DEL RESEARCH CORPORATION

226 Woodbourne Dr.
St. Louis, MO 63105

August 26, 1981

Mr. Stefan S. Pawlicki
Chief of the Material Engineering Branch
Division of Engineering
U. S. Nuclear Regulatory Commission
Washington, D.C. 20555

Dear Mr. Pawlicki,

Thank you for receiving my visit at N.R.C. today and for taking the time to discuss the A-2 problem. Indeed, I am writing this letter to record my opinions as expressed at our meeting.

It is my understanding that the Westinghouse Owners Group A-2 report is about to be accepted based on an internal review, principally authorized by Mr. R. Gamble. In order to form his own opinion, Mr. Gamble requested my assistance for making some background analysis to check the computations in the Owners Group report. The computations were linear-elastic fracture mechanics computations using a postulated 7" crack size (rather small for a 3+ inch thick pipe wall) and for a maximum code load somewhat below the yield moment of the cracked cross section. My analysis included other crack sizes but used maximum code loads since information was insufficient to go further. The involvement of elastic-plastic fracture mechanics (and "tearing instability theory") is only in the materials property evaluation in the methods used. Now such an approach is appropriate, if the flaw size and loads are strict upper limits for all the systems considered. However, it is my opinion that this may not be the case, as expressed to Mr. Gamble personally and in my cover letter to EG & G with my report.

Therefore I am reiterating my concerns here with additional explanation. Assuming that a cracked section forms a plastic hinge and subsequently showing crack stability, is an assurance of ductile overall structural behavior and load redistribution, etc. in a system such as a primary system pipe. The computation of crack stability requires considering the global statically indeterminate nature of the pipe, the motion of its supports (vessel and steam generator) and the possible plastic action: load redistributions,

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hinge formations (if more than at the cracked section) and displacement limits which ensue. The Westinghouse Owners Group report (and my own analysis as requested by Mr. Gamble) made no such evaluation. The computations which were done were limited to local loads and small flaw sizes with a linear-elastic fracture mechanics evaluation methodology.

The code of Federal regulations, 10 CFR 50, contains criteria, which state that you design using the "code" loads then you also assume that L.O.C.A. can occur. Hence the position that you can use "code" loads to show that L.O.C.A. does not occur is absurd and based on a circular argument, etc. Indeed, it is my opinion that it is clearly not accepted by the code.

Indeed, more specifically in the many NUREGS in which Newmark develops the earthquake analysis methods and others which discuss his analysis for code load usage, the preservation of overall structural ductility, (i.e. ability to sustain plastic hinge formation) is stated as required and assumed to be so in allowing their analysis to be regarded as reasonable for code approval. Therefore, the maximum code loads are appropriately used only if either structural ductility is preserved or L.O.C.A. is assumed as a possibility. I reiterate my opinion — that using code loads to supposedly show that a L.O.C.A. cannot occur is a circumvention of the stated philosophy under which the code loads were developed and again clearly not acceptable!

On the other hand, upon being told in your office that the "staff position report" (on the Westinghouse Owners Group report) contains J and T and ω and other trappings of "tearing instability theory", implying that tearing instability analysis was used is genuinely ironical. It was used only to convert material property test information to L.E.F.M. terms for an L.E.F.M. analysis. But a full tearing instability analysis to assure structural ductility was not done, even though I know that others are doing such analyses of primary systems. Hence, it appears that your staff position report my be misleading. Indeed your staff and Westinghouse both know that such analysis is possible, since last November Dr. Johnson had me give a presentation, including a description of these primary system piping analyses, at NRC and Mr. Chirigos of Westinghouse was present (the presentation was taped). Consequently, as an author of "tearing instability analysis" I hope you understand my concern that it may be a bit misused in this case where its "trappings" but not its "substance" is associated with a staff position.

Let me further state that my main reason for this letter is to personally clarify my position on this matter with you, though it is mostly a reiteration of our discussion of yesterday. As a consultant to NRC through our EG & G contracts, it is only reasonable that you should know exactly where I stand on such matters. A copy of my original report to EG & G on this matter is also enclosed so that you may note that it is not a change in stance, though this letter reinforces it.

Very best personal regards to you and your staff.

Sincerely yours,

Paul C. Paris
President

cc: Robert Hermann, NRC
Bert Barnes, EG & G

DEL RESEARCH CORPORATION

226 Woodbourne Drive
St. Louis, Missouri 63105
(314) 726-2942
9 May 1981

[redacted]

Contract Administration
EG&G Idaho, Inc.
P.O. Box 1625
Idaho Falls, Idaho 83415

Attn: Ms. Diane Dawsey, Subcontract Administrator and
Mr. B. L. Barnes, Program Specialist; NRC Support Branch

Dear Ms. Dawsey and Mr. Barnes,

Enclosed is a final format report under Subcontract No. K-8195 (Modification No. 3), FIN No. A6432, for assessment of the Westinghouse Owners Group report on primary coolant system cracking. The draft materials behind this report were submitted to Mr. Ron Gamble of NRC directly in the form of curves and notes, during a visit of Dr. Paris to NRC 17 February 1981. At that time, Mr. Gamble indicated that the method of assessment and initial results satisfied the basic needs of NRC, hence work since that time has consisted of reverifying the analysis (checking methodology) and formalizing the results leading to the enclosed report. Thus, this report is understood by us to satisfy all requirements under FIN No. A6432.

However, a technical point as emphasized in the report may lead to further requested review by NRC in this matter. In particular, the loads we were instructed to consider were "maximum code loads" as supplied by NRC apparently from Westinghouse's computations, and in our report we imply that limiting consideration to "code determined loads" may be inadequate.

Indeed, in considering a L.O.C.A. associated problem which is not a within code expected condition, it seems ironical to us to limit consideration of maximum loads to code determined loads. Therefore, though the system is found to be adequate within the (code) loads which we were instructed to use in the analysis, the analysis does not leave us feeling satisfied for the type of extremes of loading (such as massive earthquake) which is the real concern for anticipation of L.O.C.A. in our opinion. Therefore, we anticipate that NRC may well request further more elaborate and conservative analyses than those done by Westinghouse and ourselves to date, for a more prudent evaluation of the problem.

Nevertheless, we submit the current report as satisfying current and requested requirements. It has been our privilege to participate with EG&G in supplying this analysis.

Sincerely yours,

Paul C. Paris
President

PCP/tp

Encl: "A Brief Letter Report....."

DEL RESEARCH CORPORATION

226 Woodbourne Drive
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63105

A BRIEF "LETTER REPORT" OF AN ANALYSIS OF THE PWR "ASYMMETRIC LOAD PROBLEM" BASED ON INFORMATION FROM THE "WESTINGHOUSE OWNERS GROUP"

Prepared by Paul C. Paris and Hiroshi Tada under Subcontract K-8195 to Del Research Corporation from EG&G Idaho, Inc.

Prepared for Mr. Ronald Gamble of U.S.N.R.C. and transmitted verbally with notes during visit to U.S.N.R.C. February 17, 1981.

Introduction And Approach

The approach of this analysis of the so called "asymmetric load problem" (induced by assuming the severance of a primary coolant pipe) is to attempt to show that a primary pipe cannot be severed (or partially severed with a large enough crack opening) so as to cause large asymmetric loads to occur or to otherwise impede safe operations. The analysis was to be performed under limitations of both time and a limited knowledge of actual plant arrangements and detailed constraint information from each plant on all primary components (i.e. vessel, steam generator, pump and primary pipes). Only some typical component arrangement information and pipe size information was available, as will be noted later.

Therefore, a full (elastic-plastic) tearing instability analysis of these systems could not be done. On the other hand, by assuming that "maximum code loads" (axial force and moments in pipes) could be used, a plastic zone corrected linear-elastic-fracture-mechanics-analysis (L.E.F.M.) combined with elastic-plastic fracture mechanics material properties test information was found plausible for (up to) crack sizes smaller than those inducing the limit moment condition for the remaining cracked cross section. A similar analysis method [1] has been of assistance in analyzing the "A-11 problem" where the loads, internal vessel pressures, have absolute limits, such as the head bolt yield limit or safety relief limits. The current analysis herein will show a large margin on crack stability for those limited loads and crack sizes, i.e. for loads limited to code loads and crack sizes limited to mainitaining L.E.F.M. conditions.

Further, for those crack sizes and loads analyzed, since the cracks are assumed to be through cracks, it is evident that leaks are inherently a consequence of the assumptions and thus become a secondary subject of the analysis. This report does not address the fluid mechanics of leakage, but an attempt was made to conservatively estimate the crack opening (leakage) area for both normal operating loads and for "maximum code loads" so that assumed leaks could be assessed by others (fluid mechanics specialists). It is noted here that leak rates and characteristics are of interest both for assessing the plausibility of crack detection and for assessing their effect on operations (shutdown) after a significant event causing maximum loads.

Of course other potential methods were considered and attempted but found not as plausible or tenable within the limits of information available and required analysis assumptions. These alternatives will not be discussed here and thus the discussion proceeds to the actual final information used, methods employed and results obtained and transmitted to N.R.C. personnel for consideration of resolution of their immediate concerns.

Specific Information Obtained From N.R.C. And Other Sources And Used Herein

N.R.C. supplied schematic diagrams of typical plants, which are included here as Figures 1, 2, and 3. On these Figures they also supplied typical pipes sizes, specifically the outside diameter (O.D.), the thickness (t) and length (ℓ) for 2 loop, 3 loop and 4 loop plants. In addition they supplied their latest J-R curves for cast stainless steel which the author's compared with and found typical of other published stainless steel data [2]. N.R.C. also verbally acknowledged that this new data was typical of all data to their knowledge and instructed that we should use it along with Reference [2] for the purposes of this analysis. The new data is Figure 4.

Moreover, a considerable discussion of the maximum loads to be used ensued. The result was that N.R.C. requested that it be assumed that the "maximum code loads" be used as the maximum loads to be encountered and for use with all of the plants to be considered, supplied us the values (to be used simultaneously):

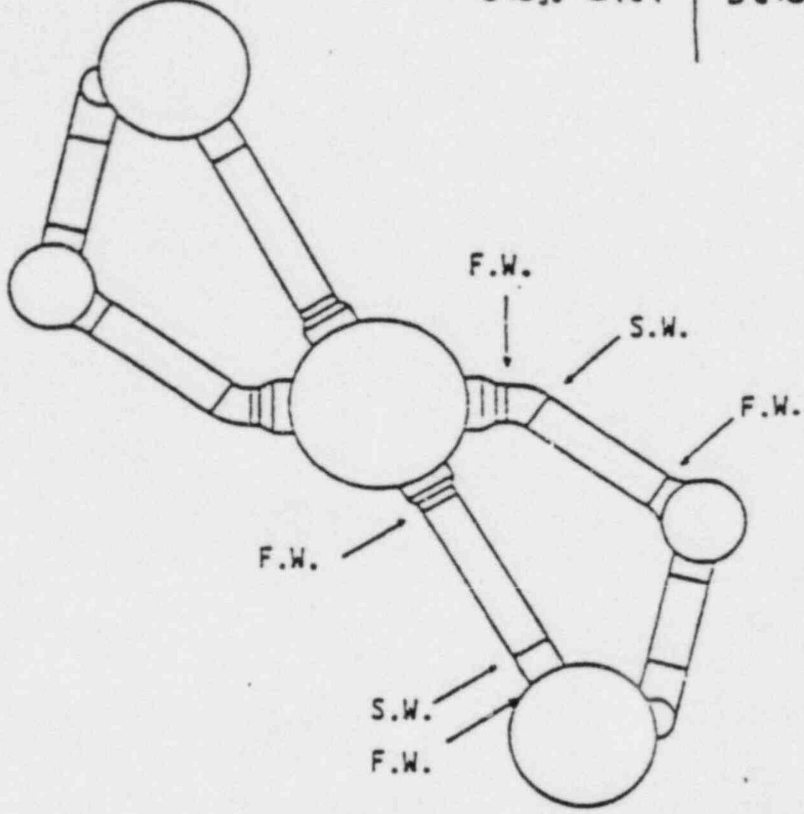
$$P_{max} = 1.8 \times 10^3 \text{ kips}$$

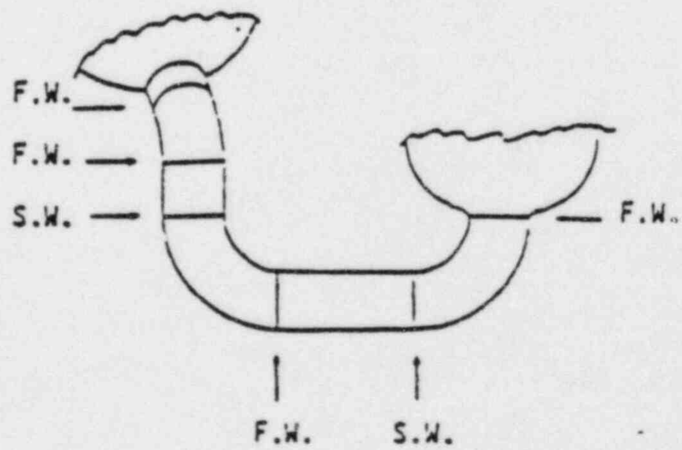
$$M_{max} = 45.6 \times 10^3 \text{ in.-kips}$$

(P = axial force. M = bending moment)

Further, normal operating conditions were given as an internal pressure of 2,200 psi and no bending moment. It is noted that the cold leg cross-sectional properties give maximum stresses when used with these loads and consequently they were used in the analysis to follow. For the purposes

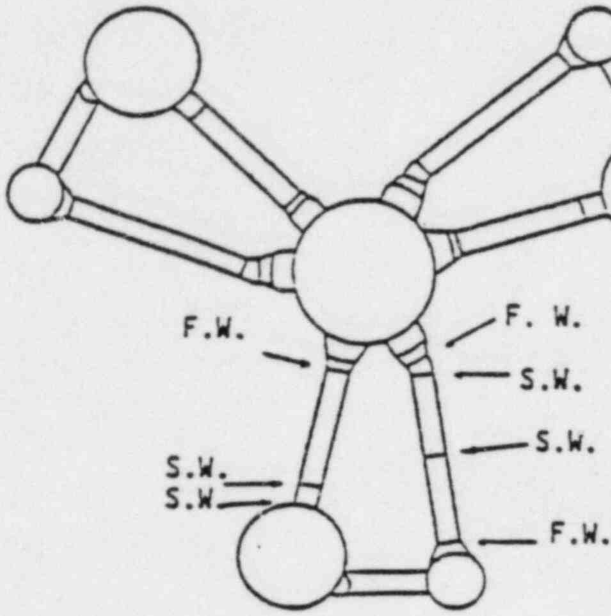
	OD	t	ℓ
Hot leg	34.2"	2.5"	16.4'
Cold leg	32.4"	2.37"	11.3'
Cross Over	36.6"	2.67"	16.4'



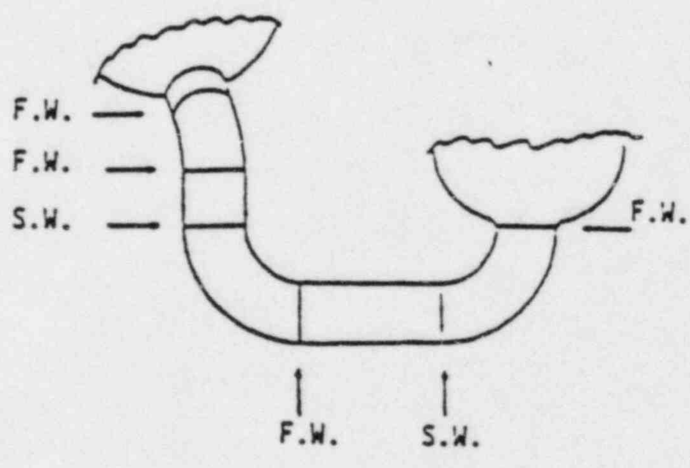


Typical 2 loop Plant

Figure 1



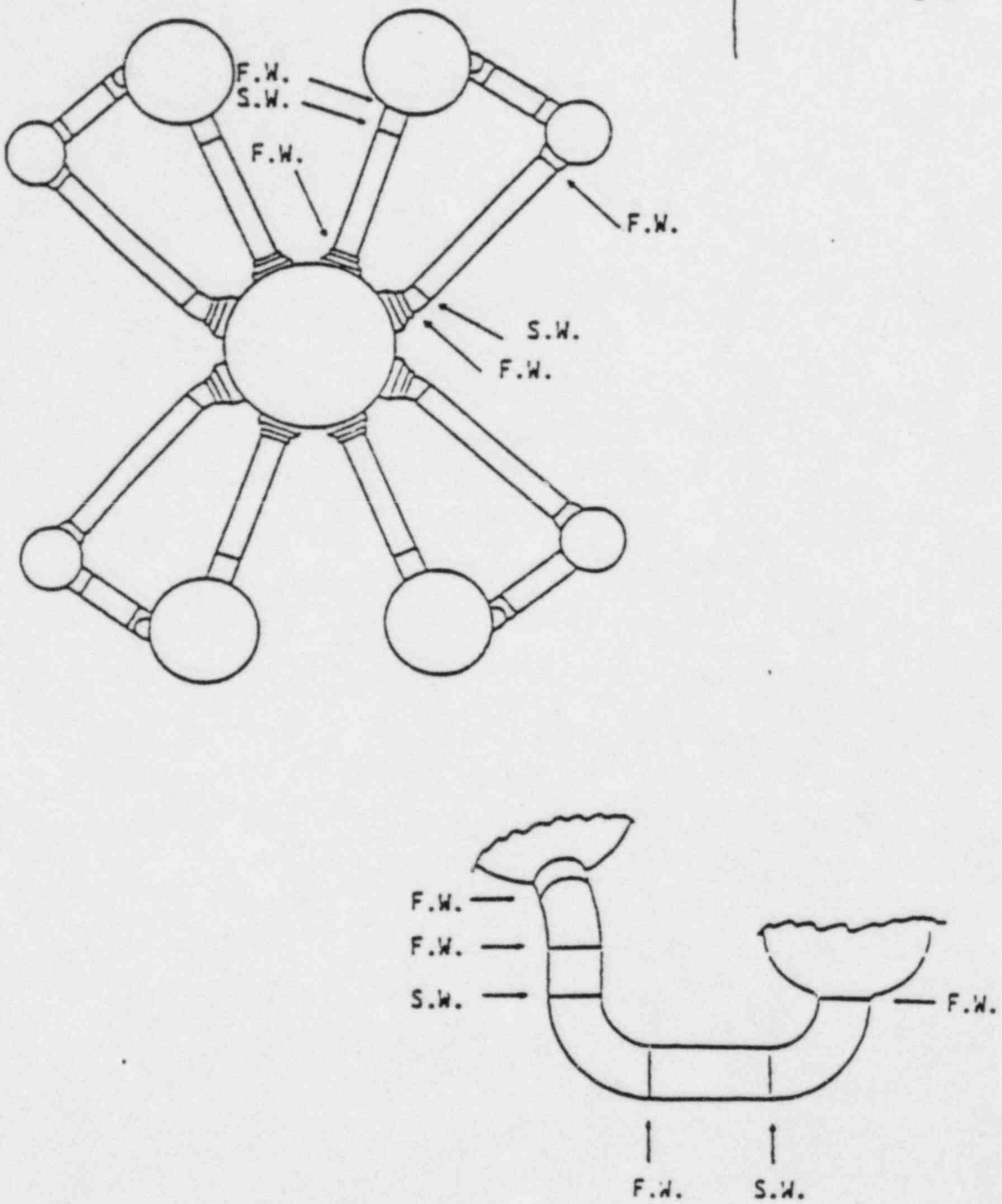
	OD	t	ℓ
Hot leg	34.2"	2.5"	16.8'
Cold leg	32.4"	2.37"	16.9'
Cross Over	36.6"	2.67"	13.9'



Typical 3 loop Plant

Figure 2

	OD	t	ℓ
Hot leg	34.2"	2.5"	16.8'
Cold leg	32.4"	2.37"	20.3'
Cross Over	36.6"	2.67"	13.4'



Typical 4 loop Plant

Figure 3

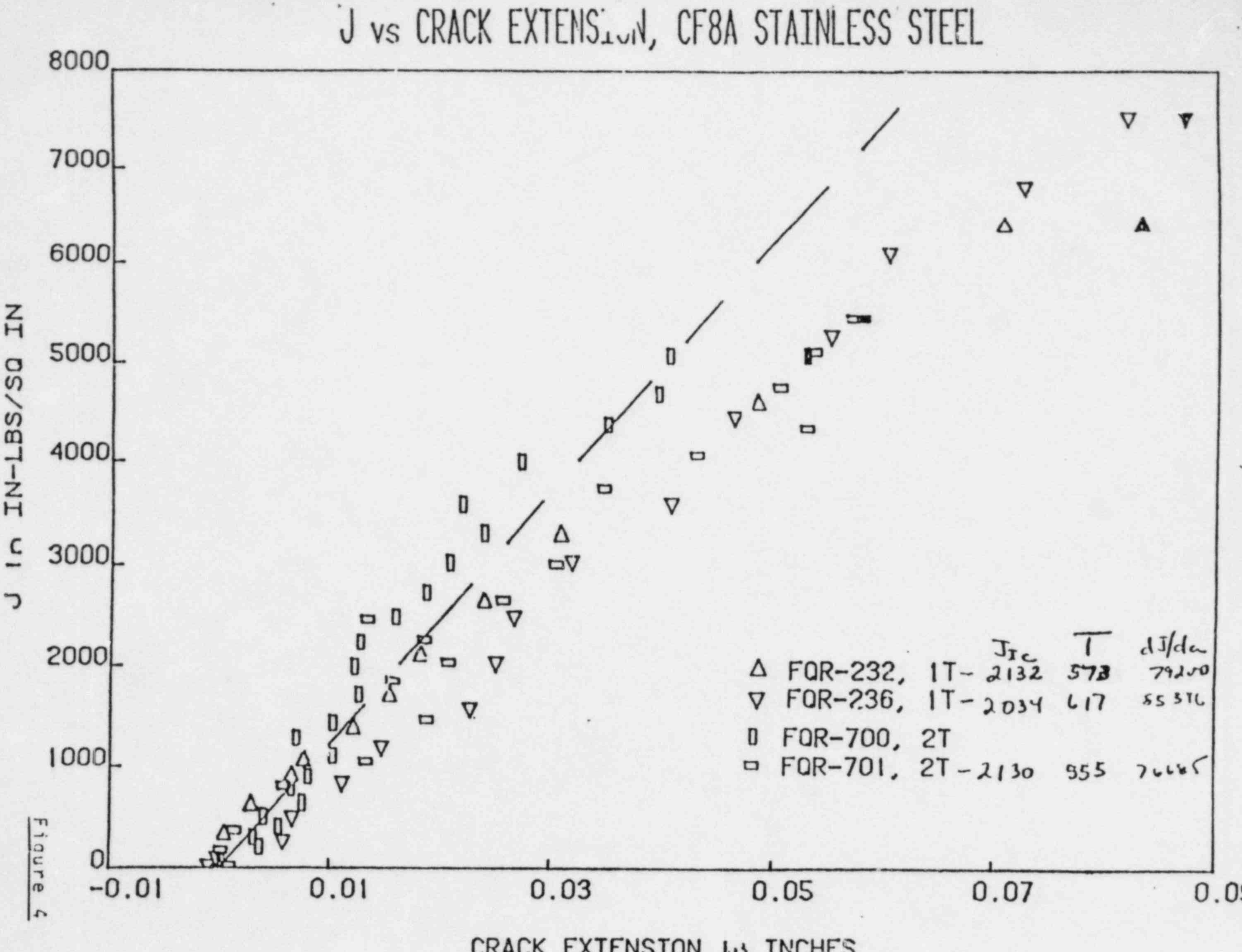

J vs CRACK EXTENSION, CF8A STAINLESS STEEL
J in IN-LBS/SQ IN
8000
7000
6000
5000
4000
3000
2000
1000
0
-0.01
0.01
0.03
0.05
0.07
0.09
CRACK EXTENSION in INCHES
J_{Ic} T dJ/da
FQR-232, 1T- 2132 573 79200
FQR-236, 1T- 2034 617 55
FQR-700, 2T
FQR-701, 2T - 2130 955

Figure 4

of computing crack length plastic zone corrections and limit moments in bending, it was agreed to adopt a flow stress, σ_0 = 60 ksi.

Other information, such as the maximum size and weight of a steam generator, was estimated by N.R.C. for purposes of comments on the applicability of the analysis as found later herein.

The Specific Analysis And Analysis Considerations

Initial analysis estimates for crack stability using the methods in NUREG 0838 [3] indicate that a circumferential through crack in any pipe would be stable if a plastic hinge had just formed at the cracked section. That is to say that the piping loop in all primary systems considered has a low L/D (length to diameter ratio) compared to that which would be critical (indicating crack stability) for typical material properties. Though this initial indication of a high degree of crack stability is required to proceed with confidence, the lack of "as built" constraint information on the primary loop components prohibits a full analysis. The larger diameter of PWR primary system pipes enhances constraint effects and influences in a much more sensitive way than for secondary systems (smaller diameter, generally higher L/D pipes). Lacking that information the method was abandoned in favor of the L.E.F.M. analysis to follow. (It is however, important to say that for the estimates performed, no inherently dangerous situations were implied within loads and crack sizes assumed.)

Subsequently, the analysis fell back to the position of assuming the "maximum code moment and axial force" were present and limiting the crack sizes to be considered to those which leave the remaining cross section below limit moment. Static fully plastic limit moment calculations were used consistant with the methods in NUREG 0838 [3] and these results are plotted on the center curve of Figure 5. This limits the

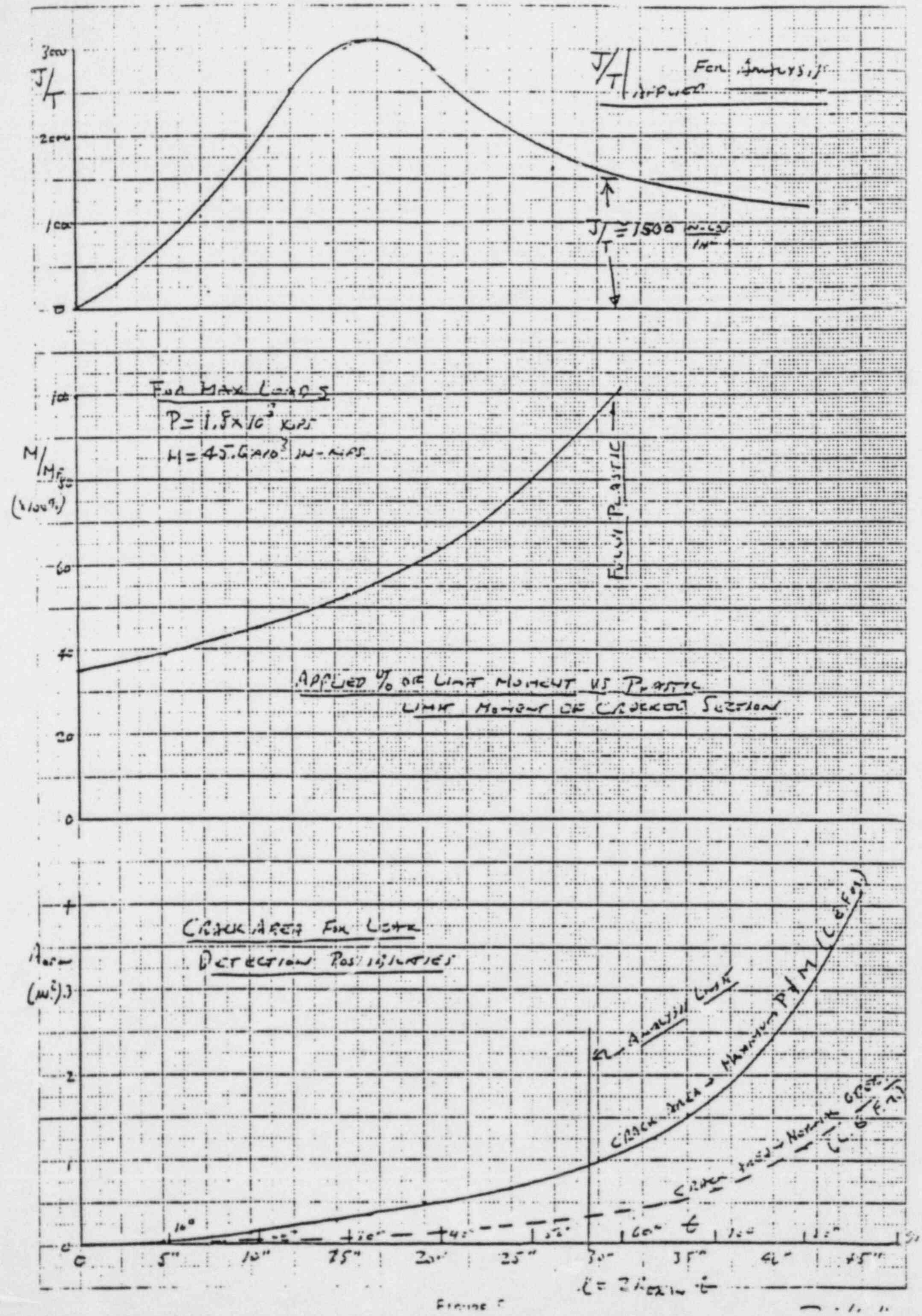

J/T
For Analysis
J/T ≅ 1500
For Max Loads
P = 1.8x10³ kips
Fully Plastic
Applied % of Limit Moment vs Plastic Limit Moment of Cracked Section
Crack Area for Leak Detection Possibilities
Analysis Limit
0
5"
10"
15"
20"
25"
30"
35"
40"
45"

Figure 5

circumferential cracks sizes to a double angle, 2θ, of over 110° or circumferential length (arc length) of about 30 inches. Since this type of stainless steel exhibits a very high degree of hardening, this is taken to be a reasonable upper limit for plastic zone corrected L.E.F.M. analysis.

In order to proceed with a L.E.F.M. analysis, the stress intensity factor, K, solution must be known for tension and bending of circumferentially cracked thin cylindrical tubes. They were not found in the avaliable literature, therefore Dr. Tada developed new asymtotic interpolation solutions in closed form for convenient use in this analysis. His methods were consistant with those employed for similar problems [4]. His results are:

for stresses (tension and bending)

$$\sigma_t = \frac{P}{2\pi R t}$$

$$\sigma_b = \frac{M}{\pi R^2 t}$$

the total crack tip stress intensity is

$$K = K_t + K_b$$

where

$$K_i = \sigma_i \sqrt{\pi (R\theta)} \, \frac{f_i(\theta)}{(1 - \frac{\theta}{\pi})^{3/2}}$$

and subsequently where

$$f_t(\theta) = \sqrt{\frac{2}{\theta}} \left[\sqrt{\tan \frac{\theta}{2}} \, (\cos \theta)^2 (1 - \frac{\theta}{\pi})^{3/2} + \sin \frac{\theta}{2} \, (\sin \theta)^2 \right]$$

$$f_b(\theta) = \sqrt{\frac{2}{\theta}} \left[\sqrt{\tan \frac{\theta}{2}} \, (\cos \frac{\theta}{2})^2 (\cos \theta)^2 \, (1 - \frac{\theta}{\pi})^{3/2} + 0.53 \sin \frac{\theta}{2} \, (\sin \theta)^2 \right]$$

Then converting the L.E.F.M. formulation to consistant use with J-integral R-curve material properties, the relation between the applied J and K was used

$$J = \frac{K^2}{E}$$

Using the values cited earlier these equations were first used to obtain he "uncorrected elastic" J-curve on Figure 6.

For correcting the full arc crack length, ℓ, the usual plastic zone correction is used

$$\ell_{eff} = \ell + 2r_y$$

where

$$r_y = \frac{1}{2\pi} \left(\frac{K}{\sigma_0}\right)^2 = \frac{JE}{2\pi\sigma_0^2}$$

At a given J level for the uncorrected curve on Figure 6 the ℓ_{eff} is the ℓ which would make the plot correct. Therefor revising these equations,

$$\ell = \ell_{eff} - \frac{JE}{\pi\sigma_0^2}$$

shows that for a given J level the final term $JE/\pi\sigma_0^2$ should be subtracted from the uncorrected crack length to give the plastic zone corrected elastic J-curve on Figure 6. It is noted that the corrected curve is not diverging from the uncorrected curve until well beyond the "analysis limit" at 100% of limit moment. This implies appropriate results well away from the regime where primary plastic instability (plastic zone instability) might occur [5].

Now, for circumferential cracks in pressurized thin cylinders, a shell bending effect magnifies the K or J contribution for pressure but does not appreciably effect bending values [1]. For the longest cracks analyzed here, the maximum effect on K_t is less than 1.4 and for the axial force bending moment combination K_t is far less than 1/2 of the total K so the error in J (magnification) is less than 35% for worst case assumptions. Indeed, bending also tends to inhibit this non-linear effect due to internal pressure, so a better estimate would be to say the J values are within 20% estimates of actual values at longer crack lengths (ℓ = 30") and much better at shorter crack lengths.

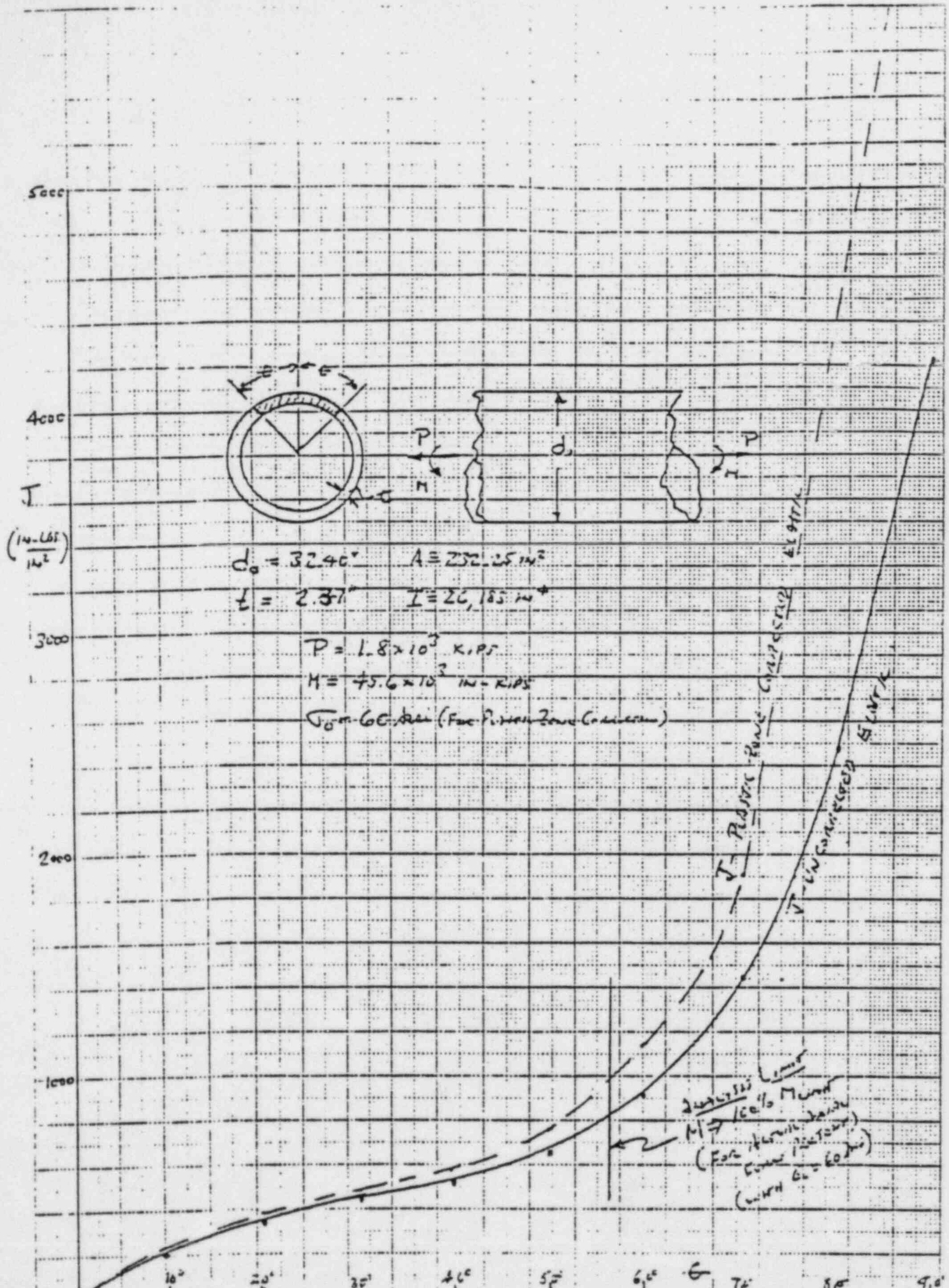


Figure 5

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Finally, the maximum J value noted on Figure 6 at the analysis limit is about 1000 in/-lbs./in.^{2} which is noted to be well below the J_{Ic} of about 3000 in.-lbs./in.^{2} which is implied by Figure 4. That means that within the assumptions of these computations, J is below J_{Ic} so no crack instability can occur since crack initiation has not yet occured. But further, if we are ultimately cautious and presume that crack growth does begin at these lower levels of J with the tearing modulus, T, values typical of those higher in J on the R-curve, Figure 4, our margin against instability is further (very conservatively) assessed. Appropriately differentiating the values on Figure 6 for J-applied to get T-applied, subsequently one may compute

$$J/T \Big|_{applied} = \frac{J\sigma_0^2}{(\frac{dJ}{da})E}$$

for each crack length and plot them as on the upper curve of Figure 5. It is noted that the values for crack lengths approaching the analysis limit exceed 1500 (in.-lbs./in.^{2}). Now, the studies in [1] and comparable analysis of J-R curves for this stainless steel indicate that J must exceed several times J_{Ic} before crack instability can occur for J/T exceeding 50 (in.-lbs./in.^{2}) which is a very large margin below the 1500 (in.-lbs./in,^{2}) found here. Therefore assuming J_{Ic} is exceeded also leads to predicting no crack instability here, provided the assumptions on loads and crack sizes are not exceeded.

After finding no crack instability possible for the assumed conditions, it then becomes relevant to consider the crack opening area (C.O.A.) for both the maximum loads and normal operating loads. This can be done approximately by a crack closing compliance calculation (e.g. see [4]). The appropriate calculation is made using

$$C.O.A. = \frac{2}{\sigma_{ave}} \int_0^{\ell} J d\ell$$

where J is obtained from Figure 6 for calculations for maximum loads and from like estimates for normal operating loads. The σ_{ave} in the formula is the weighted average stress on the crack surface with the crack pulled closed (or crack absent) which is slightly overestimated to be 28 ksi for maximum loads (i.e. simply P/A + Mc/I). For normal operating conditions it was 7.7 ksi (i.e. simply $\frac{pR}{2t}$). The resulting crack opening areas for various crack lengths are plotted as the lower curves on Figure 5. The significant values are noted as from less than one square inch to less than one-tenth of a square inch.

Consequently, the analysis shows that under "maximum code loads" and for crack sizes which assure that limit moment conditions will not be reached at the cracked section, the primary piping seems very safe against unstable crack propagation. Under such conditions a double ended pipe break is not plausible by very wide margins and the resulting crack opening areas are relatively small (i.e. less than 0.1% of the area for a double ended break).

<u>Remaining Questions On The Analysis Assumptions And Data</u>

The writers of this report simply accepted from N.R.C. the assumptions of a through-the-thickness crack and numerical values for the "maximum code loads". Some comments on these assumptions seem appropriate as they might effect limitations of the type of analysis employed.

First, most of the piping cracks experienced to date are from stress corrosion effects at welds due to in part at least welding residual stresses. In such cases a strong tendency toward part through cracks forming around the entire circumference is often demonstrated. Taking initial concern for a shallow part through crack all around along with a limited through-the-thickness crack (as analyzed), the stability of the through crack is not unlike the situation in face grooved test specimens, as far as effects of the part through cracking is concerned. Since face grooved test specimens of this stainless steel have been used to develop the J-R curves in Figure 4, this matter is of little effect on apparent material properties. Nevertheless, the fact that cracks are most often found in or near welds leads to some relevant concern that very little data is available on stainless steel for weld metal and the effected nearby material.

Moreover, part through cracking around the circumference of a pipe may considerably reduce the cross section. Indeed the limit moment limitations of this analysis should be reduced proportionately by the % of all around surface cracking which might occur. That is to say the middle diagrams curve on Figure 5 should be raised in inverse proportion to the % of thickness remaining with all around surface cracking present and could invalidate L.E.F.M. analysis.

Of even larger concern here are the assumptions in using the "maximum code loads" which were numerically stated without information on how they were otherwise obtained. Indeed, for concern about massive earthquakes, it is noted assuming that snubbers often fail then for a steam generator supported at its base by stiff piping, a lateral acceleration of 1/2 g induces bending moments considerably larger than those given for the analysis primary piping here. Indeed this is perhaps the greatest uncertainty in this analysis. Therefore it is emphasized that consideration for situations where loads exceeding limit moment are experienced, i.e. plastic hinges are formed, have not been made here. Sufficient information was not available to contemplate such an analysis. No other questions are known to these authors by which this analysis is seriously challenged.

Paul C. Paris
President
Del Research Corporation
6 May 1981

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[2] W. H. Bamford and A. J. Bush, "Fracture Behavior of Stainless Steel", A.S.T.M., S.T.P. 668, 1979, pp. 553-557.

[3] H. Tada, P. Paris and R. Gamble, "Stability Analysis of Circumferential Cracks in Reactor Piping Systems", U.S.N.R.C. Report NUREG ~~0311~~ C838, June 1979.

[4] H. Tada, P. C. Paris and G. R. Irwin, "The Stress Analysis of Cracks Handbook", Del Research Corporation, 226 Woodbourne Drive, St. Louis, Missouri 63105, 1973.

[5] P. C. Paris "CSNI Specialists Meeting on Tearing Instability", U.S.N.R.C. Report NUREG/CR0010 (CSNI Report No. 39), January 1980.