas, one important generalization can be made. The analytical model always predicts peak drywell pressures equal to or greater than the measured peak drywell pressure. This result is to be expected since all three of the significant uncertainties (vapor entrainment, condensation, and liquid carryover) have been conservatively bounded by the assumptions contained in the analytical model. The comparison of the analytical model with all of the test data is summarized in Fig. 7 where the trend of overpredicting the peak drywell pressure with increasing break size is clearly illustrated. This overprediction for known reasons at larger breaks and the very accurate prediction for tests where the uncertainties were minimized combine to provide a high degree of confidence in the ability to calculate the containment pressure transient following a loss-of-coolant accident in a large pressure suppression containment.

Design Parameters

Design Parameters Influencing Pressure Transient. As stated in the second section, there are a number of design parameters which do not affect the condensation process but do affect the containment pressure transient. The more important of these are: (a) Vent flow area, (b) suppression chamber free air volume, (c) vent flow resistance. In the past, the practice has been to establish these design parameters in strict adherence to the tested Bodega Bay geometry. With increased confidence in our ability to calculate the containment pressure transient, this approach is no longer warranted, and an increasing variety of pressure suppression containment designs has evolved including the over-under concept utilizing a steel lined concrete drywell vessel.

With increased flexibility in containment sizing, a means of predicting the effect of various design changes on the peak drywell pressure is needed. In response to this need, a graphical method of predicting the peak drywell pressure has been developed. The necessary graphs are provided in Figs. 8 through 21 for two values of vent resistance. Before explaining their use, some explanations of the trends are in order.

Increasing the suppression chamber free air volume decreases

Fig. 8 Variation of peak drywell pressure with break area and vent area; vent resistance = 6.2

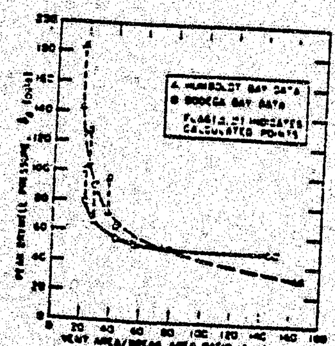

Fig. 7 Comparison of analytical model and test data

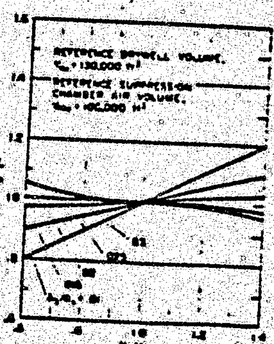

Fig. 9 Variation of peak drywell pressure with drywell free volume. Reactor vessel inside diameter ~183 in., break area = 2.6 sq ft, vent resistance = 6.2

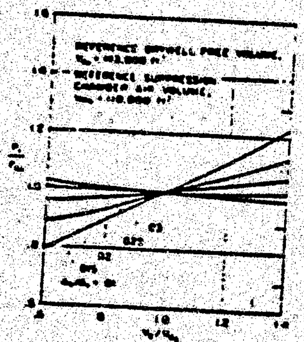

Fig. 10 Variation of peak drywell pressure with drywell free volume. Reactor vessel inside diameter ~218 in., break area = 4.3 sq ft, vent resistance = 6.2.

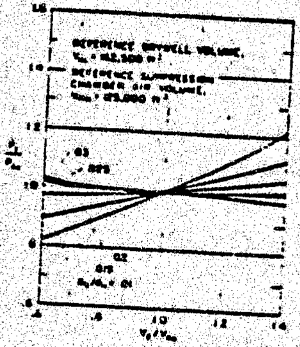

Fig. 11 Variation of peak drywell pressure with drywell free volume. Reactor vessel inside diameter ~251 in., break area = 4.3 sq ft, vent resistance = 6.2.

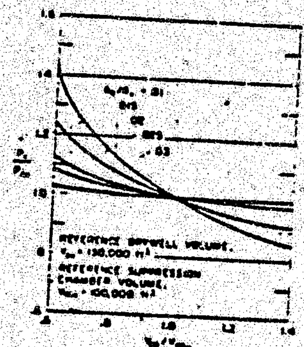

Fig. 12 Variation of peak drywell pressure with volume of air in suppression chamber. Reactor vessel inside diameter ~183 in., break area = 2.6 sq ft, vent resistance = 6.2.

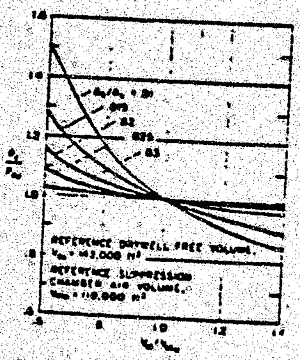

Fig. 13 Variation of peak drywell pressure with volume of air in suppression chamber. Reactor vessel inside diameter ~218 in., break area = 4.3 sq ft, vent resistance = 6.2.

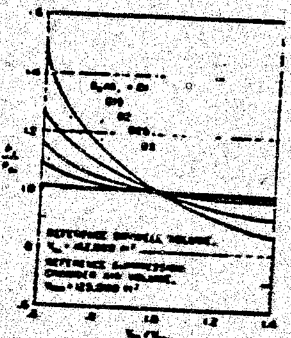

Fig. 14 Variation of peak drywell pressure with volume of air in suppression chamber. Reactor vessel inside diameter ~251 in., break area = 4.3 sq ft, vent resistance = 6.2.

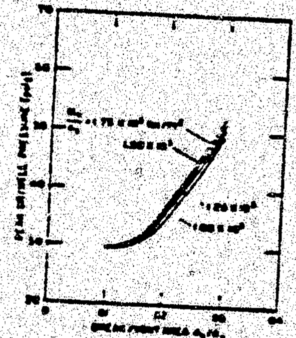

Fig. 15 Variation of peak drywell pressure with break area and vent area; vent resistance = 1.9.

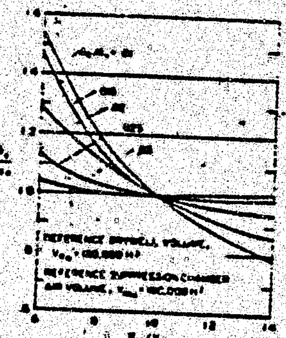

Fig. 16 Variation of peak drywell pressure with volume of air in suppression chamber. Reactor vessel inside diameter ~183 in., break area = 2.6 sq ft, vent resistance = 1.9.

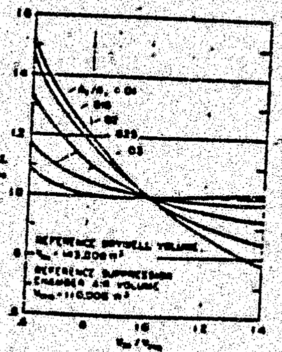

Fig. 17 Variation of peak drywell pressure with volume of air in suppression chamber. Reactor vessel inside diameter ~218 in., break area = 4.3 sq ft, vent resistance = 1.9.

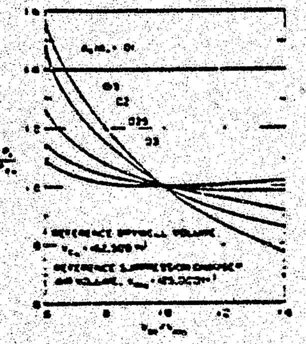

Fig. 18 Variation of peak drywell pressure with volume of air in suppression chamber. Reactor vessel inside diameter ~251 in., break area = 4.3 sq ft, vent resistance = 1.9.

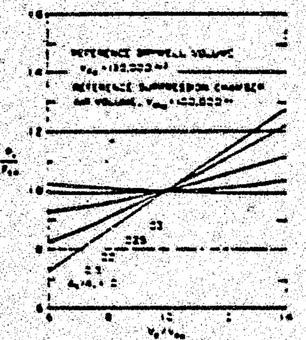

Fig. 19 Variation of peak drywell pressure with drywell free volume. Reactor vessel inside diameter ~183 in., break area = 2.6 sq ft, vent resistance = 1.9.

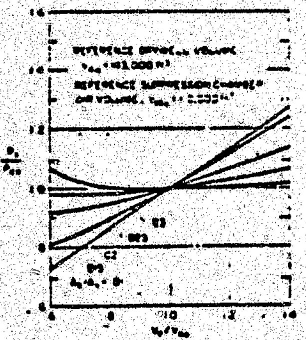

Fig. 20 Variation of peak drywell pressure with drywell free volume. Reactor vessel inside diameter ~218 in., break area = 4.3 sq ft, vent resistance = 1.9.

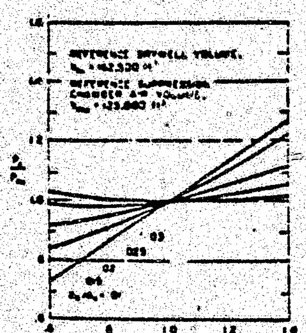

Fig. 21 Variation of peak drywell pressure with drywell free volume. Reactor vessel inside diameter ~251 in., break area = 4.3 sq ft, vent resistance = 1.9.

the end-of-reactor-blowdown suppression chamber pressure due to simple ideal gas law considerations. The reduced suppression chamber pressure is reflected in reduced drywell pressure, assuming the flow in the vents is not choked. Choked flow in the vents masks the effect of the suppression chamber back pressure and accounts for the insensitivity of drywell pressure to suppression chamber air volume at larger air volumes as indicated in Figs. 12 to 14 and 19 to 21.

The drywell free volume is normally established based on piping and equipment layout considerations with the goal being to make the drywell as small as possible. However, the drywell volume does influence the containment pressure transient and the effect is illustrated in the graphs. As can be seen, increasing the drywell volume at constant suppression chamber air volume normally increases the peak drywell pressure. This increase is due to the increased suppression chamber pressure which is caused by the increased quantity of noncondensible gases from the drywell which must be stored in the suppression chamber following the accident. However, if the suppression chamber is relatively large compared to the drywell or the vents relatively small so that the vent flow chokes, the increase suppression chamber pressure does not affect the drywell pressure. In this case, increasing the drywell volume decreases the drywell pressure by providing additional free volume in which to store the released energy[6], similar to dry containment designs. In summary, increasing the drywell volume increases the drywell pressure if the flow in the vents is not choked. If the flow is choked, increasing the drywell volume decreases the drywell pressure.

A limitation in applicability arises with containments having a very large ratio of drywell air volume to suppression chamber air volume. In this case, the peak drywell pressure occurs at the end of reactor vessel blowdown, approximately 30 seconds after the accident, rather than during the first ten seconds. (See Fig. 22.) The presented graphs will normally underpredict the peak drywell pressure for these cases. To cover these situations, a peak drywell pressure should be calculated from Fig. 23 for

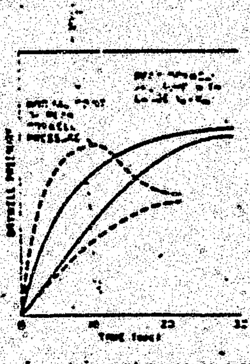

Fig. 22 Comparison of containment pressure response for large drywells with typical response

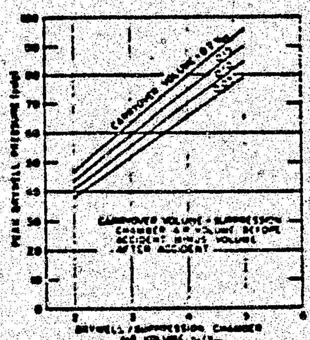

Fig. 23 Minimum peak drywell pressure, large drywell volumes

cases where $\frac{V_d}{V_{as}} > 2.0$. If the results from Fig. 23 are higher than those obtained in the normal manner, the results from Fig. 23 should be used. Since Fig. 23 is merely the end of blowdown drywell pressure it can also be used to establish this point in the transient for those cases where this pressure is not the maximum. Fig. 23 is applicable to any break/vent area ratio and vent resistance.

Design Parameters Influencing the Condensation Process. Achieving complete condensation of the steam vented to the suppression pool is fundamental to the pressure suppression concept. The analytical model described above is predicated on complete and rapid condensation. Unfortunately, the condensation process in the suppression chamber is not amenable to analysis, and in order to assure a satisfactory design, strict adherence to tested geometries is maintained. This approach is believed to be highly conservative in that there is no reason to believe condensation would not be complete over a wide range of design configurations. In fact, it is the author's opinion, based on the study of considerable data references [1, 2, 5, 6, 7] on the condensation process, that it would be very difficult to prevent condensation of a high velocity steam jet injected into a relatively small closed compartment, half filled with cold water. Nonetheless the conservative approach is recommended until a satisfactory condensation model is developed.

In order to insure applicability of the Moss Landing tests with respect to condensation, the following key design parameters of the tests should be duplicated:

1 The downcomer diameter should be no greater than 24 inches.

2 The downcomer exit should discharge downward and be submerged in the pool water by at least three feet.

3 The clearance between the downcomer exit and the pool bottom should be no less than six feet nor more than twelve feet. The twelve foot limit should be approached with caution giving due consideration to pool mixing for the case of a small break in the reactor system.

4 The downcomer spacing should not be less than 3.6 feet from center-line to center-line.

6 The ratio of design break area to vent total flow area shall not exceed 0.05.

Note there is no limit on the maximum downcomer submergence. The graphs presented are based on four feet of submergence, but are applicable up to at least six feet of submergence. However, this limitation is not a technological limit, but only a limitation in the applicability of the graphs. Greater downcomer submergence is acceptable, and the effect on performance can be predicted by the analytical model, but this parameter was not included in the graphs.

All of the above limits are based directly on the Humboldt and Bodega test results and are therefore proven values. The bases for establishing these limits are as follows:

1 The downcomers tested in the Bodega tests were nominally 24 inches in diameter. Certainly smaller downcomers would work as well; however, if the downcomers are made much larger it is possible to postulate local heating of the pool up to the saturation temperature which could result in delayed condensation.

2 Data from both the Humboldt and Bodega series of tests prove complete condensation can be achieved even with no downcomer submergence for the relatively large breaks. Therefore, the minimum submergence criterion is based more on insuring condensation in the event of a relatively small break when steam jet penetration into the pool would be reduced. The three feet of submergence was shown to completely condense the stream even with very low steam flow rates.

3 The minimum value in the range of bottom clearance is specified because of concern that the steam-air jet may be reflected off of the pool bottom and break through the water surface before complete condensation is achieved. Visual observation of pure steam jets being quenched in a pool of water indicate the jets condense quite rapidly upon leaving the downcomer so that a bottom clearance of two feet or less should be adequate. However, the incoming jet is initially largely air which hinders condensation and could result in increased jet penetration. Because of the uncertainty introduced by the steam-air mixture, it is prudent to retain the tested configurations. The Bodega tests were run with six feet of bottom clearance.

A maximum bottom clearance is specified in order to insure efficient utilization of all of the water. The 12 feet of clearance cited was tested in the Humboldt series and a uniform water temperature profile was observed at the end of blowdown.

4 The minimum downcomer spacing is specified based on the Bodega tests in order to prevent local overheating of the pool water.

5 A maximum pool temperature limit is specified in order to insure the availability of a satisfactory energy sink for the condensation of the steam. The 170 F limit represents a conservative extrapolation from the 163 F maximum temperature tested in the Bodega series of tests. Complete condensation would be expected to occur up to the saturation temperature which normally is about 260 F in this case.

6 A maximum break to vent area ratio is specified primarily to limit downcomer exit velocities to within tested values. A larger design break to vent area ratio would result in more violent pool action during the blowdown, and prudence dictates a conservative approach.

Many of the above limits may appear to be arbitrary and conservative. However, one must remain cognizant of the fact that this system will probably never be used throughout its lifetime, but were it called upon it must perform properly the first time. In the interest of public safety it is therefore imperative that every reasonable precaution be taken in order to insure proper functioning of the containment system.

Use of the Graphs. The use of the graphs presented in Figs. 8 through 21 can best be illustrated by means of an example. Assume the transient peak drywell pressure is desired for a containment housing a reactor primary system which contains approximately 3.2×10^6 lb of water (excluding feedwater) and has a design break area of 2.6 sq ft. Assume a containment design having the following parameters:

Vent area, $A_v = 100$ ft^2
Suppression chamber air volume, $V_{as} = 130{,}000$ ft^3
Drywell volume, $V_d = 180{,}000$ ft^3
Vent flow resistance, $fL/D = 6.2$

First we calculate the ratio

$$\frac{M_{fr}}{A_b} = \frac{3.2 \times 10^6}{2.6} = 1.23 \times 10^6 \text{ lb/ft}^2$$

And then the break to vent area ratio

$$\frac{A_b}{A_v} = \frac{2.6}{100} = 0.026$$

Next we calculate the following ratios using values of V_{asg} and V_{dg} obtained from Figs. 12 and 9:

$$\frac{V_{as}}{V_{asg}} = \frac{130{,}000}{100{,}000} = 1.3$$

$$\frac{V_d}{V_{dg}} = \frac{180{,}000}{130{,}000} = 1.385$$

Now from Fig. 8, a base peak drywell pressure of 59.4 psig is obtained. From Fig. 12, a suppression chamber air volume correction factor of 1.0 is read, and from Fig. 9, a drywell volume correction factor of 0.984 is obtained. Therefore, the final peak drywell pressure is:

$$P_D = 59.4\ (1.0)\ (0.984) = 58.4 \text{ psig}$$

Now we shall consider the same plant except we will decrease the suppression chamber air volume to 80,000 ft³. Then

$$\frac{V_{sc}}{V_{scR}} = \frac{80{,}000}{100{,}000} = 0.8$$

Now from Fig. 12, we find a correction factor of 1.025. Therefore,

$$P_D = 59.4\ (1.025)\ (0.984) = 59.9 \text{ psig}$$

Note that we have decreased the suppression chamber air volume by 20,000 ft³ and have only increased the peak drywell pressure by 1.5 psi.

The accuracy of the graphs is within the error band of the computer model itself over the range of system parameters of normal interest. In some extreme cases, such as a simultaneous 40 percent increase or decrease in drywell volume, suppression chamber air volume, and vent flow area, the graphs may overpredict the peak drywell pressure by as much as 7 psi. However, the graphs never underpredict the peak drywell pressure by a significant amount, and it is recommended that the designer treat the results obtained from the graphs in the same manner he would treat the results from a transient analysis directly applicable to the design being considered. Since the analytical model is inherently conservative, a large design margin is not warranted. A margin of approximately 15 percent is recommended, but this decision is up to the designer.

Summary

Sufficient information has been presented to permit the formulation of a pressure suppression containment design. The design approach has been shown to be both analytical and empirical. The analytical models employed were compared with existing test data and were shown to accurately predict the containment pressure transient following a loss-of-coolant accident. These models can now be used to predict the effect on containment performance of system volumes, break size, and vent flow area.

Design limits based on rigid adherence to the test data were established for insuring condensation in the suppression pool. These design limits generally have little influence on the containment pressure response so long as complete condensation is achieved. The design criteria presented in this report provides the containment designer with the flexibility to develop a wide spectrum of containment designs without the need for additional testing.

Acknowledgments

The author would like to acknowledge the many individuals who contributed to the development of this paper. Particular credit is due to F. J. Moody who, besides developing the two-phase critical flow model, also developed the equations for the original containment transient model from which the current model was developed with only limited modifications. D. C. Maxwell performed the digital simulation of Moody's original model. C. H. Robbins contributed valuable information concerning the details of the Moss Landing testing program, and P. W. Ianni gave invaluable technical guidance.

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COMPANY, DOCKET NO. R-850152

PACIFIC GAS AND ELECTRIC COMPANY

FINAL HAZARDS SUMMARY REPORT
HUMBOLDT BAY POWER PLANT
UNIT NO. 3
APPENDICES IV & V

March 13, 1986

PACIFIC GAS AND ELECTRIC COMPANY

FINAL HAZARDS SUMMARY REPORT

HUMBOLDT BAY POWER PLANT

UNIT NO. 3

Appendix IV

PRESSURE SUPPRESSION DEVELOPMENT PROGRAM

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PRESSURE SUPPRESSION DEVELOPMENT PROGRAM

A. INTRODUCTION

The initial design of this Unit contemplated using a large volume steel capsule for the reactor containment structure. While this dry type of containment would have been adequate, there was considerable incentive to investigate other methods.

A containment design which would reduce the problems of direct radiation and leakage from the enclosure to a low order was highly desirable from the standpoints of providing maximum safety, eliminating possible interference with the existing units, and reducing costs. One attractive possibility was the pressure suppression concept of containment.

An agreement was made with the General Electric Company, Atomic Power Equipment Department, to study the feasibility of pressure suppression containment, to conduct tests to obtain design information, and to perform analytical work to obtain the necessary design parameters and to demonstrate the adequacy of the design.

The initial testing and analytical work was performed in a three part program during 1958 and 1959. The results demonstrated the basic feasibility of pressure suppression containment and provided data required for design.

After receiving Mr. H. L. Price's letter of March 23, 1960 with an attached copy of the March 14, 1960 letter from the Advisory Committee on Reactor Safeguards, the Company designed and constructed a new facility at Moss Landing and conducted full scale tests of a 1/48th segment of the Humboldt Bay pressure suppression system. These new Moss Landing tests confirmed with considerable margin the Humboldt design.

B. INITIAL TEST PROGRAM

1. Introduction

The initial test program was divided into three phases, starting in July, 1958 and continuing through June of 1959.

Phase I consisted of preliminary analytical work and some simple tests, primarily to obtain design information for construction of a large scale test facility.

Phase II consisted of a series of tests of about three months duration using two test facilities. One facility was constructed at the Company's Moss Landing Power Plant and was used to obtain information on the performance of various arrangements of injecting steam into water. Steam at flow rates up to 100,000 lbs/hr was injected into a tank containing up to 50,000 gallons of water. Effects of variable jet configuation, size, direction, and submergence were observed, as were effects of variable pool size, shape, and temperature. Hundreds of runs were made to observe the performance of different combinations of the above variables. The information obtained relates primarily to steady flow performance.

To obtain data on transient phenomena and fission product retention in the water pool, a model of the reactor and pressure suppression system was constructed by General Electric in its San Jose shops. Twenty-two runs were made on this model simulating primary system rupture accidents. In some of these tests, tracers simulating various fission products were placed within the model reactor vessel.

Phase III of the test program consisted of analyzing the information obtained in Phase II and determining the parameters and values needed for designing a reactor containment utilizing pressure suppression. Analysis of hazards and necessary safeguard features for pressure suppression was also part of Phase III. The initial test program was essentially completed by the end of June, 1959.

2. Test Facilities

Two test facilities were constructed to obtain the experimental information needed for pressure suppression analysis and design.

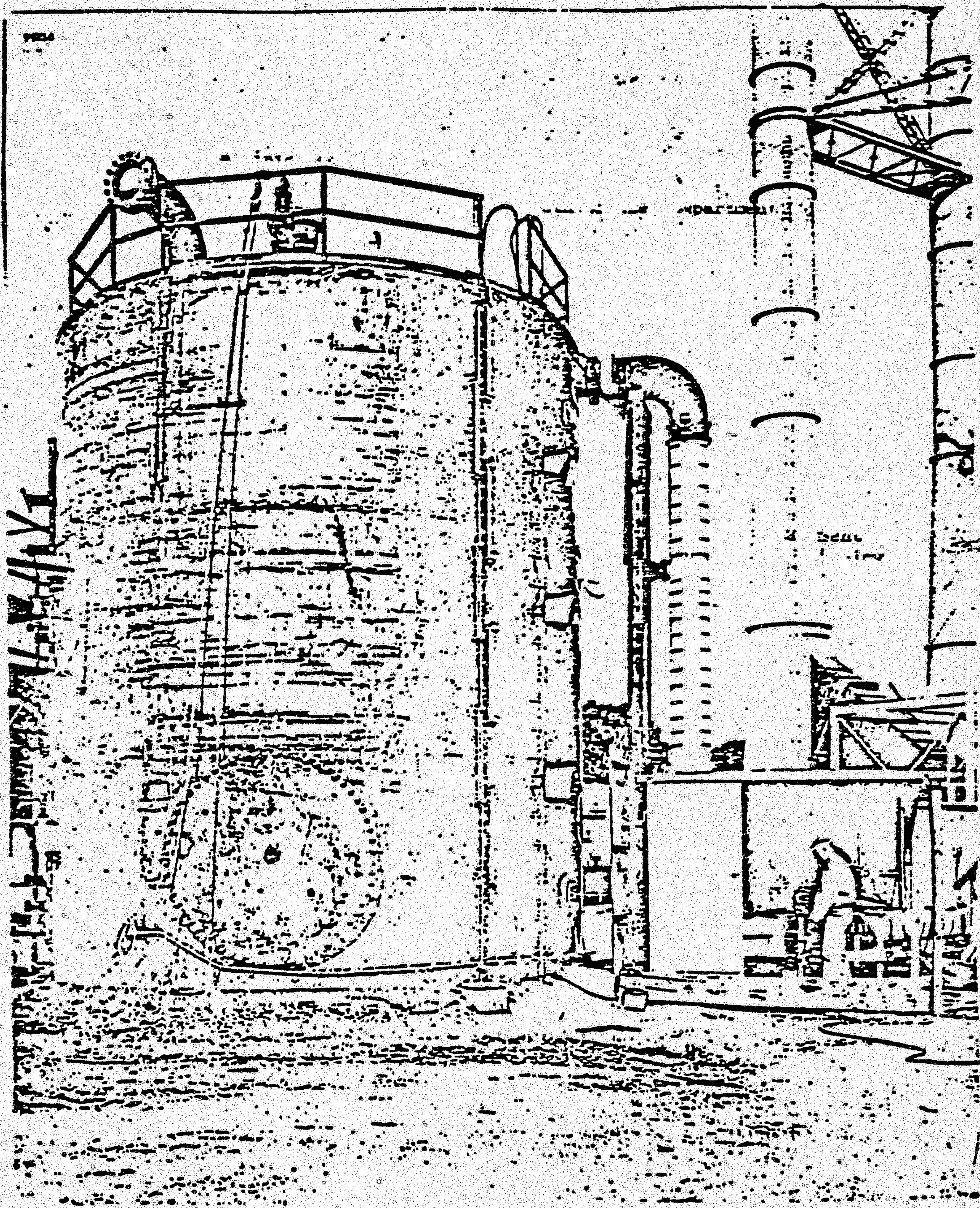
(1) The large scale condensing test facility at the Moss Landing Power Plant is shown on Figures 1 and 2. The tank is 20 feet in diameter and 24 feet high, plus heads, and is designed to withstand an internal pressure of 10 psig. The tank holds up to 50,000 gallons of water. Steam was obtained from the plant's auxiliary steam system at 100 psig saturated and was admitted to the tank at flow rates up to 100,000 lbs/hr through 14 inch inlets near the top and bottom.

The tank is equipped with glass portholes for visual observation. Injection nozzles consist of lengths of pipe with diameters of 4, 6, 8, and 14 inches. Steam can be admitted from either the top or the bottom of the tank and the nozzles can be directed either horizontally, or vertically downward.

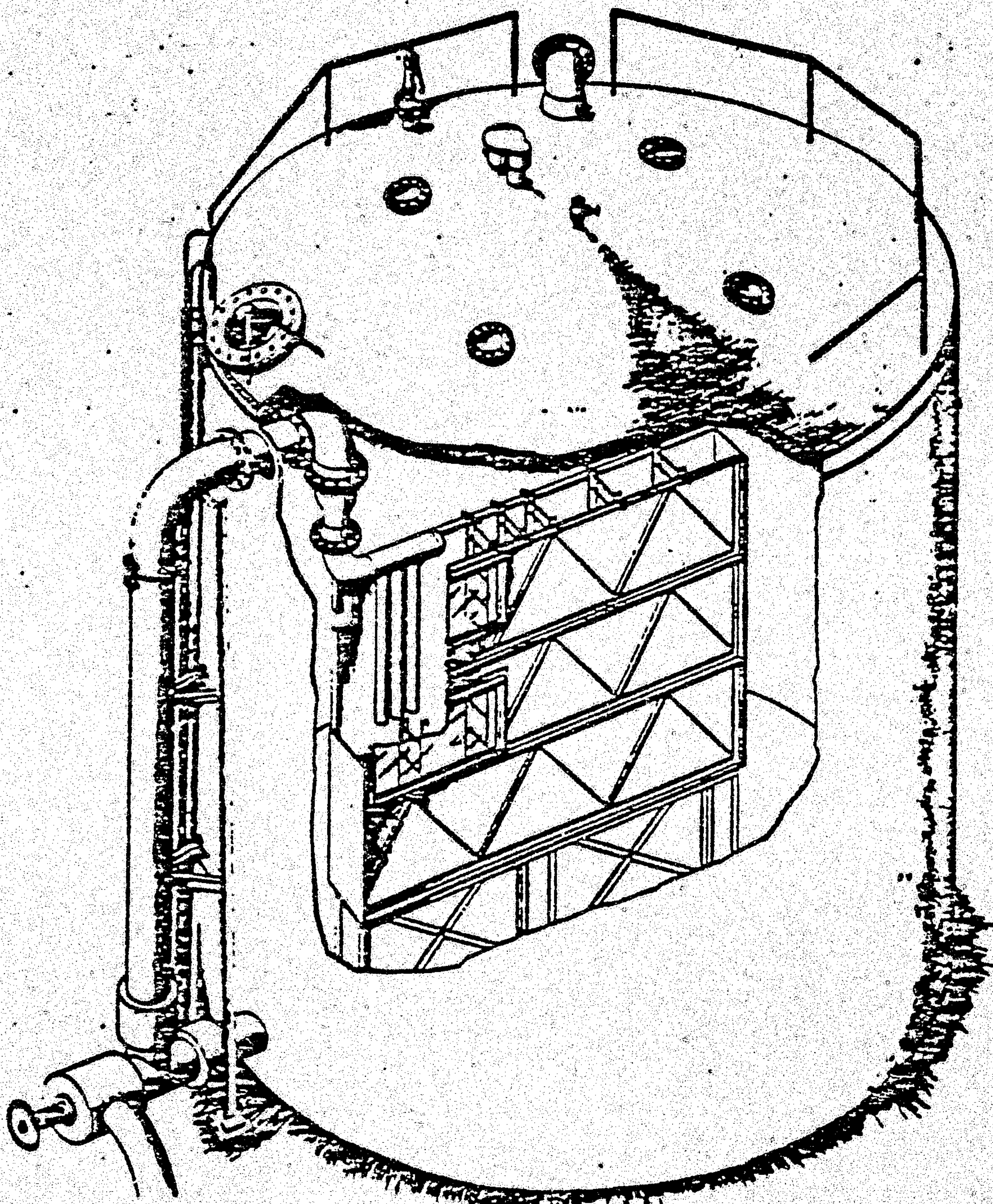
In a number of tests it was desired to confine the volume of water exposed to the steam to an amount equivalent to that exposed to the steam from a single jet in a full scale pressure suppression design utilizing many jets. For this, a rectangular compartment was constructed inside the tank 12 feet long by 12 feet deep by 18 inches wide. The compartment was designed so that either the depth, length, or width could be decreased to provide different volumes and shapes. The compartment has glass windows in it for visual observation.

The facility was instrumented to show conditions in the steam, in the water, and in the air space at the top of the tank.

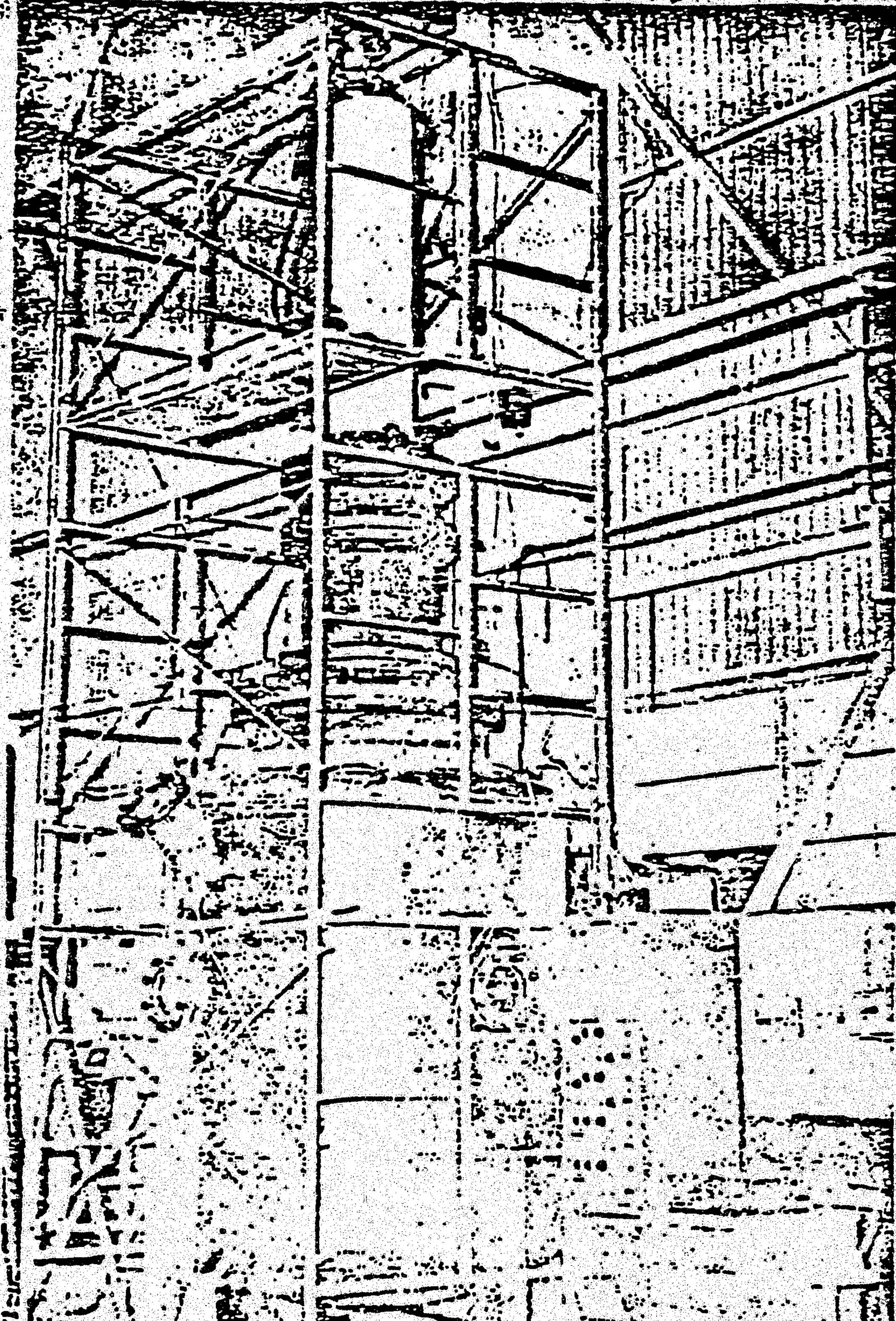
(2) The transient model test facility is shown on Figures 3 and 4. This facility consisted of a length of pipe which is the model reactor vessel, and a large cylindrical steel vessel which is a model of the pressure suppression chamber. Water was heated electrically in the model vessel to a pressure of about 1000 psi and the burst diaphragm at the bottom of the model vessel was broken to simulate primary system ruptures. The water and steam discharged through an orifice into the cone shaped volume below the model vessel which simulates the dry well. The dry well vented into the pressure suppression pool by



PHOTOGRAPH OF
CONDENSING TEST FACILITY

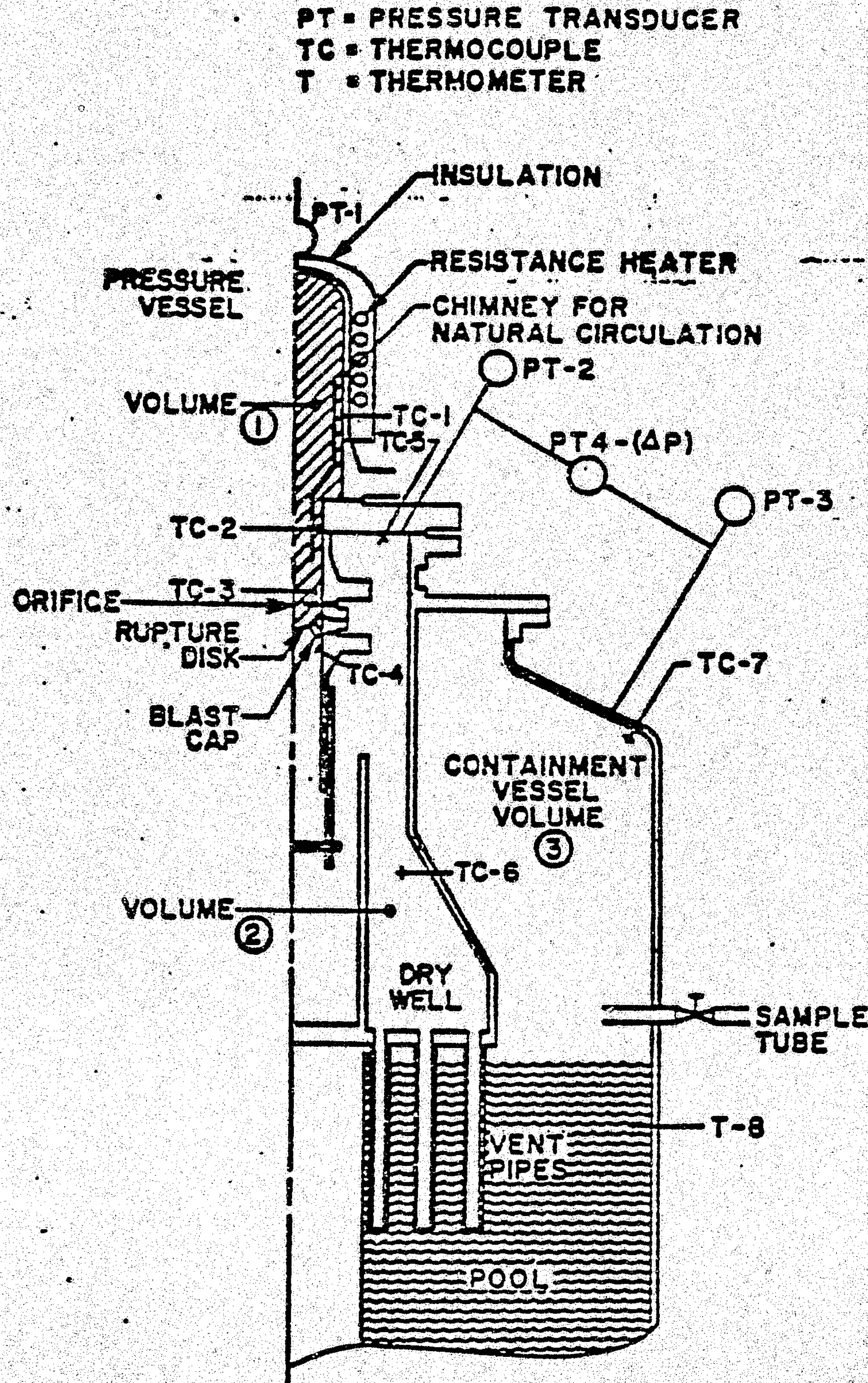


SKETCH OF CONDENSING
TEST FACILITY



PHOTOGRAPH OF TRANSIENT
TEST FACILITY

APPENDIX "V", FIGURE 3



SKETCH OF TRANSIENT TEST FACILITY

APPENDIX IV, FIGURE 4

up to 150 pipes. Variables in this facility were primary system break area, dry well volume, number of vent pipes, total vent area, and the depth of submergence of the vents in the pressure suppression pool. The test parameters were scaled down from the Humboldt Bay design by a factor of 10 for length, 100 for area, and 1000 for volume. The time scale in the model is one tenth that of the Unit.

The facility was equipped with instrumentation to obtain data on pressures and temperatures in dry well, the pool, and the air space above the pool when the diaphragm was ruptured.

In some tests, substances were added to the water in the model vessel to simulate fission products, and after the tests, samples were taken from the air space above the water pool for analysis to determine the ability of the pool to retain fission products.

3. Test Results

a. Condensing Tests Without Compartment in Tank

The first series of tests with the Condensing Test Facility were without the box compartment in the tank.

The following range of parameters was tested:

Vents:	4, 6, 8, and 14 inch diameter single straight pipes, and a triple 4 inch diameter vent
Depth of submergence:	1 inch to 6 feet
Direction of discharge	Vertically downward and horizontal
Steam flow:	10,000 to 93,000 lbs/hr
Tank water temperature	50°F to 150°F

Steam was completely condensed in all but three out of forty tests in this series. The exceptions were: 6 inch diameter pipe discharging 60,000 lbs/hr horizontally with 6 inch nominal depth of submergence; 8 inch diameter pipe discharging 83,000 lbs/hr horizontally with 6 inch nominal depth of submergence; and 8 inch diameter pipe discharging 77,000 lbs/hr downward vertically with 1 inch depth of submergence.

At high steam flow rates, the water in the pool appeared to be mixing well, the surface was agitated, and vortices formed temporarily around the pipe.

b. Tank Vibration Tests

Test results showed that tank vibration began when the water was 120-130°F or hotter. It was most severe at high steam flows.

c. <u>Tests With Compartments in Tank</u>

A third series of tests was conducted to investigate the interaction of vents discharging vertically into small volumes of water. An open top box within the tank, as shown in Figure 2, was used with windows in the sides which permitted viewing the surface and the discharging jet. Tests were of short duration, sometimes as short as 20 seconds, because of the rapid heating of the water in the compartment. Ninety-one tests were conducted to investigate the effect of steam flow rate, vent diameter, and width, length, and depth of the compartment.

The surface of the water was observed to be depressed near the vent pipe because of the momentum of the jet. The depression was observed to be as much as 5 feet in some cases. However, complete steam condensation in the water pool was always obtained with the compartment tests.

Mixing of the water in the compartments was excellent, based on observation of the flow and measurement of the water temperature.

Figure 5 is a photograph of three parallel steam jets discharging downward into the compartment. Numbers on the photograph are inches below the initial water surface.

Figure 6 is a view of the surface of the water in the compartment during steam discharge through pipes with the open end 6 feet below the top of the box. The manifold pipe for the vents is shown on the extreme right of the picture. Only the right side of the compartment is shown. On the left of the photograph is an abrupt change in elevation of the surface similar to a hydraulic jump.

d. <u>Transient Tests for Pressure Response</u>

Parameters varied in the test program included the following:

Initial reactor pressure:	600, 1000 psig
Initial fraction of reactor vessel filled with water:	0.66, 0.80
Orifice diameter:	0.6, 1.2, 1.6 inches
Dry well volume:	17.5, 20.3, 26.0 cu. ft.
Vent area:	0.5, 0.9, 2.1 sq. ft.
Vent depth of submergence	0.75, 1.0, 1.5 ft.

A typical set of pressure traces is shown on Figure 7. The reactor vessel pressure dropped off rapidly in the first .04 seconds, then fell at a slower rate up until about 2.8 seconds, and finally decayed exponentially. The three periods correspond to (1) expulsion of water before flashing occurs, (2) flow of saturated water and steam out of the vessel, and (3) flow of steam after all the water had been expelled from the vessel.

CONDENSING STEAM JETS
37,000 lbs. per hour steam
Compartment: 6" x 12' x 12'

FIGURE 5

WATER SURFACE DURING DISCHARGE
OF SUBMERGED STEAM JETS
52,000 lbs. per hour steam
Compartment: 18" x 8' x 8'

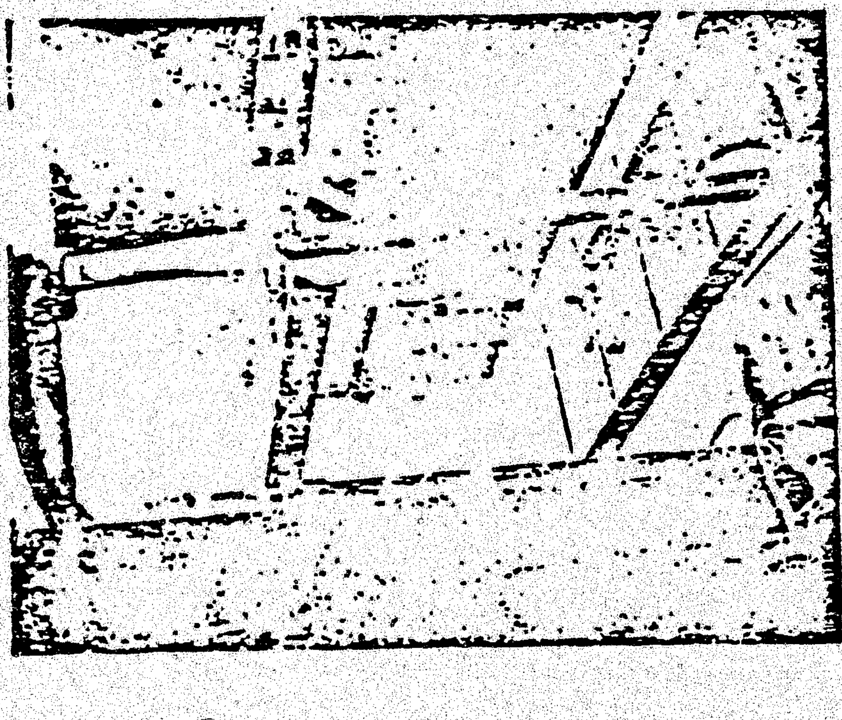
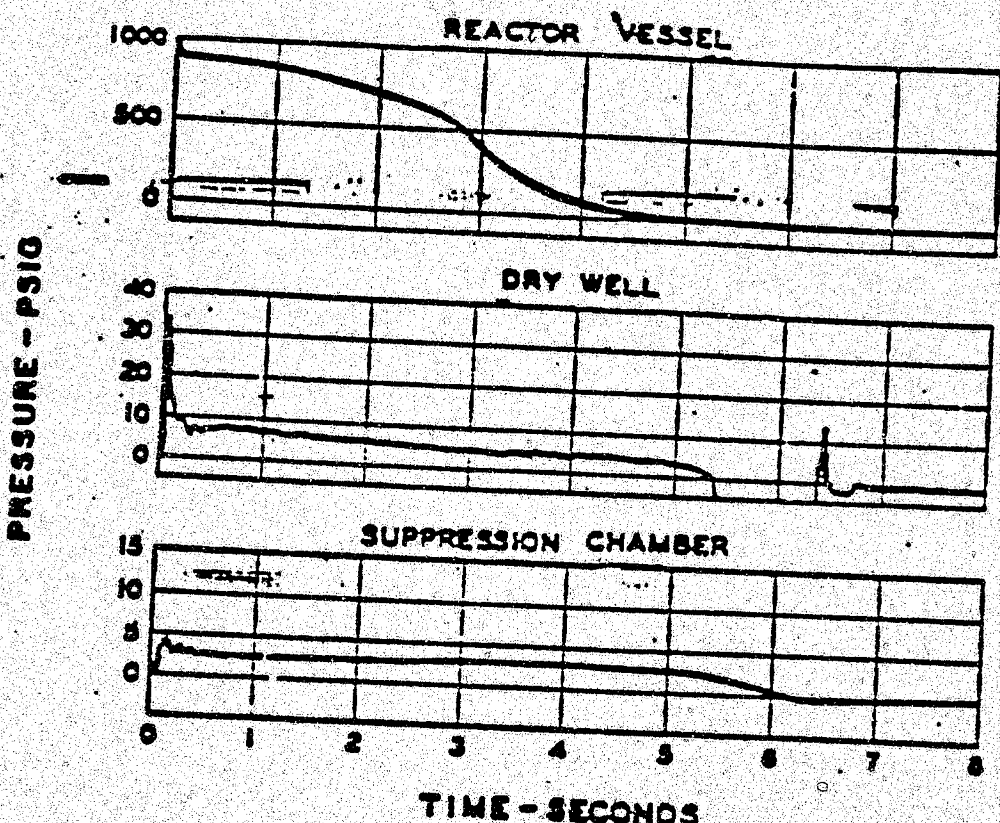


FIGURE 6



	TEST FACILITY PARAMETERS	TEST FACILITY PARAMETERS: SCALED TO HUMBOLDT BAY SIZE:	HUMBOLDT BAY PARAMETERS:
REAK AREA	0.00786 SQ. FT.	0.786 SQ. FT.	0.708 SQ. FT.
Y WELL VOLUME	17.53 CU. FT.	17,530 CU. FT.	12,500 CU. FT.
ENT AREA	0.5 SQ. FT.	50 SQ. FT.	46 SQ. FT.
ME SCALE	AS SHOWN	AS SHOWN X 10	

TYPICAL TRANSIENT
TEST RESULTS

APPENDIX IV, FIGURE 7

The dry well pressure built up rapidly, reaching a peak in about .06 seconds and then fell back rapidly. Within the range of parameters tested, the peak dry well pressure occurred at the instant the pool water was completely pushed out of the vent pipes by expanding air and steam in the dry well. In some tests, water was forced back into the dry well, by the suppression chamber air pressure, a few seconds after firing. The water then condensed the steam remaining in the dry well, creating a vacuum.

Pressure in the air space over the water pool reached a maximum shortly after the dry well peak pressure occurred. In all tests, the pressure over the water pool returned to essentially atmospheric pressure seconds after the firing

e. Effectiveness of Water Pool as a Barrier to Escaping Fission Products

The specific objective of five tests was to determine qualitatively the effectiveness of the water pool as a barrier to fission products which might be discharged into the pool from the dry well. The Transient Test Facility was used with some special equipment for releasing simulated fission products and for measuring the quantity escaping through the pool to the enclosure.

The simulated fission products used in testing were xenon, krypton, sodium iodide, iodine crystals and zinc sulfide with a mean particle size of two microns. The xenon and krypton were placed inside the pressure vessel in glass bottles which broke when the test began. The other simulated fission products were placed directly in the water in the vessel. Air samples were drawn from the space over the water pool after a test and examined for the amount of "fission product". A mass spectrometer was used to detect noble gases, a scrubbing column and examination of the scrubbing agent was used for iodine and sodium iodide, and in the case of fluorescent zinc sulfide, the air was drawn through a filter paper and the particles counted.

Test results for simulated fission product entrainment indicate that the water pool retained a very high proportion of impurities entering from the dry well. The measured separation factor between the dry well and the enclosure was of the order of 10^{-6} for solid particles, and 10^{-5} to 10^{-6} for the halogen and soluble salt. More than half of the noble gases were retained initially, which is considerably more than expected on the basis of solubility.

4. Conclusions

A summary of conclusions from the intial test program follows:

1. Condensation of steam entering water from nozzles consisting of simple open ended pipes is generally very rapid and complete. In the initial test program, all the steam was condensed except with a horizontal jet six inches below and a vertical jet directed downwards and submerged one inch below the water surface. For all other conditions, condensation was complete with water temperatures up to 170°F (the highest obtained).

2. Analytical prediction of the transient facility test results and correlation between analysis and test results indicated that the behavior of a pressure suppression system can be calculated with sufficient accuracy.

3. There is a tendency for a jet to depress the water surface in its vicinity. If air is sucked into the jet because of a depressed water surface, condensation is not particularly affected.

4. Under certain conditions, the condensation of steam becomes noisy and is accompanied by pressure oscillations. These were observed when the water temperature exceeded about 130°F.

5. Air in the injection pipe does not interfere with condensation of the steam under the conditions of the tests.

6. Shortly after the simulated rupture accident, the pressures in the dry well and in the air space above the pool return to essentially atmospheric pressure.

7. If fission products are carried through the vent pipes, the pressure suppression pool would permit release to its air space of only minute fractions of the solids and halogens.

5. Design Parameters Obtained from Testing

A pressure suppression system must be desinged so that the dry well and the pool containment will witstand the forces to which they would be subjected following the maximum credible operating accident. Phase III of the initial development program developed the values of peak pressures and other parameters to be used in design.

a. Dry Well Pressure

The initial tests showed that the maximum dry well pressure could occur under three different conditions depending upon relative magnitudes of various parameters:

(1) If the vent area is very small compared to rupture flow, a peak pressure (corresponding to dry containment) is reached before any appreciable amount of steam can escape through the vents. For this condition maximum dry well pressure depends upon dry well volume alone

(2) If the vent area is large enough that condition (1) does not occur, dry well pressure is a maximum when vent flow just balances rupture flow. For this condition dry well pressure depends on the pressure drop in the vents and dry well volume and vent submergence have no effect.

(3) If the vent area is very large, maximum dry well pressure corresponds to the pressure buildup necessary to force the water out of the vents. For this condition maximum dry well pressure depends on the rate of

pressure buildup in the dry well (a function of rupture flow and dry well volume) and the vent clearing time (a function of depth of submergence).

Methods were developed for predicting the maximum dry well pressure which were conservative compared with initial tests. However, the transient tests had a large vent area and relatively little vent pipe friction resistance, and consequently the calculation methods based on these tests neglected vent friction. When the early layouts were made for Humboldt it was apparent that vent friction would be significant, and modified methods were used to establish the design. Following the description of the full scale tests and their relation to the calculation methods more will be said about the three conditions listed above and to what extent they apply to the Humboldt design. The calculation methods including vent resistance are given in Appendix V.

Figure 7 shows the results of a test on the transient test facility. The particular test shown was on an approximate scale model of Humboldt, except that the model's vent submergence corresponds to 15 feet rather than the Humboldt design of 5 feet and the test vent friction resistance was relatively very small. The maximum dry well pressure of 34 psig occurred during condition (3) above.

The negative pressure in the dry well starting some five seconds after the start of the test was caused by the condensation of the steam in the dry well resulting in a sudden inrush of pool water to the dry well. This, in turn, caused the sharp pressure fluctuations shown. In the Humboldt design, this is prevented by vacuum breakers between the dry well and the suppression chamber.

The suppression chamber volume and amount of water in the pool in the test facility were proportionally larger than in the Humboldt design, resulting in a lower suppression chamber pressure and a lower post-accident final pressure than would be anticipated for Humboldt.

b. Suppression Chamber Pressure

Following a rupture accident, air would be expelled from the dry well through the pressure suppression pool to the suppression chamber air space. The value of the peak pressure in the chamber depends primarily on the volume of air expelled from the dry well and to a lesser extent on the heating of the air and the increased water vapor pressure in the space above the pool. Initial conditions (temperatures and pressures) in the various parts of the containment system are of minor importance but are included in the design calculations. These calculations are presented in Appendix V.

C. FULL SCALE 1/48th SEGMENT CONDENSING TESTS

1. Introduction and Summary

A transient test facility was constructed and operated at the Company's Moss

Landing Power Plant in the spring of 1960, to proof test the Humboldt design. The facility consisted of a full scale 1/48th segment of the Humboldt suppression chamber with a full scale vent pipe, a dry well, reactor vessel, and a rupture disk assembly.

The following summary in addition to information presented later in this appendix shows that the final Humboldt pressure suppression reactor containment design has been confirmed with considerable margin, by the full scale Moss Landing tests.

(1) The highest maximum dry well pressure obtained with the test facility under conditions simulating a maximum credible operating accident was 36 psig.

(2) The maximum suppression chamber pressure obtained at Moss Landing under conditions simulating the maximum credible operating accident was 9.3 psig.

(3) Tests simulating conditions more severe than the maximum credible operating accident were run. With an orifice area twice that simulating the MCOA, the peak dry well pressure was 64 psig and the suppression chamber pressure did not exceed 8 psig.

2. Relation of Humboldt Bay Design to Moss Landing Test Facility

Figure 8 shows the final arrangement of the pressure suppression portion of the Humboldt reactor containment. Its relation to the Moss Landing test facility is explained in the following paragraphs.

a. Reactor Vessel

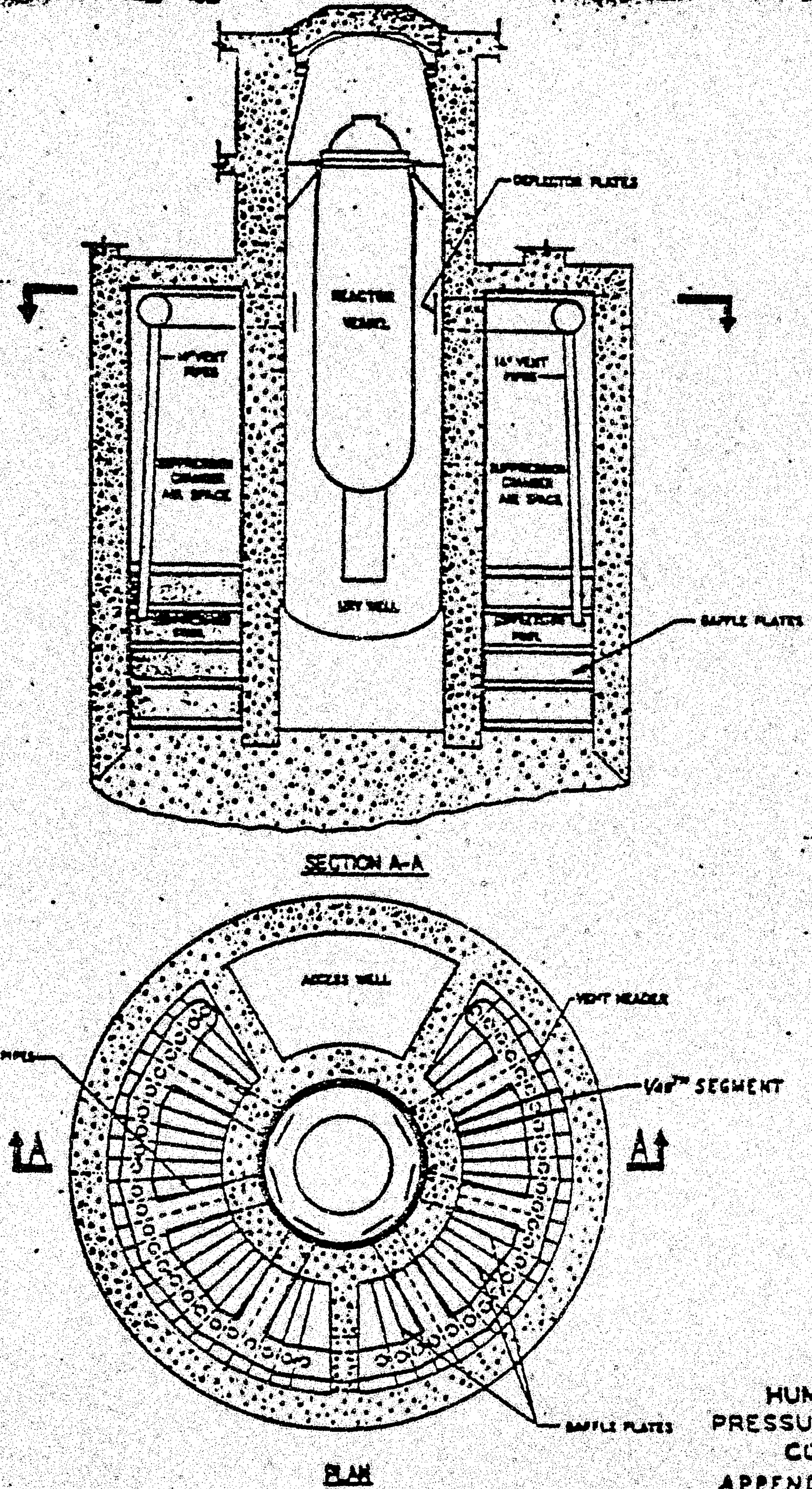
The Humboldt reactor vessel will operate normally at 1020 psig. Safety valves will limit its maximum pressure to approximately 1250 psig. The water volume in the reactor will be 1790 cubic feet and the steam volume will be 860 cubic feet. The steam and water for the Moss Landing tests were pressurized to 1250 psig in a simulated reactor vessel containing 1/48th the volume of the Humboldt vessel, with the same proportion of water and steam, and having approximately the same height.

b. Dry Well

Referring to Figure 8, the Humboldt dry well surrounds the reactor and consists of an ASME code vessel designed for 72 psig, with a volume including vent piping of 12,500 cubic feet. This steel vessel is backed by concrete to resist missiles and jet action. The Moss Landing test dry well volume was approximately 1/48th of Humboldt. This resulted in the correct amount of air being used in the test equipment. The effect of air on steam condensation is thus included in the test results.

c. Suppression Chamber

The suppression chamber surrounds the reactor dry well, except for an



HUMBOLDT BAY
PRESSURE SUPPRESSION
CONTAINMENT
APPENDIX IV, FIGURE 8

access well, and connects to it by means of six 40-inch pipes. A pie shaped segment of the suppression chamber, which is indicated by heavy lines in the section and plan views in Figure 8, contains 1/48th of the suppression chamber pool and air space volume and one of the 14-inch vent pipes. The Moss Landing suppression chamber is a full scale representation of one of the 48 segments making up the Humboldt vent system and suppression chamber. It has the same volume and linear dimensions as the segment shown by the heavy lines in Figure 8.

d. Vent Piping

The discharge from the Moss Landing dry well is through a 14-inch vent pipe, which has a flow resistance calculated to be the same as the combination of 40-inch and 14-inch pipes in the Humboldt design, as well as the same total volume and transport time. Installed in the dry well in front of the opening to the vent pipe is a deflector plate similar in proportion to those for Humboldt Bay. The vent pipe leads into the suppression chamber and extends 6 feet below the surface of the pool as in the Humboldt design.

3. Description of Moss Landing Test Facility

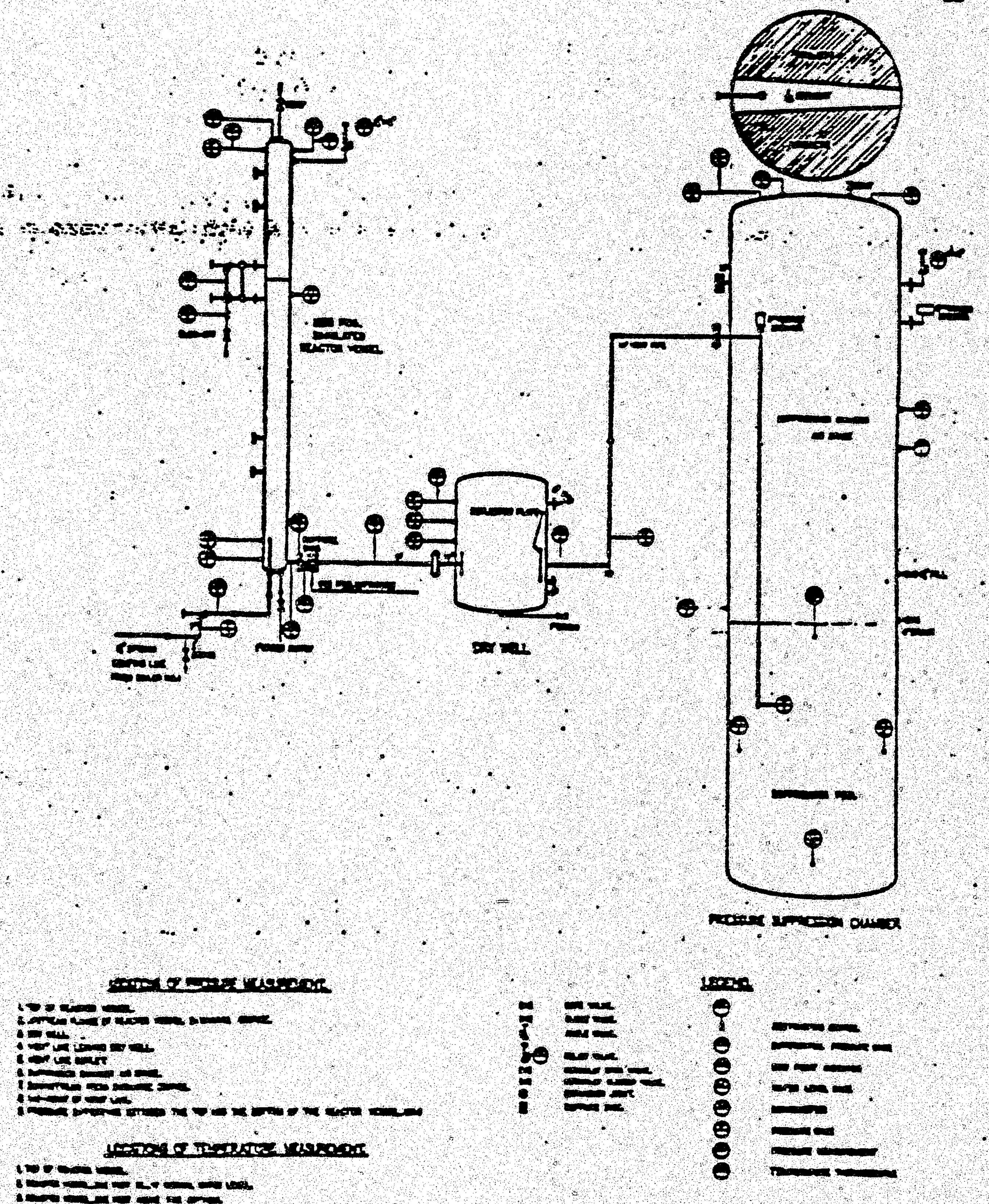
A schematic diagram of the test facility is shown in Figure 9. The major components are: (1) a simulated reactor vessel, (2) rupture discs and orifice, (3) a dry well, (4) a 14-inch vent pipe, (5) a pressure suppression chamber, and (6) instrumentation. An arrangement drawing of the facility is shown in Figure 10. A photograph of the facility is shown in Figure 11.

a. Reactor Vessel

The simulated reactor vessel is a vertical 32 foot length of 20-inch OD schedule 80 pipe. Its volume is 55.8 cubic feet, which is 1/48th of the steam and water volume of the Humboldt reactor vessel. The test vessel is insulated to prevent heat loss and is heated by means of 1400 psig saturated steam injected at the bottom. Water level gage glasses and pressure and temperature gages permit determining the desired conditionsat the time of testing.

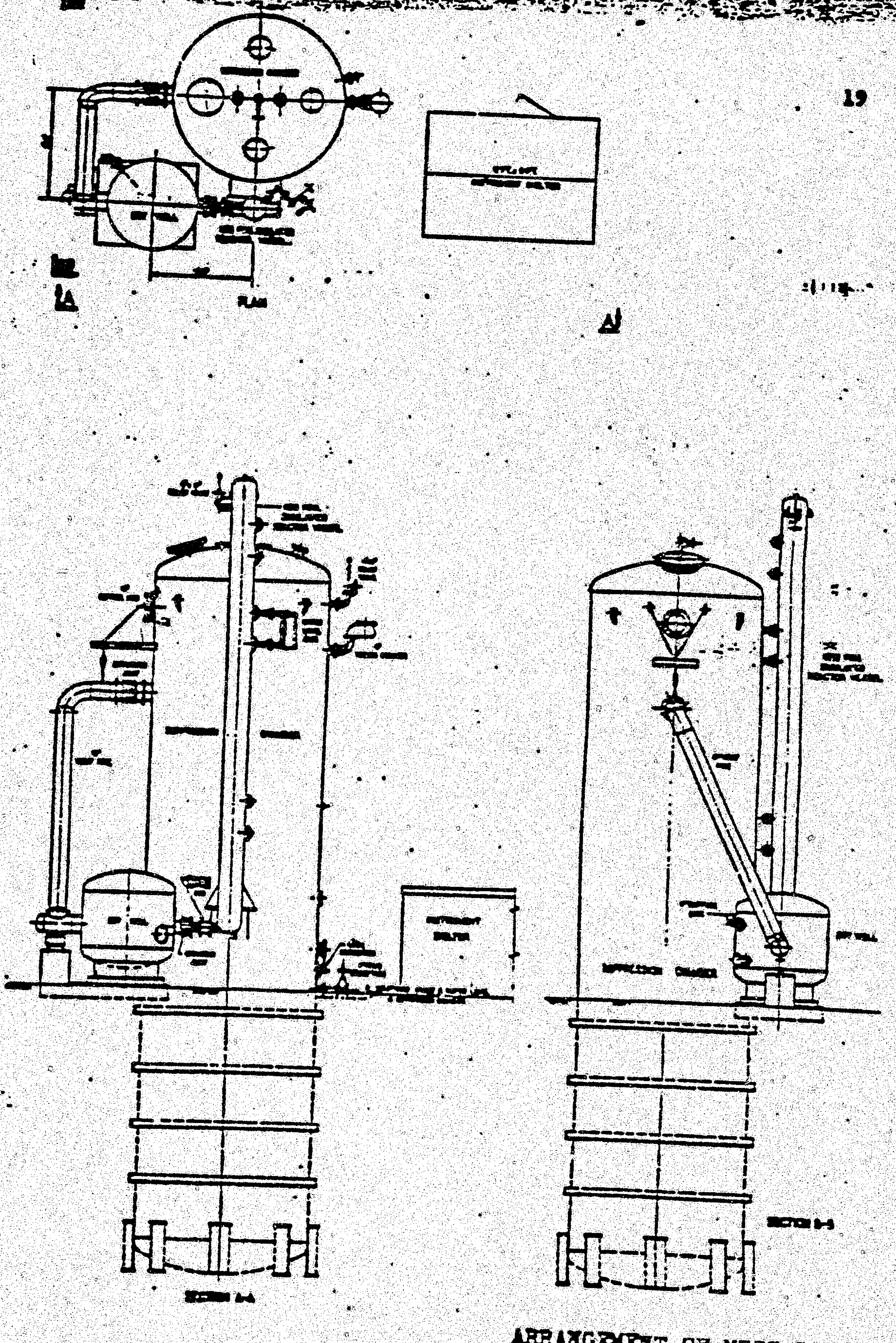
b. Rupture Discs and Orifice

The transient test is initiated by means of rupture discs in the line leading to the dry well The rupture discs are 6-inches in diameter and arranged two in series. They are designed to burst at 900 psig. Nitrogen pressure is applied between the discs, so that the differential pressure acting on each, with 1250 psig in the simulated reactor vessel, is about 600 psi. Releasing the nitrogen pressure subjects the upstream disc to 1250 psig, causing it to burst immediately; then the downstream disc will burst, with both events occurring nearly simultaneously. A sharp edged orifice 1.64 inches in diameter upstream from the rupture discs equals 1/48th of the area of a 12-inch pipe, which in the Humboldt reactor has been taken to be the size of rupture for the maximum credible accident. A Photograph of typical rupture discs is shown in Figure 12.

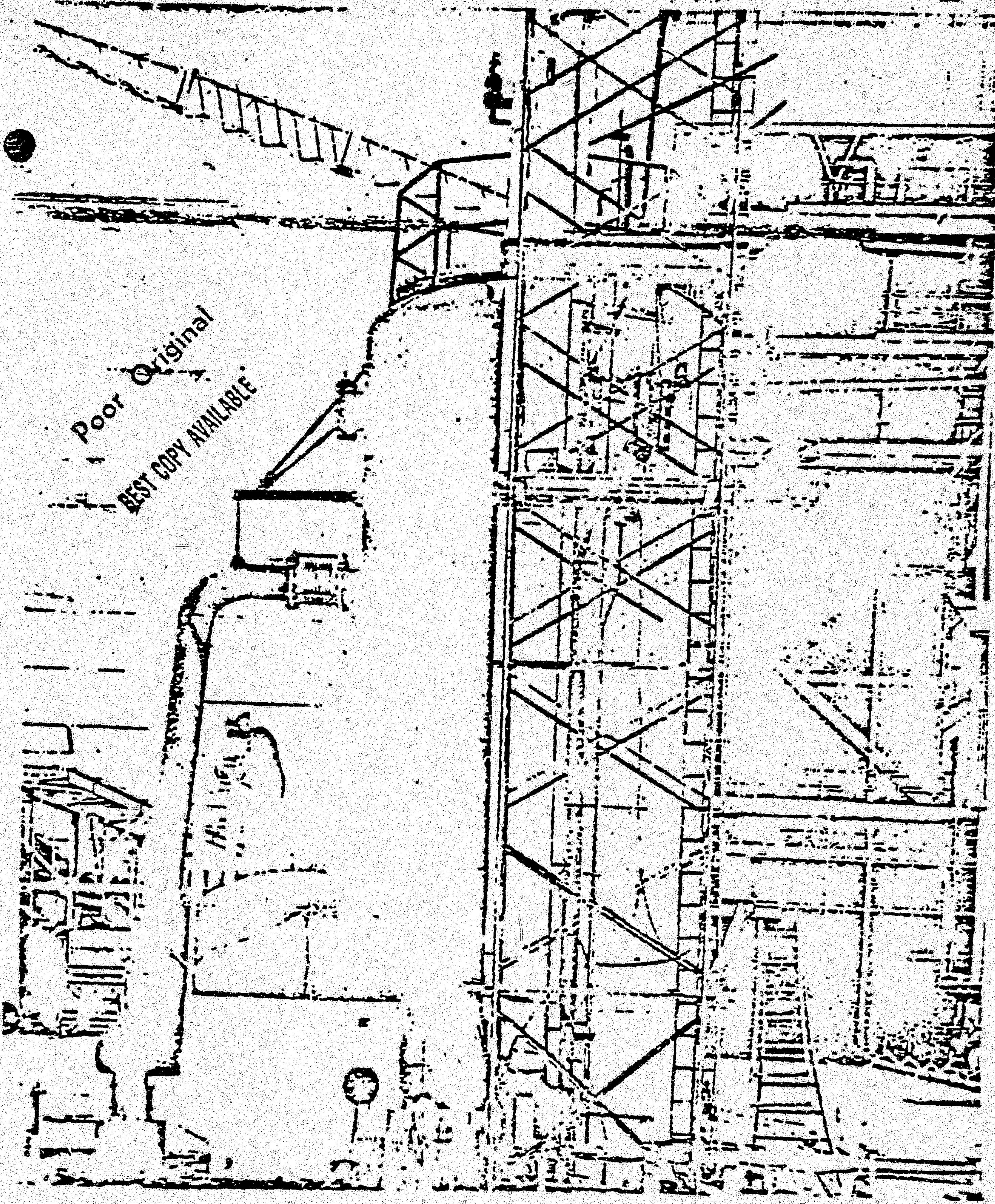


SCHEMATIC DIAGRAM OF MOSS LANDING PRESSURE SUPPRESSION TEST FACILITY

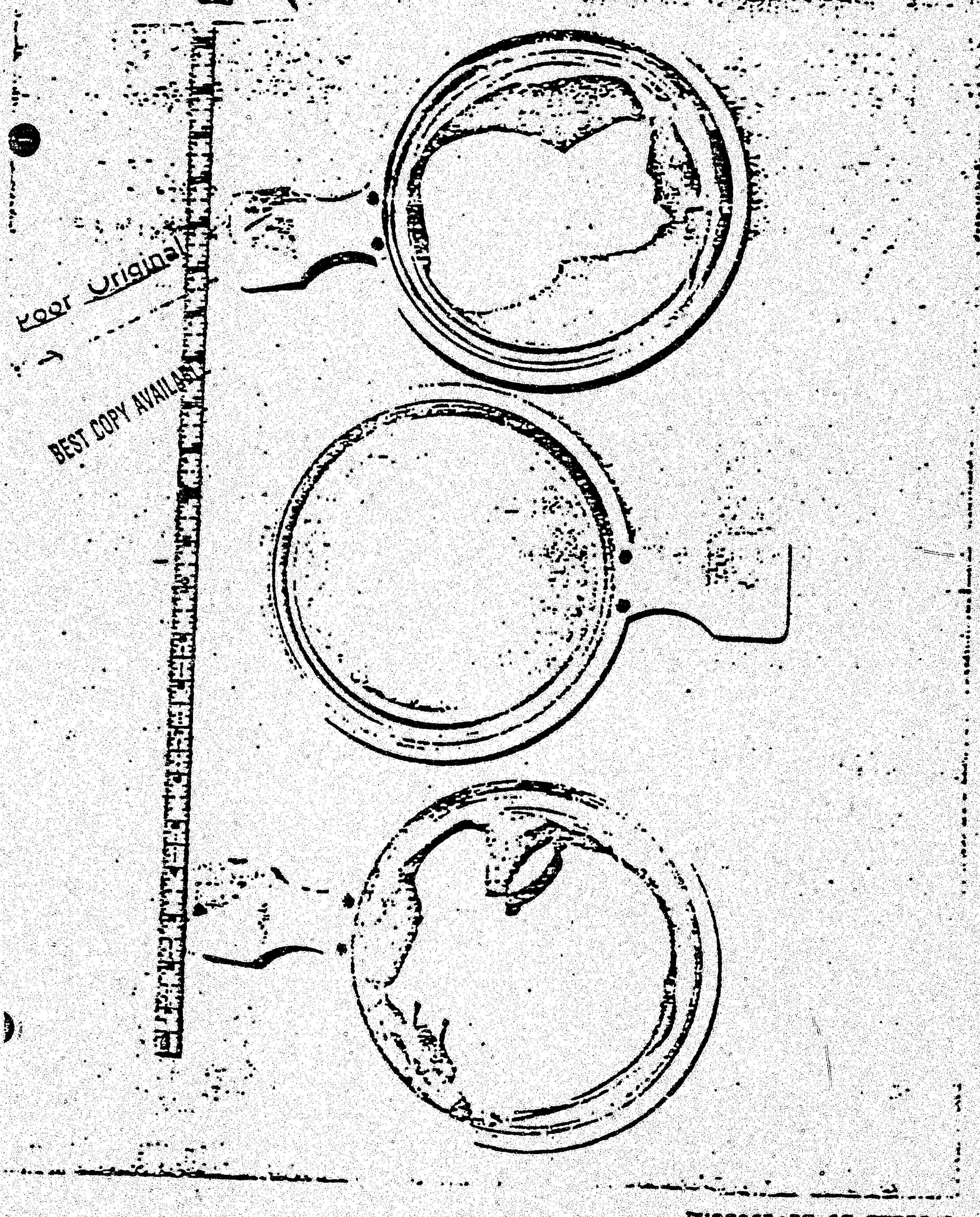
APPENDIX IV, FIGURE 9



ARRANGEMENT OF MOSS LANDING TEST FACILITY



PHOTOGRAPH OF
TEST FACILITY



PHOTOGRAPH OF TYPICAL RUPTURE DISCS

c. Dry Well and Internals

The test dry well, shown in Figure 13, is a cylindrical vessel 6.5 feet in diameter containing 230 cubic feet, including the vent pipe down to the water level, which is approximately 1/48th of the corresponding Humboldt volume. Since the Humboldt dry well will operate at a temperature of 150°F, provision was made for heating the Moss Landing dry well vessel to that temperature during some of the tests.

The piping carrying the steam and water from the simulated reactor vessel through the rupture discs is made oversize to direct rupture flow without introducing significant losses. It is terminated inside the dry well with different fittings for different tests. The fittings are shown in Figure 13. Arrangements A to E were used to produce different amounts of water carryover in the steam leaving the dry well and also accelerate or delay the time of air discharge.

d. Vent Piping

The vent piping consisted of 14-inch schedule 30 pipe with one capped tee, one long radius ell, and one short radius ell. The arrangement is shown in Figure 10.

e. Suppression Chamber

The Moss Landing suppression chamber shown in Figure 14 is contained in a tank 12 feet in diameter and 49 feet high. The suppression chamber is approximately trapezoidal in section, being 12 feet across, 2.35 feet on one base and 1.23 feet on the other. It is formed by two flat plates extending the height of the tank. The inner surfaces of the plates above the water pool are covered with ½ inch cork sheet to minimize condensation. The pool depth is 18 feet, the same as Humboldt.

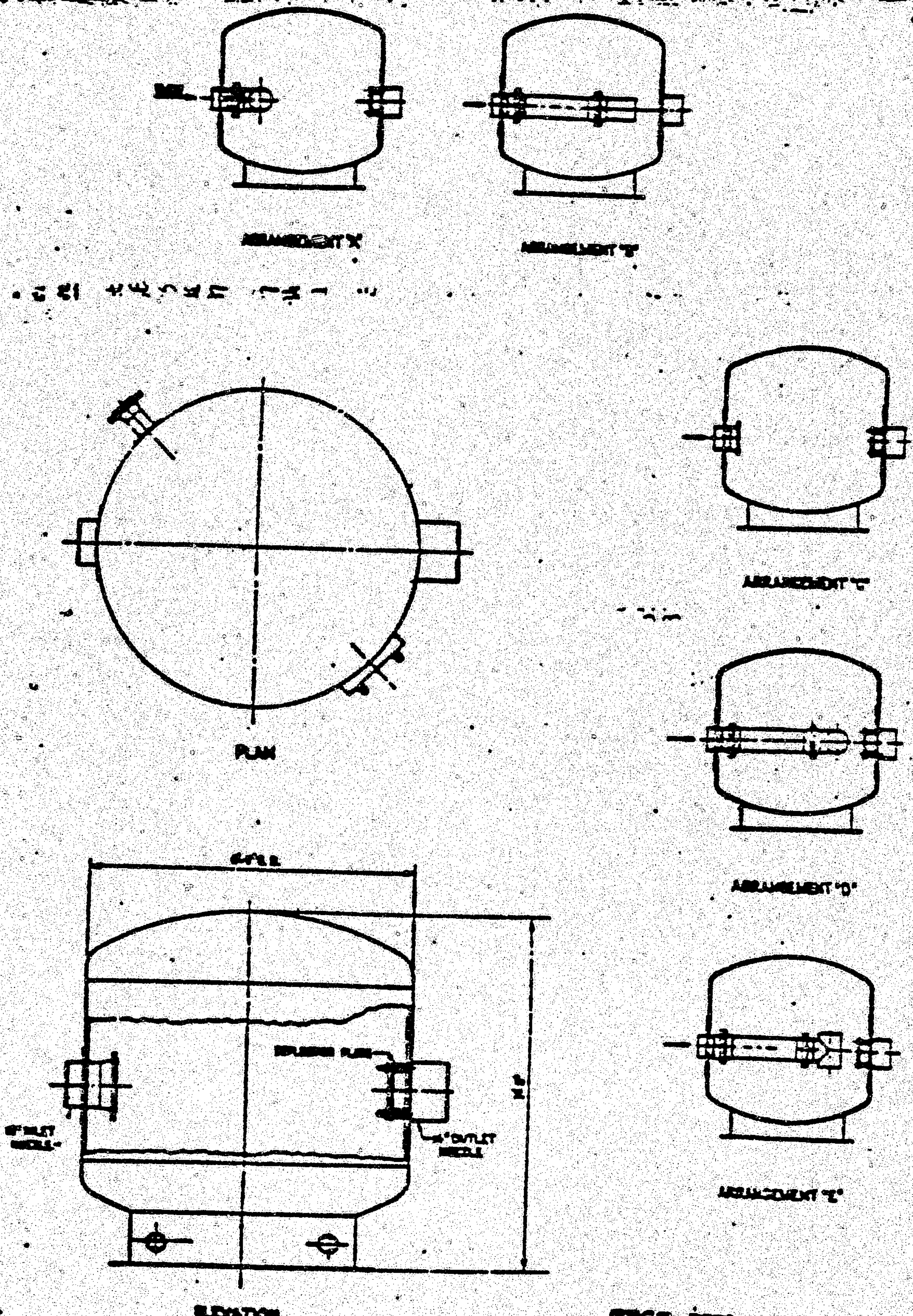
f. Instrumentation

Provision is made for the measurement of pressures and temperatures at the points shown in Figure 9. A light beam oscillograph is used to record transient pressures in the simulated reactor vessel, the dry well, and the suppression chamber. A photograph inside the instrument shelter is shown in Figure 15.

4. Test Results

a. Maximum Credible Operating Accident

A plot of three representative test results for the simulated maximum credible operating accident is shown in Figure 16. Pressure, in pounds per square inch, is plotted against time in seconds for (1) the reactor vessel, (2) the dry well, and (3) the suppression chamber. By the use of internal piping Arrangements A, B, and C, Figure 13, in the test dry well, the following conditions were produced:



TEST DRY WELL AND
ARRANGEMENT OF INTERNALS

APPENDIX IV, FIGURE 13

A table can be made of various flow properties at given pressures in the vent pipe from equations (11), (12), and (13). Equation (12) is solved for a by trial and error. This table is shown on the following page.

TABLE IV
FLOW PROPERTIES FOR SLIP FLOW

P psia	quality x	ρ_f lb/ft^3	ρ_g lb/ft^3	$x^2 \frac{\rho_f}{\rho_g}$	$(1-x)^2$	Voids a	1-a	ρ
24	.3926	59.14	.05904	154.4	.3689	.9671	.0329	2.003
27	.3875	58.96	.06592	134.3	.3752	.9646	.0354	2.151
30	.3827	58.79	.07275	118.4	.3811	.9622	.0378	2.292
33	.3783	58.65	.07954	105.5	.3865	.9597	.0403	2.440
36	.3742	58.51	.08630	94.93	.3916	.9574	.0426	2.575
39	.3703	58.34	.09302	86.00	.3965	.9551	.0449	2.708
42	.3666	58.24	.09971	78.50	.4012	.9529	.0471	2.838
45	.3632	58.11	.1064	72.04	.4055	.9507	.0493	2.966
48	.3598	57.97	.1130	66.41	.4099	.9486	.0514	3.087
51	.3567	57.87	.1196	61.56	.4138	.9465	.0535	3.209
54	.3536	57.77	.1262	57.24	.4178	.9444	.0556	3.331
57	.3507	57.67	.1328	53.41	.4216	.9424	.0576	3.447
60	.3479	57.54	.1394	49.96	.4252	.9403	.0597	3.566
63	.3452	57.44	.1459	46.91	.4288	.9383	.0617	3.681
66	.3426	57.34	.1524	44.16	.4322	.9364	.0636	3.790
69	.3400	57.24	.1590	41.62	.4356	.9344	.0656	3.904
72	.3376	57.14	.1655	39.35	.4388	.9325	.0675	4.011
75	.3352	57.05	.1719	37.29	.4420	.9306	.0694	4.119
78	.3329	56.98	.1784	35.40	.4450	.9287	.0713	4.228
81	.3306	56.88	.1849	33.62	.4481	.9269	.0731	4.329
84	.3284	56.79	.1914	32.00	.4510	.9250	.0750	4.436
87	.3263	56.72	.1978	30.53	.4539	.9231	.0769	4.544
90	.3242	56.63	.2042	29.15	.4567	.9213	.0787	4.645

Whether a critical end of line condition exists can be determined from Equation (10). If critical flow were to occur between 24 and 27 psia, from Table IV delta v would be (.4993 - .4649) = .0344 and delta P/delta v would be 87.2. Substituting these values into (10) gives:

$$87.2 = \frac{(w/\ .958)^2}{144 \times 32.2}$$

$$w = 610 \text{ lb per sec}$$

This is the minimum flow rate per 14" vent pipe which could result in a critical end of line condition with Martinelli type flow. The design flow rate from the rupture is about 200 lb per sec per 14" pipe; therefore, a critical end of line condition would not occur with Martinelli type flow. The vent flow will be affected by suppression chamber pressure, so it is assumed in subsequent calculations that back pressure on the vents is 24 psia when the peak dry well pressure occurs. This value corresponds approximately to the expected maximum chamber pressure plus static head of suppression pool water.

The two phase friction drop can be written as follows (Equations (24) and (25) of Levy's paper):

$$dP_{TP} = \frac{1}{(1-a)^2}\,\frac{f}{\rho_\ell}\,\frac{1}{2gD}\,\frac{w^2}{A}\,(1-x)^2 \qquad (14)$$

and the general flow equation (Equation (4)) becomes:

$$\frac{f}{2D}\,\frac{\rho}{\rho_\ell}\,\frac{(1-x)^2}{(1-a)^2}\,\Delta L = \frac{\rho\Delta P \pm \rho^2\Delta H/144}{(w/A)^2/144g} + \frac{\Delta\rho}{\rho} \qquad (15)$$

where
- ΔL = a small finite equivalent length of pipe, ft
- ΔP = pressure drop across ΔL, psi
- ΔH = elevation change across ΔL, ft
- $\Delta\rho$ = change in average mixture density across ΔL

Calculations paralleling the homogeneous calculations can be made using equation (15) with the data from Table IV. The same Trial A and B and pieces of piping are used as before.

The velocity head at the entrance plate is obtained from the following equation (the first two terms of equation (3) of Levy's paper).

$$\Delta P = \frac{1}{144gA}\,\Delta\left[A_g\rho_g\,u_g^2 + A_L\rho_L\,u_L^2\right] \qquad (16)$$

If the velocity in the dry well is approximately zero, this equation becomes:

$$\Delta P = \frac{1}{144g}\left[a\rho_g\,u_g^2 + (1-a)\rho_L\,u_L^2\right] \qquad (17)$$

TABLE V
CALCULATED RESULTS FOR SLIP FLOW

Slip Trial A - Solutions of equation (15) for delta L:

Piece	ΔP, psi	ρ	$\Delta\rho$	$\frac{\rho^2}{144}$	$\frac{\rho(1-x)^2}{\rho_f(1-a)^2}$	ΔL, ft
		P_6 = 24.0 psia (pool pressure at vent discharge)				
(1)	27 to 24	2.077	-.148	.0314	11.22	16.1
	30 to 27	2.222	-.141	.0343	10.67	18.7
	33 to 30	2.366	-.148	.0389	10.13	21.4 (x.196)
		P_5 = 30.6 psia (sum of delta L = 39 ft)				
(2)	33 to 30	2.366	-.148	---	10.13	19.0 (x.804)
	36 to 33	2.508	-.135	---	9.74	21.3
	39 to 36	2.642	-.133	---	9.34	23.5
	42 to 39	2.773	-.130	---	8.95	25.8 (x.023)
		P_4 = 39.1 psia (sum of delta L = 60.7 ft)				
(3)	42 to 39	2.773	-.130	---	8.95	93.0 (x.977)
	45 to 42	2.902	-.128	---	8.70	100.4
	48 to 45	3.026	-.121	---	8.36	109.6 (x.344)
		P_2 = 46.0 psia (sum of delta L = 229 ft)				
(4)	48 to 45	3.026	-.121	---	8.36	109.6 (x.656)
	51 to 48	3.148	-.122	---	8.16	117.0
	54 to 51	3.270	-.122	---	7.91	125.4 (x.799)
		P_1 = 53.4 psia (sum of delta L = 289 ft)				

At the pipe inlet:

$$u_g = \frac{Xw}{\rho_g aA} = \frac{.3536 \times 1436}{.1262 \times .9444 \times 8.8} = 484 \text{ ft per sec}$$

$$u_L = \frac{(1-X)w}{\rho_L(1-a)A} = \frac{.6464 \times 1436}{57.77 \times .0556 \times 8.8} = 32.7 \text{ ft per sec}$$

$$\Delta P = \frac{.9444}{144 \times 32.2} \times .1262 \times 484^2 + \frac{.0556}{144 \times 32.2} \times 57.77 \times 32.7^2 = 6.7 \text{ psi}$$

Dry well pressure = 53.4 + 6.7 = 60.1 psia = <u>45.4 psig</u>

TABLE V (Continued)

Slip Trial B - Solutions of equation (15) for delta L:

Piece	ΔP, psi	ρ	$\Delta\rho$	$\frac{\rho^2}{144}$	$\frac{\rho(1-x)^2}{\rho_L(1-a)^2}$	ΔL, ft
		P_6 = 24.0 psia (pool pressure at vent discharge)				
(1)	27 to 24	2.077	-.148	.0314	11.22	12.0
	30 to 27	2.222	-.141	.0343	10.67	13.9
	33 to 30	2.366	-.148	.0389	10.13	15.9 (x.824)
		P_5 = 32.5 psia (sum of delta L = 39 ft)				
(2)	33 to 30	2.366	-.148	---	10.13	14.5 (x.176)
	36 to 33	2.508	-.135	---	9.73	16.3
	39 to 36	2.642	-.133	---	9.34	18.1
	42 to 39	2.773	-.130	---	8.95	19.9
	45 to 42	2.902	-.128	---	8.70	21.6 (x.191)
		P_4 = 42.6 psia (sum of delta L = 60.7 ft)				
(3)	45 to 42	2.902	-.128	---	8.70	77.6 (x.809)
	48 to 45	3.026	-.121	---	8.36	84.5
	51 to 48	3.148	-.122	---	8.16	90.1 (x.908)
		P_2 = 50.7 psia (sum of delta L = 229 ft)				
(4)	51 to 48	3.148	-.122	---	8.16	90.1 (x.092)
	54 to 51	3.270	-.122	---	7.91	97.0
	57 to 54	3.389	-.116	---	7.70	103.5
	60 to 57	3.506	-.119	---	7.50	108.8 (x.737)
		P_1 = 59.2 psia (sum of delta L = 289 ft)				

At the pipe inlat:

$$u_g = \frac{.3479 \times 1628}{.1394 \times .9403 \times 8.8} = 492 \text{ ft per sec}$$

$$u_L = \frac{.6521 \times 1628}{57.54 \times .0597 \times 8.8} = 35.3 \text{ ft per sec}$$

$$\Delta P = \frac{.9403}{144 \times 32.2} \times .1394 \times 492^2 + \frac{.0597}{144 \times 32.2} \times 57.54 \times 35.3^2 = 7.8 \text{ psi}$$

Dry well pressure = 59.2 + 7.8 = 67.0 psia = 52.3 psig

By plotting these results with rupture flow, the dry well pressure at the balanced flow condition is found to be 50.8 psig by the slip flow method.

E. MAXIMUM SUPPRESSION CHAMBER PRESSURE

The maximum suppression chamber pressure occurs at the beginning of the cooldown period when all the dry well air has been transferred and temperatures in the chamber are a maximum. This pressure is determined by mass and energy balance. The pressure is somewhat dependent upon initial conditions so a number of conservative assumptions have been made for the design case which are as follows:

Assumptions for the Design Case

(1) The reactor is operating initially at 230 Mwt with normal water level, but the water is saturated at 1250 psig.

(2) All available heat of the reactor vessel and internals (initially at 574°F) is transferred to the coolant.

(3) Heat absorption by the dry well and chamber walls is neglected.

(4) Initial states in the suppression system are: dry well air is dry at 150°F; chamber air is dry at 100°F; and pool water is at 80°F.

(5) No significant cooldown occurs for two minutes, and reactor decay heat and continued full feedwater flow during this time add energy to the suppression system.

In addition to these assumptions it should be noted that in the absence of air pockets in the dry well all dry well air would have transferred to the suppression chamber. This complete transfer could result from continuously diluting and venting off the air by 40 times more steam than air during the venting period. Also, it should be noted that the rapid and complete condensation of vented steam is a basic requirement of the suppression system which is assumed to be satisfied in these calculations.

The suppression chamber peak pressure is determined largely by the air which has been transferred from the dry well into the chamber. In order to account for vapor pressure and minor effects, a detailed mass and energy balance is required.

The weights and energies in the system are determined from initial conditions making use of system volumes and published physical properties of the fluids and materials and accounting for energy and weight added by reactor decay heat and feedwater flow. The internal energy in the containment system after the MCOA equals the internal energy before plus decay heat and feedwater enthalpy added. The peak chamber pressure is determined by balancing the partial energies and weights by trial and error.

Table VI is a weight, volume, and energy balance before and after the MCOA. The design pressure of 10 psig is based on the calculated value of 9.3 psig. The lower part of the suppression chamber is designed for 10 psig plus the hydrostatic pressure of the pool water or 17.8 psig at the bottom.

TABLE VI

ENERGY BALANCE FOR CHAMBER PRESSURE

	Pressure psia	Temp. F	Volume cu ft	Weight lbs	Btu/lb	Energy 1000 Btu
1. Before MCCA						
Reactor vessel	--	574	--	400,000	Base	--
Reactor water	1265	574	1,790	79,555	575.4	45,776
Reactor steam	1265	574	860	2,527	1100.7	2,781
Dry well air	14.7	150	12,500	814	8.6	7
Chamber air	14.7	100	33,400	2,370	Base	--
Pool water	14.7	80	20,900	1,305,000	48.02	62,666
2. Energy Added						
Decay heat	--	--	--	--	--	4,000
Feedwater:						
first minute	--	--	--	13,500	240	3,300
second minute	--	--	--	13,500	60	800
Totals			69,450	1,817,266		119,330
3. After MCCA						
Reactor vessel	--	237.9	--	400,000	-36.98	-14,792
Steam, R & DW	(24.0)	237.9	15,150	895	1084.6	971
Chamber air	22.0	126.2	31,367	3,184	4.6	15
Moisture in air	2.01	126.2	(31,367)	181	1052.0	190
Pool water	--	126.18	22,933	1,413,006	94.09	132,946
Totals	24.0 psia = 9.3 psig		69,450	1,817,266		119,330

F. TRANSIENT (VENT CLEARING) DRY WELL PRESSURE

An initial transient dry well pressure occurs as the water which corresponds to vent submergence is cleared from the vents. For the Humboldt conditions and physical proportions, this pressure is less than the steady flow pressure, so it does not affect the design.

The vent clearing pressure can be observed on some of the Moss Landing tests. Tests with less than 100% water carryover have a low steady flow pressure and the clearing pressure for these tests is sometimes the maximum dry well pressure. This can be observed on test 25 of Figure 16 of Appendix IV. At something less than $\frac{1}{2}$ second, dry well pressure is a maximum. At the same time chamber pressure is just beginning to move off zero, which indicates that no significant venting has yet occurred. (As has been mentioned earlier, test 27 shown on the same figure has a relatively low vent clearing pressure because the rupture jet discharges directly into the vent entrance.)

A transient analysis method was developed in the initial phases of the pressure suppression development program. This was found to be conservative in predicting the small scale transient test results. However, this method neglected friction resistance of the vent pipes and it was not known until the full scale tests were run whether the method could be applied to the Humboldt design where vent resistance is significant. The full scale Moss Landing tests have clearly shown that even with significant vent resistance the vent clearing dry well pressure is lower than the steady flow design pressure for the Humboldt conditions.

The original analytical method was extended to include vent resistance. Also a second rather elaborate method was developed which divided the vent pipe resistance and volume capacity into a series of distributed constants.

It was apparent from the results of the distributed constants method that the original method with some minor modifications could be used to give essentially the same results. Following is the modified original method.

The method consists of taking a small time step from time zero of say .01 seconds and solving for all pressures and flows in the system. With all system pressures known at .01 seconds another time step can be taken and system pressures and flows found. This procedure is continued one time step at a time until the transient peak pressure is found.

In the equations which follow, the primed functions represent the values from the preceding time step which are known.

In a trial and error solution for a given time step (delta t), a trial velocity (u) of water in the vents is assumed. The acceleration of water in the vents (a) is (u-u')/delta t. The displacement of water in the vents (x) is:

$$x = x' + \frac{1}{2} a(\Delta t)^2 + u' \Delta t \qquad (18)$$

The static pressure at the discharge of the vents (P_5) is the chamber pressure (P_6) (assumed constant until water is cleared from the vents) plus static head of pool water, which increases slightly as the water from the vents raises the pool level, plus an acceleration pressure, which assumes that pool water acceleration below the vent discharge is negligible compared to the net upward acceleration of pool water above the vent discharge.

$$P_5 = P_6 + (H+x/R)\rho/144 + (H+x/R)\rho a/R144g \quad (19)$$

where
- H = initial vent submergence = 6 ft
- x = total displacement of water in the vents, ft
- R = ratio of pool surface area to vent area
- ρ = density of water, lb per cu ft
- a = acceleration of water in the vents, ft per sec^2
- g = gravity constant = 32.2 ft per sec^2

A similar equation can be written for the pressure (P_4) at the air-water interface inside the vent.

$$P_4 = P_5 - (H-x)\rho/144 + (H-x)\rho a/144g + (H-x)f\rho u^2/288Dg \quad (20)$$

where the last term represents water friction in the vents and

- f = friction factor
- D = vent inside diameter, feet

Pool water mass below the vent discharge may be significant. Adding one vent pipe diameter to H in the above equations makes the calculations agree very well with test results.

The results of the distributed constants method showed that for an MCOA transient with Humboldt's physical proportions the pressure drop of air in the vents is insignificant compared with the water pressure drops given by equations (19) and (20). Most of the air pressure drop was found to be the entrance velocity head. Resistance drops down the pipe were not significant because the air flow rate (lb per sec) and velocity diminishes going down the pipe. The air flow rate is determined principally by vent volume filling, and it can be assumed if vent entrance velocity head is small that the total air volume in the dry well and vents acts as a single large volume.

Dry well pressure change during a time increment can be predicted for the single total air volume by a mass and energy balance. Table VII shows typical balances for the first few hundredths of a second of the Humboldt design MCCA. No mixing of steam and air is assumed. (Mixing would cause the pressure to be lower.) Compression of air would be essentially reversible adiabatic and the relation between air pressure and volume is $PV^{1.4}$ = constant. The energy in the air is the work done on it by the steam-water less the work done by air on the water in the vents. The work is $P\Delta V 144/778$ where P is the average air pressure during the increment and ΔV is the volume change of steam-water, air, or water displacement. (Actually, the air energies in Table VII were obtained by summing smaller increments than those shown.) Air temperature is obtained from the state equation for an ideal gas (PV/T = constant). The total change in internal energy of the steam-water mixture in the dry well is equal to the total enthalpy added (from the rupture) less the work done on the air which is represented by the change in volume of the steam-water mixture. (For reference see Mechanical Engineers' Handbook, Lionel S. Marks, Fifth Edition, page 286.) Steam and water properties from the steam tables (internal energy, specific volume, etc.) complete the information needed to make the balances.

TABLE VII
VENT CLEARING ENERGY BALANCES

	Pressure psia	Temp. F	Weight lbs	cu ft/lb	Btu/lb	Volume cu ft	Energy Btu
1. Time = 0 seconds							
Dry well air	14.70	150	816.2	15.3	Base	12,500	--
2. Energy added in first .02 seconds (Displacement = .004 ft)							
Water from rupture	1265	574	195	.02256	580.6	--	113,217
Work done on water in vents						.2	-.6
Totals (1 and 2)			1011.2			12,500	113,216
3. Time = .02 seconds (Displacement = .004 ft)							
Air	18.14	188.0	816.2	13.2	-	10,760	5,260
Steam	"	222.8	79.5	21.9	1080.5	1,738	85,896
Water	-	"	115.5	.0168	191.0	2	22,060
Totals (3)	18.14 psia		1011.2			12,500	113,216
4. Energy added in next .02 seconds (Displacement = .028 ft)							
Water from rupture	1265	574	195	.02256	580.6	--	113,217
Work done on water in vents						1.3	-5
Totals (3 and 4)			1206.2			12,501	226,428
5. Time = .04 seconds (Total displacement = .03 ft)							
Air	21.43	219.3	816.2	11.7	-	9,549	9,680
Steam	"	231.6	157.2	18.8	1082.9	2,948	170,211
Water	-	"	232.8	.0169	199.9	4	46,537
Totals (5)	21.43 psia		1206.2			12,501	226,428
6. Energy added in next .02 seconds (Displacement = .06 ft)							
Water from rupture	1265	574	195	.02256	580.6	--	113,217
Work done on water in vents						2.8	-12
Totals (5 and 6)			1401.2			12,504	339,633
7. Time = .06 seconds (Total displacement = .09 ft)							
Air	24.63	247.0	816.2	10.6	-	8,647	13,515
Steam	"	239.2	233.3	16.5	1084.9	3,851	253,140
Water	-	"	351.7	.0169	207.5	6	72,978
Totals (7)	24.63 psia		1401.2			12,504	339,633

Similar balances are made for successive times. The displacement in the energy balance would correspond to the trial value of u.

The trial and error solution for a given time increment is completed by trying new values of u until P_4 from equation 20 equals dry well pressure in the corresponding energy balance.

After an increment is balanced a check on the assumption of equality of air pressure can be made. The vent entrance flow is the product of air density and air volume in the vent at the end of an increment less the same product at the beginning of the increment. From this the vent entrance velocity and velocity head pressure can be found. If the vent entrance velocity head pressure is small (less than 2 psi, say) and the velocity is considerably below sonic, air vent friction may be considered insignificant and the results obtained above are substantially correct.

Table VIII is a tabulation of pressures and flows for .01 second time steps for the Humboldt design MCGA rupture flow calculated by the method presented above. These are the results for each time step after the error in the trial and error process has been reduced to zero. The mass of pool water down to one foot below the end of the vent pipe is included.

The highest vent entrance velocity calculated for the MCGA was less than 350 ft per sec and the highest entrance velocity head was 1.1 psi which shows that air pressure drop is not particularly significant.

TABLE VIII - VENT CLEARING TRANSIENT
MODIFIED ORIGINAL METHOD - DESIGN MCOA

Time Seconds	Water Displacement Feet	Dry Well Pressure psia	Pressure in the Pool 1 ft below the end of the vent pipe. psia
.00	0	14.70	17.71
.01	0	16.44	17.80
.02	0	18.14	17.88
.03	0	19.82	17.96
.04	0	21.43	18.04
.05	.1	23.02	18.11
.06	.1	24.63	18.19
.07	.1	26.19	18.27
.08	.2	27.71	18.35
.09	.2	29.23	18.43
.10	.3	30.74	18.51
.11	.4	32.18	18.60
.12	.5	33.60	18.68
.13	.7	35.01	18.78
.14	.9	36.42	18.88
.15	1.0	37.85	18.99
.16	1.3	39.31	19.12
.17	1.5	40.72	19.27
.18	1.8	42.03	19.42
.19	2.1	43.33	19.61
.20	2.5	44.61	19.84
.21	2.9	45.90	20.12
.22	3.4	47.18	20.50
.23	3.9	48.48	21.03
.24	4.5	49.78	21.86
.25	5.2	50.97	23.33
.262	6.0	52.38 (37.7 psig)	26.60

Table VIII shows that as the vents clear there may be a fairly high differential pressure in the pool. The exact magnitude of the resulting pool water acceleration could not be determined without a study of the acceleration characteristics of the pool and the air flow from the end of the vent pipe. However, the calculated magnitude of the pressure unbalance in the pool shown on Table VIII indicates that the general upward acceleration of water above the end of the vents could increase from about one g at .22 seconds to something on the order of 7g at the time of clearing. The Unit design can accommodate the pressure in the suppression pool.

Figure 3 is a comparison of full scale test results with calculated results. Line X is the locus of the calculated vent clearing points for the test proportions at various rupture sizes and six foot submergence neglecting water mass below the pipe exit. The test points parallel this line quite closely. Calculations for Figure 3 are based on the proportions of the test facility which give a slightly different rate of pressure buildup than the Unit design which was used for Table VIII. This figure also includes air pressure drop in the vent pipes which adds about 1 psi to the calculated MCOA clearing pressure.

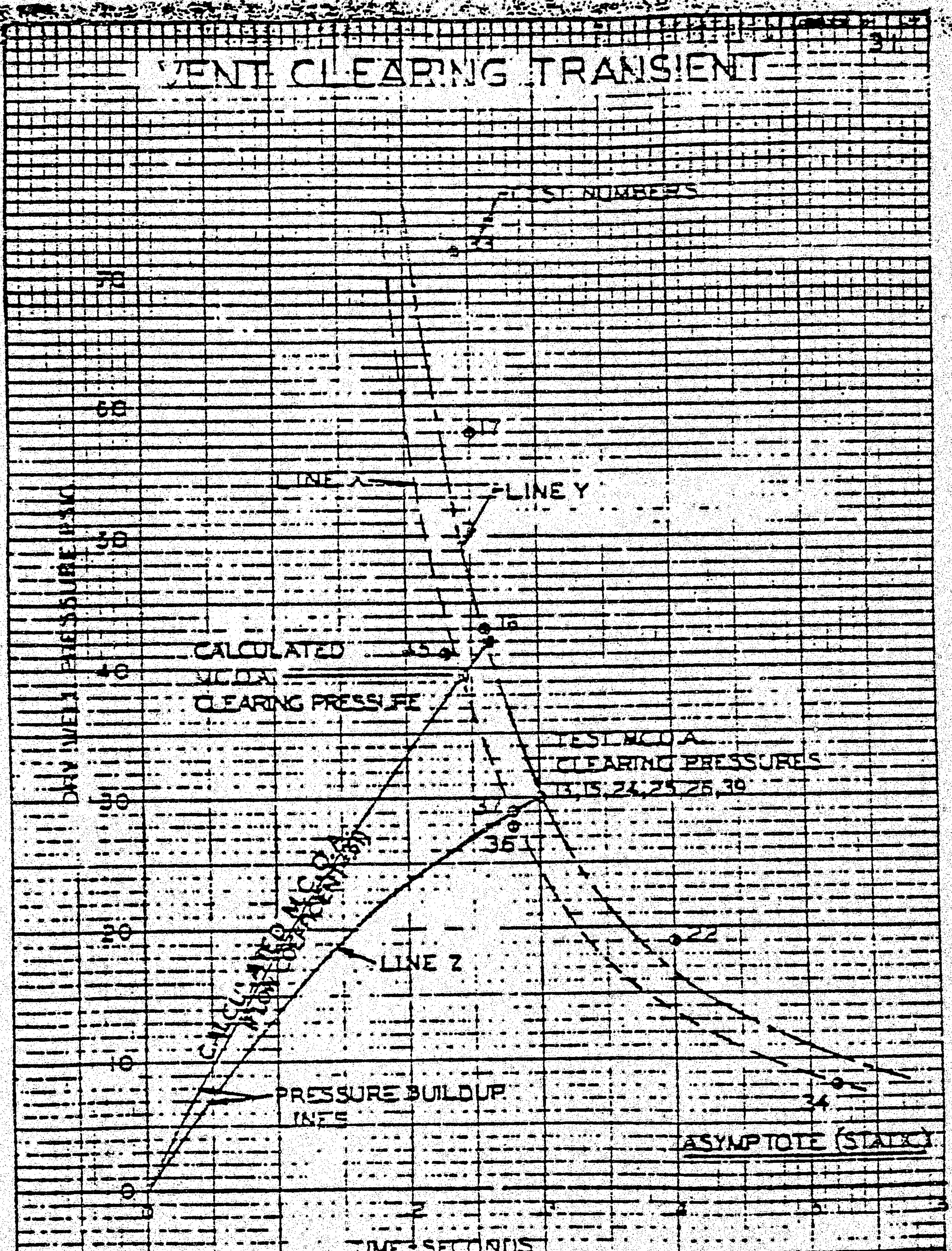
The calculated solid line on Figure 3 is essentially a series of dry containment type calculations which, if continued as though there were no vents, would rise until all available energy was released from the reactor system. This ultimate (no venting) pressure would correspond with the usual dry containment design pressure.

Since the pressure buildup can be predicted by energy balance, which is essentially the established design method for dry type (capsule) containment, the proof of the vent clearing analytical method is how well it predicts clearing time for a given pressure buildup. Clearing time determines the vent clearing pressure for a given pressure buildup. (Steam and air mixing and non steady rupture flow would have a minor effect.)

It is apparent from Figure 3 that the calculated clearing time represented by the dashed line falls slightly short of most of the test results. This strongly indicates that water mass in the pool below the vent pipe exit has an effect. Line Y on Figure 3 shows the effect of including the mass of one extra pipe diameter depth of water in the calculations. This appears to agree very well with test results.

The lower rate of pressure buildup in the tests indicated by Line Z on Figure 3 is mainly due to lower rupture flow rate than that which corresponds to a .61 flow coefficient. Heat transfer to the walls is apparently insignificant in the vent clearing period for MCOA tests. Initial dry well temperatures varied as much as 70F on the MCOA tests of Figure 3 yet there is no observable difference in the pressure buildup.

The tests show a rapid drop in reactor vessel pressure at the beginning of the event. This suggests that flashing has not yet begun in the rupture flow or in the vessel. Therefore, it is expected that the rupture flow rate during the vent clearing period would be higher than during the later venting period. The test flow rate can be determined approximately from the slope of the pressure buildup line by means of energy balances. For example, using Line Z



APPENDIX V
FIGURE 3

on Figure 3 as energy balance results the ratio between the slope of the test line to that of Line Z at a given pressure is the ratio of the test flow rate to calculated. Applying this comparison to the MCCA test results gives a ratio of about 70% for the first 0.1 sec., falling off to something on the order of 50% near the end of the vent clearing transient. This appears to be consistent with the ratio of 54% observed during the steady flow condition.

From the foregoing analysis and discussion it is concluded that the vent clearing transient is sufficiently well understood that peak pressure can be predicted.

C. DYNAMIC EFFECTS

The three types of dynamic effects related to the design of the pressure suppression containment are:

(1) Rupture jet flow reaction and impact

(2) Shock wave

(3) Missiles

These are discussed here in that order.

1. Rupture Jet Flow Reaction and Impact

Rupture jet flow reaction on the reactor vessel would be equal in magnitude to the impact of the jet on the wall of the dry well according to Newton's Third Law (the principle of action and reaction). The magnitude of this force (F) is given by the following formula (Equation 72, "Elementary Fluid Mechanics", John K. Vennard):

$$F = Q\rho u/g \qquad (30)$$

where F = reaction or impact force, pounds

Q = volume rate of flow cu ft/sec

ρ = fluid density, lbs/cu ft

u = velocity of the free jet, ft/sec

g = gravity constant = 32.2 ft/sec^2

The product of $Q \cdot \rho$ is w, the rupture flow rate from equation (1). The velocity of the free jet, u, is related to the total available pressure drop (reactor pressure minus dry well pressure) by the familiar velocity head formula:

$$\Delta P = \frac{u^2}{2g}\,\frac{\rho}{144} \qquad \text{or } u = 12\sqrt{2g\,\Delta P/\rho} \qquad (31)$$

Combining (1), (30) and (31) gives:

$$F = 288\ C A \Delta P \tag{32}$$

The parameters C and A come from equation (1) and are the flow coefficient and rupture area, respectively.

For Humboldt MCCA conditions and initial dry well pressure of zero psig:

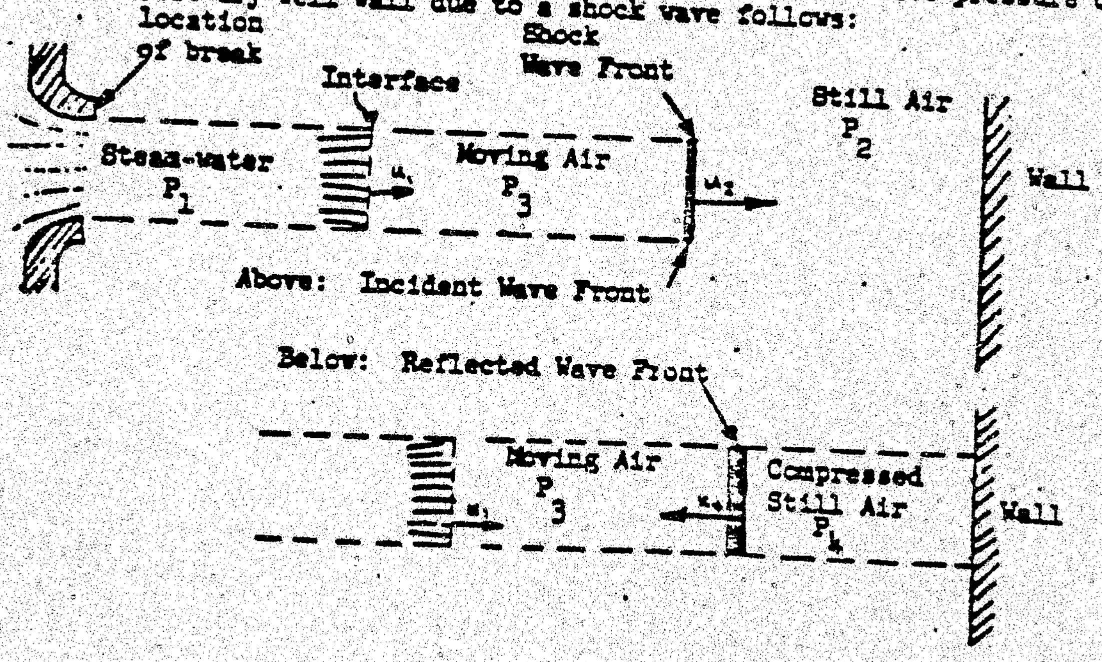
$$F = 288 \times .61 \times .705\ (1250-0) = 155{,}000 \text{ pounds}$$

It is interesting to note that the jet momentum plus pressure force at any distance along the jet must equal F according to Newton's Third Law. Therefore, since the jet after leaving the rupture cannot exert a reaction on the vessel, the free jet cannot gain momentum force by flashing. The result of flashing in the jet would be to spread the 155,000 pound force over a larger area of the dry well wall than if no flashing were to occur. The highest dynamic pressure on the wall by the jet cannot exceed 1250 psig.

2. Shock Wave

A shock wave could be generated at the start of a primary system rupture accident if the rupture were to occur instantaneously. As the steam-water mixture began to flow into the dry well it would compress the initially stationary air ahead and cause a compression (shock) wave to move through the air. When the shock wave front encountered a wall it would be reflected back into its tail causing a blast pressure on the wall roughly double the incident shock wave pressure.

The schematic diagram used in determining the maximum blast pressure on the nearest dry well wall due to a shock wave follows:



Symbols used in the diagram are defined as follows:

P_1 - pressure of steam water behind interface, psia
P_2 - pressure of undisturbed air, psia
P_3 - pressure of moving air ahead of interface, psia
P_4 - pressure behind reflected shock (blast pressure) psia
u_1 - velocity of steam-water/air interface, ft per sec
u_2 - velocity of incident shock wave, ft per sec
u_4 - velocity of reflected shock wave, ft per sec

The steam-water is assumed to expand isentropically from its initial liquid state to a steam-water mixture at pressure P_1. The velocity at the interface, u_1, is found from the conservation of energy and the physical properties of steam and water. The energy equation corresponds to equation (5) on page 8 of this appendix which can be written as follows:

$$u_1^2 = 2gJ\Delta h \qquad (33)$$

where Δh is the drop in enthalpy of the steam-water from the primary system condition to the free jet.

For constant entropy (.7794 Btu per lb °F) and initial enthalpy of 580.6 Btu per lb values of u_1 are computed for trial values of P_1:

P_1 (psia)	s_{fg}	s_f	X	h_f	h_{fg}	h_1	u_1
70	1.1906	.4409	.284	272.6	907.9	530.3	1587
75	1.1787	.4472	.282	277.4	904.5	532.1	1559
80	1.1676	.4531	.2795	282.0	901.1	534.0	1529

Headings s_{fg}, s_f, h_f, and h_{fg} are entropies and enthalpies from the steam tables and X is steam quality in the steam-water discharge (pounds steam per pound mixture).

The Rankine-Hugoniot equation ("The Dynamics and Thermodynamics of Compressible Fluid Flow", Asher H. Shapiro, equation 5.26) relates conditions upstream and downstream of a shock wave. If air densities corresponding to P_2 and P_3 are ρ_2 and ρ_3 (lbs/cu ft), it follows from the continuity of flow across the shock wave front that ρ_3/ρ_2 is equal to $u_2/(u_2 - u_1)$, and the Rankine-Hugoniot equation may be written as follows:

$$\frac{u_2}{u_2 - u_1} = \frac{\frac{(k+1)}{(k-1)}\frac{P_3}{P_2} + 1}{\frac{P_3}{P_2} + \frac{(k+1)}{(k-1)}} \qquad (34)$$

where k is the isentropic exponent for air which is 1.4.

Application of the momentum theorem to the flow through the shock wave front gives the following equation (Shapiro, equation 5.3):

$$144(P_3 - P_2) = \frac{w}{Ag}(u_2 - (u_2 - u_1))$$

where w/A is the flow rate per unit area of air through the shock wave front in lbs per sec-ft^2 and g is the gravity constant (32.2 ft per sec^2). Since $w/A = \rho_2 u_2 = \rho_3(u_2 - u_1)$ the above formula can be written as follows:

$$u_1 = \frac{144 P_3 g}{\rho_3 (u_2 - u_1)} - \frac{144 P_2 g}{\rho_2 u_2} \qquad (35)$$

The energy equation is as follows (Shapiro, equation 5.1):

$$h_3 + \frac{(u_2 - u_1)^2}{2gJ} = h_2 + \frac{u_2^2}{2gJ}$$

where h_3 and h_2 are the enthalpies corresponding to pressures P_3 and P_2 in the diagram and J is 778 ft lbs per Btu. If temperatures T_3 and T_2 in °R correspond with h_3 and h_2, considering air an ideal gas permits the above equation to be changed to the following:

$$Jc_pT_3 + \frac{(u_2 - u_1)^2}{2g} = Jc_pT_2 + \frac{u_2^2}{2g}$$

The relation between gas constant R (53.3 ft-lb per lb °R for air) and specific heat c_p (.241 Btu per lb °R) is $Jc_p = Rk/(k-1)$ (Shapiro, page 42). Combining this and the perfect gas relation ($RT = 144P/\rho$) with the preceding equation gives the following:

$$\frac{144k}{k-1}\left(\frac{P_3}{\rho_3}\right) + \frac{(u_2 - u_1)^2}{2g} = \frac{144k}{k-1}\left(\frac{P_2}{\rho_2}\right) + \frac{u_2^2}{2g} \qquad (36)$$

For trial values of P_3 the three simultaneous equations (34), (35), and (36) can be solved for the three unknowns ρ_3, u_1, and u_2. Following are the results for P_3 = 70, 75, and 80 psia and for the initial state of air dry at 14.7 psia and 150°F: (ρ_2 = .0652 lbs per cu ft)

P_3 psia	ρ_3 lbs per cu ft	u_1 ft per sec	u_2 ft per sec
70	.180	1580	2480
75	.186	1670	2570
80	.193	1740	2640

Noting that $P_1 = P_3$, it follows from the above table and the previous similar table that P_3 = 70.3 psia, u_1 = 1585, u_2 = 2485, and ρ_3 = .180.

For the reflected shock the following similar set of simultaneous equations apply:

$$\frac{u_4 + u_1}{u_4} = \frac{\frac{(k+1)}{(k-1)}\frac{P_4}{P_3} + 1}{\frac{P_4}{P_3} + \frac{(k+1)}{(k-1)}} \tag{37}$$

$$u_1 = \frac{144\, P_3 g}{\rho_4\, u_4} = \frac{144\, P_3 g}{\rho_3 (u_4 + u_1)} \tag{38}$$

$$\frac{144k}{k-1}\frac{(P_4)}{(\rho_4)} + \frac{u_4^2}{2g} = \frac{144k}{k-1}\frac{(P_3)}{(\rho_3)} + \frac{(u_4 + u_1)^2}{2g} \tag{39}$$

In these equations ρ_4 is the air density (lbs per cu ft) which corresponds to pressure P_4; all other parameters have been defined previously.

These three simultaneous equations are solved for the three unknowns u_4, ρ_4, and P_4. Following is the summary of the results for the Humboldt Design MCOA:

P_3 = 70.3 psia
u_1 = 1585 ft per sec
u_2 = 2485 ft per sec
ρ_3 = .180 lb per cu ft
ρ_4 = .446 lb per cu ft
u_4 = 1175 ft per sec
P_4 = 253 psia

The initial blast pressure would be conservatively 253 psia or 238 psig on the portion of the wall of the dry well nearest the rupture.

3. Missiles

The following missiles have been postulated to provide a design basis for containment missile protection:

(1) A 10 foot section of 12 inch main steam line.

(2) A 10 foot section of 8 inch feedwater line.

(3) A steel plate type fragment one-half inch thick.

Following is the method used for determining the maximum velocity of the postulated missiles.

Considering first the 12 inch line, the greatest impulse which a 10 foot section could receive would be from reaction of steam escaping from one end only. An expansion wave would travel the length of the section, finally relieving the pressure, and the jet action would cease. The expansion wave would travel at sonic velocity which for steam initially at 1265 psia is 1570 ft per sec. The impulse is given by:

$$\frac{1250 \text{ lb/in}^2 \times 102 \text{ in}^2 \times 10 \text{ ft}}{1570 \text{ ft/sec}} = 810 \text{ lb sec}$$

The linear velocity imparted to the pipe, which would weigh about 890 pounds, by this impulse would be:

$$\frac{810 \text{ lb sec} \times 32.2 \text{ ft/sec}^2}{890 \text{ lbs}} = 30 \text{ ft per sec}$$

If the pipe were to break off near the reactor vessel, the pipe could be accelerated by the impingement of steam escaping from the reactor vessel. This action would occur simultaneously with the impulse calculated above but would not be additive; that is, the impingement of steam on an end of the pipe would inhibit escape of steam from this end. The velocity resulting from impingement will now be calculated to see if a higher 12 inch pipe missile velocity could occur.

The force, F lbs, acting on the pipe section is related to the drag coefficient, C_d, the difference in velocity between the jet, u_j, and the pipe section, u, the density of the jet, ρ lb per cu ft, and the area, A sq ft, upon which the jet acts by the following equation:

$$F = \frac{1}{2} C_d A \rho (u_j - u)^2 / g \qquad (40)$$

The acceleration of the missile, a ft per sec^2, and the relation between displacement, x ft, velocity, and acceleration is given by the following, where W is the weight of the missile (890 lbs):

$$F = (W/g)a$$

$$\frac{du}{dt} = a; \; u = \frac{dx}{dt}$$

Therefore: $udu = adx = (Fg/W)dx$

and $$\frac{udu}{(u_j - u)^2} = \frac{C_d A \rho \, dx}{2W}$$

Integrating between the limits 0 to u, and 0 to x gives the following:

$$\ln \frac{u_j - u}{u_j} + \frac{u}{u_j - u} = \frac{C_d A \rho x}{2W} \qquad (41)$$

In order to solve equation (41) for the final missile velocity, u, the values of u_j and ρ must be found and some assumption must be made concerning the total linear movement of the missile, x, under the influence of the jet. It is reasonable to assume that the maximum distance through which a 10 foot length of main steam line might be accelerated before striking a wall or being moved out of the jet is on the order of three feet, and it is assumed that x = 3 feet.

From Page A-20 of Crane Technical Paper No. 410 the discharge pressure of the jet would be approximately 700 psig conservatively assuming the rupture is like a well rounded nozzle, and the jet density would be approximately 1.5 lbs per cu ft.

Assuming jet discharge is sonic, the jet velocity would be about 1570 ft per sec. Reynolds number is greater than 10^6 and the drag coefficient would not be greater than 0.4. Assuming the jet acts on the total area of the end of the pipe missile (.887 sq ft), equation (41) can be solved for the missile velocity, u:

$$\ln \frac{1570 - u}{1570} + \frac{u}{1570 - u} = \frac{.4 \times .887 \times 1.5 \times 3}{2 \times 890}$$

$$u = 65 \text{ ft per sec}$$

Since this velocity is higher than that previously calculated, this last value is used in the design.

For the 8 inch feedwater line discharging from one end the expansion and relief of pressure would occur more slowly than for the steam line missile. Something on the order of 1000 ft per sec or a total discharge time of .01 sec would be reasonable for 10 feet of the 8 inch line. The impulse from this discharge would be:

$$1250 \text{ lbs/in}^2 \times 45.6 \text{ in}^2 \times .01 \text{ sec} = 570 \text{ lb sec}$$

The linear velocity imparted to the pipe, which would weigh 430 pounds, by this impulse would be:

$$\frac{570 \text{ lb sec} \times 32.2 \text{ ft/sec}^2}{430} = 43 \text{ ft per sec}$$

If instead of this impulse, the 8 inch pipe were accelerated by the jet of water from the reactor vessel, equation (41) would apply. If no flashing were to occur, the rupture flow rate from equation (1) would be:

$$w = 12 \times .61 \times .317 \sqrt{64.4 \times 44.4 \times 1250} = 4400 \text{ lbs per sec}$$

The maximum velocity in the jet would be given by equation (31):

$$u_j = 12 \sqrt{2g \Delta P/\rho} = 12 \sqrt{64.4 \times 1250/44.4} = 512 \text{ ft per sec}$$

The feedwater line is located in a very restricted space and significant horizontal movement of this postulated missile is very unlikely. Shielding concrete would interfere with vertical movement. An accelerating distance of two feet is assumed for this missile. Reynolds number is greater than 10^6 and 0.4 drag coefficient is used. The area of the end of the pipe would be .405 sq ft and equation (41) can be solved for the missile velocity, u:

$$\ln \frac{512 - u}{512} + \frac{u}{512 - u} = \frac{.4 \times .405 \times 44.4 \times 2}{2 \times 430}$$

$$u = 83 \text{ ft per sec}$$

For the half inch thick steel fragment it is assumed that the flat surface is always normal to the impinging flow and that it is accelerated by the 12 inch steam jet for a distance of five feet. The weight of the fragment would be 20 pounds per square foot of surface.

For acceleration by the steam jet:

$$\ln \frac{1570 - u}{1570} + \frac{u}{1570 - u} = \frac{.4 \times 1.5 \times 5}{2 \times 20}$$

$$u = 480 \text{ ft per sec}$$

In summary, the following missiles and missile velocities might be possible in a credible major accident:

(1) A 10 foot section of 12 inch main steam line with a velocity of 65 ft per sec.

(2) A 10 foot section of 8 inch feedwater line with a velocity of 83 ft per sec.

(3) A steel plate type fragment one half inch thick with a velocity of 480 ft per sec.

Missile penetration is calculated from the following formula for missiles which do not increase in diameter during penetration: (See Page 32, Nuclear Safety, December 1960).

For penetration:

$$\frac{1}{2}\frac{U}{g}u^2 = U(.34t^2 + .032t)D \qquad (42)$$

U is the ultimate tensile strength of the dry well steel,psi, where
D is the diameter of the impact area, inches.
u is missile velocity, ft per second.
t is the plate thickness, inches.

For dry well steel U is taken as 60,000 psi. Estimated penetration of the dry well vessel by the three missiles is as follows:

(1) $$.34t^2 + .032t = \frac{890 \times 65^2}{64.4 \times 60{,}000 \times 12} = .0811$$

$$t = .44 \text{ inches}$$

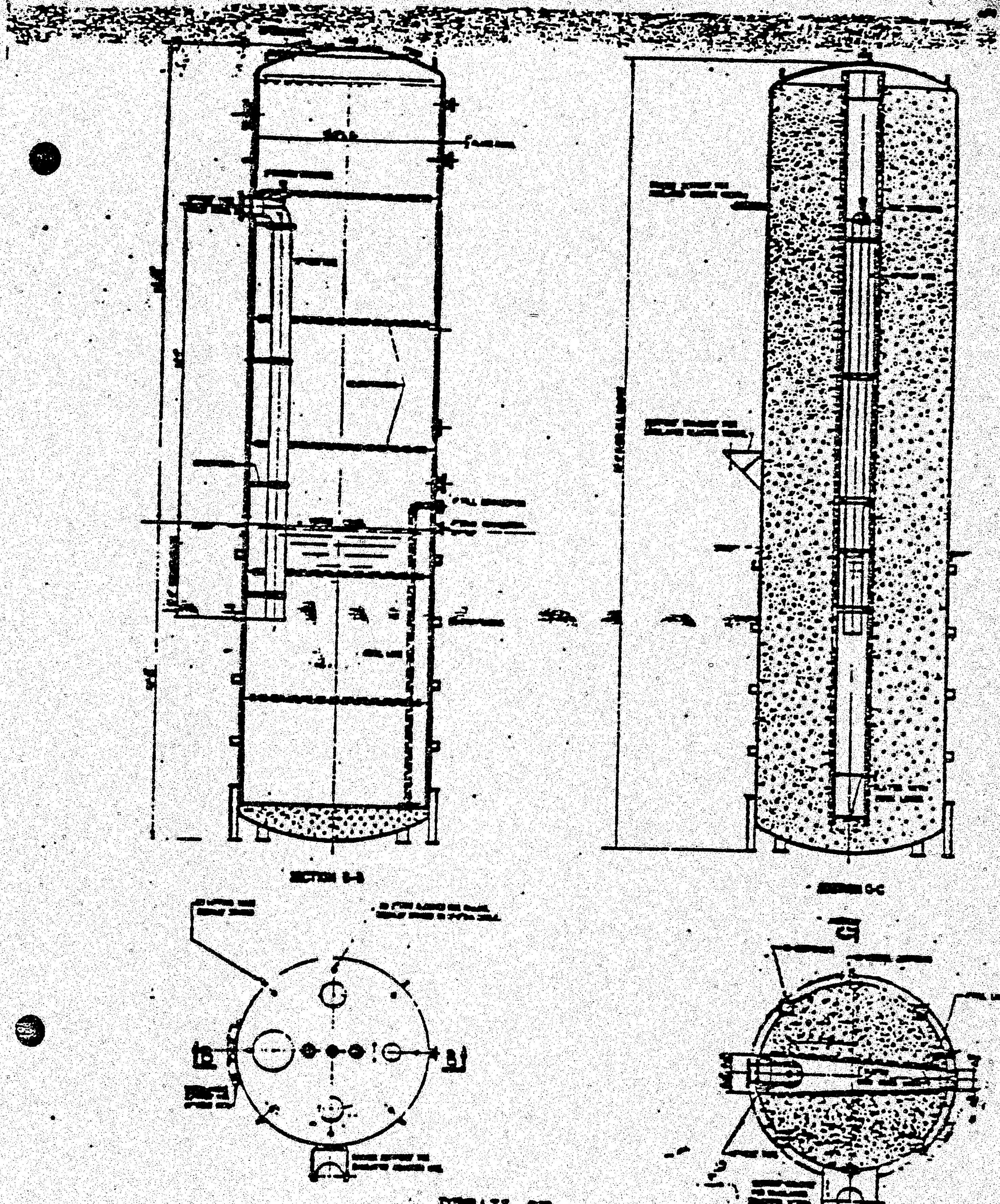
(2) $$.34t^2 + .032t = \frac{430 \times 83^2}{64.4 \times 60{,}000 \times 8} = .0958$$

$$t = .49 \text{ inches}$$

(3) $$.34t^2 + .032t = \frac{(20/24) \times 480^2}{64.4 \times 60{,}000 \times .5} = .0994$$

$$t = .50 \text{ inches}$$

Since the minimum thickness of the dry well steel is 61 inches, it is apparent that the missiles could not penetrate the dry well vessel.

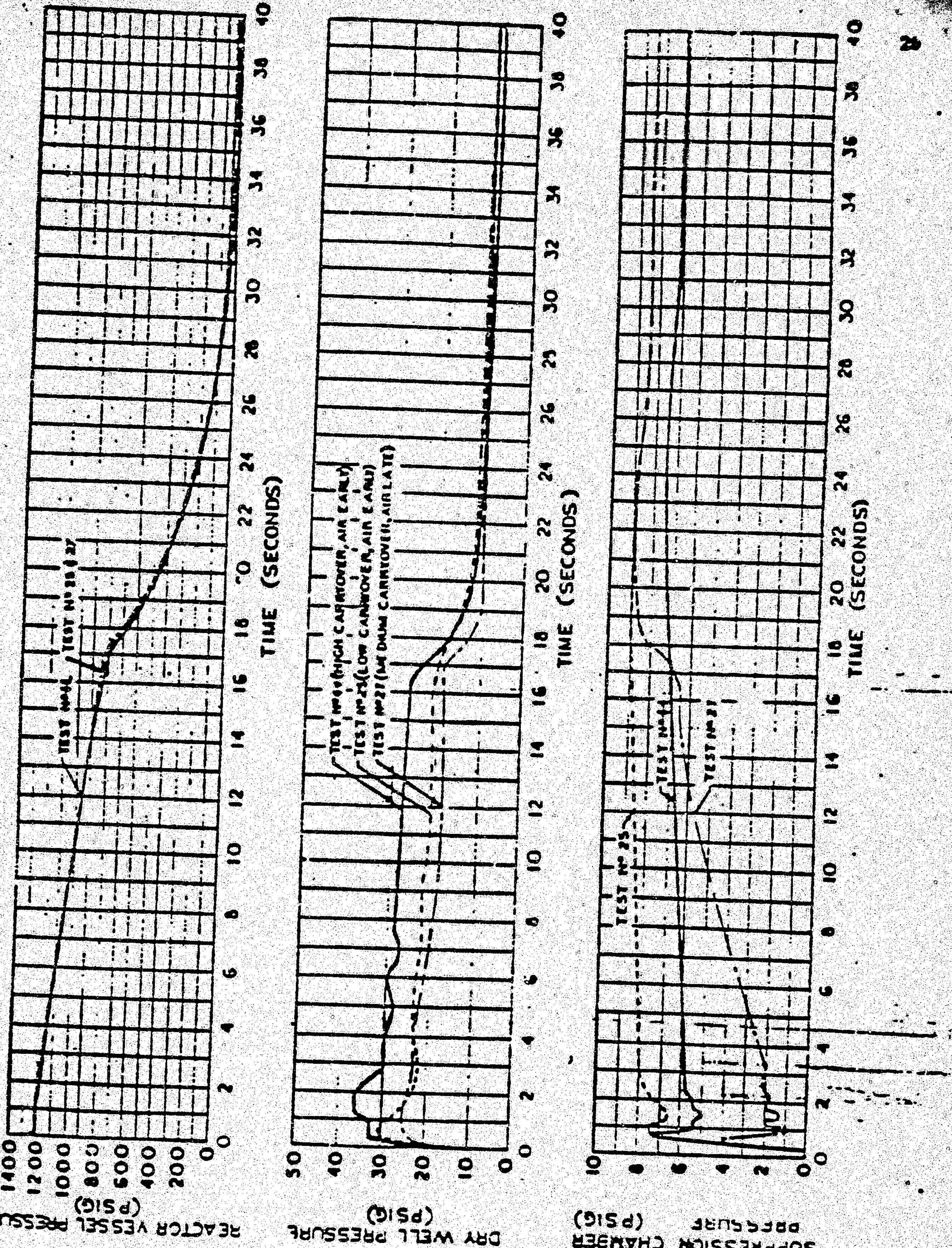


DETAIL OF
SUPPRESSION CHAMBER

APPENDIX IV, FIGURE 14

PHOTOGRAPH INSIDE
INSTRUMENT SHELTER

APPENDIX IV, FIGURE 15



PRESSURE-TIME CURVES FOR MAXIMUM CREDIBLE ACCIDENT

APPENDIX IV. FIGURE 16

Test No. 44 Arrgt. A - High carryover (75%), air leaves dry well early.
Test No. 27 Arrgt. B - Medium carryover (58%), air leaves dry well late.
Test No. 25 Arrgt. C - Low carryover (11%), air leaves dry well early.

The reactor vessel pressure-time curve is essentially the same for all tests. The pressure declines at a uniform rate while the water is being expelled from the reactor vessel. Then, approximately 17 seconds after initiation of the test, the pressure starts to decline more rapidly, indicating that the water has been completely expelled from the reactor vessel. At about 30 seconds it levels off at about the pressure of the dry well.

The dry well pressure curves for Tests Nos. 44 and 25 show high initial maximums accompanying earlier expulsion of air. Test No. 27, with later expulsion of the air, shows a lower maximum pressure due to the ram affect of the rupture jet discharging directly into the vent entrance. As shown in Table I, the maximum pressure of 36 psig for Test No. 44 was not exceeded on any test for Humboldt maximum credible operating accident conditions, regardless of dry well configurations.

The suppression chamber pressure curves vary with the time the air leaves the dry well. In Tests No. 44 and No. 25, the suppression chamber reaches about 80% of its maximum pressure during the first second or two. Test No. 27, on the other hand, shows the effect of the late air transfer. After the initial surge the suppression chamber pressure rises gradually, but eventually reaches the same maximum pressure as Test No. 25. The generally lower chamber pressures on Test No. 44 are accounted for by differences in initial temperatures. This indicates that the suppression chamber pressure rise is due primarily to the carryover of dry well air, and further that steam condensation is essentially instantaneous regardless of when the air comes over.

The Moss Landing tests show that dry well pressure at the end of the test is approximately 8 psig. This pressure is due to hot saturated steam (at about 235°F) remaining in the dry well, and corresponds to the suppression chamber pressure.

The oscillograph records of Tests Nos. 44, 25 and 27 are shown on Figure 17. These show, in addition to the pressure-time curves plotted in Figure 16, similar data for other locations in the test equipment.

b. Tabulation of Data for Different Tests

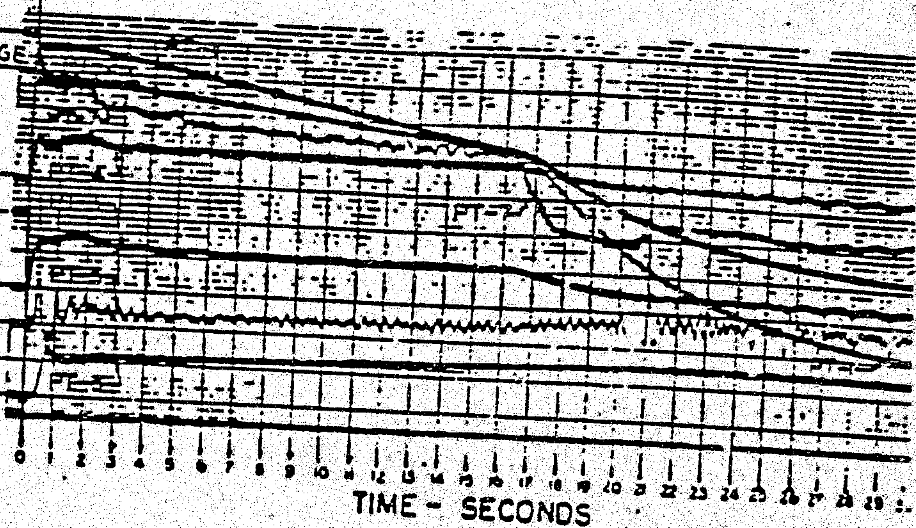
Table I gives a tabulation of data, including dry well and suppression chamber maximum pressures, for a number of different tests, including Tests No.s 44, 25 and 27, referred to above. Blanks appear where data were not obtained.

TABULATION OF REPRESENTATIVE TEST DATA

Test No.	Reactor Vessel: Initial Pressure Psig	Reactor Vessel: Water Volume Cu.ft.	Reactor Vessel: Water Discharge Time-sec.	Orifice: Diameter Inches	Orifice: % of NCOA	Dry Well: Internal Arrange-ment	Dry Well: Max. Press psig	Dry Well: EOT(1) Press psig	Dry Well: Residual Water cu.ft.	Dry Well: % Carry over	Suppression Chamber: Sub-merg ft.	Suppression Chamber: Max. press psig	Suppression Chamber: EOT(1) Press psig
13	1250	38.3	16.4	1.64	100	D	32	8	11.8	88	6	8.0	7.5
14	"	39.6	-	1.64	"	E	32	-	10.4	38	6	7.5	-
15	"	39.6	17.0	1.64	"	A	33	7	4.2	75	6	8.8	8.0
16	"	39.4	10.3	2.32	200	A	64	7	3.1	82	6	8.0	7.4
17	"	39.4	7.9	2.84	300	A	90	5	2.4	87	6	8.5	7.0
[illegible]	1250	38.7	16.9	1.64	100	A	33	5	3.6	77	-0.5(4)	6.5	6.0
4	"	38.2	16.8	"	100	A	32	8	2.7	84	6	9.3	8.5
5	"	39.2	16.8	"	"	C	32	8	15.1	11	6	8.9	8.0
6	"	39.2	16.7	"	"	C	33	8	-	-	6	8.5	7.5
7	"	38.7	17.0	"	"	B	25	5	7.1	58	6	8.9	7.0
8	1250	38.7	17.0	1.64	100	B	25	5	6.8	61	6	7.8	6.8
9	"	49.8	21.8	"	"	A	33	6	3.7	78	-1.5	7.3	6.8
0	"	48.9	21.2	"	"	A	35	10	4.3	74	-3.0	14.5	10.0
1	"	48.9	1860.	.14	0.7	A	6	5	26.2	0	6	6.0	6.0
2	"	39.2	10.7	2.32	200	A	59	5	3.7	78	-2.0	7.2	5.0
3	1250	38.7	6.0	3.28	400	A	128	9	1.9	89	6	8.5	7.5
4	"	38.7	81.0	.60	13	A	10	8	-	-	6	6.5	6.3
5	1000	39.2	31.7	1.10	45	A	18	8	9.0	48	6	8.0	7.0
6	1250	39.2	16.9	1.64	100	A	35	7	3.9	77	6	8.2	7.5
7	"	39.2	17.3	1.64	100	F(3)	33	7	4.8	72	6	8.0	7.0
8	1250	39.2	16.5	1.64	100	F	32	2	3.5	80	6	-	-
[illegible]	"	39.2	16.8	"	"	F	32	5	-	-	6	-	-
[illegible]	"	37.8	16.5	"	"	A	34	10	4.3	75	6	11.3(2)	10.8
[illegible]	"	38.7	16.9	"	"	A	34	15	4.2	75	6	12.5	9.5
[illegible]	"	38.7	16.7	"	"	A	37	13	-	-	6	13.6	12.6
[illegible]	1250	37.8	310.	.30	3	A	8	7	-	-	6	7.0	7.0
[illegible]	"	37.8	16.6	1.64	100	A	36	8	4.1	75	6	7.5	7.0
[illegible]	"	39.6	10.7	2.32	200	A	65	6	3.5	80	6	7.9	7.5

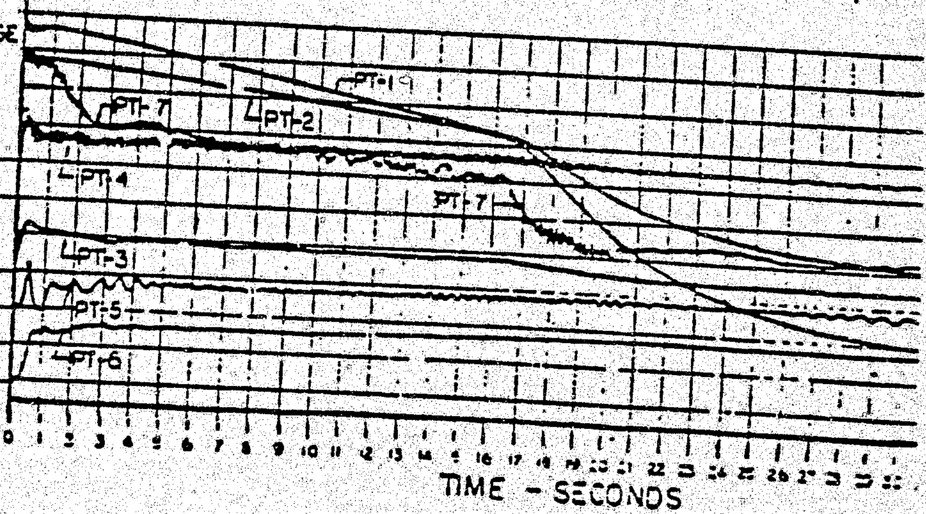
EOT Pressure is the end of test run pressure before cool-down.
Runs 40, 41 and 42 injected extra air into the dry well during the venting period.
Arrangement F is arrangement A without the vent entrance plate.
Negative submergence indicates that the vent was initially out of the water.

POINT NO.	TRANSDUCER LOCATION
PT-1	TOP OF REACTOR VESSEL
PT-2	REACTOR VESSEL DISCH. FLANGE
PT-4	VENT-LINE ENTRANCE
PT-7	DISCH. LINE D/S ORIFICE
PT-3	DRY WELL
PT-5	VENT LINE OUTLET
PT-6	SUPPRESSION CHAMBER



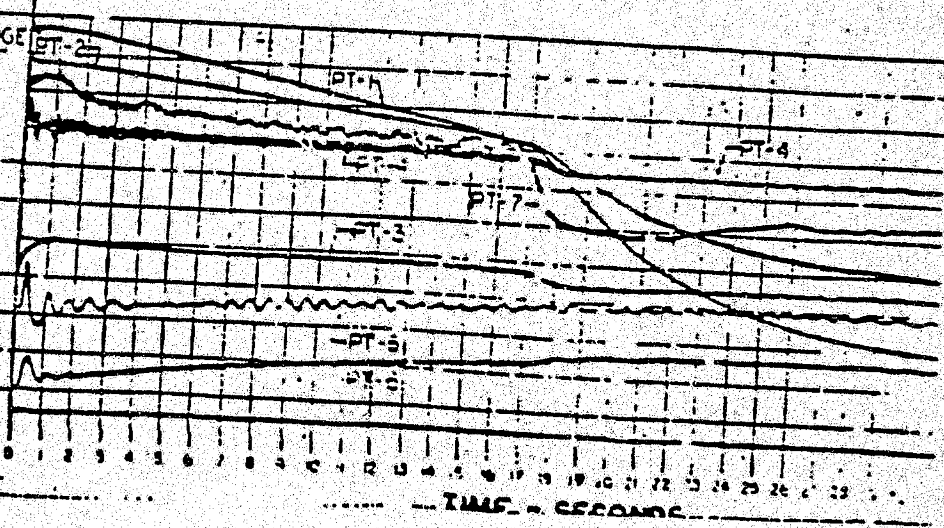
TEST NO. 44

POINT NO	TRANSDUCER LOCATION
PT-1	TOP OF REACTOR VESSEL
PT-2	REACTOR VESSEL DISCH. FLANGE
PT-4	VENT LINE ENTRANCE
PT-7	DISCH. LINE D/S ORIFICE
PT-3	DRY WELL
PT-5	VENT LINE OUTLET
PT-6	SUPPRESSION CHAMBER

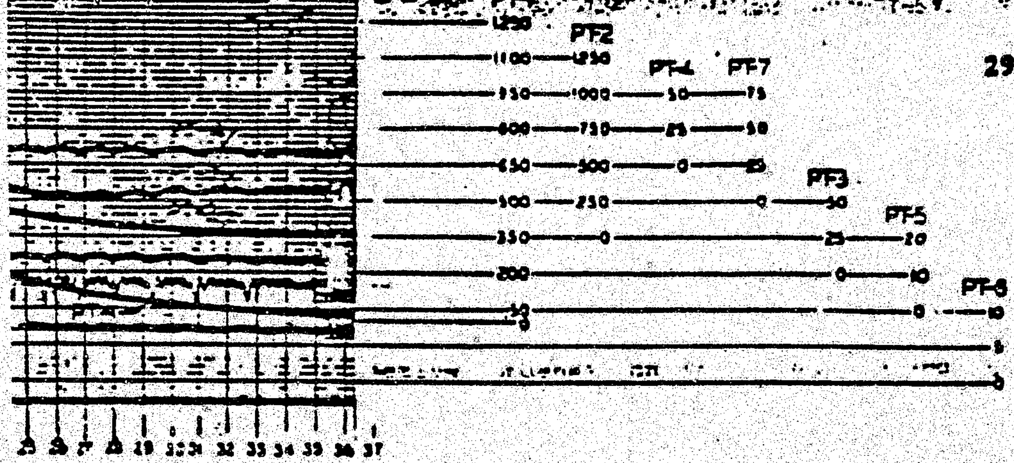


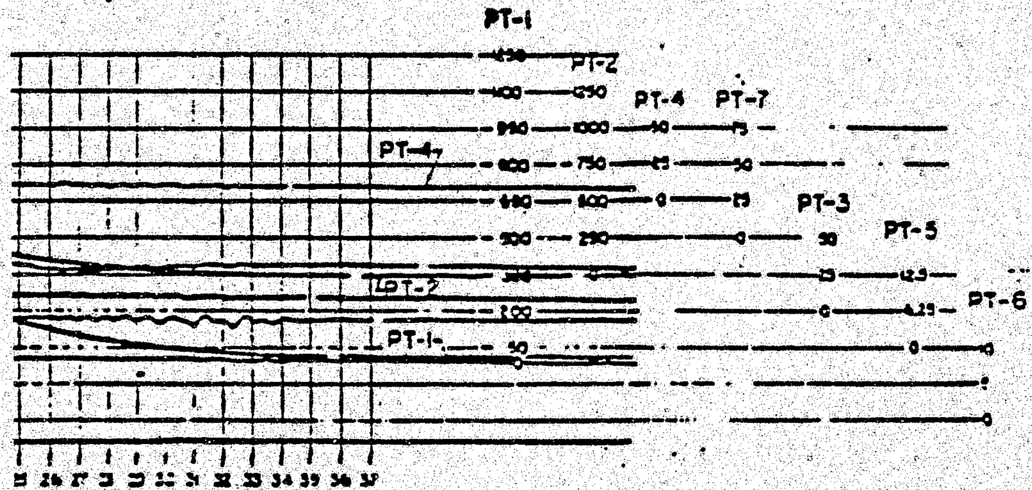
TEST NO 25

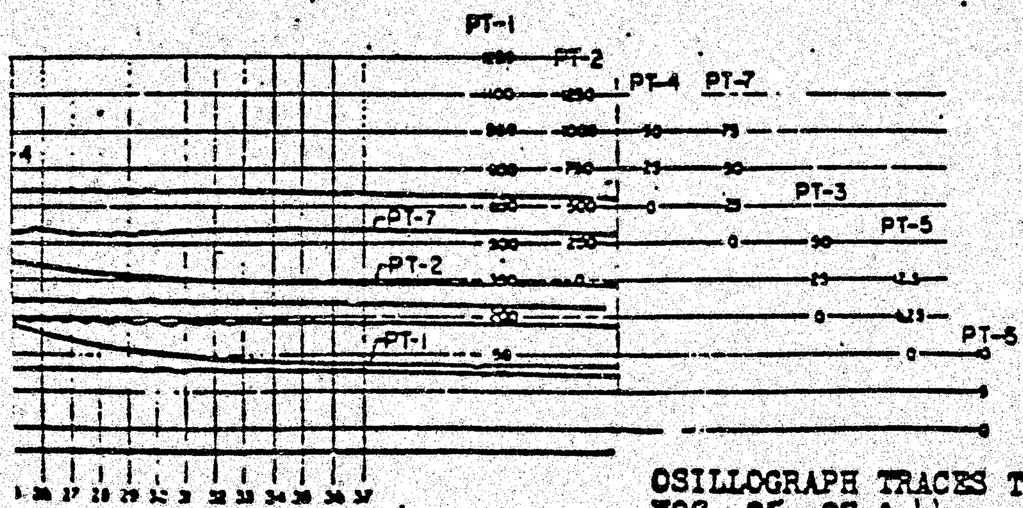
POINT NO	TRANSDUCER LOCATION
PT-1	TOP OF REACTOR VESSEL
PT-2	REACTOR VESSEL DISCH. FLANGE
PT-4	VENT LINE ENTRANCE
PT-7	DISCH LINE D/S ORIFICE
PT-3	DRY WELL
PT-5	VENT LINE OUTLET
PT-6	SUPPRESSION CHAMBER



TEST NO 27







OSILLOGRAPH TRACES TEST
NOS. 25, 27 & 44

APPENDIX IV, FIGURE 17

Tests preceding No. 13 were not representative of Humboldt conditions throughout. In some cases the location of all components of the instrumentation had not been finalized and their accuracy demonstrated. The data for all test runs is available, however, and much of it is useful.

In tests up to and including No. 12 the dry well was preheated by an electric cable to approximately Humboldt operating conditions. Tests showed that there were no noticeable effects from preheating and it was discontinued. The dry well temperature at the end of the tests listed in Table I was approximately 235°F.

c. Flow Rates Larger than Maximum Credible

In addition to the tests corresponding to the maximum credible operating accident, several runs were made with larger orifice areas. Test No. 16 was run with an area corresponding to twice that of the maximum credible accident. The maximum dry well pressure for this test was 64 psig, compared to the Humboldt design pressure of 72 psig. The maximum suppression chamber pressure for this test was 8 psig.

Test No. 17 was run with an area corresponding to three times the maximum credible accident The maximum dry well pressure was 90 psig and the maximum suppression chamber pressure was 8.5 psig.

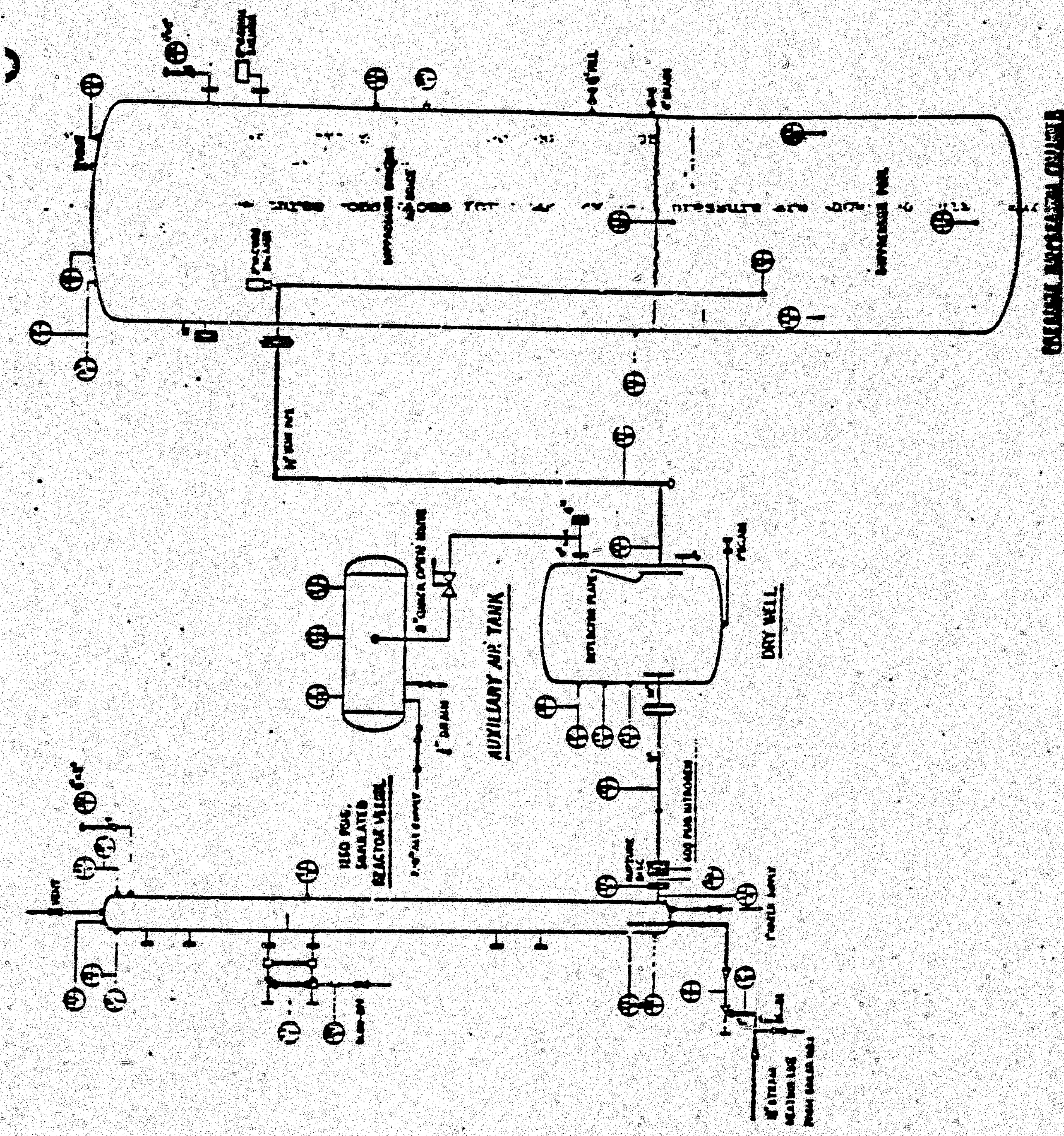
Test No. 33 was run with an area corresponding to four times the MCOA. The maximum dry well pressure was 128 psig and the maximum suppression chamber was also 8.5 psig. Test equipment limitations and not suppression chamber condensing capacity prevented runs at still higher flow rates. These test demonstrate the very rapid condensation even under these severe conditions and show that the maximum flow rate for complete condensation has not been reached.

d. Low Flow Rates

Condensation tests at low flow rates were qually successful. With an orifice diameter of .14 inches which corresponds to the flow from a one inch pipe break at Humboldt the maximum pressure in the suppression chamber was 6.0 psig. With an orifice diameter of 0.6 inches 6.5 psig was recorded in the suppression chamber. At low flow rates steam condensation takes place in the submerged vent pipe and the Humboldt design effectively provides for it.

e. Additional Air During Condensing Cycle

Figure 18 shows an auxiliary air tank which was added for test runs 40, 41 and 42. A quick opening valve was used to discharge air into the dry well from the auxiliary tank which was presaurized to 91 psig for test No. 41. The quantity of stored air was about equal to that of the dry well.



AUXILIARY AIR TANK
ARRANGEMENT

APPENDIX 17, FIGURE 18

With an orifice corresponding to the maximum credible operating accident and the valve from the auxiliary air tank still closed the test was initiated in the usual manner. Five and one half seconds later the quick opening valve was operated. The results are shown in Figure 19. The first part of the pressure suppression curve has the expected initial peak of almost 7 psig, then it dips briefly followed by a slow increase until 5½ seconds after the test has started. For the next 2 seconds it continues but at a greater rate during which about 60% of the air in the auxiliary tank flowed into the dry well. Additional air in the auxiliary tank then discharged more slowly as the dry well pressure fell.

The maximum suppression chamber pressure for this test, which from an air standpoint, is much more severe than Humboldt conditions, was 12.5 psig. This pressure is entirely accounted for by the air carryover indicating prompt steam condensation in spite of the extra flow of air during the condensation period.

f. Suppression Chamber Observations

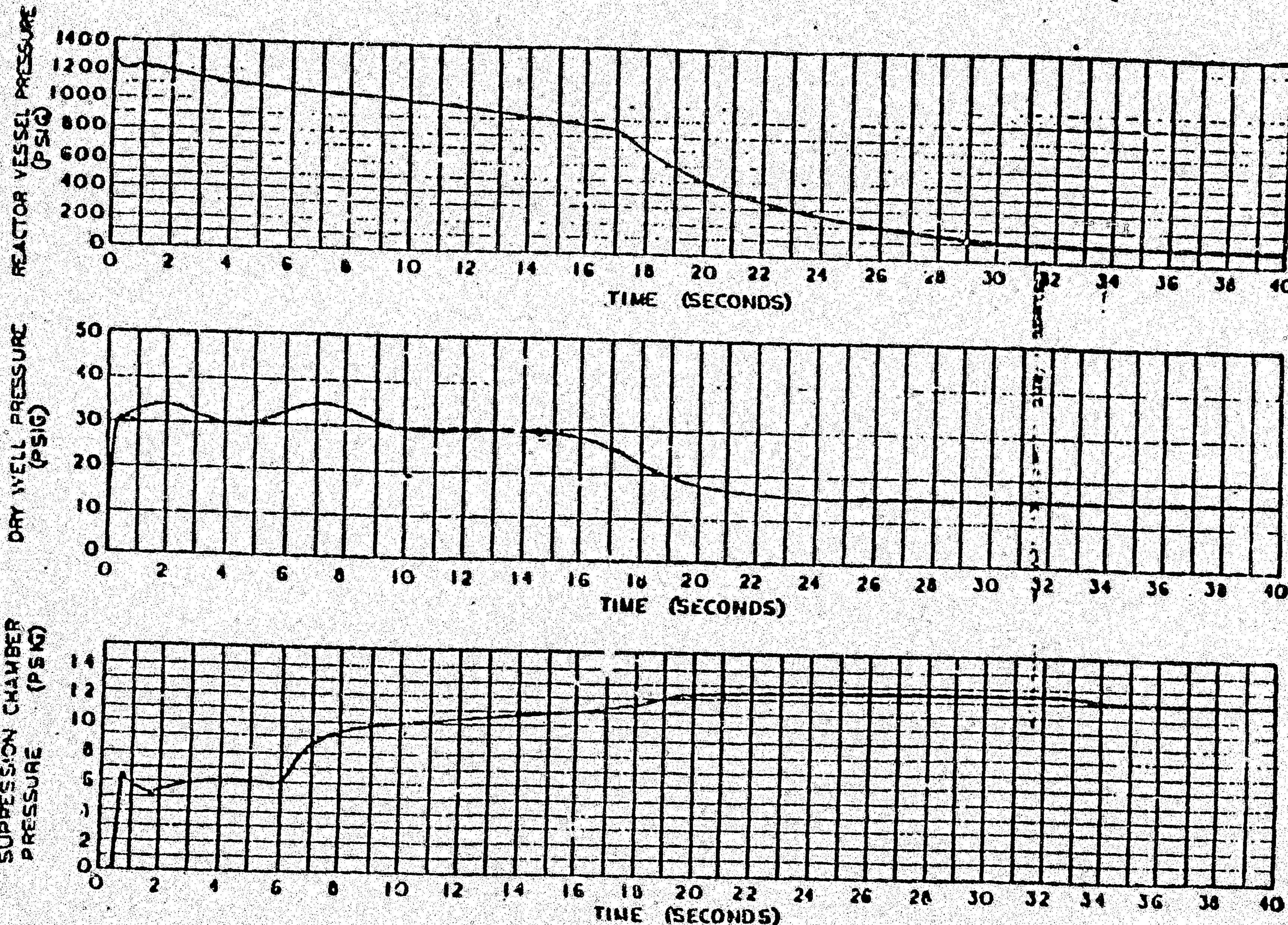
Although it was demonstrated that air carryover does not interfere with steam condensation, it is of interest to know what has been ovserved in the suppression chamber during some of the test runs.

It has been observed that the sudden release of air from the submerged vent pipe accelerates the pool level upward resulting in water being thrown high in the chamber. It is believed that the mass of water was moved upward by air in the pool. This water acting as a piston compresses the upper air in the suppression chamber until the air from the pool slips through the water which then falls back in the pool. The initial peak in the suppression chamber pressure-time curves is believed to be caused by this phenomena because no peak occurred in tests where the end of the vent pipe was located above the normal pool level.

g. Suppression Pool Temperature and Temperature Rise

In Test No. 26 with an orifice area corresponding to the maximum credible accident, the intitial pool water temperature was about 140°F. This was 60°F higher than the design value of 80°F. The maximum dry well pressure was 8.5 psig indicating satisfactory steam condensation at this pool temperature. No vibration of the suppression chamber occurred, probably due to its mass and the dampening effect of the earth.

The amount of temperature rise in pool water during a test does not appear to have any effect on operation of pressure suppression within the range tested. Average pool temperature rise for different tests corresponding to Humboldt conditions was 29°F; the calculated value is 33°F. The difference between the two is primarily due to heat absorption by air and metal in the test facility which was neglected in the calculation. To show that this heat absorption does not significantly affect pressure suppression, tests with 25% more water in the reactor vessel and 45% less water in the condensing pool resulted in pool temperature rises as high as 58°F without failure to promptly condense the steam.



PRESSURE TRACE WITH AUXILIARY AIR

h. Vent Submergence

Steam condensation in the suppression system is relatively insensitive to vent pipe submergence. Four tests were conducted with the pool water level lowered so that the discharge end of the vent pipe was completely out of the water at the start of the tests. Tests No. 20, 29, and 32 with the vent pipe initially ½, 1½, and 2 feet out of the water resulted in lower suppression chamber pressures than similar tests with normal submergence (because of the larger chamber air volume). Test No. 30 which was initiated with the vent pipe exposed three feet had effective condensation until near the end of the test. The low flow velocity at the end of the test was not sufficient to bring all of the steam down to the water and the chamber pressure rose an additional 6 psi. Apparently steam jetting into the pool will condense, but steam wafting from a vent uncovered several feet may escape. These tests showed that even if vent pipes should become exposed one or two feet during operation of the pressure suppression system, prompt condensation would occur.

5. Conclusions

The results obtained in the Moss Landing tests confirm the adequacy of dry well and suppression chamber design pressures. These tests further confirm that:

(1) Condensation of a steam jet in water under Humboldt Bay design conditions is rapid and complete.

(2) Uncovering of a vent pipe through agitation of the water in the pool would not affect condensation.

(3) Dry well air does not prevent prompt completion of steam condensation.

(4) The 14-inch vent pipes are adequate in size and number.

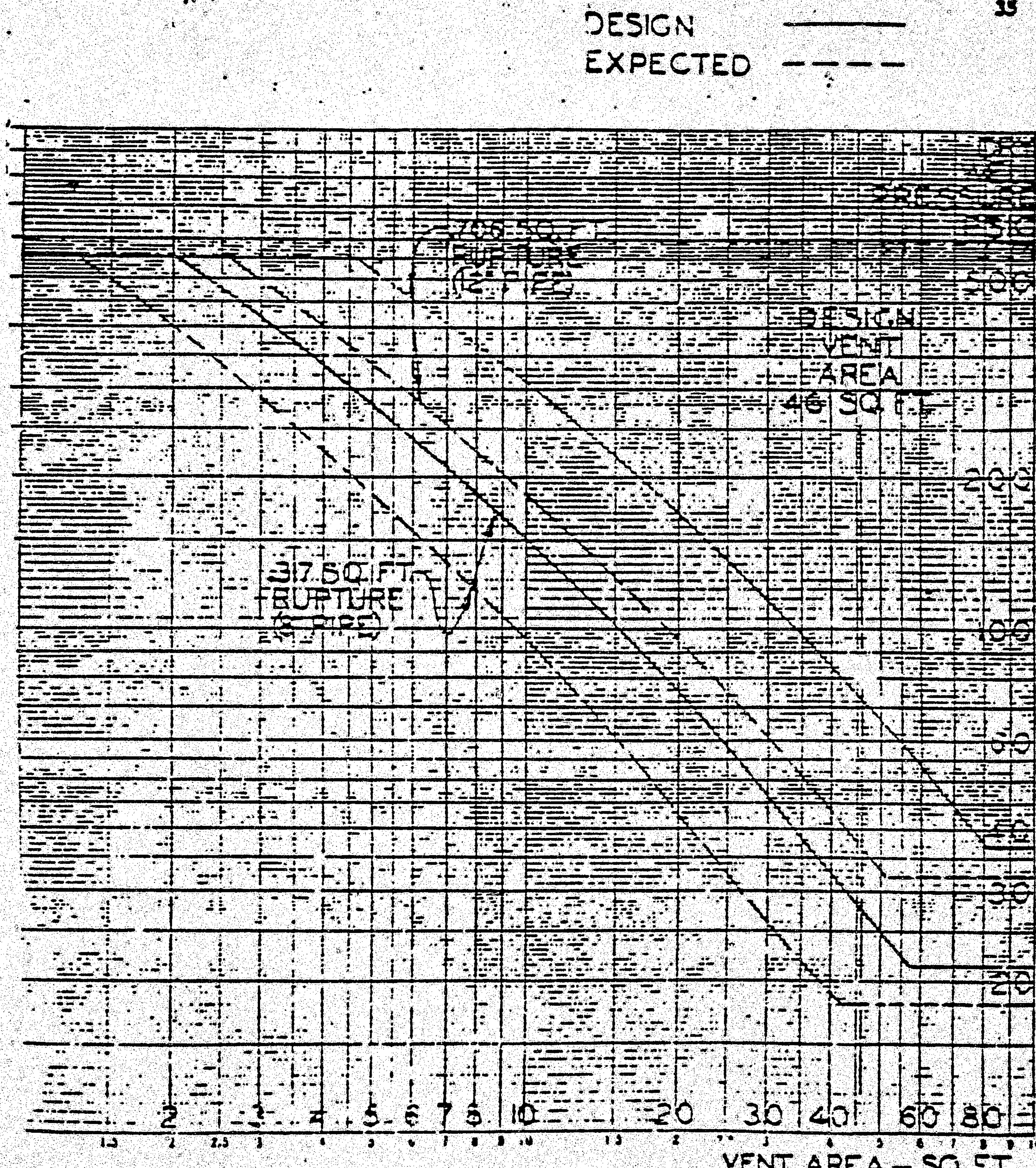
(5) Ample water is available for condensation.

(6) At least twice the maximum credible break area can be handled by the Humboldt pressure suppression system without exceeding its design conditions. Flows corresponding to four times the maximum credible accident break area can be condensed by the suppression chamber.

(7) The initial pool water temperature can be at least 60°F higher than the design value without affecting maximum dry well and suppression chamber pressures.

6. Design Parameters

The design parameters obtained from the intitial tests (page 13 of this Appendix) were in substantial agreement with the full scale tests. The addition of vent friction resistance to the design methods was checked by the full scale tests.



VENT AREA VERSUS
DRY WELL PRESSURE

APPENDIX IV. FIGURE 20

Figure 20 is a plot of vent area versus peak dry well pressure for different break areas calculated by the methods presented in Appendix V. The solid lines represent the design methods and the dashed lines represent the expected results calculated by the same methods except based on lower rupture flow rates as indicated by the full scale tests. Except for variable vent area, all other parameters correspond to the Humboldt design. Vent friction is included and friction per unit vent area has been kept the same as Humboldt.

The three conditions for peak dry well pressure are shown on this figure. Condition (1), low vent area, is shown as the upper left portion of each curve. Here venting
containment pressure
is the central slopi
resistance. Conditi
of the curves. Here
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APPENDIX V

PRESSURE SUPPRESSION ANALYSIS AND DESIGN METHOD

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APPENDIX V

PRESSURE SUPPRESSION ANALYSIS AND DESIGN METHOD

A. INTRODUCTION

This appendix describes the analysis and design method for pressure suppression. Included are design calculations for the Humboldt pressure suppression system. Test results and calculations for the full scale Moss Landing test facility are also included in parts of this appendix to verify the method or to show where margin exists.

B. PRESSURE SUPPRESSION SEQUENCE

The analysis and design of a pressure suppression system can be divided roughly into three general areas of analysis:

(1) Dynamic effects
(2) Venting period
(3) Cooldown period

The significant dynamic effects are the air shock wave, possible generation of missiles, and the rupture jet flow reaction and impact which continues during the venting period. These effects are considered in Section G of this Appendix.

The venting period begins with an initial pressure transient followed by a quasi-steady flow condition. It is during the venting period that the actual suppression of pressure occurs, and the dry well design depends primarily upon pressures which occur during this period.

After the available energy of the primary system is exhausted and venting stops, a temporary steady state condition exists at the beginning of the cooldown period. This condition is the primary consideration in determining the pressure rating of the suppression chamber.

C. RUPTURE FLOW RATE

Humboldt dry well was designed on the basis of a rupture flow rate corresponding to nonflashing water flow with a flow coefficient of .61 through a .705 sq ft opening (12 inch schedule 80 pipe). This value of the flow coefficient is generally accepted for the flow of cold water through a square edged orifice (Crane Technical Paper No. 410, page A-19). The flow rate determined in this manner was known to be conservative for flashing mixtures but in the absence of accurate information at the time the Humboldt pressure suppression system was designed it was believed to be the proper one to use.

Benjamin and Miller (page 419, Transactions ASME, July 1941) investigated the flow of saturated water through small square edged orifices. These ranged in size from .247 to .879 inch diameter in a 6 inch line. They found that the flow rate decreases with an increase in pressure. Extrapolating from their Figure 13 to the 800 to 1250 psi range would result in flow rates about 65% of that predicted by the orifice flow equation with a .61 coefficient.

Also, they found that flow rate decreases as orifice size increases and as initial temperature increases.

The average flow rate in the Moss Landing tests can be calculated from water discharge time. For every orifice size tested, the flow rate was significantly less than the value calculated on the basis of a .61 flow coefficient and liquid only in the orifice. A comparison between measured and calculated flows is shown in Table I.

TABLE I

Orifice Diameter, inches	Relation of Test to Calculated Flow Rate
.14	80%
.30	80
.60	80
1.10	62
1.64 (Max. credible)	54
2.10	46
2.32 (two times max. credible)	44
2.84 (three " " ")	41
3.28 (four " " ")	39

(One test with a 1.10 inch orifice was initiated at 1000 psig. The ratio of test to calculated flow for this test with the range of pressure during the water flow period of 1000 down to 720 psig was 70% or about 10% more than for the 1250 to 850 psig. This seems to be in line with the effect of pressure on flow coefficient observed by Benjamin and Miller.)

Sargent and Lundy conducted tests with full scale rupture sizes on a simulated reactor pressure vessel. These are reported in a written discussion by Alf Kolflat of Whelchel-Robbins ASME Paper 59-A-215. They had a very short length (less than one foot) of 12" Schedule 80 pipe in series with a 12" rupture disc. This was located on the bottom of a vessel about 3 feet inside diameter and 22 feet long.

S&L Test No. 2 showed that 8000 pounds of water emptied completely in 5½ seconds. The transient measurements indicate that most of the water was gone in 4 seconds. The value of the test flow rate can be bracketed by assuming at one extreme that the 8000 pounds empty in 5½ seconds and the other extreme that the 8000 pounds empty in 4 seconds. This results in a ratio of test to predicted flow of 28 to 31% allowing for pressure change during the interval. This set up is a close simulation of the Humboldt maximum credible rupture except the pressure at the beginning of the S&L test was 580 psig instead of 1250 psig at Humboldt which would appear to make Humboldt even more conservative.

From the foregoing different sources it would seem that there is considerable justification for using a flow rate of 54% of that predicted for liquid flow with a .61 flow coefficient for the Humboldt maximum credible rupture of the 12 inch pipe. With this value it will be shown that calculations and test results of dry well pressure are in substantial agreement. This introduces a considerable margin in the Humboldt dry well design pressure as will be shown.

D. STEADY FLOW DRY WELL PRESSURE

The maximum dry well pressure under the quasi-steady vent flow condition determines the design pressure for the Humboldt dry well because for the Humboldt design it is higher than the initial pressure buildup to expel the water from the vent pipes. The initial transient pressure buildup is analyzed in Section F.

1. Comparison of Calculations with Test Results

The dry well pressure under flow conditions is the sum of the pressure at the discharge end of the vent pipe (which may or may not be critical end-of-pipe pressure) and the pressure drop in the vent pipe. Vent pipe pressure drop depends on the amount of water carried into the vents with the steam. The zero carryover case can be calculated by conventional steam flow methods. The Humboldt design is based on the conservative assumption of 100% carryover; that is, all the unflashed water in the dry well is carried into the vent pipe with the steam, and additional flashing of water to steam occurs as the mixture flows down the vent pipe.

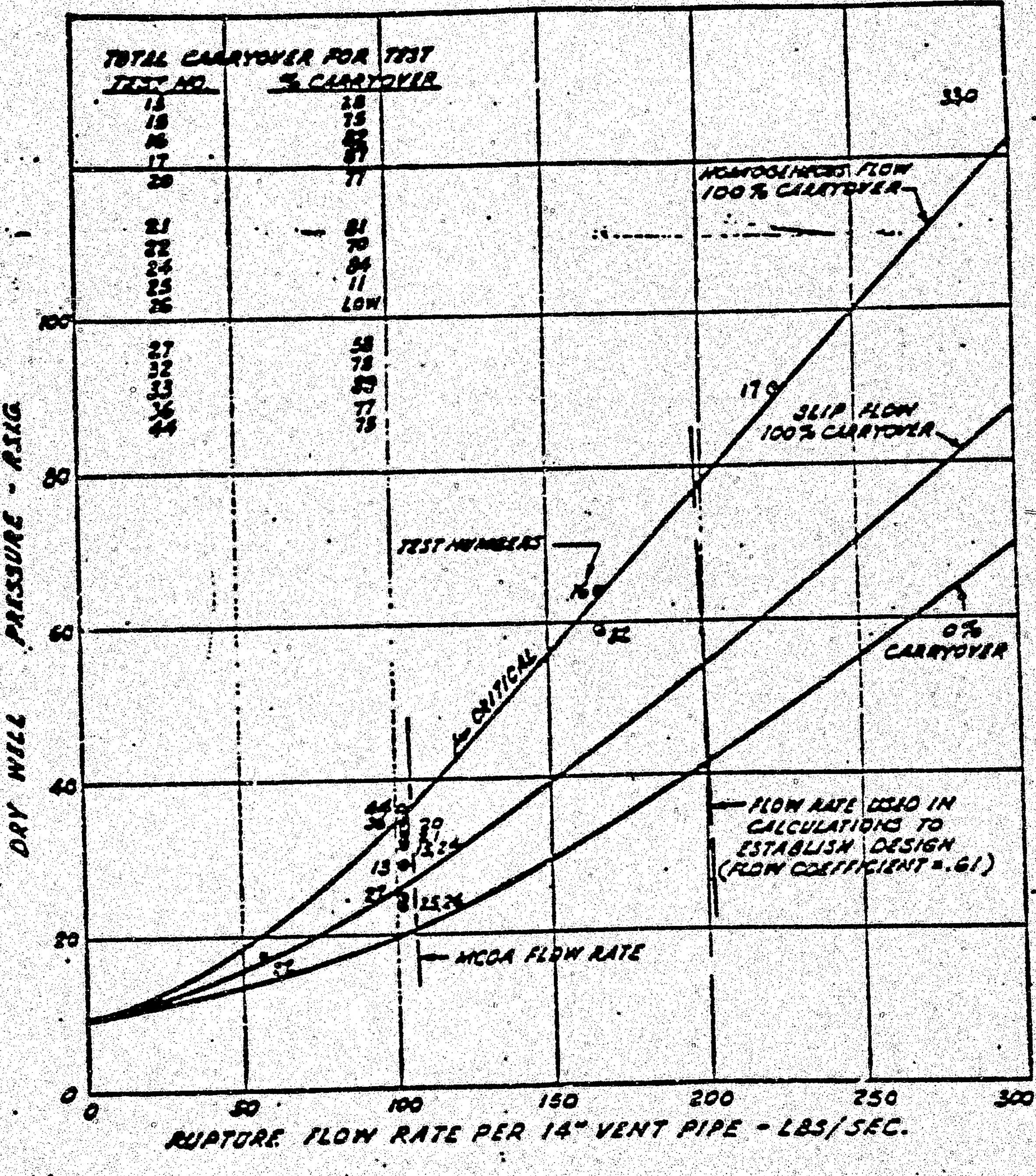
Two methods have been used for calculating vent pipe pressure drop with water carryover. The homogeneous method assumes that water and steam flow as a uniform mixture (fog flow). The second method assumes that the water slips in relation to the steam as the mixture flows down the pipes and this is called slip or Martinelli type flow.

Figure 1 shows a comparison of calculated maximum dry well pressure assuming (1) homogeneous flow with 100% carryover (2) Martinelli type flow with 100% carryover and (3) 0% carryover or all steam flow.

Some of the Moss Landing test points have also been plotted in this figure and they show reasonably close agreement with the homogeneous curve. Some of the low carryover test points shown on the curve are lower than the transient peak dry well pressure. (For instance, in test 25 on Figure 16 of Appendix IV the transient peak of 32 psig is followed by the quasi-steady flow pressure of 24 psig.) Figure 1 indicates that the homogeneous method used in establishing dry well design is a good and fairly accurate method for predicting steady flow dry well pressure.

2. Equivalent Length of Vent Pipes

In order to determine the friction component of vent pipe pressure drop it is necessary to determine the total friction factors or equivalent straight pipe for all the fittings. Following is a summation of the friction resistance equivalent length for Humboldt, and for comparison the same is given for the full scale Moss Landing test facility.



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COMPARISON OF CALCULATED DRY WELL PRESSURE
WITH TEST RESULTS
(DURING PSEUDO-STEADY VENT FLOW PERIOD)

APPENDIX V FIGURE 1

Equivalent Length of Humboldt Vent Piping

14" straight pipe:			39 ft
14" entrance (or branch flow tee):	.59 > K > .5, say .55		61
40" ring header:		L = 0 ft	
40" tee, divergent flow:	K = .7	L = 229	
40" straight pipe		L = 14	
40" entrance and entrance plate:	K = .841	L = 275	
Total of 40" piping:		518 ft x .281 =	145 ft
Total 14" equivalent length			245 ft

The 40" equivalent length is converted to 14" equivalent length by making use of the fact that the friction pressure drop in one foot of 40" pipe is equal to the friction pressure drop in 0.281 feet of 14" pipe carrying 1/8 the flow and having the same friction factor. For flashing flow in pipes 14" and larger, a friction factor of .01 has been used.

Equivalent Length of Test Vent Piping

14" entrance and entrance plate:	K = .841	93 ft
14" straight pipe:		51
14" S.R. ell:	.39 > K > .26, say .30	33
14" L.R. ell:	.26 > K > .13, say .20	22
14" tee, flow through branch:	K = .60	66
expansion joint:	K = .06	7
Total 14" equivalent length		272 ft

3. **Rupture Flow Rate Used in Design**

The rupture flow is determined from the orifice flow equation which can be written as follows:

$$w = 12CA_o \sqrt{2g\rho\Delta P}$$

where w = flow rate, lb per sec
C = flow coefficient
A_o = orifice area, sq ft
ρ = fluid density, lb per cu ft
g = gravity constant, 32.2 ft per sec^2
ΔP = difference in pressure before and after the orifice, psi.

For the design assumptions (no flashing in the orifice and a flow coefficient of .61) and assuming a dry well pressure in the range of 0 to 100 psig, the rupture flow would be in the range of:

$$w = 12 \times .61 \times .705 \sqrt{2 \times 32.2 \times 44.4 \, (1250 - 100 \text{ to } 0)}$$

$$= 9350 \text{ to } 9750 \text{ lb per sec}$$

Section C indicates that the actual flow rate would be only 54% of the value predicted by the method above, or about 5,000 lb per sec.

4. Dry Well Design Method (Homogeneous)

For 100% water carryover in the vents, the maximum pressure during the flow period occurs when flow out of the dry well via the vents equals the flow from the rupture into the dry well. To determine the design point, two trial vent flow rates can be assumed which straddle the 9350 to 9750 lb per sec range of rupture flow and a point of balance flow and dry well pressure are found.

The following Bernoulli type equation is the basis for determining pressure drop in a section of pipe carrying a homogeneous fluid with changing density:

$$dP = \frac{f\,dL}{D}\;\frac{u^2}{2g}\;\frac{\rho}{144} + \frac{\rho\,u\,du}{144g} + \frac{\rho\,dH}{144} \qquad (2)$$

where
- dP = incremental drop in pressure, psi
- f = friction factor
- dL = incremental equivalent length of pipe, ft.
- D = pipe inside diameter, ft
- u = mixture velocity in the pipe, ft per sec
- g = gravity constant, 32.2 ft per sec^2
- ρ = mixture density, lb per cu ft.
- dH = incremental increase in elevation, ft.

The first term on the right side of equation (2) is the familiar Darcy formula for friction; the second term on the right is the drop due to acceleration ($F = ma$); and the right hand term is the drop in elevation head.

Substituting $w/\rho A$ for u, where A is sq ft area of the pipe and w is the lbs per sec in the pipe, and rearranging gives:

$$\frac{f\,dL}{D} = \frac{\rho\,dP - \frac{\rho^2\,dH}{144}}{(w/A)^2/288g} + 2\,\frac{d\rho}{\rho} \qquad (3)$$

which is closely approximated by finite increments of length and pressure drop:

$$\frac{f\Delta L}{2D} = \frac{\rho\Delta P - \frac{\rho^2\Delta H}{144}}{(w/A)^2/144g} - \ln\frac{\rho_1}{\rho_2} \qquad (4)$$

where ρ_1 and ρ_2 are the densities at the beginning and end of a finite increment, ρ is the average of ρ_1 and ρ_2 and ΔH will be negative for downward flow in the vertical 14" pipe.*

---

* This equation is rearranged as follows so that ΔL can be found directly. The term $(w/A)^2/144g$ is represented by G.

$$\Delta L\left(1 + \frac{2D}{f}\,\frac{\rho^2\Delta H/\Delta L}{G144}\right) = \frac{2D}{f}\left(\frac{\rho\Delta P}{G} - \ln\frac{\rho_1}{\rho_2}\right) \qquad (4a)$$

For horizontal pipes $\Delta H/\Delta L = 0$ and the left side reduces to ΔL.

Equation (4) is sometimes referred to as the general flow equation. In order to solve it, the density of the mixture as a function of pressure, P, in the pipe must be found from the following relations:

$$h = h_{fo} - \frac{u^2}{2gJ} \tag{5}$$

$$X = (h - h_f)/h_{fg} \tag{6}$$

$$\rho = \frac{1}{v_f + X\, v_{fg}} \tag{7}$$

where h = enthalpy of the mixture in the pipe at pressure P, Btu per lb

h_{fo} = enthalpy of water in the reactor vessel (stagnation enthalpy of the flowing mixture)

J = 778 ft-lbs per Btu

X = steam quality (lb per sec steam per lb per sec total mixture)

h_f = enthalpy of saturated water in the pipe

h_{fg} = evaporation enthalpy in the pipe

v_f = specific volume of saturated water in the pipe, cu ft per lb

v_{fg} = evaporation specific volume in the pipe

Equation (5) represents the First Law of Thermodynamics as applied to adiabatic steady flow in a pipe. As a mathematical convenience, equation (5) is usually reduced to $h = h_{fo}$ in flow calculations, which infers a constant enthalpy flow process. (That is, an amount of heat is assumed to be added which just equals the kinetic energy of the mixture.) This simplification has been used in establishing Humboldt design and is justified because it has a small and conservative effect as will be shown.

A "critical end-of-pipe" condition could exist if the back pressure on the vents (chamber pressure plus submergence) is lower than critical end-of-pipe pressure. For a given flow rate, the critical pressure occurs when all the incremental pressure drop is required to accelerate the mixture, and dL and dH would have to be zero. Equation (3) would reduce to the following at the critical end-of-pipe.

$$\rho^2 \frac{dP}{d\rho} = \frac{(w/A)^2}{144g} \tag{8}$$

The right hand term has been previously referred to as G.

The specific volume of the mixture, v, is the reciprocal of density, ρ, and equation (8) can be written as follows:

$$\frac{dP}{dv} = \frac{(w/A)^2}{144g} = G \tag{9}$$

or

$$\frac{\Delta P}{\Delta v} = G \tag{10}$$

On the assumption that air would have transferred from the dry well to the suppression chamber before the quasi-steady flow condition occurred, the suppression chamber pressure would be about 7 psig and back pressure on the vents would be about 24 psia, which corresponds to the sum of chamber pressure, static head of water, and atmospheric.

Table II is a tabulation of flow properties at given pressures in the vent pipe using equations (6) and (7) and letting $h = h_{f_o} = 580.6$ Btu per lb.

[illegible]rsection which [illegible] psig.

The last column is the higher pressure less the lower pressure of two consecutive lines in the table divided by the corresponding difference in specific volumes.

Table II

FLOW PROPERTIES FOR HOMOGENEOUS FLOW

P psia	Quality X	ρ	$\Delta P/\Delta v$
24	.3926	.1502	
27	.3875	.1698	3.891
30	.3827	.1897	4.854
33	.3783	.2098	5.952
36	.3742	.2300	7.143
39	.3703	.2505	8.451
42	.3666	.2711	9.868
45	.3632	.2920	11.41
48	.3598	.3130	13.04
51	.3567	.3341	14.85
54	.3536	.3555	
57	.3507	.3771	
60	.3479	.3987	
63	.3452	.4207	
66	.3426	.4427	
69	.3400	.4649	
72	.3376	.4873	
75	.3352	.5099	

For trial vent flows these calculations will use 8616 and 9768 lbs/hr instead of rounded numbers because these particular values were used in establishing the design. Areas of 14 and 40 inch pipes are .958 and 8.40 sq. ft.

	Trial A	Trial B
Total vent flow, lb per sec	8616	9768
40" vent pipe flow, lb per sec	1436	1628
14" vent pipe flow, lb per sec	179.5	203.5
For 14" pipes: $(w/A)^2/144g = G =$	7.57	9.73
For 40" pipes: $(w/A)^2/144g = G =$	6.30	8.10

Comparing the values of G for 14 inch pipes with values of delta P/delta v in Table II as indicated by equation (10) it is apparent that critical end-of-line pressure should exist for homogeneous flow at about 35 psia for Trial A and about 40 psia for Trial B. By plotting the values of delta P/delta v as a function of P, the critical points are found to be more nearly 35.5 and 40.2 psia.

For calculation the vent piping is divided into four pieces as follows. Values of K correspond to the values given on page 6.

(1) Vertical 14" pipes

$\Delta H/\Delta L = -1$ and total L = 39 ft
$f = .01$
$D = 1.1042$ ft $A = .958$ sq ft
P_5 = pressure at 14" pipe inlet
P_6 = pressure at 14" pipe discharge

(2) Entrance of 14" pipe at ring header

$\Delta H = 0$ $D = 1.1042$ ft
total $L = KD/f = .55 \times 1.1042/.01 = 60.7$ ft
P_4 = pressure just inside ring header at most remote 14" pipe

(The ring header is omitted because the K values in the header after each branch are small and negative indicating a slight rise in pressure rather than a drop. P_3, the pressure at the beginning of the ring header, is assumed equal to P_4).

(3) Discharge of 40" entrance pipe (tee with divergent flow)

$\Delta H = 0$ $D = 3.27$ ft
total $L = .7 \times 3.27/.01 = 229$ ft
P_2 = pressure at 40" pipe discharge

(4) Entrance plate and 40" entrance pipe

$\Delta H = 0$ $D = 3.27$ ft
total $L = (.136 + .115 + .590)\ 3.27/.01 + 14 = 289$ ft
P_1 = pressure at entrance plate

TABLE III

CALCULATED RESULTS FOR HOMOGENEOUS FLOW

Trial A -- Solutions of equation (4a) for delta L:

Starting with the critical end pressure and taking various delta P's the delta L's are found and then summed for each length of pipe.

Piece	ΔP, psi	$\frac{\rho \Delta P}{G}$	$\ln \frac{\rho_1}{\rho_2}$	$\frac{2D}{f} \frac{\rho^2 \Delta E/\Delta L}{G\ 144}$	ΔL, ft
		P6 = 35.5 psia (Critical end pressure)			
(1)	36 to 35.5	.0151	.0131	.0105	0.4
	39 to 36	.0953	.0854	.0117	2.2
	42 to 39	.1034	.0790	.0138	5.5
	45 to 42	.1115	.0743	.0161	8.4
	48 to 45	.1200	.0695	.0185	11.4
	51 to 48	.1280	.0652	.0212	14.2(x.780)
		P_5 = 50.4 psia (sum of delta L = 39 ft)			
(2)	51 to 48	.1280	.0652	---	13.9(x.220)
	54 to 51	.1367	.0621	---	16.5
	57 to 54	.1451	.0590	---	19.0
	60 to 57	.1534	.0558	---	21.5
	63 to 60	.1620	.0537	---	23.9(x.025)
		P_4 = 60.2 psia (sum of delta L = 60.7 ft)			
(3)	63 to 60	.1950	.0537	---	92.5(x.975)
	66 to 63	.2053	.0510	---	101.0
	69 to 66	.2160	.0489	---	109.3(x.346)
		P_2 = 67.0 psia (sum of delta L = 229 ft)			
(4)	69 to 66	.2160	.0489	---	109.3(x.654)
	72 to 69	.2270	.0470	---	117.8
	75 to 72	.2374	.0453	---	125.6(x.794)
		P_1 = 74.4 psia (sum of delta L = 289 ft)			

The mixture velocity at the entrance plate (flow area = 8.8 sq ft) is:

$$u = w/\rho A = 1436/.506 \times 8.8 = 323 \text{ ft per sec}$$

The velocity head at this point is:

$$\Delta P = \rho u^2/2g\ 144 = .506 \times 323^2/64.4 \times 144 = 5.7 \text{ psi}$$

Allowing for the velocity head in the dry well (about 0.8 psi), the dry well pressure is 79.3 psia or 64.6 psig.

TABLE III (Continued)

Trial B — Solutions of equation (4a) for delta L:

Piece	ΔP, psi	$\rho \frac{\Delta P}{G}$	$\ln \frac{\rho_1}{\rho_2}$	$\frac{2D}{f}\ \rho^2 \frac{\Delta H/\Delta L}{G 144}$	ΔL, ft
		P_6 = 40.2 psia (critical end pressure)			
(1)	42 to 40.2	.0451	.0457	.0111	0.8
	45 to 42	.0868	.0743	.0125	2.8
	48 to 45	.0933	.0695	.0144	5.3
	51 to 48	.0998	.0652	.0165	7.7
	54 to 51	.1064	.0621	.0187	10.0
	57 to 54	.1130	.0590	.0212	12.3
	60 to 57	.1197	.0558	.0236	14.5(x.007)
		P_5 = 57.2 psia (sum of delta L = 39 ft)			
(2)	60 to 57	.1197	.0558	---	14.1(x.993)
	63 to 60	.1262	.0535	---	15.1
	66 to 63	.1330	.0510	---	18.1
	69 to 66	.1397	.0489	---	20.1(x.625)
		P_4 = 67.9 psia (sum of delta L = 60.7 ft)			
(3)	69 to 66	.1680	.0489	---	78.0(x.375)
	72 to 69	.1765	.0470	---	84.7
	75 to 72	.1846	.0453	---	91.1
	78 to 75	.1930	.0437	---	97.6(x.246)
		P_2 = 75.7 psia (sum of delta L = 223 ft)			
(4)	78 to 75	.1930	.0437	---	97.6(x.754)
	81 to 78	.2014	.0421	---	104.2
	84 to 81	.2100	.0407	---	110.8
	87 to 84	.2187	.0395	---	117.2(x.003)
		P_1 = 84.1 psia (sum of delta L = 289)			

The mixture velocity at the entrance plate is

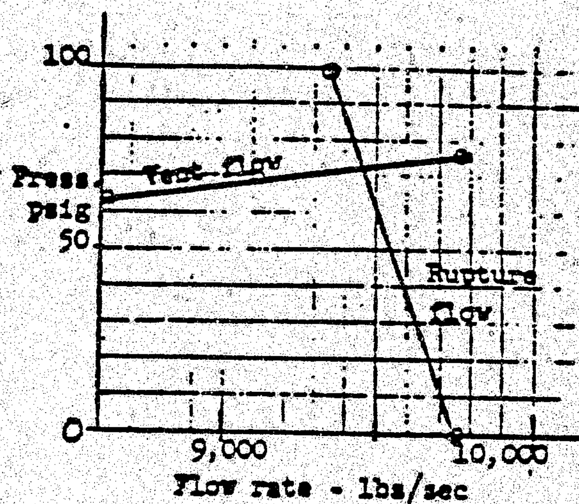
$$u = v/\rho A = 1628/.580 \times 8.8 = 319 \text{ ft per sec}$$

The velocity head at this point is:

$$\Delta P = \rho u^2/2g\ 144 = .580 \times 319^2/64.4 \times 144 = 6.4 \text{ psi}$$

Allowing for the velocity head in the dry well (about 0.8 psi), the dry well pressure is 89.7 psia or 75.0 psig.

The accompanying graph shows calculated rupture flow and total vent flow as a function of dry well pressure. The intersection of the two lines is the balanced flow condition. The dry well design pressure is the pressure at the intersection which is 72.0 psig.

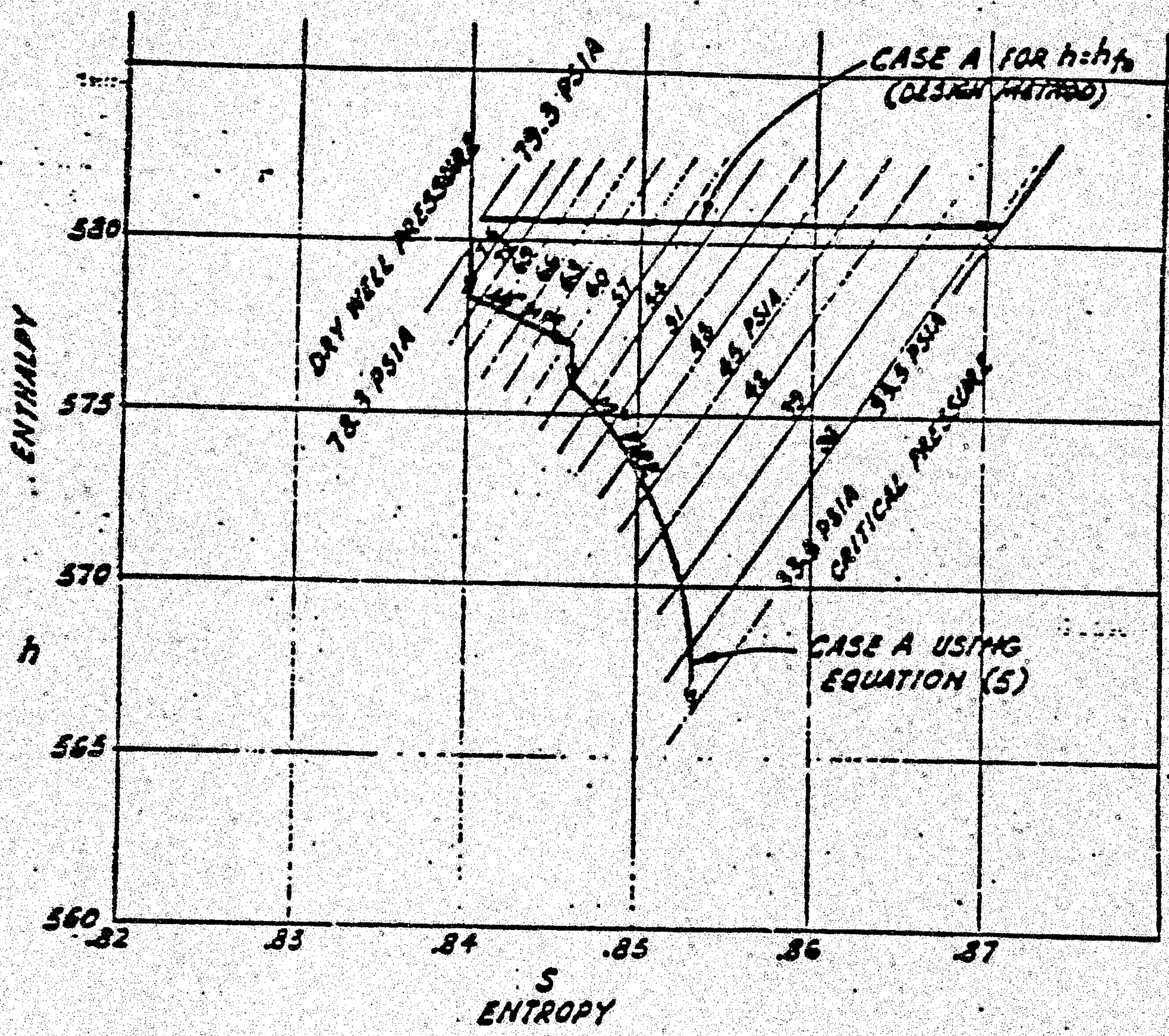


To show that the simplifying assumption of constant enthalpy is justifiable, calculations have been made for Trial A using equation (5). These results are compared with the Trial A calculations of Table III on an enthalpy-entropy (Mollier) diagram on Figure 2. This shows that using equation (5) without simplification results in a slight (6%) improvement in predicting critical end-of-pipe pressure, but that dry well pressure is only about 1% different from the design method. In the full scale tests with vent flow corresponding to design vent flow (which required a 2-times MCOA rupture area), there was no evidence of existence of critical pressure. This indicates that the homogeneous assumption is pessimistic in predicting critical pressure, and slip flow at the vent discharge must be significant. However, the effect of significant slip at the discharge would tend to make the homogeneous method slightly conservative and the error in dry well pressure would not be significant.

Calculations similar to those shown on Table III were made for different rupture flow rates to obtain the upper line on Figure 1. The Moss Landing tests indicate that the rupture flow rate could be only 54% of the value used to establish Humboldt design. Figure 1 shows that the corresponding dry well pressure is about 35 psig or half the design value. This introduces a margin of approximately 100% in the Humboldt dry well design.

5. Dry Well Pressure by the Slip Method (Modified Martinelli)

The dry well pressure has been calculated by the Modified Martinelli method for comparison with the homogeneous calculations.



ENTHALPY - ENTHROPY CHART
SHOWING EFFECT OF EQUATION (5) ON VENT PIPE PRESSURE DROP

APPENDIX V FIGURE 2

Curve 5 of the Martinelli-Nelson paper* is the basic empirical relationship for determining steam void fraction with Martinelli type (slip) flow. S. Levy** developed a theoretical relationship which gives good agreement with Curve 5 of Martinelli-Nelson. The theoretical relationship enhances extrapolation and will be used in these calculations.

These calculations assume 100% carryover and assume Martinelli type flow in fittings as well as in straight pipe. This assumption is preferable to assuming Martinelli flow in straight lengths only and homogeneous flow in fittings. The latter assumption would involve extending the Martinelli method into areas which it was not developed to handle, and it is questionable whether the conventionally calculated equivalent lengths (or K factors) of fittings could be used.

The steam quality, X, and void fraction, a, can be determined at various pressures from the following relations:

$$X = (580.6 - h_f)/h_{fg} \tag{11}$$

$$\frac{(1-X)^2}{1-a} + \frac{X^2}{a}\,\frac{\rho_f}{\rho_g} - \frac{1}{2}\,\frac{(1-X)^2}{(1-a)^2} - \frac{1}{2} = 0 \tag{12}$$

where
- X = steam quality (lb per sec steam per lb per sec total flow)
- a = void fraction (portion of pipe area occupied by steam)
- 580.6 = enthalpy of saturated water in reactor vessel, Btu per lb
- h_f = enthalpy of saturated water at pressure P , Btu per lb
- h_{fg} = evaporation enthalpy at pressure P., Btu per lb
- ρ_f = density of saturated water at pressure P , lb per cu ft
- ρ_g = density of saturated steam at pressure P., lb per cu ft

Equation (12) is equation (16) of Levy's paper and is equivalent to Curve 5 of Martinelli-Nelson. (There is a typographical error in equation (16) of Levy's paper which omitted the term $(-\frac{1}{2})$).

The average density of the mixture at pressure P is given by the following equation:

$$\rho = \rho_f\,(1-a) + \rho_g a \tag{13}$$

The air could have transferred from the dry well to the suppression chamber before the quasi-steady state vent flow condition occurred. Therefore, the suppression chamber pressure will be about 7 psig, and it will be shown that a critical flow condition at the end of the vent pipe will not occur with Martinelli type flow in the vents.

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* R. C. Martinelli and D. B. Nelson, "Prediction of Pressure Drop during Forced Circulation Boiling of Water", Trans. ASME, 1948.

** S. Levy, "Steam Slip -- Theoretical Prediction from Momentum Model" ASME Paper 59-HT-15.

PECO EXHIBIT SL-4

PH 3-14-86
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PENNSYLVANIA PUBLIC UTILITY
COMMISSION v. PHILADELPHIA ELECTRIC
COMPANY, DOCKET NO. R-850152

BODEGA BAY UNIT I

PRELIMINARY HAZARDS SUMMARY REPORT
APPENDIX I

March 13, 1986

PACIFIC GAS AND ELECTRIC COMPANY

BODEGA BAY ATOMIC PARK

UNIT NUMBER 1

EXHIBIT C

PRELIMINARY

HAZARDS SUMMARY REPORT

APPENDIX I

PRESSURE SUPPRESSION TEST PROGRAM

Submitted to the United States Atomic Energy Commission
December 28, 1962
Docket No. 50-205

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LIST OF FIGURES

PRESSURE SUPPRESSION TEST PROGRAM

A. Introduction and Summary

A pressure suppression test facility was constructed and operated at the Company's Moss Landing Power Plant in the summer of 1962 to proof test the Bodega Bay containment design. In general, the facility was similar to that for Humboldt Bay and used some of the same hardware. The facility consisted of a full scale 1/112th segment of the Bodega suppression chamber with one full scale vent pipe and one model reactor and dry well vessels with about 1/112th the volumes of the Bodega design.

The results of the test program confirmed the adequacy of the Bodega pressure suppression containment design. Principal results were:

1. Condensation of steam in the suppression pool was rapid and complete under conditions far more severe than those associated with the maximum credible operating accident (MCOA); the suppression chamber pressure did not exceed 30 psig on any test.

2. The highest dry well pressure obtained on any test simulating the design basis accident for the dry well was 52 psig. (The design basis accident is described in Section C of Appendix II.)

3. Variations in suppression pool water level, pool temperature, dry well temperature; subcooling of the reactor vessel water; and use of nozzles with rounded entrances as well as sharp-edged orifices had only minor effects on the operation of pressure suppression.

B. Purpose of Test Program

Although basically the design of the Bodega pressure suppression containment is quite similar to that for Humboldt Bay, there are some significant differences.

1. The vent pipe size is 24" diameter rather than 14" diameter.

2. There are some appreciable differences in the geometry of the two systems.

3. In Bodega the suppression pool is being "worked harder", that is, the energy input rate to the pool per unit water volume is higher by a substantial factor than at Humboldt. Test results showed this factor to be about 2.4 under MCOA condition.

Because of these differences, testing of the Bodega containment design to demonstrate satisfactory performance was considered to be desirable.

C. Description of Test Facility and Relationship to Bodega Bay Design

1. General Arrangement

Figures 1 and 2 show the general arrangement of the test facility. The Bodega reactor vessel was simulated by a model reactor vessel which was mounted on the side of the partially buried tank containing the suppression chamber. The other large tank shown simulates the Bodega dry well vessel and is connected to the suppression chamber by a vent line simulating one of the Bodega vent lines.

A rupture accident is simulated by breaking rupture disks mounted on the flange at the bottom of the reactor vessel and letting the water and steam in the reactor vessel discharge into the dry well through an orifice or nozzle.

2. Reactor Vessel

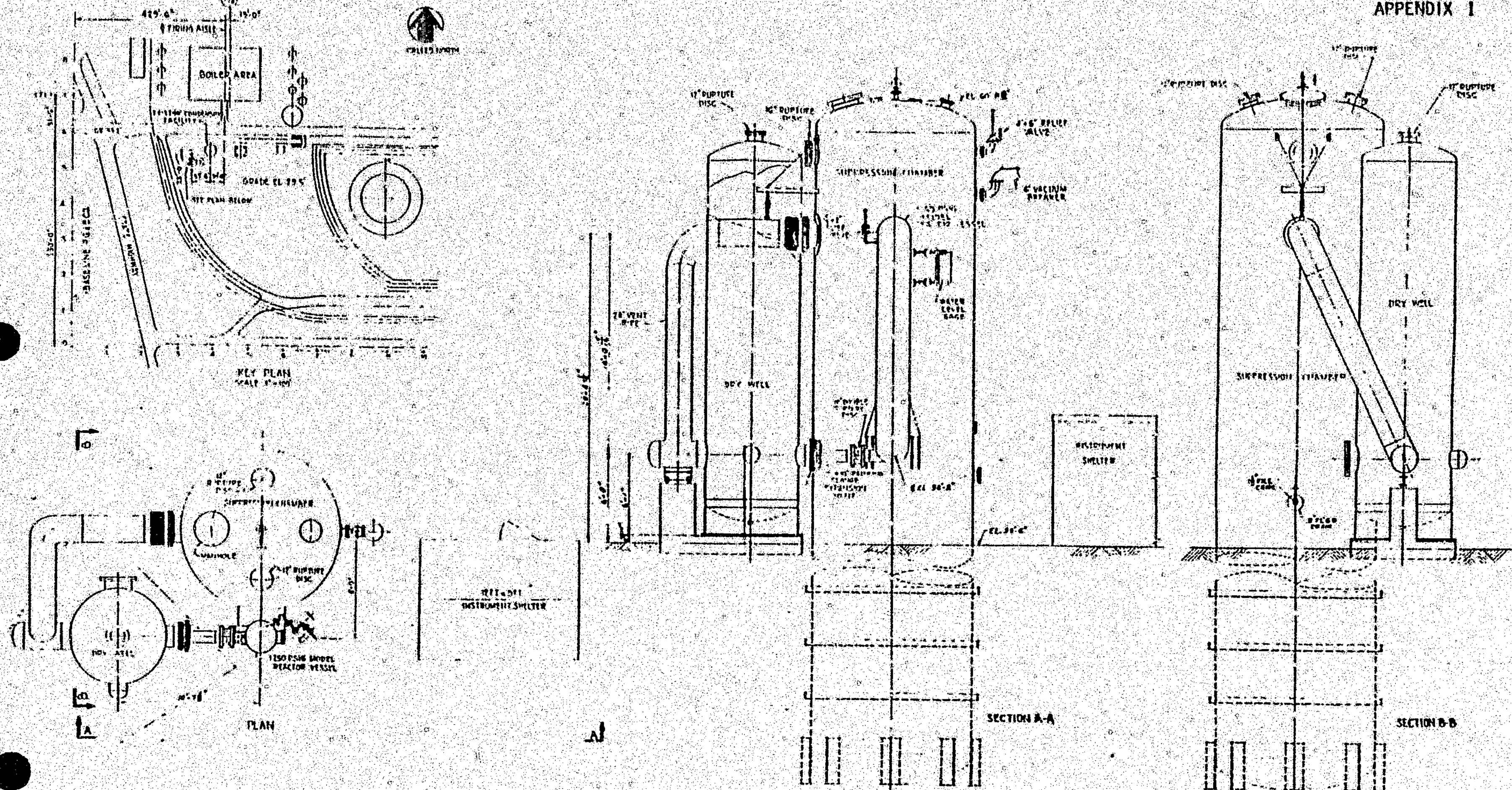
The test reactor vessel was 27" I.D., 21 ft. long over heads and was designed for 1,250 psig internal pressure in accordance with the ASME code. It was made from carbon steel. It was equipped with a 10" discharge nozzle and other nozzles for instrumentation, venting, draining, and admission of heating steam. Its contained volume was 80 $ft.^3$ and most tests were run with about 54 $ft.^3$ of water in it. This latter figure is about 1/112th of the water in the Bodega primary system.

3. Dry Well

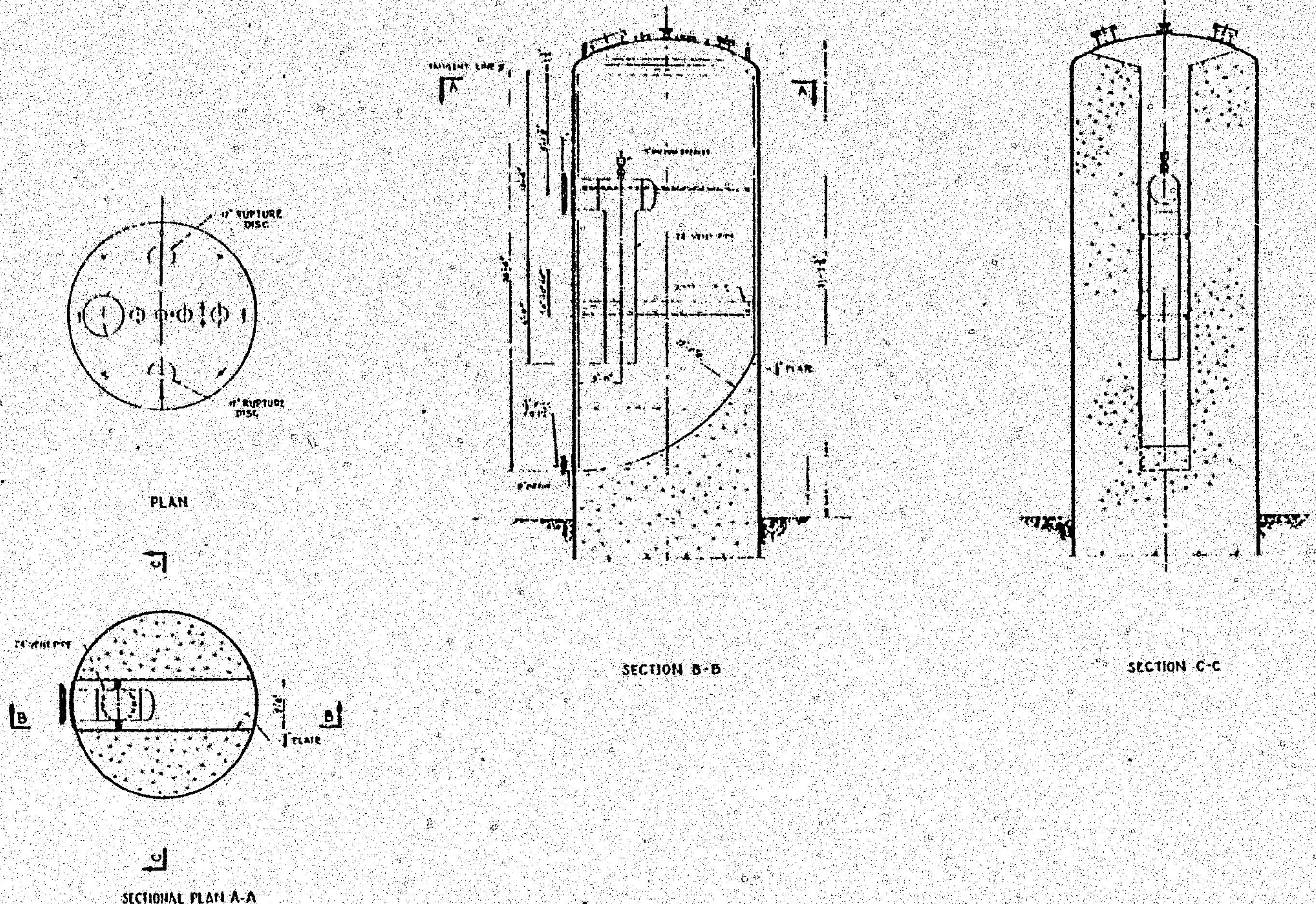
The test dry well was 85" I.D., and 29 ft. long over heads. It was designed for an internal pressure of 150 psig in accordance with the ASME code and was made from carbon steel. It has a 20" inlet nozzle for the steam-water mixture from the reactor vessel and a

OVERSIZE

DOCUMENTS



ARRANGEMENT OF TEST FACILITY
FIGURE 2



SUPPRESSION CHAMBER ARRANGEMENT
FIGURE 3

.24" discharge nozzle connected to a vent line leading to the suppression chamber. Other nozzles were provided for instrumentation, venting and draining, and over-pressure relief.

The dry well had a contained volume of 1,100 ft.^{3}, somewhat larger than 1/112th of the present Bodega design. This was done in case future design changes increase the size of the Bodega dry well. Actually, dry well size does not affect dry well pressure unless the vent piping is exceedingly small. Dry well size does effect suppression chamber pressure because the air in the dry well is largely or entirely forced into the suppression chamber.

After the first few tests a deflector plate was installed in front of the discharge opening in the dry well to cause greater dispersion of the steam-water jet coming through the inlet nozzle and increase air carry over to the suppression chamber.

4. Suppression Chamber

The suppression chamber was contained within a vessel 12 ft. I.D. and 49 ft. long over heads. The vessel was partly buried in the ground and was the same vessel used for this purpose during the Humboldt tests.

The suppression chamber itself, see fig. 3, was a section of the vessel described above, extending across the diameter of the vessel from the top down to 28 ft. below the top. The sides of the section were parallel and 3 -8" apart. The bottom was curved with a 13' radius. The sides and bottom of the suppression chamber were steel plates. The rest of the vessel was filled with concrete.

The suppression chamber contained 670 ft.^{3} of air and 339 ft.^{3} of water. The air volume was 1/112th of the total air volume of the Bodega design and the water volume was 1/112th of the effective water volume of the Bodega design at the time the test facility was designed. At present the Bodega suppression chamber is somewhat larger than it was when the test facility was designed, providing more water and air space. Therefore the test suppression chamber was being worked harder than the Bodega chamber would be. The water volume in the test facility was that amount of water associated with one vent pipe in the Bodega design. Part of the water in the Bodega suppression chamber lies under the eight vent pipes coming from the dry well and for test purposes was not considered available for condensing steam. However, all of the air in the suppression chamber was considered available for absorbing the pressure rise following an accident.

5. Vent Pipe

The vent pipe was a 24 in. pipe connecting the dry well to the suppression pool. Its length was 45 ft. and it contained two tees and one ell. The normal submergence of the end of the vent pipe below the pool surface was four feet.

The diameter, length, and flow resistance induced by fittings were the same as that of an average vent pipe in the Bodega design.

6. Rupture Disk Assembly

A 10 in. double rupture disk assembly together with an orifice or nozzle was used to simulate a pipe break. The assembly was located immediately downstream from the test orifice or nozzle which in turn was located on the discharge flange of the test reactor vessel. The assembly contained two rupture disks, each rated to break at 900 psig at 575° F. With test reactor pressure at 1,250 psig and a gas pressure of about 650 psig between the two disks, the disks would hold. When the gas pressure was vented off, the upstream disk would break, followed closely by the downstream disk and the test would be under way.

Figure 4 is a photograph showing the rupture disk assembly mounted in place. An orifice plate may be observed on the upstream side of the assembly.

Figure 5 shows rupture disks before and after testing and also the orifice plate simulating the MCOA rupture size.

7. Orifices and Nozzles

The simulated break size for each test was established by an orifice or nozzle which was machined in a steel plate mounted on the discharge flange of the test reactor vessel. Orifices were machined with a sharp, square upstream edge, a 1/16 inch flat section, and the downstream face beveled at an angle of 60° from the axis. An orifice with a diameter of 3.24 in. diameter simulated the break area associated with the MCOA, that is, it had an area 1/112th of that of a double ended break of 28 in. O.D. pipe. Other orifice sizes tested had diameters of .905 in., 1.66 in., 2.48 in., 3.74 in., 4.50 in., and 5.12 in.

The nozzles were made with an entrance radius and straight throat length each equal to 1/4 of the throat diameter, which approximates the shape of the entrance to the Bodega reactor vessel recirculation outlet lines. Nozzle diameters tested were .905 in., 2.00 in., 3.24 in., and 4.50 in. Additionally, the 3.24 in. nozzle was tested with the straight throat section removed, leaving the entrance radius intact.

OVERSIZE DOCUMENTS

8. Instrumentation

Figure 6 shows schematically the arrangement of the instrumentation and figure 7 shows some of the instrumentation in the control shack. The pressure transducers were of the strain gage type and their output was recorded by a light beam oscillograph. The temperature transducers were thermocouples and their outputs were recorded by a multi-point strip chart recorder. Certain principal pressures were also indicated by gages in the control shack.

D. Method of Performing Tests

The following were the principal steps in performing a test on this facility:

1. The desired test orifice or nozzle together with the double rupture disk assembly would be installed on the discharge flange of the reactor vessel.

2. The suppression chamber would be filled with water to the desired level. The desired level would be established by overflow from a nozzle in the side of the suppression chamber.

3. The reactor vessel would be filled with water until the desired level was established in the gage glass on the reactor vessel.

4. Saturated steam from a 1,400 psig source would be admitted through a nozzle in the bottom of the reactor vessel to heat the water. During this time the reactor vessel would be vented to remove any non-condensible gases and drained to maintain the desired water level. Heating up to a pressure of 1,250 psig would take about one to one and a half hours.

4a. Several tests were run with the water in the reactor vessel initially subcooled. For these tests (Tests Nos. 39 to 45) subcooling was accomplished by heating up to a pressure less than 1250 psig corresponding to the water temperature desired. The reactor was then pressurized to 1250 psig by admitting steam from the 1400 psig steam source to a nozzle in the top of the reactor vessel

5. When reactor pressure reached about 600 psig, nitrogen would be admitted to the space between the two rupture disks to maintain a pressure in this space equal to about one half of reactor pressure. As reactor pressure increased, pressure in this space would be increased.

6. When the desired test pressure in the reactor vessel was reached, all vents, drains, and other openings in the reactor vessel, dry well vessel, and suppression chamber would be closed or checked to be closed.

7. Final instrument checks would be made and the oscillograph started (the temperature recorder would already have been running during the reactor vessel heating part of the test.)

7a. Several tests were run to determine the effect of pre-purging the dry well. For these tests (Tests Nos. 39 to 45) steam was admitted to the dry well by opening a valve connected to the 1400 psig steam source. This steam was permitted to flow until the suppression chamber pressure stopped rising, indicating air purging had ceased. This valve was then closed and the test continued normally.

8. The nitrogen pressure between the rupture disks would be vented off and the disks would break. During the blowdown from the reactor vessel, gages in the control shack indicating dry well and suppression chamber pressures would be observed as a check on the results recorded by the oscillograph.

9. The various test vessels would then be vented and preparations for the next test would begin. Frequently these would include recalibration of the pressure transducers which proved to be quite stable throughout the test program.

E. Test Program

1. Start-up Tests

Table I is a listing of principal test parameters and results for each test. The first six tests were made with the suppression chamber open and with increasing orifice size and reactor water volume to check out the adequacy of the test facility itself. During this series of tests two failures occurred which caused the loss of most test results. During test No. 1 a gasket on the upstream face of the orifice plate broke when the blowdown was initiated; this was probably due to improper tightening of the bolts on this joint. During test No. 4 the rupture disks broke unexpectedly for unknown reasons. These were the only incidents causing serious loss of test data during the entire test program.

2. Tests Without Deflector Plate

Tests Nos. 7 through 13 were run with the suppression chamber closed and with increasing orifice sizes up to 5.12 in. diameter. Water levels in the reactor vessel and in the suppression chamber were at normal levels corresponding to Bodega design. Operation was satisfactory for all tests, however suppression chamber pressure was lower than predicted. This was caused by the failure to expel all the air from the dry well during the test. To increase the air carry over, a deflector plate was installed in front of the outlet nozzle in the dry well so that the jet of steam-water mixtures coming from the inlet nozzle would be dispersed, causing more air to be purged from the dry well.

3. Tests With Deflector Plate

Tests Nos. 14 and subsequent were run with the deflector plate installed. Test results indicated most (about 90%) of the dry well air was being forced into the suppression chamber.

These tests were run with a variety of orifices and nozzles. Most tests were run with all reactor water at or very near saturation temperatu[re] the suppression chamber filled to a depth of 11 feet, and the dry well

TABLE I
PRESSURE SUPPRESSION TEST RESULTS FOR BODEGA

		Reactor Vessel			Orifice		Dry Well		Suppression Chamber	
	Test No.	Press range dur. blow-down, psig	Water Volume cu ft	Water Discharge Time-sec	Diameter inches	Water Blowdown lb/sec-ft^2	Max Press psig	Temp°F before /max	Max Press psig	Temp°F before /max
without deflector plate	1	1225/	14.4		0.453			70/	open	66/
	2	1250/	13.7		0.906			65/	"	67/
	3	1250/1050	13.7	5.3	1.66	7700		61/	"	71/
	4	1250/			3.24			64/	"	67/
	5	1250/725	54.8	7.97	3.24	5200	23	71/	"	79/
	6	1250/845	54.8	20.9	1.66	7450	9	65/	open	70/
	7	1250/845	54.8	20.6	1.66	7590	28	78/257	25	93/128
	8	1250/810	54.8	11.2	2.48	6270	24	70/240	21	79/111
	9	1250/720	54.8	7.65	3.24	5440	21	75/229	17	84/110
	10	1250/660	54.5	6.34	3.74	4910	31	62/222	19	76/108
	11	1250/630	54.5	4.72	4.50	4560	42	70/220	18	85/117
	12	1250/590	54.8	3.80	5.12	4420	55	83/230	18	101/131
	13	1250/900	54.8	53.2	0.906	9830	26	61/265	24	71/90
with deflector plate	14	1250/750	53.5	7.45	3.24	5440	37	65/248	27	70/97
	15	1250/710	52.8	5.99	3.74	5030	40	70/242	26	71/100
	16	1250/570	54.5	3.80	5.12	4400	63	77/239	26	82/110
	17	1250/730	54.5	7.88	3.24	5240	37	68/252	28	77/[illegible]
	18	1250/900	54.1	46.8	0.906n	11040	30	68/270	27	68/82
	19	1250/720	54.1	7.68	3.24n	5350	36	60/247	28	63/92
	20	1250/620	52.8	4.24	4.50n	4930	52	72/239	27	74/97
	21	1250/700	54.5	7.41	3.24sn	5580	36	65/240	27	70/96
	22a	1210/740	54.8	7.65	3.24	5440	36	75/254	28	76/101
	23b	1250/690	54.8	8.20	3.24	5070	37	70/253	29	75/97
	24c	1250/705	54.5	7.37	3.24sn	5600	34	150/261	22	80/110
	25d	1250/700	54.5	7.94	3.24	5210	36	67/251	28	70/102
	26	1250/830	54.8	15.75	2.00n	6850	33	65/268	29	81/102
	27	1250/690	54.8	7.47	3.24	5560	38	66/245	29	88/112

a. approximately 16°F initial subcooling b. 5 ft submergence c. Preheated dry well d. 3 ft submergence
n. nozzle sn. shortened nozzle

PRESSURE SUPPRESSION TEST RESULTS FOR BODEGA (Page 2)

	Reactor Vessel					Dry Well		Suppression Chamber	
Test No.	Press range dur. blow-down, psig	Initial Subcooling, °F	Water Volume cu ft	Water Discharge Time-sec	Orifice Diameter inches	Max Press psig	Temp °F before /max	Press, psig before /max	Temp °F before /max
39	1250/640	25	54.8	7.45	3.24 sn	41	265/269	14/19	120/163
40	1250/600	35	54.8	7.57	3.24 sn	52	255/271	22/30	65/113
41	1250/510	50	54.8	6.9	3.24 sn	50	260/274	22/28	77/121
42	1250/620	50	54.8	7.35	3.24	47	262/277	23/29	89/126
43	1250/610	70	54.8	7.14	3.24 sn	49	262/275	23/29	90/121
44	1250/460	90	54.8	6.8	3.24 sn	50	260/269	20/25	83/126
45	1250/450	110	54.8	6.2	3.24 sn	47	257/271	20/28	74/112

sn. shortened nozzle

at ambient temperature. However, of the first 27 tests, four tests had different initial conditions, as follows:

A. Test 22 was run with the water in the bottom of the reactor vessel subcooled 16°F below saturation, in order to simulate subcooled water in the bottom of the Bodega reactor vessel.

B. Tests 23 and 25 were run with the suppression pool level respectively raised and lowered one foot to simulate possible variations in the Bodega pool level.

C. Test 24 was run with the dry well preheated to 150°F by injecting steam into it before running the test. This was to simulate the maximum expected dry well temperature at Bodega during plant operation. (Note: During the Humboldt testing, the dry well was heated to 150°F for several tests. No significant difference was observed between tests with a heated and an unheated dry well.)

During all the tests run with the deflector plate, suppression chamber pressure never exceeded 30 psig and the dry well pressure never exceeded 38 psig with an orifice or nozzle of MCOA size except for the special tests described in the next paragraph. The highest dry well pressure achieved was 63 psig with an orifice 250% of MCOA break size. The special initial conditions for the four special tests described above had no significant effect except for the test with a preheated dry well where pressures were lower due to loss of air in the dry well because of preheating it with steam.

- Tests With Dry Well Pre-Purging and Subcooled Water in the Reactor Vessel

Tests No. 39 through 45 were run with an initial dry well pre-purge and with reactor vessel water subcooling ranging from 25 to 110°F. Results of these tests are tabulated on Page 2 of Table I (page 7a). The maximum dry well pressure does not follow any particular trend. However, the test with 90°F subcooling gave the highest pressure drop in the vent piping. The variations in dry well pressure appear to result principally from differing amounts of air in the suppression chamber resulting from variations in the initial temperature.

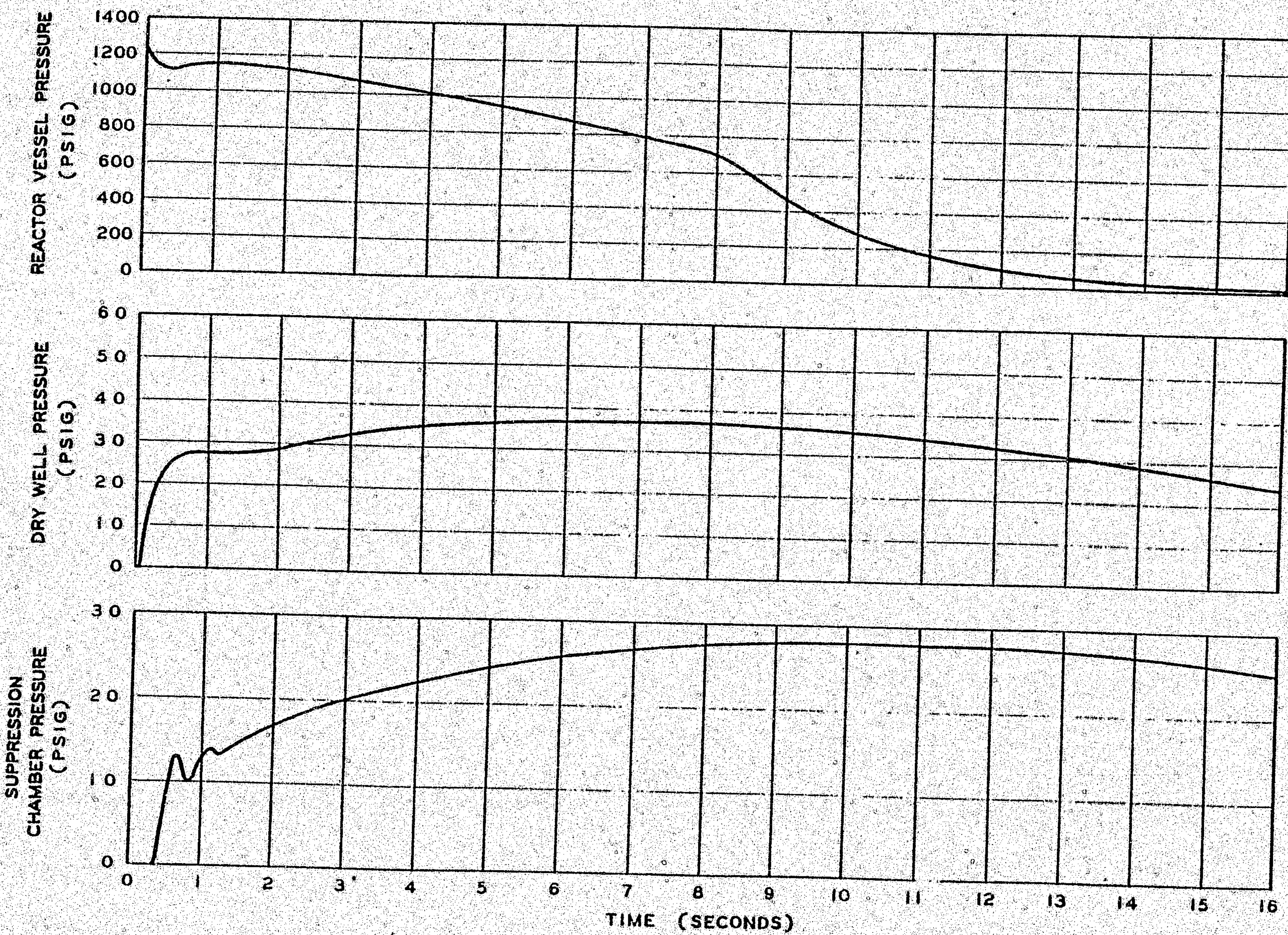
- Representative Pressure Traces

Figures 8, 9, 10, and 11 show the pressure traces for six tests. Figures 8, 9 and 11 are principal pressures for two tests replotted for clarity. Figure 10 is a reproduction of the oscillograph records for three other tests. Some characteristics of these traces are discussed in the next section.

Test Results

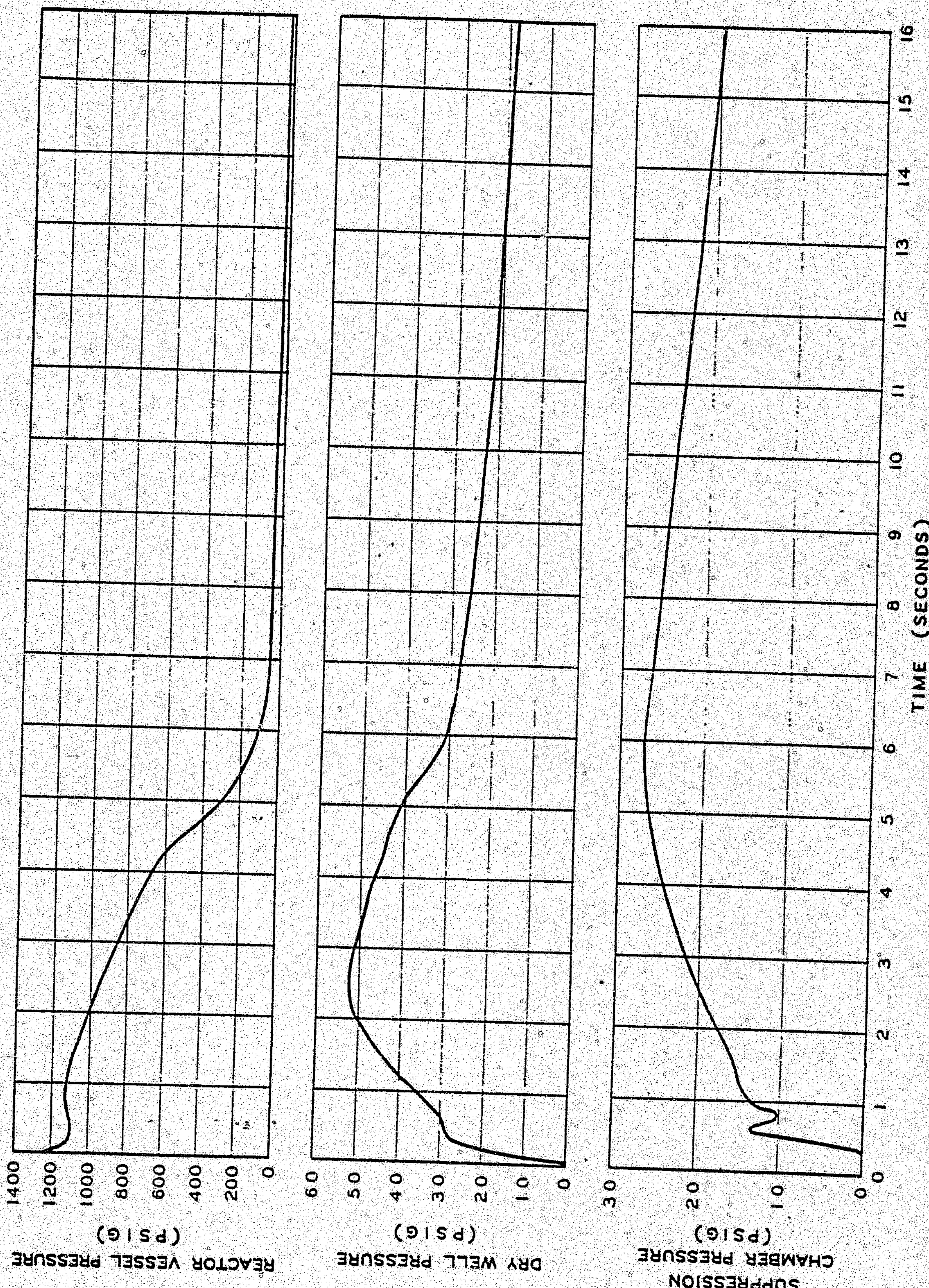
1. Reactor Vessel Pressure

For all tests, reactor vessel pressure was at or near 1,250 psig initially. After the rupture disks broke there was a sharp drop in pressure for all tests, the amount of the drop increasing with orifice size and amount of initial reactor vessel water subcooling. This was



RESULTS OF TEST 17 FOR A 3.24 INCH ORIFICE
(MCOA BREAK AREA)

FIGURE 8



RESULTS OF TEST 20 FOR A 4.50 INCH NOZZLE
(APPROX. TWICE MCOA BREAK AREA)

FIGURE 9

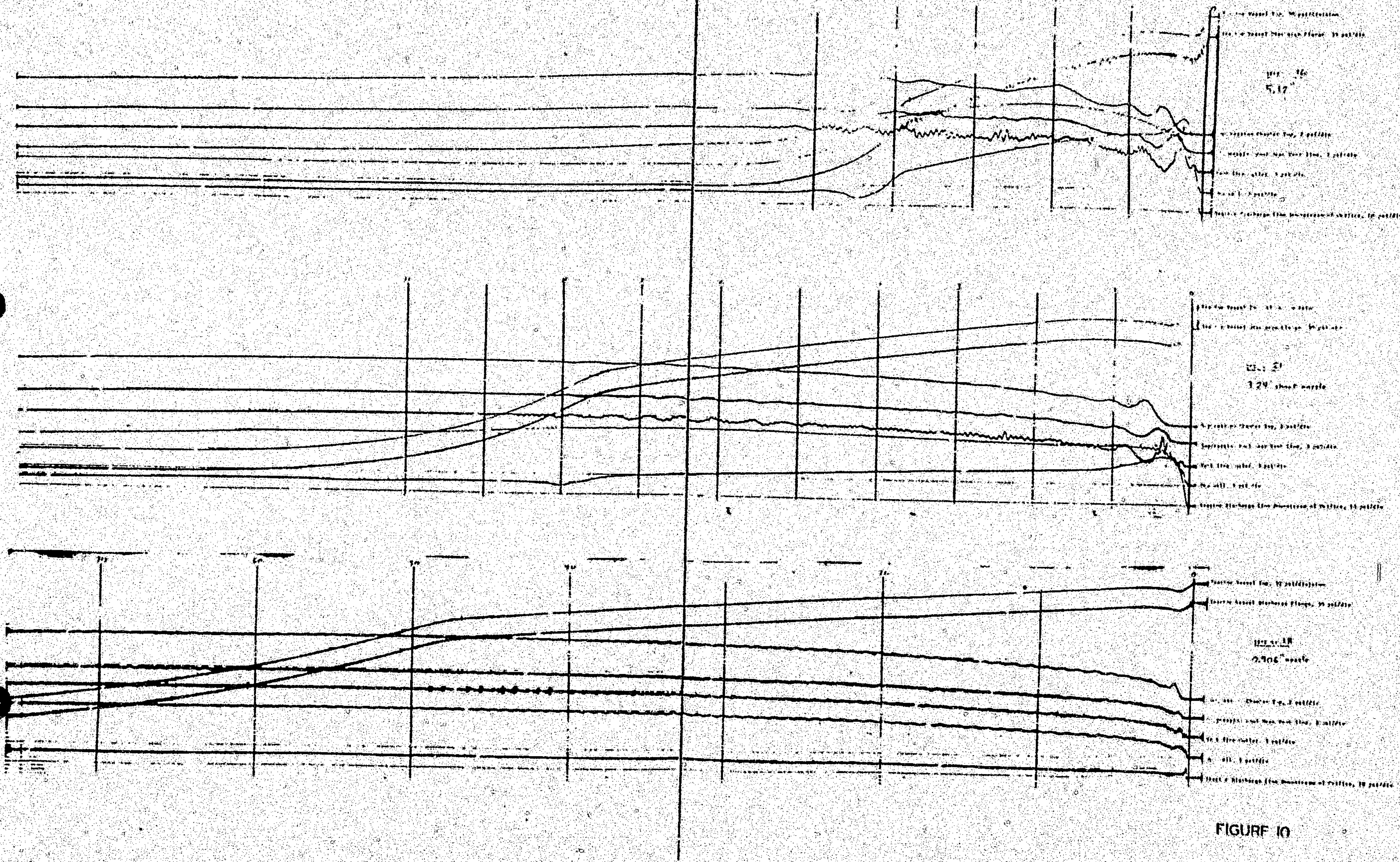
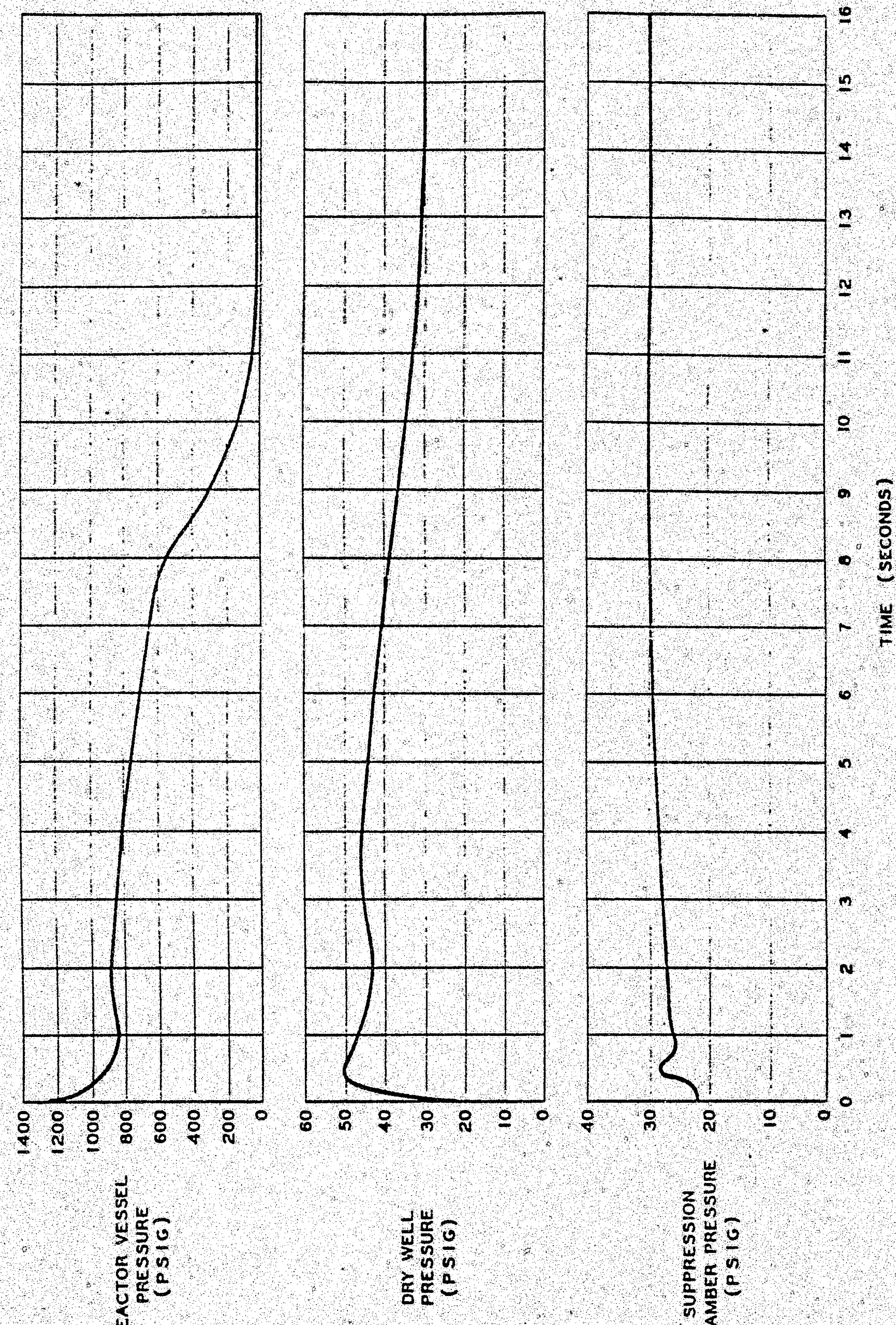


FIGURE 10

12651



RESULTS OF TEST 40 FOR A 3.24 INCH NOZZLE WITH 35° F INITIAL SUBCOOLING AND PRE-PURGED DRY WELL

FIGURE 11

followed by a short period of fairly steady pressure and then a gradual pressure decrease which was in turn followed by a more rapid rate of decrease. The initial drop in pressure is assumed to be caused by:

(1) A brief delay between initiation of flow and start of flashing of the water in the vessel, and

(2) The need for sufficient pressure loss to establish the rate of flashing that corresponds to the flow rate from the vessel.

The change from a gradual pressure decrease to a more rapid rate of decrease is assumed to occur at the time that all water has been expelled from the vessel; the more rapid rate of decrease resulting from the fact that there is no more water to flash and help maintain pressure. The time after start that this change occurs is assumed to be the duration of water flow for purpose of calculating flow rates.

All these characteristics were also observed during the Humboldt tests.

2. Suppression Chamber Pressure

The suppression chamber pressure trace for Tests Nos. 1 through 27 showed an initial sharp rise, generally to about 12 or 13 psig, followed by a slight drop in pressure; then pressure would rise gradually reaching a maximum at about the time all water and steam was expelled from the reactor vessel. This gradual pressure rise was smooth for the smaller orifice sizes and was accompanied by some pulsations for the larger orifice sizes.

The initial sharp rise in pressure followed by a slight drop was observed during the Humboldt testing. Visual observations of suppression pool action were also made during the Humboldt testing and this pressure characteristic was attributed to the observed violent but brief upward surge of pool water caused by a large blast of air coming over from the dry well at the start of the test and resulting in momentary compression of the air in the suppression chamber air space. Visual observation was not considered practical during the Bodega testing but it is considered that this pressure characteristic is due to the cause described above.

Following the initial sharp rise in pressure, the gradual rise is caused by continued purging of the remaining air from the dry well and heat up. Before the deflector plate was installed in the dry well, this purging was not complete, but after its installation, purging was nearly complete. At no time did the suppression chamber pressure exceed 30 psig, indicating that the condensation of steam under all conditions tested was rapid and complete. Variations of plus or minus one foot in pool water level had no significant effects on performance.

For Tests Nos. 39 through 45, the suppression chamber pressure traces have similar shapes to those of tests without pre-purging. However, the initial sharp rise is only about 6 psi above the pre-purged pressure before the test. The gradual rise following the initial sharp rise has a much lesser slope than tests without pre-purging because little air remains in the dry well to be purged during this period. The gradual rise in the suppression chamber pressure trace for pre-purged tests is essentially all due to heating up of the chamber air and vapor while energy is being absorbed in the suppression pool water.

2. Dry Well Pressure

Calculations made of dry well pressure predicted that critical flow would not occur at the end of the vent pipe, as was the case in the Humboldt design. This means that dry well pressure would then be suppression chamber pressure plus vent pipe pressure drop. The calculations also predicted that the initial pressure build up in the dry well before the water was blown out of the submerged end of the vent line would not be the maximum pressure. The tests proved these predictions to be correct. At the start of a test, dry well pressure would rise very quickly until the water in the vent line was blown out and venting started. Then pressure would rise at a slower rate, governed by suppression chamber pressure and vent line pressure drop, and decrease toward the end of the test as flow rate and pressure drop decreased.

The maximum dry well pressure for all ten tests of the first 27 with an orifice or nozzle simulating the MCOA was 38 psig. This is somewhat less than the calculated pressure for the MCOA principally because the calculations assume that all dry well air goes over to the suppression at the beginning when flow rate and vent line pressure drop are at a maximum.

The test with the dry well preheated resulted in somewhat lower pressure as some air had been driven out by the preheating steam.

Tests with orifices or nozzles of sizes other than that simulating the MCOA yielded pressures that were close to but slightly under calculated pressures for reasons stated above.

Tests with dry well pre-purge and subcooled reactor water gave higher dry well pressures than predicted on the basis of average rupture flow rate. Comparison of maximum dry well pressures on these tests with the calculated curve (Figure 1, Appendix II) indicates that the instantaneous value of the rupture flow rate at the time of maximum dry well pressure may be higher than average rupture flow rate by a factor of 1/3 to 1/2 for the conditions of these tests. The tests showed that the vent clearing transient dry well pressure was not the maximum dry well pressure for the 3.24" break with pre-purge.

G. Conclusions

The following principal conclusions which may be drawn from the results of the test program are:

1. Condensation of steam in the suppression chamber is rapid and complete for flows associated with break areas at least up to 250% of MCOA.

2. Variations in suppression pool level of plus or minus one foot did not affect performance of the containment system.

3. Moderate subcooling of reactor water and dry well preheating did not affect performance significantly.

4. Use of nozzles instead of orifices to simulate a break resulted in an increase of rupture flow of less than 10%.

5. Pre-purging the dry well coincident with subcooled reactor water resulted in a maximum dry well peak pressure of 52 psig.

PECO EXHIBIT RJM-1

PJ 3-14-86

NL7

PENNSYLVANIA PUBLIC UTILITY
COMMISSION v. PHILADELPHIA ELECTRIC
COMPANY, DOCKET NO. R-850152

LICENSING CONSTRAINTS TO LIMERICK SCHEDULE ACCELERATION

FEBRUARY 19, 1986

I. INTRODUCTION

This exhibit has been prepared to explain the effect of the specific licensing environment at Limerick and special requirements due to Limerick's high population density on the schedule for the NRC's issuance of a low power operating license for Limerick 1. It was developed jointly by PECO and PECO witness Dr. Roger J. Mattson, subject to the supervision of Dr. Mattson.

Section II provides a description of the various components comprising the NRC's licensing process and their interdependencies, expanding on the brief description contained in the direct testimony of Dr. Roger J. Mattson. The interaction between the NRC and the applicant during the application review process is described. Section II also describes in summary fashion the licensing process as it was actually experienced by Limerick 1. The NRC requirement for PECO to perform a Probabilistic Risk Assessment (PRA) as a prerequisite to receipt of an operating license (OL) is shown to be unique to Limerick.

Section III describes the unique role the PRA played in the Limerick licensing process. Background leading to NRC's request to perform a PRA is described. Factors influencing preparation of the PRA and NRC review of the PRA are described, the uncertain role of the PRA in the licensing process is discussed, as well as litigation of the PRA before the Atomic Safety and Licensing Board.

Section IV describes the development of an earliest possible schedule for low power licensing based on actual Limerick 1 experience. This Exhibit shows that preparation, review and litigation of the PRA were the limiting activities on any accelerated path to low power licensing, and the licensing activities associated with the PRA could not have been accomplished sooner than mid-May 1984.

II. DESCRIPTION OF THE NRC PROCESS LEADING TO ISSUANCE OF A LOW POWER OPERATING LICENSE

Applications for an operating license are supported by a Final Safety Analysis Report (FSAR) as required by 10 CFR 50. The FSAR describes the completed plant design, including the procedures and technical specifications under which plant operation will be conducted. It also includes analyses demonstrating the safety of the plant under normal and design basis accident conditions. Also supporting the application is the Environmental Report - Operating License Stage (ER-OL) as required by 10 CFR 51. The ER-OL describes the environmental impact of plant operation. Following submittal of the application and supporting documents, the NRC conducts an acceptance review to determine whether those documents are sufficiently complete to formally initiate the licensing process. When such a determination is made, the application is docketed.

A. Components of the NRC Licensing Process

The NRC's licensing process comprises four separate activities which are formally initiated upon docketing of an operating license application, and which proceed in parallel until all reach a state of completion sufficient to allow issuance of a low power operating license. Each of these four activities is described in this section.

1. NRC Safety Review

The purpose of the NRC Staff review of an applicant's FSAR is to ascertain that the facility design, construction, and planned operation comply with established NRC requirements, thereby assuring that the health and safety of the public are protected. This determination must be made prior to issuance of a low power operating license.

The NRC's review is performed in accordance with NUREG-0800, "Standard Review Plan for the Review of Safety Analysis Reports for Nuclear Power Plants" or SRP. An applicant's compliance with NRC requirements is determined by NRC Staff reviewers who compare the acceptance criteria contained in each section of the SRP against the corresponding FSAR section. When a staff reviewer finds that information is needed beyond that provided in the FSAR in order to understand how the facility compares to the SRP criteria, a request for additional information is transmitted to the applicant. The requested information is typically provided in the form of a revision to the FSAR. Following receipt, the NRC Staff reviewer considers the new information, which may, in itself, trigger additional information requests. Meetings between the applicant and the NRC staff are often utilized to gain a mutual understanding of one another's position on, or interpretations of, particular issues. Such meetings are normally followed by an FSAR revision or other form of written information submittal. This process is repeated by each of about 20 NRC technical branches for each SRP section for which it has review responsibility.

Where intervenors allege before the Atomic Safety and Licensing Board (ASLB) early in a proceeding that one or more aspects of the facility fail to comply with NRC requirements, the NRC Staff reviewers are effectively put on notice that they may, at a later date, find themselves under oath, testifying and being cross-examined on whether the intervenor's allegations have been carefully examined as described in the Staff's Safety Evaluation Report (SER) and found without merit. As a result, the NRC Staff review tends to be more detailed and more prescriptive, issues raised by intervenors are scrutinized more closely by NRC reviewers, and the results are better documented in the SER than for non-contested issues. Administrative Law Judge Lawrence Brenner, Chairman of the Limerick ASLB, put it succinctly when he stated:

> "The staff can take care of it [intervenor concerns] in the FES [or SER], or they can take care of it under your intense, withering cross-examination. And they can decide what's more pleasant to them." (TR.8640)

Over 1000 requests for additional information are routinely received by applicants, and 20-50 substantial revisions to the FSAR are common before all items are closed. When the number of items remaining unresolved between the applicant and staff is reduced to about 20, the NRC publishes the results of its safety review in the SER. The outstanding items identified therein are resolved as described in the direct testimony of Dr. Roger J. Mattson, and the results are reported in a series of Supplemental Safety Evaluation Reports (SSERs). These SSERs identify any new problems arising during the continuing review process. This interaction between Staff and applicant continues until all open items are closed in a final SSER.

Publication of the SER and SSERs sufficient to demonstrate compliance with all applicable regulatory requirements is a prerequisite to issuance of a low power operating license.

2. <u>NRC Environmental Review</u>

The purpose of the NRC staff review of an applicant's ER-OL is to evaluate the environmental effects of operation of the facility and then to consider these effects in the cost-benefit balance required by the National Environmental Policy Act of 1969 (NEPA) prior to issuance of an operating license.

The NRC's environmental review is conducted in much the same manner as its safety review, using NUREG-0555, "Environmental Standard Review Plans" (ESRP) in lieu of the SRP. Staff review activities concentrate on changes in the construction-related environmental impacts that may have arisen since the environmental review at the construction permit stage of licensing. The ER-OL review also considers the environmental impacts of station operation.

In addition, the NRC's ER-OL review was expanded in 1980 as a result of the accident at Three Mile Island (TMI) to require an examination of the risks associated with severe accidents. The March 28, 1979 accident at TMI led to changes in NRC policies regarding the considerations to be given to severe accidents from an environmental as well as a safety point of view. The NRC issued on June 13, 1980 (45 FR 40101) a statement of "Interim Policy on Nuclear Power Plant Accident Considerations Under The National Environmental Policy

Act of 1969" or Interim Policy. Some of the key positions presented in this policy statement include:

- It is the position of the Commission that Environmental Statements prepared by the NRC staff, pursuant to Section 102(C)(i) of the National Environmental Policy Act of 1969, shall include a reasoned consideration of the environmental risks (impacts) attributable to accidents at the particular facility or facilities within the scope of each such statement. In the analysis and discussion of such risks, approximately equal attention shall be given to the probability of occurrence of releases and to the environmental consequences of those releases.

- The extent to which events arising from causes external to the plant which are considered possible contributors to the risk associated with the particular plant shall also be discussed. Detailed quantitative considerations that form the basis of probabilistic estimates of releases need not be incorporated in the Environmental Statements but shall be referenced therein. Such references shall include, as applicable, reports on safety evaluations and generic probabilistic risk assessments.

- The environmental consequences of releases whose probability of occurrence has been estimated shall also be discussed in probabilistic terms. Such consequences shall be characterized in terms of potential radiological exposures to individuals, to population groups, and where applicable, to biota.

- Health and safety risks that may be associated with exposures to people shall be discussed in a manner that fairly reflects the current state of knowledge regarding such risks. Socioeconomic impacts that might be associated with emergency measures during or following an accident should also be discussed. The environmental risk of accidents should also be compared to and contrasted with radiological risks associated with normal and anticipated operational releases.

- In promulgating this interim guidance, the Commission acknowledges that there are and will likely remain for some time to come many uncertainties in the application of risk assessment methods, and it expects that its Environmental Impact Statements will identify

> major uncertainties in its probabilistic estimates. On the other hand the Commission believes that the state of the art is sufficiently advanced that a beginning should now be made in the use of these methodologies in the regulatory process, and that such use will represent a constructive and rational forward step in the discharge of its responsibilities.

The NRC's practice in environmental reviews was immediately modified to provide discussions of severe accident risk in NRC's final environmental statements (FESs) in accordance with this policy. These discussions normally use generic models of accident probabilities and environmental consequences, but site-specific data are used in the consequence analysis [Ref. 1,2].

NRC Staff reviewers interact with the applicant in the review of an ER-OL in much the same manner as for safety reviews. Revisions to the ER-OL, rather than the FSAR, typically result. As with safety reviews, contested plants receive more indepth and prescriptive reviews, and NEPA issues raised by intervenors receive closer scrutiny by the NRC Staff than other aspects of the ER-OL review.

In accordance with NEPA requirements, the NRC publishes and distributes for review and public comment a draft environmental statement (DES) documenting the results of its environmental review. Following a 45-day minimum comment period required by statute, each comment received must be resolved by the NRC. On occasion, the NRC publishes its DES prior to completion of its review of one or more environmental issues. In such cases, a DES supplement is published at a later date. This supplement is subject to the same review, comment and comment resolution requirements as the DES. Subsequently, the NRC publishes a FES which reflects the resolution of all comments received.

Publication of the FES is a prerequisite to issuance of a low power operating license.

3. Advisory Committee on Reactor Safeguards (ACRS) Review

The ACRS independently reviews applications for operating licenses and makes recommendations directly to the NRC Chairman. ACRS reviews tend to focus on issues of current generic concern and the controversial aspects of the NRC staff review of a particular application. Thus, the ACRS is also influenced, albeit indirectly, by the degree and nature of intervention in the hearing process. Because the ACRS charter includes advising the Commissioners on the adequacy of existing and proposed safety standards, its reviews are not constrained by existing regulatory limitations, as, for example, the SRP serves to constrain the NRC staff's ability to delve into areas not specifically covered by NRC regulations.

The ACRS interacts with the NRC staff and applicants at public meetings where presentations are made to the ACRS, and questioning is conducted by the ACRS members. Following one or more such meetings with the ACRS subcommittee assigned to a particular license application, a final meeting with the full ACRS is held. If the ACRS is satisfied that the facility can and will be operated safely, an ACRS report is sent to the chairman of the Commission. In especially controversial reviews, such as that for

Limerick, several meetings of the full ACRS may be required, and interim reports to the Commission may be issued.

A final ACRS report is normally a prerequisite to issuance of a low power operating license.

4. <u>Atomic Safety and Licensing Board (ASLB) Hearing Related Activities</u>

The ASLB conducts any hearings associated with an application for an operating license. Following docketing of the application, a three-judge licensing board is named, comprising a chairman, who is always a lawyer, and two technically-qualified members. A Federal Register notice informs members of the public that an operating license application has been docketed by the NRC and invites petitions to intervene. If no petitions are received, no intervenors are admitted, and, unless the ASLB raises issues <u>sua sponte</u>, no hearings are held. This is an uncontested licensing proceeding. If petitions to intervene are received, the ASLB will, following receipt of any clarifying information it deems necessary from the involved parties, conduct a prehearing conference.

The purpose of this first prehearing conference is to explore the proposed contentions (allegations of non-compliance with regulatory requirements) of the intervenors, and in light of the very early stage of the licensing process, determine which contentions are sufficiently specific to be formally admitted, which should be conditionally admitted pending completion of some licensing process milestone, and which should be denied for lack of specificity or basis. Because of Commission policy to not unduly delay operation of capital intensive nuclear plants, timely completion of necessary litigation, while protecting the rights of the parties, is of primary concern to the ASLB. Thus, the applicant and staff are subject to ASLB pressures to reach early resolution of the contested aspects of the safety or environmental reviews so that litigation may proceed. Written status reports from the applicant and NRC Staff and additional prehearing conferences are mechanisms used by the ASLB to monitor the progress of NRC reviews. Informal discovery is ordered by licensing boards to minimize the time required for formal discovery activities at a later date. After the NRC Staff and the applicant have resolved to their satisfaction the contested aspects of the NRC safety or environmental review, the ASLB orders final specification of related contentions, formal discovery, filing of direct testimony and evidentiary hearings. Following the hearings, there is filing of proposed findings of fact by all parties. Upon consideration of the record, the ASLB issues a Partial Initial Decision (PID) containing its findings of fact and conclusions of law. Thus, several rounds of hearings and PIDs may be required prior to licensing, their order being determined by the completion dates of NRC review of the matters contested.

Litigation must be complete and PIDs issued on all matters in contention except for offsite emergency planning prior to issuance of a low power operating license.

B. <u>Limerick 1 Licensing Process</u>

This section describes in summary fashion how the Limerick 1 licensing experience tracked the general licensing process described above. From this experience one can develop an earliest possible licensing schedule. As shown in the analysis here, the earliest possible OL schedule, considering only the PRA, was much later than the 1982 date proposed by OCA witness O'Brien-Kreitzberg and Associates, Inc. (OKA").

In many ways, the actual low power licensing process experienced by Limerick 1 can be considered both efficient and routine, leading to issuance of a low power operating license only 39 months after docketing of the application. As shown on Schedule 1, this is the third shortest interval between docketing and issuance of a license for all plants licensed since the TMI accident in March 1979, and the shortest interval for plants whose OL proceedings were contested by intervenors. This was accomplished despite the fact that licensing activities associated with NRC review of the Limerick Probabilistic Risk Assessment (PRA) were both unique to Limerick and greatly complicated NRC's environmental reviews and ASLB activities, as discussed in detail in Section III.

Because the NRC refused to initiate review of the FSAR prior to receipt of the PRA (Ref. 5), PECO applied for operating licenses for Limerick 1 and 2 on March 17, 1981 (Ref. 3) following completion of PRA documentation. Following review of the application and supporting documents for completeness (acceptance review), the application was docketed on July 27, 1981, formally beginning the licensing process.

1. NRC Safety Review

NRC staff review of the FSAR began at docketing and proceeded as described in Section II.A.1. The NRC published the Limerick SER (Ref. 4) in August 1983, SSER 1 in December 1983 and SSERs 2 and 3 in October 1984, in support of low power licensing. Three additional SSERs have been issued to date.

During the review process, the higher population density surrounding Limerick, the issues raised by intervenors, and the resultant threat of hearings caused the NRC staff to press for earlier resolution or implementation of safety related design changes than was its practice for other reactors. Fire protection, environmental qualification, anticipated transients without scram (ATWS), and TMI issues are exemplary of this treatment of Limerick relative to such plants as Susquehanna and LaSalle. The rebuttal testimony of Roger J. Mattson, David R. Helwig, and Edward F. Sproat, III contains further discussion of this matter.

2. NRC Environmental Review

NRC staff review of the ER-OL began at docketing and continued through issuance of its DES in June 1983, a DES Supplement (Ref. 6) addressing the Interim Policy on severe accidents in December 1983, and the FES (Ref. 7) in April 1984. As described in the FES at pages 1-4, the NRC's treatment of Limerick under the Interim Policy was unique:

> "... [T]he analysis of severe accident consequences in this document has a number of unique features that enhance its value for gaining insight into the uses and limitations of PRA in assessing environmental

> consequences: (i) the Limerick PRA is the only large-scale PRA thus far available for contemporary BWRs that includes external events as potential accident initiators; (ii) the site is substantially higher than average in terms of population density; and (iii) very few OL reviews can be anticipated to have available a large-scale PRA. Accordingly, much more information is available in the case of Limerick for the assessment of severe accident consequences than has been customary in environmental impact statements for other OL actions and by the same token, this has led to an expanded scope of analysis consistent with the Statement of Interim Policy that should not be expected for other OLs."

This unique treatment of Limerick caused the FES to be a prerequisite to the conduct of hearings before the ASLB on PRA-related contentions.

3. <u>ACRS Review</u>

The independent ACRS review resulted in an unusual interim report in October 1983 recommending low power licensing. It was unusual in the sense that the ACRS held full power licensing hostage to its further review of the NRC staff's review of the Limerick PRA.

4. <u>ASLB Hearing Related Activities</u>

Following notice in the Federal Register in August 1981, petitions to intervene were received from 13 parties. In November 1981 a joint statement of proposed contentions was filed, comprised of 218 separate issues (Ref. 9). Many proposed contentions contained alleged deficiencies in the PRA and emergency plans. Many others alleged deficiencies in the subject areas discussed in PECO Exhibit 2. Over the subsequent two years, many contentions, especially those related to TMI requirements, were dropped by intervenors in response to PECO commitments to implement NRC-approved modifications prior to fuel load. Others were denied by the ASLB for lack of specificity or basis, and a number of others ultimately went to hearing. Issues litigated prior to low power licensing were:

- Supplemental Cooling Water Supply
- Aircraft Carburetor Icing
- Quality Assurance for Welding
- Effects of Nearby Pipeline Explosions
- Environmental Qualification of Electrical Equipment
- Onsite Emergency Planning (the only TMI-related issue)
- Adequacy of the NRC's DES Supplement and FES in addressing severe accidents under its Interim Policy

Beginning in October 1982, fifty-two days of evidentiary hearings were held on these issues, which culminated in the issuance of the ASLB's First PID in March 1983 and the Second PID in August 1984.

The last issue litigated before issuance of the Second PID was DES Adequacy. This issue thus controlled the schedule for full power licensing. Hearings on this issue began on May 22 and concluded June 20, 1984. As described in Section III, issuance of the FES was a prerequisite to the filing of testimony on DES-based contentions.

By contrast, LaSalle was an uncontested proceeding, and Susquehanna, while contested, required a total of only 7 days of evidentiary hearings to be held prior to low power licensing.

5. Summary of the Limerick Licensing Process

On October, 26, 1984 the NRC issued a low power operating license for Limerick Unit 1 based on:

(a) The First PID, dated March 8, 1983

(b) The SER, dated August 1983

(c) SER 1, dated December 1983

(d) SSER 2, dated October 1984

(e) SSER 3, dated October 1984

(f) The FES dated April 1984

(g) The Second PID, dated August 29, 1984

(h) The ACRS Interim Report dated October 18, 1983

A time line illustrating the actual Limerick licensing experience is included as Schedule 3.

III. THE ROLE OF THE LIMERICK PRA IN THE LICENSING PROCESS

This section describes the background, preparation, NRC review, and litigation related to the Limerick PRA as it pertains to development of an earliest possible licensing schedule.

A. Background Leading to NRC Request to Perform a PRA for Limerick

The Reactor Safety Study (WASH-1400) was published in 1975. It was the first application of probabilistic risk analysis techniques to nuclear power plants. It was funded by the Atomic Energy Commission, and it included two plants, one PWR and one BWR, the latter being a BWR model 4 with Mark I containment modeled after the Peach Bottom Atomic Power Station. The study was performed by an independent group, and it contained a number of first-of-a-kind models for nuclear power plant systems analyses, including event trees and fault trees, statistical analyses, and physical analyses of severe accidents.

The NRC's' licensing staff evidenced moderate interest in the PRA techniques employed in WASH-1400 for several years. The study itself and the new techniques it employed found some limited use as a supplement to the regulatory process, but they were not approved by NRC management for use in the licensing process. The study was used at a policy level in government to aid in deliberations involving the Price-Anderson Act.

By 1977, there was a considerable controversy within the nuclear industry surrounding PRA techniques and their utilization in safety regulation. The NRC commissioned an ad hoc review group chaired by H.W. Lewis to review these matters and make recommendations to the Commission. The group's report was published in September 1978 (NUREG/CR-0400). Although the group found fault with several elements of WASH-1400, (e.g., it criticized the manner in which ATWS events were analyzed) and found that WASH-1400 was inscrutable, the fault-tree/event-tree methodology was found to be sound and worthy of wider use by NRC. Unfortunately, early press reports emphasized some negative statements by Dr. Lewis in his presentation of the group's report to the NRC. It was charged that the Lewis group had found the PRA techniques to be flawed, and people demanded to know where NRC staff had relied upon WASH-1400 in the regulatory process. The ensuing document searches to satisfy Congressional inquiries had a chilling effect upon uses of PRA in the reactor licensing process.

The effect of this controversy dominated NRC staff thinking about PRA until the accident at TMI-2 in March 1979. The accident was typical of a class of accident sequences that WASH-1400 had shown to be of high safety significance. As a result, PRA was reborn nearly overnight, and technical reviewers at NRC scrambled to find ways to put the technique to work.

The first broad-scale use of PRA in the licensing process was in the summer and fall of 1979. The NRC staff, depending upon senior analysts from the Reactor Safety Study, performed a probabilistic risk comparison of auxiliary feedwater systems for pressurized water reactors then in operation (see description contained in NUREG-0660, TMI Action Plan). This study identified a number of cost-effective design improvements in operating PWRs to lessen the chances of their experiencing an accident like the one at TMI-2. The NRC Staff, the ACRS, the Commissioners, Congressional oversight committees, and the Presidential commission that investigated the TMI-2 accident all praised this new found use of PRA.

In the fall of 1979 and early in 1980, there were other applications made of PRA in the licensing process. Congressional committees held hearings that included discussions of whether nuclear plants in areas of higher population density than TMI-2 might be too risky. They wanted to know if the designs and the emergency preparedness at sites such as Indian Point, Zion, and Limerick were sufficient to offset the potential for catastrophe involving their large, nearby populations. PRA is the only technique available to answer such questions in a thorough, technical manner. In response to the Congressional pressure, as well as a pending petition from the Union of Concerned Scientists (UCS) to shutdown the operating Indian Point reactors for the same reason, the NRC in December 1979, requested that the owners of Indian Point and Zion nuclear plants perform 60 day studies to assess the risk reduction potential of additional severe accident mitigation features. On February 11, 1980, the NRC granted, in part, the UCS petition and on February 15, 1980 requested public comments on the form of future Commission actions on Indian Point, including the conduct of a special adjudicatory proceeding. On February 25, 1980, the owners of Indian Point and Zion filed a joint response to the NRC's December 1979 request. That response contained a commitment to perform detailed PRAs for both plants. On May 30, 1980, the NRC Commissioners ordered initiation of a special adjudicatory proceeding to gather information and make recommendations to the Commissioners on whether Indian Point Units 2 and 3 should be shutdown or other action taken.

In early 1980, the NRC was actively attempting to identify other plants whose populous sites warranted action similar to that taken at Indian Point and Zion (Ref. 10). Limerick was identified as the only other reactor, either operating or under construction, for which NRC would require a risk assessment (Ref. 11). Subsequently, on May 6, 1980, the NRC requested that PECO prepare a PRA for Limerick using WASH-1400 methodology (Ref 12). A further indication of Limerick's unique treatment is the fact that while other OL applicants have voluntarily performed PRAs, in no case has NRC required PRA review and approval by the NRC staff prior to issuance of an operating license and in no other case has the PRA played a role in a plant's licensing.

In May 1980, the NRC had not decided how the Limerick PRA would be utilized in the licensing process, how it would be reviewed by NRC, or what criteria would be applied to judge the adequacy of conclusions and recommendations that might be reached by PECO on the basis of the study (Ref. 11,13,14,15).

Even though its use was uncertain, the PRA for Limerick was required to be performed and reviewed prior to operation (Ref. 11,15,17,30). Furthermore, any design changes shown by the PRA to be necessary to compensate for the high population density at Limerick had to be implemented prior to NRC granting an operating license (Ref. 17). The requirement for implementation of these changes prior to plant operation was justified in view of the level of perceived public risk from operation without the changes, such as those required for ATWS, and because of increased occupational radiation exposure had these changes been delayed until after initial operation.

B. <u>Preparation of the Limerick PRA</u>

At the time Limerick's PRA was undertaken, there was no guidance from NRC or others as to the acceptable scope, methods, or data to be relied upon. Such guidance was first published by NRC in NUREG/CR-2300, in January 1984. Although one of the plants treated in WASH-1400 was Peach Bottom, there were modeling (e.g. ATWS) and scope (e.g. seismic events) discrepancies in that analysis that made it inadequate for the purposes required of the Limerick PRA by NRC. At the time the NRC request was made for the Limerick PRA, no commercial risk assessment of a BWR had ever been performed. Significant research work had been undertaken by NRC on the computer codes and data to be employed in such studies; however, this work was still in a research mode and had not been tested in commercial applications of PRA. Further, the NRC required that PECO respond to criticisms of WASH-1400 contained in the Lewis report (NUREG/CR-0400).

As a result, a number of unique activities and technical undertakings were performed, including site specific assessment of the Limerick plant and environment, detailed analysis of different types of transients, the first risk assessment of a Mark II containment, as well as significant enhancement in the development of accident sequence categories that are used to group similar sequences to simplify subsequent steps of the analysis.

In addition, the May 1980 NRC request specified a performance period of 120 days for the PRA. This schedule was extremely short in terms of both historical perspective and current practice. A PRA procedures guide, NUREG/CR-2300 (Ref. 16), subsequently recommended the minimum time for producing a PRA of Limerick's scope to be 18 months.

An interim meeting to review the programmatic aspects of the study was held with the NRC on July 11, 1980. This meeting provided the NRC with an opportunity to comment on the overall direction and scope of the study. On October 2, 1980, interim results were provided to the NRC. These results described accident sequences but did not provide detailed quantification values.

The results of the study and the quantified core damage frequency and risk curves were presented to the NRC at a public meeting on December 9, 1980. On December 11, 1980, substantial information was submitted to

Sandia National Laboratory to initiate its independent review for the NRC staff. This submittal included essentially all of the information to be published in the formal PRA in March 1981, including system details, fault trees, event trees, population data, initial conditions, assumptions of the study, success criteria, and fission product inventory information.

On March 17, 1981, the PRA was formally submitted to the NRC along with the Limerick OL Application (Ref. 3).

C. NRC Review of the Limerick PRA

NRC review of the PRA was slowed by the lack of availability of qualified reviewers, the first-time nature of the review, and the uncertainty within the NRC regarding the role the PRA would ultimately play in the licensing process. These factors are addressed in further detail in this section.

1. Availability of Qualified Reviewers

Sandia National Laboratory initially assisted the review of the Limerick PRA under contract to NRC's Office of Nuclear Reactor Regulation (NRR). However, because of manpower and technical limitations, the Sandia review was terminated in mid-1981 and personnel at Brookhaven National Laboratory (BNL) were retained under contract to assist in the NRR review, as were consultants from Purdue University and the University of California at Los Angeles. These same personnel were relied upon by NRR to assist in the Sandia review of the Indian Point PRA. Other than this limited pool of experts, there were no other NRC staff contractors judged by the staff to be adequately qualified at that time to assist in the staff review.

Following transfer of documentation and BWR/PRA familiarization, BNL's review began in earnest in January 1982. The NRC staff review of the Indian Point PRA continued to take precedence for staff and contractor resources over the review of the Limerick PRA. The reasons were that Indian Point was already licensed, and there were serious questions raised about the safety of continuing operations (Refs. 15,17,18).

The staff and its consultants from Sandia and BNL participated in the Indian Point special proceeding until February 1984.

2. The First-Time Nature of the Limerick PRA

As described above, at the time the NRC requested the Limerick PRA there was no guidance from the NRC or others as to what constituted acceptable scope, methodology or documentation. As a result, the individuals involved in the review effort had no choice but to rely in large part on their own judgement. This lack of review standards led to NRC information requests which resulted in four revisions to the PRA, followed by a draft report documenting the results of BNL's review in September 1982 (Draft NUREG/CR-3028, Ref. 19). This report, in PECO's opinion, was deficient in several significant aspects (Ref. 20,21) which can be attributed to differing professional opinions on the part of BNL reviewers. BNL issued its final report on the Limerick PRA in February 1983 (NUREG/CR-3028, Ref. 22).

In April 1983, BNL undertook review of a revision to the ER-OL and its supporting documentation which had been requested by the

NRC in August 1982 (Ref. 23), as part of its environmental review. Citing its Interim Policy, the NRC requested a substantial increase in the scope and documentation of the PRA. In response, a second document, the Severe Accident Risk Assessment (SARA) was developed by PECO. SARA incorporated the results of the existing PRA as well as the additional analyses requested by the NRC into a single document which formed the basis for a March 1983 revision to the ER-OL. At the NRC's request, SARA was submitted to NRC and BNL in April 1983. In August 1983, BNL released the preliminary results of its review of SARA in a draft NUREG/CR report (Ref. 24).

NRC/BNL reviews of the PRA and SARA culminated with the issuance of NUREG-1068 (Ref. 25) in September 1984.

3. <u>The Evolving Role of the PRA</u>

NRC's initial motivation to order a PRA for Limerick was the feeling that because of its high population site, the plant was more likely than others to present disproportionately high societal risk. Consequently, comparison to the Wash-1400 risk was ordered (Ref. 14). After studying the PRA, a better understanding of the safety of Limerick evolved within the NRC. The NRC Staff found very low societal risks at Limerick, low enough that risk comparisons ceased to have regulatory impact. The ultimate use of the PRA turned out to be as a design and procedure refinement tool, rather than a comparison to WASH-1400 (Ref. 26). As described in Section II.B.2, the NRC took advantage of the existence of the PRA to require extensive NEPA-related analyses not required of other OL applicants under its Interim Policy (Ref. 7). A detailed retrospective description of the NRC's ultimate use of the Limerick PRA is contained in its September 1984 report "Review Insights on the Probablistic Risk Assessment for the Limerick Generating Station", NUREG-1068 (Ref. 2), provided, in pertinent part, as Schedule 2.

D. Litigation Related to the PRA

Approximately 30 proposed contentions alleging deficiencies in the PRA were received by the ASLB in November 1981 (Ref. 9). At the first prehearing conference in January 1982, the ASLB was unable to understand NRC staff counsel's representation as to the use to which the NRC would put the PRA. This understanding is essential to the determination of the admissibility of the proposed contentions.

In response, the NRC staff filed the affidavit of Albert Schwencer and Ashok C. Thadani (Ref. 17). In that affidavit, it is stated that,

> "The Staff's position with respect to the applicant's PRA will not... be reached until the issuance of the SER (and the SSER)..."

and, with respect to priority,

> "SECY-81-25 clearly states, however, that the determination of whether any special mitigation features will be required for Limerick (and, if so, what features) will be made after completion of the Zion/Indian Point studies, as part of the overall long-range program. SECY-81-25, p. 7, para. 3. Thus, the Commission is aware of the Staff's establishment of priorities with respect to the completion of the review of the PRA's and the recommendation of any special mitigation features which may be deemed necessary for these three sites. The Staff has proceeded with the review of the PRA for Limerick on a schedule consistent with this assignment of priorities."

The affidavit is silent on the use of the PRA under the Interim Policy.

In its June 1982 First Prehearing Conference Order (Ref. 28), the ASLB again expressed its "uncertainty as to precisely what use would be made by the Staff of PRA in its review of the operating license application...and to what extent PRA should be considered in this proceeding", but admitted the PRA contentions subject to further specification "within a month or two after the Staff's review of the PRA is issued."

In its order of February 10, 1983, the ASLB ordered respecification of conditionally admitted PRA-based contentions. On April 12, 1983, the intervenors filed both respecified and new contentions based on NUREG/CR-3028. On April 13, 1983, the NRC staff filed a second report to the ASLB "...definitively explaining the scope and purpose it will make of the applicant's probabilistic risk assessment..." (Ref. 29). The report reads nearly identically to Schedule 2, attached.

Thus, it was not until April 1983 that the NRC staff had finally decided how the Limerick PRA was to be used in the safety and environmental portions of the ASLB hearings.

At the May 9, 1983 prehearing conference, the staff informed the ASLB that the results of NRC's Environmental Review of the PRA would not be reported in the DES, but in a supplement thereto, to be issued in October, 1983, with an FES to follow in March 1984. It was recognized at this prehearing conference that the schedule-limiting matter before the ASLB pertaining to issuance of a low power license would be PRA related contentions (Ref. 30).

By Order dated July 26, 1983, the ASLB ordered the filing of proposed contentions based on the ER-OL and SARA by September 1, 1983. In response, new proposed contentions were received from intervenors Limerick

Ecology Action (LEA) and the City of Philadelphia (City).

In its October 28, 1983 order confirming rulings made at an October 17-18, 1983 prehearing conference, the ASLB deferred arguments on the admissibility of LEA and City contentions until after issuance of the DES Supplement, still scheduled for issuance in October 1983. On December 16, 1983, the NRC issued the DES Supplement describing its review of the PRA under its Interim Policy.

By order dated January 20, 1984, the ASLB required the filing of contentions based on the DES Supplement by February 14, 1984. In response, LEA and City filed additional contentions. Arguments on the admissibility of proposed DES-based contentions were held on March 19, 1984, and the ASLB made oral rulings on March 20. A schedule for hearings was proposed, based on the pre-filing of testimony four working weeks after issuance of the FES, which was issued on April 6, 1984.

On April 9, 1984, the NRC staff reported to the ASLB on the record that it had finished its safety review of the PRA and concluded that no use would be made of it in the safety portion of the Limerick OL process.

On May 11, 1984, testimony on LEA and City DES-based contentions was filed. Evidentiary hearings on these issues began on May 22, 1984, and concluded on June 20, 1984, after 8 days of hearings.

On August 29, 1984, the ASLB issued its Second PID deciding all issues in controversy which were prerequisite to issuance of the low power operating license.

IV. DEVELOPMENT OF AN EARLIEST POSSIBLE SCHEDULE FOR COMPLETION OF THE LOW POWER LICENSING PROCESS FOR LIMERICK 1

This section examines each component of the licensing process actually experienced by Limerick 1 and describes whether and how each might have been accelerated to support an earlier issuance of the low power operating license.

FSAR and ER-OL preparation schedules in late 1976 were based upon submittal of an operating license application to the NRC in September 1978. Had construction plans indicated a need, an application could have been filed by that date. NRC acceptance review could be expected to take about the four months actually experienced. Docketing could have been expected in January 1979, formally initiating the NRC safety and environmental reviews, ACRS independent reviews and ASLB hearing related activities.

As demonstrated in Section II.B, Limerick's elapsed time from docketing to issuance of the low power operating license is very short compared to that for other reactors. It can therefore be conservatively assumed that all components of the license process except those aspects related to the PRA and litigation of any of the hypothetically deferred items that are described below would have proceeded at the rate at which they actually occurred following docketing, even though Limerick's licensing period would have spanned the two year post-TMI period. This assumption implies that new regulatory requirements were addressed and resolved with the NRC as they arose and that any design changes required to meet aspects of those new requirements would have been deferred, just as was done by Susquehanna and LaSalle. As discussed elsewhere in PECO testimony, this is an unrealistic and erroneously conservative assumption. (See rebuttal testimony of Roger J. Mattson, David R. Helwig and Edward R. Sproat.) It further implies that additional litigation related to deferred items would have resulted (because, in actuality, each of these issues was contested), but that such litigation would have occurred shortly after issuance of the SSERs documenting completion of resolution of each contested issue, resulting in an ASLB PID about one year after issuance of the SSERs. The date of issuance of these SSERs, however, would have been highly dependent on the NRC staff's willingness to defend its decision to allow deferral of plant changes related to these new regulatory requirements for Limerick. This would have required a change in the prior position of the staff with regard to the timing of such changes. Furthermore, the staff would be making its deferral decision knowing full well that the items were the subject of conditionally admitted contentions. It is unlikely that the staff would have done this.

As established in Section III.A, not until early 1980 did the NRC require a detailed PRA for any reactor, operating or under construction. Not until April 1980 was the need for a Limerick PRA discussed with the NRC Commissioners who approved the request (Ref. 11). Therefore, the requirement to perform a PRA could not have been issued earlier.

As established in Section III.B, the Limerick PRA was prepared in 10 months, 8 months less than the minimum recommended time in the PRA Procedures Guide in January 1984 (Ref. 16). This preparation was accomplished despite the lack of available guidance and commercial experience on which to base the Limerick effort. Additionally, the Limerick PRA was the first commercially prepared BWR PRA as well as the first PRA to model a Mark II containment. These factors establish that the PRA could not have been prepared sooner.

As established in Section III.C, NRC review of the PRA was hindered by the unavailability of qualified reviewers and the higher priority assigned to the Indian Point and Zion efforts which continued until February 1984. The review-lengthening effects of differing professional opinions and independent calculations caused by a lack of PRA review standards weighs against a shorter review. These factors, combined with the fact that the NRC staff was already under constant pressure from the ASLB to issue its review documents at the earliest possible time, as established in Section III.D, lead to the conclusion that NRC's review of the Limerick PRA could not have been completed sooner.

As established in Section III.D, the ASLB was unrelenting in its efforts to define the NRC staff's use of the PRA and expedite issuance of the NRC review documentation on which litigation would be based. The 4-week period provided for filing of testimony following issuance of the FES could conceivably have been reduced to two weeks without jeopardizing the intervenors' due process rights. Also, established hearing schedules for non-PRA contentions prevented the ASLB from conducting the PRA hearings on eight contiguous days; doing so would allow the hearings to conclude in less than 2 weeks.

Similarly, the period following completion of hearings until issuance of the second PID was utilized by the ASLB to develop findings of fact and conclusions of law for a number of non-PRA related contentions. Because these issues would have been litigated earlier, this period could conceivably be reduced to about 2 weeks, given that failure to issue the PID would have delayed issuance of the low power license.

These facts and conservative assumptions lead to the conclusion that ASLB hearing related activities occurring after NRC staff issuance of the FES could have been completed approximately 3 1/2 months earlier than the actual August 29, 1984 issue date for the Second PID.

Applying the above assumptions, the earliest possible licensing milestone dates become:

Submit OL Application:	9/1/78
Docket:	1/12/79
NRC Request PRA:	5/6/80 (Actual)
DES Issued:	11/15/80
SER Issued:	1/15/81
Submit PRA:	3/17/81 (Actual)
ACRS Letter:*	4/3/81
ASLB PID on all issues <u>except</u> PRA/SARA/DES and Deferred Items*	2/12/82
SSER 2 & 3 Issued Addressing <u>inter</u> <u>alia</u> Deferred Items*	3/17/82
ASLB PID on Deferred Items:**	3/17/83
DES Supplement Issued:	12/16/83 (Actual)
FES Issued:*	4/6/84 (Actual)
SARA/DES Testimony Filed:	4/20/84
Hearings Begin:	4/27/84
Hearings End:	5/7/84
ASLB PID on SARA/DES:*	5/14/84
NRC Issues Low Power OL:	5/14/84

A time line illustrating this hypothetical earliest possible licensing schedule is included as Schedule 4.

*Prerequisites to Low Power Licensing

**As previously described, this is an unreasonable assumption, adopted merely to simplify the analysis. In light of the NRC licensing moratorium after TMI and the general shortage of NRC staff resources during this assumed licensing period, the numerous NRC requirement changes during this period which delayed and added complexity to the NRC staff review and ASLB hearing process, and the fact that NRC staff and Commissioners would have been reluctant to agree to deferral of items that were contested, an ASLB PID on all non-PRA/SARA contentions could not have been issued this early. In fact, as explained in other PECO testimony, the issuance of such a decision would not have been expected under even the most optimistic circumstances until at least mid-1984.

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SCHEDULE 1

Plant	Docket Date	Low Power License Issue Date	Difference	
St. Lucie 2 **	02/17/81	04/06/83	25M	19D
Millstone 3 **	02/02/83	11/25/85	33M	23D
Limerick 1 ---	07/27/81	10/26/84	38M	29D
Catawba 1	06/11/81	12/06/84	41M	25D
Grand Gulf 1	06/30/78	06/16/82	47M	16D
Susquehanna 1	07/31/78	07/17/82	47M	16D
River Bend 1	08/25/81	08/29/85	48M	4D
Palo Verde 1	06/20/80	12/31/84	54M	11D
Callaway 1	10/19/79	06/11/84	55M	22D
San Onofre 2	02/23/77	02/16/82	58M	23D
LaSalle County 1 **	05/11/77	04/17/82	59M	6D
Waterford 3	12/18/78	12/20/83	60M	2D
Wolf Creek	02/19/80*	03/11/85	60M	22D
Virgil C. Summer	02/24/77	08/06/82	65M	12D
Palo Verde 2	06/20/80	12/09/85	65M	19D
Washington Nuclear 2	06/22/78	12/20/83	65M	28D
Susquehanna 2	07/31/78	03/23/84	67M	22D
Byron 1	11/30/78	10/31/84	71M	1D
LaSalle County 2	05/11/77	12/16/83	79M	5D
San Onofre 3	03/23/77	11/15/83	79M	22D
North Anna 2	05/01/73*	04/11/80	83M	10D
McGuire 1	05/20/74	06/12/81	84M	22D
Joseph M. Farley 2	08/30/73	10/23/80	85M	23D
Sequoyah 2	01/31/74	06/25/81	88M	24D
Salem 2	08/27/71*	04/18/80	103M	21D
McGuire 2	05/20/74	03/03/83	105M	10D
Enrico Fermi 2	04/04/75	03/20/85	119M	16D
Diablo Canyon 1	10/02/73	04/19/84	126M	17D
Diablo Canyon 2	10/02/73	04/25/85	138M	23D
Shoreham	04/14/73*	07/03/85	146M	19D

* - Dates Indicated are for O.L. Application submittal.

** - Uncontested Proceeding.

SCHEDULE 2

NUREG-1068

Review Insights on the Probabilistic Risk Assessment for the Limerick Generating Station

Manuscript Completed: August 1984
Date Published: August 1984

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

1.4 Use of PRA

Some aspects of the staff's views on the use of the Limerick PRAs have evolved somewhat since the applicant was first requested to perform a PRA as the state of the art of PRA knowledge has advanced.

The staff's initial expression on the usage of the Limerick PRA was set forth in the staff's May 6, 1980 letter to the applicant as discussed in Section 1.2. The staff visited the subject of usage of the PRA again in an affadavit filed with the Atomic Safety and Licensing Board (ASLB) for Limerick on February 1, 1982 in response to an ASLB request. The staff again responded to an ASLB request by filing on April 13, 1983 and May 24, 1983 additional statements on the NRC staff's use of the Limerick PRA in the safety portion of the Limerick proceeding, in the environmental portion of the Limerick proceeding and for additional uses outside the Limerick licensing proceeding. These uses are summarized below.

Use in Safety Portion Of Limerick Proceeding

The Staff used the information that evolved from the review of the Limerick PRA, particularly information concerning risk dominant sequences, to check whether such sequences were attributable to structures, systems, components or procedures which failed to satisfy NRC regulatory requirements. If non-conformances had been identified, then the items involved would have been changed to conform to NRC requirements in order for the necessary licensing findings to be made.

In the event that a dominant risk sequence had been identified which was significant to overall facility safety but was attributable not to a failure of compliance with Commission regulations but to a unique design aspect of Limerick, the Staff would have considered additional measures to compensate for the unique problem.[1]

To the extent that such information has some significant relationship to the Limerick design, the Staff has used information relating to such matters as potentially significant sequences, specific system or component failure data, and containment failure models as derived from its review of other PRA's to test the reasonableness of data and assumptions used in and conclusions resulting from the Limerick PRA. The PRA review supplemented the staff's traditional deterministic safety review.

The staff's Regional Office also some plans usage of the PRA as discussed in a letter to the applicant dated February 28, 1984 to provide a priority ranking of the relative importance to safety of systems and components.

Use in Environmental Portion of Limerick Proceeding

The staff planned to use the information resulting from its review of the Limerick PRA to assess the risk of accidents beyond the design bases, in accordance with the Commission's Statement of Interim Policy Concerning Nuclear Power Plant Accident Consideration Under the National Environmental Policy Act of 1969, 45 Fed. Reg. 40101 (July 13, 1980). The discussion of accidents beyond the design bases in the Limerick Environmental Statements (DES and FES) was along the general lines of such discussions in other Environmental Statements issued since the Commission published its Interim Policy Statement. However, the underlying information in the Limerick DES and FES was case-specific data derived from the Limerick PRA where other recent FES's have used generic information adjusted to the specific case.

[1] Depending on the nature of such unique problem, if any, there are various regulatory provisions which may be applicable: e.g. the implementation of 10 CFR Part 100 has included consideration of compensatory engineered safety features to offset adverse siting characteristics such as large nearby populations.

The staff planned to compare the overall risk of Limerick with the overall risk of other facilities using such information as may be available from other PRA's, including WASH-1400. From this, the Staff planned to assess whether the risk at Limerick is significantly greater than that associated with other reactor facilities in general, giving due consideration to the wide range of uncertainties that may be involved.

If the risk were determined to be within the range associated with other facilities, then the comparison would be of only background use in the Limerick proceeding (that is, in providing perspective on the fact that Limerick has no unusual characteristics). The staff planned a comparison of offsite risks associated with plant accidents, with the risks of normal operation and with the risks associated with other human activities in the area surrounding the Limerick site. The ultimate comparison of significance is whether the environmental impacts of Limerick (including this impact) are outweighed by the benefits of Limerick.

If the risk were determined to be significantly greater than that associated with other reactor facilities, (that is, to be disproportionate), then the Staff would have considered the need to recommend compensatory features.

Additional Uses Outside The Limerick Licensing Proceeding

In addition to the uses to which the Staff put the Limerick PRA in the licensing of Limerick, the Staff is using the Limerick PRA as a part of its general expansion of the scope of PRA knowledge and as a potential source of information concerning safety effectiveness and costs of prevention and mitigation features for the severe accident rulemaking program for proposed new standard plants or other possible severe accident rulemaking activities.

Another purpose for which the PRA was used was as a basis for voluntary improvements in the facility. In fact, the applicant has already used the Limerick PRA as a basis for making voluntary improvements at Limerick.[2]

The Commission has recently stated in its policy statement, Safety Goal Development Program, 48 Fed. Reg. 10772 (March 14, 1983), that the safety goals "will not replace the NRC's reactor regulations [and that the] NRC will continue to use conformance to the regulatory requirements as the exclusive licensing basis for plants." We believe that the Staff's use of the PRA as reflected above is consistent with the Commission's safety goals policy statement.

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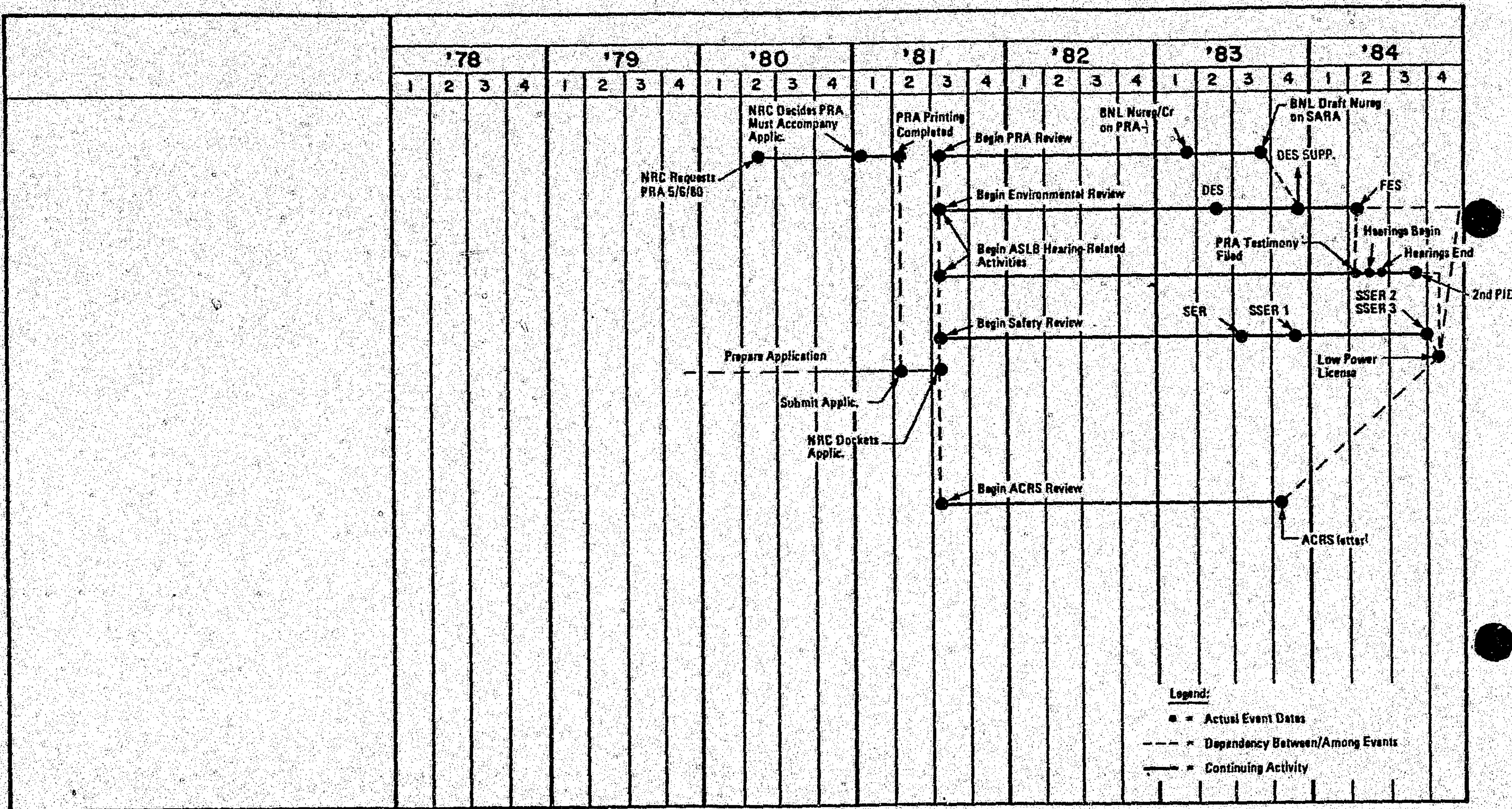
[2] These include: (a) redundant air supplies to the ADS, (b) separate injection nozzles for coolant makeup into the reactor vessel, (c) added crossover valves in the RHR service water systems, and (d) new procedures to enhance the recovery of the power conversion system for containment heat removal. In addition, the Applicant's choice of the ATWS prevention/mitigation fix referred to as Option 3A was aided by information derived from the PRA.

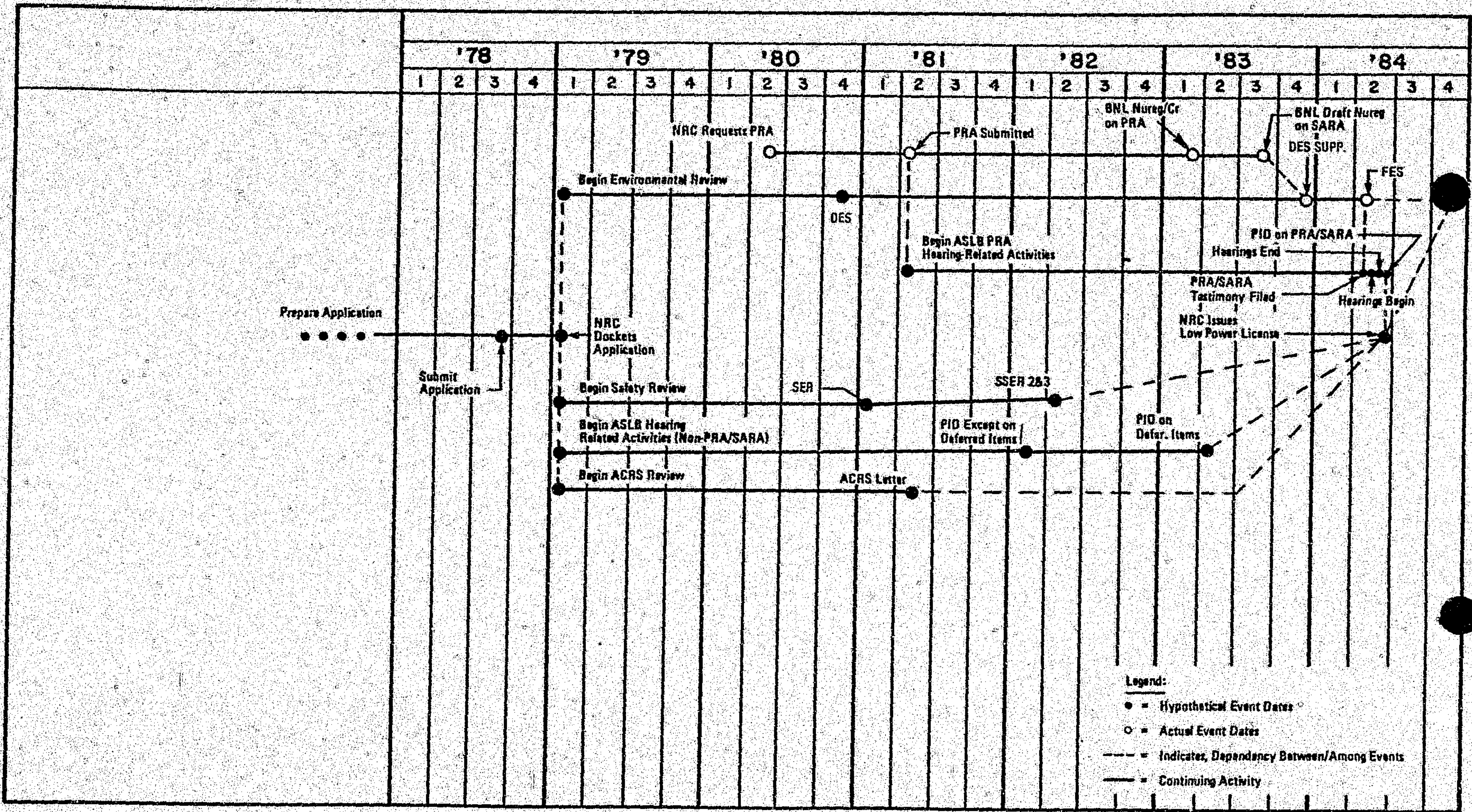
Schedule 3

Limerick Unit 1 As-Built Licensing Schedule



Schedule 4

Hypothetical Earliest Possible Licensing Schedule For Limerick Unit 1



8-4 3-14-86
R-850152
#59

PENNSYLVANIA
PUBLIC UTILITY COMMISSION
Harrisburg, PA 17120

Public Meeting held May 29, 1980

RECEIVED
MAR 17 1986
SECRETARY'S OFFICE
Public Utility Commission

COMMISSIONERS PRESENT:
Susan M. Shanaman, Chairman
Michael Johnson
James H. Cawley
Linda C. Taliaferro

Securities Certificate of Philadelphia Electric Company in the matter of a four hundred million dollar ($400,000,000) Revolving Credit/Term Loan.

S-80054561

ORDER

BY THE COMMISSION:

On May 7, 1980, Philadelphia Electric Company filed this securities certificate seeking Commission registration of a $400,000,000 Revolving Credit/Term Loan.

The proceeds of this financing will provide sufficient funds to meet the minimum requirements when Philadelphia Electric Company cannot obtain the requisite funds through conventional capital markets due to temporarily inadequate coverage ratios or circumstances in the financial community detrimental to the best interest of Philadelphia Electric Company and its ratepayers. This issuance will not be in lieu of a conventional securities offering, but will be used only when access to conventional capital markets is not available on acceptable terms.

The Commission has examined this securities certificate and determines that the issuance of a $400,000,000 Revolving Credit/Term Loan is proper for the present and probable future capital needs of Philadelphia Electric Company, and that the securities should be registered in accordance with Chapter 19 of the Public Utility Code; THEREFORE,

IT IS ORDERED:

1. That Securities Certificate of Philadelphia Electric Company in the matter of a four hundred million dollar ($400,000,000) Revolving Credit/Term Loan, is hereby registered.

2. That Philadelphia Electric Company file with us, within 60 days thereafter, a statement setting forth (a) the date or dates of issuance; (b) the principal amount thereof; (c) the effective interest rate thereon; (d) throughout the entire seven-year life submit quarterly statements indicating (1) the total amount outstanding, (2) the lending bank or banks and (3) the interest rate thereon and (e) a detailed list of the total actual expenses incurred in obtaining the total financed arrangement.

BY THE COMMISSION

William P. Thierfelder
Secretary

ORDER ADOPTED: May 29, 1980

ORDER ENTERED: May 29, 1980

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3-14-86
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R-850052

PENNSYLVANIA
PUBLIC UTILITY COMMISSION
Harrisburg, PA 17120

Public Meeting held April 3, 1981

Commissioners Present:

Susan M. Shanaman, Chairman
Michael Johnson
James H. Cawley
Linda C. Taliaferro

DOCUMENT FOLDER

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MAR 17 1986
SECRETARY'S OFFICE
Public Utility Commission

Securities Certificate of Philadelphia Electric Company in the matter of a European Revolving Line of Credit not in excess of the principal amount of one hundred million dollars ($100,000,000).

S-814655

DOCKETED
MAR 24 1986

O R D E R

BY THE COMMISSION:

On March 2, 1981, Philadelphia Electric Company filed this Securities Certificate seeking Commission registration of a European Revolving Line of Credit not in excess of the principal amount of one hundred million dollars ($100,000,000). Philadelphia Electric Company has voluntarily extended the statutory consideration period to April 3, 1981.

The arrangement of the two-year line of credit will involve a group of banks in Europe and The British Isles. Credit Suisse First Boston Limited, an affiliate of The First Boston Corporation, will act as agent for Philadelphia Electric Company. Neither Credit Suisse First Boston Limited nor The First Boston Corporation is affiliated with Philadelphia Electric. Interest rates for the loans will be based on the London Interbank Offered Rate plus a commitment fee of approximately three-eighths of one percent.

The Commission has examined this Securities Certificate and determines that the obtainment of a European Revolving Line of Credit in the principal amount of $100,000,000 is proper for the present and probable future capital needs of Philadelphia Electric Company, and that the Securities Certificate should be registered; THEREFORE,

IT IS ORDERED:

1. That Securities Certificate of Philadelphia Electric Company in the matter of European Revolving Line of Credit not in excess of the principal amount of one hundred million dollars ($100,000,000) is hereby registered.

2. That Philadelphia Electric Company file with this Commission, within 60 days thereafter, a statement setting forth (a) the actual expenses incurred in arranging the line of credit and (b) details of the terms and conditions of the credit agreement.

3. That Philadelphia Electric Company notify this Commission, within 30 days, of the dates, amounts and effective interest rates of any borrowing under the European Revolving Credit Agreement.

BY THE COMMISSION,

William P. Thierfelder
Secretary

(SEAL)

ORDER ADOPTED: April 3, 1981

ORDER ENTERED: April 3, 1981

Staff Exhibit 39

R-850152
1409
3-14-

PENNSYLVANIA
PUBLIC UTILITY COMMISSION
Harrisburg, PA 17120

Public Meeting held May 13, 1983

Commissioners Present:

Linda C. Taliaferro, Chairman
Michael Johnson
James H. Cawley

RECEIVED
MAR 17 1986
SECRETARY'S OFFICE
Public Utility Commission

Securities Certificate of Philadelphia Electric Company in the matter of establishing a Eurodollar Revolving Line of Credit not in excess of $300,000,000.

S-834918

ORDER

BY THE COMMISSION:

On April 14, 1983, Philadelphia Electric Company filed this Securities Certificate in the matter of establishing a Eurodollar Revolving Line of Credit not in excess of $300,000,000.

Philadelphia Electric Company proposes to arrange a seven-year revolving line of credit with a group of banks located mainly in Europe, the Middle East and Asia. This new revolving line of credit represents a replacement and expansion of Philadelphia Electric Company's $100,000,000 European Revolver which expires on April 6, 1983. Morgan Stanley International, an affiliate of Morgan Stanley & Co., Inc., will act as agent for Philadelphia Electric Company.

Borrowings under the line of credit will be divided into four portions of up to $75,000,000 each. Interest rates for the loans will be based on the London Interbank Offered Rate plus a commitment fee on the unused commitment.

Philadelphia Electric Company may prepay borrowings on any interest payment date in minimum amounts of $25,000,000. Prepayments shall be applied against the most recent borrowing and amounts so prepaid may be reborrowed from time to time until the maturity date.

The Commission has examined this Securities Certificate and determines that the obtainment of a Eurodollar Revolving Line of Credit in the principal amount not in excess of $300,000,000 appears to be necessary or proper for the present and probable future capital needs of Philadelphia Electric Company, and that the Securities Certificate should be registered; THEREFORE,

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DOCKETED
MAR 24 1986

IT IS ORDERED:

1. That Securities Certificate of Philadelphia Electric Company in the matter of establishing a Eurodollar Revolving Line of Credit not in excess of $300,000,000, is hereby registered.

2. That Philadelphia Electric Company file with this Commission, within 60 days thereafter, a statement setting forth (a) the actual expenses incurred in arranging the line of credit and (b) details of the terms and conditions of the credit agreement.

3. That Philadelphia Electric Company notify this Commission, within 30 days, of the dates, amounts and effective interest rates of any borrowing under the European Revolving Credit Agreement.

4. That Philadelphia Electric Company shall not use any of the proceeds from this issuance for further construction of Limerick Unit No. 2, unless granted express approval by the Commission.

BY THE COMMISSION,

Jerry Rich
Secretary

(SEAL)

ORDER ADOPTED: May 13, 1983

ORDER ENTERED: May 13, 1983

Staff Exhibit 70

3-14-86
1109
R-850152

PENNSYLVANIA
PUBLIC UTILITY COMMISSION
Harrisburg, PA 17120

Public Meeting held March 23, 1984

Commissioners Present:

Linda C. Taliaferro, Chairman
Michael Johnson

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MAR 17 1986
SECRETARY'S OFFICE
Public Utility Commission

Securities Certificate of Philadelphia Electric Company in the matter of the Limerick Revolving Credit/Term Loan, not in excess of $800,000,000.

S-845051

DOCKETED
MAR 24 1986

ORDER

BY THE COMMISSION:

On February 24, 1984, Philadelphia Electric Company filed this Securities Certificate seeking Commission registration of the Limerick Revolving Credit/Term Loan, not in excess of $800,000,000.

Philadelphia Electric Company arranged a credit facility with a syndicate of major banks wherein Citibank, N.A. will act as agent bank. The credit facility will provide Philadelphia Electric Company with a four year Revolving Credit agreement followed by a four year amortizing Term Loan which results in an eight year credit facility. Proceeds from this credit facility will provide funds to Philadelphia Electric Company for completion of the Limerick Nuclear Generating Station No. 1, costs associated with suspending the construction of the Limerick Nuclear Generating Station No. 2 and repayment of a portion of its outstanding short-term debt. The above-mentioned transactions will require approximately $600,000,000. The remaining $200,000,000 will be utilized as a contingency fund to accommodate possible unforseen costs.

The Commission has examined this Securities Certificate and determines that the Limerick Revolving Credit/Term Loan, not in excess of $800,000,000 is necessary or proper for the present and probable future capital needs of Philadelphia Electric Company, and that the Securities Certificate should be registered; THEREFORE,

IT IS ORDERED:

1. That the Securities Certificate of Philadelphia Electric Company in the matter of the Limerick Revolving Credit/Term Loan, not in excess of $800,000,000, is hereby registered.

3-14-84
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R-850152

PENNSYLVANIA
PUBLIC UTILITY COMMISSION
Harrisburg, PA 17120

Public Meeting held March 23, 1984

Commissioners Present:

Linda C. Taliaferro, Chairman
Michael Johnson

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RECEIVED
MAR 17 1986
SECRETARY'S OFFICE
Public Utility Commission

S-845051

Securities Certificate of Philadelphia Electric Company in the matter of the Limerick Revolving Credit/Term Loan, not in excess of $800,000,000.

DOCKETED
MAR 24 1986

O R D E R

BY THE COMMISSION:

On February 24, 1984, Philadelphia Electric Company filed this Securities Certificate seeking Commission registration of the Limerick Revolving Credit/Term Loan, not in excess of $800,000,000.

Philadelphia Electric Company arranged a credit facility with a syndicate of major banks wherein Citibank, N.A. will act as agent bank. The credit facility will provide Philadelphia Electric Company with a four year Revolving Credit agreement followed by a four year amortizing Term Loan which results in an eight year credit facility. Proceeds from this credit facility will provide funds to Philadelphia Electric Company for completion of the Limerick Nuclear Generating Station No. 1, costs associated with suspending the construction of the Limerick Nuclear Generating Station No. 2 and repayment of a portion of its outstanding short-term debt. The above-mentioned transactions will require approximately $600,000,000. The remaining $200,000,000 will be utilized as a contingency fund to accommodate possible unforseen costs.

The Commission has examined this Securities Certificate and determines that the Limerick Revolving Credit/Term Loan, not in excess of $800,000,000 is necessary or proper for the present and probable future capital needs of Philadelphia Electric Company, and that the Securities Certificate should be registered; THEREFORE,

IT IS ORDERED:

1. That the Securities Certificate of Philadelphia Electric Company in the matter of the Limerick Revolving Credit/Term Loan, not in excess of $800,000,000, is hereby registered.

2. That Philadelphia Electric Company file with this Commission, within 30 days after each loan obtained through the Limerick Revolving Credit/Term Loan, a statement setting forth (a) the amount, date and maturity period of the loan, (b) the interest rate applicable thereto, (c) a description of the use of the proceeds, and (d) a detailed list of the total actual issuance expenses.

3. That Philadelphia Electric Company shall not use any of the proceeds from this Securities Certificate for further construction of the Limerick Unit No. 2, except as expressly provided for in the Commission's Orders issued in respect to the Limerick Investigation.

BY THE COMMISSION,

Jerry Rich
Secretary

(SEAL)

ORDER ADOPTED: March 23, 1984

ORDER ENTERED: March 23, 1984

PA-ED...
PH 3-19-86
#67

BEFORE THE

PENNSYLVANIA PUBLIC UTILITIES COMMISSION

DOCKET NO. R-850152 (LIMERICK 1)

PHILADELPHIA ELECTRIC COMPANY

RECEIVED
MAR 17 1986
SECRETARY'S OFFICE
Public Utility Commission

DOCKETED
MAR 24 1986

SURREBUTTAL AND EXHIBITS

OF

RANDALL J. FALKENBERG

ON BEHALF OF

PHILADELPHIA AREA INDUSTRIAL ENERGY USERS' GROUP*

*Allied Corporation, Fibers Division
Boeing-Vertol Co.
BP Oil
The Budd Company, Inc.
Liquid Air Corp.
Lukens Steel Co.
Nabisco Brands, Inc.
SDC/A Burroughs Corp.
Smith Kline Beckman Corp.
Sun Refining & Marketing Co.
3M Company
U. S. Steel Corp.

DOCUMENT FOLDER

February 1986

Kennedy and Associates
Atlanta, Georgia

PENNSYLVANIA PUBLIC UTILITIES COMMISSION
DOCKET NO. R-850152 (LIMERICK 1)
SURREBUTTAL OF RANDALL J. FALKENBERG

Q. **Please state your name and business address.**

A. Randall J. Falkenberg, Suite A-1220, 1150 Hammond Drive, Atlanta, Georgia 30328.

Q. **Have you testified previously in this proceeding?**

A. Yes, I was cross-examined on February 14, 1985 on my prepared direct testimony.

Q. **What is the purpose of the testimony you are about to give?**

A. I would like to respond to the testimony of various PECO witnesses concerning the ratemaking proposals and regulatory principles contained in my original direct testimony. I would like to provide the PUC some additional information on sinking fund depreciation in light of Mr. Wroblewski's testimony.

Without restating my entire original testimony, I would like to point out that I stand by my original work and do not find that my reasoning is swayed by the arguments presented by various Company witnesses.

As a result I will concentrate only on the most significant aspects of these witnesses' testimony. However I would like to point out that in several cases it is not necessary for me to rebut the Company witnesses, because they have already presented substantial rebuttal to each other.

SINKING FUND DEPRECIATION

Q. Have you reviewed the testimonies of Mr. Farling and Mr. Wroblewski concerning Sinking Fund Depreciation?

A. Yes, I have. I originally proposed Sinking Fund Depreciation to be computed utilizing a 6% sinking fund rate. I have no substantial objection to adoption of the PP&L style sinking fund depreciation discussed by Mr. Wroblewski. I have verified his calculations and I am submitting Falkenberg Surrebuttal Exhibit 1, a revision to my original Falkenberg Exhibit 3, which details the impact of the Wroblewski calculation of the PP&L style sinking fund depreciation on our modified and corrected phase-in proposal. Under this proposal PECO would be granted a total rate increase of $417 million, an amount not substantially different from my original calculation. This amount would be phased-in as $139 million in year 1, $278 million in year 2, $417 million in year 3, and $556 million in years 4, 5 and 6. Provided that the Commission finds this approach more acceptable then the traditional sinking fund depreciation computed at a 6% rate, I will endorse this approach.

Q. Mr. Wroblewski points out that the modified sinking fund depreciation technique was not used in conjunction with the PP&L phase-in proposal. Do you have a comment on this?

A. Yes, I do. It is interesting to note that the reason that the PP&L sinking fund was not combined with the phase-in proposal was that the Commission rejected the phase-in on the grounds that it was moot point. In other words, the Commission reduced the PP&L claim to such a significant degree that it perceived no need to phase-in the rate increase. I presume that Mr. Wroblewski would prefer to have the rate increase I recommend in my Surrebuttal Exhibit 1 as opposed to the rate increase implied by the PP&L order.

Q. Mr. Farling, PECO's auditor, suggests that sinking fund depreciation is not a generally accepted accounting principle. Would you care to comment on this statement?

A. Yes. I've attached as Falkenberg Surrebuttal Exhibit 2 the auditor's opinion from Pennsylvania Power and Light Company's 1984 annual report. The opinion states that the financial statements of PP&L were prepared in accordance with "generally accepted accounting principles". I note that the auditor of PP&L, Deloitte, Haskins and Sells, has not qualified its PP&L opinion on the basis of utilization of sinking fund depreciation. However, it is pointed out as a note to the financial statement that PP&L did utilize the modified sinking fund depreciation. As a result, I would suggest that adoption of the PP&L style sinking fund depreciation should not cause Mr. Farling any undue anguish in certifying PECO's financial statements. If it does cause Mr. Farling a significant problem, PECO could apparently use Deloitte, Haskins and Sells as an auditor.

DECLINING REVENUE REQUIREMENTS

Q. **Mr. Falkenberg, two PECO witnesses have suggested that your correction to the phase-in proposal is unwarranted because it rests upon the notion that Limerick revenue requirements will decline over the next six years. Mr. Wroblewski and Mr. Hill take issue with your correction to the PECO phase-in. Do you have any comments?**

A. Mr. Hill and Mr. Wroblewski seem to be adopting positions which support the O&M and capital additions projections of Mr. Chernick and Mr. Komanoff. This is in direct contradiction to testimony of other PECO witnesses. In effect, Mr. Hill and Mr. Wroblewski are arguing that the capital additions to Limerick would be so substantial as to offset any decline in rate base due to depreciation expense and deferred taxes. I have not assumed that capital additions will reach such high levels. However, it does appear that the testimony of Dr. Hieronymus is in direct contradiction to the assumptions of Mr. Hill and Mr. Wroblewski. Turning to Falkenberg Surrebuttal Exhibit 3, we see Dr. Hieronymus's calculation of Limerick I requirements during the first 6 years of operation of the plant. As is clearly seen in Dr. Hieronymus's calculations, revenue requirements on Unit 1 and 50% of common plant decline substantially. Under Dr. Hieronymus's assumptions they decline from $857 million in 1986 to $764 million in 1991. This level of cost reduction is comparable to the amount that I predicted in my original Exhibit 2 which assumed unit 1 and 50% of common plant is in included in rate base without sinking fund depreciation. The amount of decline in revenue requirements predicted by Dr. Hieronymus is actually substantially greater than that utilized in my Falkenberg Surrebuttal Exhibit 1 under the modified form of sinking fund depreciation.

Q. **What is the implication for the Limerick cost/benefit analysis if Mr. Hill and Mr. Wroblewski are correct that Limerick rate base wil never decrease through time?**

A. If Mr. Hill and Wroblewski are correct, then the $2 billion benefit of Limerick cited by Dr. Hieronymus would be reduced by over $1.5 billion during the life of the plant. Thus, Dr. Hieronymus' testimony seems to completely contradict the testimony of Mr. Hill and Mr. Wroblewski.

Q. **Mr. Hill testifies that the projection of decline in revenue requirements upon which your correction to the phase-in is based is unwarranted. Mr. Hill testifies that:**

"Finally, the arguments presented by Mr. Falkenberg are not unique to Limerick 1. Over the period of 1974 to 1985, the Company placed 4 nuclear units in service at Peach Bottom and Salem for ratemaking purposes, all of which were reflected in rates in the same manner ..... As Limerick 1. At no time during the period 1974-85 was there any base rate reduction made either by filing of Philadelphia Electric Company or a specific rate order by the Commission".

Please comment on Mr. Hill's testimony.

A. The circumstances of Philadelphia Electric are substantially different today than they were during the period of time from 1974 to the early 1980's. This is not my conclusion, rather this is evident from the testimony of Mr. Joseph F. Paquette, Jr. His testimony points out that several conditions had a severe impact on rates in the 1970's and early 1980's:

"1) Escalating inflationary pressures affected all cost and expenses since 1970 with annual inflation rates consistently were in excess of 5.0% and in the 8.0 to 9.5% range during several periods (1974, 1975, 1979, 1980 and 1981);

2) Interest rates escalated, increasing our costs and decreasing our interest coverage ratios;

3) Coupled with the general inflation was the effect of the Middle East boycott on fuel oil prices beginning in the fall of 1973. The Arab oil embargo drove up fuel prices which pushed up electricity prices;

4) The development of nuclear power as an essential, economical source of generation for growth and oil displacement inherently required more capital. Moreover, as the decade progressed the capital costs of nuclear power plants grew, requiring the attraction of greater and greater amounts of capital. Additional capital attraction requirements were also associated with our coal generating equipment, principally due to the need to add costly new equipment for environmental protection;

5) Environmental restrictions forced the Company to use low sulfur oil which increased costs further;

6) The higher electric prices resulted in reduced consumer demand for electricity."

Many of these factors are no longer present today. We are now in a period of falling oil prices, falling inflation, declining interest rates and most significantly a period of time when PECO does not need to build new nuclear generation in order to economically serve load. If the Company would adopt the economically prudent and sensible course of action of pursuing the alternatives to Limerick Unit 2, the need for future rate increases would be substantially reduced.

Q. Please summarize your points concerning the decline in Limerick revenue requirements.

A. PECO's own witnesses have projected that the revenue requirements from Limerick will decline. Either the revenue requirements will decline or Limerick is much less economically viable than both PECO and I have projected. Secondly, there is nothing inherent in my proposal which would prevent PECO from earning the allowed return on the Limerick plant during the six year phase-in period. In fact, should any increase in cost occur PECO is free at any time to seek additional rate increases to reflect the increased cost of service. Thus there is nothing inherent in my proposal which would result in PECO earning below its allowed rate of return. In effect, all I have done is removed a hidden attrition allowance of significant proportion from PECO's rates. The Commission should adopt this adjustment which amounts to $28 million under the PP&L style sinking fund depreciation. PECO should not be granted a $28 million attrition allowance.

DISCOUNT RATE

Q. Mr. Falkenberg it appears your choice of discount rate is substantially

responsible for your conclusion that Limerick 1 is not a cost effective capacity addition for PECO over its life. Several PECO witnesses, notably Messrs. Hill, Hieronymus and Perl take issue with your choice of discount rate. Please explain the significance of this concept and why you believe that these witnesses are wrong.

A. The discount rate utilized in an economic analysis such as this plays a central role in determining the conclusion of whether the investment alternative is attractive or not. The analysis I utilized an incremental cost of capital as the discount rate. This is common practice within the utility today. According to Mr. Hill, PECO has used the after tax discount rate for all economic studies performed at PECO for the past thirty years.

Q. **What is the justification that PECO uses for its after-tax discount rate?**

A. Aside from Mr. Hill's justification which is that it has been used for 30 years at PECO, the general consensus seems to be that it is the discount rate which is neutral as to accounting conventions. This, in effect, is Dr. Perl's argument for the use of the after-tax discount rate. While this is an interesting point, to some degree, it misses the point. I would like to return to the subject of sinking fund depreciation to illustrate what is wrong with this line of reasoning. In the Limerick 2 proceeding, Dr. Perl testified that the after-tax discount rate was correct because it was neutral as to accounting conventions. He testified that, if, under a higher discount rate an alternative plan looked more attractive, then the Commission could simply change accounting conventions and produce revenue requirements for the nuclear plant which were identical to the alternative. Dr. Perl testified "For example, the Commission could shift from straight line to sinking fund depreciation, or put the plant into rate base gradually over several years." The problem with this approach, of course, is that while different accounting conventions produce identical revenue requirements under the after tax cost of capital, not all accounting conventions are created equal, at least in the eyes of PECO or its auditors. Mr. Farling testified in this case that sinking fund depreciation is not a generally accepted accounting principle. Mr. Wroblewski testified that PECO does not support any form of sinking fund depreciation. Apparently they would not support the view of Dr. Perl or myself that the PUC could simply shift to sinking fund to mitigate nuclear rate shock. The implication of Mr. Farling's testimony, as I read it, is that he might have to qualify his opinion on PECO's financial statement. The only reason I can think of why Mr. Farling would have to do this is that he does not believe that the quality of PECO's financial position as reported, is correct. In effect, he is suggesting that there is some <u>risk</u> that future rates might not be allowed which recover the full amount of sinking fund depreciation over the life of the Limerick plant. Now, according to Dr. Hieronymus and Dr. Perl, the present value of the revenue requirements of Limerick would be the same at the after-tax cost of capital under sinking fund depreciation or straight line. In fact any phase-in, even what might be called a "zero coupon" phase-in (i.e., waiting until the last year of the plant's life and then collecting all of the revenue requirements) could be considered equal under the discount rate used by Dr.'s Perl and Hieronymus. Dr. Perl and Dr. Hieronymus could show Mr. Farling's numerous revenue requirements calculations that would show PECO will collect the same amount of money under either of the depreciation plans I've discussed. With Dr. Hieronymus and Perl convinced that PECO is getting the same amount of money under sinking fund depreciation why should Mr. Farling be concerned about the correctness of the financial statements? The answer is quite simple. Mr. Farling has a higher discount rate than Dr. Perl or Dr. Hieronymus. His discount rate is apparently equal to an after-tax cost of capital plus some additional risk premium which accounts for the uncertainty of future cost recovery. In effect, Dr. Perl's and Dr. Hieronymus' calculation of revenue requirements overstates the value of those revenues in Mr. Farling's mind. Considering that Mr. Farling is the representative of the shareholders who is supposed to see that the books are kept straight, it is not surprising that he would be reluctant to put off into the future the benefit of regular amounts of straight line depreciation. He would rather have it all today, even if Dr. Perl and Dr. Hieronymus contended that the revenue requirements collected under straight line or sinking fund depreciation were identical.

In effect it is Mr. Farling's contention that PECO's financial statements might be qualified, that is the ultimate proof that the after-tax discount rate is inappropriate. In my view there is much less uncertainty as to the future collection of sinking fund depreciation for the shareholders, than there is of the projected future benefits of the Limerick Nuclear Plant for the customers.

I am adopting an approach similar to Mr. Farling's. I am suggesting that a discount rate higher than the after-tax cost of capital be utilized. In the case at hand, I am utilizing the industry norm, the pretax incremental cost of funds.

Mr. Farling's testimony is interesting in light of Dr. Perl's recent testimony. I believe that if Dr. Perl had been honest with us, for example, in the Limerick 2 proceeding, he would have said that the "Commission could switch to sinking fund depreciation or adopt a phase in proposal. However, at that point in time PECO would bring in witnesses such as Mr. Farling who would dispute the use of these approaches and the Company would argue that they were not a good idea." Thus, if you accept the notion that reasonable changes in accounting conventions, which the Company won't support, will be made then you can accept the after-tax discount rate proposal of Dr. Perl and Dr. Hieronymus. If you believe these accounting changes won't be made without extreme resistance from PECO you are left with choice of a higher discount rate. I find it disappointing, however, that these two gentlemen can testify that the after-tax discount rate is the proper rate. However, when the expensive new capacity comes into rate base, the utility companies which they represent never seem to be in favor of a phase-in longer than a few years and seldom favor adoption of reasonable accounting convention such as sinking fund depreciation. It is disingenuous for Dr. Perl and Dr. Hieronymus' to support the use of the after-tax discount rate on the basis that it is neutral to accounting conventions. Their clients certainly are not neutral to accounting conventions.

Q. You have stated that the after-tax discount rate is not the industry norm and that the pretax discount rate is. However, the EEI survey which you presented in your original testimony does not use the term "discount rate". Please comment.

A. I have examined the questionnaire which accompanied the EEI survey and reread the survey. I do not find the term "discount rate" utilized anywhere in the survey. However, the survey is absolutely absurd unless the term "cost of capital used for internal economic evaluation" is interpreted as the discount rate. The reason for this is that it makes absolutely no sense to interpret the after-tax cost of capital as the cost of capital for internal revenue requirements analysis in any sense other than as a discount rate. The reason is that any revenue requirements analysis takes the cost of capital and adjusts it for the fact that debt interest is an expense while equity is taxable. Mr. Hill and Dr. Hieronymus should know this very well. These gentlemen are simply hiding behind semantics in order to protect their position. As further evidence of the absurdity of the position taken by PECO witnesses on this point I would like to quote from a brief written by Northeast Utilities in 1985 concerning the proper discount rate (note that Northeast Utilities uses the pre-tax discount rate):

"The order states that the use of pre-tax or after-tax discount rates is a highly controversial issue within the utility industry and that both approaches are used. The order fails to recognize, however, that unrefuted record evidence shows that a large majority of utilities employ the pre-tax rate. Late Filed Exhibit - 8."

The evidence to which Northeast Utilities was referring was the EEI survey. Northeast Utilities could be considered to be an outside, unbiased observer of what this survey means, and they clearly interpret it as I do: it is discussing the discount rate. The position of Dr. Hieronymus, that the survey simply requests the inputs to various utility revenue requirements models is downright silly at best and insincere at worst.

Q. What implications would using PECO's stated discount rate have for the analysis which you performed previously?

A. I have performed my analysis using Mr. Paquette's response to the EEI survey. Thus, the analysis I performed now tests whether Mr. Paquette would have considered Limerick to be cost effective utilizing the information he had provided to the Edison Electric Institute. The results of this analysis are provided in Falkenberg Surrebuttal Exhibit 4. It shows that under this analysis using a 14.52% cost of capital and the after-tax discount rate of 11.3% that Limerick 1 and common result in over $600 million in higher rates for customers over the life of the plant. Thus using the Company's after-tax discount rate and the cost of capital which PECO uses for its internal economic evaluations, the Company would not consider Limerick to be a cost effective addition. Note that I have utilized (but don't adopt) the benefits and O&M expenses computed by Dr. Hieronymus. Thus, there should be no dispute as to the difference between my results and those which PECO would obtain.

Q. What are some of the other problems with the after-tax discount rate?

A. One of the key problems with the after-tax discount rate is that it imputes a tremendous benefit from the tax deductibility of debt interest to the present value of the benefits created by the project. In effect, the debt used to finance the project is an expense which is simply passed through to customers. Thus, in computing the after-tax discount rate and applying it to the capital charges, one finds a discount rate which is, as stated, neutral to accounting conventions. However, applying this same discount rate to avoided capacity or energy costs is a highly dubious procedure which produces some counterintuitive results.

Q. Please explain.

A. Falkenberg Surrebuttal Exhibit 5 shows my calculation of Limerick costs and benefits under assumptions essentially identical to those utilized by Dr. Hieronymus. Under these conditions my model predicts lifetime benefits of $1928 million over its life while Dr. Hieronymus predicts $1956 million. This is utilizing PECO's 12.7% cost of capital and the after-tax discount rate. The Exhibit shows case 2, which computes the costs and benefits using the after tax discount rate and cost of funds obtained from the Paquette response to the EEI survey. Under his assumptions the Limerick plant now produces lifetime benefits of only about $413 million. Thus, simply correcting for the cost of capital substantially reduces the benefits Dr. Hieronymus would perceive in the Limerick plant. However, some surprising results emerge if one speculates as to possible changes in federal tax laws. It has been suggested that marginal tax brackets be lowered for corporations from 46% to 35%, for example. If one performs the analysis using a 35% marginal tax bracket, instead of the current figure, the present value of benefits of Limerick decline from $413 million to $384 million over the life of the plant. Under this analysis I have assumed for the sake of argument only, that the benefits in avoided capacity and energy cost don't change any as a result of the change in tax code. Thus, what has happened is simply the cost of Limerick has been reduced because the taxes associated with the plant have been reduced. President Reagan suggested on at least one occasion that corporate income taxes might be eliminated entirely. Thus, I have run another scenario utilizing a 0% income tax rate. The interesting result is that rather than producing a $413 million benefit over its life under all of PECO's assumptions, the plant would produce only a $10 million benefit over its life. In every year before 2022 the plant would produce substantial losses to customers on a present worth basis. The absurdity of this result points out the unreasonableness of the use of the after-tax discount rate. In these analyses all that has happened has been that the cost of the Limerick plant to customers has been reduced. None of the benefits have been reduced in nominal terms. Thus, one is left to question the following: if tax laws changed in such a way to reduce the cost of Limerick, why is it that the lifetime benefit of the plant would be reduced? The answer is simply that the after-tax discount rate has increased. In effect, by reducing the discount rate for the tax deductibility of debt interest, we are giving much greater credit to avoided capacity and energy cost savings created by the plant in future years. In other words, the future is worth more to us if we have to pay high income taxes. This is just the opposite of what many people believe. It is often asserted by supply side economists that reducing marginal tax rates will stimulate investment in productive capacity. This seems to be the opposite conclusion one would draw from careful analysis of the economics of the Limerick nuclear plant. These results are hard to reconcile with common sense and clearly leads one to question the use of the after-tax discount rate.

Q. **Do you have any further evidence that supports your contention that the majority of American utility companies use the pre-tax discount rate?**

A. Yes. Falkenberg Surrebuttal Exhibit 6 shows a portion of the Technical Assessment Guide prepared by EPRI. The report states that EPRI uses the incremental cost of capital as a discount rate because it is in more general use in American utilities.

Q. **Please summarize your testimony concerning discount rates.**

A. The testimony I have given illustrates clearly that PECO's auditor is unimpressed by the notion that the after-tax discount rate is neutral to accounting conventions. PECO's auditor seems to prefer generally accepted accounting principles and not simply accounting principles which produce the same revenue requirements under the after-tax discount rate. Application of Mr. Paquette's cost of capital to Limerick plant even using the Company's net of tax approach would produce results showing that Limerick 1 is not a cost effective addition over its life. In addition, it is clear from the interpretation of one other utility company and myself that the EEI survey was discussing the discount rate and not simply an input to a revenue requirement model. Finally, the absurdity of the use of the after-tax discount rate was vividly illustrated by showing that using this discount rate under reduced levels of income taxes, actually reduces lifetime benefits of the Limerick plant under company assumptions. From this discussion it is clear that the methodology proposed by the Company produces unreasonable results.

GUTH, HOCH AND PERL REBUTTAL TESTIMONY

Q. Have you reviewed the rebuttal testimony of Dr. Perl concerning the capital cost estimate of the Limerick Plant?

A. Yes, I have. Dr. Perl's analysis is quite unimpressive and simply misleading. Dr. Perl has redone my regression analyses and shown that even making different assumptions and including different data, when he performs the analysis <u>correctly</u>, he simply finds that PECO underestimated the cost of Limerick based on historic trends. However, in an apparent attempt to simply produce a result lower than the Company projected in 1978, Dr. Perl simply eliminates a significant variable from his regression. This can be seen in his testimony on schedule 23. The COD-squared variable has a higher degree of significance than the variable which Dr. Perl includes in his regression analysis. Dr. Perl suggests that this variable should be removed because it is related to the COD variable. However, this variable is significant for the following reason: it shows that the annual increase in capital costs for nuclear plants was accelerating through time. In essence this variable is necessary to measure the trend towards substantial increases in the capital costs of nuclear plants. There is no rational basis for excluding the significant variable from an analysis such as this without replacing it with a variable which is designed to compensate its effect. In effect, what Dr. Perl has shown is that if done improperly, regression analysis will produce unreasonable results. I agree with this, and I am sure Dr. Perl agrees as well.

Q. **What specifically is Dr. Perl's complaint about your analysis, and do you have a remedy?**

A. Dr. Perl is bothered by the fact that COD and COD-squared variable are related. Thus, he simply drops the COD-squared term and obtains his erroneous result. Falkenberg Surrebuttal Exhibit 7a shows my calculation of Limerick cost projections circa 1978 under Perl's linear regression approach. The indicated result is $1285/KW. However, inspection of the data suggests an alternative specification using a single time related variable was more reasonable. I used a variable equal to (COD-1970) squared. This single variable properly accounts for the decline in costs until 1970 and the rapid increase thereafter without using two related variables in the model. The new model has vastly superior statistics (R-square of 86.2 vs. 77.8 and standard error of $77.2/KW vs $97.8/KW) to Dr. Perl's linear model. These statistics are comparable to my original 2 variable model and yield a prediction of $2,590/KW, slightly higher than my original result. Thus, correcting for Dr. Perl's concern and properly re-estimating the model produces a result almost twice as high as Dr. Perl's erroneous result.

Q. **Do you have any comments regarding the testimony of Mr. Guth and Mr. Hoch?**

A. These witnesses have misinterpreted my testimony. I did not imply PECO should have relied exclusively on trend line modeling for load forecasting in the 1970's. I simply implied it is a useful yardstick to measure PECO's forecasts against. Mr. Guth has done the same thing. We are both attempting to find out to what extent bad luck prevented PECO from producing more accurate forecasts.

It is not an unusual expectation to believe that past trends will continue for some time into the future. To the extent that past trends do not persist, then decisions based on the assumption of those continued trends will prove to be wrong. This can perhaps justifiably be considered bad luck. In the case at hand, PECO's load falling below or above that predicted by past trends is bad luck whether the result is negative or positive. The analysis performed was designed to find out how different PECO's forecasts were from historic trends. The analysis which I performed suggests that there were critical periods of time when PECO's forecasts were substantially worse than those produced by other methods available at the time. Thus, it is simply incorrect for PECO to state they couldn't have done a better job of load forecasting than they did. In effect I have shown that a very simple approach could have been used which would have produced results which were more reasonable vis-a-vis history. The utilization of this trend technique was commonplace in the utility industry. Attached as Falkenberg Surrebuttal Exhibit 8 is a portion of the 1970 Federal Power Commission report entitled The Methodology of Load Forecasting. The report clearly states that extrapolation or trending had proven to be a successful form of forecasting up to that point in time. Thus there was no inherent reason to suspect it would not at least provide a reasonable basis for comparison.

Q. **PECO witness Hoch states that trending has been rejected by many Public Service Commissions as being the worst method. He also cites a PUF article that suggests the end use technique is the best approach. Please comment.**

A. I don't disagree that end use techniques have proven more successful in forecasting residential sales than trending or econometric models. I also don't dispute that some Commissions don't believe trending provides an adequate means of forecasting. However, these are recent developments. The period of time in question, the early 1970's, was a time when trending was not as widely criticized and when end use techniques were not widely available within the industry. To a large extent these criticisms are simply PECO's means of injecting hindsight into the process. In effect what PECO is saying is that because an article published in 1985 suggests that trending has not turned out to be as effective as end use forecasting, then PECO's use of forecasts that were substantially different from past trends in the early 1970's was acceptable. In effect what PECO is doing, as Dr. Hieronymus would describe it, is using hindsight as a filter to interpret past events.

Q. What about Mr. Guth's contention that PECO struggled for a period of time before adopting better forecasting techniques?

A. Mr. Guth seems to be implying that he agrees with my analysis. He states that his analysis shows that PECO's forecasts were higher than proceeding trend data suggested from at least 1972 to 1977 or 1978. What seems to be different is Mr. Guth's degree of tolerance. Mr. Guth seems to think it is acceptable for a company to struggle for 5 years with the fact that load is not materializing as expected. I don't have a problem with the idea it takes time for a company to respond to the types of changes which occurred in the utility industry in the 1970's. However, 5 years is a long time to wait, particularly in light of load forecasts which predicted growth the first year of each forecast to be 2 or 3 times higher than the actual level achieved during the prior 10 year period.

One is led to question why the forecasts were so high at that time. I believe examination of Mr. Hoch's testimony explains part of the problem. Mr. Hoch testifies that

"On top of our history of a steady growth, we were experiencing a rapid growth in air conditioning load which seriously affected our summer peak, and we were suffering from capacity shortages. This experience led us to project that our sales on load would go faster than the historic trend." (emphasis added)

One is led to question why capacity shortages would influence the load growth projection. In effect what Mr. Hoch is saying, is the Company was afraid it wasn't going to have enough capacity in the short run to meet demand. To compensate the Company raised its forecast above the level they expected. In addition, Mr. Hoch's testimony regarding the fact that load was growing rapidly due to air conditioner saturation suggests another one of the reasons why the forecast might have been biased. Air conditioning saturation advanced rapidly in the 60's. However, once people had an air conditioner and had their houses sufficiently chilled they didn't need to go out and buy an additional air conditioner. By projecting a growth greater than the historic figures, PECO in effect was suggesting that air conditioner purchases would accelerate rather than decline as saturation took place.

Q. Please summarize you comments concerning PECO'S load forecast.

A. I believe that the PECO witnesses have misinterpreted my testimony on this subject. The testimony I have given was not designed to suggest that PECO should have relied exclusively on trend models to predict load growth in the 1970's or even today. However, the analysis which I have performed suggests that PECO's load growth projections were inconsistent with experienced trends. The argumemts put forth by PECO's witnesses suggest that unforeseen events i.e. events which could not have been predicted, were the primary reason for PECO's load forecasts turning out to be too high. The analysis which I have performed clearly establishes that PECO'S forecasts would have been much too high even had no unforeseen events taken place and past trends continued. I have shown that PECO's errors in forecasting can be broken into two components. One component which I will call bad luck is the result of unexpected events taking place. The second component is simply avoidable forecast error due to mistakes in judgment. During a critical period of Limerick planning, PECO forecasts were in error, due to avoidable errors, not due to bad luck alone.

THE PRUDENCE STANDARD

Q. Have you reviewed the rebuttal testimony of Dr. Hieronymus and Perl concerning the prudence standard?

A. Yes, I have. There are some substantial flaws in the reasoning of these witnesses. Dr. Hieronymus' view of prudence seems to stem from his notion of rate of return regulation. Dr. Hieronymus is suggesting that because PECO subjects itself to rate of return regulation, it is insulated from the risk of making a bad investment. What Dr. Hieronymus seems to suggest is that only if PECO did not have rate of return regulation, would it be reasonable for its owners to take the risk of making bad investments. Of course, PECO has rate of return regulation because it is a monopoly. According to Dr. Hieronymus, if we're agreeable to allowing PECO to charge any rates that they desired, then the Company will take the risk of making bad investments. Of course, if PECO could charge any rates they wanted, then there would also be little risk of ever having the burden of a bad investment placed upon the shareholders. Thus the fundamental premise of Dr. Hieronymus' prudence theory is unsound. At the very best Dr. Hieronymus could only argue that the level of return that has been allowed, needs to be raised in order to reflect the degree of risk placed upon the Company. It is my testimony that the level of return that PECO is requesting is supposed to reflect the risk placed upon the Company in the market place. Inasmuch as the Pennsylvania PUC has, at least according to Dr. Hieronymus, for the last 7 years now adopted a "used and useful" philosophy for ratemaking, it seems that the marketplace must already be placing this risk premium upon PECO.

Q. **Dr. Hieronymus agrees that he has misinterpreted the implicit contract between customers and investors, at least as it has been interpreted in Pennsylvania.**

However, he seems to think that the contract didn't change dramatically until the PP&L excess capacity order in 1983. Do you agree?

A. No, I do not. Under Dr. Hieronymus' view that prudence only is the appropriate standard, it would appear that in 1979 when Three Mile Island went out of service, Metropolitan Edison could have been allowed to accumulate AFUDC on the undamaged reactor. Under Dr. Hieronymus' vision of prudence, TMI, being a prudent investment, would have been entitled to a return. Since the plant was not used and useful the Commission might not have allowed a current return. Certainly if prudence was the only standard, the Commission would have allowed a deferred return on the unit. The fact that the Commission has not allowed a current or deferred return for these past several years suggests that at least since 1979 the Commission has taken a stand that the used and useful principle carries more weight than the prudence principle.

Q. **Dr. Hieronymus seems to contend that what he calls a "quasi-prudence" test which you propose is too easily failed. Please comment.**

A. I disagree completely. I believe that Dr. Hieronymus prudence standard is much too easily passed. According to Dr. Hieronymus the only way that one can fail the prudence test is to have acted unreasonably. Thus it doesn't matter if PECO's management was pursuing a <u>questionable</u> course of action, a <u>risky</u> course of action, or a <u>dubious</u> course of action. All that matters is that it simply wasn't <u>unreasonable</u>.

In order to demonstrate the significance of this I would like to point out some of the falacies in Dr. Hieronymus' standard. Under Dr. Hieronymus' standard, it would be almost impossible for anyone ever to be held to blame for an accident while driving in excess of the speed limit. While traffic laws require that individuals who exceed the speed limit pay penalties, under Dr. Hieronymus' standard it would appear that the claim could be made that the speeders aren't unreasonable. After all, every day of the year people drive in excess of the speed limit and don't have accidents. Under Dr. Hieronymus' approach, the driver who was involved in an accident while speeding would most likely be able to avoid liability claims. I would be quite interested in seeing Dr. Hieronymus testifying in such a case. It would appear that Dr. Hieronymus would say that only if it could be proven that the same driver, while driving within the speed limit, would not have become involved in the accident could the individual be considered liable. If Dr. Hieronymus could prove that the driver couldn't stop in time while driving within the speed limit then he would not be liable for economic damages. While the individual might be guilty of a traffic charge of speeding, he could not be liable for any <u>economic</u> damages for the accident. The reason of course is that it is not <u>unreasonable</u> to believe that it's possible to drive safely in excess of the speed limit. Thousands of people do it every day and don't get involved in accidents.

The analogy to PECO is relevant. PECO utilized load forecasts which were substantially higher than historic experience would have suggested. PECO utilized capital cost assumptions which were substantially lower than historic data would have suggested. PECO, has at least in the Limerick 2 investigation, assumed that nuclear plant O&M expenses would hold constant in real dollars. PECO uses the discount rate which is "accounting neutral" and favorable to Limerick. However, the problem ratepayers have is that PECO is not required to assume responsibility for any of their projections. In effect, PECO has nothing to lose by projecting, for example, that nuclear O&M expenses will remain constant in real dollars. It may not be unreasonable to estimate that any cost will remain constant in real dollars. When oil prices were going through the roof, there were always those who said they might fall, for example. There were others who said they might go up at the rate of inflation. Thus by making a series of assumptions favorable to the desired course of action, i.e. completion of the plant, PECO was able to produce a set of projections which show the benefits outweighted the costs of the facility in question. When the rate case arrives and PECO files for an increase in rates to recover the cost of the plant, it no longer feels bound by any prior projections. Case in point: while PECO testified in the Limerick 2 proceeding that nuclear plant O&M expenses would not rise in real terms, as the basis for the claim in this PECO rate case it has included a real rate of escalation in its O&M expenses for Limerick. In addition, PECO is no longer "accounting neutral" concerning sinking fund. While the Company can support any one of their assumptions as being reasonable or at least not unreasonable, the fact remains that the project has turned out to be an economic failure for the

Company and the customers. Thus the Company now seeks to include the cost of the plant in rates, on the basis that its projections were not imprudent or more precisely that the Company's projections were not unreasonable. Dr. Hieronymus' testimony regarding prudence may be summarized as follows:

"As long as we are not unreasonable or imprudent, then we are presumed to be prudent. While our projections may be irresponsible, and we may not live up to our previous rhetoric about accounting conventions, we can not be proven unreasonable. In effect, the reward for not being unreasonable is that we get all the money we want when we want it."

I don't believe that this is a reasonable ratemaking standard for the Commission to utilize. Prudence is not a haven for the irresponsible or insincere. Truth in advertising laws are in effect to prevent situations of this sort.

Q. Please summarize your rebuttal testimony.

A. I stand by my original presentation. The criticisms leveled by PECO witnesses either missed the point of my testimony entirely or are simply logically incorrect. The sinking fund depreciation proposal presented by Mr. Wroblewski is acceptable to me and results in only a slight change in my recommended rate increase. The arguments raised by Company witnesses regarding the decline in revenue requirements, the proper choice of discount rates, load forecasting, and the prudence standard are simply incorrect. What is more interesting is that in fact many of the Company witnesses testimonies contradict and rebut the arguments presented by PECO in this case.

Q. Does this conclude your testimony?

A. Yes.

Randall J. Falkenberg

Randall J. Falkenberg

State of Georgia
County of Fulton

Subscribed and sworn to before me, a notary public in and for the State and County aforesaid.

My commission expires

MY COMMISSION EXPIRES SEPT. 12, 1988

This 27th day of February 1986

Notary Public

BEFORE THE

PENNSYLVANIA PUBLIC UTILITIES COMMISSION

DOCKET NO. R-850152 (LIMERICK 1)

PHILADELPHIA ELECTRIC COMPANY

EXHIBITS

OF

RANDALL J. FALKENBERG

ON BEHALF OF

PHILADELPHIA AREA INDUSTRIAL ENERGY USERS' GROUP*

*Allied Corporation, Fibers Division
Boeing Vertol Co.
BP Oil
The Budd Company, Inc.
Liquid Air Corp.
Lukens Steel Co.
Nabisco Brands, Inc.
SDC/A Burroughs Corp.
Smith Kline Beckman Corp.
Sun Refining & Marketing Co.
3M Company
U. S. Steel Corp.

February 1986

Kennedy and Associates
Atlanta, Georgia

Falkenberg Surrebuttal Exhibit 1

Modified and Corrected Phase-In with 50% Common Plant
Using Witness Wroblewski's PP&L Sinking Fund Depreciation

Year	Limerick Fix Charge	Other Change	Total Revenue Reqmt.	Peco Phase-In	Corrected Phase-In
					696
1986	724	279	445	148	139
1987	707	279	428	297	278
1988	695	279	416	445	417
1989	688	279	409	593	556
1990	682	279	403	593	556
1991	678	279	399	593	556
Total 1986:1991	4174	1673	2500	2670	2500
Six Year Average	696				

Auditor's Opinion in PP&L 1984 Annual Report

Notes to Financial Statements

1. Summary of Accounting Policies

Accounting Records

Accounting records are maintained in accordance with the Uniform System of Accounts prescribed by the Federal Energy Regulatory Commission (FERC) and adopted by the Pennsylvania Public Utility Commission (PUC).

Associated Companies

Investments in unconsolidated subsidiaries (all wholly owned) and in Safe Harbor Water Power Corporation (of which the Company owns one-third of the outstanding capital stock representing one-half of Safe Harbor's voting securities) are recorded using the equity method of accounting. Unconsolidated subsidiaries operate in the United States and are engaged in coal mining, holding coal reserves, oil pipeline operations and real estate investment.

The Company believes that its financial position and results of operations are best reflected without consolidation of these subsidiaries since they are not engaged in the business of generating or distributing electricity. All unconsolidated subsidiaries considered in the aggregate would not constitute a "significant subsidiary" as that term is defined by the Securities and Exchange Commission.

Utility Plant and Depreciation

Additions to utility plant and replacement of units of property are capitalized at cost. The cost of units of property retired or replaced is removed from utility plant accounts and charged to accumulated depreciation. Expenditures for maintenance and repairs of property and the cost of replacement of items determined to be less than units of property are charged to operating expenses.

For financial reporting purposes, depreciation is computed on a straight-line method using rates based on the estimated useful lives of property, with the exception of the Susquehanna nuclear plant which is depreciated on a modified sinking fund method, which method is also used for rate-making purposes. Provisions for depreciation, as a percent of average depreciable property, approximated 2.5% in 1984, 2.7% in 1983 and 3.3% in 1982.

Cost of Decommissioning Nuclear Plant

An annual provision for decommissioning costs of the Susquehanna nuclear plant equal to the amount

Auditors' Opinion

Deloitte
Haskins+Sells
Certified Public Accountants

One World Trade Center
New York, New York 10048

To the Shareowners and Board of Directors of Pennsylvania Power & Light Company:

We have examined the balance sheets of Pennsylvania Power & Light Company as of December 31, 1984 and 1983 and the related statements of income, earnings reinvested, and changes in financial position for each of the three years in the period ended December 31, 1984. Our examinations were made in accordance with generally accepted auditing standards and, accordingly, included such tests of the accounting records and such other auditing procedures as we considered necessary in the circumstances.

In our opinion, such financial statements present fairly the financial position of the Company at December 31, 1984 and 1983 and the results of its operations and the changes in its financial position for each of the three years in the period ended December 31, 1984, in conformity with generally accepted accounting principles applied on a consistent basis, after restatement for the change, with which we concur, in the method of accounting for leases as described in Note 2 of the financial statements.

Deloitte Haskins & Sells

February 4, 1985

Source: Auditor's Report in PP&L 1984 Annual Report

FALKENBERG SURREBUTTAL EXHIBIT 3

Hieronymus Calculation of Decline in Limerick 1
and 50% Common Revenue Requirements.

	Year	Orig. Cost Additions Carrying Charges (a) (1)	Station O&M (2)	Other O&M (3)	Total Costs (4)x(1) +(2)+(3)
1	1986	$747.27	$79.01	$31.24	$857.52
2	1987	$713.90	$85.33	$31.62	$830.85
3	1988	$685.60	$93.01	$32.98	$811.59
4	1989	$660.09	$101.39	$39.96	$801.44
5	1990	$635.23	$110.51	$33.90	$779.64
6	1991	$610.30	$117.14	$37.10	$764.54

FALKENBERG SURREBUTTAL EXHIBIT 4
Limerick 1 and 100% Common Plant
Cost/Benefit Analysis using Paquette EEI Survey
Response and After Tax Discount Rate

	Cumulate Net P.V.R.R. (1) (millions)
1990	-2766
2000	-3075
2010	-1967
2022	-619

1. A negative value indicates Limerick produces higher rate. A positive value indicates Limerick produces lower rates. This exhibit shows that by using Mr. Paquette's response to the EEI survey and the after tax discount rate, Limerick 1 and 100% of Common results in a $619 million penalty to customers' rates over the life of the plant.

FALKENBERG SURREBUTTAL EXHIBIT 5
Limerick 1 and 50% Common Plant
Cost/Benefit Analysis at Various Tax Rates
Using After Tax Discount Rate

Case	1986-2022 PVRR 1.) (millions)	
12.7% Cost of Funds	1928	(Hieronymus Result 1956)
Paquette EEI Cost of Funds 50% Tax Rate	413	
Same as 2. with 35% Tax Rate	384	
Same as 2. with 0% Tax Rate	11	

A negative value indicates Limerick produces higher rates.
A positive value indicates Limerick produces savings.
In these examples, using assumptions highly favorable to PECO, the lifetime benefits of Limerick decline substantially as taxes on the plant are reduced. This shows the unreasonable results obtained from use of after tax discount rate.

Falkenberg Surrebuttal Exhibit 6

TAG™
Technical Assessment Guide

Keywords:
Alternatives
Evaluation Methods
Utility Economics
Fuel Prices
Cost Data
Performance Data

EPRI

EPRI P-2410-SR
Special Report
May 1982

ELECTRIC POWER RESEARCH INSTITUTE

candidate technology if the revenue code is substantially revised. For convenience, the illustrative tables appended to Chapter B-3 of the yellow pages and Appendix A of the white pages that are to be used in economic evaluations performed for EPRI, include tables both with and without the tax preferences.

While a government owned utility system does not pay income taxes, a payment in lieu of income taxes is often included in carrying charges. Accordingly, the carrying charge tables in Appendix A (white pages) and those appended to Chapter B-3 for illustrative purposes (yellow pages) both with or without the tax preferences can be used for all types of utility systems.

B-2.5 PROPERTY TAXES AND INSURANCE

In this Guide, property taxes and insurance are assumed to be levelized at 2% of the installed cost of an investment item and are included as a carrying charge.

B-2.6 DISCOUNT RATE

The discount rate to be used in present value calculations is related to the weighted cost of capital. Most utilities use a discount rate equal to the weighted cost of capital, but some use an "after tax cost" equal to the weighted cost of capital less the tax rate times the debt return. Neither method is completely appropriate under all circumstances for all electric utilities. A discount rate equal to the weighted cost of capital has been selected for use at EPRI because of its more general use in the electric utility industry.

For illustrative purposes in the yellow pages, the weighted cost of capital is assumed to be 10 percent and is reflected in the tables appended to Chapter B-3. The actual weighted cost of capital to be used in all economic evaluations performed for EPRI is reflected in Appendix A of the white pages.

B-2.7 FURTHER DISCUSSION OF ESCALATION AND INFLATION

Section B-1.3 introduced the general concepts of escalation and inflation and Section B-2.4.2 discussed return on investment and cost of money. This section will present the effect of escalation and inflation on the cost of money.

The discount rate is dependent upon the current money market which has experienced a general increase in inflation for a number of years. In general, FALKENBERG SURREBUTTAL EXHIBIT 7a

Regression Equation Relating Total Cost Per KW
of Nuclear Plants Completed from 1968 to 1978
to Commercial Operation Date and other Factors

Variable	Variable Mean	Regression Coefficient	t-Statictic
Installed Cost $/KW (1)	-	-	-
Constant	-	-5683.6173	-
C.O.D.(2)	74.1973	81.8536	11.284
INITIAL/ADD-ON (3)	0.7059	153.5125	4.997
MW (4)	808.3529	-0.216	-2.209
Northeast (5)	0.3725	88.4733	3.103

Number of Observations

Adjusted R Squared = .7782
R Squared = .7959
Multiple R = .8921

Standard Error of Est. = 97.7677
Predicted Limerick Cost $/kW = 1285.

Sources and Notes

1. Installed cost per KW is measured in total installed cost (including AFUDC) per KW of capacity. Source is Alabama Power Co.'Power Plant Cost Trends' -January 1984 and 1983 TVA Survey of Nuclear Plant Costs.

2. C.O.D. is year and month of commercial operation; source TVA and APCO surveys.

3. Initial Add-On Indicator = 1 for First Unit at Site, 0 for subsequent units.

4. Gross Capacity in Megawatts for the unit.

5. Northeast Indicator = 1 for Utilities in FERC region 1, 0 otherwise.

FALKENBERG SURREBUTTAL EXHIBIT 7b

Regression Equation Relating Total Cost Per KW
of Nuclear Plants Completed from 1968 to 1978
to Commercial Operation Date and other Factors

Variable	Variable Mean	Regression Coefficient	t-Statictic
Installed Cost $/KW (1)	-	-	-
Constant	-	91.8233	-
(COD - 1970) xx2 (2)	23.6221	9.6528	15.231
INITIAL/ADD-ON (3)	0.7059	128.8656	5.347
MW (4)	808.3529	-0.1009	-1.417
Northeast (5)	0.3725	72.9107	3.244

Number of Observations

Adjusted R Squared = .8617
R Squared = .8728
Multiple R = .9342

Standard Error of Est. = 77.1981
Predicted Limerick Cost $/kW = 2590

Sources and Notes

1. Installed cost per KW is measured in total installed cost (including AFUDC) per KW of capacity. Source is Alabama Power Co.'Power Plant Cost Trends' -January 1984 and 1983 TVA Survey of Nuclear Plant Costs.

2. C.O.D. is year and month of commercial operation; source TVA and APCO surveys. This variable equals the squared differnece between commerical operation and 1970.

3. Initial Add-On Indicator = 1 for First Unit at Site, 0 for subsequent units.

4. Gross Capacity in Megawatts for the unit.

5. Northeast Indicator = 1 for Utilities in FERC region 1, 0 otherwise.

The 1970 National Power Survey, Federal Power Commission; Part IV
Section 4

A REPORT TO THE FEDERAL POWER COMMISSION

THE METHODOLOGY OF LOAD FORECASTING

PREPARED BY

THE TECHNICAL ADVISORY COMMITTEE
ON LOAD FORECASTING METHODOLOGY
FOR THE NATIONAL POWER SURVEY

1969

FALKENBERG SURREBUTTAL EXHIBIT 8

PREFACE

The need for a comprehensive study of the methodology of electric utility load forecasting has been recognized by many in the electric utility industry and government. No basic reference source has existed covering the data requirements and current methods and techniques used in this area of utility operations. The Federal Power Commission, cognizant of this need, established the Load Forecasting Methodology Committee as one of four technical advisory committees formed to assist the Commission in updating the National Power Survey of 1964. The assignment given this Committee was to determine the state of the art of load forecasting, to assess the need for improved methods and techniques and to suggest means of meeting such need.

The Committee's study of which this report is the result was conducted in cooperation with many segments of the electric power industry. The Committee wishes to express its appreciation to all contributors who have given time and assistance in preparation of this study. There are too many to name here individually, but thanks are due particularly to members of the Commission staff who worked with the Committee, to the Regional Advisory Committees who helped obtain data from utilities and to the many electric utilities who submitted statistical data and other information regarding their operations and load forecasting procedures.

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Federal Power Commission Staff:	
Phyllis H. Kline	Office of Economics
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Jerry R. Milbourn	Committee Secretary

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CHAPTER V—CURRENT FORECASTING METHODS

Forecasting techniques are tools. No single method or group of techniques in itself assures success in forecasting. Knowledge and judgment of the forecaster in applying selected techniques in a given utility load situation are essential. So is final judgment of the elements used in arriving at the ultimate load forecast.

The number and kinds of forecasting methods used vary considerably from utility to utility. Use of several methods is common. Differences in methods result in part from variations in economic and geographic conditions, system characteristics and mix of loads in the utility areas.[1] For example, population may change rapidly in one utility area and be stable in another. Utilities with large cooling loads have an interest in developing estimates of historical cooling loads and load weather relationships and use these in forecasting cooling loads. Utilities serving industrial loads which are highly responsive to the business cycle and which constitute a large proportion of total load usually put more emphasis upon analysis of industrial loads than do utilities serving a stable and small industrial load.

A. Basic Forecasting Methods

Forecasting methods can be grouped into two categories: extrapolation and correlation.

1. Extrapolation

Extrapolation is based upon the assumption that future growth will be a continuation of a discernible pattern of past growth. Specific methods include compound rates of growth, annual increments, fitting of mathematical growth curves and use of graphs of treated or untreated historical data.

Extrapolation often produces acceptable results because electric loads exhibit stable growth over rather long periods. Residential, outdoor lighting and service loads appear to be largely insulated from the business cycle. However, forecasters relying predominantly upon this method may fail to recognize underlying changes which eventually will affect future growth. For example, a succession of very hot summers might mask declining growth in non-air conditioning loads.

2. Correlation

Correlation relates electric power loads to selected associated factors. Correlation methods include scatter diagrams, simple correlation, multiple correlation and simple or complex models. While results from these techniques, especially the more sophisticated methods, cannot be accepted at face value but must be evaluated in terms of the theories underlying the techniques, including their limitations, they provide insight into the causes of past growth and its variation and quantify relationships between load and factors which affect load. This leads to a clearer understanding of the factors which cause growth and of their relative importance. Further, when forecasts deviate from actual loads, the correlation approach is helpful in identifying causes of deviation.

One problem associated with correlation methods is the need to obtain and select forecasts of these associated factors, i.e., independent variables, such as population, income, appliance saturation, etc. There is no assurance that this can be done with any greater accuracy than forecasting electric loads directly. Despite this difficulty, correlation is useful because it forces the forecaster to consider and analyze future load in a context of other factors rather than as a completely independent phenomenon.

It is important, however, that the analyst/forecaster avoid the mistake of drawing conclusions from spurious correlations which have a high degree of statistical significance but no logical relationship.

B. Special Information and Judgment

Although extrapolation and correlation are fundamental to the art of load forecasting, they

[1] Specific forecasting methods employed by four electric systems are detailed in Appendix A.

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R-850152

March 14, 1986

Re: Pennsylvania Public Utility Commission, et al.,
v. Philadelphia Electric Company
Docket No. R-850152

Jerry Rich, Secretary
Pennsylvania Public Utility Commission
New Filing Section
Room B-18 - North Office Building
P. O. Box 3265
Harrisburg, PA 17120

Dear Secretary Rich:

Enclosed for filing with the Commission please find the original and three (3) copies of PAIEUG Exhibit 13 in the above-captioned proceeding.

All parties of record to this proceeding have been provided with copies.

Very truly yours,

McNEES, WALLACE & NURICK

By David M. Kleppinger
David M. Kleppinger

DMK/jf
Enclosure
HAND DELIVERED

cc: All Parties of Record (w/encl.)

<u>CERTIFICATE OF SERVICE</u>

Pennsylvania Public Utility Commission, <u>et</u>. <u>al</u>., :
v. : Docket No. R-850152
Philadelphia Electric Company :

I hereby certify that I have served a copy of the foregoing document on all known parties of record to this proceeding, all by first class mail (unless otherwise indicated) properly addressed as follows:

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David M. Kleppinger

David M. Kleppinger

Dated this 14th day of MARCH, 1986, at

Harrisburg, PA

Q. Please provide the A allocators you developed for the OCA allocations with PAIEUG 4CP demands.

A.

A-Series Allocators for OCA Allocations with PAIEUG 4CP Demands

	A1	A1a	A2	A3	A4
Total	1.00000	1.00000	1.00000	1.00000	1.00000
High Tension	0.41462	0.41899	0.35927	0.35064	0.00000
Primary	0.09286	0.09384	0.08727	0.08845	0.14603
Secondary	0.14172	0.14321	0.11527	0.11682	0.19275
Rate RH	0.03919	0.03960	0.09465	0.09593	0.15848
Rate R	0.27630	0.27921	0.27725	0.28098	0.46651
Rate OP	0.00003	0.00003	0.01412	0.01431	0.02376
Street Lighting	0.00005	0.00006	0.00686	0.00696	0.01153
Septa & Amtrak	0.01814	0.00779	0.03011	0.03052	0.00000
Other Utilities	0.01492	0.01508	0.01326	0.01344	0.00000
Inter Departmental	0.00217	0.00220	0.00192	0.00195	0.00093

	A5	A5a	A6	A7a	A7b
Total	1.00000	1.00000	1.00000	1.00000	1.00000
High Tension	0.25104	0.25569	0.00000	0.00000	0.00000
Primary	0.06040	0.06152	0.00000	0.00000	0.00000
Secondary	0.15119	0.15399	0.22602	0.17252	0.17330
Rate RH	0.12431	0.12661	0.18626	0.13288	0.13366
Rate R	0.36592	0.37270	0.54611	0.65063	0.65363
Rate OP	0.01864	0.01899	0.02781	0.03911	0.03930
Street Lighting	0.00907	0.00924	0.01350	0.00475	0.00000
Septa & Amtrak	0.01818	0.00000	0.00000	0.00000	0.00000
Other Utilities	0.00000	0.00000	0.00000	0.00000	0.00000
Inter Departmental	0.00125	0.00127	0.00030	0.00010	0.00010